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**LARGE TELESCOPE
EXPERIMENT PROGRAM (LTEP)**

VOLUME I PART 2

PERKIN-ELMER

PERKIN-ELMER

OPTICAL GROUP NORWALK, CONNECTICUT

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2-METER LTP SPACE SHUTTLE APPLICATION

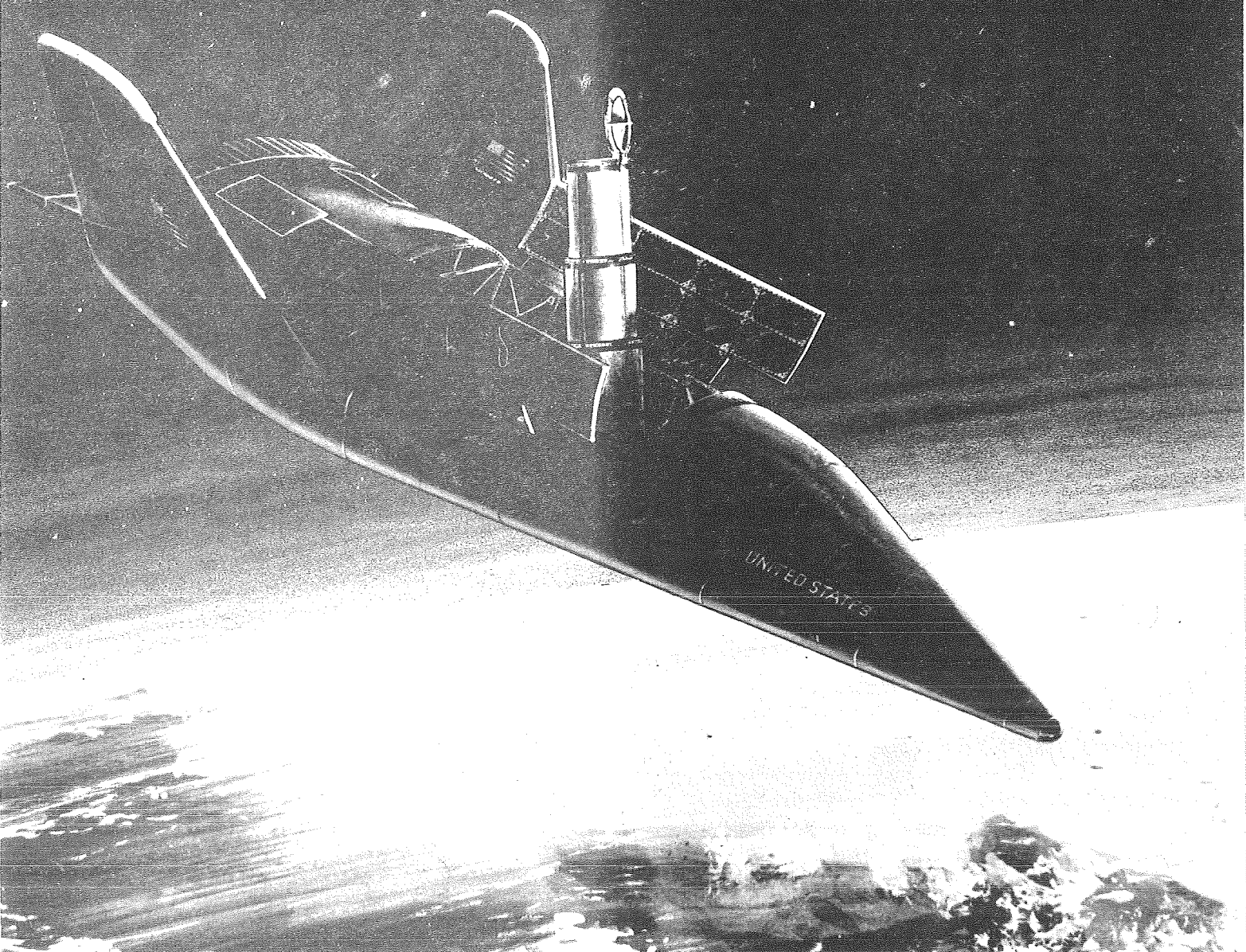


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CHAPTER 13 - OPTICAL TECHNOLOGY EXPERIMENTS

EXPERIMENT NO. 1 - SEGMENTED PRIMARY MIRROR

Experiment Objective

The concept of Active Optics for large astronomical telescopes consists of measuring the optical figure of the primary mirror; computing any errors; and automatically realigning the mirror to its original design figure, either by positioning pieces of a segmented mirror (Figure 168) or by flexing a relatively thin mirror as discussed in Experiment 7. A variety of optical figure monitors have been evaluated for the figure sensing; however, interferometric measurements are the only ones that produce quantitative data with the required accuracy and short response time required for an automatic control system.

The fundamental objective of this experiment is to provide an engineering evaluation of the most promising technical approaches to achieving diffraction-limited performance of space optical systems leading to the space telescopes of the future with apertures of 200 to 400 inches. The individual segments would be aligned to each other to form the diffraction-limited 2-meter aperture. The position of each mirror segment would be sensed by the mirror figure sensor (a phase-measuring interferometer) and moved to the proper location by the Active Optics actuator servo mechanism. The figure of each individual segment would also be individually monitored during the operational life time of the LTP to determine mirror material thermal and dimensional stability.

The entire instrument would be useful as a large diffraction-limited telescope for evaluation of the astronomical observations which can be conducted with an Active Optics segmented primary mirror.

Experiment Justification

The justification for using an Active Optics primary in the 2-meter OTES is based on the need for a large, diffraction-limited orbiting astronomical telescope. The advantages of having such a telescope above the earth's atmosphere have been described in the technical press. Quoting a recent publication* of the Ad Hoc Committee on the Large Space Telescope of the Space Science Board.

"The image of a point source should be nearly as sharp as is permitted by the finite wavelength of light and held steady with respect to the photon-analyzing instruments. The field of view over which good images are obtained should be as wide as feasible. Sharp imagery is the second objective** in placing a telescope above the atmosphere, whose motions and thermal microstructure corrugate the wavefront, with a resultant loss of phase coherency, together with spreading and distortion of the image."

* Scientific Uses of the Large Space Telescope, Space Science Board. Nat'l. Academy of Sciences, Washington, D.C., 1969.

** The other objective was extended spectral range for primary optics.

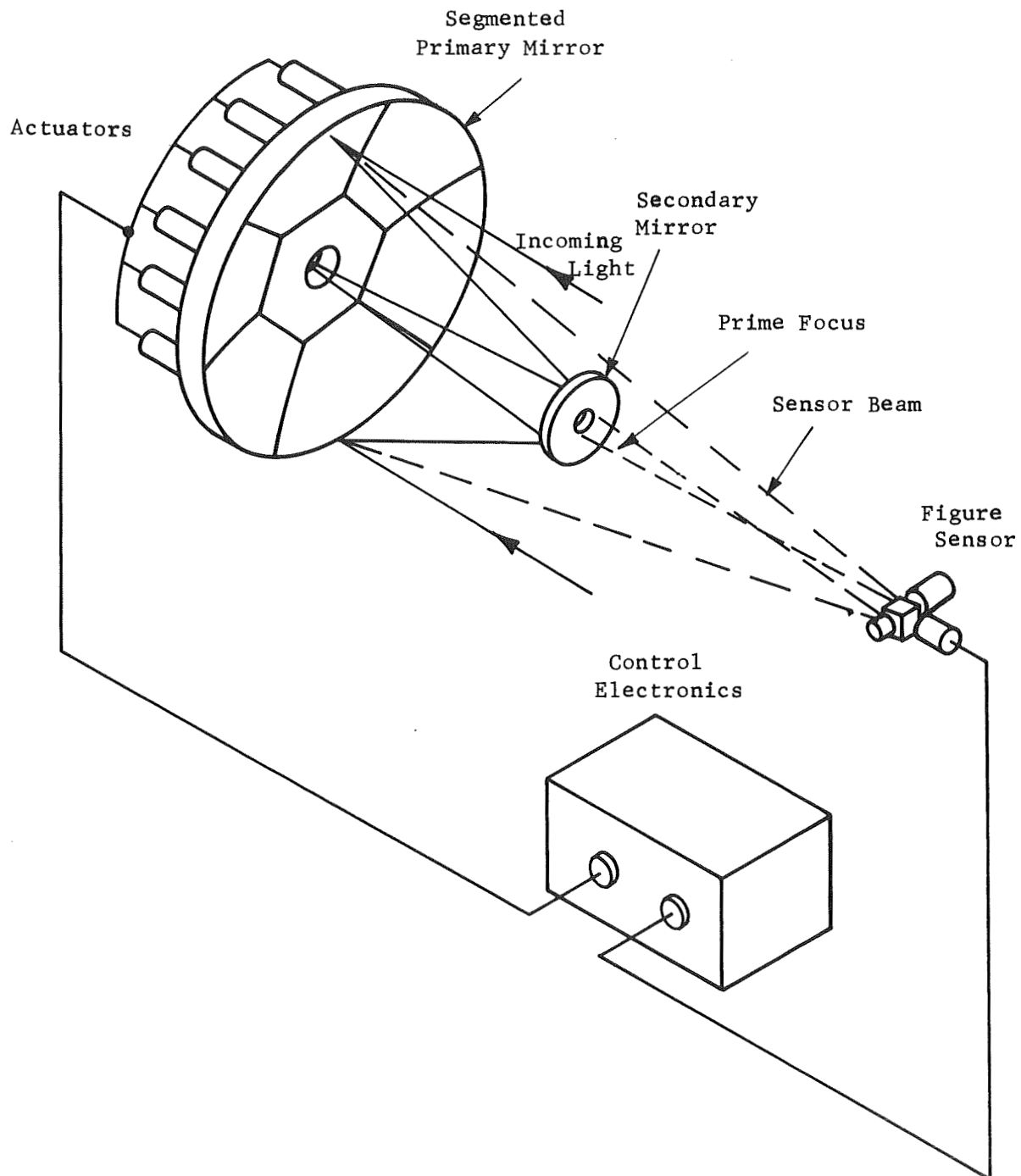


Figure 168. Active Optics Concept

Active Optics is the most promising technique for fulfilling this need by making possible the construction of a large telescope mirror which can maintain its figure to the diffraction-limited quality required.

There are important advantages of an active segmented optics primary mirror.

- A diffraction-limited large mirror can be manufactured
- Weight saving in large mirrors.
- Maintenance in space of primary mirror by astronaut
- "Launchability" of a giant mirror by assembly of segments in space.

The 2-meter aperture LTEP will function as a precursor to the 3- to 5- meter NASO. The 2-meter telescope flight will be low altitude, concentrating on optical technology and manned observatory operations. The importance of the 2-meter LTEP stems from the space experiments planned and the solutions to questions which cannot be answered until the flight data is reduced. The justifications for the 2-meter LTEP telescope and the optical experiments are listed below:

- Resolution of the daylight astronomy question.
- Resolution of the low-high altitude orbit question for space telescopes.
- Resolution of the primary mirror material long-term dimensional stability and thermal distortion tolerances.
- Help in resolving the astronomy mission for a National Astronomy Space Observatory.
- Provision of space data on telescope suspensions for the next generation of less than 2-meter space telescopes.
- Resolution of some of the manned astronomy questions including the duties and performance limits of astronauts working with large space telescopes.
- Provision of space maintenance data in a large telescope conceived and designed from the very beginning for in-space flight maintenance.

The flight experiment of the 2-meter telescope for collection of optical technological data is even more useful to NASA because the 2-meter LTP is able to function as a 2-meter telescope with diffraction-limited performance. Thus, it can also be used for astronomical observations during the experiment phase after the launch and will be available for full time astronomy use at the conclusion of the optical technology experiments.

Recent Technology Advances in the Active Optics Concept

The concept of aligning three segments of a spherical mirror has been demonstrated successfully at Perkin-Elmer* and has been discussed in detail in the previous OTES reports.** This section discusses technology advances[†] in the design concept for key elements in an optical figure sensor which are suitable for eventual use in a spaceborne active optics telescope. The specific objectives are to develop: 1) a solid state optical frequency shifter, 2) a technique for measuring a parabolic primary mirror, and 3) a concept for measuring the alignment of the secondary mirror. The basic figure sensor employs an interference phase measuring interferometer combined with electronic circuits for making measurements in real time.

Two approaches were investigated for the frequency shifter:

1. Doppler frequency shift by mechanical translation of a reference reflector
2. Rotating $1/4\lambda$ plate in circularly polarized light, where rotating birefringence effect is produced by a rotating shear stress wave in an electro-acoustical-optical device.

The Doppler approach was selected because of its simplicity and ease of manufacture. The breadboard unit consisted of a reference mirror mounted in a piezoelectric cylinder. A triangular-waveform electronic drive generates a cyclic displacement of 4λ per excursion at a rate that produces an optical frequency shift of ± 500 Hz.

*Robertson, H.J.: Active Optical System for Spaceborne Telescopes. NASA CR-66297, The Perkin-Elmer Corporation, Oct. 1966.

Robertson, H.J.: Active Optical System for Spaceborne Telescopes Final Report. NASA CR-66489, The Perkin-Elmer Corporation, Dec. 1967.

**Wischinia, H.F.: Optical Technology Experiment Satellite. Perkin-Elmer Engineering Reports Nos. 8500 and 8900.

[†]Many of the design concepts were reduced to practice in Contract NAS20-2012 monitored by ERC.

Three approaches were investigated for the aspheric measurement technique:

1. An aspheric reference reflector that generates wavefront distortions identical to the optical path difference between the parabola and a reference sphere.
2. A null corrector that generates spherical aberrations that are exactly complementary to the spherical aberrations of a parabola when viewed from its paraxial center of curvature.
3. Analysis of the moiré pattern resulting from the superposition of a real and a calculated fringe pattern resulting from the optical path difference between the parabola and a spherical reference reflector.

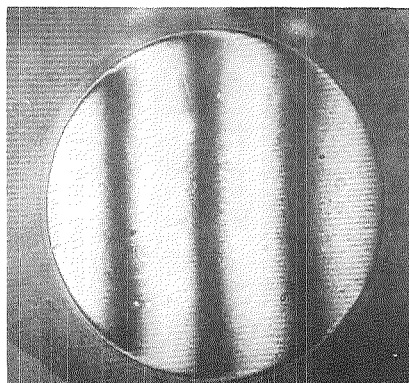
The moiré pattern approach was selected because it offered greater flexibility and fewer optical elements that might introduce figure errors, and the calculated fringe pattern is relatively simple to manufacture. A reference pattern was designed and made to measure an 8-inch f/1 parabola. Tests showed that the technique can be used with the phase measuring interferometer with no added complexity. Tests with good f/1, 8-inch parabola showed that the technique is practical, but more calculations must be performed to make a perfect reference pattern.

An optical arrangement was investigated for measuring the secondary alignment with the primary mirror figure sensor. Calculations indicate that tilt alignment can be measured with the required accuracy. Axial alignment must be monitored at the instrument focal plane.

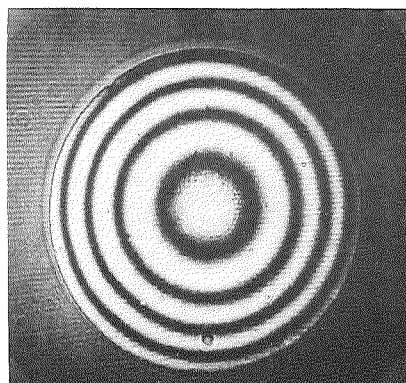
A recommended optical arrangement and block diagram has been developed for a working model figure sensor that uses the Doppler frequency shifter and moiré pattern analysis.

Aspheric Mirror Phase Measuring Interferometer

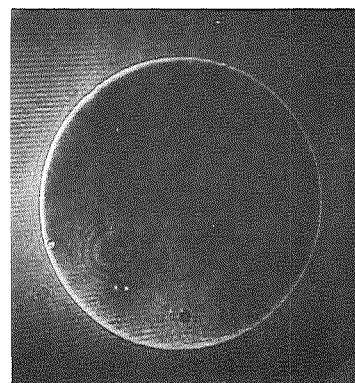
In the active optics application, one beam of a Twyman-Green interferometer is reflected from a plane reference reflector; the second beam is converted by a lens to a diverging spherical wavefront. (See Figure 169.) The figure sensor is located so that the diverging wavefront is concentric about the center of curvature of the mirror under test. If the mirror has a spherical surface and is aligned so that its center of curvature is located at the center of divergence of the wavefront, the light will be reflected back exactly upon its incident path. It will then pass back through the lens and again become a plane wavefront. Thus, for a perfect spherical mirror, interference takes place at the beamsplitter between two coplanar wavefronts. When there are surface irregularities on the mirror under test, or when it is misaligned, the return wavefront will not be a coplanar with the reference wavefront at the point of interference. For tilt misalignment, the interference wavefronts will be approximately plane but not parallel, producing a fringe pattern consisting of straight lines. For axial misalignment, interference will take place between a plane wavefront and a slightly spherical wavefront. This produces a circular fringe pattern. Small localized figure errors produce localized phase shifts in the fringe pattern.



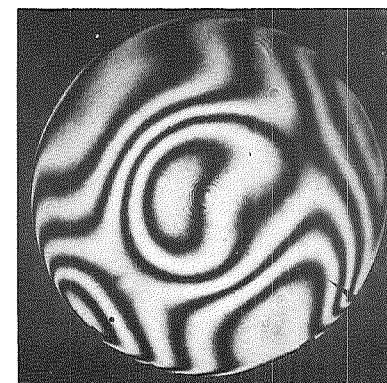
Tilt Error



Axial Error



Correct



Astigmatism

PERKIN-ELMER

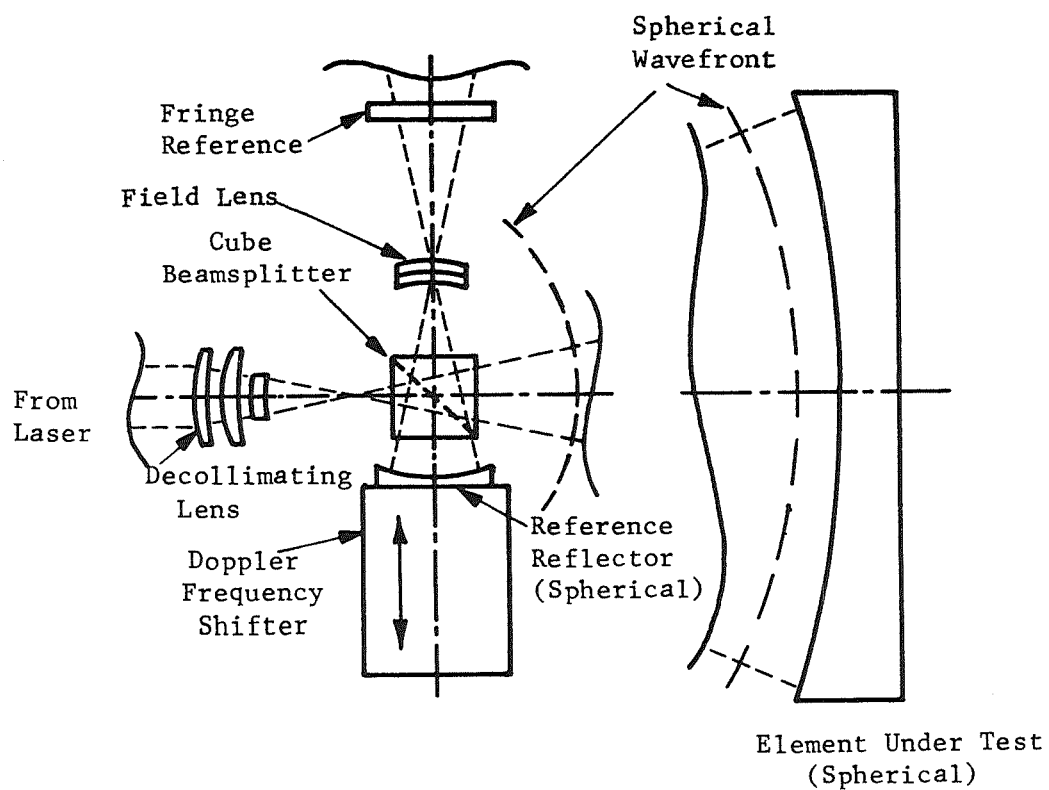


Figure 169. Figure Sensor Concepts

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In a practical telescope, the primary mirror will not have a spherical figure, but will probably be close to a parabola. Therefore, techniques are required to adapt the figure sensor to aspheric mirror measurements.

The figure of the primary mirror in a spaceborne telescope must be maintained with an accuracy that will provide diffraction-limited performance. This means that the figure sensor must be able to detect not just interference fringes but errors that result in only small fractions of a fringe. The concept of electronic optical phase measurement was developed to provide this accuracy. This concept has been described in detail by a number of previous publications* and will be described only briefly here.

The electronic phase measuring interferometer is a two-beam interferometer in which optical wavefront phase is converted to an electronic carrier phase and is then electronically analyzed. This makes it possible to sense figure errors in real time. Figure 170 is a schematic of a simplified version of the phase measuring interferometer with coherent illumination and with plane reflecting surfaces in each interference beam. If both reflectors are perfectly plane and parallel to the wavefront, interference will occur between two plane parallel wavefronts producing a light pattern with uniform intensity over its entirety. If the reference reflector is translated at constant velocity along the optical axis normal to the wavefront, then the light pattern intensity will vary through maximum and minimum levels in a sinusoidal fashion with identical phase over the entire pattern. Two detectors are placed in the output plane: one at a position arbitrarily selected as a reference, and one at a spot corresponding to a spot of unknown figure on the test surface. These convert the cycling light pattern into two cycling electronic voltages or carriers. If a small portion of the test surface is high in relation to the rest of the surface, there will be a relative phase shift of the electronic signal generated from the corresponding point in the light pattern.

In practical implementation of this concept, linear translation of the reference reflector is regarded as causing a continuously increasing phase shift in the reference beam. Continuous increase in phase of a carrier is the same as a fixed change in frequency. Thus, an optical frequency shifter is required for the electronic phase measuring interferometer.

* New Developments in Interferometry. The Perkin-Elmer Corporation, Sept. 1967.

Staff of the Perkin-Elmer Corporation: Advanced Optical Figure Sensor Techniques. Report No. 8660, The Perkin-Elmer Corporation, Jan. 1968.

Crane, R.: Interference Phase Measurement. Applied Optics, Vol. 8, No. 3, Mar. 1969, p. 538.

Crane, R.: An Experimental 20-Inch Segmented Active Mirror. IEEE Trans. AES, March 1969.

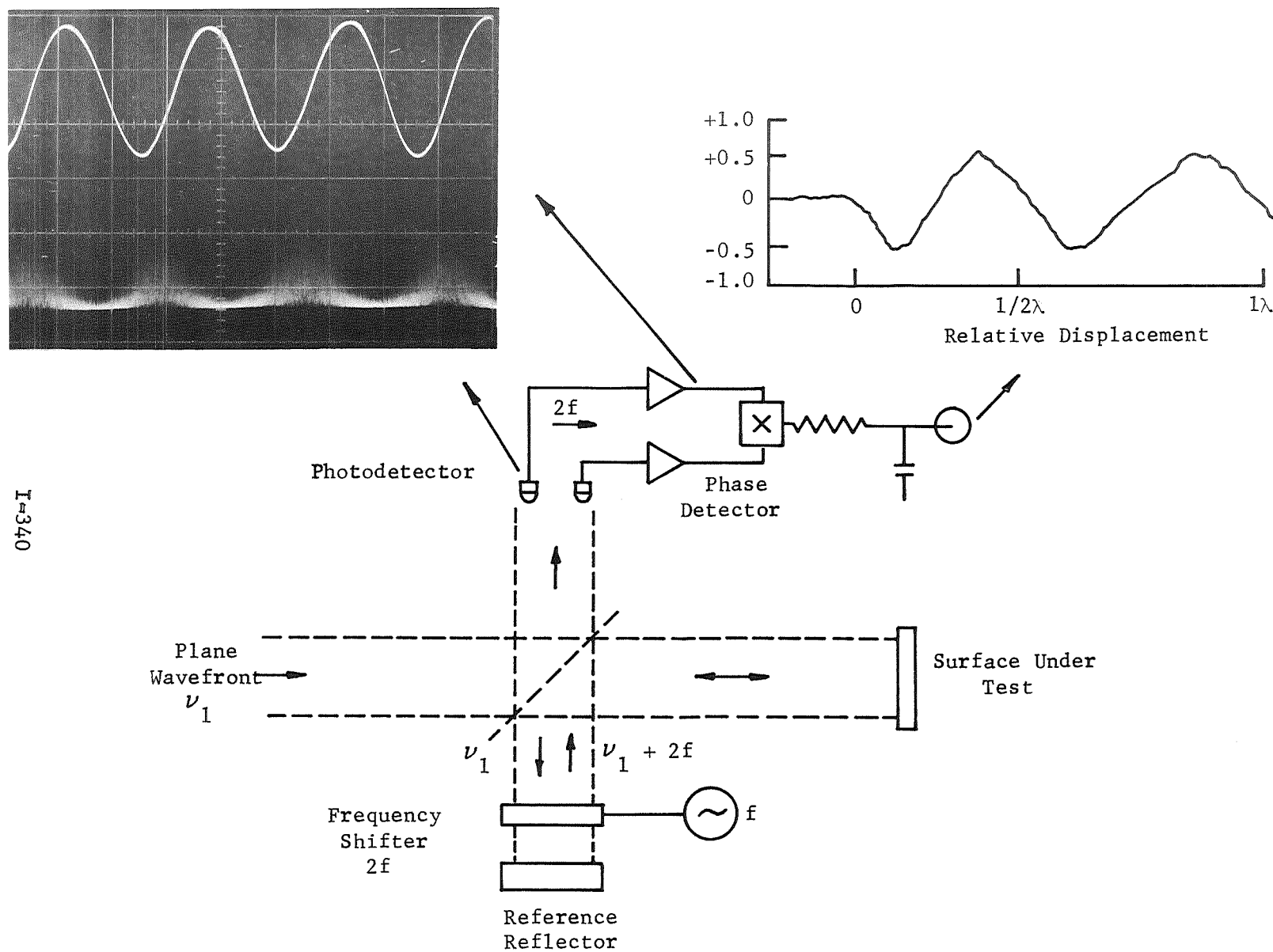


Figure 170. A Phase-Measuring Interferometer Concept

Therefore, Perkin-Elmer, under contract to NASA/ERC,* satisfied the following objectives:

- Design, fabricate, test, and evaluate two different solid-state frequency shifters for use in a phase-measuring interferometer.
- Design, fabricate, test, and evaluate the necessary optics to render the Twyman-Green interferometer capable of measuring an 8-inch-diameter, f/1 parabola.
- Study the problem of primary-to-secondary alignment in a space telescope with regard to the possibility of using the primary mirror figure sensor to measure secondary alignment also.

Results of Advanced Figure Sensor Program*

A summary of the work completed is presented in Figures 171 through 175 .

A Doppler frequency shifter was selected because it is simple and was shown to work successfully. Figure 171 shows the mechanical assembly, consisting of a reference reflector mounted in a piezoelectric cylinder. When voltage is applied between the inner and outer walls, a piezoelectric effect causes the cylinder length to change, in turn causing the reflector to translate in an axial direction. Figure 172 shows how this device is used in a Twyman-Green interferometer. A cyclic linear drive voltage is applied to the cylinder. This produces a sine wave variation of the interference light pattern, which in turn produces two sine wave carrier signals. The discontinuities in the output waveforms shown in Figure 172 occur when the drive voltage reverses direction. The frequency of the output carrier is equal to the frequency shift of the optical beam in the reference arm.

The problem involved in the measurement of an aspheric reflector, i.e., an aspheric surface is not equidistant from its nominal center of curvature, is shown in Figure 173. This figure shows the difference between a parabola and a circle of the same paraxial radius when viewed from their nominal centers of curvature. The " Δ path difference" of up to 100 wavelengths must be accommodated in the figure sensor. The selected solution uses a moiré analysis in which the fringe pattern generated by the actual " Δ path difference" is correlated with a reference pattern which is computed on the basis of a perfect optical element under test. Figure 174 shows how the correlation product is obtained. For simplicity, plane reflectors were used in preparing this figure.

Figure 175a shows a calculated fringe pattern for an 8-inch f/1 parabola. Figure 175b shows the measured fringe pattern for the same mirror. Figure 175c shows a pattern obtained by correlating (a) and (b). The residual fringe pattern is due to the fact that geometric distortion in a field lens was neglected in the computation. It was shown by laboratory demonstration that a moiré pattern can be used with the electronic phase measuring technique. Thus, a complicated aspheric reflecting surface can be measured by a real-time figure sensor.

* Contract NAS20-2012

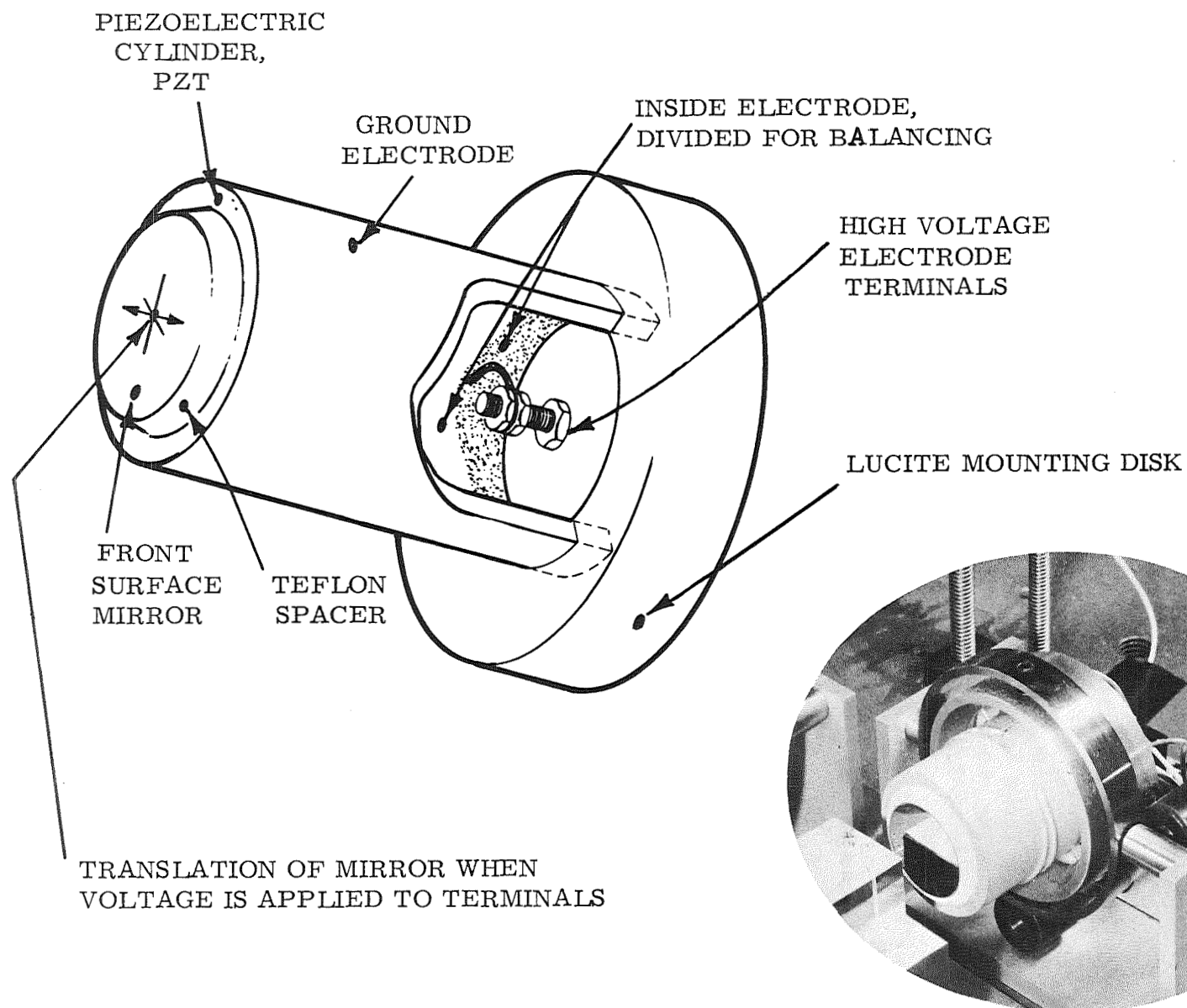


Figure 171. Frequency Shifter Assembly

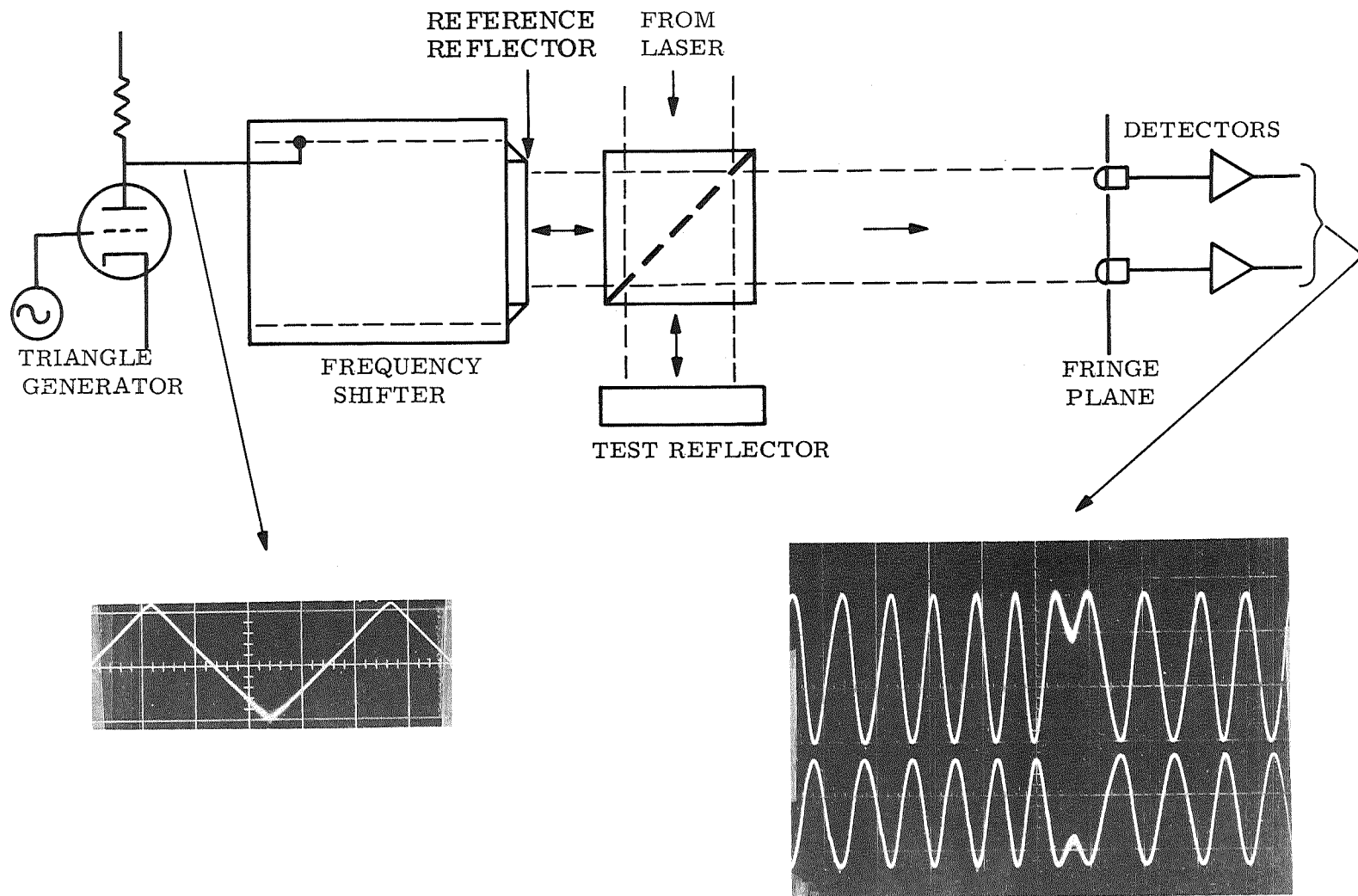


Figure 172. Frequency Shifter Operation

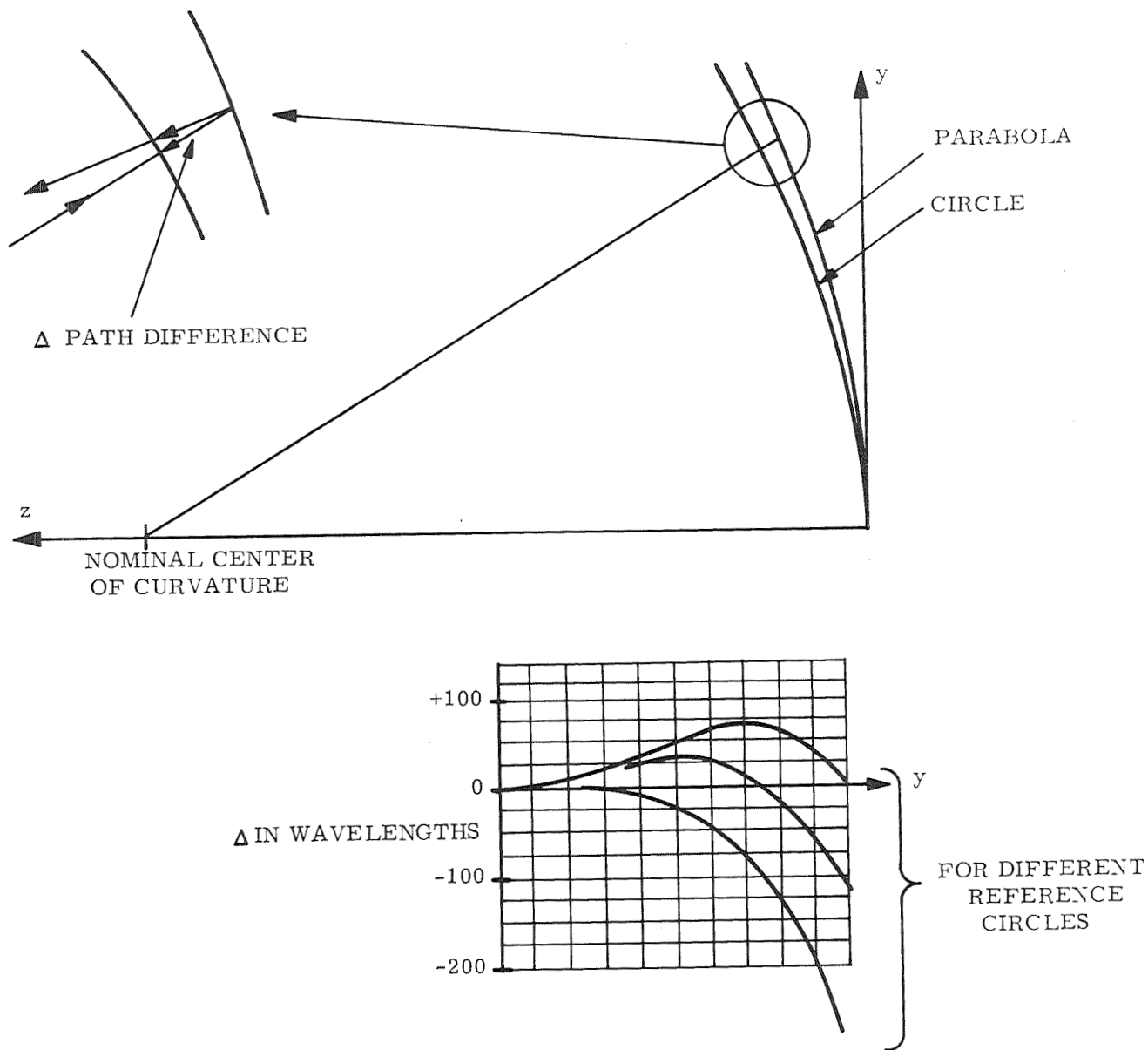


Figure 173. Aspheric Measurement Problem

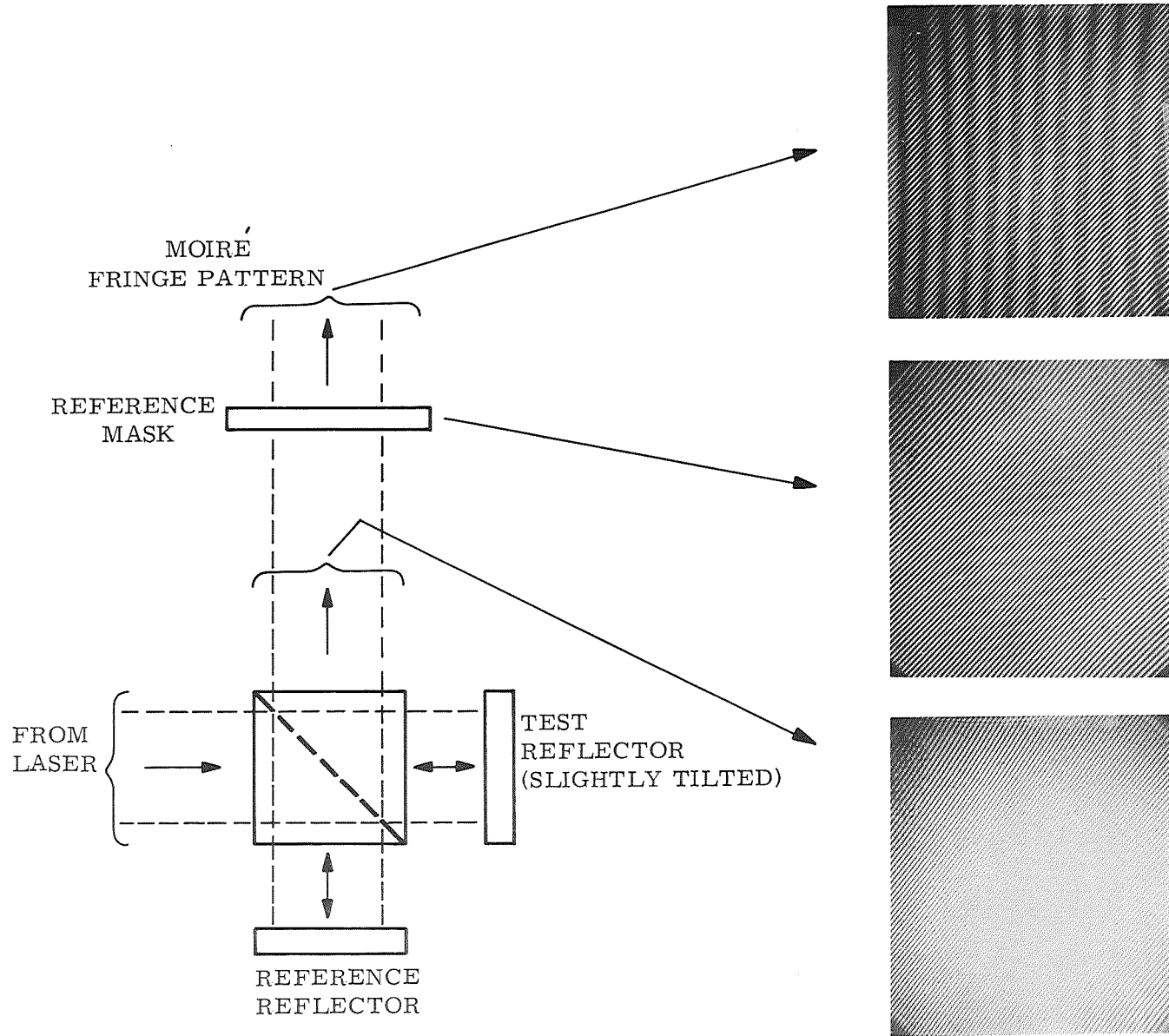
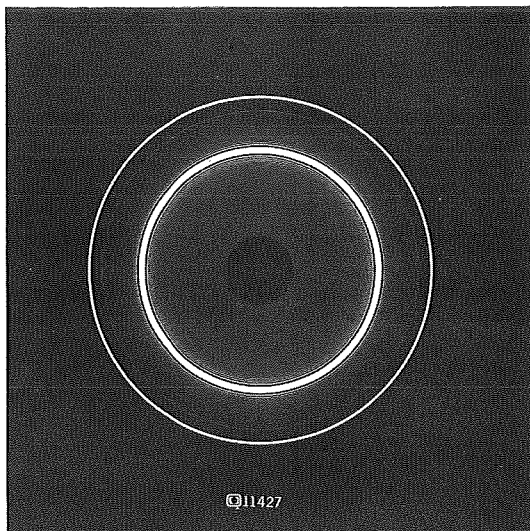
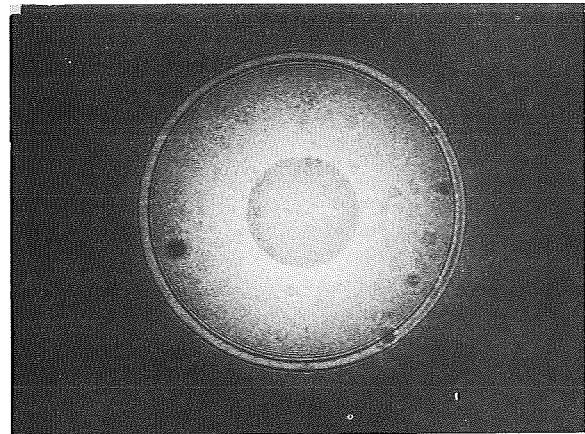


Figure 174. Moiré Pattern Analysis Approach

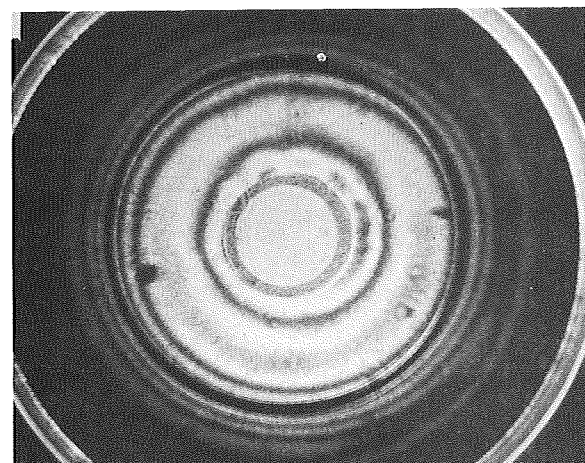
(a)



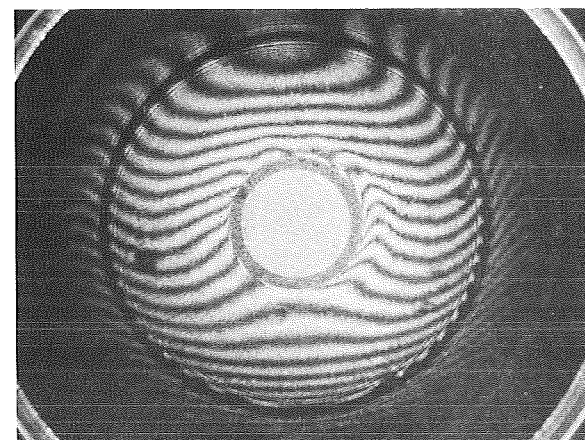
(b)



(c)



(d)



- (a) Reference Fringe pattern made from the calculated difference between a sphere and a parabola.
- (b) Actual Fringe pattern measured from the difference between an $f/1$ parabola and a reference spherical mirror.
- (c) Moiré pattern obtained from multiplication of actual pattern and calculated pattern.
- (d) Moiré pattern similar to (c) above, but with the reference reflector tilted slightly.

Figure 175. Fringe Patterns

Figure 176 shows the optical portions of the breadboard figure sensor set up to test the 8-inch parabola. Figure 177 shows the recommended optical arrangement for a "working model" of this type of figure sensor.

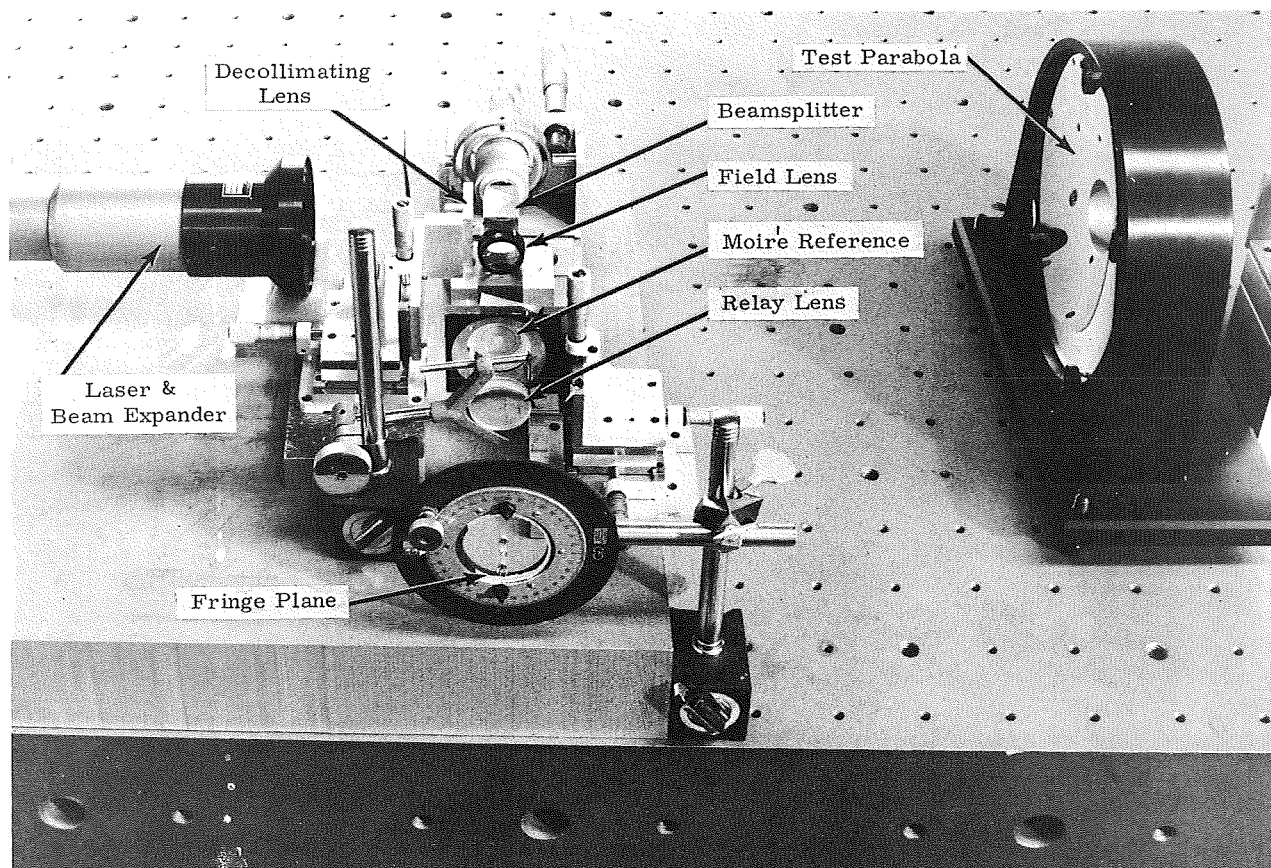


Figure 176. Breadboard Model of the Advanced Figure Sensor

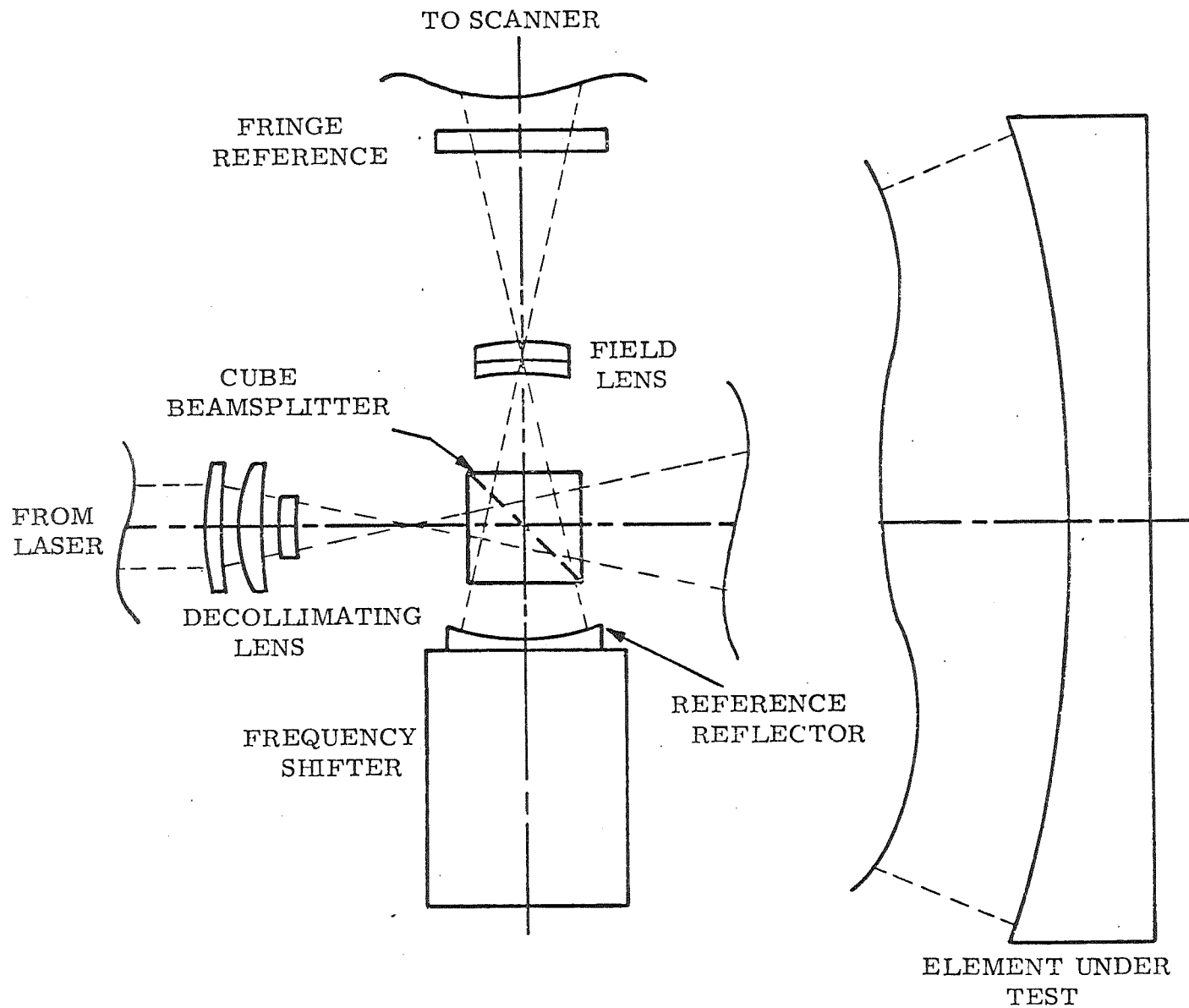


Figure 177 . Recommended Interferometer Arrangement

EXPERIMENT NO. 2 - SUPER-COLD TELESCOPE

Experiment Objective

Many of the preferred mirror materials (Fused Silica, CerVit) have the characteristic that they have a zero expansion coefficient at some low temperature. The effect of thermal gradients on the mirror can be significantly reduced if the telescope can be operated at a temperature very close to this zero crossing temperature. Also, from strictly thermal heat transfer and space physics considerations, it is definitely advantageous to operate at super-cold temperatures of typically -80°C . Since this is exactly the zero crossing temperature of fused silica, it has been assumed, for the sake of arguments presented for this experiment, that the mirror material is fused silica. Most of the conclusions reached are also applicable for other mirror materials at some other acceptable super-cold temperature.

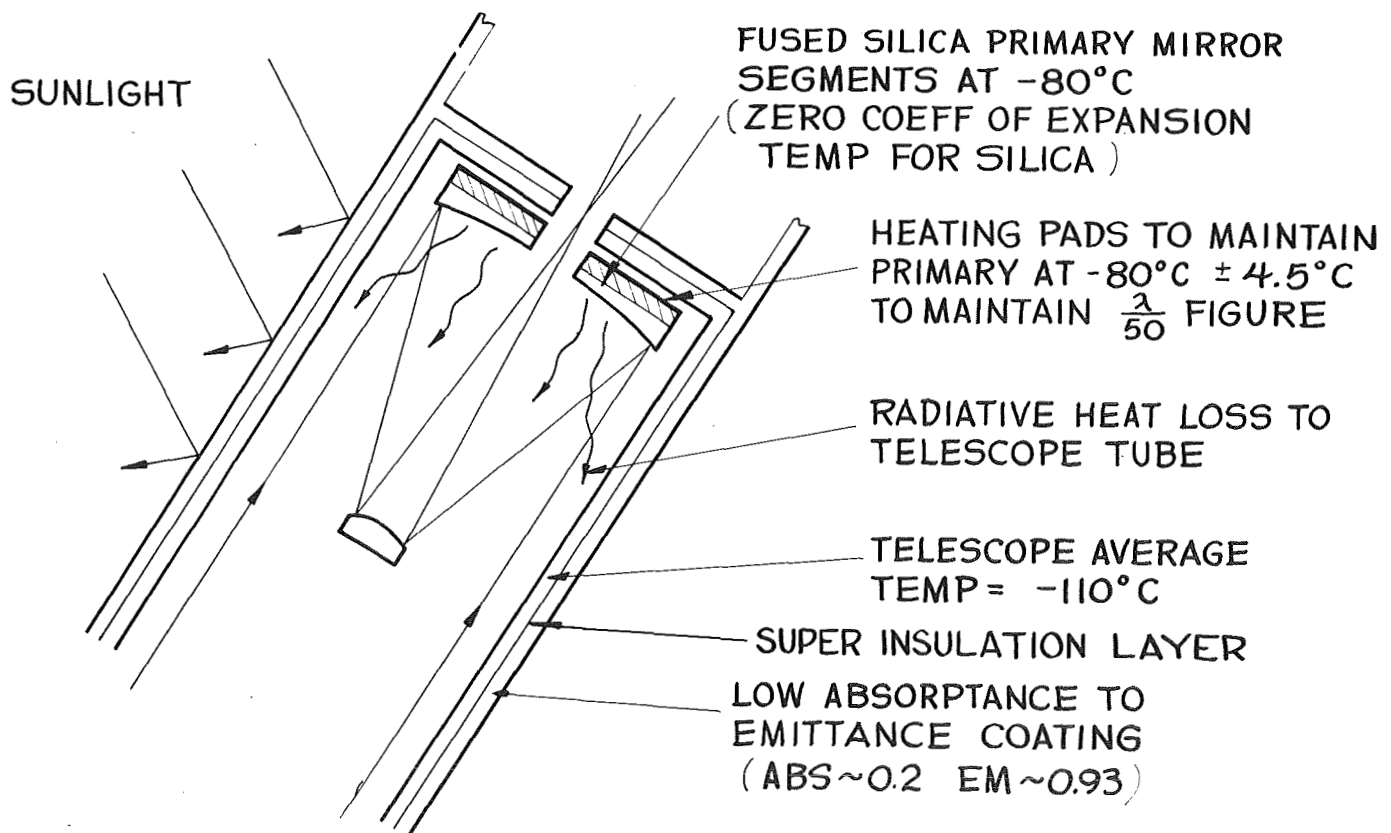


Figure 178. Thermal Arrangement for Supercold Telescope

The objective of this experiment is to gather engineering data on a telescope structure and an optical system that are maintained at this low temperature, by passive means, over long periods of time on station in space. Surface coatings have been developed which have low absorptance-to-emittance ratios and, based on the utilization of coatings such as these, the very cold temperatures required for this experiment seem quite feasible and justifiable.

Basic Experiment Hardware

Since this experiment is an engineering evaluation of performance limits, no unique hardware is necessary. The telescope structure and the primary mirror thermal behavior at -80°C or as close to -80°C as can be obtained would be design considerations for the LTEP.

Experiment Justification

The 1/50 wave rms figure performance of large-aperture optical systems in space is absolutely dependent upon the mirror behavior in the presence of thermal gradients. The sensitivity of optical systems to very small non-uniform temperature changes increases as the aperture diameter increases. The thermal analysis which determines the magnitude of mirror deformation can be found starting on page I-236 of Perkin-Elmer Report 8500. . This experiment is part of the search for space-worthy techniques which would minimize the sensitivity of the system to these unwanted thermal gradients and, at the same time, would lower the magnitude of the residual thermal gradients themselves. The need for this experiment will remain great until truly zero-coefficient-of-expansion materials are invented which are suitable for large-aperture optical systems. Zero coefficient of expansion, as it is used here, refers to materials which have $\alpha = 0$ at an achievable ambient temperature. Based on calculations performed during Phase I and IA of the OTES program, the ambient temperature will vary $\pm 20^{\circ}\text{F}$ over the year in space due to small changes in satellite attitude with respect to the solar vector. Therefore, while fused quartz has zero coefficient of expansion at -80°C , it is not considered a zero-coefficient-of-expansion material since the coefficient is finite at temperatures adjacent to -80°C as shown in Figures 179 and 180.

The problem of thermal deformation of the mirror figure is significantly eased if the mirror is operated around its temperature of zero thermal expansion. The advantage is illustrated by considering the deviation of temperature which expands a volume element 10 cm thick by $\lambda/50 \sim 10^{-8}\text{m}$. This yields (for quartz at -80°C) a temperature range of as much as $\pm 4.5^{\circ}\text{C}$ in comparison to only $\pm 0.25^{\circ}\text{C}$ at room temperature for the 10-cm thick volume element.*

Specifically, great attention to thermal detail in the design of the spacecraft will be required to yield a temperature of -80°C for the primary mirror.

*This comparison for other mirror materials is presented in Figures 47 through 49 of Perkin-Elmer Corporation Engineering Report No. 8900.

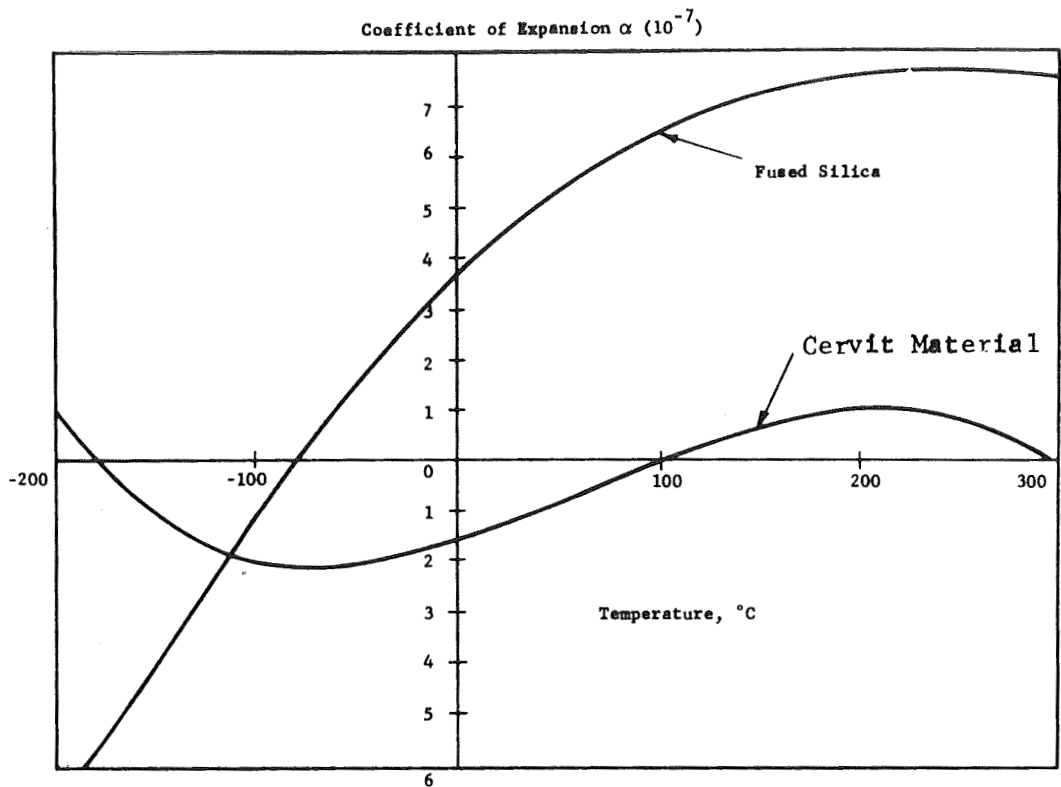


Figure 179. Thermal Expansion of Fused Silica and Low Expansion Cervit Material

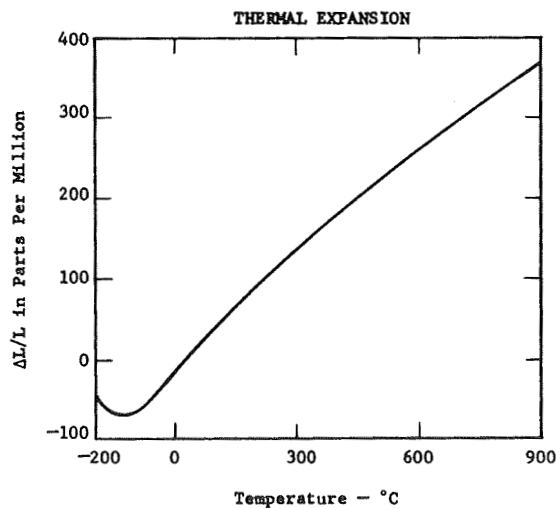


Figure 180. Corning Fused Silica Code 7940

If one considers the long life demanded of future large orbital telescopes, then additional means may be needed to heat or cool the primary to this optimum temperature as the exterior surfaces of the telescope age with the passage of time.

For example, the following parameters relate to an $f/3$ paraboloid of quartz, 30-cm thick, 3 meters in diameter and made in one piece (i.e., not segmented). The tube of the telescope is assumed to have an average temperature of about -100°C (-165°F). A heating pad covering the entire back of the primary maintains its temperature uniformly at -80°C . The heat transfer from the primary to the "cold" tube should be only via radiation from the reflective coating (emissivity 0.05) and amounts to about 14 watts. The constant flow of heat from the back to the front of the primary entails a temperature distribution of uniform gradient in the quartz disk with a temperature difference between back and front of 0.3°C . The resulting thermoelastic deformation of the reflective surface was calculated. It consists of a turned up edge of the paraboloid but is of negligible magnitude. Even at a temperature which deviates from the optimum value of -80°C by $\pm 7^{\circ}\text{C}$ (i.e., -73°C to -87°C) the axial deformation, due to a front-to-back temperature difference of 0.3°C , is approximately $\lambda/50$.

The method of operating the primary at its temperature of zero thermal expansion has an experimental disadvantage when using active thermal control as described in Experiment No. 8. The isothermal approach of this experiment, and particularly the nonuniform temperature approaches of Experiment No. 8 and part of Experiment No. 1, are also useful to correct mirror-figure deformations which are not of thermoelastic origin (for instance, due to long-term stress relaxation). This thermal figuring capability obviously decreases when the thermal expansion coefficient (α) of the mirror material is reduced.

EXPERIMENT NO. 3 - UTILIZATION OF ASTRONAUT CAPABILITY (EVA AND IVA)

Experiment Objective

The experiment objective is to develop the astronaut's skill in the erection, operation, and maintenance of large space optical systems. The preferred operation procedures outlined in the LMSC Volume of this report all require astronaut participation to some extent. Also, the proposed retrofit resupply missions (e.g., the Michelson stellar interferometer or replacement of instrument modules) will require significant astronaut participation. The purpose of this experiment is to determine the capabilities of the astronaut operating in both EVA and IVA. This type of data is received both for determining a baseline for the astronaut operations in the MOAT class of instruments and in determining the maximum complexity for retrofit missions.

Astronaut skills will be evaluated in tune-up operations as well as trouble shooting. Problem areas may arise during disengagement of the telescope from the launch vehicle, erection (if required), and operation of the telescope. It is in this general area of maintenance where the true advantage of properly trained astronauts will become most obvious.

Experiment Justification

Since the investment in a future giant-aperture telescope will be so large, it would be absolutely necessary to extend the life of the operating unit by routine and special maintenance operations on the part of visiting astronauts. The telescope would be too large to pressurize, so the astronaut would be required to develop the necessary EVA skills, and the equipment designed would be required to provide the astronaut with carefully engineered provisions for maintenance. This experiment would provide the development of the astronaut maintenance skills necessary for future space observatories. Also, EVA operations and IVA operations can be compared and base lined for future missions.

EXPERIMENT NO. 4 - UTILIZATION OF ASTRONAUT TO MEASURE PRIMARY MIRROR FIGURE WITH SCATTERPLATE TECHNIQUE

Experiment Objective

Since the astronaut will be available immediately following telescope erection, he can use a small amount of additional apparatus to obtain scatterplate interferograms of the primary mirror. The interferograms would be recorded photographically as now done in the testing of the Stratoscope II mirror. The films could be developed in space using Bimat techniques, and preliminary interpretations of the quality of the interferograms could be established by the astronaut before concluding the space portion of the experiment. Although the astronaut could ascertain the contrast and general quality of the photographic interferogram, and, therefore, continue to expose scatterplate films until an adequate set of photographic plates was obtained, he would not be able to interpret the quality of the primary nor would he be able to telemeter the information contained on the plates back to earth. The exposed plates would have to be returned to earth with the astronaut for measurement, data reduction, and evaluation.

Experiment Justifications

The utilization of the astronaut to provide certification of the quality of the primary would be of value, independent of the measurements of the figure sensor (from Active Optics). The scatterplate photographs are much too difficult to obtain without human participation. However, astronaut time could be devoted to obtaining these interferograms. Their basic value in the LTEP program, as presently envisioned, would be to provide the calibration of other figure-measuring techniques and certification of the quality of the primary mirror when it is on station in space.

Experiment Procedure

The astronaut can set up the scatterplate apparatus and expose the photographic film to obtain the scatterplate interferogram. The photographic plate could be developed in space and the astronaut could interpret the quality of the plate and continue to expose and develop plates until he had one of adequate contrast to take with him on his return to earth. The plate would be measured and the data would be reduced back on earth in order to certify the final figure of the primary mirror in space.

Basic Experiment Hardware (See Figure 181)

Assuming that the asphericity of the primary is partially compensated by a null lens, the phase measuring requires the interferometer (Experiment 1) input/output beams to come to a focus at the "center of curvature". However, the SPI requires a nearly collimated (i.e., beam focused on reference area of primary) beam passing through the scatterplate at the "center of curvature". These two requirements are compatible if a hole is inserted in the middle of the scatterplate (Figure 181a).

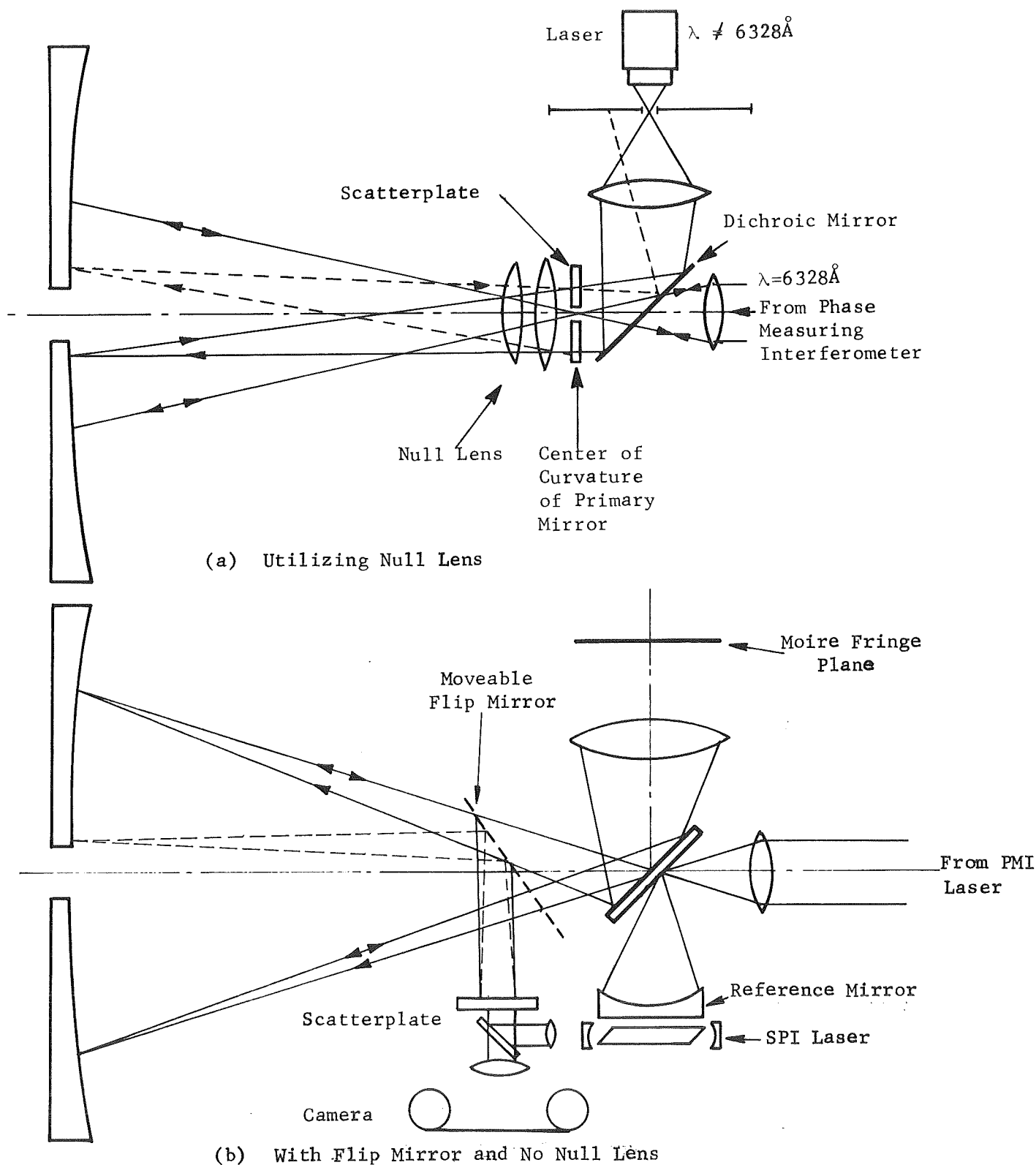


Figure 181. Combination of PMI and SPI at "Center of Curvature"

The light ($6328\text{\AA}$) from the phase-measuring interferometer is focused at the center of curvature of the primary mirror and passed through a small hole in the center of the scatterplate. Light from a laser of different wavelength (argon or doubled YAG) is folded into the telescope optical axis by a beam divider and dichroic mirror and focused through the scatterplate onto a reference point on the primary mirror. The scatterplate affects a small portion of the incident light and scatters it over the entire mirror surface. Similarly, a small portion of the undeviated return light is scattered on the second pass through the scatterplate. These two scattered components interfere to produce a circular fringe pattern on the photographic plate. The number of circular fringes recorded can be varied by displacing the scatterplate from the center of curvature of the mirror.

Preliminary calculations indicate that interferometers with nominally 5-inch diameter critical elements (scatterplate in SPI and beamsplitters for PMI) are possible for the proposed system. Thus, an alternate technique has been proposed for duplexing the SPI and PMI that does not require a null lens (Figure 181b). The mirror segments are aligned via the usual techniques using the PMI. After locking the servo actuators, the moveable flip mirror is inserted as shown in Figure 181b. The stability of the moveable flip mirror is not critical since it must remain stationary only over the time interval the shutter is open.

Optics for both systems will be nominally 5 inches in diameter. Thus the errors from optical inhomogeneity (typically $\Delta n \approx 2 \times 10^{-6}$) are a serious limitation in all the refractive elements. Therefore, the layout of Figure 181b probably is the preferred choice.

Shuttle Implementation

The Lockheed study (volume II) has shown that performing the scatterplate experiment during EVA introduces other problems. However, if it should develop that the space shuttle is the vehicle for the LTEP, then it may be feasible for the scatterplate measurement to be done IVA. The telescope could be attached to a life cell at the aperture end while still in the stowed position in the shuttle, and the astronaut could then make the scatterplate measurements in a shirt-sleeve environment. This possibility obviously needs further study as the details of the shuttle are developed.

EXPERIMENT NO. 5 - MIRROR-FIGURE ASSESSMENT ON EARTH USING A SPACECRAFT LASER
(Deleted from the 2-Meter LTEP)

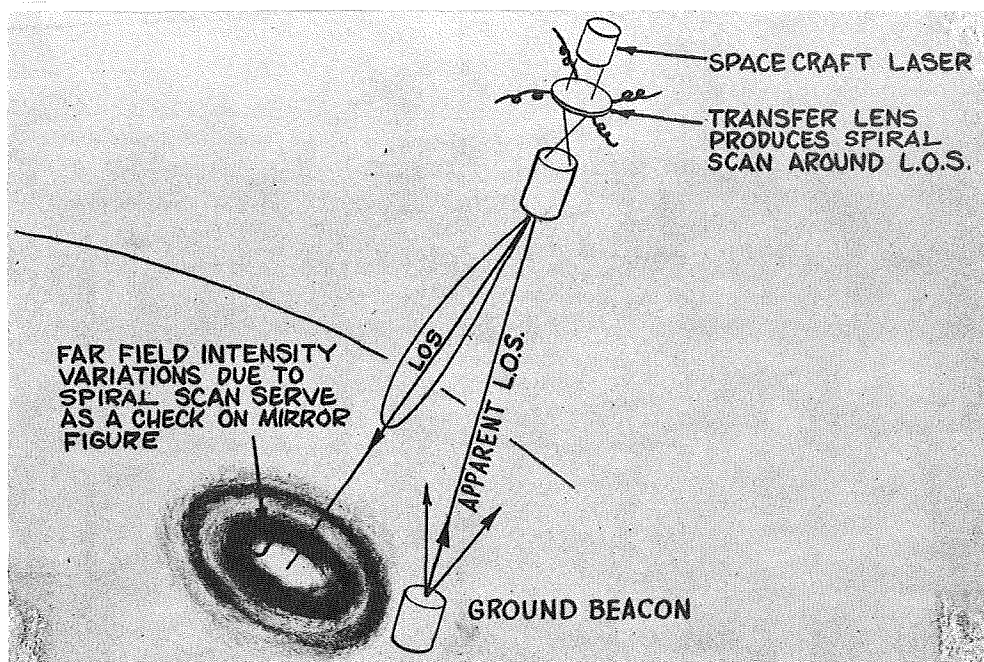


Figure 182. Technique for Determining Mirror Figure

Experiment Objective

The measurement of the Airy disk dimensions (and the associated intensity distribution of the concentric rings) of the spacecraft optical system can provide a quantitative evaluation of the performance of the optical system while the telescope is in space. These measurements can be made on Earth by utilizing a laser at the focal point and by steering the laser beam in a controlled pattern about the line of sight (LOS) of the uplooking telescope. Since the telescope in space is locked onto the Earth beacon which is adjacent to the uplooking telescope, and since the downgoing beam can be directed in a precise raster scan about the LOS of the Earth beacon, the uplooking telescope detector output can be correlated with the raster scan information. In this manner, a video-type display of the Airy disk of the spacecraft optical system is obtained.

This experiment cannot be conducted from a low orbit since the angular rate is much too high to permit locking onto the Earth beacon. For further details, refer to Perkin-Elmer Report 8500.

EXPERIMENT NO. 6 - MIRROR COATING FACILITY IN SPACE

Introduction

The experiment plan below was developed in connection with the Phase A Optical Technology Apollo Extension System (OTES) Study. It is reproduced here along with the results of subsequent study of aluminum coatings for space use carried out as part of the present LTEP program. The later findings suggest that oxygen densities at lower orbital altitudes may degrade aluminum coatings in relatively short periods of time (as little as 10 hours). Experimental data of oxygen densities versus degradation rates of aluminum coating materials should be determined before proceeding with such a coating facility experiment.

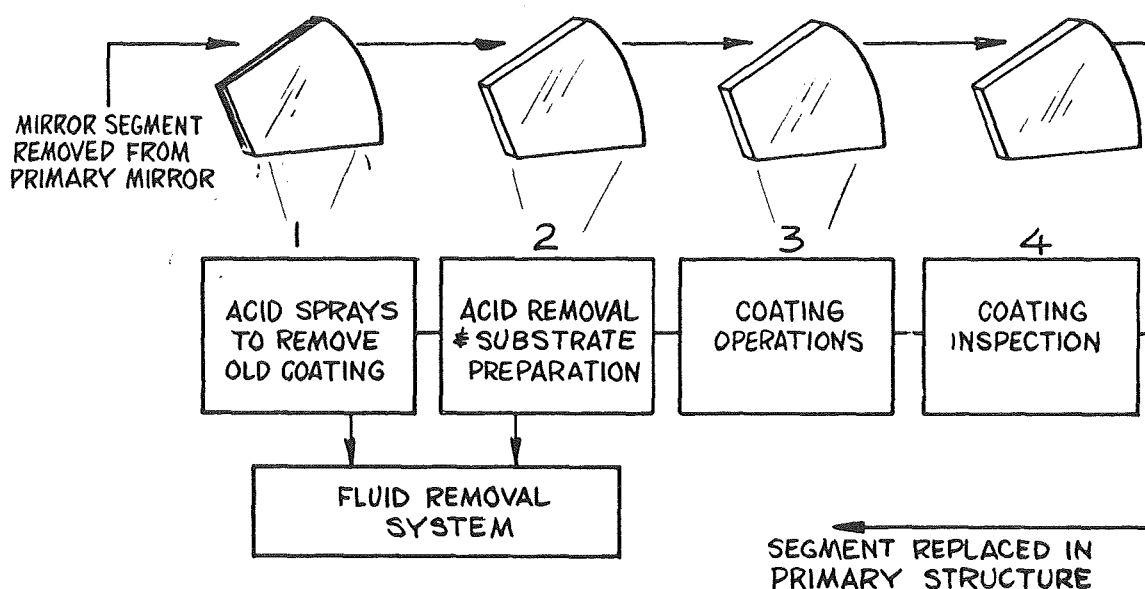


Figure 183. Orbital Mirror Coating

Experiment Objective

To evaluate the effectiveness and utility of an equipment complex aboard the spacecraft which could accomplish:

- Removal of deteriorated reflective mirror coatings.
- Substrate preparation for new coatings.
- Application of new reflective coatings for mirrors.

Experiment Justification

It is a matter of practical experience that ultraviolet mirror coatings deteriorate significantly from the time they leave the coating facility until the time that they are in space. The long-time environmental effects of space

equipment including rocket exhaust plume depositions, micrometeoroid impact, vacuum, and radiation are not known to date. However, it can be anticipated that even an adequate coating for one series of experiments will require alterations for other experiments. For example, enhanced reflectivity coatings are available for utilization with laser light. Yet, these coatings would be most inadequate for uv observations. Since a large-aperture telescope with a segmented primary would permit both the stripping of old coatings and special recoating operations, the proposed experimental facility would be invaluable. The equipment would be designed to function with astronauts in EVA removing mirror segments from the telescope and placing them in the recoating chamber.

The astronaut would then replace the mirror segments back in the telescope. (Refer back to Experiment No. 3 and see page II-59 of Perkin-Elmer Report 8500.)

Basic Experiment Hardware (See Figure 183 and refer to Perkin-Elmer Engineering Report No. 8900, Vol. II, p. 50.)

- Pressure chamber (which permits entry and exit of mirrors to be coated.)
- Structure and clamping arrangement to hold mirror in place.
- Acid sprayers (to remove old reflective surface and prepare substrate.)
- Neutralizer fluid sprayers.
- Fluid removal apparatus.
- Coating deposition apparatus.
- Coating inspection apparatus.

Ultraviolet Reflectivity of Aluminum Coatings Deposited in Orbit

Unoxidized aluminum exhibits a uniformly high reflectivity over an amazing spectral range* (Figure 184). Therefore, the feasibility of using aluminum coatings deposited in orbit deserves further work to establish the feasibility of this potentially attractive technique.

There are three primary factors which can significantly degrade the ultraviolet reflectivity of fresh aluminum deposited in orbit.

- 1) Oxidation
- 2) Overcoating by deposition of contaminants
- 3) Particle (electrons and protons) radiation exposure

Degradation of reflectivity is hastened by uv. exposure.

* Reference: Madden, R.P., Canfield, L.R. & Hass, G., "Journal of Optical Society of America" 53, 620, (1963)

** Hass, G., Ramsey, J.B., Heaney, J.B., and Trido, J.J., Applied Optics 8 225, (1969)

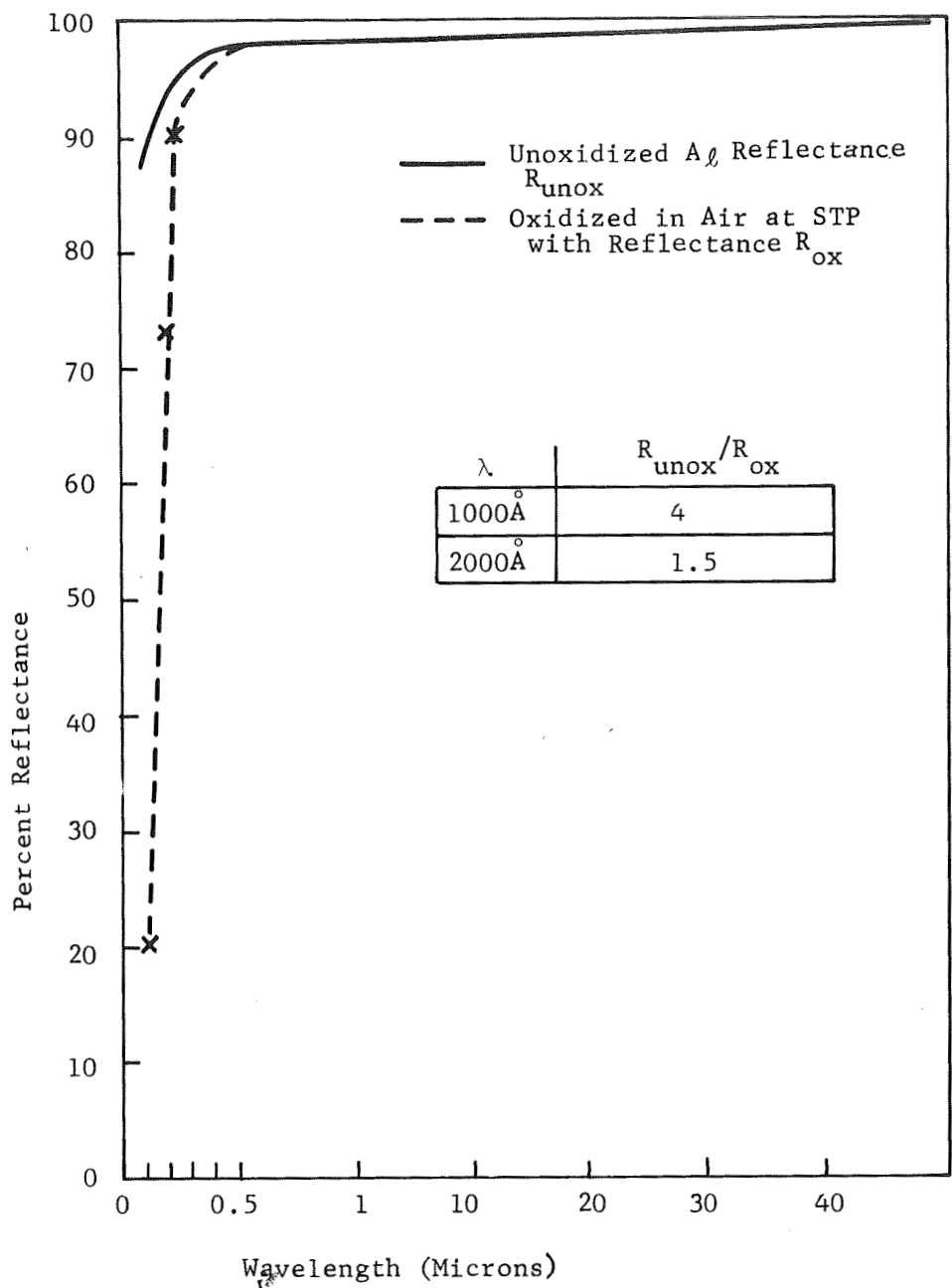


Figure 184. Aluminum Reflectivity

There is no data available presently which answer directly the question of the magnitude and time scale of the ultraviolet reflectivity degradation of aluminum from the above factors for a coating deposited in orbit and located near a manned or unmanned spacecraft in the spectral range from 1000Å to 4000Å. Experiments carried out during Gemini 12 showed that the ultraviolet transmission of quartz decreased as much as 10 percent for exposures of 23 hours in orbit.* This degradation in the region from 2000Å to 4000Å was caused by overcoating by contaminants.

Since the atomic and molecular oxygen densities are known for various altitudes** and since some measurements of the ultraviolet reflectance of freshly deposited aluminum coatings have been made in the laboratory,† it is possible to make rough estimates of the reflectance degradation with time due to oxidation assuming that the environment of the spacecraft contributes nothing to the pristine orbital oxygen densities. At 1085Å, the reflectance will decrease at a rate of about 0.25 percent/minute initially from an initial value of about 80 percent and will reach its asymptotic value of 20 percent in about 10 hours at a 150 km orbit if the decay is exponential. Since the oxygen partial pressure for the laboratory experiments can only be constrained by an upper limit and since the spacecraft environment will be characterized by oxygen densities in excess of the pristine orbital values, the time estimate given above is most likely an optimistic upper limit. The laboratory data on the rate of decrease of the reflectance with pressure are not consistent; however, it is reasonable to expect the initial rate to scale down directly with a decrease in the pressure. Therefore, in a 1000-km orbit where the atomic oxygen pressure is about 1.5×10^{-10} mm, the oxidation will reduce the reflectance at 1085Å from 80 percent to 20 percent, the asymptotic value, in about one year.

On the ATS III satellite, some intriguing data were obtained on the behavior of oxidized aluminum coatings.†† It was found that 2000 hours' exposure to direct solar radiation caused a 60-percent reduction in reflectance at the 3000Å to 4000Å region. For similar samples and identical exposures the

*Muscari and Cunningham: Gemini 12 Contamination Study. Martin Company, Report R-67-2, Denver, Colorado, January 1967.

**Johnson, F.S.: Satellite Environment Handbook. Stanford University Press, 1965.

†Madden, et al., loc. cit.

Vehse, R.C., Arakawa, E.T., and Starford, J.L.: J. Opt. Soc. Amer. Vol. 59, 1967, p. 551.

††Heaney, J.B.: Private Communication.

decrease in reflectance was less than 10 percent when a quartz window was placed 0.10 inch in front of the aluminum coating. It is not known whether the degradation is due either to the ultraviolet below 1800Å enhancing oxidation or to proton bombardment. Earlier work* has excluded the electron component as a cause for this degradation.

In summary, vital information is needed before a sound conclusion can be drawn as to whether in-orbit deposition of aluminum will provide a significant increase in ultraviolet reflectivity for a reasonable length of time. The questions requiring more data are:

- 1) What are atomic and molecular oxygen densities near manned and unmanned spacecraft?
- 2) What is the density and identity of ultraviolet contaminants in spacecraft environment?
- 3) What is the rate of decay of reflectance of freshly deposited aluminum at around 1000Å at oxygen partial pressures from 10^{-7} to 10^{-10} mm?
- 4) At pressures to be experienced in orbit, what is the degradation in reflectance at 1000Å due to ultraviolet exposure and particle bombardment?

The available data cited above are not particularly encouraging in that it appears that the above questions will have to be answered quite specifically before an affirmative decision can be made with regard to the advantages of aluminum deposited in orbit.

*Hass, G., Ramsey, J.B., Heaney, J.B., and Trido, J.J.: Applied Optics. Vol. 8, 1969, p. 275.

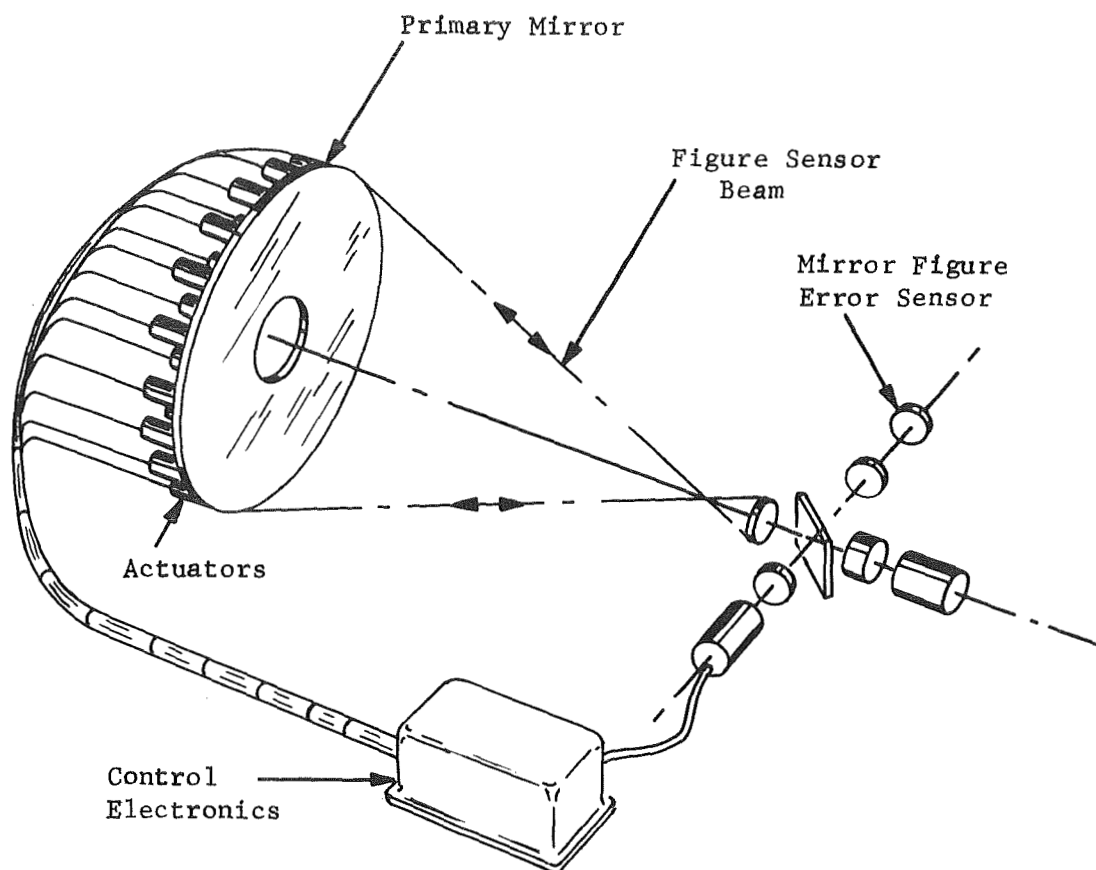


Figure 185. 2-Meter Deformable Mirror System

Experiment Objectives

A viable alternative to the segmented active primary mirror was successfully demonstrated at Perkin-Elmer in the summer of 1969. The deformable mirror has the distinct advantage that the magnitude of active control can be varied to optimize the mirror and minimize the effects of the thermo-mechanical environment with a minimum of equipment. For example, the deformable mirror concept was verified by a system composed of a 30-inch mirror with a 60:1 aspect ratio (6:1 is commonly considered standard in the astronomical community). A total of 61 mirror force points were used for the system. The objective of Experiment No. 7 is to determine the minimum number of actuators (which infers minimum complexity) that are required to maintain the proper figure of the primary mirror. Several potential primary mirror geometries are conceivable.

The first concept is an 80-inch mirror with approximately a 60:1 aspect ratio (Figure 185). This requires a scaling of the concept as proven in the recent laboratory experiments.

The second concept is of a more experimental nature. One of the primary mirror segments could conceivably be thin and deformable (Figure 186). The engineering data derived from such a compound system would prove most valuable in determining the relative merits of deformable and segmented primary mirror control for future 3-meter and larger telescopes.

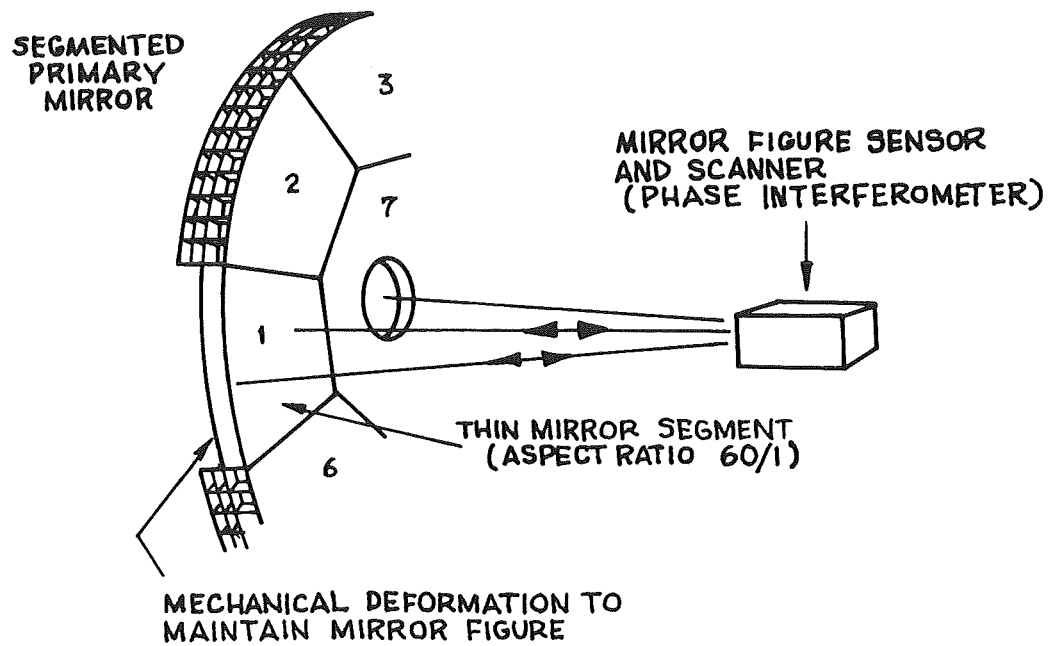


Figure 186. OTES Segmented Mirror Technology Experiment Concept

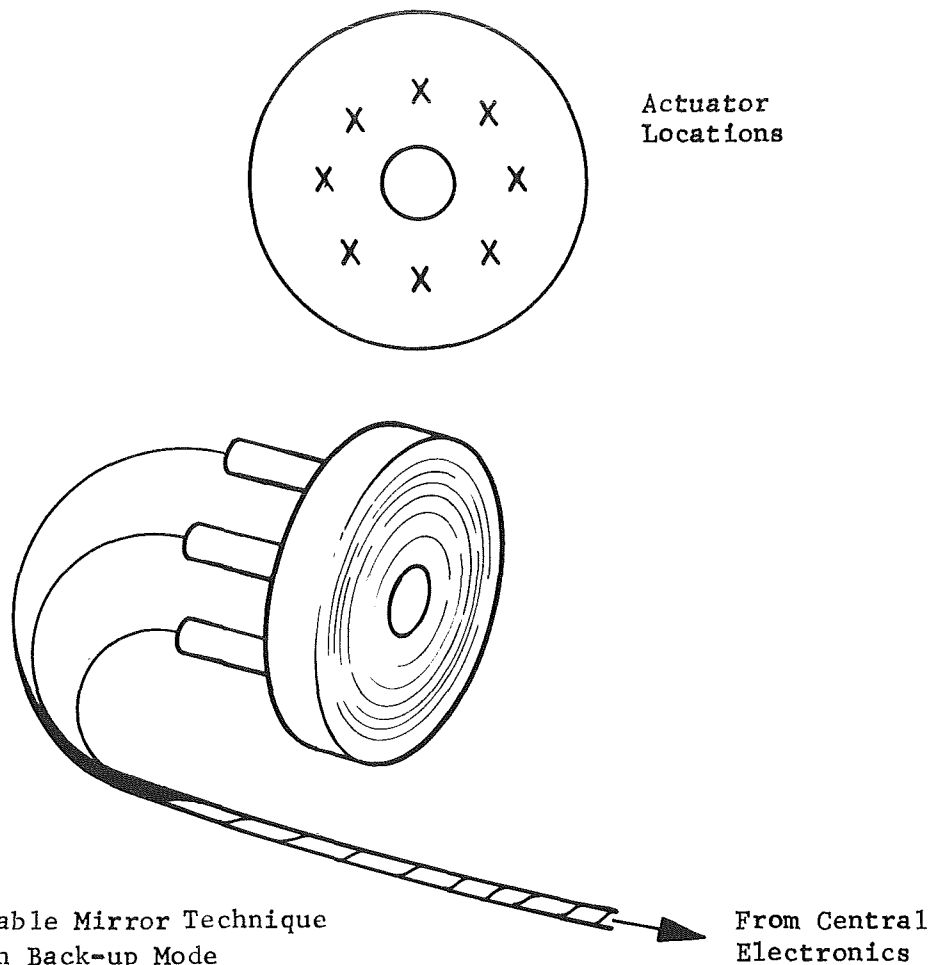


Figure 187. Deformable Mirror Technique
Used in Back-up Mode

The third alternative is to utilize the deformable mirror concept in a back-up mode for conventional mirrors of 1.8 (6 foot) to 2 meters in diameter. As with any large advance in a technology, it is best to have a back-up mode for the critical new elements. Factors which could conceivably introduce some unexpected mirror aberrations are

- 1) Unexpected thermal gradients across the mirror in orbit
- 2) Unrealistic zero-g simulation of mirror during earth-based testing and fabrication
- 3) Material stability
- 4) Stresses introduced during launch

Figure 187 demonstrates a possible configuration for a deformable mirror back-up. The most likely aberration that can not be corrected by a focus change is astigmatism. Because of the nature of astigmatism (i.e., two mirror curvatures at right angles with slightly different radii), an arrangement of actuators positioned as shown in Figure 187 should be sufficient to correct the aberration. Naturally, more study is required to determine the minimum number of actuators and the optimum position of actuator points.

Development of Deformable Thin Mirror

The objective of the Deformable Mirror Program,* was to investigate, analytically and experimentally, the feasibility of obtaining diffraction-limited optics for space application by actively stressing a thin flexible mirror into the proper shape. For the purpose of this study, the reference shape was defined as spherical.

The program was divided into two phases. In Phase I the specific objectives were:

- (a) To analytically determine the parameters for a thin mirror as follows:
 - 1) Optimum thickness-to-diameter ratio.
 - 2) Optimum placement and type of actuator to be used.
 - 3) Required figure accuracy prior to active control, i.e., manufacturing tolerance to achieve a final $1/20\lambda$ rms error.
 - 4) Effect of mirror interactions, including discontinuities in the mirror surface, such as the boundaries imposed by a finite radius and a hole in the center of the mirror.
 - 5) Predicted rms figure error during active control of the mirror.
- (b) To design, fabricate, and evaluate an actuator of the type determined by the above analysis, for use in the thin mirror control system.
- (c) To design the control system in sufficient detail to show its feasibility.
- (d) To fabricate and test a 30-inch-diameter, thin, fused-silica mirror. (The 30-inch diameter was chosen as appropriate for use in the existing vacuum tank facility.) Accuracy of figure, thickness and f-number of the mirror were to be those determined by the above analysis as the most appropriate for the magnitude and scale of corrections to be made. The tests were to be of a quantitative nature and of sufficient accuracy to allow comparison of the mirror before and after stressing.

In Phase II the specific objectives were:

- (a) To fabricate a suitable mounting assembly with actuators to provide the forces and displacements required to strain the thin mirror and produce the necessary figure corrections to achieve the design goal figure accuracy of $1/20\lambda$ rms.

* Contract NAS1-7103 Mod. 1 issued by Langley Research Center to Perkin-Elmer.

- (b) To design and fabricate the electronics required to generate control signals for the individual actuators from the figure sensor developed under NASA Contract NAS1-5198 (i.e., the segmented mirror experiment).
- (c) To combine the figure sensor, electronics, actuators, and mirror into an automatic control system, the design objective of which was to maintain the figure error to within $1/20 \lambda$ rms or better. Sequencing and programming could be done manually.
- (d) To evaluate the closed-loop operation of the control system to determine the accuracy of operation, employing the knife-edge test, the point source test, and scans of the mirror surface with the phase measurement interferometer.

The results obtained during the program are:

1. The mirror was fabricated from fused silica to the following dimensions.

Diameter	-	30 inches
Thickness	-	0.50 inch
Radius of Curvature	-	178 inches
2. A force application arrangement of 61 points located hexagonally on 3.75-inch centers was selected to remove errors of the spatial frequency expected and to test predictions for response of a high-order interacting control system.
3. A theoretical approach for synthesizing a stable high-order interacting control system was developed. Predicting stability required knowledge of the mirror reaction to the multiple forces used to maintain the desired mirror figure. A structural analysis program was used to obtain estimates of the static deflections of a point-loaded, thin, shallow, spherical mirror. The calculated deflections were compared to experimentally measured deflections. The analytical program was used to determine the properties of the 30-inch-diameter mirror, from which it was possible to predict that a stable control system could be designed.

A control system was designed and built employing parallel analog control channels and using a matrix of feedforward elements to counteract the effect of mirror interaction and hence improve servo response. From the analysis of the mirror interaction, feedforward network values were determined.
4. Differential spring type actuators capable of applying forces with a range of $\pm$ two pounds and producing local displacements of a wavelength were designed and built.

5. A mirror support system to minimize the external stresses on the mirror due to the mounting arrangement itself was designed and constructed.
6. The existing phase measurement interferometer figure sensor was modified for use with the thin mirror control system by adding tilt and focus control and a photodiode matrix for parallel sensing of the 61 mirror image points corresponding to the actuator locations.
7. The mirror, actuators, and support system were assembled and mounted in a vacuum tank.
8. The mirror was aligned, and the accuracy of alignment and the precision and response of the control system were tested with the following results:

The final mirror figure with a near optimum mirror-to-figure-sensor spacing was measured to be better than $\lambda/50$ by analysis of interferograms and profile scans, and this performance was qualitatively confirmed by pinhole tests.

The control system response to step disturbances verified the theoretical predictions and the control precision was observed to be better than $1/150$ wavelength rms from repeatability measurements made with the profile scan test.

Analytical Study of Deformable Mirror

The basic concept of Active Optics applied to a thin mirror is deceptively simple. In principle, the mirror figure error is measured and the mirror is stressed to remove the error. In practice, there were several difficult problems to be solved. There were the questions of what kinds of errors had to be accommodated, how thick the mirror should be made, whether force or displacement correction was to be used, and how the mirror should be mounted. The key problem was finding a systematic relationship between what could be measured and the types of figure control that were possible (subsequently referred to as the control system).

As discussed previously, there are three general classes of deformable mirrors. It was noted that development of the thin deformable mirror would prove the general concept. Also, fabrication and test of a thin deformable mirror will generate results which can be easily modified for systems utilizing either a semi-deformable thick mirror or a deformable mirror segment. Thus, the system of Figure 188 was designed, fabricated, and tested. The number and position of the force points is shown in Figure 189. The spacing of the actuators

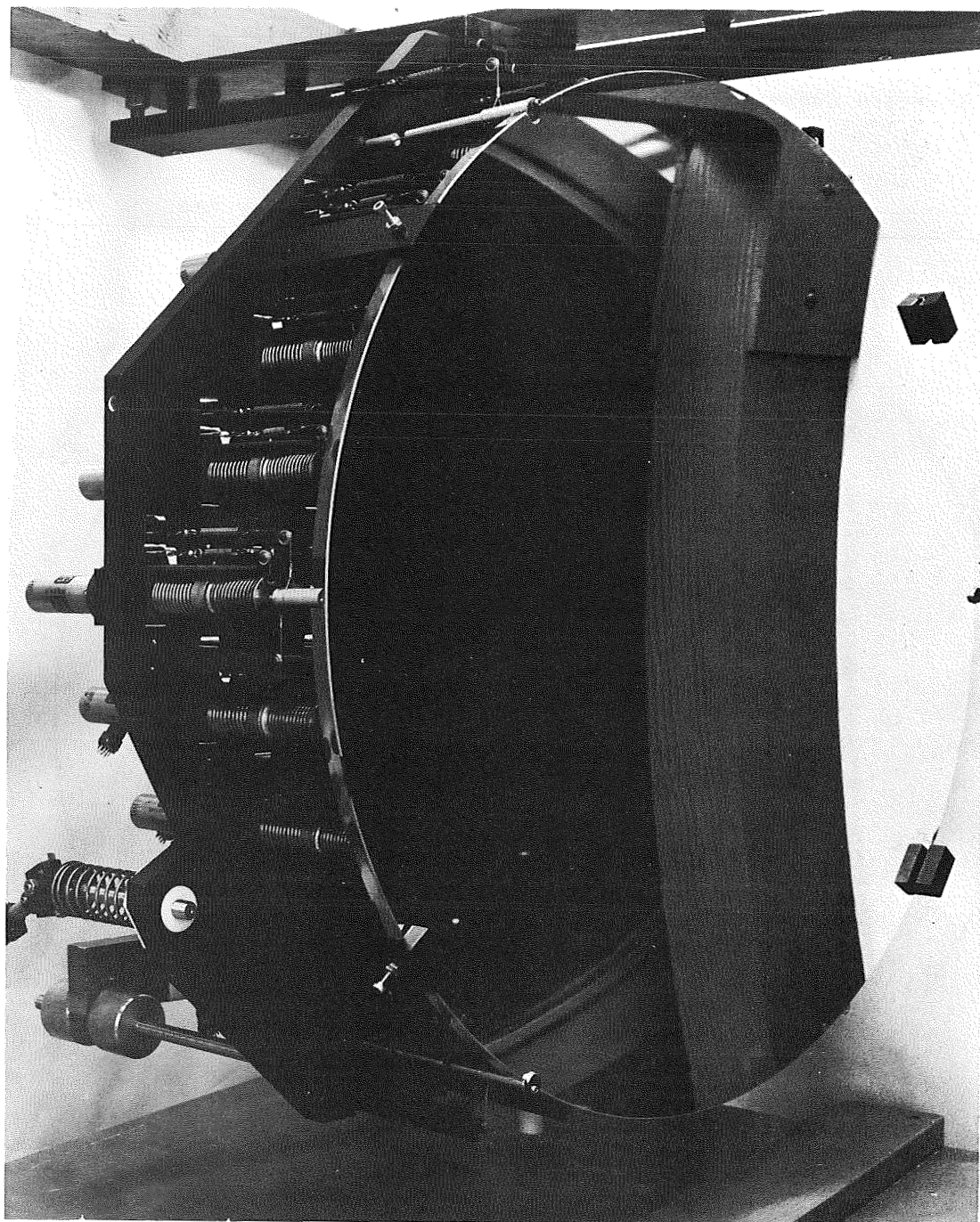


Figure 188. Active Optics, 30-Inch-Diameter, 1/2 Inch Thick, Thin Deformable Mirror and Actuator Assembly

- Flex Actuator Location
- ⊙ Reaction Support Location

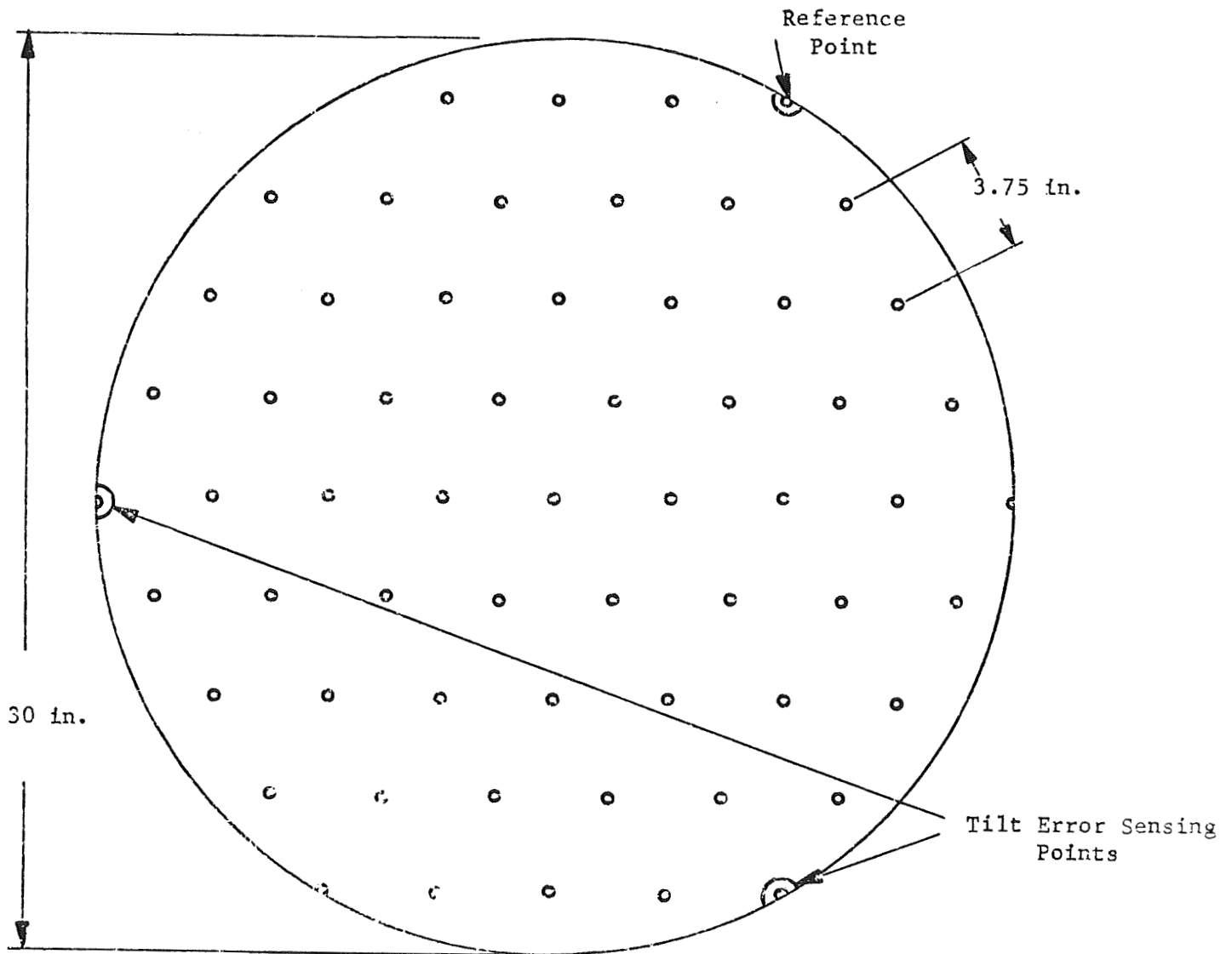


Figure 189 Actuator and Reaction Support Locations on Thin Deformable Mirror

and the amplitude of correction that they are required to make depend upon the mirror aberrations, which are due to fabrication tolerances, gravitational environment, thermal environment, spontaneous stress release, and dynamic loading to be removed.

It was initially predicted that the figure deformations of a 30-inch thin mirror could be maintained below 1 wavelength peak overall and less than $1/10$ wavelength peak over any area with a diameter of 6 inches. The actual errors exceeded these estimates by a factor of 2, but their removal was still well within the capability of the control system. It would certainly be desirable, however, to produce, from the standpoint of long term reliability in an orbiting system, a figure accuracy that minimizes the number of actuators required to remove deformations and to make the mirror diffraction limited. Initial calculations indicate that, for a given actuator spacing, a local amplitude of deformation of approximately $1/2$ wavelength can be removed without leaving a residual ripple of greater than $1/40$ wavelength peak. A residual ripple of $\pm 1/40$ wavelength would introduce an rms error of approximately $1/50$ wavelength. This means that the amplitude of a deformation with the highest spatial frequency that can be corrected should be no greater than $1/2$ wavelength.

The actuator spacing should be determined ideally by the highest spatial frequency of the deformations that might be expected from all sources. A typical diffraction-limited mirror, such as the 36-inch Stratoscope mirror shown in Figure 190, illustrates the type of residual deformations that may be expected after figuring.

As was expected, thermal effects are not the limiting factors for laboratory tests. For space operation, the change from a gravity to gravity-free environment may cause significant mechanical distortion. However, after correct alignment and operation have been established, thermal effects will probably predominate.

The principal sources of figure error for the laboratory experiment are those introduced during fabrication and the stresses introduced by the mounting structure. This is somewhat artificial in terms of the eventual spaceborne utilization of these techniques. However, the range of correction that the system is capable of making indicates that it will be possible to design a system of this sort that will be capable of living in and adapting to the environment in which it is to operate.

The actuator placement chosen gives uniform spacing of the actuators over the entire mirror (see Figure 189). Each actuator is symmetrically surrounded by other actuators except those near the edge discontinuity. This is not necessarily the optimum arrangement of actuators. For instance, when more is known about the distribution of stresses due to the space environment, it might be desirable to vary the actuator density as a function of radial position

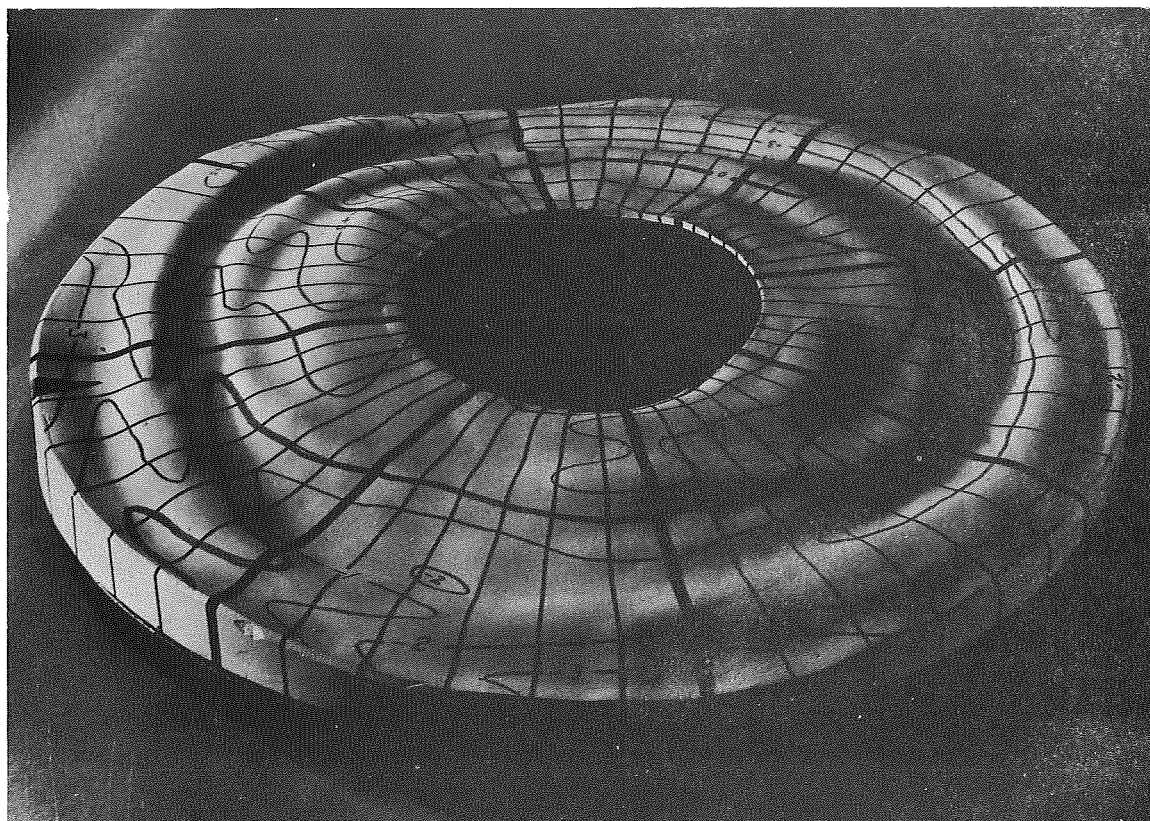


Figure 190 . Model of Residual Deformations in 36-inch Stratoscope Mirror I-378

or to add a few actuators near the unsupported segments of the mirror's edge. For the present, however, the evenly distributed arrangement shown was considered adequate to demonstrate the control system feasibility and to produce the desired final figure accuracy.

The maximum spatial frequency of the deformation that can be removed is 7.5 inches per cycle based upon an actuator spacing of 3.75 inches and the fact that at least three points of contact are required to generate a non-plane figure change. The 58-actuator distribution shown gives degree of control over static deformations with a high spatial frequency component such as is typical of the residual deformations observed in the Stratoscope mirror. In the present case, it also demonstrates the ability of a multiple-loop control system to operate in a stable mode with the number of actuator points to be expected in a larger mirror system.

System Configuration

The basic control system configuration is as shown in Figure 191. The mirror is a 30-inch diameter, 1/2-inch thick, fused-silica, spherical, F/3 reflector. It is controlled by n actuators.

The thin mirror flex actuator can be idealized in general by defining force and displacement actuators in terms of their relative spring constants. The actuator motor can produce either a force or a displacement. The force or displacement can be considered to be working in parallel with a spring and mass in the mobility analogy,* or in series with a spring and mass in the classical analogy.** If the spring's constant is much greater than the other spring constants of the system, the actuator is considered to be a "displacement" actuator. An example of this type of actuator is a rigid piezoelectric element. If the spring constant is much lower than the other spring constants of the system, the actuator is considered to be a "force" actuator. An example of this type of actuator is a motor-controlled lead screw, which drives a very soft spring. The properties of three actuator types characterized by three different relative spring constants are developed below:

- High Spring Constant (Displacement Actuator, see Figure 192a.)

In this case, a displacement-producing device is approximated by setting the spring constant high with respect to the other spring constants of the system. In this configuration, the displacement step sizes are very nearly equal in size to those displacements that are produced in the mirror, and a displacement produced at one point has relatively little effect on the displacement of other actuator points. The forces applied to the mirror at each actuator

* Force = current

** Force = voltage

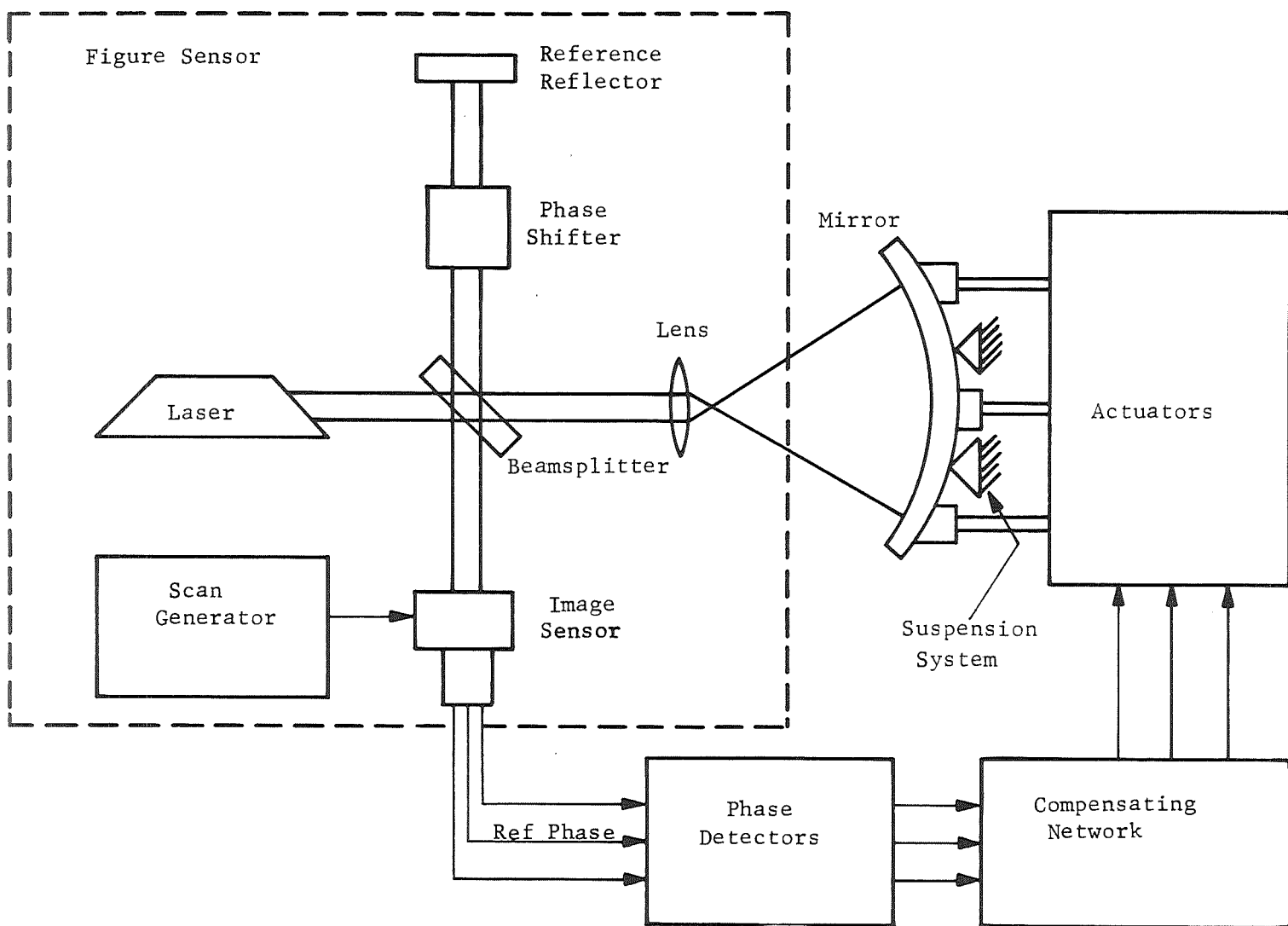


Figure 191. Control System Configuration for Thin Deformable Mirror

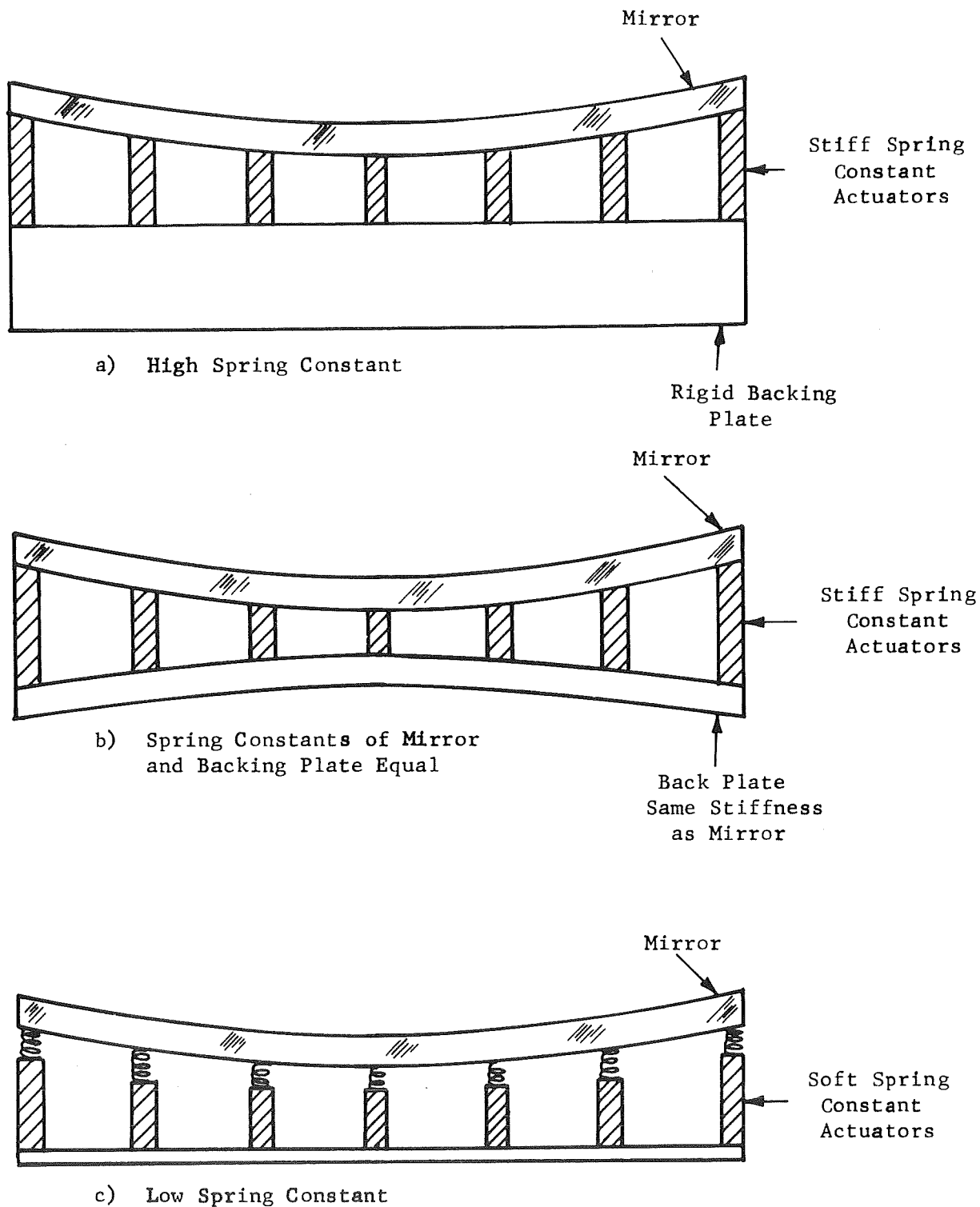


Figure 192. Idealized Actuator Devices

point, however, are definitely dependent upon the other actuator displacements. This means that the interaction due to mechanical coupling through the mirror is automatically accommodated so that a local displacement can be obtained at any point by programming a single actuator at that point. It is assumed that the backing plate is very rigid in this case and that a component representing the backing plate rigidity is included in the actuator spring constant.

The highest spring constant in the 0.5-inch thick mirror is that constant associated with a displacement of a single actuator with all of the other actuators held fixed. The displacement-force relationship for this case is approximately 10 microinches displacement with a 1 pound force for 3.75-inch actuator spacing. This gives a spring constant of 100,000 pounds per inch. If an effective actuator spring constant of at least 10 times this value is desired to approximate a displacement actuator, the actuator and backing plate together must present a spring constant of 10^6 pounds per inch. This requires a backing plate with the rigidity of a 2-inch thick steel plate and actuators with the rigidity of a 1-inch-long by 0.5-inch-diameter glass rod.

As the size of the mirror increases, the effective thickness of the backing plate will scale approximately proportionally if the number of actuation points remains the same. This would reduce the weight-saving advantage to be obtained with an active optical system utilizing a thin deformable mirror.

- Spring Constant Equal to the Approximate Magnitude of the Other Spring Constants of the System (See Figure 192b).

In this case, the displacement step size is larger than the displacements created in the mirror. Both the step size and the applied forces depend strongly upon all the actuators. In general, neither the displacement nor the force applied is a unique function of the drive applied to a particular actuator. One particular configuration, however, does provide the independent displacement characteristic observed in case 1. This configuration uses a very stiff actuator and a backing plate that has the same relative displacement characteristics as the mirror. That is, the shape of the displacement at any point is the same as the corresponding point on the mirror for a given force configuration. The only practical way to obtain this same displacement characteristic is to use a backing plate that is identical to the mirror. (An infinitely rigid backing plate that has the "same" shape but zero amplitude also satisfies this requirement but has already been considered in case 1.) The advantage this system presents is again the automatic localization of the actuator effect (if the actuators themselves are very rigid), as in case 1, without a highly rigid and, consequently, massive backing plate.

The backing plate should be of the same material and geometry as the mirror. The mounting arrangement would be restricted and probably complex in order to maintain the symmetry between mirror and backing plate. The actuators must have the same characteristics required for case 1 above.

- Low Spring Constant Force Actuator, (See Figure 192Q).

In this case, a force actuator is approximated by setting the actuator spring constant very low with respect to the other spring constants of the system. In this configuration the size of the actuator displacement step is much larger than the corrections applied to the mirror. The force applied at a given actuator is dependent upon the control of that actuator and is relatively independent of the forces exerted by the other actuators. The displacement produced, however, is dependent upon the effects of all the actuators. The displacements of the mirror surface are essentially independent of changes in the shape of the backing plate.

Consider, for example, the 30-inch diameter, 0.5-inch thick mirror having actuators on 3.75-inch centers applied to the mirror through springs that have a rate of 4 pounds per inch. A displacement of the backing plate of 1×10^{-3} inch can give a maximum mirror displacement of 1/100 wavelength for this configuration. This does not represent a very stringent tolerance for the dimensional stability of the backing plate and, consequently, it can be a relatively flexible structure.

The basic advantage of the force actuation approach is that there is no requirement for a relatively massive backing plate and there is no necessity for correction of changes in mirror figure due to the displacements of the backing plate. In the force actuation system, the precision mirror is the natural restraining spring that converts force into displacement; therefore, it appeared worthwhile to develop the force actuator method experimentally since it preserves the weight-saving advantage of the Active Optical approach.

This discussion has been in terms of a mirror plus a backing or reaction plate. In a trestle-arrangement support structure, there is no backing plate, and interaction takes place between actuator groups. Actuators work in overlapping groups of four, and the groups of actuators react through the mirror itself.

Consideration of the actuator type to be employed led to the choice between force or displacement type output. Very high stiffness actuators have been shown to imply heavy supports. This latter requirement is perhaps unrealistic in a space application where conservation of payload is important. Therefore, a force type actuator was used in this experiment.

The major disadvantage of the force output actuator is that displacement interaction within the mirror introduces the possibility of instability in the control system. One of the primary functions of the control system then was to negate the mirror interaction by deliberately introducing interactions in a suitable compensation network.

Control System Selection

The basic control problem was to accurately sense a mirror's figure and to provide a means to deform its shape so as to insure an rms figure error of less than 1/50th of a wavelength.

The main difficulty in achieving this objective was considered to be the mirror's internal displacement interaction. Several possible approaches to obtaining the objective, in spite of the difficulty of mirror internal interaction, are:

- N independent, continuously-tracking feedback loops
- A sampling adaptive system
- Optimal control based on minimization of mean square error
- Self-organizing control
- Nodal excitation

It was decided that the first approach was the most straightforward and offered the least complex implementation. This did not preclude the other approaches from future consideration, but the project design effort was directed toward the first approach.

The basic control system data processing approach was selected after evaluating three competitive techniques:

- Analog data processing - In the analog approach, the feedforward resistors, determined from the inverted influence coefficient or stiffness coefficient matrix of the mirror, decouple the parallel channels.
- Digital data processing - In the digital approach, the stiffness coefficient matrix is stored in the data-processor memory. The analog error signals in each channel are converted to digital signals, and multiplication with the appropriate stiffness coefficients is accomplished by a small general purpose digital computer that is appropriately programmed. The products are summed and distributed to the appropriate actuator channels where they are converted to analog signals again. This data handling is accomplished by the digital computer.

- Hybrid digital and analog data processing. This system stores the stiffness coefficient matrix in a digital memory storage unit that is used to control an analog signal multiplier. The multiplier is a binary code ladder that is programmed through switches controlled by the memory.

Various combinations of parallel and sequential operation were considered as subclassifications of these methods of processing. The analog data processing method was selected for implementation in this experiment.

Control System Implementation

The control system concept is based upon a matrix of 61 points or nodes of force application arrayed on the mirror. For the decoupled system, a figure error measured at a particular node is used to generate a constellation of forces that are applied to the back of the mirror in the vicinity of that node. In principle, the constellation of forces is computed so that it generates only a local deformation in a small area centered on that particular node, while all other portions of the mirror are unaffected. Any random figure error profile is then considered to be the sum of many small area errors where the small area size is determined by the actuator spacing. This approach effectively decouples the control channels for each node by the use of cross-feeds in the forward loops, or "feedforwards", that counteract the interactions in the mirror.

The diagram of Figure 193 illustrates the general control concept. Interactions between the tilt and focus control loops and the flex control loops are decoupled by keeping the response time of the flex control loops much longer than that of the tilt and focus control loops, i.e., on the order of five to ten times slower.

Error signals for control of the flex actuators are developed from information at the output of a diode array. Fifty-eight channels of signal amplification, filtering, hard limiting, and phase detection are used in the figure error detector for parallel operation. Fifty-eight servo amplifiers are used for simultaneous operation of all channels.

Feedforwards, consisting of resistive components between channels, provide an analog multiplication and summing of the error signals. If decoupling were necessary between every pair of channels and no symmetry were present, approximately 3600 resistors would be required. However, a maximum of 19 resistors for each node, or a total of less than a thousand resistors for the full matrix were estimated to be sufficient to insure stability.

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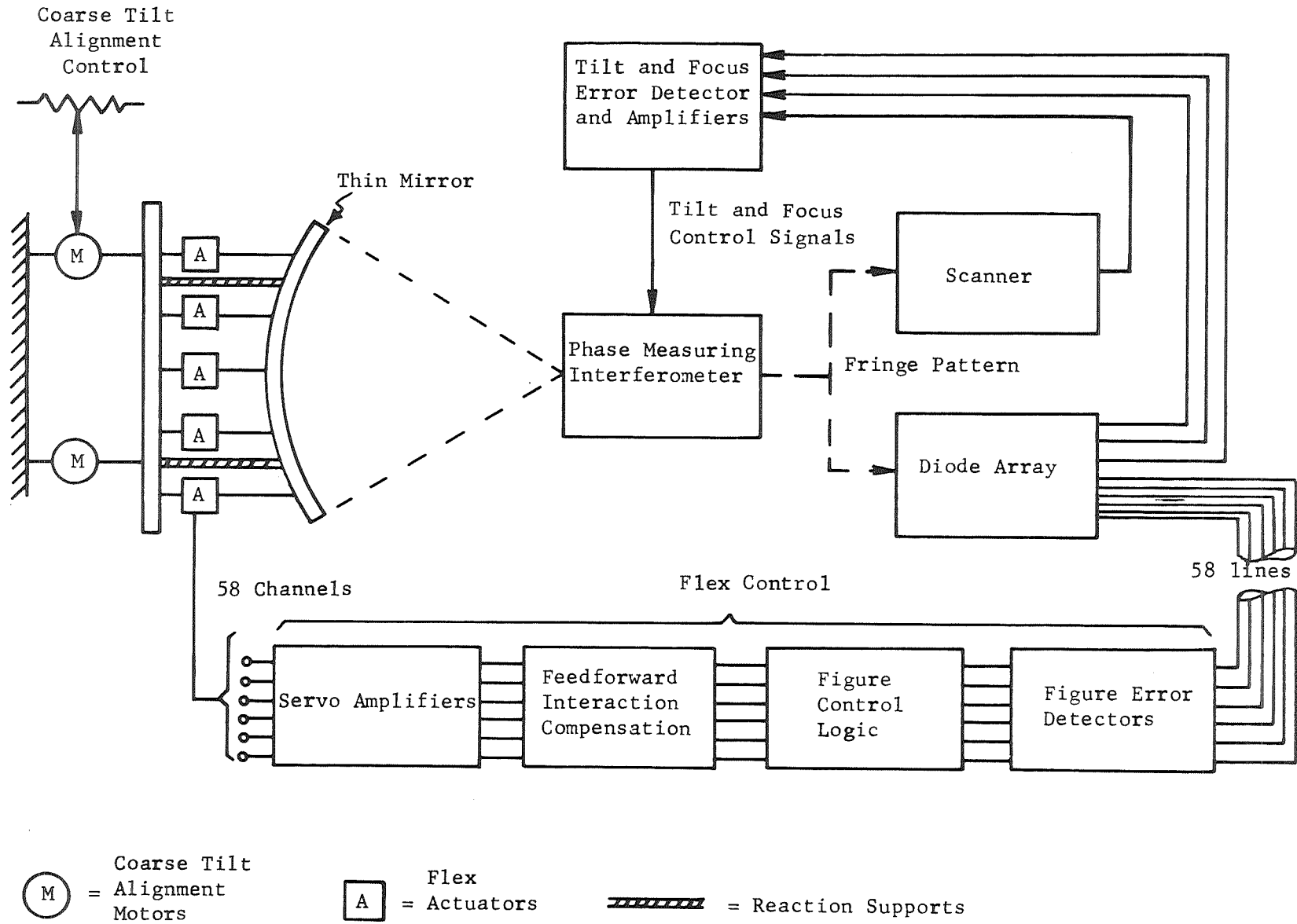


Figure 193. Control System Block Diagram

Control System Functions

The control system functional diagram is shown in Figure 194. The 61 control channels are almost identical with some small differences in frequency response, cross coupling, and switching functions between the flex channels and the tilt and focus channels. Each channel takes its signal from an integrated circuit photodiode and field effect transistor, preamplifier circuit detector at the output of the phase measurement interferometer. Each signal is compared with a reference signal to obtain phase information. The dc voltage at the output of the phase detectors, proportional to the phase difference observed between signal and reference, is applied in appropriate amplitude to the servo amplifiers through the feedforward matrix of resistors. These resistors distribute the signal in the proportion determined by the analysis of the mirror interaction as appropriate to obtain decoupled loop operation.

For initial alignment, a switching arrangement is provided that allows the input to each channel to be taken from a preprogrammed position on the image dissector face plate instead of from the integrated circuit photo-detectors. This allows each channel to be aligned by sweeping the movable spot from a nearby aligned channel to the channel being aligned and then switching the input to the signal from the individual photo-detector. This step is necessary because of the possibility of integral fringe displacements existing initially between actuator locations.

Thin Mirror Fabrication

The first step in the fabrication procedure was to process a 30-inch diameter, 4-inch thick, fused-silica mirror blank, which had been selected for low uniform strain relief to obtain a high quality spherical surface on one side. Since the techniques for producing a spherical mirror surface on a thick plane are well established, this operation did not involve any new procedure. The next step was to reduce the thickness of the mirror to 1/2 inch by removing material from the back. The amount of warping due to released stress in the material was expected to be small since the material was relatively strain free. The procedure for removing material was: first, to cut off a slab 2 inches thick and then to remove the remainder by grinding. The 2-inch slab was cut by a moving wire that carried a grinding slurry. Most of the remaining material to be removed was taken off on a surface generator, but the final operation to remove the last millimeter of material utilized a polishing tool. Successively finer grits of grinding compound were used to remove any local strains introduced in the coarse grinding process.

The mirror was first tested after the spherical front surface had been generated but before the thickness was reduced. An interferogram of the mirror surface at this stage is shown in Figure 195. Evaluation of the interferogram yielded the contour map shown in Figure 196.

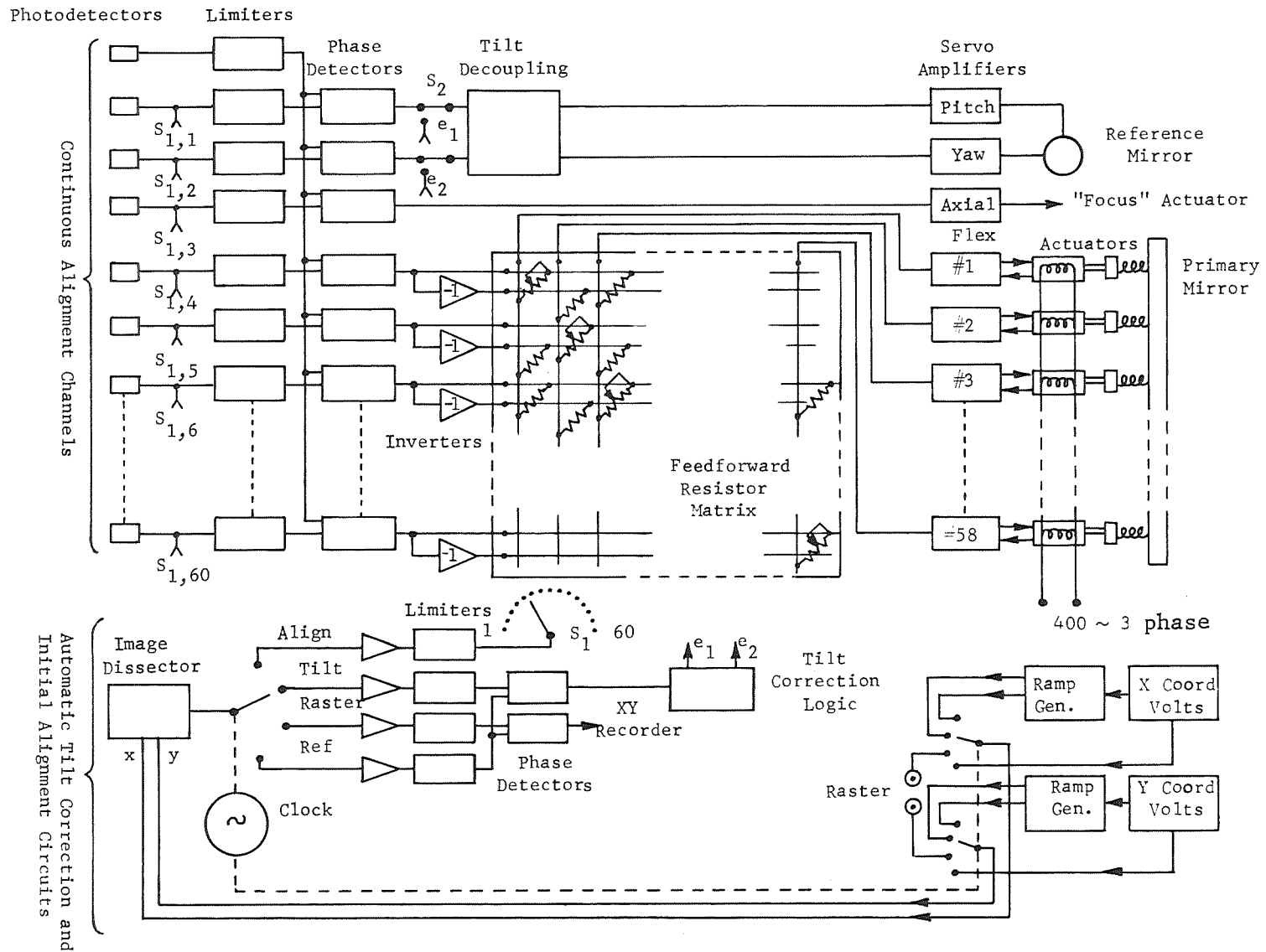


Figure 194 . Functional Diagram of Closed Loop Control System for Thin Deformable Mirror

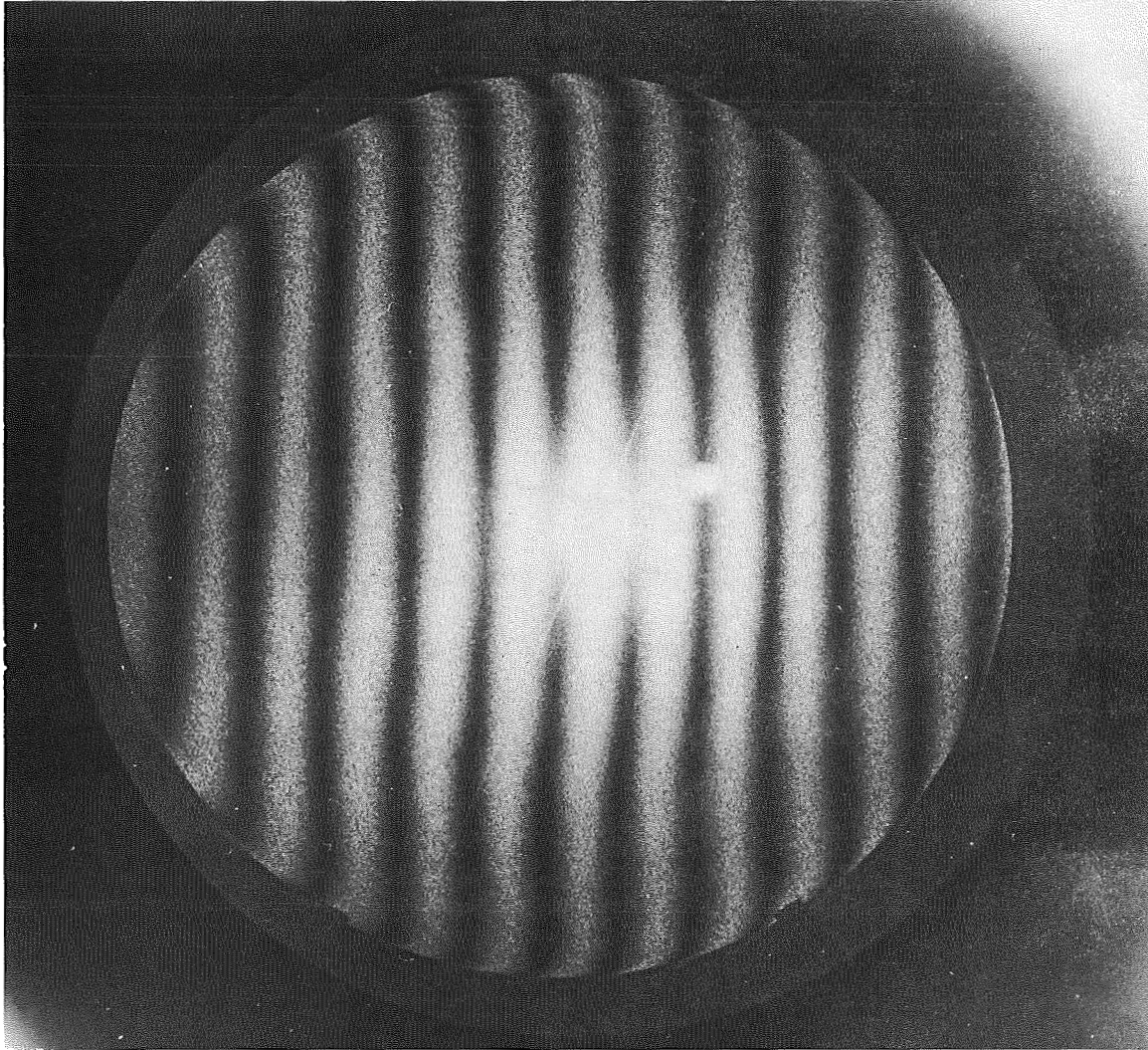


Figure 195. Scatterplate Interferogram of Mirror
Before Thickness Reduction

Contour Values Given
in $\frac{1}{100}$ Wavelength at
6328A

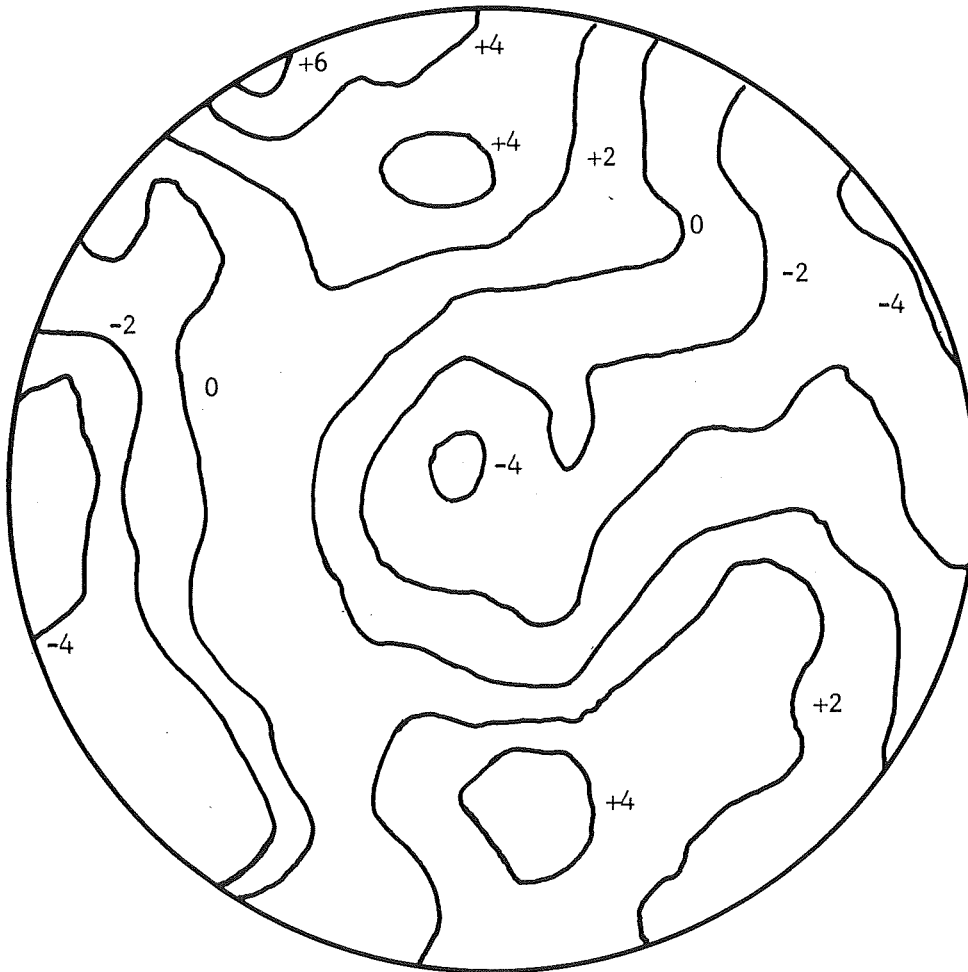


Figure 196. Contour Map of 30-inch Deformable Mirror Before Thickness Reduction (Contours Show Departures from a Best Fit Sphere)

The largest peak deviations of the mirror from a best fit sphere are observed to be $+0.06$ wavelength and -0.04 wavelength. The mirror therefore was within the tolerances of $1/10$ wavelength peak-to-peak that had been desired at this stage. This does not include approximately $1/2$ inch of edge around the mirror, which was observed to be somewhat rolled off. This rolling off might have been further reduced in order to bring this $1/2$ -inch zone within the desired tolerances but little overall improvement in performance of the actively controlled system could be expected for the effort involved.

The test of the $1/2$ -inch-thick mirror to obtain its figure in an unstressed condition involved considerably more delicacy in the supporting arrangement than did the test of the thick mirror. The rigidity of the mirror is a function of the thickness cubed so that the amplitude of deflection due to a local non-distributed force is on the order of 8^3 times as great for the $1/2$ -inch thick mirror compared to the deflection produced by the same force on the 4-inch thick mirror. A noncompensated local force of less than an ounce can produce a bending of the $1/2$ -inch mirror of a wavelength.

The method used to support the mirror during test was an air support technique recently developed at Perkin-Elmer for use with large optical elements and provided a uniform pressure distribution much as the air bag technique does but with fewer problems of pressure control.

The evaluation was performed using a scatterplate interferometer at the center of curvature. The mirror was mounted in a vacuum tank that was evacuated to 2 to 3 inches of mercury absolute pressure, with the optical axis vertical. The interferometer was mounted at the top of the tank just above a vacuum window and, therefore, at atmospheric pressure for convenience of operation.

Measurements of the surface topography of the mirror were performed for several orientations of the mirror relative to the support system. This was done to indicate how much of any observed distortion was caused by the mirror itself and how much was caused by any small nonuniformities in support pressure.

Some variations of figure were observed in the different support positions, introducing an astigmatism on the order of $1/2$ wavelength in addition to the distortions that were observed to rotate with the mirror.

The mirror was finally tested while mounted with its axis horizontal on the weight and reaction support system with which it was actually used. Interferograms were obtained with the phase measurement interferometer. The figure observed in this condition was very close to the average of those measured on the air bag support, within the $\pm 1/2$ -wavelength variation described in the preceding paragraph.

The interferogram shown in Figure 197 shows the surface figure of the mirror while supported on the weight and reaction support system used in the closed-loop experiment. There are approximately two wavelengths of astigmatism plus a kidney-shaped plateau of approximately 1 wavelength amplitude. Although this was more than twice the amplitude initially desired, it was still expected to be within the range of amplitude that could be removed with the designed actuator spacing to obtain a final alignment with less than $1/20$ wavelength rms error.

System Performance

Four measurement techniques were employed to obtain a quantitative, as well as a qualitative evaluation of the accuracy and precision of the mirror alignment during control. Information was obtained about: the amplitude of departure of the controlled surface from ideal (both locally and rms), the precision and repeatability of the control system, and the dynamic range of the system (i.e., its ability to remove large errors).

One of the most graphic techniques for obtaining mirror figure accuracy is by recording the output of a test interferometer photographically. In this case, the test interferometer was already setup and aligned as the figure sensor in the Active Optics control loop. This test shows the local deformations of the mirror quantitatively to within the accuracy of the errors introduced by the interferometer itself, which were previously shown to be less than $1/50\lambda$ peak.* For a spherical mirror the interferograms are very easy to interpret. Figure 198 shows an interferogram of the mirror in a relaxed state before alignment. A topographical model (Figure 199) was made from this interferogram. The difference between levels of the topographical model represents a $1/2$ -wavelength change in figure error with respect to a perfect sphere.

Figure 200 shows an interferogram of the mirror after alignment by the Active Optics control system. In this case the wavefront in one arm of the interferometer was tipped relative to the wavefront in the other arm in order to obtain a set of parallel straight-line fringes. The deviation of these fringes from a straight line and from uniform spacing is proportional to the deformation at the mirror, again, within the accuracy allowed by any errors introduced by the interferometer.

Figure 201 shows an interferogram of the aligned mirror at a mirror-to-figure-sensor spacing approximately 0.160 inch larger than that used for Figure 200. The mirror curvature can be maintained at any given radius within the range

* Crane, R., Jr.: An Experimental Twenty-Inch Segmented Active Mirror, IEEE Trans. on Aerospace and Electronic Systems. Vol. AES-5, No. 2, March 1969, pp. 279-286.



Figure 197. Interferogram of Thin Mirror Before Active Correction



Figure 198 . Interferogram of 30 Inch Mirror Before Alignment, Supported With Optical Axis Horizontal

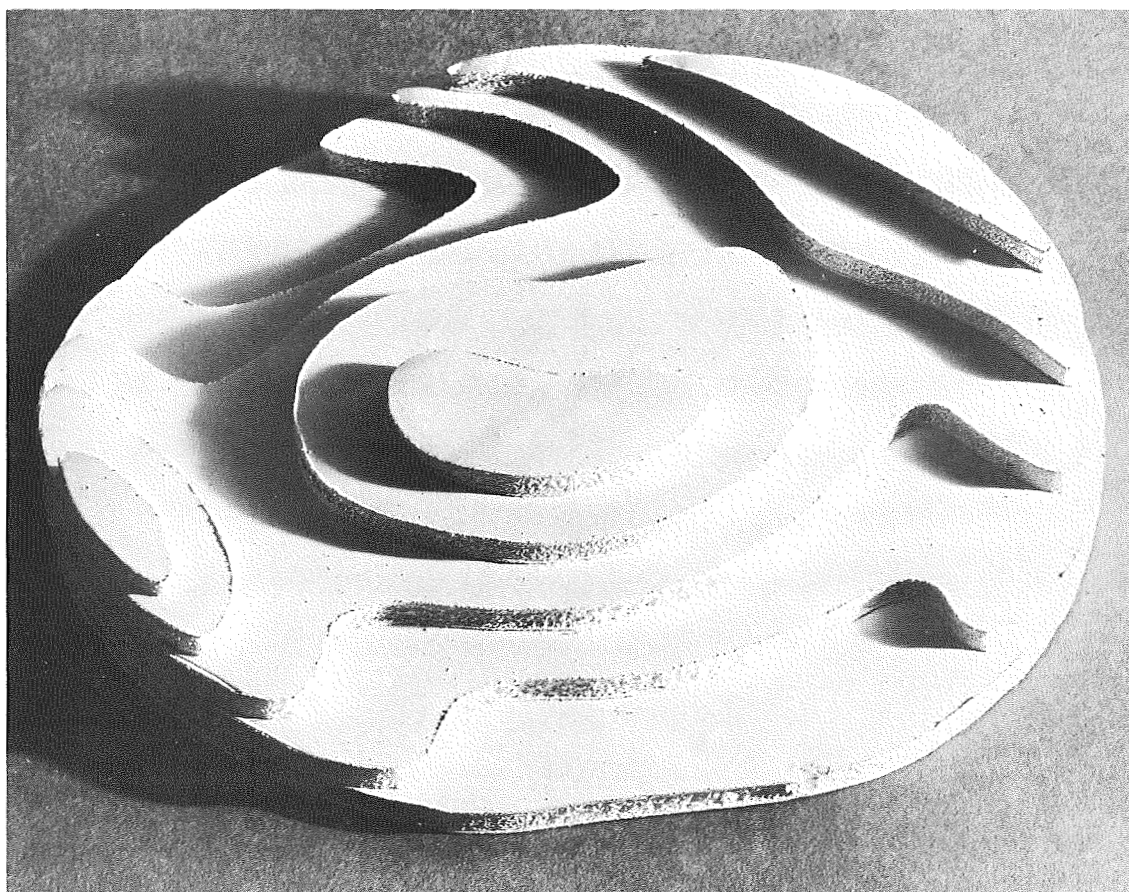


Figure 199 . Model of Mirror Deviations from Spherical Surface Before Alignment - Each Level Represents $1/2$ Wavelength

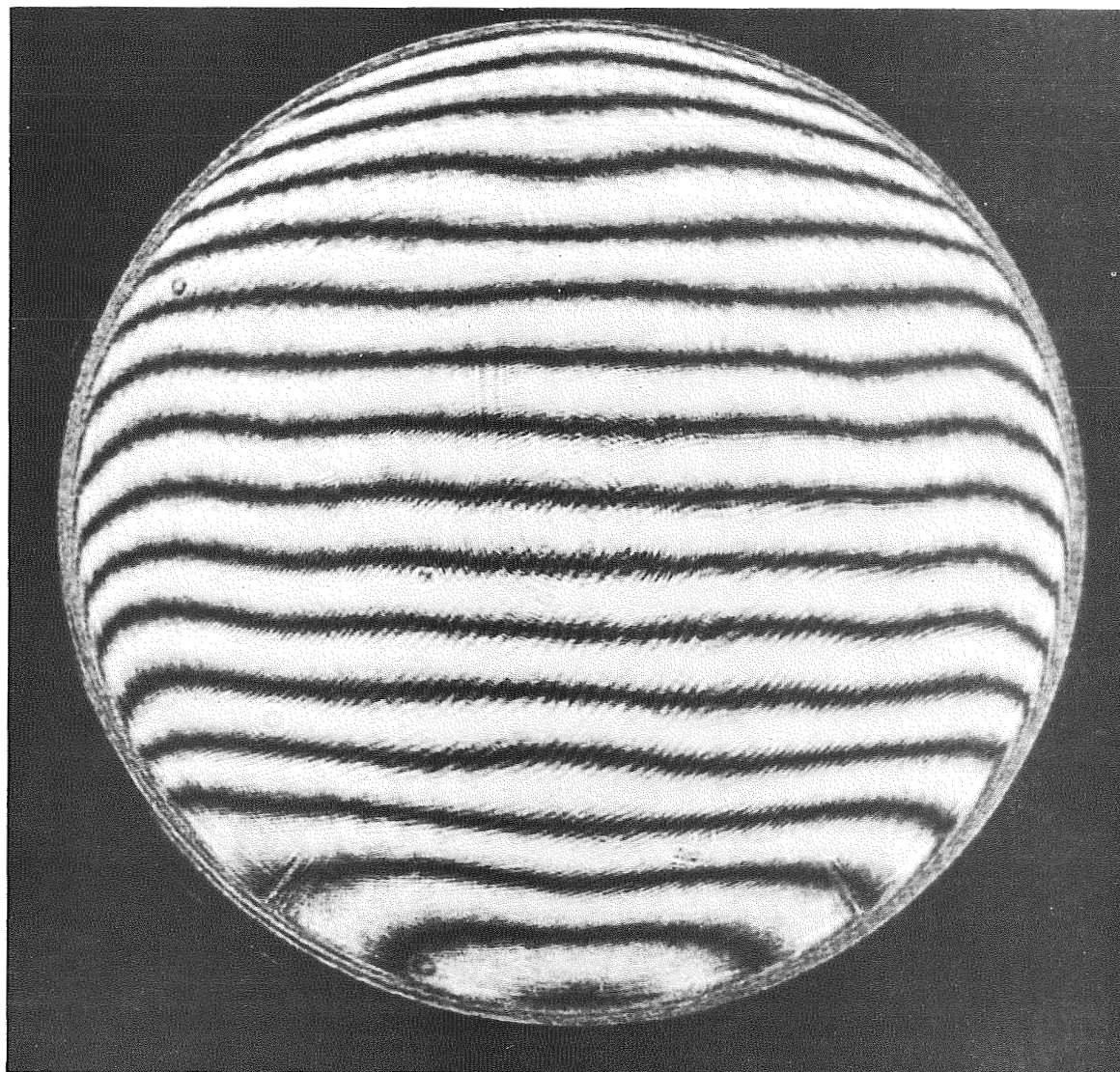


Figure 200 . Interferogram of 30 Inch Mirror after Alignment with Active Optics Control System (Wavefront Tilted to Obtain Straight Line Fringes)

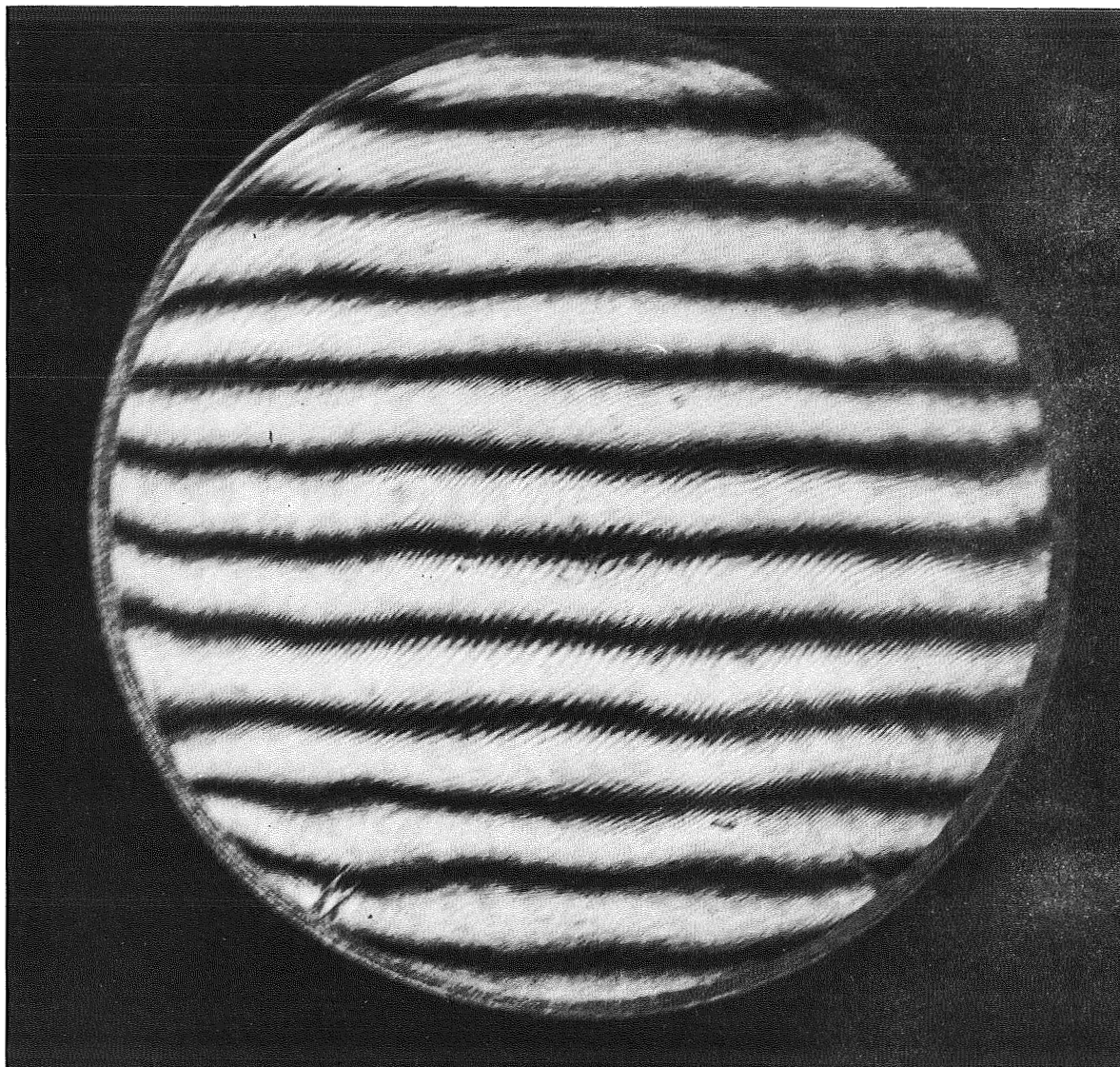


Figure 201 . Interferogram of 30 Inch Mirror After Alignment with Active Optics Control System. (Mirror to Figure Sensor Distance Adjusted for Close to Optimum Mirror Figure)

of the actuators by keying the "focus" control to the value of the output voltage of the position-indicating potentiometer of one of the flex actuators for that position. It is apparent that the radius selected for Figure is closer to optimum than is the radius used in Figure 201. The residual deformations measured for the two cases were $1/50$ wavelength rms and $1/50$ wavelength rms, respectively.

The interferograms described in the previous paragraph have the disadvantage that the control system must be turned off to record them. This did not seriously degrade their accuracy as the mirror figure was observed to maintain itself very well after thermal equilibrium had been reached; however, it was desired to obtain similar quantitative information about the mirror surface under control to determine the precision and repeatability of the control system.

Figure 202 shows a set of profile scans obtained by scanning the output of the phase measurement interferometer using the Image Dissector and applying the output of the phase detector to an x-y recorder. This gives a set of curves very similar in appearance to the interference fringes observed in Figure 200. The mirror was scanned twice so that each curve consists of a double trace. The variations between traces give an indication of the precision of the control system. The repeatability is within $1/50$ wavelength everywhere, indicating that the precision error introduced by the control system is less than $1/250$ wavelength rms. Figure 203 shows models of the mirror taken from the interferograms before and after active correction.

A pinhole source 0.0001-inch in diameter was located approximately $1/8$ inch to one side of the center of curvature of the mirror and the image formed by the mirror was observed through a microscope. The pinhole test does not contain errors from the interferometer or control electronics and directly tests the ability of the mirror to perform the function for which it is designed, that is, to form an image. Therefore, the pinhole test is a very useful and necessary, as well as graphic, cross-check on the performance of the system. Analysis of the pinhole image can give quantitative information about the rms figure error, but it cannot be correlated with specific local areas of the mirror. Figure 204 is a photograph of the image for the mirror alignment shown in the interferogram of Figure 201. The first dark ring is 0.00025 inch in diameter in the actual image so that the photograph is a 5000X enlargement. The uniformity and symmetry of the first dark ring and the first bright ring outside the Airy disk are quite good and indicate that the alignment accuracy is indeed close to the $\lambda/50$ rms indicated by an analysis of Figure 201.

The knife-edge test is a traditional test that is useful for showing the location of mirror errors but is very difficult to obtain good quantitative information about error amplitude, especially for near-diffraction-limited systems. A sample knife-edge photograph is shown in Figure 205. A similar picture of the mirror figure can be obtained in an interferogram as shown in Figure 206. The knife-edge test does not include errors introduced by the interferometer optics, but, as previously noted, these are small.

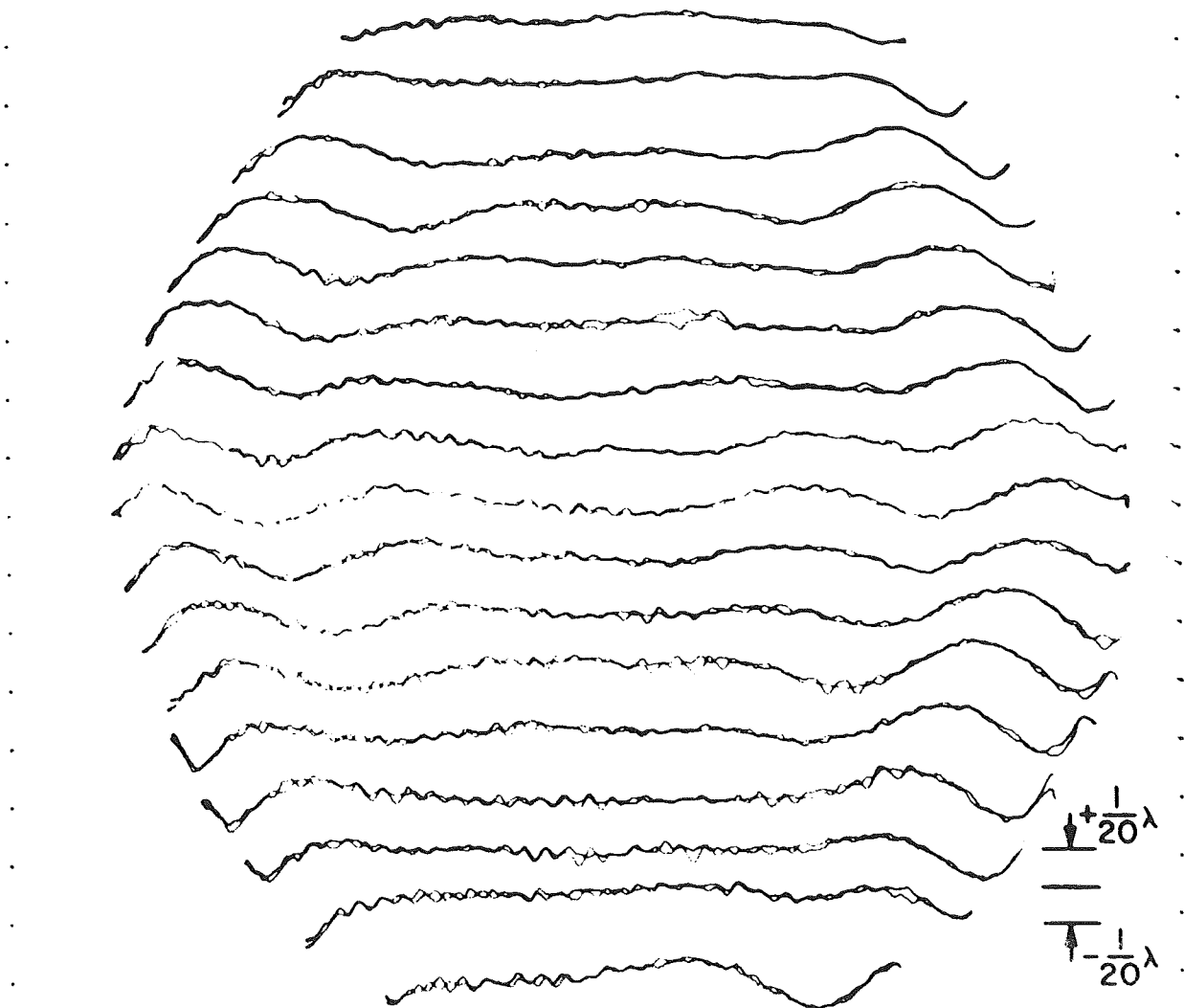
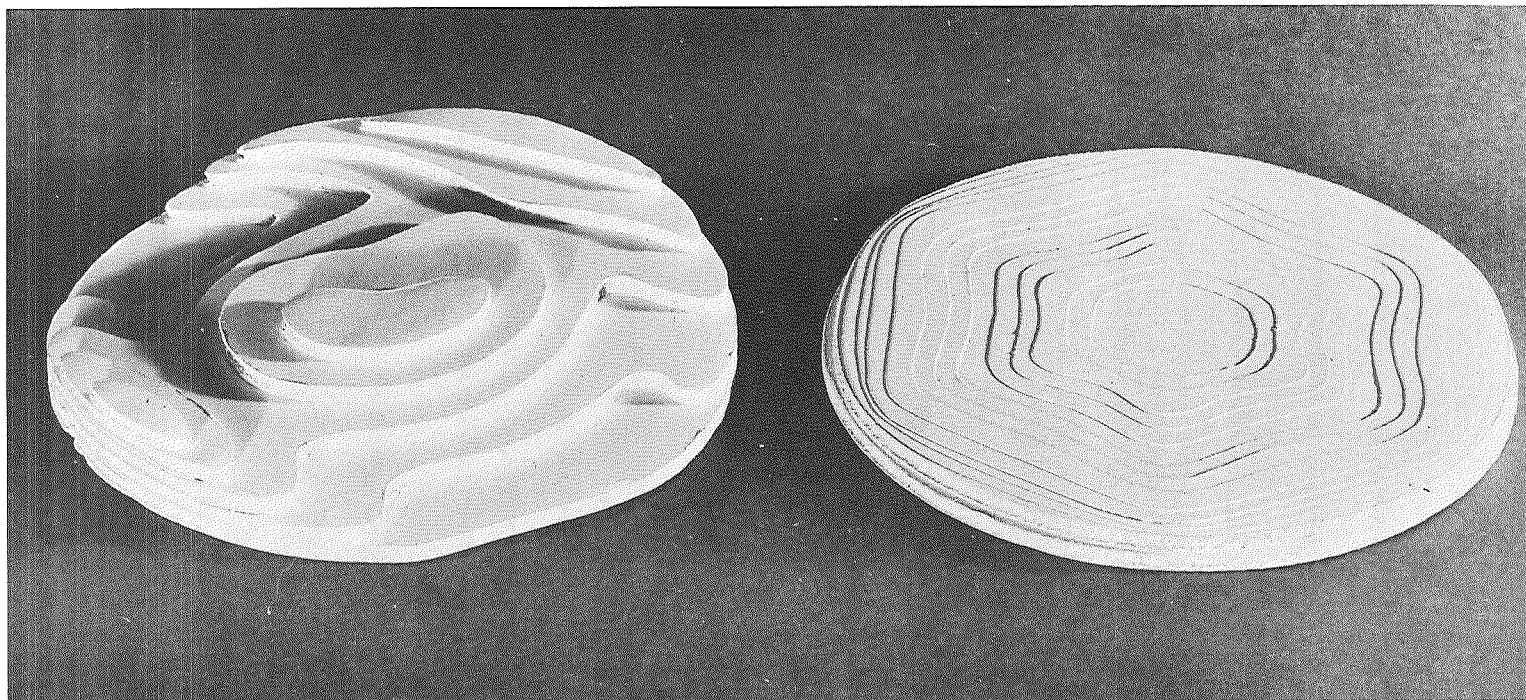


Figure 202 . Mirror Figure Profile Scans from Phase Detector Output
During Active Control

I-400



Each Level Represents $1/2$ Wave

Each Level Represents $1/40$ Wave

Figure 203 . Models of Mirror Deviations From Spherical Surface Before and After Alignment

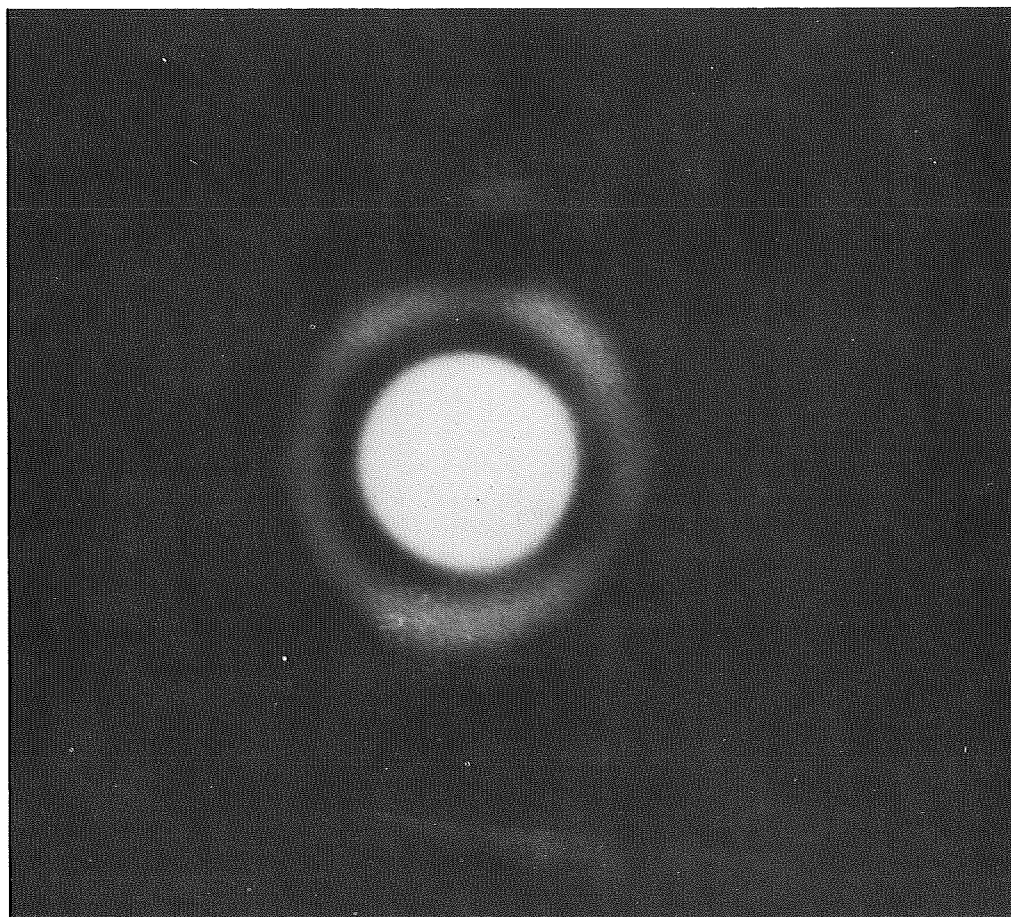


Figure 204. Image of 0.0001-inch Pinhole Source at Mirror Center of Curvature. (5000 X Enlargement)

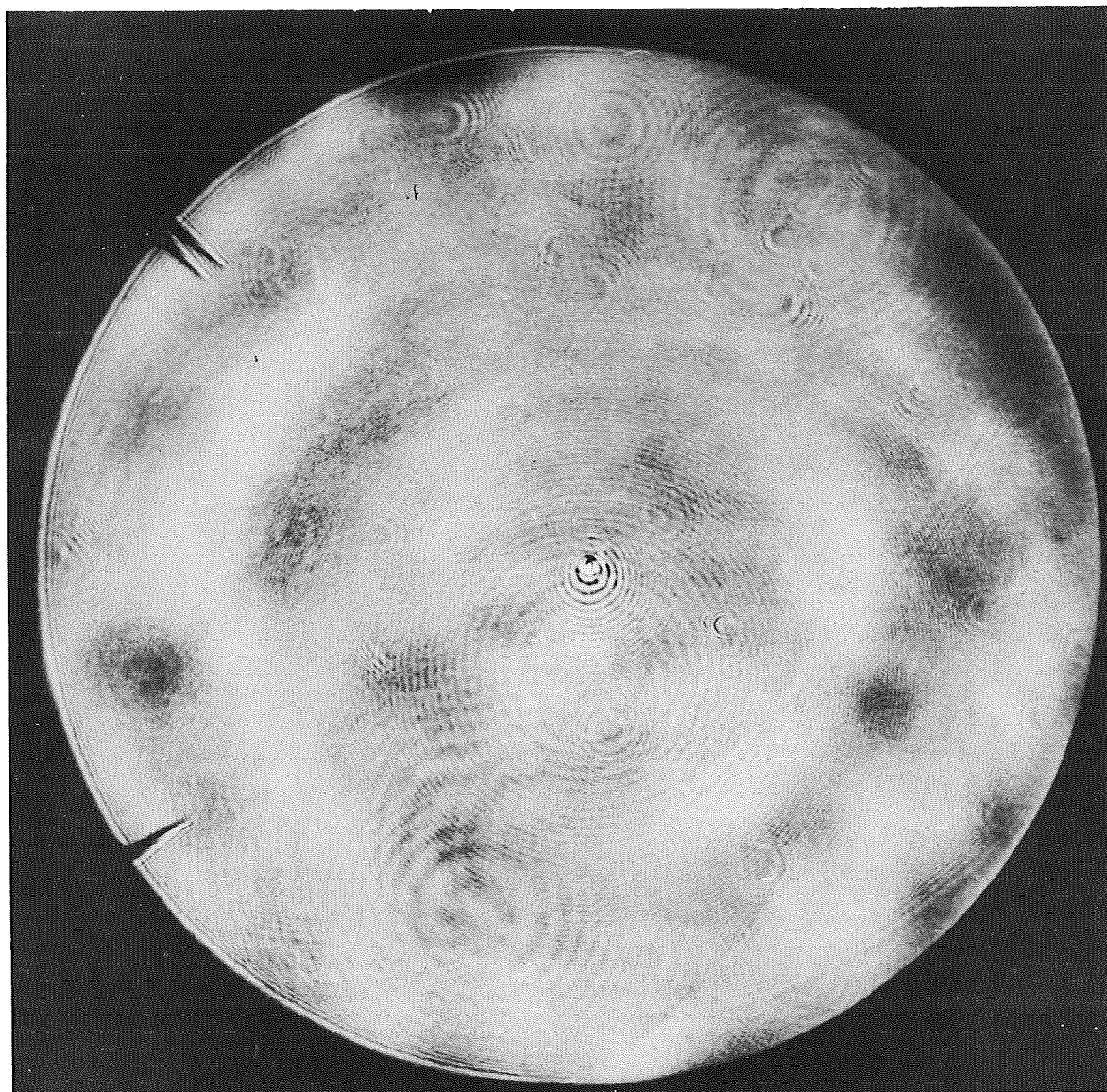


Figure 205 • Foucault Knife Edge Test of 30 Inch Mirror After Alignment

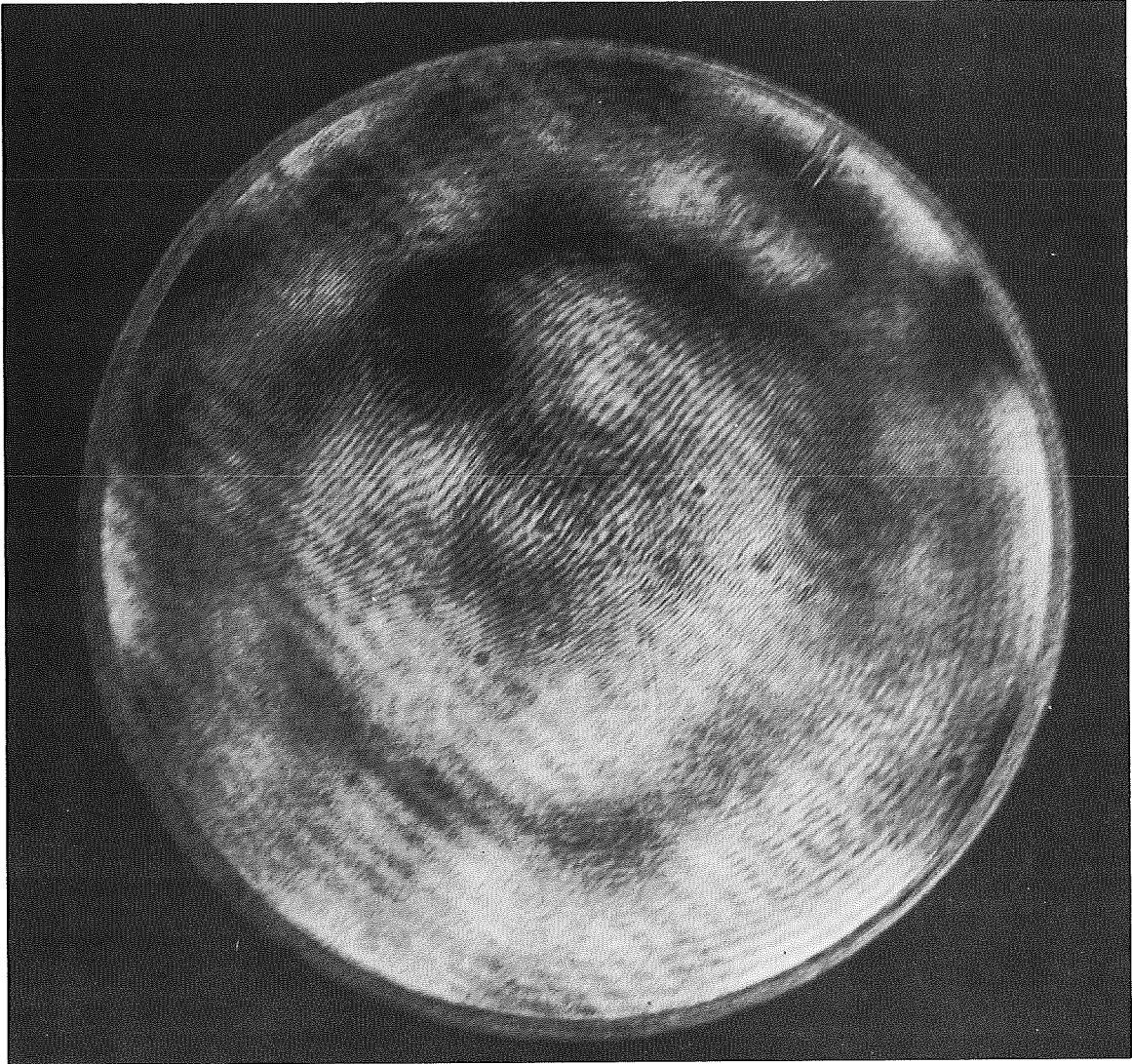


Figure 206. Interferogram of 30 -Inch Mirror After Alignment

EXPERIMENT NO. 8 - ACTIVE OPTICS USING HEAT CONTROL

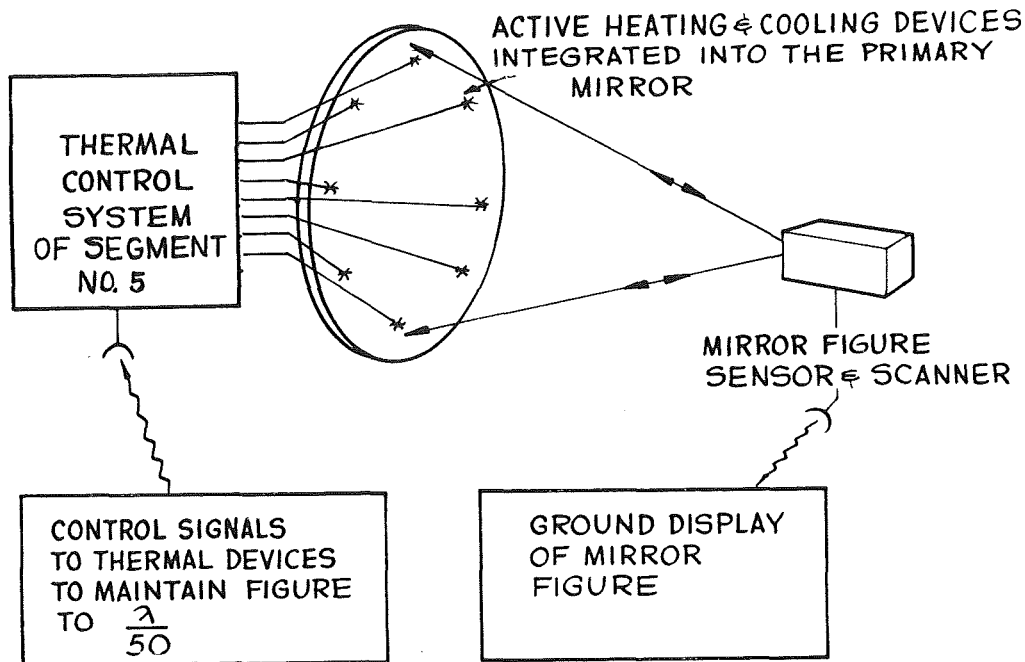


Figure 207. Scheme For Thermal Control of Mirror Figure

Experiment Objective

The objective of this experiment is to monitor the figure of a monolithic mirror using active heating and cooling devices, and to control the mirror thermal devices from the ground in coordination with a ground display of current mirror figure while attempting to maintain an overall rms figure of $1/50$ wave over the entire mirror.

Experiment Justification

It is clearly recognized that the need for a future, large, orbiting telescope will require some form of figure sensing (Experiment No. 1) and some form of active control to keep the figure of the mirror to $1/50$ wave rms. The segmented approach to Active Optics (Experiment No. 1) and the deformable mirror are two active approaches to the solution of maintaining diffraction-limited performance of large-aperture optical system. A third approach is the utilization of heating (and possibly cooling) elements integrated with the primary mirror system to force the mirror into an isothermal condition in the presence of undesirable

thermal gradients or to induce gradients into the mirror to make minor corrections to the mirror figure of a conventional mirrors. Active thermal control has been used successfully in France on an 0.8-meter ground telescope.*

The application of this approach to an orbiting telescope is considerably more complicated since the desired resultant figure is an order of magnitude (or two) more precise and the thermal environment is considerably more difficult. However, the technique itself is worthy of evaluation and, at this point in the LTEP program, the preliminary analysis which has been conducted is sufficient to include the approach as a proposed experiment. This experiment has the advantage that it is complementary to the deformable mirror in that a single mirror may be equipped with both force actuators and thermal sources without unreasonable complication.

Active Thermal Control Discussion

The nonuniform and constantly varying temperature environment of space will induce temperature gradients in a large mirror structure and, thus, deform the reflecting surface. The proposed experiment is concerned with various aspects of the solutions available to an equipment designer to correct thermoelastic figure deformations of a large mirror in orbit.

The approach is the technique of obtaining active optics through thermal control by using discrete heating (and/or thermoelectric cooling) cells in the back of the mirror. Such an arrangement cannot equalize a given nonuniform temperature distribution, but it can introduce appropriate additional thermoelastic deformations to the mirror in order to restore the figure of the reflective surface. Undoubtedly, this approach will involve complicated relations between the measured figure error and the appropriate activation of thermal cells.

Additional ground experiment work is in order and should be performed before a space experiment is actually committed. A. Couder* of France used this approach some years ago for correcting mirror distortions which are symmetrical to the axis of the mirror. He has reported on the satisfactory correction of the "edge effect" by means of a circular heating pad applied to the back of a 0.8-meter mirror. The described "edge effect" is commonly encountered with Earth-based telescopes during the temperature drop of a night-time observation. The outer ring of the mirror disk adjusts itself faster to the environmental temperature than the center. This results in a "turned-down edge" of the optical surface. He found that a power of 3 watts into the heating pad corrected an "edge effect" of 1/2 wave. He also demonstrated with some knife-edge cut-off photographs that a power dissipation of 25 watts over-corrected the mirror and produced a pronounced turned-up edge.

The power necessary to implement the heating cell method described above for active thermal control has been calculated as less than 14 watts. This is based on the approach that the heat input required by the mirror is determined by the magnitude of the nonuniform part of the radiant coupling to the mirror from the telescope side walls and the open front end. Also, the thermal conduction through the mirror mount structure to the telescope and spacecraft structure can be controlled to negligible values. The telescope tube is relatively long as illustrated in the concept drawings for the 2-meter aperture telescope.

*Beer, A.: Vistas in Astronomy, Vol. I, Pergamon Press, New York, 1955, p. 372.

EXPERIMENT NO. 9 - 1/100 ARC-SECOND TELESCOPE POINTING TECHNOLOGY

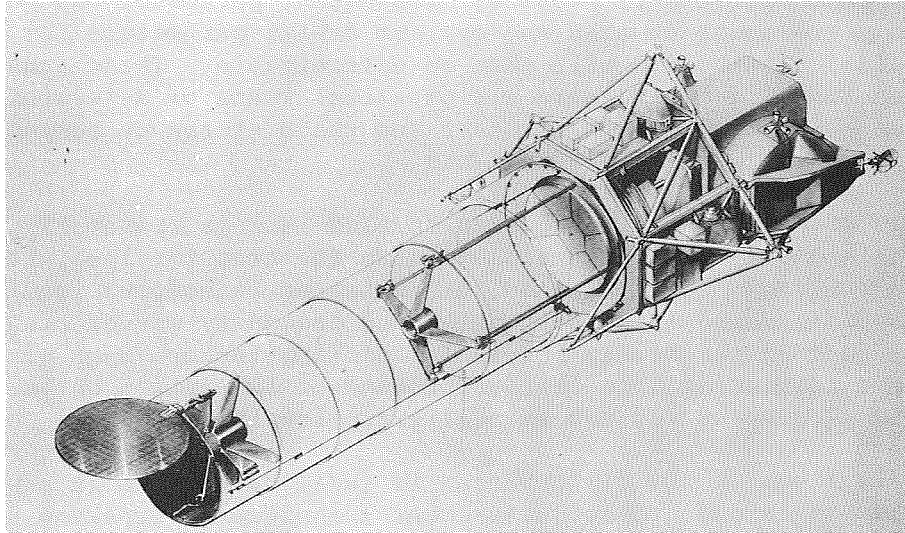


Figure 208. LTEP Technology Telescope

Experiment Objective

This experiment is concerned with the stringent requirements associated with 1/100 arc-second pointing of space astronomical telescopes.

The objective is to develop suitable space transfer lens techniques as well as body point techniques for orbital telescopes. The finest precision pointing techniques for large-aperture space optical systems are needed. This experiment will involve the compensation of pointing error sources and development of new technology to approach the pointing stability of 0.005 arc-seconds or less. This experiment can also provide knowledge of the design features required to optimize the pointing performance of an even higher resolution telescope such as the 3-meter OTES or MOAT with the corresponding improvement in pointing to 0.003 arc-seconds. Because of the low orbit, this experiment group must use stars as guidance targets but an attempt to use Earth laser beacons would be worthwhile.

Experiment Justification

Performing this engineering experiment in space can verify that 1/100 arc-seconds (or less) pointing systems can be reduced to practice and, therefore, permit the technical go-ahead of the generation of space observatories for the 1970-1985 period. The gathering of data of 1/100 arc-second pointing performance in the presence of orbital vehicle disturbances and during daylight as well as darkness provides the justification for this experiment.

Telescope Pointing Experiments

The availability of such information will allow assessment of pointing system performance, via comparison of measured and predicted results, and serve to uncover unexpected affects if present. Subsequent parameter changes required for improved performance of LTEP and optimum design parameters for other orbiting optical instruments will then be determinable. These ends can be met with each LTEP module if there are practical means of determining and/or adjusting the guidance system parameters and the disturbance components and evaluating the resulting error components.

Determination of disturbance components in each case is reasonably straightforward. For example, when operation is with the transfer lens, its motions represent very closely the positional disturbances introduced from vehicle motions plus disturbance effects of noise and secondary mirror mispositioning. The latter effect arises mainly from solar heating-induced warp of the structural tube and, depending upon the performance of the active optic alignment correction loops, may be reduced to negligible levels.

Separate evaluations of the disturbances is facilitated through controlled pointing experiments and proper measurement techniques. Tracking of bright guide stars in a direction nearly opposite to that of the sun, for example, will accentuate the relative magnitude of vehicle mispointing by suppressing the other disturbance effects. Moreover, a separate estimate of noise in signal is possible under these and other conditions, since the noise spectrum is white (or flat) up to frequencies well above both the transfer lens loop bandwidth and the highest frequency associated with expected disturbances. This permits the use of bandpass electronics for noise measurements in the frequency range uncorrupted by other disturbance effects. Subsequent measurements, run with the LTEP line of sight at other angles more nearly normal to the solar flux, will have introduced controlled amounts of incident solar flux, and hence any secondary mirror mispointing which may accompany the resulting structural warp.

Spectral analysis of the transfer lens motions as monitored during track can be employed for further separation of positional disturbance into components; viz, the structural warp fundamental frequency equals the orbital frequency while the gravity gradient is twice this. Similar analysis of the recorded error signals (from the guidance error sensors) will aid in determining the resulting error components which are related amplitude and phasewise to the disturbance by the guidance loop transfer function. This system characteristic is well known in the case of the transfer lens approach, since it is a function of variable parameters (loop gain and time constants) and fixed constants (transfer lens mass, etc.) which are readily determined during ground testing.

In the free float guidance configuration, a similar approach is possible to determine noise and disturbances, which will be primarily torques applied directly to the telescope. Recorded torquer signals would serve here for disturbance indication in a fashion similar to that stated previously for the transfer lens position signals. The situation is a bit more complex but nonetheless similarly analyzed when the gimbal axis suspension system is operating. Torquer signals as well as gimbal axis angles would be recorded, the former signals serving to identify the total torque disturbance acting on the LTEP telescope and the latter signals (plus the known gimbal bearing spring constants) providing data on those torque components attributable to bearing imperfections.

We shall now discuss the methods of pointing error evaluations in greater detail. The most direct method in the case of the imagery modules (C and F) is to examine the resulting photos. This is most meaningful from the standpoint of astronomical usage since the end result, the photo, places in clear evidence whether the line-of-sight stability was adequate during the exposure. Short exposures on bright objects can indicate the optical degradations separate from the overall error, including mispointing, shown by photos of longer duration taken on dimmer objects. Controlled experiments involving different guide star brightness, LTEP orientations relative to the sun, gain, shaping, etc., can, as mentioned earlier, accentuate or suppress one or more components of disturbance and/or the resulting errors. However, the delineation of error components by such means with photographic analysis is considered to be very limited and, hence, photo inspection is normally used only as a means of clearly substantiating results obtained by different means. One such alternate would involve the use of a separate sensor positioned to split a relatively bright star image into two or four parts. The temporal error signals recorded from this sensor, when the telescope is guiding on other stars, can be much more readily spectrally analyzed. The use of the bright star, it should be noted, reduces the associated signal errors due to noise to acceptably low levels. The foregoing methods were utilized to demonstrate the tracking performance of the Stratoscope II system, both when aloft and with the instrument fully operative in a vacuum-cold chamber to simulate actual conditions aloft. This was conveniently done with the instrument in its primary operational mode, wherein one sensor generates x-y pointing errors while a gyro serves as a roll reference. The other star sensor, normally reserved to provide roll error information should the gyro fail, was used to monitor the mispointing. Extra equipment for the express purpose of pointing performance appraisal was thus avoided, a fortunate circumstance which will not prevail in LTEP. A less direct method, also demonstrated with Stratoscope for in-flight performance evaluation, utilizes error signals as generated by the normal guidance star-splitting photosensors and hence requires no added equipment. The approach is depicted in Figure 209 which also shows some typical response characteristics to make clearer the following description. Actual line-of-sight disturbances are represented by θ_{in} which, in the transfer lens arrangement, are vehicle pointing errors. The transfer lens motions introduce corrections θ_o and reduce the overall line-of-sight error to ϵ . The signal V to the control electronics is related

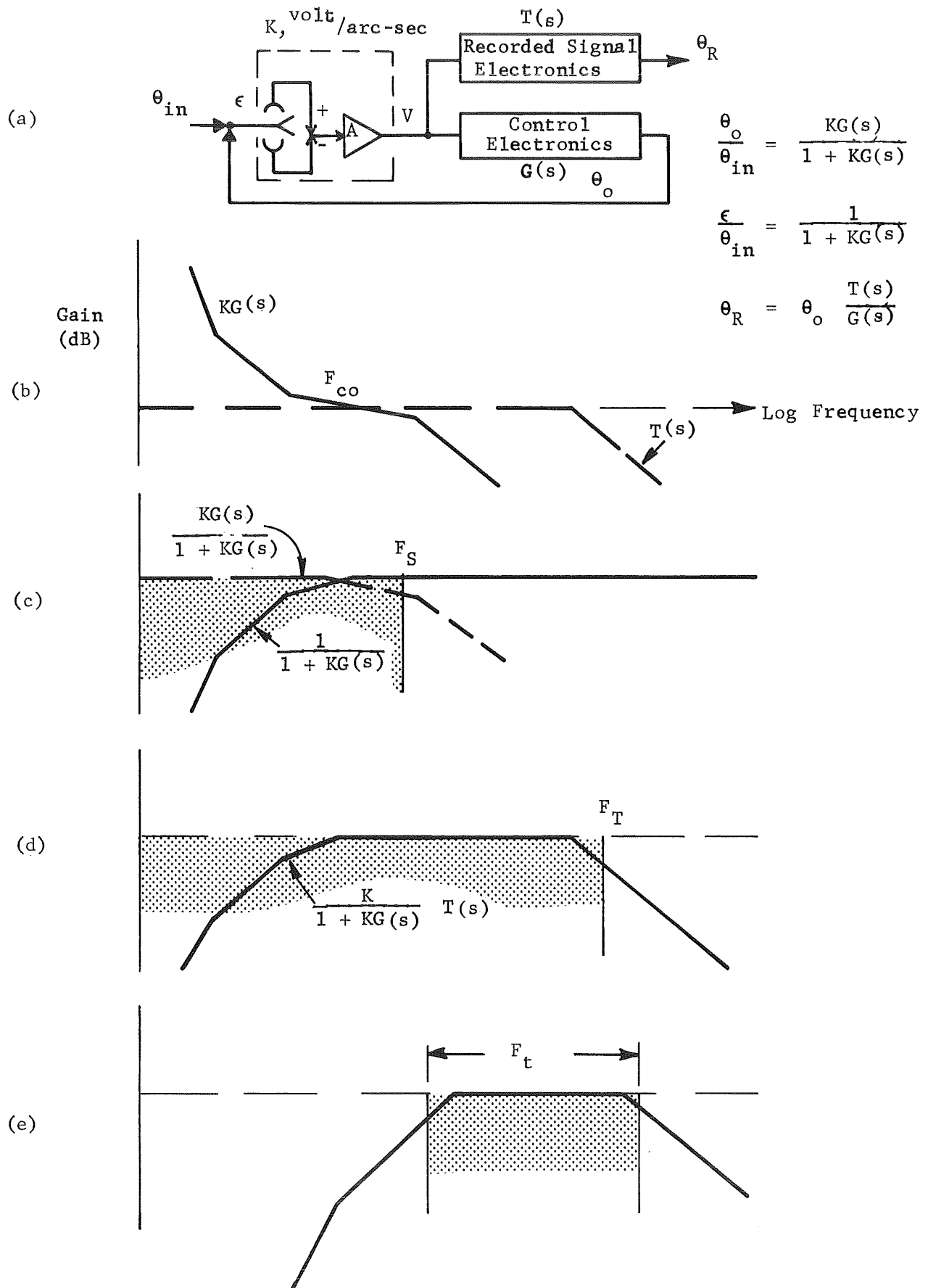


Figure 209. Open Loop-Closed Loop Relationships

to the actual pointing error through the scale factor K and hence indicates the actual residual pointing error. Thus the recorded signal θ_R contains an error component

$$\theta_{R_1} = \epsilon KT(s)$$

representing actual pointing error,

$$\epsilon = \theta_{R_1} / KT(s).$$

This is related by an amount $1 + K(s)$ to the actual disturbances with which an amplitude and phase comparison is then possible. This straightforward procedure is complicated by the presence of noise inputs for which any resulting θ_o components are errors. Noise components at low frequency can produce significant errors (θ_o motions) without appreciable monitored voltages (V): i.e.,

$$\frac{\theta_o}{\theta_{noise}} \triangleq \frac{\theta_o}{\theta_{in}} = \frac{KG(s)}{1 + KG(s)} \approx 1$$

when $KG(s) \gg 1$

$$\frac{V}{\theta_{noise}} \triangleq \frac{V}{\theta_{in}} = \frac{K}{1 + KG(s)} \approx \frac{1}{G(s)}$$

Hence, noise-induced error is not accompanied by a proportionally related signal V to the recording electronics as in the case of actual vehicle pointing errors. However, an approach for determining noise errors is possible provided the noise spectrum is of known shape. For the case of white noise (which is usually encountered), the rms value of θ_R can be used to determine the noise per root cycle since the effective noise bandwidth of $KT(s)/1 + KG(s)$ is determinable. The value so obtained can then be used to infer the noise error in the system whose response is $KG(s)/1 + KG(s)$ and whose equivalent noise bandwidth is F_s . Consider the output power spectrum Φ_o of a filter which has a white or flat input power spectrum Φ_{in} . The relationship between the spectra is determined by the filter transfer function $H_{(w)}$; i.e.,

$$\Phi_o = H_{(w)} H_{(w)}^* \Phi_{in}$$

where the * designates the complex conjugate. The rms output signal θ_o can be expressed as

$$\theta_o|_{rms} = \sqrt{\int_{-\infty}^{+\infty} \Phi_o dw} = \sqrt{\int_{-\infty}^{+\infty} H_{(w)} H_{(w)}^* \Phi_{in} dw}$$

For the special case where Φ_{in} is flat over the frequency range where $H(w)$ is significant, the foregoing expression reduces to

$$\theta_o|_{rms} = \sqrt{2\pi \Phi_{in}} \sqrt{\frac{1}{2\pi} \int_{-\infty}^{+\infty} H(w) H^*(w) dw}$$

where the first radical for present purposes represents the random input noise component in equivalent arc-seconds rms per root Hz and the term under the second radical represents the filter's equivalent noise bandwidth in root Hz.*

A servo with open loop characteristic $KG(s)$ (Figure 209b) will respond to noise inputs as determined by the expression

$$\text{closed loop response} = \frac{KG(s)}{1 + KG(s)}$$

when unity feedback exists (refer to Figure 209c). The equivalent noise bandwidth F_s can be determined as indicated by the preceding expressions and used to obtain the rms pointing error from

$$\theta_o|_{noise} = \sqrt{2\pi \Phi_{in}} \sqrt{F_s}$$

On the other hand, the noise-induced rms value of θ_R is expressible as

$$\theta_R|_{noise} = \sqrt{2\pi \Phi_{in}} \sqrt{F_T}$$

where F_T is the equivalent noise error determined from the θ_R transfer function H_R (refer to Figure 209c and d).

$$H_R = \left[\frac{K}{1 + KG(s)} \right] T(s)$$

Hence the estimated value of noise-induced pointing error is simply expressible as:

$$\theta_o|_{noise} = \theta_R|_{noise} \sqrt{\frac{F_s}{F_T}}$$

The problem with this approach is that the recorded signal may contain significant low frequency error components arising from disturbances. These will lead to erroneously increased values for noise-induced pointing error, unless the troublesome low frequency components are sufficiently attenuated by shaping $T(s)$ to have a bandpass characteristic (refer to Figure 209e).

*The resulting equivalent noise bandwidth, ENBW, is based on a double-sided spectrum and thus is twice that obtained when a single-sided spectrum is assumed.

The inflight tracing accuracy of Stratoscope II evaluated by the foregoing methods was found to be between 0.015 and 0.05 arc-second rms when tracking with guide star images between 0.5 and 2.0 arc-seconds in diameter. Moreover, it was estimated that the pointing precision with good quality images would be improved by a factor of at least 2.5. Inspection of the photographic images with a microscope and with a microdensitometer revealed details between 0.06 and 0.1 arc-second indicating that pointing accuracy must have been at least as precise (refer to Figure 210 and 211). Microdensitometer slit scans of the photographic negatives were also made to determine the scale factor of the guide star pointing error sensor. This was then used to determine the pointing error from signals monitored by the redundant guide star sensor. Figure 210 shows a representative negative from which the slit scan result of Figure 212 was obtained. Figure 213 is a trace of the signal as monitored during this exposure. The rms error is determined as shown once the scale factor is obtained by integrating the slit scan result and estimating the slope at the center of the resulting curve (refer to Figure 212). Since the scale factor is 33.5 volts per arc-second for diffraction-limited images, then the actual scale factor is expressible as

$$\left[\frac{33.5 \text{ vdc}}{\text{arc-second}} \right] \left[\frac{\text{Actual Slope}}{\text{Diffraction-Limited Slope}} \right]$$

The tracking errors so determined were found to agree within a factor of about 2 with the results from analysis of the actual guidance sensor signals. Closer agreement could be expected had diffraction-limited images been available.

Since all present LTEP modules involve guide star image splitters for generating error signals, pointing assessment can be accomplished by analysis of such signals. Corroboration for the results in the case of the imagery modules (C and F) can best be accomplished by analysis of the final photos. In the cases of the Rowland Circle spectrometer, substantiation of results can be obtained by observing and analyzing the light level fluctuations at a fixed wavelength. (Information regarding the relationship between mispointing and fluctuations is presented elsewhere in this report) in the section dealing with pointing accuracy requirements. In the Rowland circle instrument, operation with the target star image intersected by one edge of the entrance slit will accentuate the fluctuations due to pointing errors. This condition will ease the task of pointing assessment by increasing the signal-to-"noise in signal" ratio for the light level sensor and, in addition, by linearizing the relationship between error and sensed signal. Under such circumstances, it should be noted, the spectrograph operates in effect as an independent single-axis tracking error monitor.

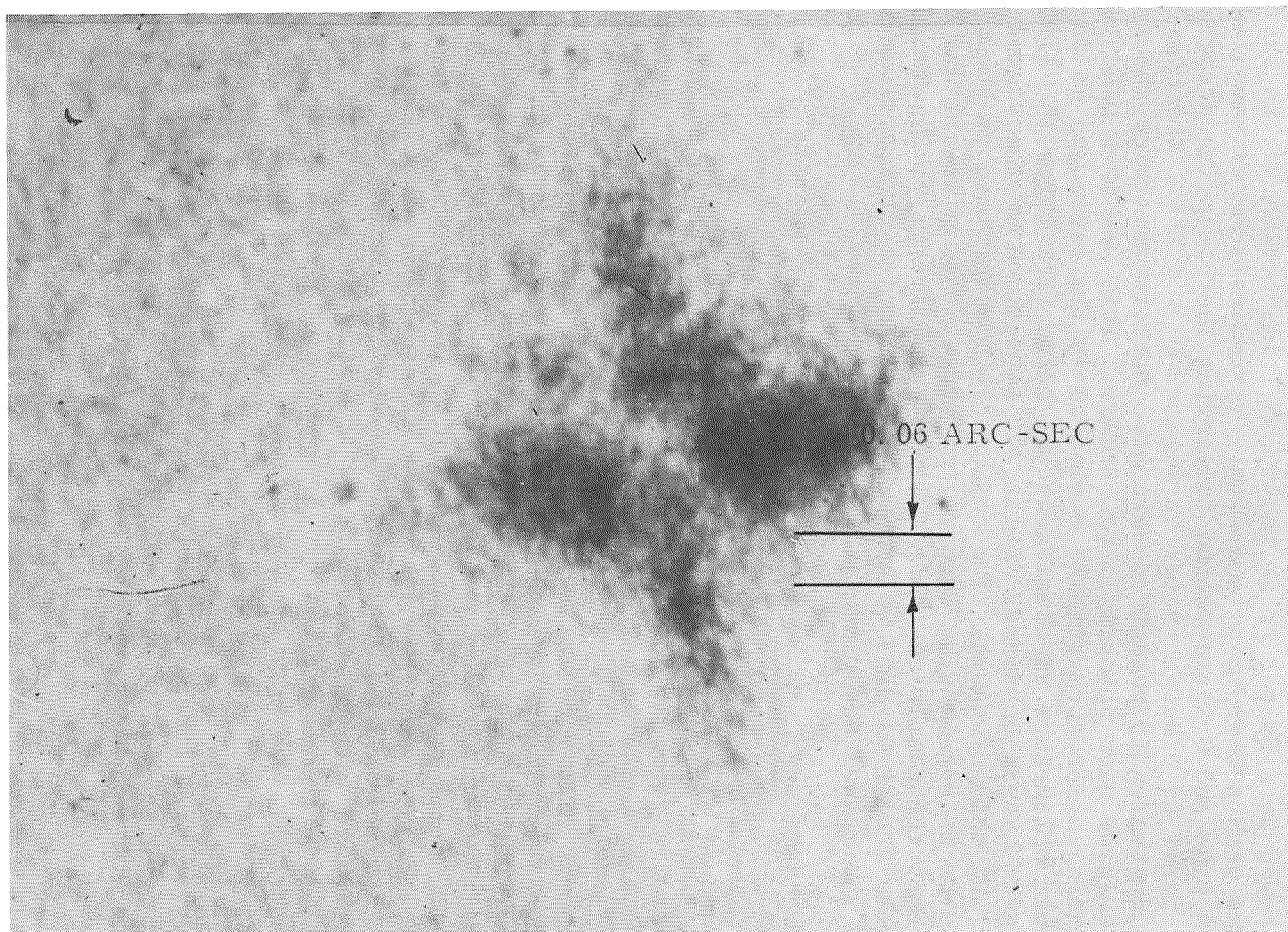


Figure 210. Example Image
 Negative: #64
 Object: M8
 Exposure: 60 Sec

SOURCE: M8
NEGATIVE: #64
EXPOSURE: 60 SEC
SCAN APERTURE: 28μ DIAMETER

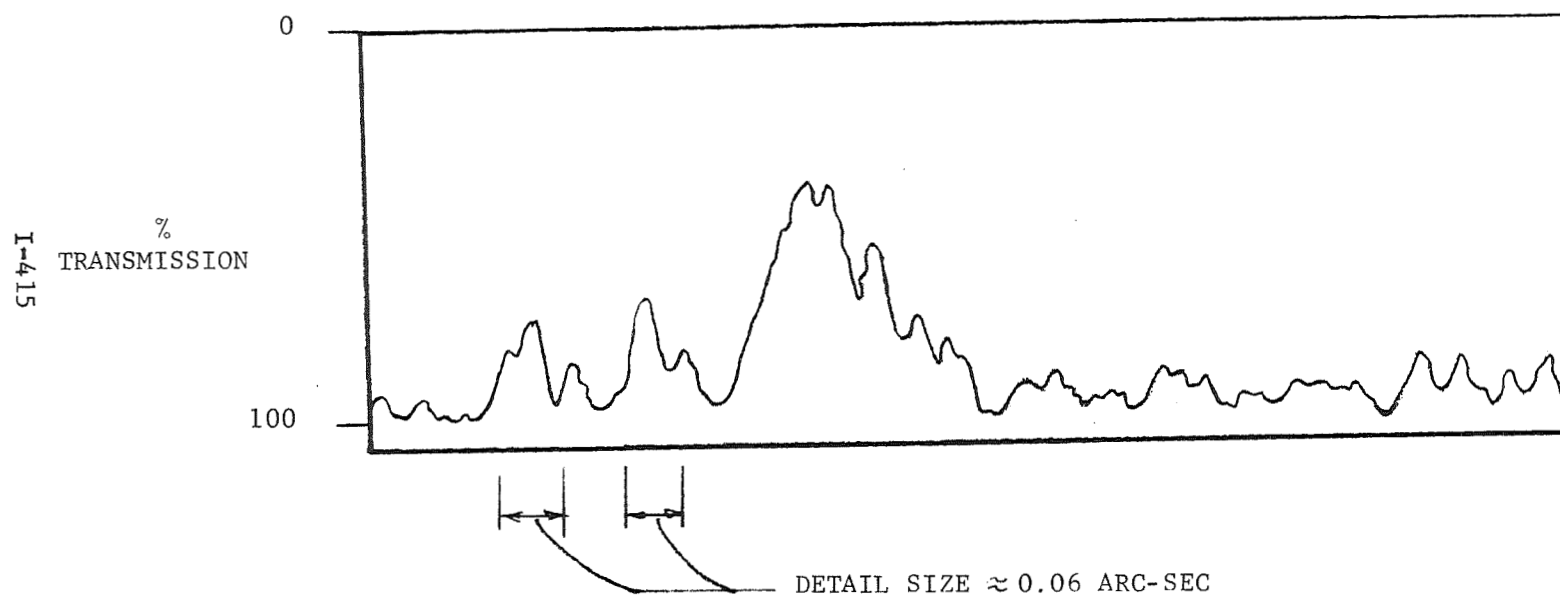
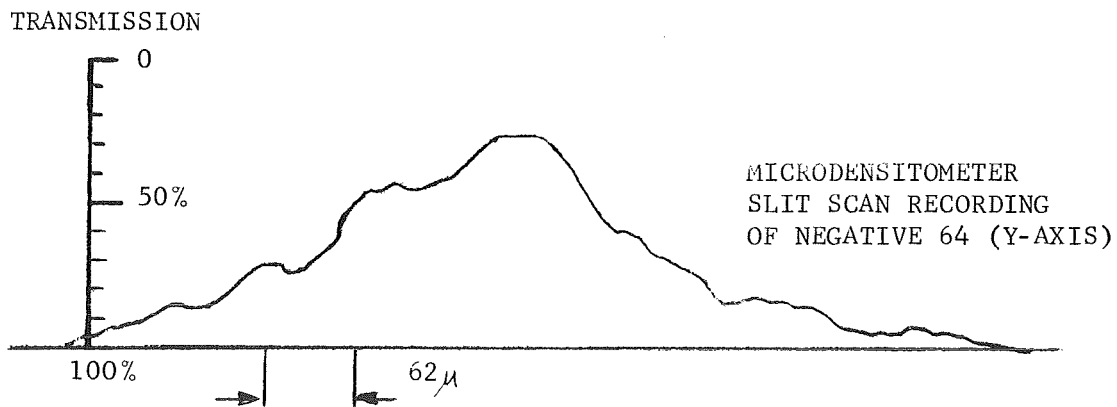
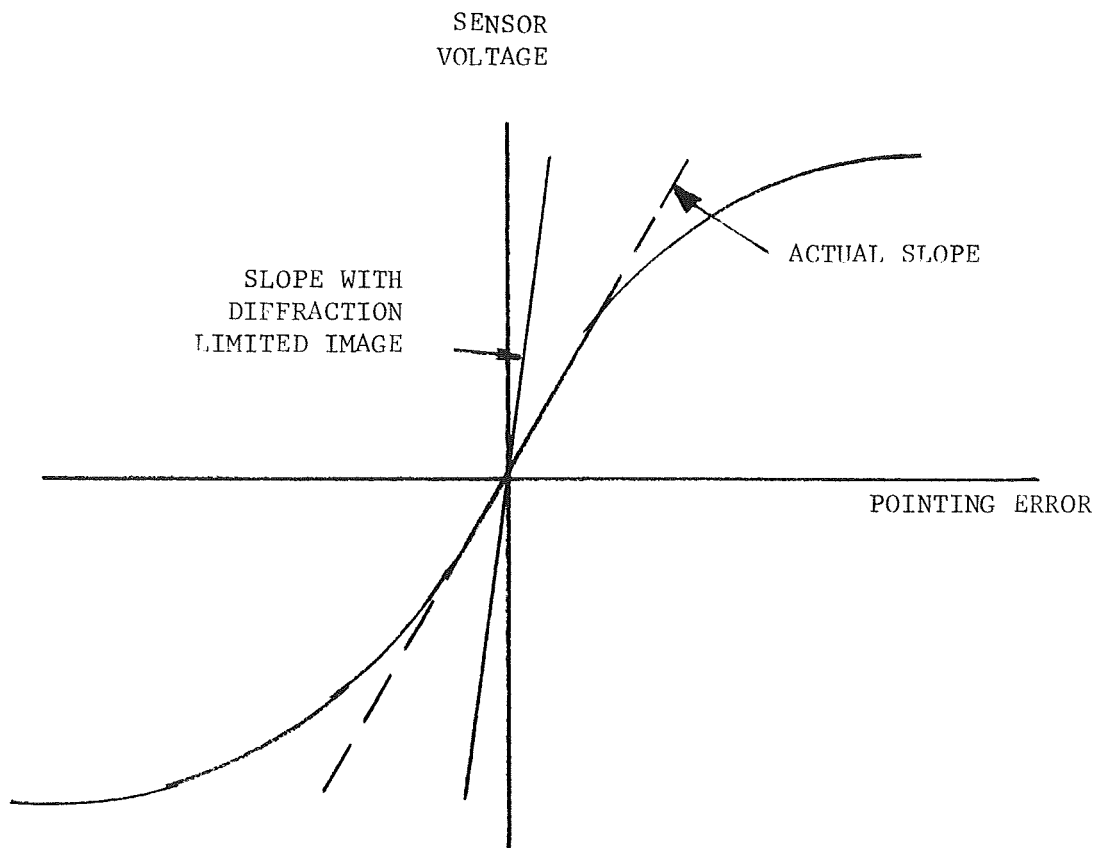
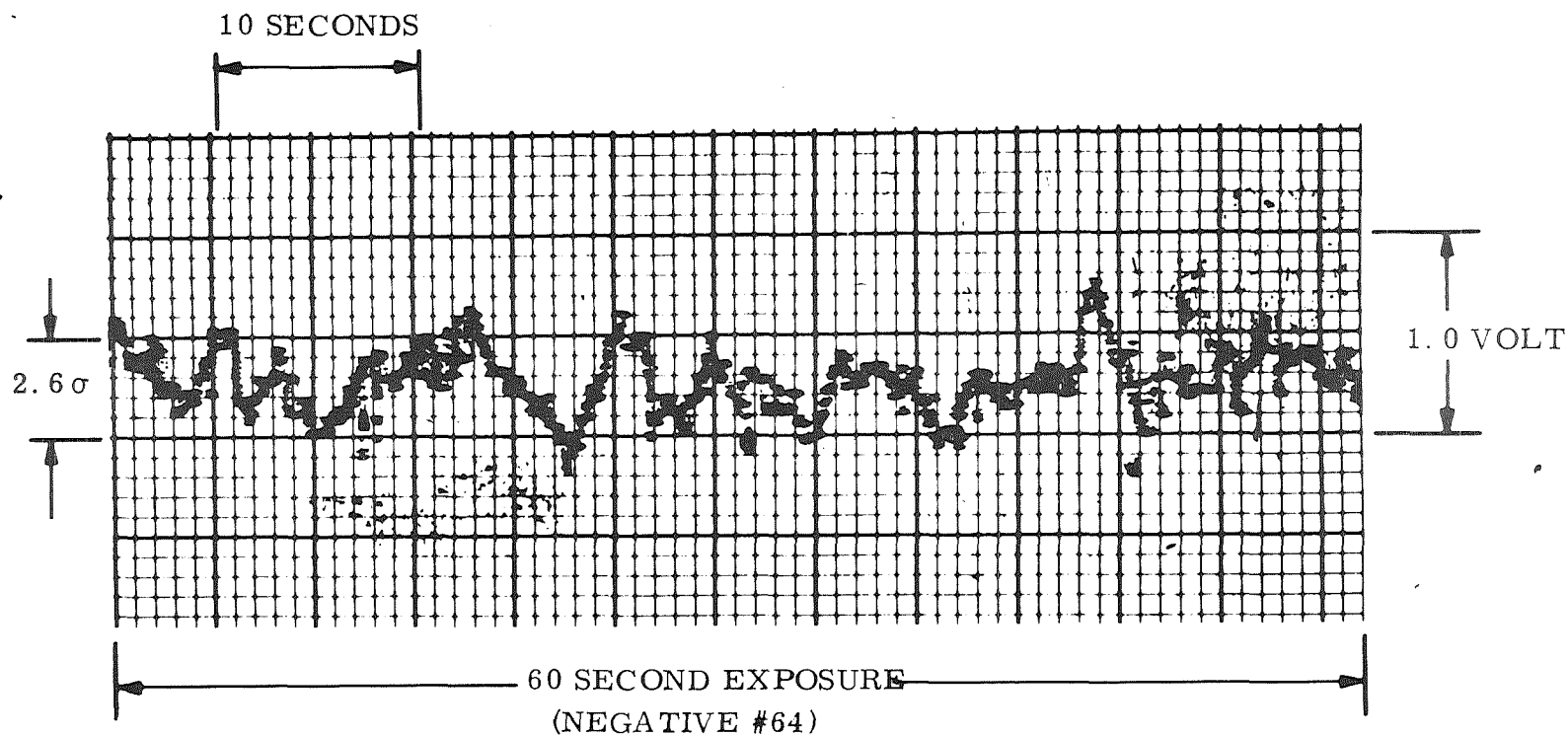


FIGURE 211. MICRODENSITOMETER SCAN OF IMAGE

FIGURE 212. SENSOR CHARACTERISTIC





EXAMPLE CALCULATION:

$$\text{POINTING ERROR ANGLE } (\theta_E) = \frac{V_E \text{ (VOLTS)}}{33.5 \frac{\text{VOLTS}}{\text{ARC-SEC}}} \times \frac{\text{DIFFRACTION LIMITED SLOP.}}{\text{ACTUAL SLOPE}}$$

FIGURE 213. TYPICAL POINTING MONITOR SIGNAL

I-417/418

EXPERIMENT NO.10 - OFF-AXIS TELESCOPE PERFORMANCE

(Deleted - Refer to OTES Interim Report* for Additional Details)

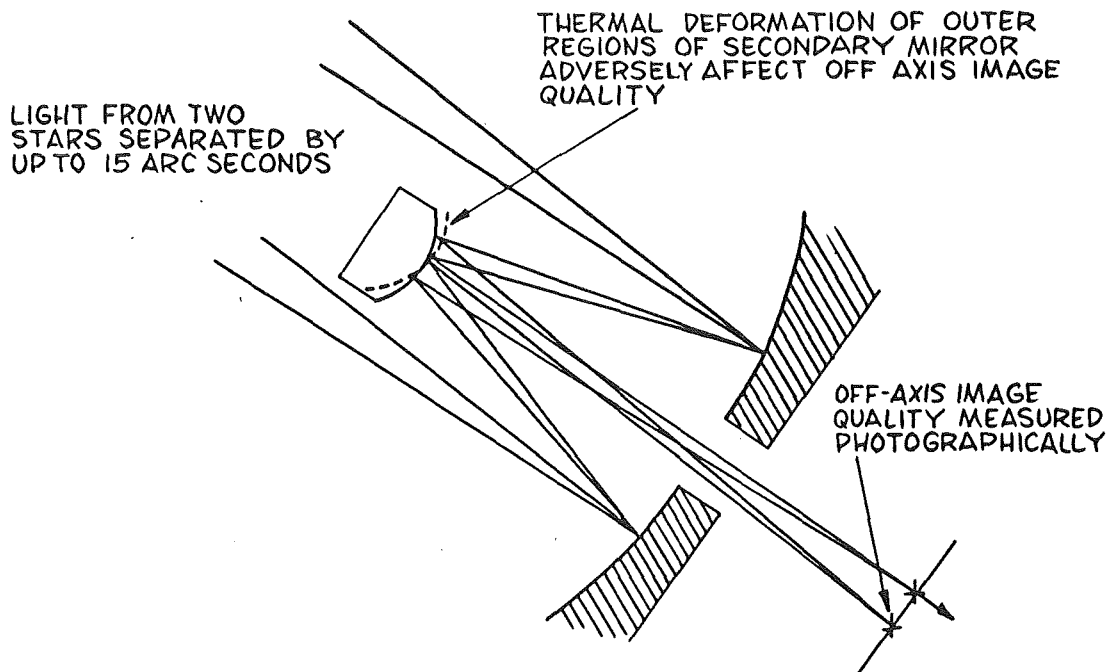


Figure 214. Telescope Geometry

Experiment Objective

A large-aperture, space optical telescope must have excellent off-axis performance as well as on-axis performance. This is necessary since the guide stars used to stabilize the line of sight for the telescope will not be on-axis, and it is these dim guide stars which are used to keep the telescope pointing at the target of interest.

Therefore, the objective of this experiment was to measure the off-axis performance of the astronomical telescope by analyzing images at several points in the focal plane. While photographs might be taken of the magnified image, this is a difficult task for a large telescope on station in space.

This experiment was removed from further consideration as a recommended experiment because the significance of the results, when obtained, would not be comparable with the importance of the other recommended experiments.

* Perkin-Elmer Report No. 8319

PERKIN-ELMER

EXPERIMENT NO. 11 - RADIATION NOISE EFFECTS ON DETECTORS IN POINTING LOOPS

(Deleted - A recently-reported research project* sponsored by NASA under contract NAS 7-100 proves that relatively little can be done to optimize the signal-to-noise in the face of radiation noise beyond simple pulse and height clipping. For this reason, the experiment is deleted.)

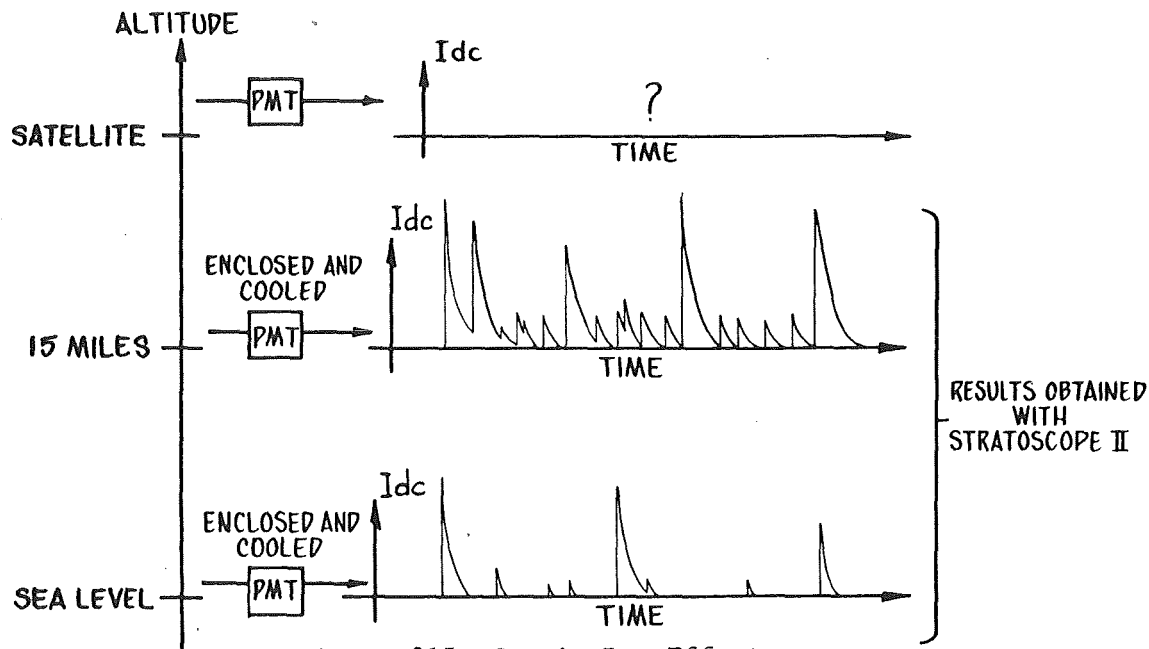


Figure 215. Cosmic Ray Effects

Experiment Objective

Pulse-height-versus-frequency measurements** on S20-type photomultiplier tubes indicate that both DC dark current and anode pulses of high amplitude are increased with operating altitude inside the Earth's atmosphere. It is suspected that these increases are attributable to cosmic rays (or the secondary radiation caused by cosmic rays). Ground tests with a Co^{60} radioactive source have yielded pulse-height-versus-frequency curves with changes very similar to those caused by operation at increased altitude. While the amount of DC current increase was modest, the noise in this additional component appeared significantly higher than predicted by Schott noise theory. This was due to the presence of very infrequent, yet unusually high magnitude, anode pulses. The objective of this experiment would be to obtain similar data, for one or more photosensors, corresponding to satellite environments. The motivation for the experiment is simply that such noise could severely degrade tracking performance in a space optical system.

Experiment Justification

This experiment, if performed in space, would be an economic trade with simulation of the radiation environmental conditions expected in space. The space data collected can be used to predict the degradations of photosensor performance to be expected as a function of shielding techniques. For example, the

*Young, A.T.: Photometric Error Analysis-IX: Optimum Use of Photomultipliers. Applied Optics, Vol. 8, No. 12, December 1969, pp. 2431-2447.

**Performed on the Stratoscope II Program at altitudes less than 100,000 feet.

type of data that could be developed during the conduct of this experiment is illustrated in the sketch shown in Figure 216. The block diagram necessary to implement this experiment is shown in Figure 217.

Technology Benefits

The information obtained would be of significant value from both the communication and the tracking standpoints. Correlation between the pulse analyzer and radiation monitoring equipment measurements would lead to a better understanding of the phenomena involved and possibly the use of presently available radiation data in photosensor output noise calculations. Photosensor design changes and shielding techniques to obtain greater radiation immunity could influence the design of future telescopes for astronomy as well as deep-space laser communication systems.

Basic Experiment Hardware

On the Ground

- Computer (to supply data on satellite position versus time)
- Data storage (for data transmitted from satellite)

In Space

- Photosensor(s)
- Pulse height analysis equipment
- Data storage capability
- Time reference
- Light source(s)
- Radiation monitoring equipment

Experiment Discussion

The radiation noise induced in photosensors operating at low light levels can critically affect the performance of pointing and tracking loops and the detectors used for optical communications experiments. Experiment No. 11 is designed to measure the pulse height distribution from a variety of photosensors and is illustrated in Figures 216 and 217. In addition to the amplitude distribution, the temporal statistics of the radiation-induced noise should also be measured since this can influence the error characteristics of an optical communication channel.

Of equal importance to optical technology is the deleterious effects of high radiation fields on the optical components required for optical tracking, imaging, and communications experiments. Earth simulation experiments can be performed to estimate the effects of the Van Allen radiation belts by bombarding samples with high energy electrons and protons, but the integrated effect of the long-term exposure to the Van Allen belt environment is best performed by experiment in space.

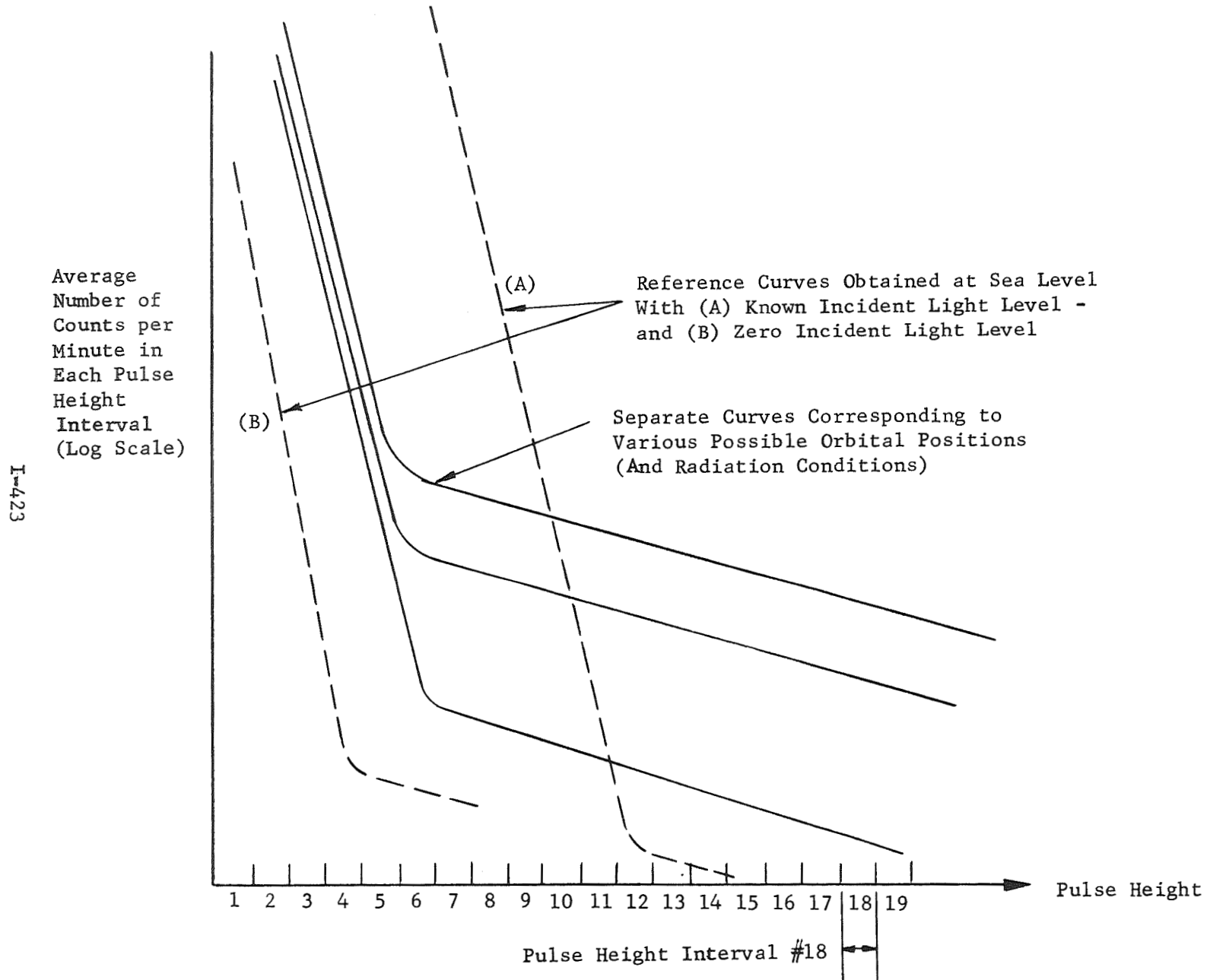


Figure 9216. Type of Data Expected for Radiation Effects Experiment

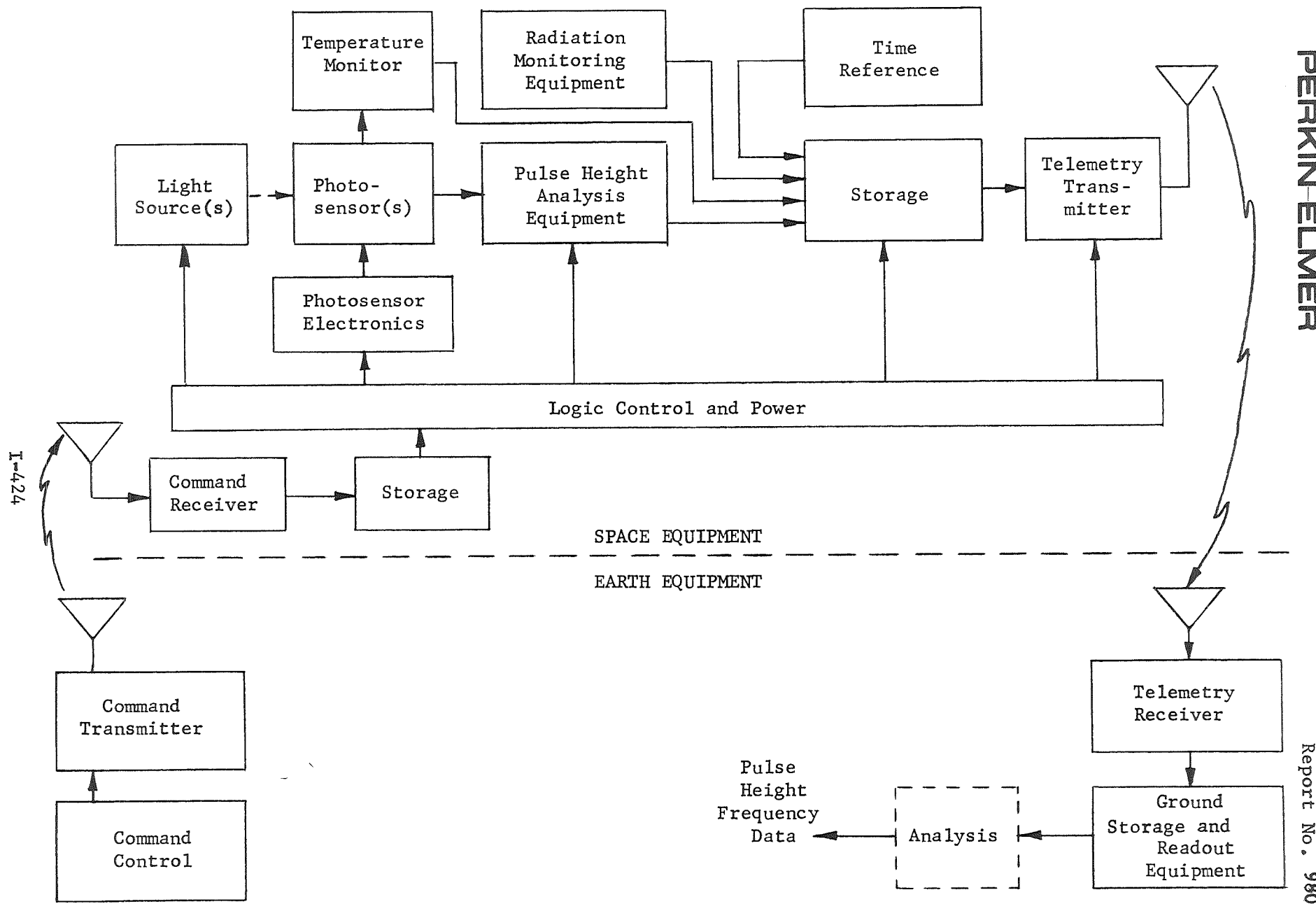


Figure 217...Block Diagram of Radiation Noise Effects on Detectors in Pointing Loops Experiment

EXPERIMENT NO. 12 - MIRROR-FIGURE MEASUREMENTS CORRELATED WITH NUMEROUS TEMPERATURE SENSORS

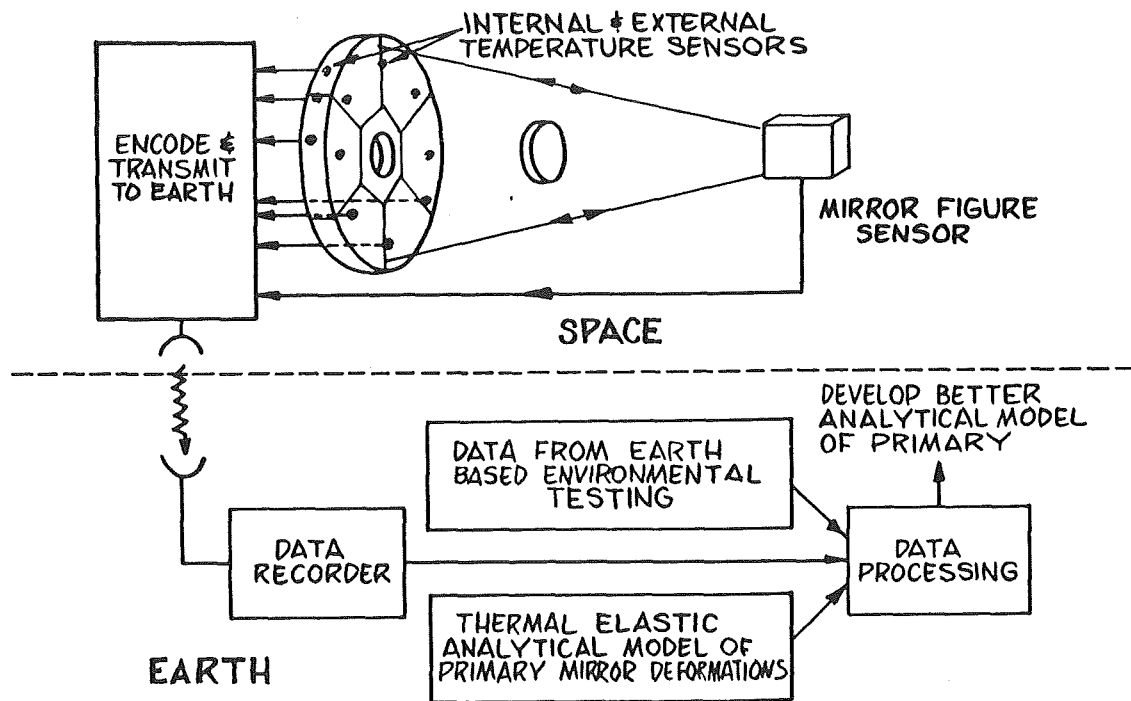


Figure 218. Thermal End Figure Data Processing

Experiment Objective

The objective of this experiment is to verify that the temperature distributions assumed in the thermal design (and simulated during the earth-based environmental tests) are actually realized in space operation.

Future large-aperture space telescopes must maintain diffraction-limited performance in the space thermal environment. In order to assess the effect of the thermal environment, with varying thermal gradients on mirror performance, it is necessary to estimate the expected range of operating temperatures, as well as the lateral and longitudinal thermal distributions. The satellite capsule itself is often exposed to the direct rays of the sun on one side and to the cold of space on the other. The expected temperature history also involves radiative interaction with the earth as well. In space applications involving operation in a vacuum, temperature gradients assume substantial proportions in the absence of the equalizing effects of convection currents which exist at normal atmospheric pressures.

It is, therefore, important to verify experimentally if the temperature distributions assumed in the thermal design (and simulated during the earth-based environmental tests) are actually realized in space operation. These earth-

based tests consist of superimposing controlled temperature distributions on the primary mirror of the space telescope and correlating this information with measurements of primary mirror-figure error.

Experiment Justification

This experiment will allow verification of the thermal elastic analytical models for the temperature distributions and primary mirror deformations as well as verification of the earth-based thermal simulation tests.

This spaceborne experiment will consist of monitoring the operating temperature distributions and figure-error deviations of the primary mirror. This data will then be encoded for real-time return transmission to earth for data processing and correlating with results of analytical thermal elastic models and earth-based environmental simulation tests of primary mirror deformations.

Technology Benefits

This experiment will provide useful empirical data on temperature distributions that will have deleterious effects on the diffraction-limited primary mirror performance.

Basic Experiment Hardware

On the Spacecraft

- Primary mirror with external and internal temperature sensors
- Mirror figure sensor and scanner
- Real-time data transmission to earth

On Earth

- Telemetry receiver
- Data recording and processing and special display equipment

A flight program such as the one proposed in Experiment No. 12 must be preceded by an experimental ground program to provide the basis for evaluating the data that would result from the spacecraft. This program is discussed in detail in the chapter on Non-Space Experiments.

Although it is beyond the scope of the present phase of the program to do the appropriate ground testing, Perkin-Elmer has performed thermal vacuum testing of a large, 32-inch eggcrate fused quartz mirror. Princeton University made available the OAO program mirror to evaluate thermoelastic deformation behavior. In addition to the controlled thermal deformation work, Perkin-Elmer has conducted the analytical work that is necessary to construct mathematical models which will establish the magnitude of the figure deformations resulting when nonsymmetrical thermal gradients are applied. This work is discussed in sufficient detail in the OTES Report, Perkin-Elmer Engineering Report No. 8900.

EXPERIMENT NO. 13 - TELESCOPE ALIGNMENT MEASUREMENTS CORRELATED WITH
NUMEROUS TEMPERATURE SENSORS

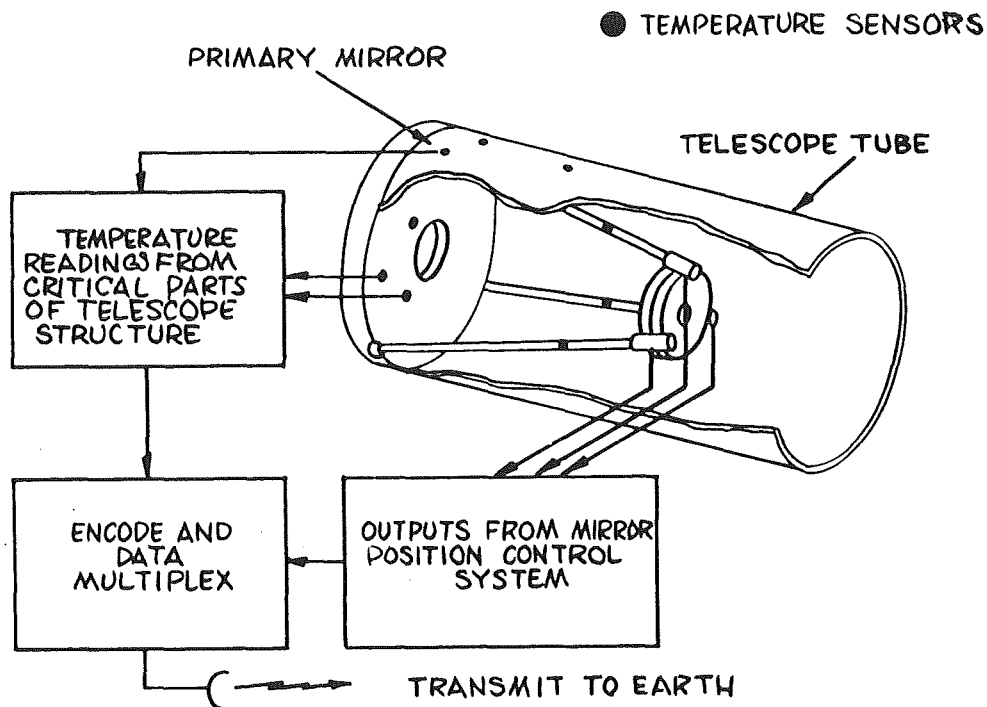


Figure 219. Thermal and Alignment Data Processing

Experiment Objective

The objective of this experiment is to measure the temperature at critical locations of the telescope structure and supporting optical subsystems. Temperature data originating from the primary/secondary telescope structure can be compared to the outputs of the secondary mirror positioning control system.

Temperature variations leading to differential expansion or contraction of various portions of the telescope structure will cause misalignment of the optics. This may be caused by inclusion of materials of different coefficients of expansion, by temperature differing from point to point, or by a combination of both.

Experiment Justification

This experiment provides measured data from space equipment for diagnostic evaluation of the telescope point-to-point thermal distributions. The measured temperatures are correlated in both magnitude and location with alignment

design requirements and alignment measurements. Future large orbiting telescopes will be dependent upon experimentally verified design values for alignment devices.

Technology Benefits

Telescope structure design changes to obtain greater immunity to the space thermal environment could be one potential outcome of the experimental results.

Basic Experiment Hardware

On the Spacecraft

- Temperature sensors
- Secondary mirror positioning control system
- Telemetry transmitter

On the Earth

- Telemetry receiver
- Data storage

EXPERIMENT NO. 14 - EFFECTS OF EARTHSHINE ON MIRROR FIGURE

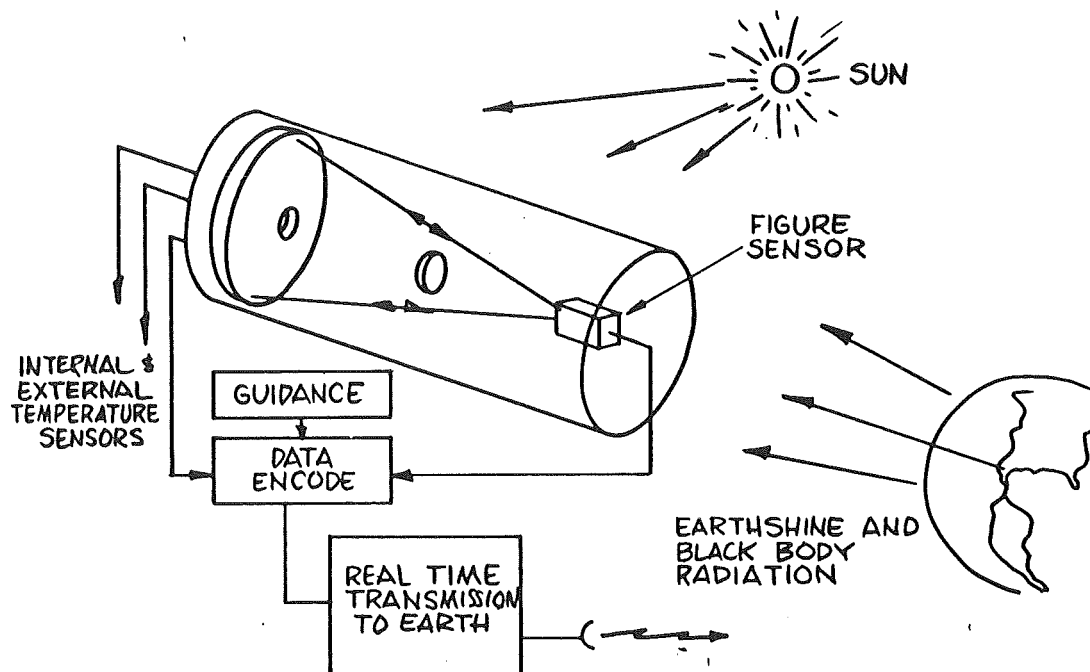


Figure 220. Thermal Data Processing

Experiment Objective

The objective of this experiment is to obtain experimental verification of primary mirror figure errors and deformations as a function of the earthshine and earth self-emission input to the primary mirror. For this experiment, the telescope is oriented earthward such that all or part of the primary mirror is subjected to earthshine radiation. The resulting change in figure is then monitored. The temperature sensors on the primary (Experiment No. 12) and the orientation and intensity of the earthshine and radiation are monitored also.

Experiment Justification

Earthshine and self-emission, which are incident on the primary mirror, can introduce significant primary figure errors which depend upon aperture and altitude. Special off-pointing or telescope capping would be required to keep the earth's image out of the field of view of the telescope for these cases of the earth occulting the target star or planet.

At 260-mile altitudes, the maximum earthshine is 481 watts/m^2 . Assuming a mirror reflectivity of 95 percent, the additional heat flux from earthshine incident on the mirror is, thus, 24 W/m^2 . This energy corresponds to a 75-watt input for a 2-meter aperture, which is enough to cause a temperature gradient in the mirror. The long thermal time constants expected in the primary mirror are not compatible with the 1-1/2 hour cycle between the maximum and minimum of earthshine. Thus, transient thermal measurements are required to determine how detrimental earthshine is to a large primary (3-meter) mirror figure at various prospective altitudes. If the earthshine creates significant mirror errors, this experiment will help define proper operating procedures for the MOAT telescope viewing stars near the earth disk. The experiment will provide useful data for large-aperture terrestrial and planetary reconnaissance systems.

Basic Experiment Hardware

This experiment requires the data outputs from the figure sensing equipment (Experiment No. 1), the temperature monitoring system (Experiment No. 12), and an earthshine measuring device located at the front of the telescope. A simple device would consist of a sensitive bolometer mounted at the focus of a small lens in a telescope tube, which is a scaled-down version of the main telescope tube. This allows the bolometer to receive radiation from the same field of view as the main telescope. The total radiation incident on the primary mirror from various sources can then be monitored and transmitted to the ground.

EXPERIMENT NO. 15 - SPACE AND RADIATION EFFECTS ON OPTICAL ELEMENTS

(This experiment should be carried out as a piggyback or a synchronous-orbit satellite.)

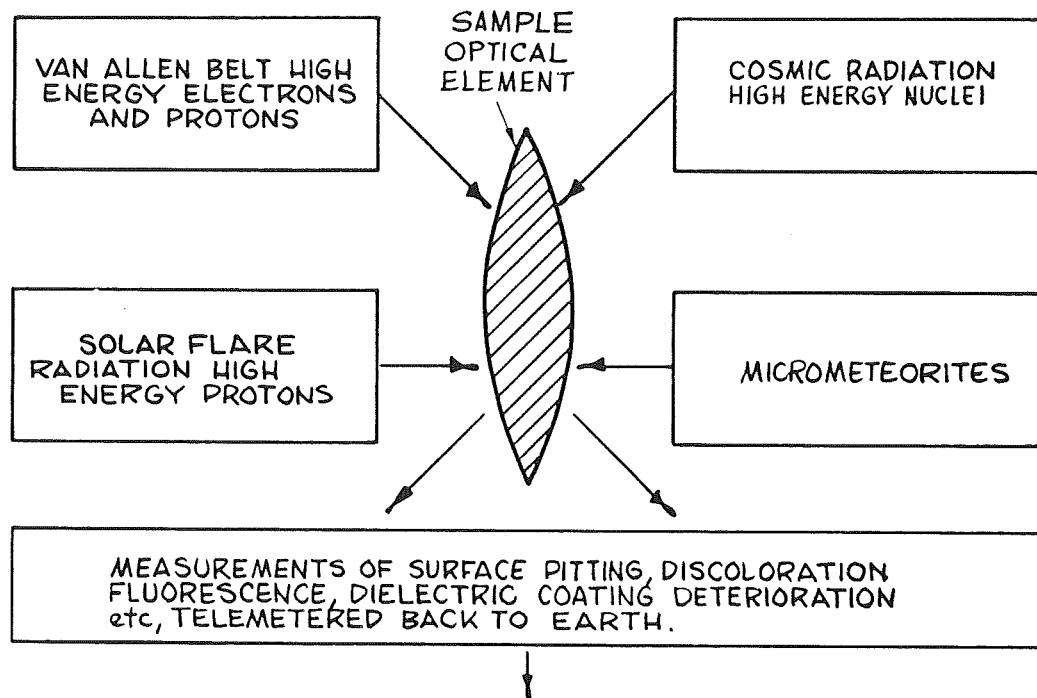


Figure 221. Space and Radiation Effects on Optical Elements

The design and instrumentation for optical systems operating in a space environment requires extensive data on the space environment and knowledge about the effects the environment has upon the performance of typical optical components, in order to assess the effect of the space environment upon system performance. Although a wealth of information has been obtained about the space environment in the past decade, and a clearer picture of the space environment is beginning to emerge, presently available knowledge and information are not sufficient for the purpose of space optics instrumentation.

The information available at this time about the effects of solar flares, for example, on the space environment in the outer layer of the magnetosphere is not sufficient and a space vehicle in synchronous orbit would pass through this region.

* Sampe, : The Effects of Space Environment on Materials. 11th National Symposium and Exhibit, April, 1967.

In the area of investigation on the material damage by the space environment, certain components of the space environment have been simulated in the laboratory and the material damage due to such components as micrometeoroids, high energy bombardment and γ - ray radiation has been investigated.* However, the total cumulative effects which the combination of these damaging components and many other components of the space environment have upon the materials are not possible to evaluate. Consequently, the fact that it is difficult if not impossible to simulate the space environment points to the need to perform the environmental testing of critical optical components in the space environment.

Environmental testing has already been performed on sample mirror surfaces and coatings in the ATS-C series.** The specialty flight experiment discussed here is to conduct space environment testing on transmitting materials and coatings which would be used for lenses, partially transmitting mirrors and filters in space instruments.

Although the experiment is primarily an environmental test, the parameters of the measurements in the experiment will have scientific interest as well.

Configuration of Experiment

The experimental package will be launched in synchronous orbit for the period of two years. During the flight, the experiment will measure the transmission of each sample material in the spectral range from 0.3μ to 1.3μ and the measurement will be scanned with a 1-percent spectral bandwidth. In addition, the experiment will measure the surface scatter of each material. After the flight is over, the package is returned to the earth and further measurements are to be performed on the ground. During the flight the experimental data are sent to the ground station through a communication channel as the experiments are performed on command from the ground station.

The significance of the spectrally scanned transmission measurement and the surface scatter measurement is that they are considered to be the most important parameters in optical system design. In addition, the transmission is affected chiefly by the penetrating radiations, and the surface scatter is affected chiefly by micrometeoroid bombardment and sputtering. The experimental results, therefore, contain additional information on the space environment and its fluctuation.

Description of Experiments

Twenty sample materials are placed in a circular tray as shown in Figures 222 and 223. During the experiment the tray rotates in steps, and at each step one sample enters into the transmission measurement housing and another

* Goetzel, Clans G.: Space Material Handbook. 2nd Edition, January, 1965.

** Emerling, R., Holloway, J.: Reflectometer Experiment. December 1967, Contract No. NAS 5-9669.

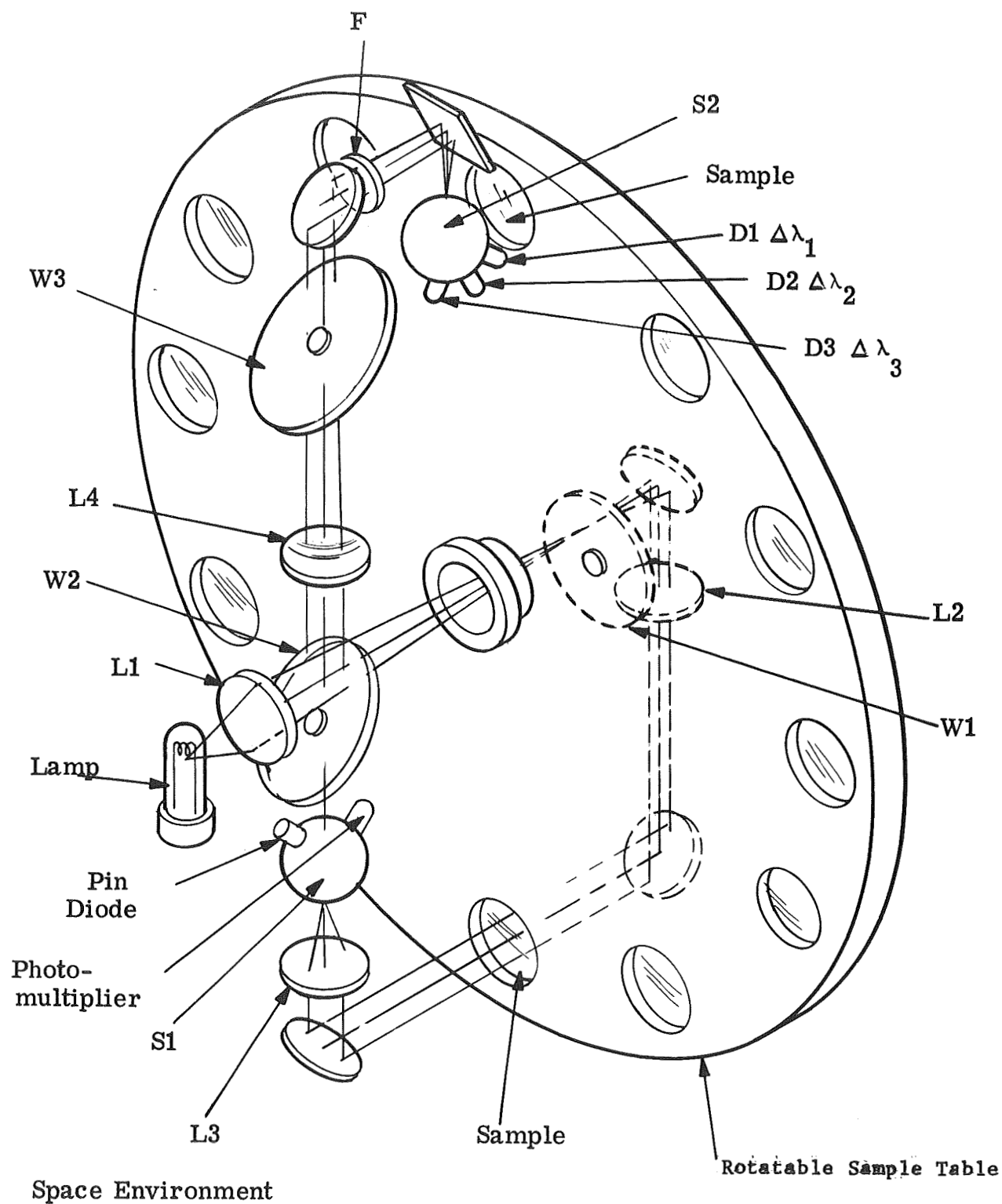
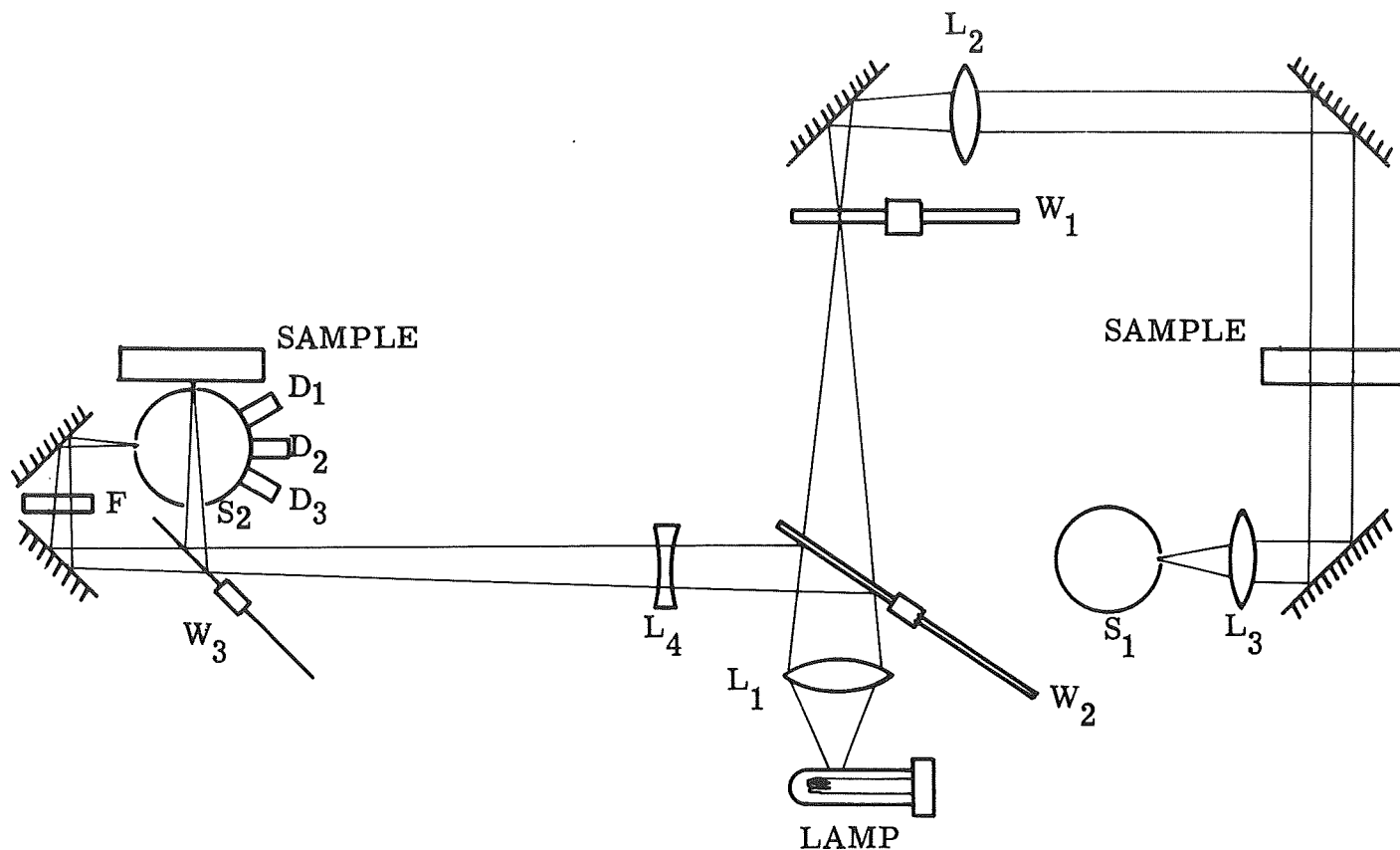


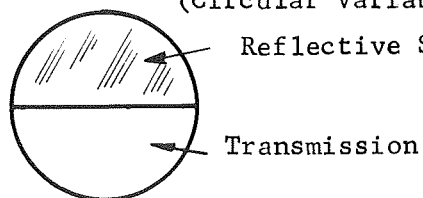
Figure 222 • Transmission and Scatter Measuring Instrument



1. Lamp: Tungsten 3000°K

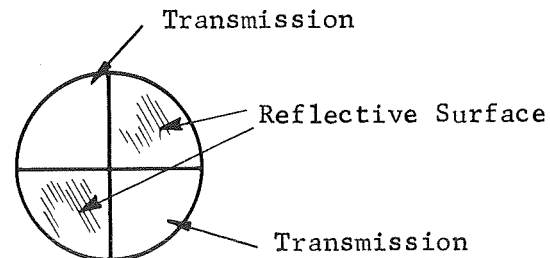
2. W_1 : Rotating Bandpass Filter
(Circular Variable Filter)

3. W_2 :



Motors Should be Synchronous Motors

4. W_3 :



5. F: Attenuator 70%

6. S_1, S_2 : Integrating Sphere

7. D_1, D_2, D_3 : Detectors

Figure 223 . Optical Schematic,
Scatter and Transmission
Experiment

sample into the scatterometer. The sample properties will be measured daily for the initial 30 day period of the mission and measurements made on a monthly basis thereafter for the period of the mission. One of the sample placement housing contains a beam stop and an opening so that in each cycle of rotation of the sample tray reference readings of 0-percent and 100-percent transmission for the case of transmission measurement and 0-percent scatter due to the opening for the case of the scatterometer are obtained for calibration purposes. The experiment package shown in Figure 222 is estimated to measure 20x20x20 inches and weighs 20 lb, approximately, and it is to be mounted on the side of the spacecraft such that all except two sample positions are exposed to the space environment. The beam from the tungsten lamp is chopped by the rotating mirror W₂ so that the beam is switched between the transmission measurement path and the scatterometer path.

Transmission Measurement

During the interval when the beam is directed to the transmission measurement path, the image of the tungsten lamp element is focused onto the rotating bandpass filter (circular variable filter) which has a 1-percent spectral bandwidth and whose bandpass wavelength varies from 0.3 μ to 1.2 μ as the filter rotates. The output of the filter is collimated and then transmitted through the sample and the transmitted beam is measured in the integrating sphere.

The intensity of the transmitted beam is measured by the detectors which include a photomultiplier for the 0.3 μ to 0.6 μ region and a pin diode for the 0.6 μ to 1.2 μ region in the integrating sphere, and its electrical output is sampled by the electronic system.

Scatterometer

During the interval when the beam from the tungsten lamp is directed to the scatterometer path, the beam is chopped by the rotating mirror W₃ so that the beam is switched alternatively to the sample surface through the integrating sphere and to the inner surface of the integrating sphere through a 70-percent attenuator. The set of detectors placed in the integrating sphere measures the scattered light from the surface of the sample at one instant and the 70 percent attenuated beam at the next instant. Comparing the two measurements the surface scatter is obtained. An additional reference of 0-percent surface scatter is available in each cycle of sample tray rotation from the open aperture which also serves as 100 percent transmission reference.

Test Samples

The final decision on the selection of samples is to be preceded by consultations with people who have direct interest in the experiment. The tentative and partial selection of test samples is listed in Table 31.

TABLE 31. TEST SAMPLES

[illegible]

* Information concerning the materials and multilayer coating design are not included since these are proprietary to Perkin-Elmer.

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EXPERIMENT NO. 16 - REMOTE MANUAL OPTICAL ALIGNMENT

(Deleted - Refer to OTES Interim Report* for additional details)

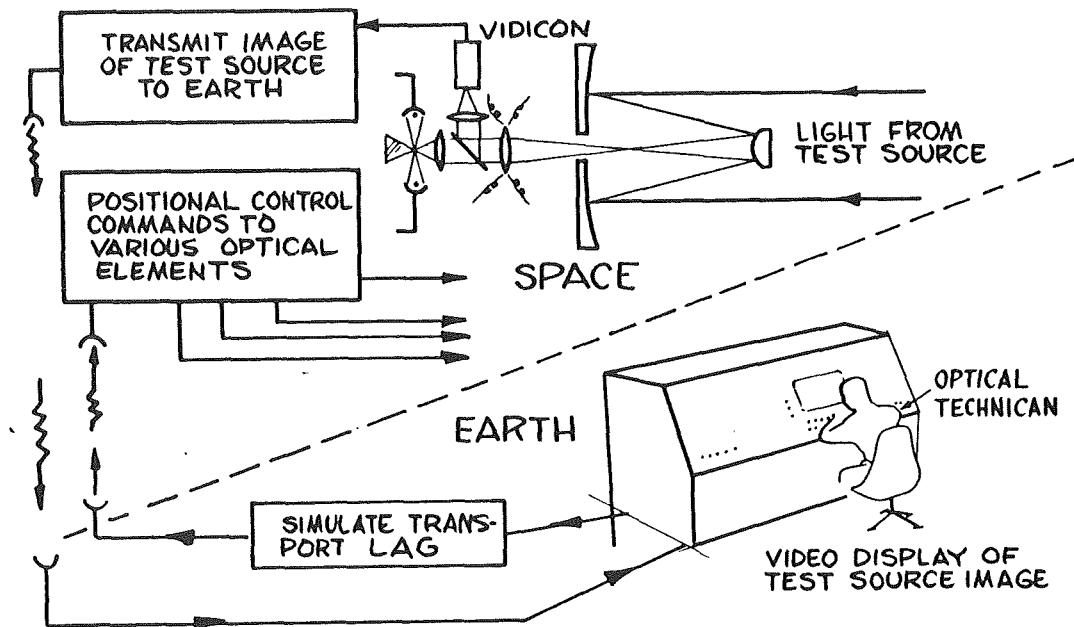


Figure 224. Remote Optical Alignment Schedule

Experiment Objective

The experiment considered the equipment requirements and the feasibility of realigning a large-aperture system in space. The performance of both the remote control system and the human operators was to be evaluated. Since the operator will need information about system performance which is essentially visual (image quality at various places in the optical path), the real-time television capability of the spacecraft was to be utilized. The communication transport lag of a deep-space vehicle can be simulated easily with ground equipment, and its effects on human performance could be evaluated.

The experiment is deleted at present from the group under consideration because it is essentially a demonstration in space of a laser communication application rather than a basic optical technology experiment.

*Perkin-Elmer Report 8319.

EXPERIMENT NO. 17 - FIGURING OPTICAL SURFACES IN SPACE BY ENERGETIC
IONIC BEAMS (Deleted - Refer to OTES Interim Report*
 for additional details)

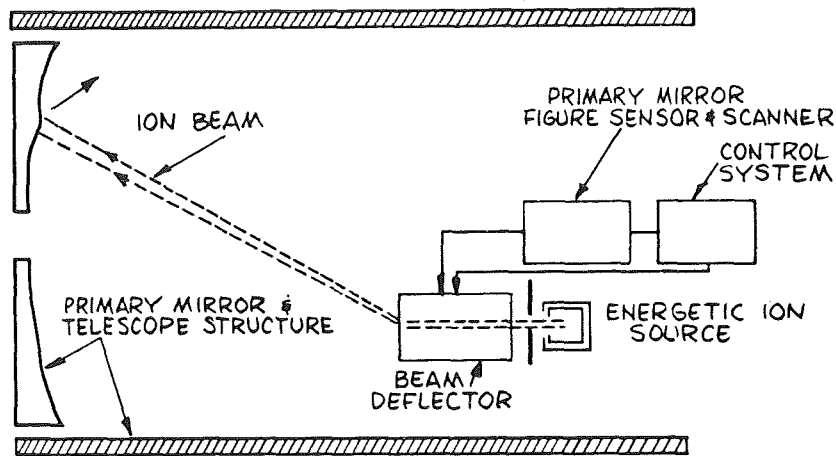


Figure 225. Ionic Polishing

Experiment Objective

The objective of the experiment is to test the ion polishing technique of mirrors while the primary mirror is in the zero "g" field in space.

The recent discovery** that a directed beam of heavy ions effects a uniform removal of a surface of fused silica upon which the beam impinged offers exciting possibilities for the LTEP. Ionic polishing can be used on board the spacecraft to make small corrections to the primary mirror figure while the mirror is in space. Electronic deflectors may be employed to direct the ion beam to the required zone on the mirror. An ionic polisher used in conjunction with the primary mirror figure sensor and scanner, operated in a servo mode, could be used to remove localized high areas in the primary mirror while the mirror is in the zero "g" field.

Errors in compensating for and simulating zero gravity, vacuum, and thermal conditions on Earth will create deformations in the mirror surface when the telescope is placed in space operation. The ionic polishing process would allow figuring of the mirror to $\lambda/50$ accuracy after the LTEP is placed into synchronous orbit. This new and novel technique has the potential of revolutionizing the methods for final figuring of diffraction-limited optical primaries.

The experiment is deleted at present from further consideration (Phase B) of the LTEP Program due to its very immature state. While this technique might well be used in the future, it is too early to include in the present LTEP Program.

* Perkin-Elmer Report 8319.

** Meinel, A.B.; Bashkin, S.; and Loomis, D.A.: Controlled Figuring of Optical Surfaces by Energetic Ion Beams. J. Appl. Opt., vol. 4, no. 12, Dec. 1965, p. 1674.

EXPERIMENT NO. 18 - OCCULTING DISK EXPERIMENT

(Deleted - Refer to OTES Interim Report* for additional details)

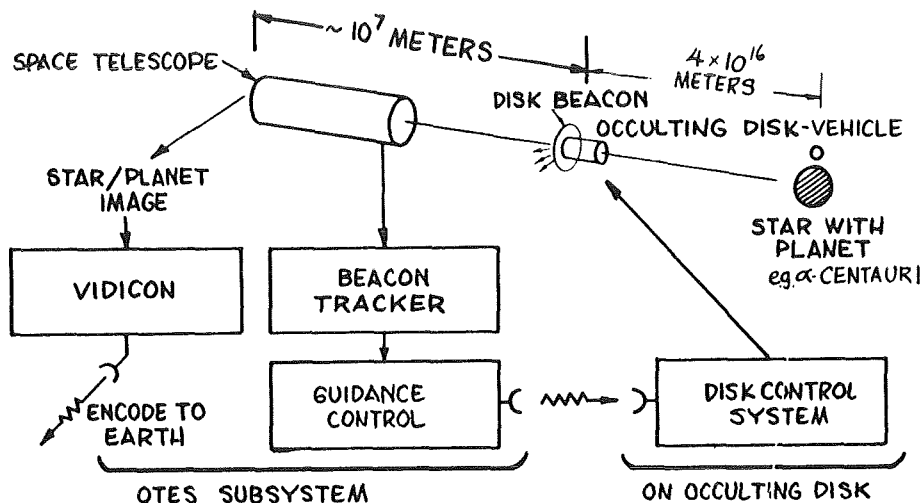


Figure 226. Occulting Disk Scheme

Experiment Objective

The discovery of planetary systems around nearby stars having a physical relationship similar to the relationship between our earth and our sun is an exciting realization for optical astronomy. If the target star's planetary system resembles the solar system, then the target star is 25 magnitudes brighter than its brightest nearby planets. A spaceborne, three-meter telescope can have a diffraction-limited resolution in the visible spectrum of 0.06 arc-second. This angular resolution is probably inadequate to see major planets in the reflected light from their parent star. Even with an order of magnitude increase in diameter of the space telescope, the scattered light from the parent star is still bright enough to obscure a nearby planet.

There is a possible technique** that would ameliorate the scattered light problem at the price of extremely precise guidance. This method involves the use of a large opaque occulting disk launched far in front of the space telescope to reduce the direct light from the star. During the observation of the star, the space telescope would track a beacon on the occulting disk. Controlled and very precise remote positioning of an occulting disk in a closed loop mode is required to locate the disk precisely in the line of sight to the star. The magnitude of relative motion between the occulting disk and the telescope line of sight determines the observation time. This observation would have to be repeated some time later in order to distinguish planets, by means of their orbital motion around the parent stars, from faint stars in the background. Apodization of the occulting disk will increase the contrast between the scattered light of the star and planet.

This experiment is too ambitious for the LTEP Program since it requires an additional space vehicle acting as the occulting disk with very precise positioning of the occulting vehicle required.

* Perkin-Elmer Report 8319.

** Spitzer, L., Jr.: Beginnings and Future of Space Astronomy. American Scientist. Vol. 50, no. 3, Sept. 1962, p. 473.

EXPERIMENT NO. 19 - PROPAGATION OF DIFFRACTION-LIMITED LASER BEAMS
(Deleted from 2-Meter LTP Astronomy Telescope)

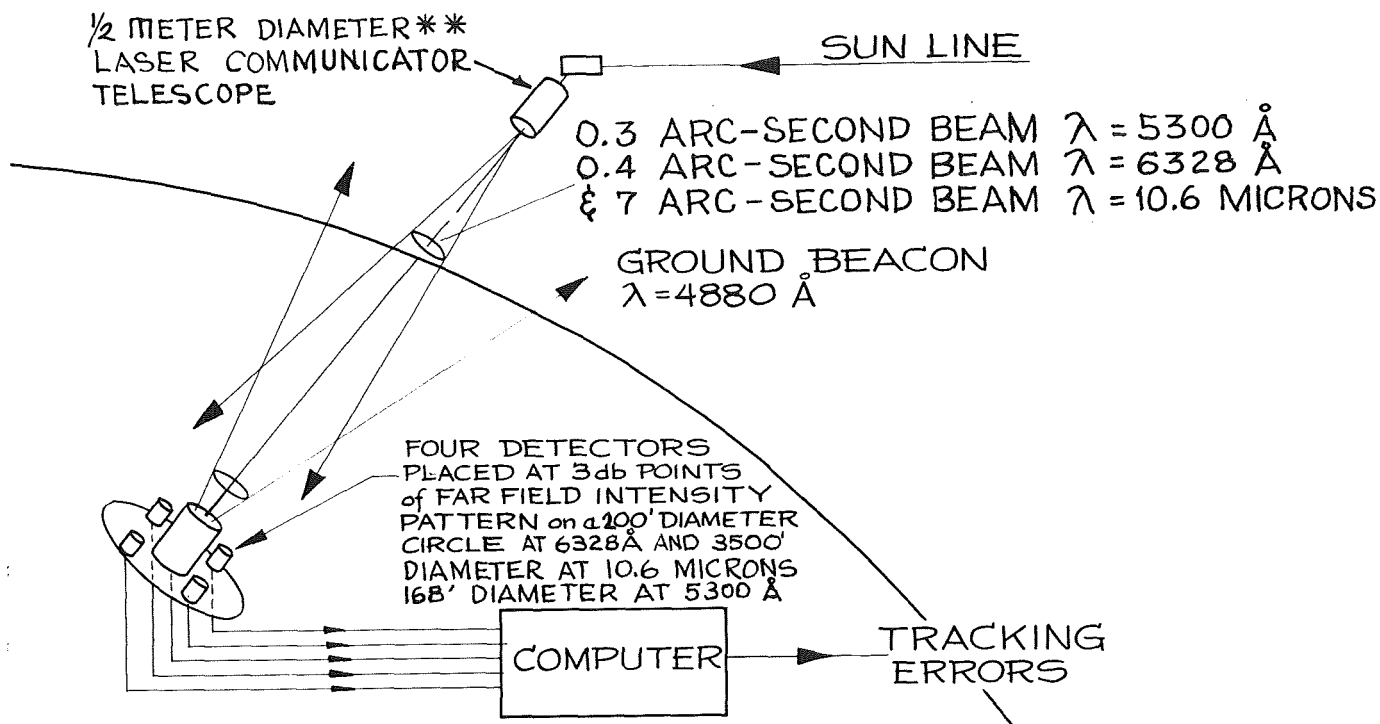


Figure 227. Propagation of Diffraction-Limited Laser Beams

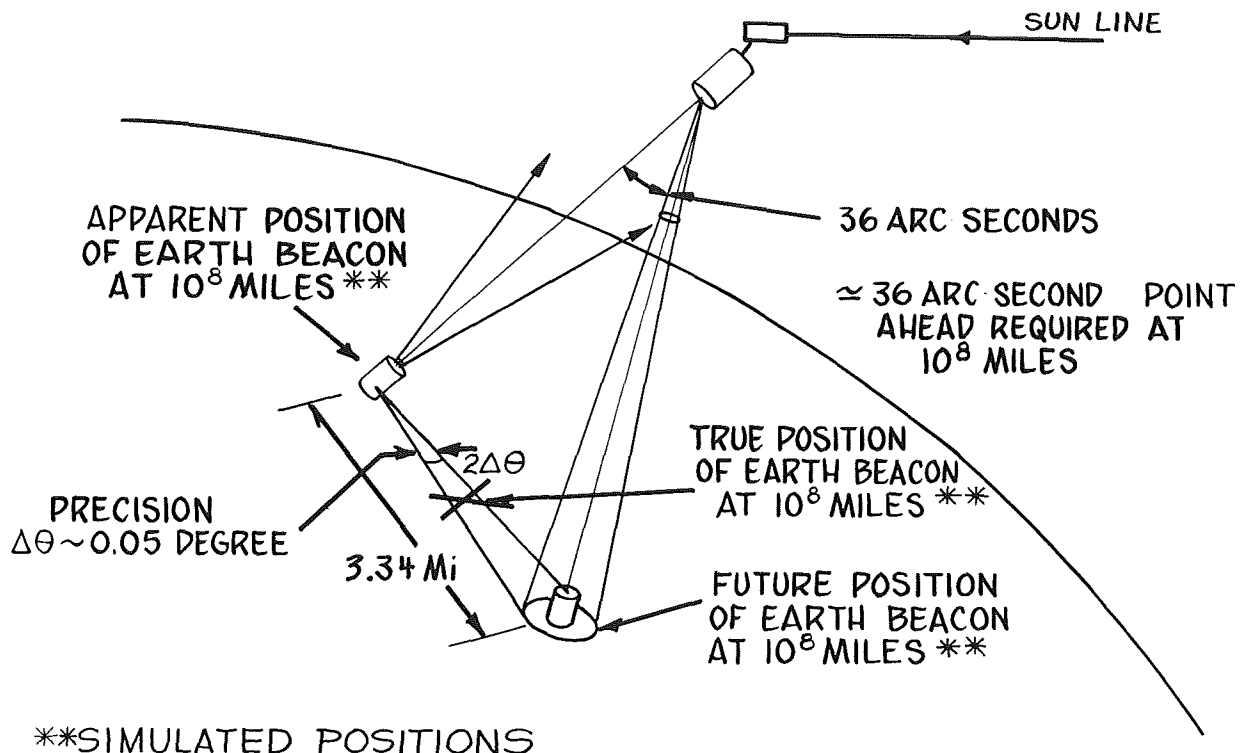
Experiment Objectives

Earth reception of a narrow laser beam transmitted from a deep-space communication telescope requires that the beam be very precisely directed. The space measurements of fractional arc-second laser beam tracking capability is the purpose of this experiment.

Since the technology objective is related to optical communication, this experiment is deleted from the LTP.

EXPERIMENT NO. 20 - POINT AHEAD

(Deleted From the 2-Meter LTP Astronomy Telescope)



**SIMULATED POSITIONS

Figure 228. Point Ahead Geometry

Experiment Objectives

To demonstrate that it is feasible to point a space telescope's transmitting LOS ahead of its receiving LOS, by up to approximately 36 arc-seconds. The LOS to be accurate enough to establish an optical communication link back to the earth receiving station whose transmit laser beacon is radiating.

Experiment Justification

Successful execution of this satellite experiment on OTES at 20,000 miles, under conditions which are closely equivalent to those anticipated at 10^8 mile ranges, will establish the feasibility of deep-space-to-earth laser communication systems which utilize narrow beams.

EXPERIMENT NO. 21 - LASER BEACON ACQUISITION IN SPACE

(This experiment is to obtain laser communication technology data. Since communication technology is no longer an objective of LTEP, this experiment is deleted.)

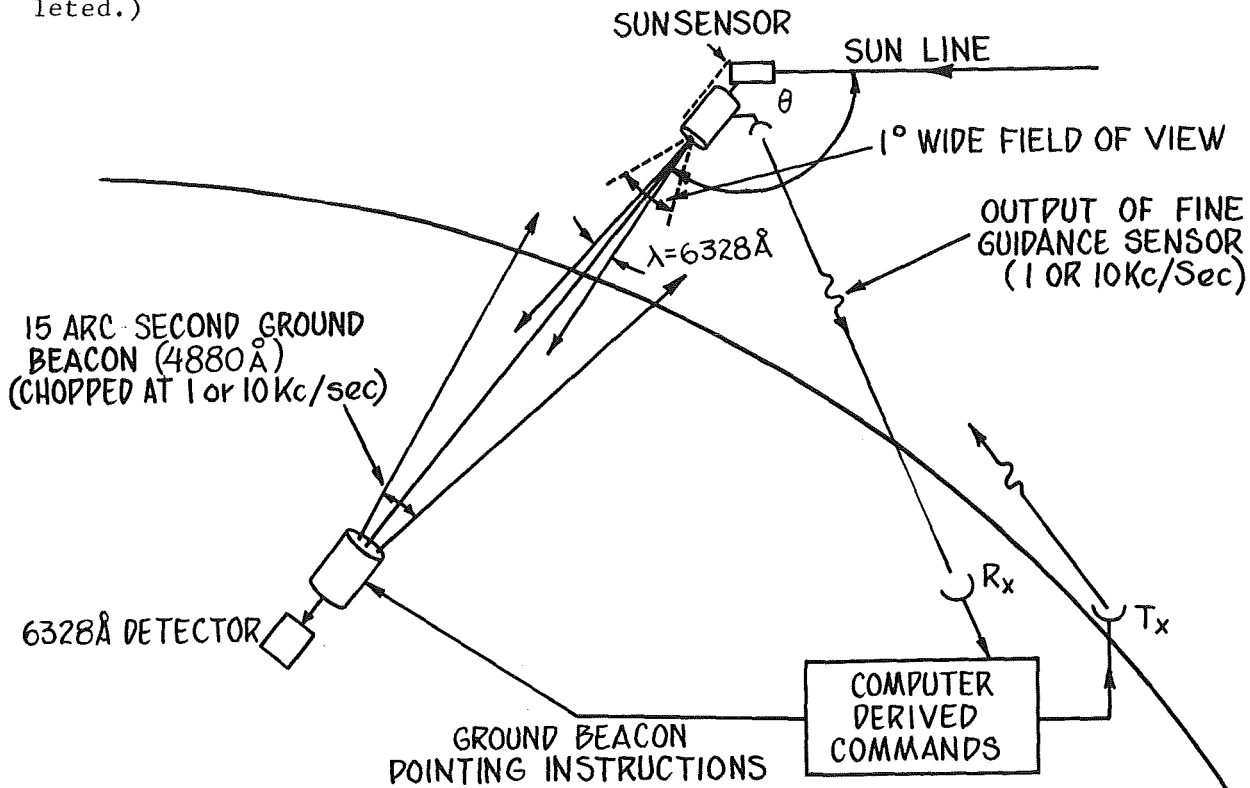


Figure 229. Laser Beacon Acquisition

Experiment Objective

Assess the merits of one or more acquisition methods for finding (or acquiring) a laser ground beacon. The reliability, simplicity, and acquisition speed of each approach should be determined. Many acquisition schemes are possible. A deep-space vehicle, for example, could search with a relatively wide FOV ($\sim 1^\circ$) at a given angle from the vehicle-sun reference line until beacon power is received. This approach might benefit by incorporation of auxiliary earthshine detectors to enhance the detection ability under high albedo and earth illumination conditions. Another approach might be to utilize vehicle star tracking equipment, if present, to establish perhaps a two-axis reference with stored angular data to locate earth and the beacon without a search mode. A third approach might conceivably involve both microwave and laser ground beacons to provide both coarse and fine position error detection capability during acquisition. Since eleven synchronous satellite experiments (viz. 5, 20, 23, 24, 26, 27, 28, 29, 30, 31, 32) are dependent upon the acquisition of a ground laser beacon, the 2-meter OTES at the 260 mile orbit should execute as much of Experiment 21, as originally described in Report 8500, as practical from the low orbit. (See Figure 229.)

Experiment Justification

Acquisition capability must be provided if the eleven synchronous orbit experiments are to be performed. An engineering experiment, devoted to performance evaluation and optimization procedures, is consistent with the importance of developing a truly spaceworthy laser beacon acquisition system. Further, the provision of two completely redundant acquisition subsystems aboard a technology satellite such as an OTES stems from the need for a high confidence factor subsystem. The provision of a last-ditch backup acquisition system is justified for the low orbit flight of the 2-meter OTES.

The attached parametric family of curves in Figure 230 considers the capability of the synchronous spacecraft to rotate rapidly or slowly about the sun line and the effect of this capability upon the time during which the earth beacon image will travel through a 1° field of view. Since the sun offset angle can vary from small angles (but no less than a few degrees or the direct rays of the sun would enter the telescope) to large angles approaching 90° , the sun offset angle is plotted as the abscissa or independent variable of the parametric curves. The synchronous spacecraft rotational rate capability about an arbitrary axis is considered over the range from 1 complete revolution in 1/2 minute to 1 complete revolution in 1/2 hour. The faster the spacecraft rate, the shorter the period of time that the Earth beacon image will be inside the 1° field of view.

Note that the rates are measured by values in the range of revolutions per minute for this synchronous orbit simulation of deep space. For the 2-meter OTES, the angular rates are in the vicinity of several degrees per second, and the corresponding time that a laser beacon image would traverse a 1° field of view is now a matter of a fraction of a second. The acquisition problem at low orbital altitudes is considerably more difficult and the likelihood of success is correspondingly lower. However, the number of opportunities to conduct acquisition experiments will be high due to the long life of the orbiting vehicle.

Basic Experiment Hardware

The basic method for acquiring an earth laser beacon utilizes a search about the vehicle-sun reference line (as described above); the basic hardware involved includes the following:

<u>On the Ground</u>	<u>In Space</u>	<u>Other</u>
• Computer	• Optical telescope with wide detection FOV	• Command and telemetry subsystems
• Laser	• Optical pointing error sensor (viz. 4-quad detector, PMT, etc.)	
• Laser modulator	• Predetection filtering	
• Optical telescope	• Field stop (automatic or remotely controlled)	
• Telescope gimballed mount	• Acquisition subsystem a) sun sensor, coarse b) sun sensor, fine and gimballed Attitude controllable vehicle Suspension subsystem	

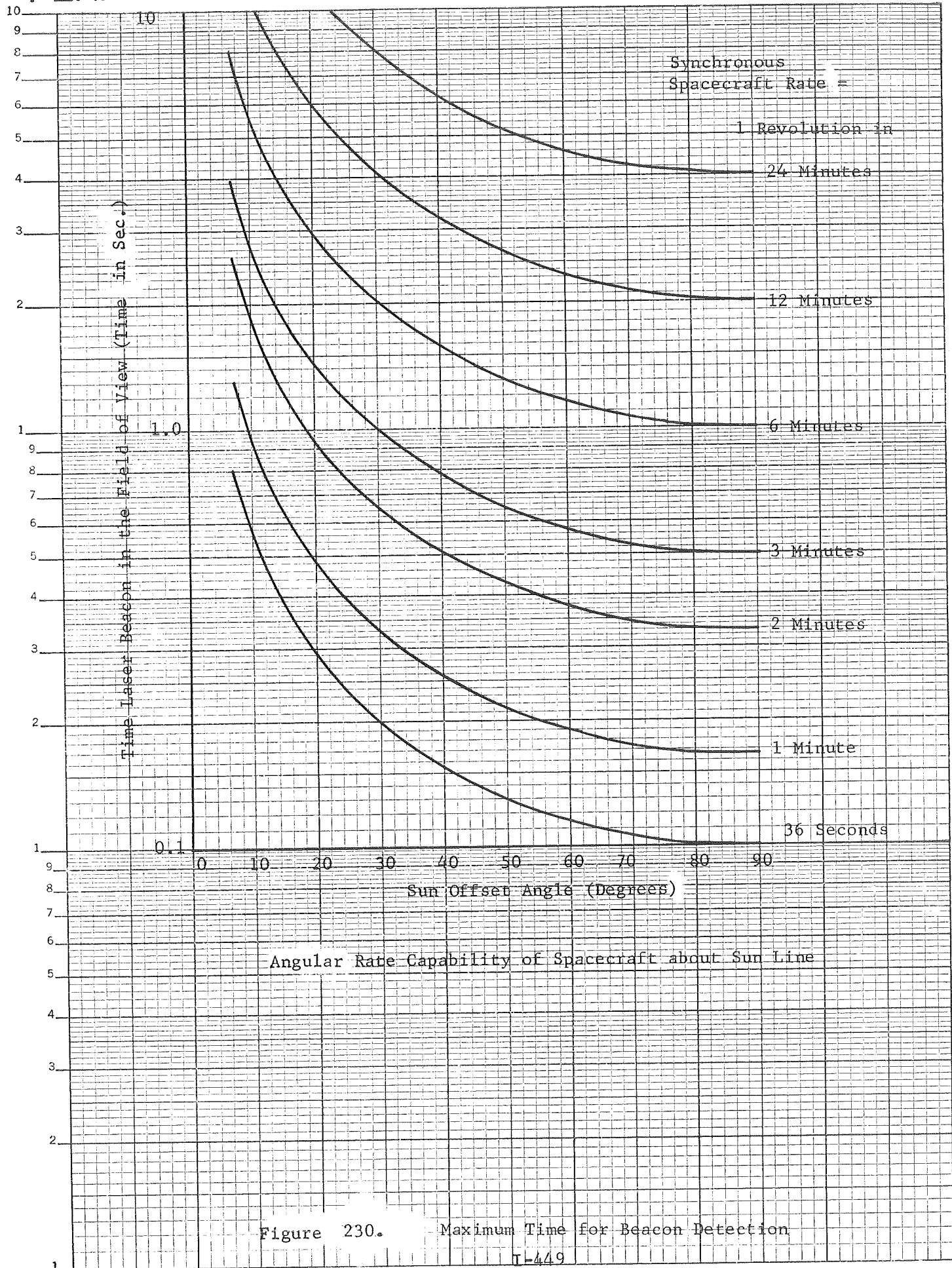


Figure 230. Maximum Time for Beacon Detection

The reflecting field stop divides the coarse (reflected) field and the fine (transmitted) field. As the image of the ground beacon enters the coarse field of view, the beacon energy is focused by the field lens on one or more of the quadrants of the 4-quadrant photosensor. This signal, when fed to the pointing control selector (Figure 231) causes the vehicle to rotate and consequently forces the beacon image toward the central hole of the reflecting field stop.

The first operation during acquisition is to determine the approximate position of the sun by using the coarse sun sensor. The output signal of this device is used to roll the telescope to a position where the sun is in the FOV of the fine sun sensor. Once the fine sun sensor has acquired the sun, the telescope rolls about the sun LOS. The rotation rate is controlled by the difference amplifier with the rate gyro and rate reference inputs.

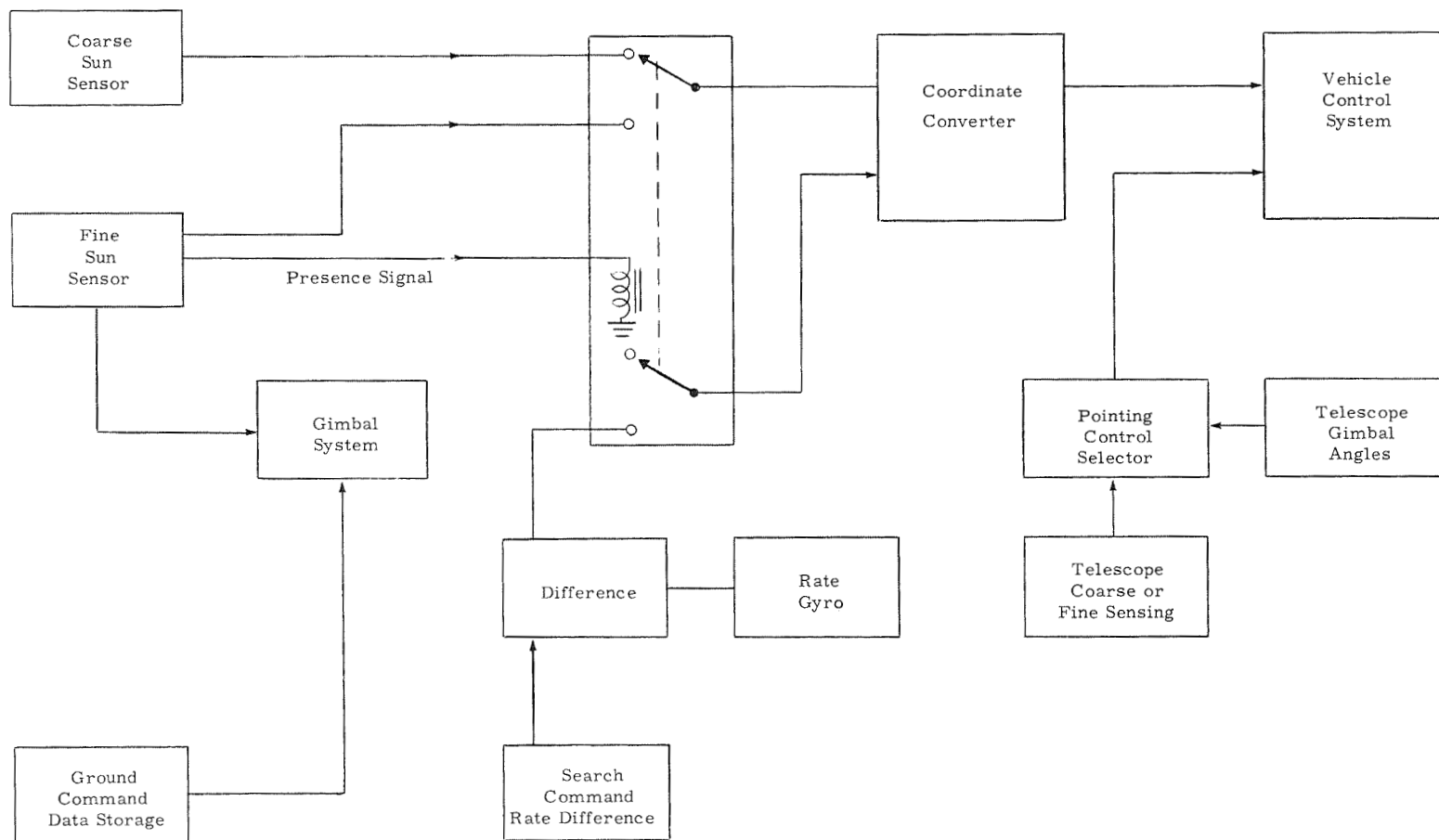


Figure 231. Acquisition

I-451/452

EXPERIMENT NO. 22 - EARTHSHINE AND CLOUD COVER EFFECTS ON ACQUISITION AND
TRACKING (Deleted from the 2-Meter LTEP Astronomy Telescope)

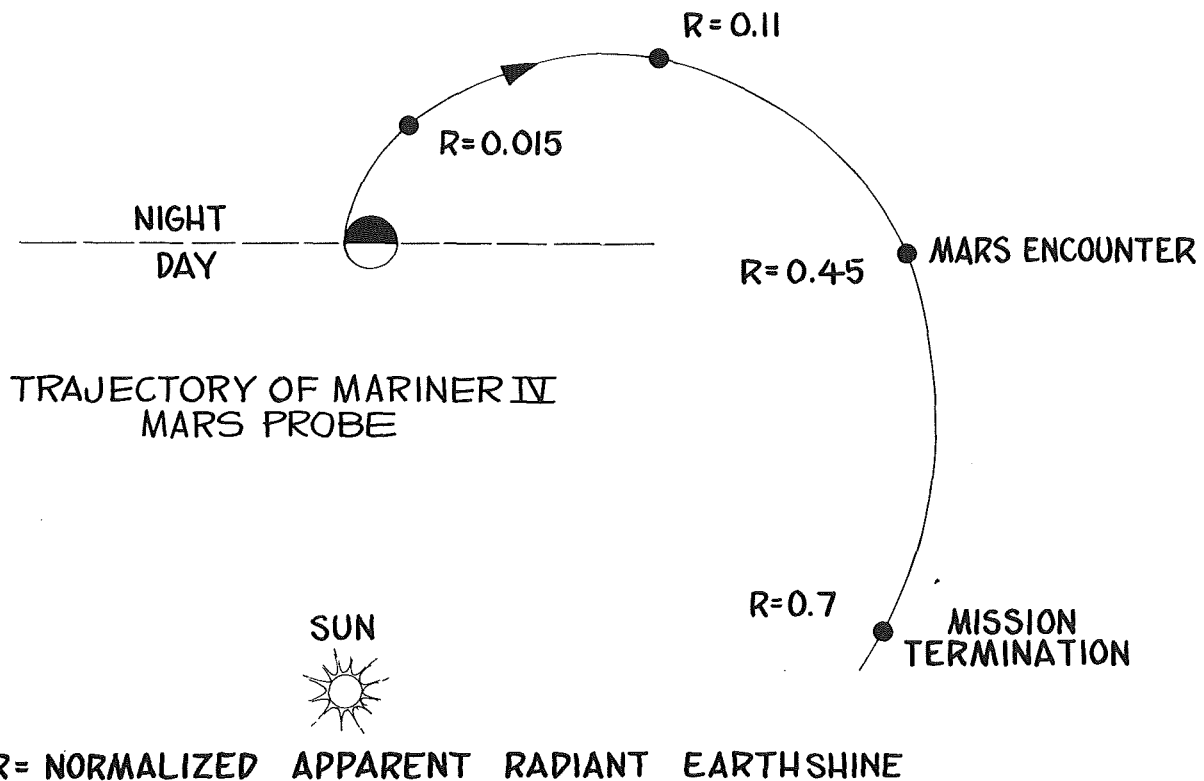


Figure 232. Earthshine Variation

Experiment Objective

Figure 232 above illustrates the variation in the earthshine as seen by a Martian space vehicle looking back at earth. The figure shows the trajectory of Mariner IV in a coordinate system fixed with respect to the earth and sun. Note that during the early part of the mission the probe is viewing the "night" earth. After encounter, the probe is assumed to be in orbit around Mars.

The experiment objective is to assess the ability of a space optical (acquisition and tracking) system to acquire and track a laser ground beacon in the presence of degradations induced by naturally occurring earthshine. The range of conditions should include those occurring on moonless nights through those occurring with high-noon sun with effects of various albedo conditions (water glint, snow fields, clouds, etc.). Tracking measurements are also needed for various cloud cover conditions. Experiment data is needed to support the selection of future lasers based on their ability to penetrate cloud layers.

EXPERIMENT NO. 23 - ONE-MEGABIT LASER COMMUNICATIONS EXPERIMENT
(Deleted from the 2-Meter LTEP Astronomy Telescope)

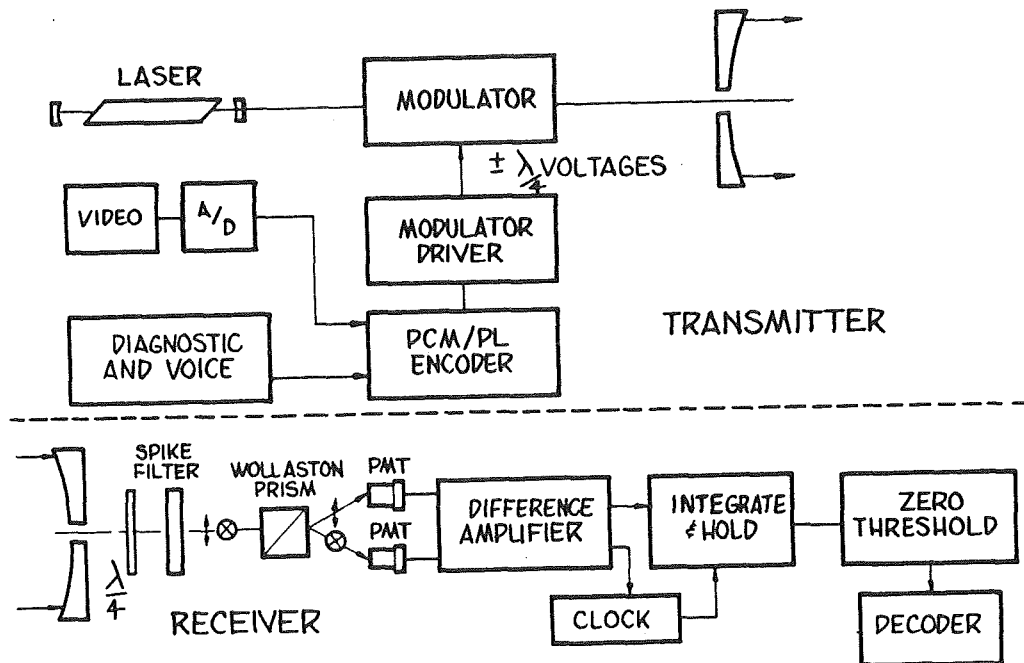


Figure 233. Laser Communication Block Diagram

Experiment Objective

This experiment proposes to gather the experimental data necessary to establish the feasibility of interplanetary megabit optical communications. Since the free space loss of a Mars-to-earth communication link is 74 dB greater than the 22000-mile path, the power requirements of the experiment system can be significantly smaller than the requirements for future interplanetary links. However, all the requirements for acquisition, tracking, encoding and decoding, operation of laser and modulator, and reliability can be adequately simulated and tested. The data to be transmitted would consist of digitized TV and telemetry from the spacecraft diagnostics equipment as shown in Figure 233.

This experiment, which requires a synchronous orbit vehicle, will develop the engineering and scientific data needed to design future deep-space optical communication systems that can operate with a channel capacity in the vicinity of one megabit/second. Since the 2-meter OTES flight is based on a 260 n.m. orbit, the experiment must be accomplished on the 1/2 Meter Laser Communications Satellite configuration or equivalent. Refer to Report 8500 page I-145 for additional details.

EXPERIMENT NO. 24 - ROTATION ABOUT THE LINE OF SIGHT (RLOS)
(Deleted from the 2-Meter LTP)

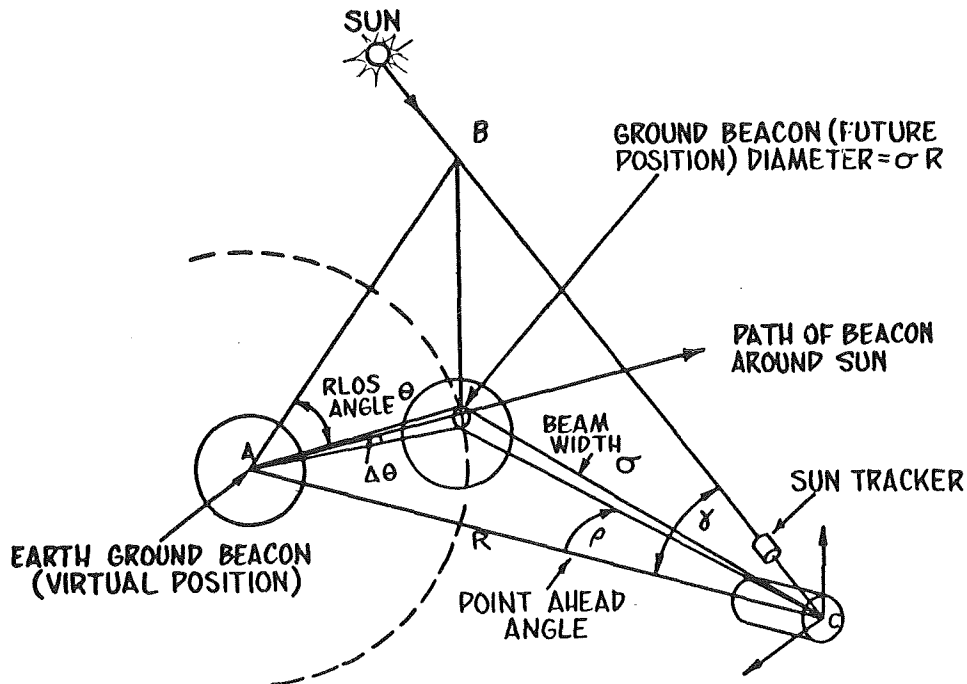


Figure 234. RLOS Geometry

Experiment Objective

There is a requirement to provide a rotational reference about the line of sight (RLOS) for the transmitting optics of an optical communications system on a spacecraft located in space. One experiment objective is to evaluate on the OTES an optimal RLOS technique for a megabit optical communication system operating at 10^8 miles. The RLOS technique assumed for deep-space employs an auxiliary onboard sun tracker working in conjunction with the vehicle telescope viewing the earth laser beacon.

A second experiment objective is to investigate RLOS techniques which are feasible for earth satellite applications. For example, the intersatellite communication link will require an RLOS technique.

The RLOS experiment is required for any space optical communication link which has the point-ahead capability. It is recommended for the 1/2-Meter Laser Communication Satellite Experiments in a space flight operating at a 24-hour orbit. See Report 8500, p. I-155 for additional details.

EXPERIMENT NO. 25 - TRACKING TRANSFER EXPERIMENTS
(Deleted from the 2-Meter LTP)

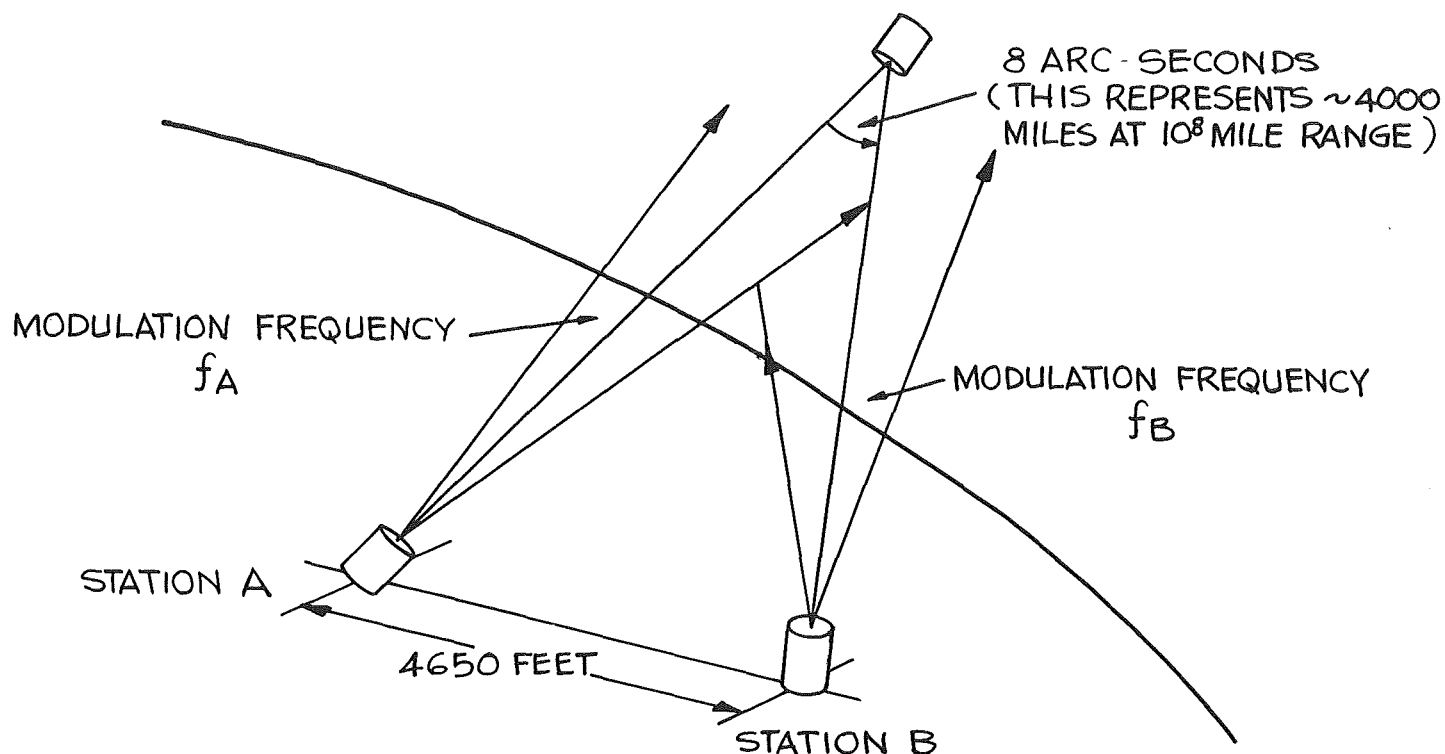


Figure 235. Tracking Transfer Geometry

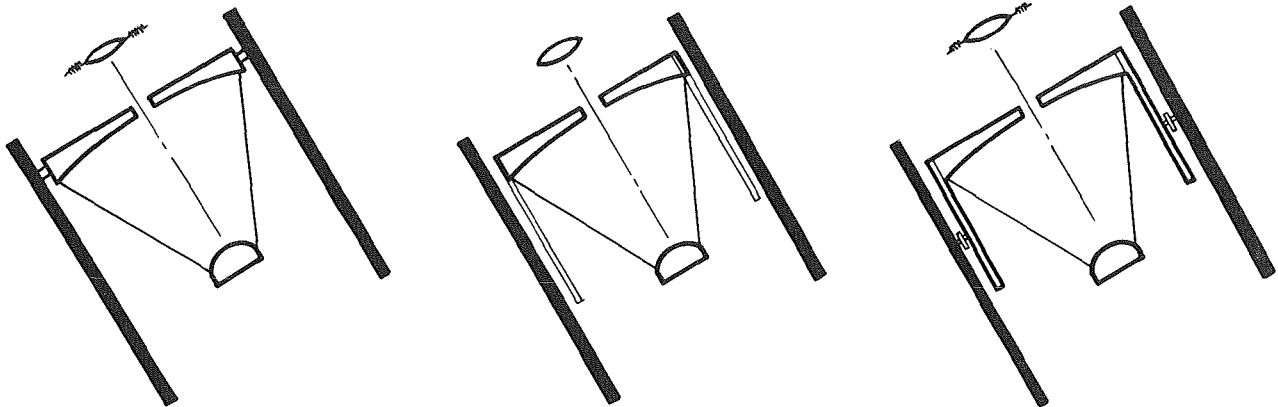
Experiment Objective

Following the period of time during which the satellite has been tracking Station A, the optical communication link must be shifted to Station B (an angular displacement of some known number of arc-seconds). This procedure will simulate the condition in deep space when the vehicle has locked its receive optical system onto the apparent position of the ground station and is now faced with the problem of transferring communication to a different ground station. It is important that the deep-space optical communication system have the capability of transferring its beam from one station to the next without going through an acquisition operation between each station transfer.

This experiment requires a 24-hour orbit rather than the 260 n.m. orbit of the 2-meter OTES. It is recommended for the Laser Communication Satellite Experiments. For additional details, refer to Report 8500, p. I-160.

EXPERIMENT NO. 26 - POINTING SYSTEM TECHNIQUES

(See Chapter 7 on Telescope Pointing for additional development of this experiment.)



1. BODY POINT & TRANSFER LENS

2. FREE FLOAT SYSTEM
(MAGNETIC TORQUING)

3. GIMBAL SUSPENSION
& TRANSFER LENS

GATHER TRACKING DATA WITH AND WITHOUT
KNOWN FORCING DISTURBANCES

Figure 236. Pointing Stabilization Techniques

Experiment Objective

Fractional arc-second tracking errors due to natural disturbances can be reduced by providing highest isolation against external disturbances, thereby allowing bandwidth minimization. A space telescope for communications or astronomical use will have its line of sight disturbed by extraneous torque inputs. While resorting to higher pointing servo bandwidths can reduce the torque-induced pointing errors, such increase is undesirable in that it enhances the magnitude of pointing errors due to noise, both in signal plus background light, and noise that originates in the control electronics. Since reduction of torque disturbances will result in increased pointing precision, telescope suspension systems which provide greatest isolation from external torques are desirable. The objective of this experiment would be to assess the isolation capabilities of various promising suspension systems by measuring telescope pointing errors when other conditions are maintained essentially constant.

Experiment Justification

The most promising suspension system for the large telescopes such as the 3-meter LTEP or the MOAT is the body point plus all reflective transfer lens technique. This takes advantage of the zero "g" space environment by having the telescope floating free of the cluster during tracking operations. Such an arrangement provides complete isolation from manned vehicle originating torques.

This experiment is an engineering evaluation, in space, of basically different pointing techniques for space telescopes. It also provides experimental evidence on the level of spacecraft disturbing torques which can be tolerated by each of the space telescope pointing techniques. As considered for the OTES, the experimental approach will include the gathering of tracking data from Experiment 9. The performance of each pointing system can be evaluated and the results incorporated in future manned observatory design. There is equipment (such as control moment gyros) aboard the LEM Rack that can introduce motion disturbances in each of the three rotational degrees of freedom.

Figure 237. below shows some calculated disturbances introduced aboard a 100,000 slug-ft² telescope by normal motions of a 200-pound crewman. Figure 237 illustrates that torso or body motions would cause disturbances greater than the large telescope could tolerate. That is, the basic tolerance level is about 0.01 arc-seconds while the torso motions and body motions introduce disturbances in the range of 3.6 to 36 arc-seconds.

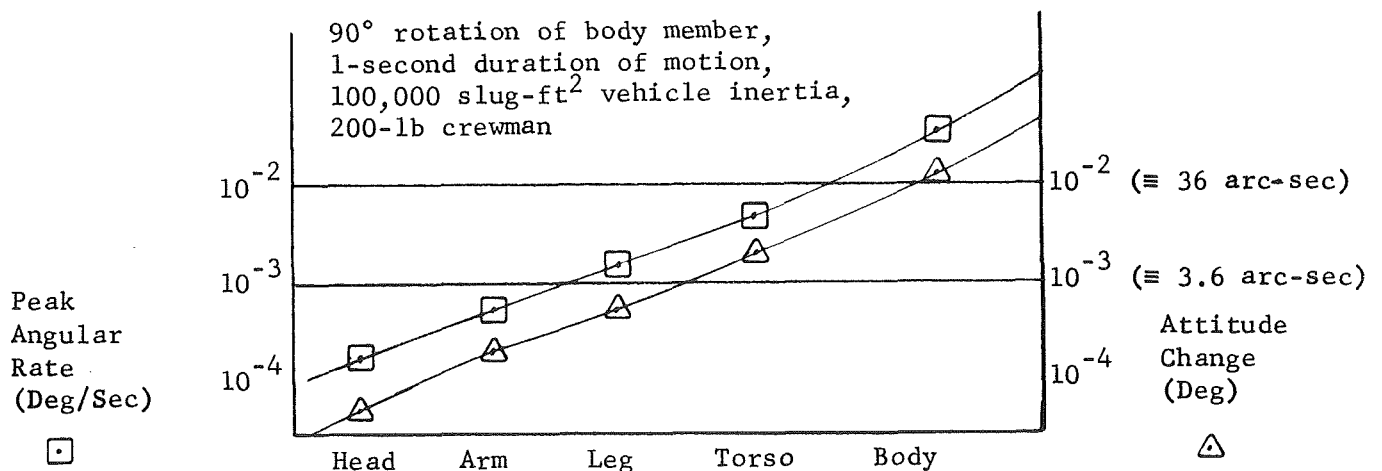


Figure 237. . Vehicle Motion Due to Crew

Thus, it can be seen that some form of fine pointing system control (beyond that provided by the spacecraft attitude control) is required for a large space telescope.

The three pointing techniques in this flight experiment to be evaluated against each other are:

- The body point technique with a transfer lens
- The free float technique using magnetic torquing
- The gimbal suspension with a transfer lens.

The body point technique with a transfer lens is the appropriate approach for the large orbiting telescopes such as the 3-meter OTES, the Manned Orbiting Astronomical Telescope (MOAT), and the Large Orbital Telescope (LOT). The free float technique is considered very appropriate for smaller orbital telescopes such as the 2-meter OTES and, the Advanced Princeton Satellite (1-meter, diffraction-limited aperture). The gimbal suspension with a transfer lens is appropriate for ATM and smaller telescopes. The latter technique is the system used on Stratoscope II, and is the technique which Perkin-Elmer is developing in the VCPS (Vernier Control and Pointing System) for the ATM for MSFC.

The experience Perkin-Elmer has had with the transfer lens in the Stratoscope II and the Laser/Optics Techniques work in NAS 8-20115 is adequate to draw on during the feasibility study work of the current phase of the LTP. However, with respect to the free float technique, the Advanced Princeton Satellite (APS) work is the project that is most advanced.* This space telescope project has been in study, design and breadboard since 1966. The project has an inertia model of the telescope supported by unique air bearing for testing of the free float techniques. This equipment was used by the OTES Project Team at Perkin-Elmer to conduct a laboratory experiment of special interest to the OTES Experiment No. 26.

Laboratory Free Float Pointing System Experiment for OTES

The experiment that was conducted with the APS magnetic suspension pointing system breadboard in support of OTES Experiment No. 26, was the measurement of the magnitude of isolation achievable by a free float telescope in the presence of simulated spacecraft motions. The laboratory layout is shown in Figure 238. The experimental procedure was to physically displace the magnetic pusher over large distances (equivalent to spacecraft motion) and measure the resulting displacement of the breadboard axis. The magnetic pusher magnet was moved over distances which were variable to a maximum of 1.0 inches and at a rate of 0.38 inches/second. The results were measured with an autocollimator and the disturbances resulting from these tests were noted to be less than the resolution limit of the autocollimator (this is about 0.2 to 0.5 arc-seconds). While the tests are really incomplete without going down to measurements in the vicinity of 0.01 arc seconds or less, the magnitude and rate of the disturbances induced into the equipment are unusually high.

* Perkin-Elmer Report 8688, Section 5 contains a full technical report on the Free Float system, its design, construction and test results.

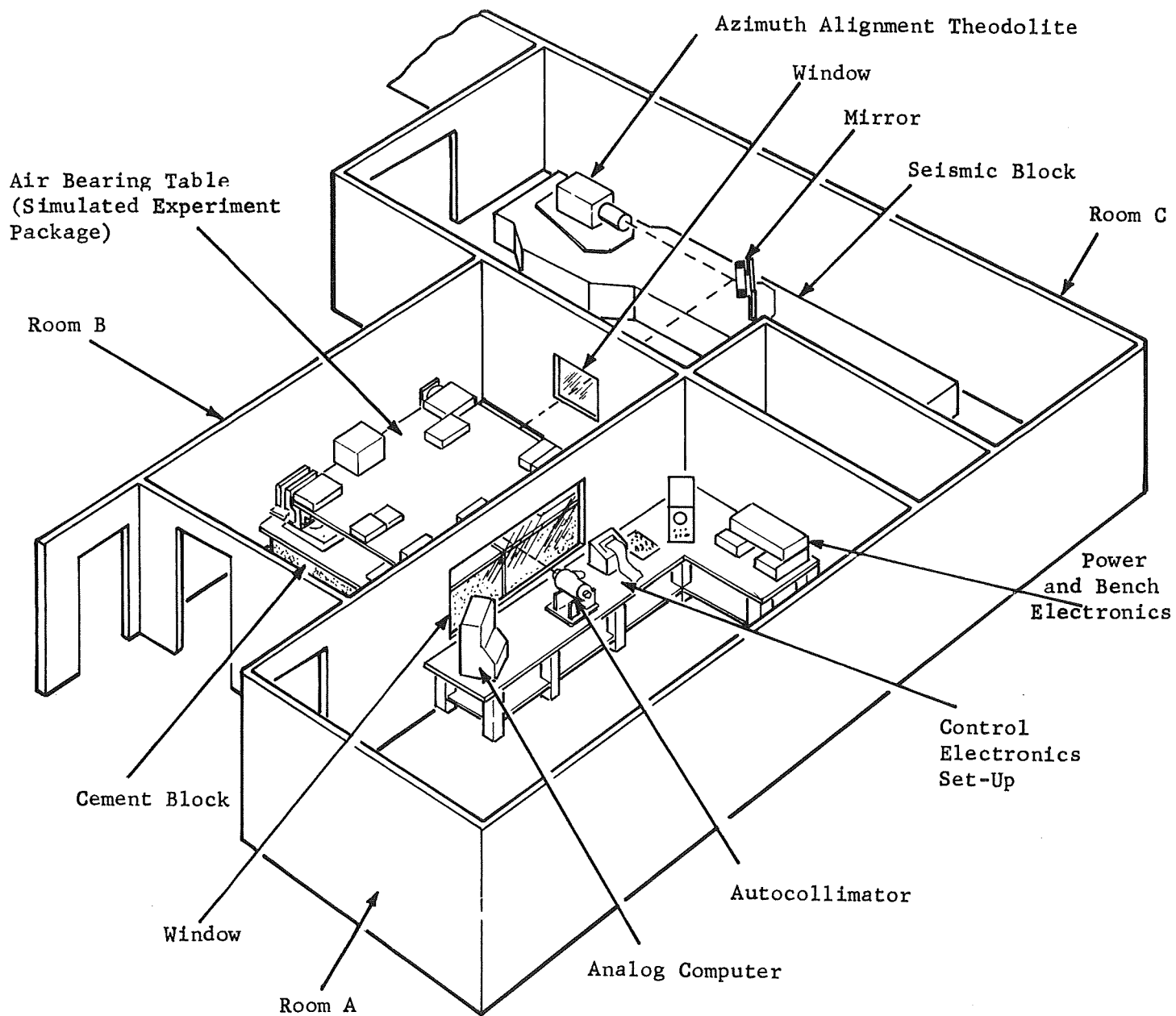


Figure 238. Laboratory Layout

One of the main features of the APS system is that its experiment package is physically disconnected during operation from the outer spacecraft which in this case is the OAO spacecraft. Although the experiment package pointing control system uses the OAO spacecraft as an inertial torquing reference to achieve its pointing control, it is not in turn subjected to the disturbances created by the motions of the spacecraft.

One of the special components used in the APS system to achieve control is a magnetic "pusher" which is similar in principle to solenoids on loud speaker coils. This unit consists of a permanent magnet with an "air gap" that contains a coil where the permanent magnet is located on the outer spacecraft and the coil is attached to the experiment package. In operation, an electrical signal (current) is derived from the experiment package angular displacement sensors and fed to the coils which in turn set up magnetic repulsion or reaction forces in the pusher which effectively "push" against the spacecraft to control fine pointing.

In this OTES laboratory experiment, one of the permanent magnets (See Figures 239 and 240) of the APS breadboard was mechanized to allow it to travel "in and out" with respect to its coil mounted on the simulated experiment package. The pointing system was activated, placed into its final operating or pointing control mode and the pusher was commanded to move to a specific position at a fixed linear slewing rate. In each case the magnet was allowed to return to its original position and the experiment was rerun for a different magnet position.

At the present time, neither the APS Project nor the OTES Project have adequate funds to make the measurements in the 1/100 arc-second region. The measurements could be made only by placing the entire breadboard into a vacuum tank and using a very precise instrument in the vacuum tank to note the angular disturbances resulting from simulated spacecraft motions.

Before a flight project is committed to this free float concept, the vacuum tank tests should be conducted. This work is recommended to NASA in Chapter 1 of Perkin-Elmer Report No. 8900.

Justification Summary

One of the most important aspects of the 2-meter LTP flight is that the data gathered can be used to evaluate the ability of the space telescope to point with adequate precision in the 260 n.m. orbit. As previously shown in this report, the disturbances due to earth anomalies, magnetic field, and air drag are several orders of magnitude higher than the disturbances that take place at the synchronous orbit. Thus, there is a conflicting set of conditions for the future large orbiting telescopes:

- High Orbit - with advantages of low disturbances but with potential disadvantages of daylight operations only.
- Low Orbit - with advantages of night time operations, but with potential disadvantages of excessive input disturbances to the pointing system.

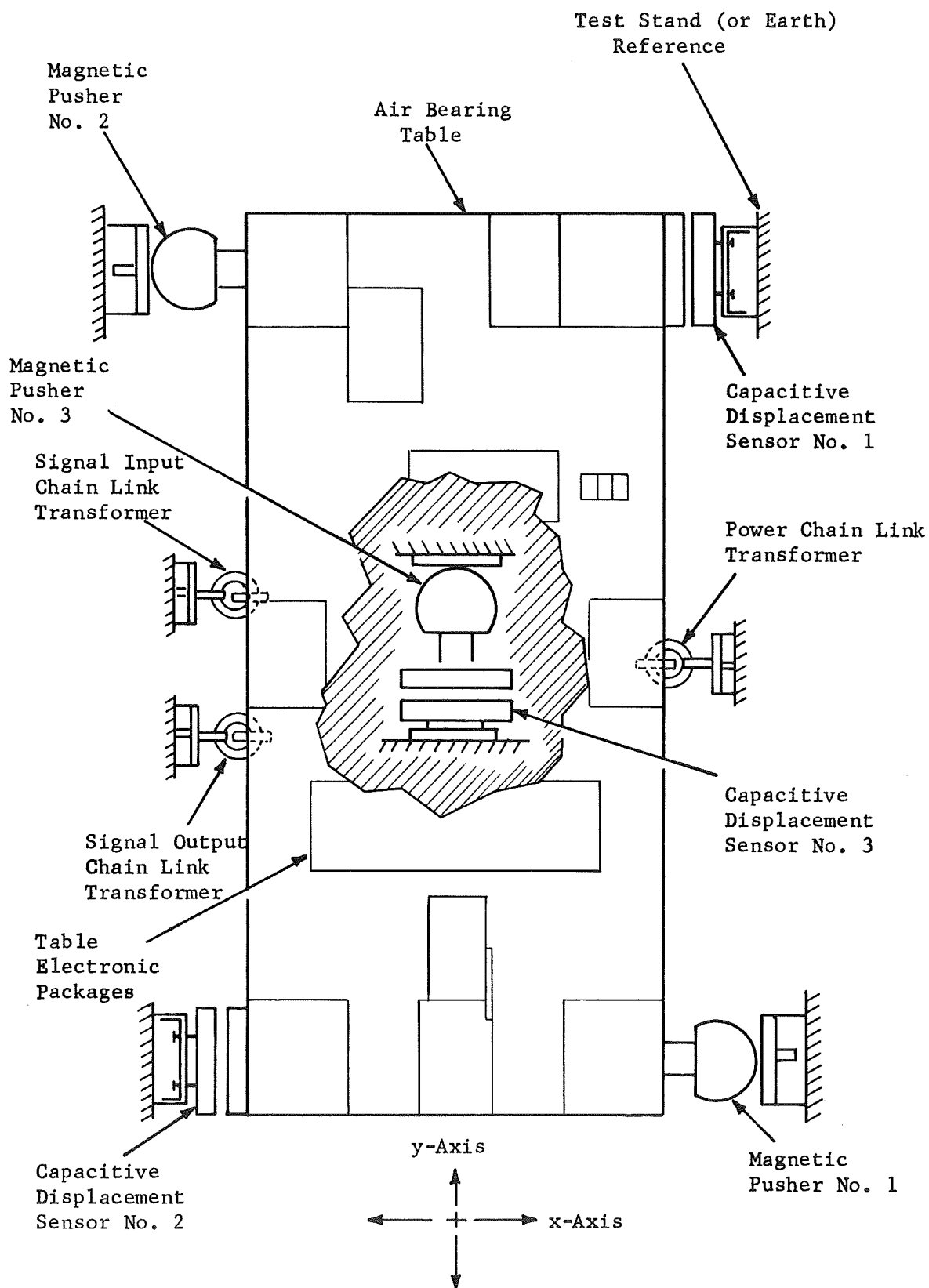


Figure 239. Pointing System Experimental Setup

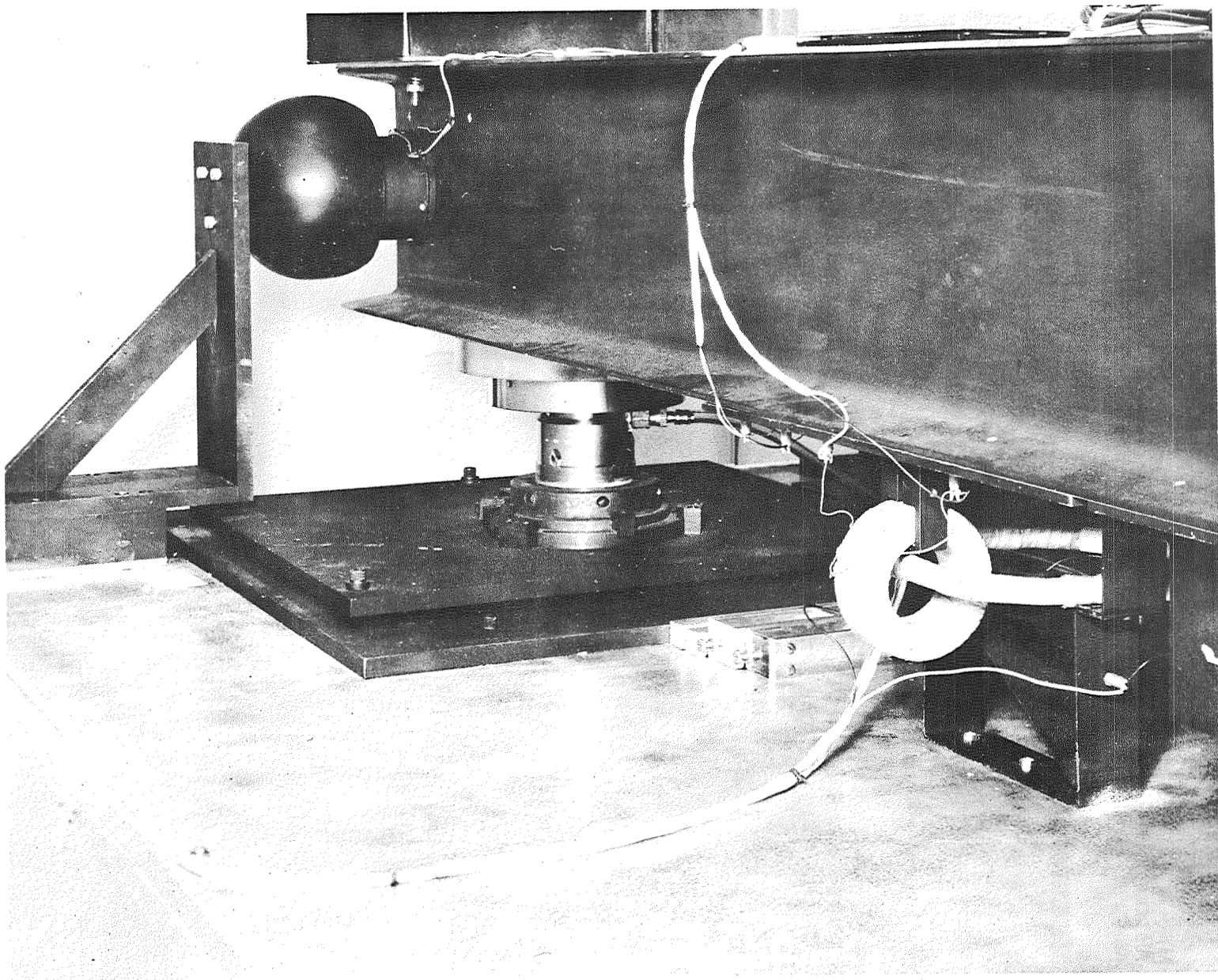


Figure 240. Free Float Pointing System

The proposed Experiment No. 26 will provide information and answers which, in turn, will guide NASA personnel in their decisions on orbital altitude for the NASO.

Experiment Procedure

The three types of suspension systems to be compared in this experiment are the body point mode, gimbals and torquer motor mode, and the free float mode. The body point mode is used in conjunction with a movable transfer lens to achieve fine tracking when the torquer motors are locked and the telescope is rigidly caged to the vehicle. The gimbal mode uses the torquer motors to provide coarse guidance and a transfer lens for the fine guidance system. The free float mode occurs when the telescope is disconnected from the vehicle structure and utilizes an active magnetic control system to provide fine pointing. See Figure 241. Refer back to Experiment No. 9 for additional details.

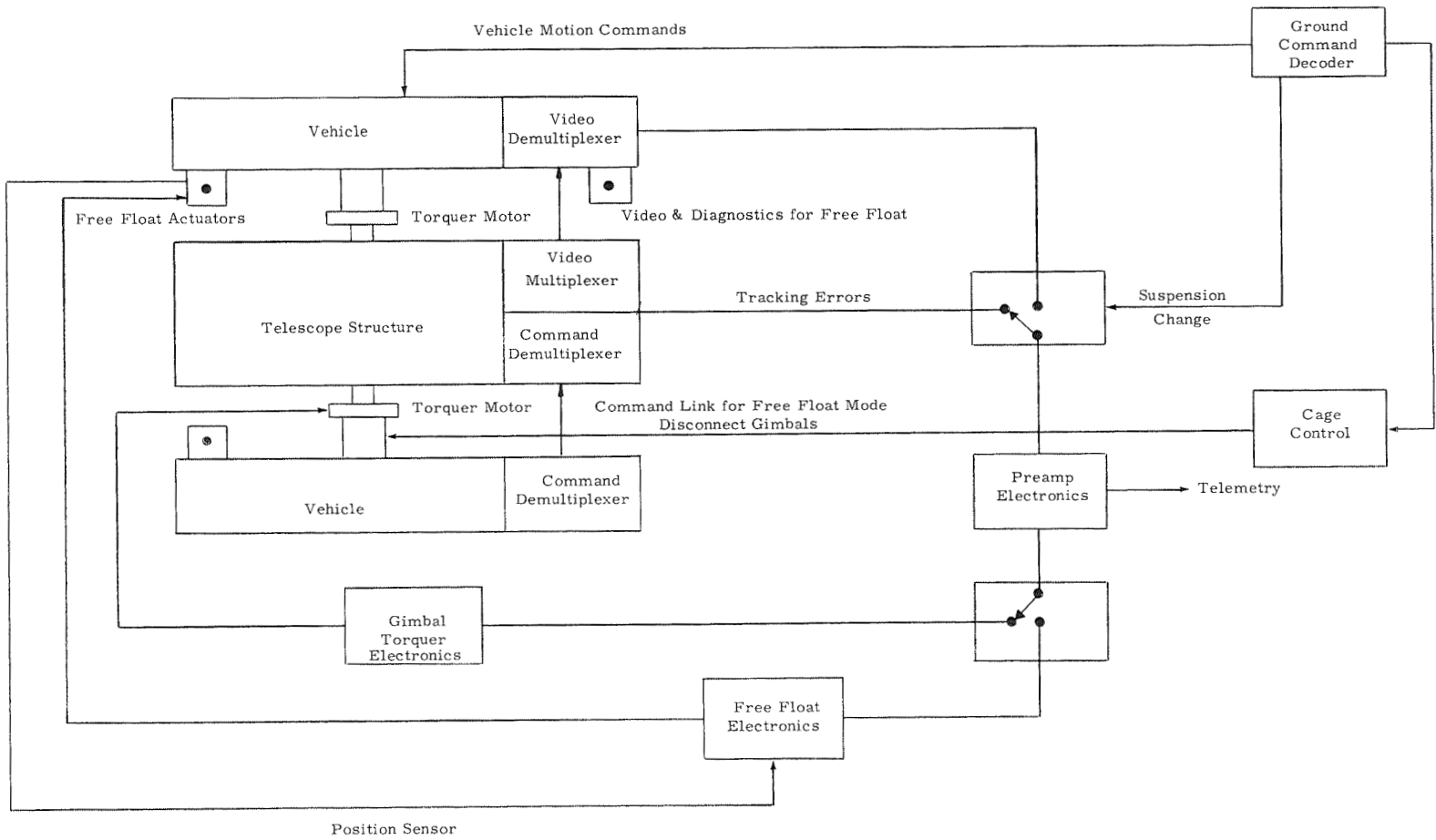


Figure 241. Pointing System Techniques, Block Diagram

I-469/470

EXPERIMENT NO. 27 - SPACE-TO-GROUND-TO-SPACE LOOP CLOSURE
(Deleted from the 2-Meter LTFP)

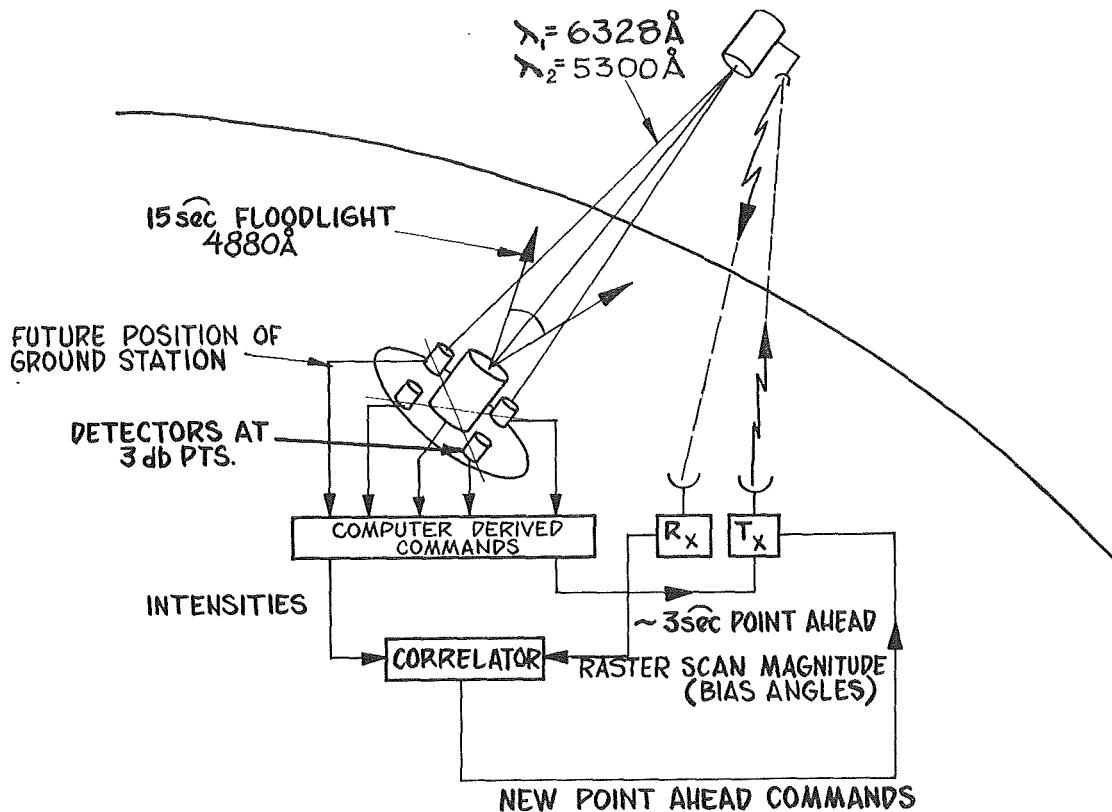


Figure 242. Tracking Verification Scheme

Experiment Objective

Whenever pointing performance (to a precision of 1/10 arc-second down to 1/100 arc-second) of a space optical telescope is considered, the question of verification of performance is raised. In a synchronous orbit optical communication experiment system, the verification is obtained by using the illustrated technique. This experiment is an instrumentation procedure that can be used to calibrate the accuracy of space-transmitting beam control loops. The objective of the experiment is to develop the effectiveness of this instrumentation loop. The utility of the loop at longer ranges (with corresponding increases in the transport lag of the photons between earth and the spacecraft) will decrease. At synchronous altitude, transport delays of 0.2-second round trip can be accommodated.

This experiment can not be conducted at the 260 n.m. orbit of the 2-meter OTES. The spacecraft is in the line of sight of the ground station for less than 4 minutes and the angular rates between the laser beam and the space telescope are impractically high. The experiment remains recommended for a synchronous orbit flight. Further details can be found in Report 8500 starting on page I-164.

EXPERIMENT NO. 28 - ATMOSPHERIC SCINTILLATION AND IMAGE JITTER EXPERIMENT
(Deleted from the 2-Meter LTP)

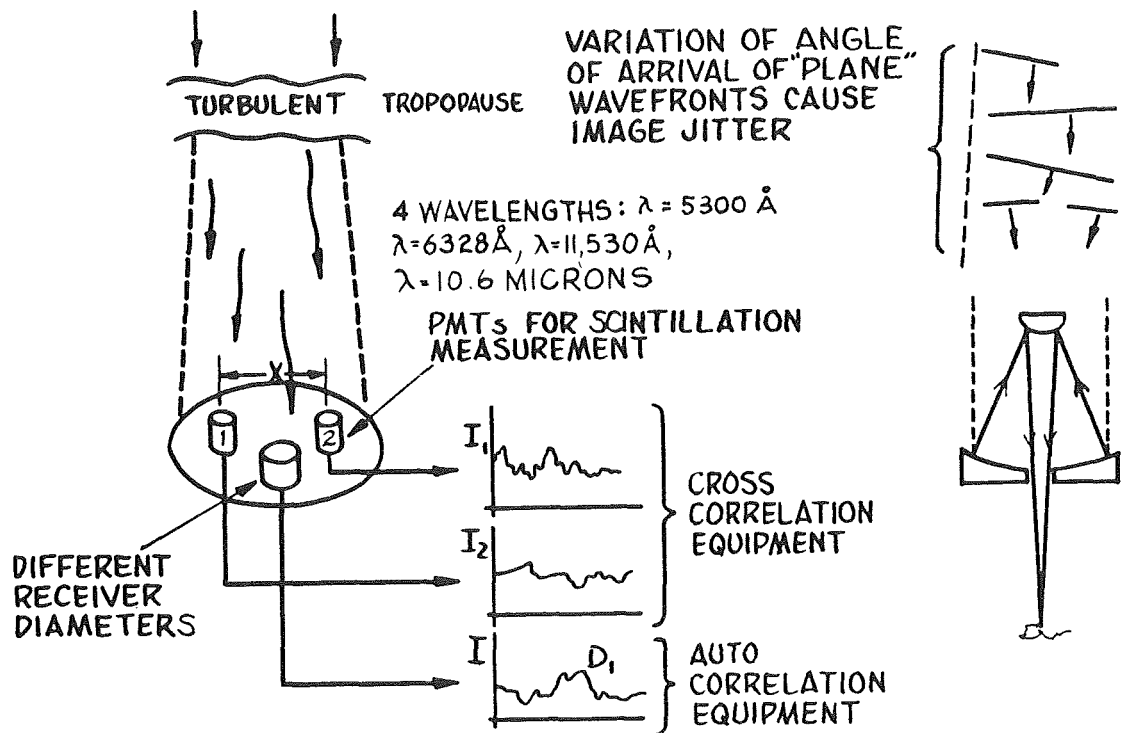


Figure 243. Scheme for Measurement of Scintillation and Image Jitter

Experiment Objective

The experiment objective is the measurement of fluctuations of light intensity and image jitter from a coherent source (after passage through the entire atmosphere). This experiment should be conducted for up and down transmission of coherent light between the spacecraft and earth. These intensity and image fluctuations will be correlated with measurements of intensity and image fluctuations of starlight. This should be done for several small receiver aperture sizes, for at least two laser wavelengths, for day and night operation, for several zenith angles, and for various meteorological conditions. In addition, there should be determination of the auto- and cross-correlations and power spectral densities of the intensity and image jitter fluctuations. This information should be correlated with the outputs of a meteorological data collection system.

This experiment is recommended for a synchronous orbit vehicle; however, it cannot be executed in the low orbit 2-meter OTES. Additional information is published in Report 8500 starting on page I-168.

EXPERIMENT NO. 29 - HIGH-RESOLUTION ATMOSPHERIC ABSORPTION MEASUREMENT

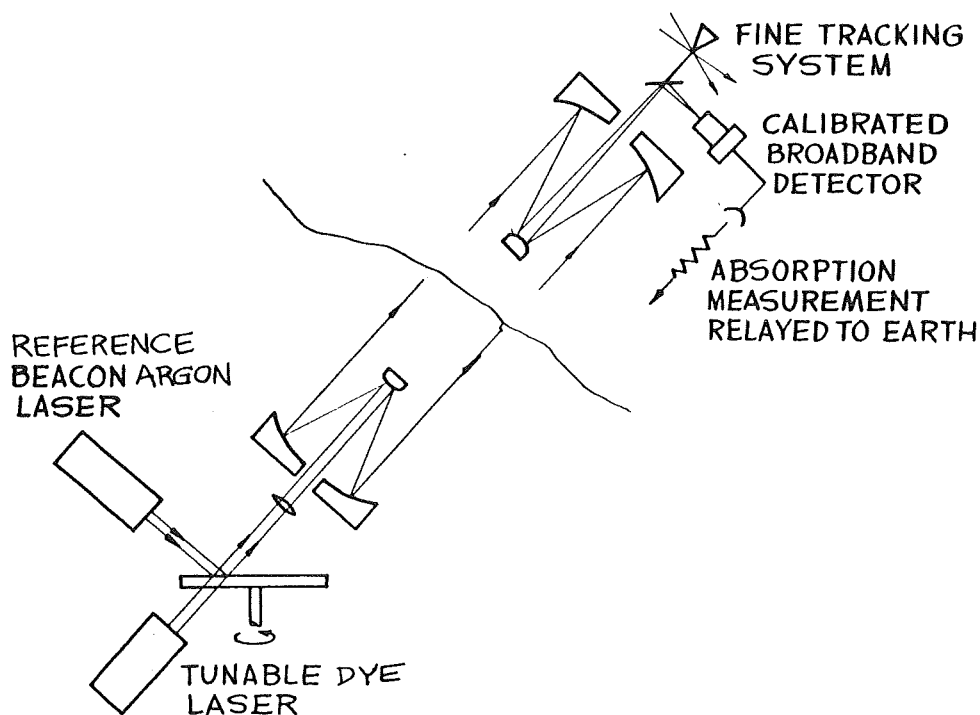


Figure 244. High-Resolution Atmospheric Absorption Measurement

Experiment Objective

A sketch of the laser spectroscopy experiment is shown in Figure 244. The rotating reflective chopper time multiplexes the argon beacon with the output from a continuously tunable laser. The argon beacon is used as the reference in determining the absolute absorption of the tunable laser radiation. Recent advances in dye lasers have produced lasers which oscillate over wide spectral regions (0.36μ to 1.1μ) and with oscillation line widths approaching $0.01\text{\AA}$. Since this line width is narrower than many of the atmospheric absorption lines, significant data on the structure of the atmosphere and quantitative analysis of atmospheric pollutants may be obtained from the link illustrated above.

The OTES Report^{*} suggested that a similar experiment be conducted using the parametric oscillation tunable laser. The low output power of these devices required the use of giant apertures (typically, 2-meter apertures) in space. The present state of the art in dye lasers is 10^{-2} -joule output per pulse radiated into a 5-milliradian beam (approximately 10 times diffraction limited). Undoubtedly this performance will be surpassed in the future. Thus, it is now possible to perform the High-Resolution Atmospheric Resolution Measurement Experiment with an orbiting optical package with nominally a 10- to 20-cm aperture. Thus, it is recommended that this experiment be transferred to an alternate vehicle (e.g., earth resources satellite or optical communication experiment) or be contained as a peripheral experiment to the LTP.

^{*} Wischnia, H.F.: Phase A Study, Optical Technology Apollo Extension System. Vol. 1, Perkin-Elmer Engineering Report No. 8900. October, 1967;

EXPERIMENT NO. 30 - OPTICAL HETERODYNING ON EARTH
(Deleted from the 2-Meter LTEP)

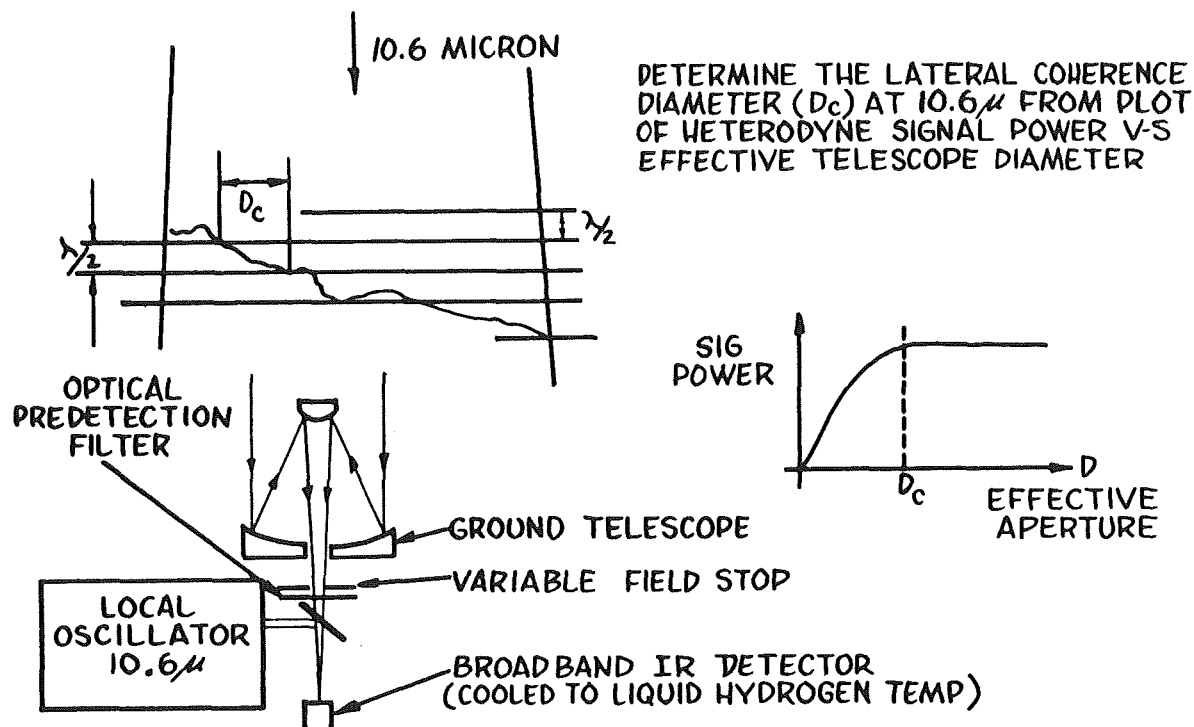


Figure 245. Optical Heterodyning on Earth

Experiment Objectives

The experiment objective is to determine the extent to which atmospheric turbulence affects space-to-Earth optical communications, when the carrier is 10.6 microns and optical heterodyne detection is employed on earth. Atmospheric turbulence will produce phase fluctuations in the optical signal. These fluctuations are the result of refractive index changes produced mainly by random temperature variations.

This experiment requires a synchronous orbit flight such as the Laser Communication Satellite Experiments. It can not be conducted on the 2-meter OTES at the planned 260 n.m. altitude.

In Report 8500 starting on page I-197, Experiment No. 30 is analysed in considerable detail. The material is not repeated here, but it represents the basis for the conduct of a synchronous optical technology flight.

A basic problem in the use of 10.6 micron laser light for deep-space laser communications was unearthed in the beginning of the present phase of work. Despite the removal of this experiment from the 2-meter OTES, the analysis is so germane to the development of this phase of optical technology, that it is included in Perkin-Elmer Report 8900 as part of Chapter 5 starting on page I-474.

EXPERIMENT NO. 31 - OPTICAL HETERODYNING IN SPACE (Deleted-

This experiment is to obtain laser communication technology data. Since communication technology is no longer an objective of LTP, this experiment is deleted.)

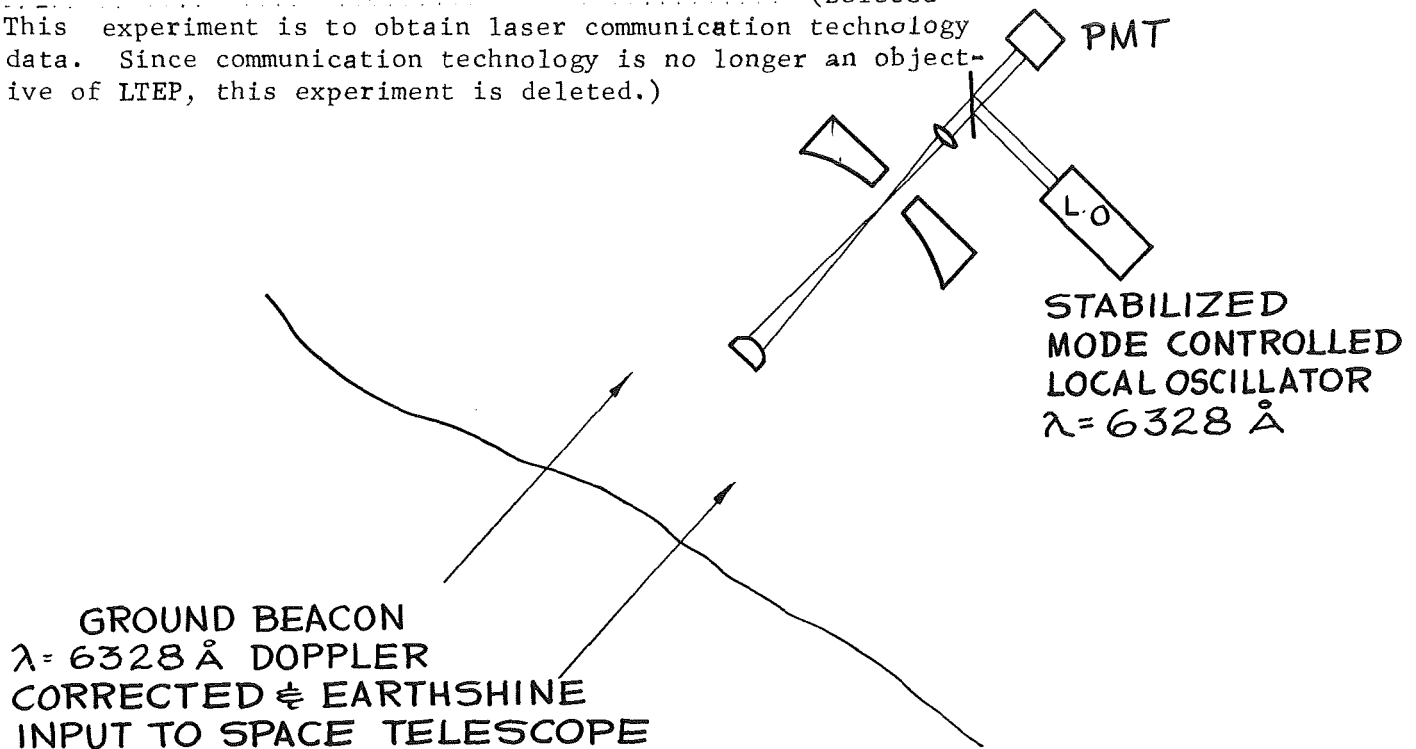


Figure 246. Optical Heterodyning in Space

Experiment Objective

The objective of this experiment is the development of sufficient engineering experience with the design and operation of a spaceborne optical heterodyne system so that its feasibility, reliability, performance, and cost can be evaluated relative to an alternate intensity detection scheme using predetection filtering. Due to the low earth orbit of the 2-meter OTES, moderately large Doppler shifts will be developed in the optical carrier. The development of techniques that can accommodate large Doppler shifts will be aided by the conduct of Experiment No. 31.

Experiment Justification

The advantage of optical heterodyne detection in space appears at present to be substantially outweighed by several disadvantages: the requirement for high-power, frequency-stabilized, and mode-controlled lasers; severe tuning requirement; and the necessity for broadband photodetectors.

Careful examination of the mission parameters for a deep-space planetary orbiter is required before a decision can be made as to whether optical heterodyne detection in space has sufficient advantages over intensity detection to justify the additional equipment complexity. The proposed experiment would provide the flight experience type of data for this technique.

EXPERIMENT NO. 32 - ATMOSPHERIC EFFECTS ON POLARIZATION
(Deleted from the 2-Meter LTEP)

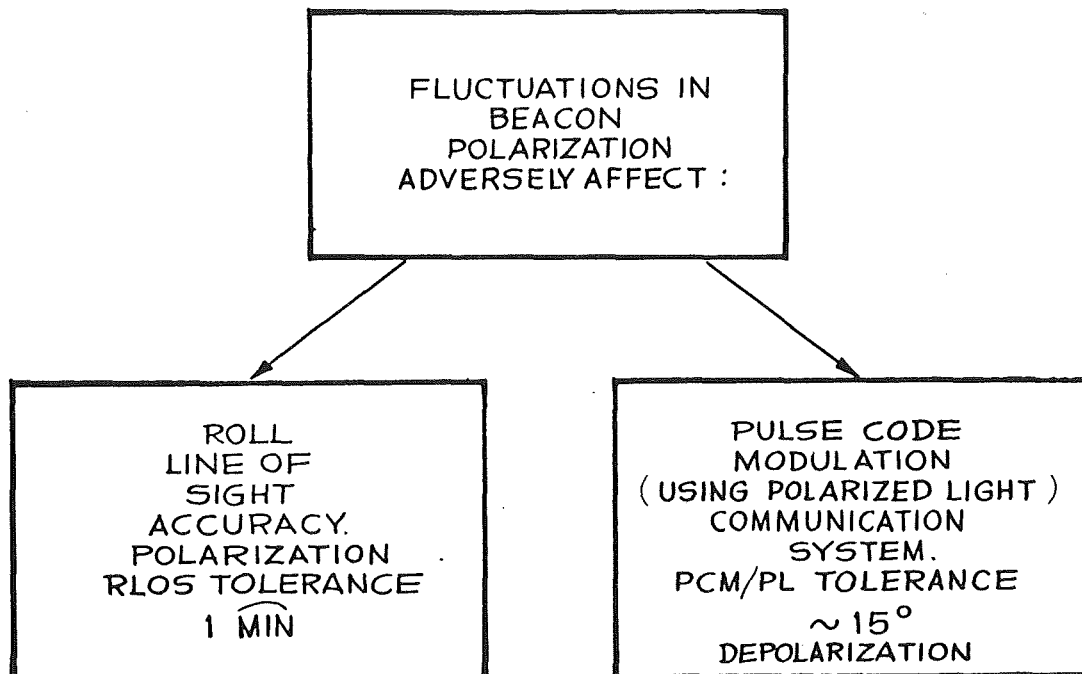


Figure 247: Atmospheric Effects on Polarization

Experiment Objectives

The objective of Experiment No. 32 is the measurement of polarization effects of the atmosphere on a down-going laser beam. The interaction of a magnetic field (such as the Earth's magnetic field) with a material medium (such as the atmosphere) causes a rotation of the plane of polarization of transmitted light. This is the Faraday effect. For the Earth's atmosphere, a Faraday rotation of the order of 1 arc-minute may be observed.* Faraday rotation is unimportant to communications** and attitude control, but there may be other unknown effects.

This experiment is impractical to conduct from the 2-meter OTES at the 260 nautical mile orbit. Experiment 32 remains a recommended optical technology experiment, but it should be conducted from synchronous orbit. Further details on the experiment can be found in Perkin-Elmer Report No. 8500 starting on page I-215.

* Derived in Perkin-Elmer Report No. 7846, pages 3-24.

** Peters, W.N. and Arguello, R.J., Fading and Polarization Noise of a PCM/PL System, IEEE Conference on Laser Engineering and Applications, June 1967.

EXPERIMENT NO. 33 - CLEAR AIR TURBULENCE RESEARCH

(Deleted from the 2-Meter LTP Astronomy Telescope)

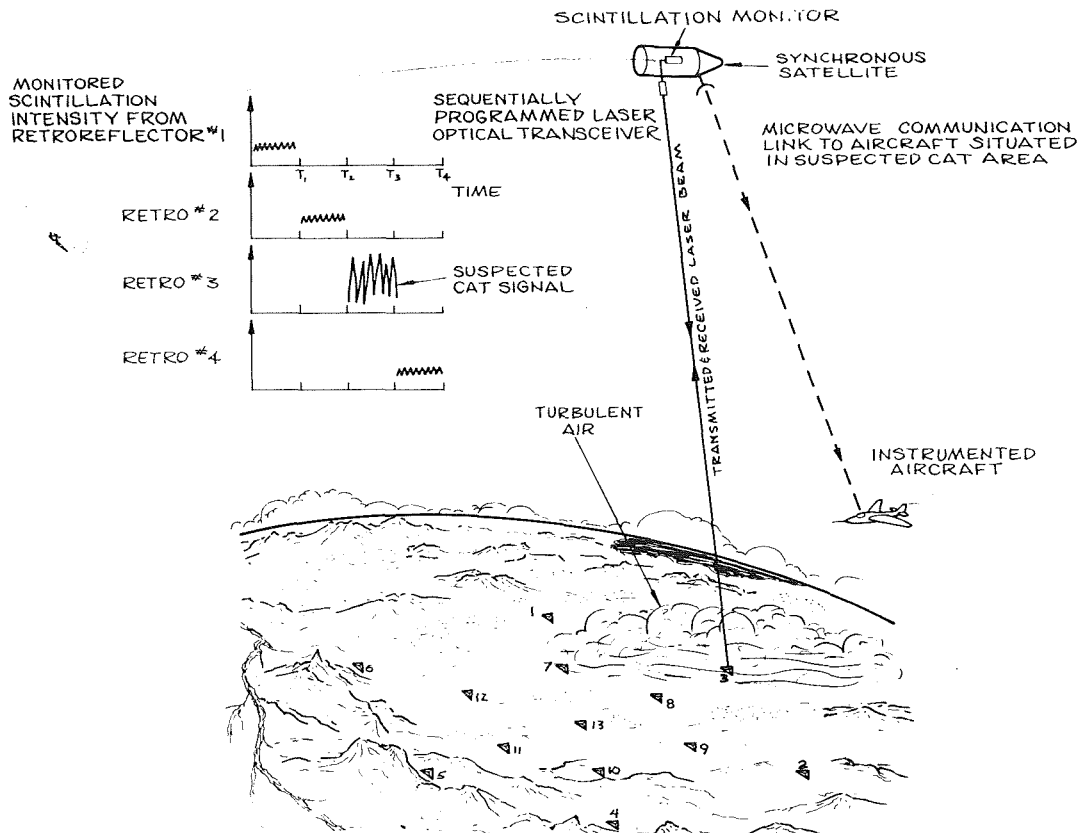


Figure 248. Measurement of Clear Air Turbulence

Experiment Objective

The objective of the proposed experiment is to utilize the spaceborne laser telescope system of the OTES Program to aid the Clear Air Turbulence (CAT) research activities. At present, one of the most difficult problems facing research workers is locating the Clear Air Turbulence area. The proposed experiment would assist by taking advantage of the programmable laser beam aboard a synchronous spacecraft, which can be detected after passing through the suspect regions during the daytime as well as the nighttime. The concept of the experiment stems from the theoretical work that has been done in correlating starlight image scintillation to CAT. If there is correlation between starlight scintillation and CAT, then there will be correlation between laser light and CAT. Since the real problem is to locate the area where CAT exists, the OTES can make a real contribution by using inexpensive and unattended retroreflectors at various sites on the By programming the laser beam from the satellite to these areas (and note that an extremely large number of retroreflectors can be used, because their cost is low), that area where CAT is indicated can be investigated by an instrumented chase plane.

EXPERIMENT NO. 34 - OPTICAL SCATTERING ENVIRONMENT *

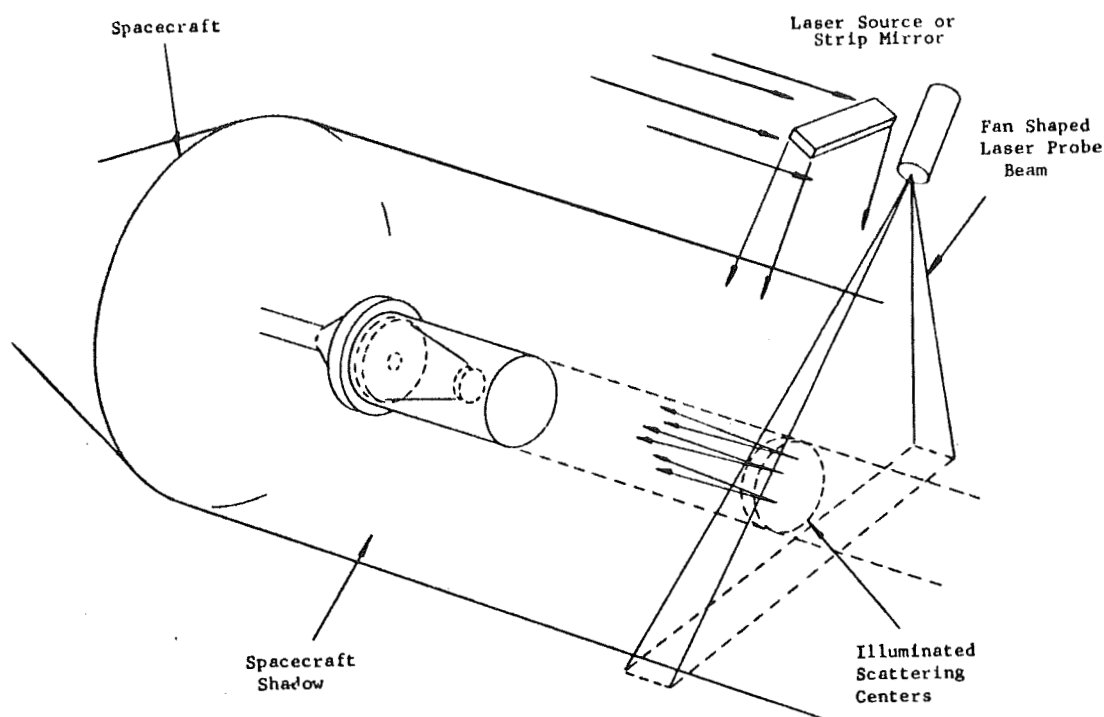


Figure 249. Measurement of Optical Scattering

Experiment Objective

Observations made by several astronauts from the Mercury and Gemini vehicles suggest that limiting optical environment of the spacecraft is determined by the amount of debris surrounding the vehicle. Sunlight scattered from the debris provides a background radiance which has, so far, prevented visual observation of stars greater than second magnitude. The object of the experiment is to give a quantitative measure of the amount of sunlight scattered into an orbiting telescope by the cloud of debris accompanying the spacecraft. The measurements should also be made using a monochromatic beam to help in determining the scattering function for the particles as a function of scattering angle.

Experiment Justification

The scattered light from a particle in front of the telescope can adversely affect an orbiting telescope in two ways:

- By providing a uniform background irradiance in the telescope focal plane which, in turn, determines the magnitude of the dimmest star that can be detected.
- By producing mispointing in the tracking system.

* See also Volume II, Section . . .

** Ney, E.P.; Huch, W.F.: Science 153, July 1966.

If the scattering from individual particles is isotropic, they will not begin to be imaged at the infinity focus if they are within a distance

$$R_C = \frac{D}{\theta}$$

where θ = the total field of view of the telescope, and D is the telescope aperture. Therefore, for a large aperture, small field-of-view telescope, isotropic scatterers will produce a uniform background irradiance which will decrease the tracking system S/N ratio. The amount of scattered light per resolvable angle determines the limiting visual magnitude which can be detected. If the scattering function contains strong forward or side lobes, whose angular width is less than θ , the particle will be "imaged" for distances less than R_C . The particle thus behaves as a "star" and can seriously affect the tracking system.

The illustration at the beginning of the experiment shows the proposed method of measuring the scattering function as a function of angle γ and distance of the scattering volume in front of the telescope, R . The telescope is mounted to a space vehicle which is oriented so that the telescope line of sight near the front of the telescope is within the shadow of the space vehicle. A light beam whose lateral size is much larger than the telescope aperture (to reduce the need for precise probe beam pointing) is directed onto various parts of the shadow region. The light beam can either be a fan-shaped laser beam or can be produced by using a strip mirror to reflect sunlight into the shadow region.

Experiment Discussion

Large Debris Particles

Many theoretical models can be used to estimate the amount of scattering from molecules and particles of debris in the front of the aperture. The scattering from particles whose diameters are much larger than the wavelength of the scattered radiation ($d > 10\lambda$) can be estimated if it is assumed that the particle is a Lambertian reflector and also diffracts light into the forward direction.

The amount of light scattered into a direction, γ , from a Lambertian sphere illuminated by sunlight of intensity I_s (watts/cm²) can be described by

$$I = I_s \cdot \frac{\rho}{\pi} \cdot \frac{2}{3} r^2 [(\pi - \gamma) \cos \gamma + \sin \gamma] \text{ watts/steradian}$$

where r = particle radius and ρ is the particle reflectivity. The amount of light scattered into each resolvable angle of the telescope is thus

$$I = I_s \frac{\rho}{\pi} \frac{2}{3} r^2 [(\pi - \gamma) \cos \gamma + \sin \gamma] \left(\frac{\pi}{4} \cdot 1.22 \frac{\lambda}{D} \right)^2 \text{ watts/res. element}$$

This is shown as a function of scattering angle and particle size for a 1-meter aperture telescope in Figure 161. Since the peak solar radiation occurs for $\lambda \sim 0.5$ micron, a value of $1.22 \times 5 \times 10^{-7}$ is taken as the angular limit of resolution. Calculation of the total amount of light scattered into the telescope requires a knowledge of the size and number density distribution of the debris particles, but is independent of distance in front of the telescope for ranges less than R_C .

Figure 250 also includes a star magnitude scale calculated by assuming the sun's usual magnitude = -27 and that a 1-meter diameter dish is used to image a star. The curves indicate the severity of the problem since the scattered power per resolution element is shown for only one particle of a given radius in front of the telescope. In practice, one expects many thousands of debris particles to contribute to the scattering.*

The forward scattered power per resolution element can be calculated from diffraction theory by assuming from Babinet's principle that the total scattered power is given by

$$I_{\text{scatt}} = \pi r^2 I_s \text{ watts/4}\pi \text{ steradian}$$

The angular dependence is given by**

$$I(\beta) = I(o) \left[\frac{2J_1(u)}{u} \right]^2 \cos^4 \left(\frac{\beta}{2} \right) \text{ watts/unit area}$$

where $u = \frac{2\pi r}{\lambda} \sin \beta$, $I(o)$ is the intensity in the forward direction. Since most of the light goes into the first two maxima of the diffraction pattern and, assuming angles sufficiently small such that $\sin \beta \sim \beta$, $\cos \beta \sim 1$, it can be shown approximately** that

$$I(o) = \frac{\pi^2 r^4 I_s}{\lambda^2}$$

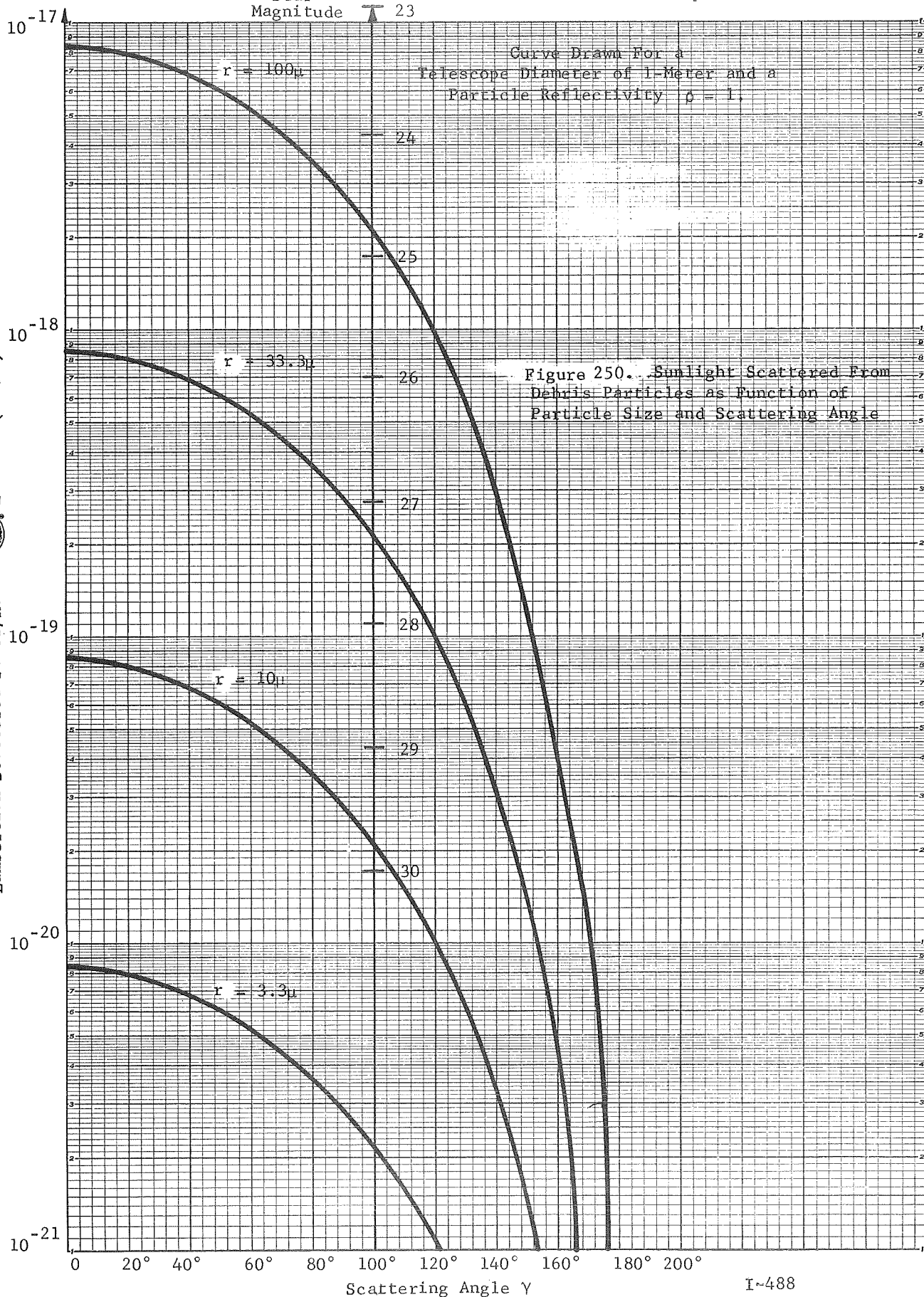
The diffracted power per resolution element is derived as

* Newkirk, G. Jr.: Planetary & Space Science, August, 1967, p. 1267. Submitted August 1966.

** Middleton: Vision Through the Atmosphere. P.31, Equation (3-13).



Lambertian Scattered Power/Resolution Element (Watts)



$$I_{\text{diff}} = \frac{\pi^2 r^4 I_s}{\lambda^2} \left[\frac{2J_1(u)}{u} \right]^2 \cdot \frac{\pi}{4} \left(\frac{1.22\lambda}{D} \right)^2 \text{ watts}$$

The envelope of the function I_{diff} is calculated from the asymptotic expansion for $J_1(u)^*$, i.e.,

$$J_1(u) = \sqrt{\frac{2}{\pi u}} \cos \left(u - \frac{3\pi}{4} \right) + e^{I_m(u)} \left[\text{terms of order } \left(\frac{1}{u} \right) \right]$$

and is shown in Figure 251 as a function of particle radius and scattering angle $\gamma = (\pi - \beta)$.

The diffracted light occurs most strongly in the forward direction and, for this reason, will not limit the performance of an orbiting telescope since a telescope cannot be used close to the sun line.

Small Particles

The scattering from small particles (0.1 to 5 micron) can be computed rigorously by using Mie scattering functions which are well tabulated as functions of particle size, refractive index and wavelength. Little is known about the particle size distribution or the particle refractive index for space debris and consequently meaningful calculations have not been performed at present.

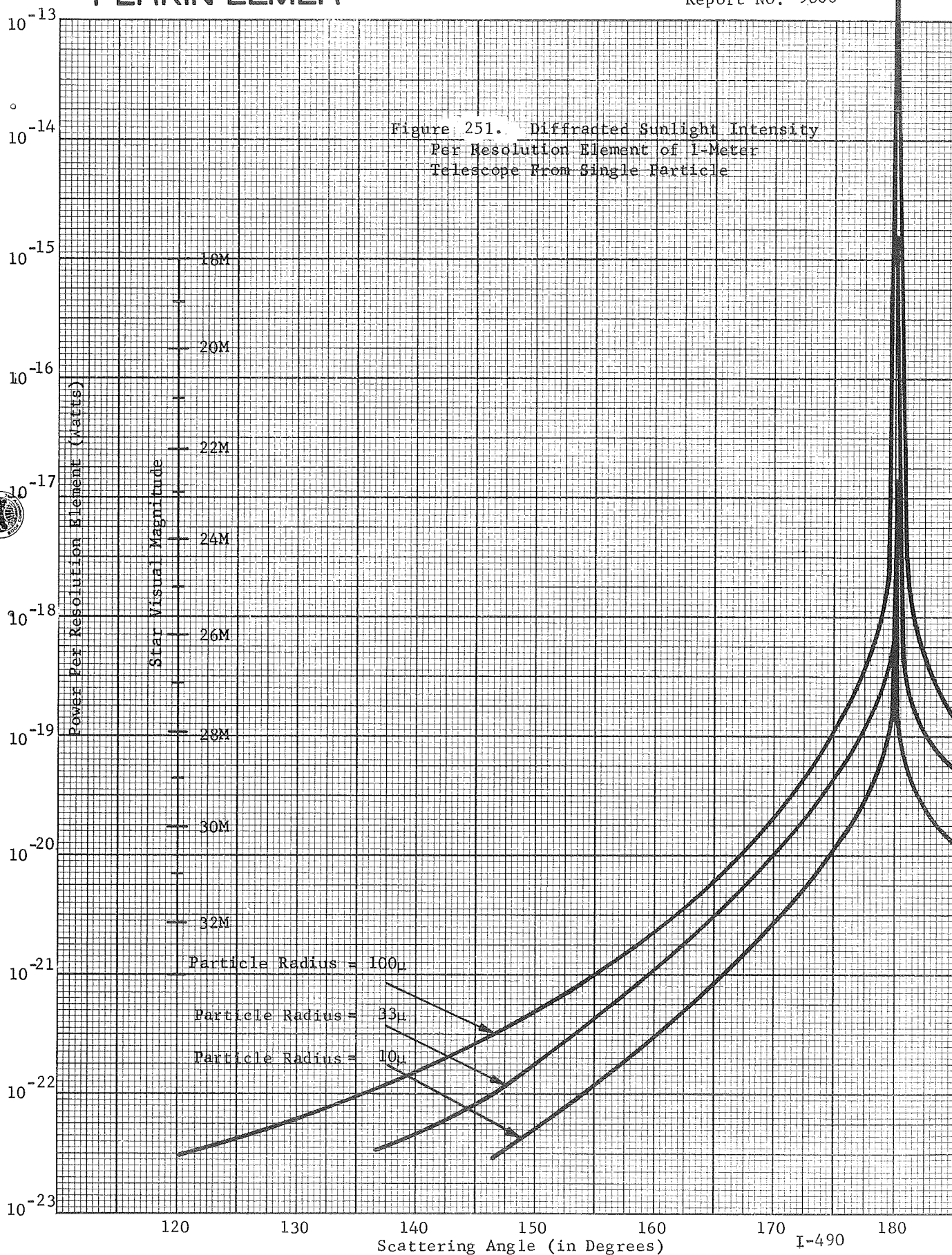
In the photograph (Figure 252) supplied by Dr. Newkirk in a preprint of his paper,** tracks of scattered light from dust motes floating in front of a balloon-borne coronagraph are plainly visible. This illustration is graphic evidence of the need to collect data in this scattered light area in space in order to proceed with future space observations.

* Abramowitz, M.; Stegun, I.A.: Handbook of Mathematical Functions

** Newkirk, G., Jr.: Planetary and Space Science. To be Published.



Figure 251. Diffracted Sunlight Intensity
Per Resolution Element of 1-Meter
Telescope From Single Particle



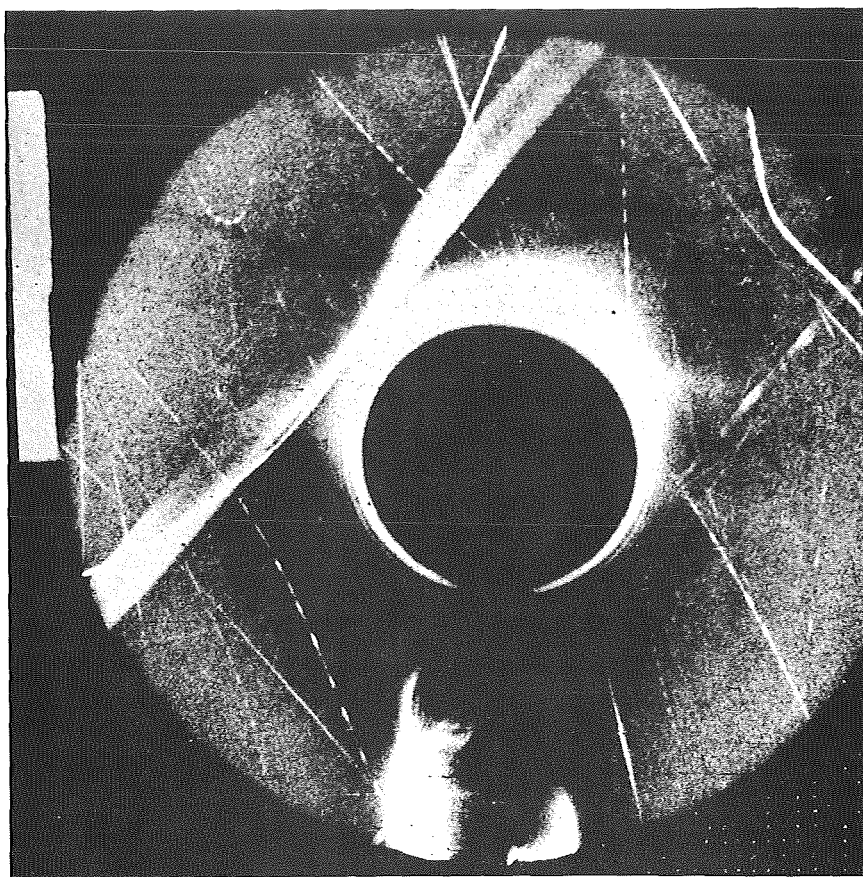


Figure 252. Light Scattering by Dust Motes

Experiment Procedure

The experiment measures the amount of light that is scattered into the telescope field of view by debris particles orbiting with the spacecraft and telescope system. As shown in Figure 253, the spacecraft is oriented so that the telescope is pointing away from the sun with its optical axis aligned along the direction of the solar vector. A modulated light source (He-Ne laser) or reflected sunlight is then used to illuminate the scattering particles in front of the telescope. The strip mirror redirects sunlight into the region in front of the telescope so that the total scattered light can be measured as a function of the distance in front of the telescope aperture.

The laser source is used to obtain scattering data for a specific wavelength by illuminating the particles with a fan-shaped beam. The single wavelength results will simplify the data reduction required to estimate the size and particle distribution around the spacecraft.

I-492

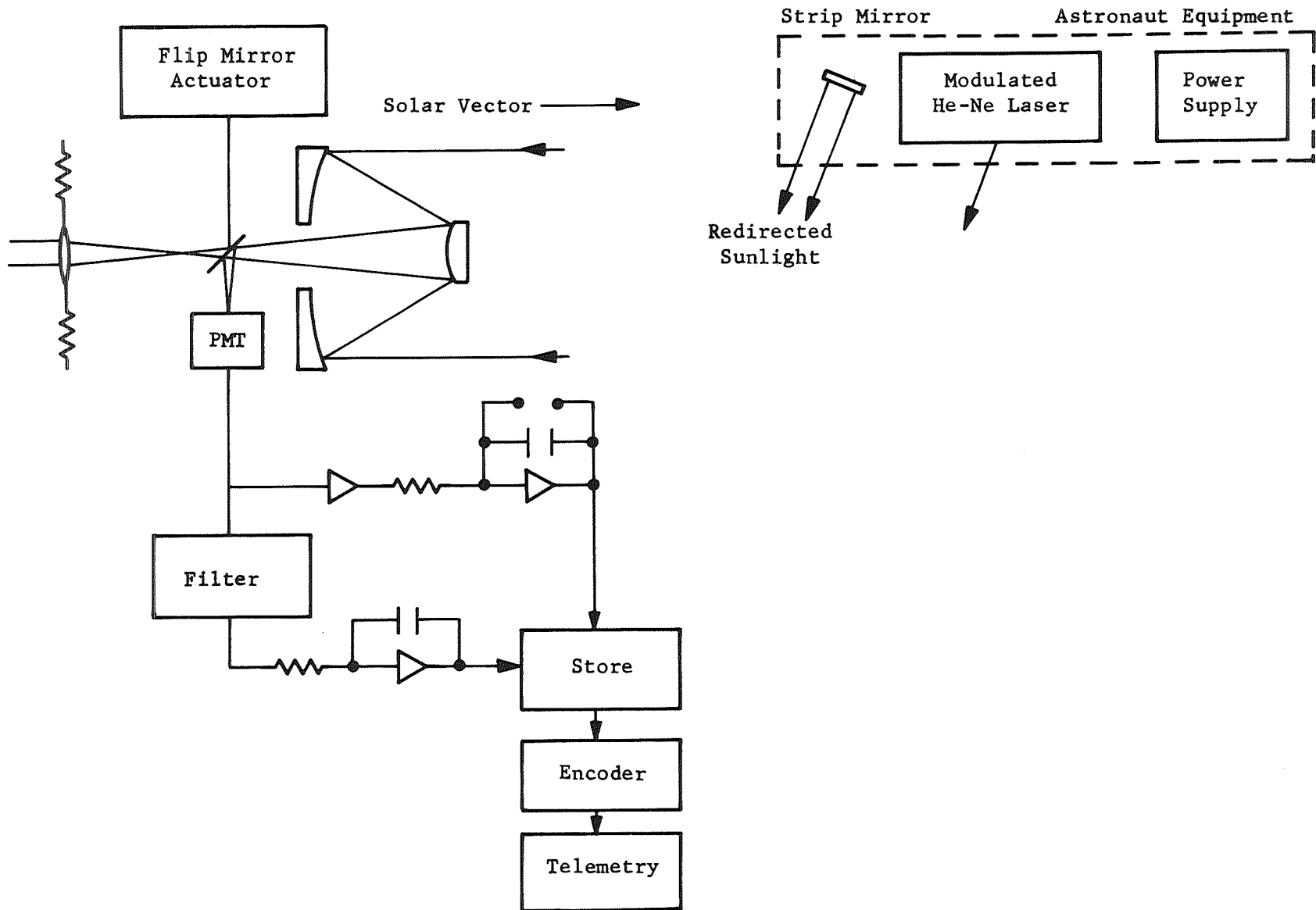


Figure 253. Block Diagram of Optical Scattering Environment Experiment

EXPERIMENT NO. 35 - STELLAR INTERFEROMETRY

Experiment Objective

The utility of launching an orbiting Michelson stellar interferometer is examined in the section on Astronomical Utilization with the recommendation that the interferometer baseline should exceed 100 meters if useful data is to be obtained. This is a significant structural complication to the LTEP design concept. (See Figure 254.) However, considering the extended operational lifetime of the LTEP in conjunction with the large cargo bay and low launch cost of the space shuttle, the retrofit of the LTEP to accept a Michelson stellar interferometer is a distinct possibility. The original Michelson experiment was an impressive accomplishment because of sophisticated optical instrumentation and as a fundamental demonstration of optical coherence theory. An optimist will argue that the original results of Michelson can be easily extended by an orbiting system because of the absence of perturbing atmospheric effects in space. The pessimist will also quite correctly argue that to launch, erect, and operate an instrument of dimensions exceeding 300 feet within sub arc-second accuracy, wavelength tolerance will be exceedingly difficult.

The objective of this experiment is to temporarily convert the orbiting LTEP to a stellar interferometer. Measurements on the dimensional stability of the structure will be of great importance in the design and fabrication of even larger stellar interferometers in the future.

Experiment Justification

The experiment justification is discussed in significant detail in Chapter 3 on Astronomical Utilization.

Basic Experiment Hardware

The following discussion will present a system with supporting preliminary calculations which is a viable alternative (Figure 255) to the classic configuration for the Michelson interferometer. The technique utilizes the class of prisms* which have a constant deviation property. The two prisms which were considered in this analysis are the penta prism and roof penta prism (penta prism with one reflecting surface replaced by a roof prism). Using matrix notation, the requirement of a prism system for the application being considered is

$$\bar{I}_o = \bar{P}_2 \cdot \bar{P}_1 \cdot \bar{I}_i \quad (1)$$

where $\bar{I}_o$ and $\bar{I}_i$ are 3-vectors representing the direction cosines of the input and output, respectively.

P_1 , P_2 are the matrix operators for the prisms of Figure 255.

The Michelson stellar interferometer will have no errors if the prisms P_1 and P_2 have the following property:

*The word "prism" in this section should be construed to mean a series of first surface mirrors oriented in the same sequence as the surfaces of their classic solid glass analogs. This type of construction obviates the aberrations caused by changes in the index of refraction.

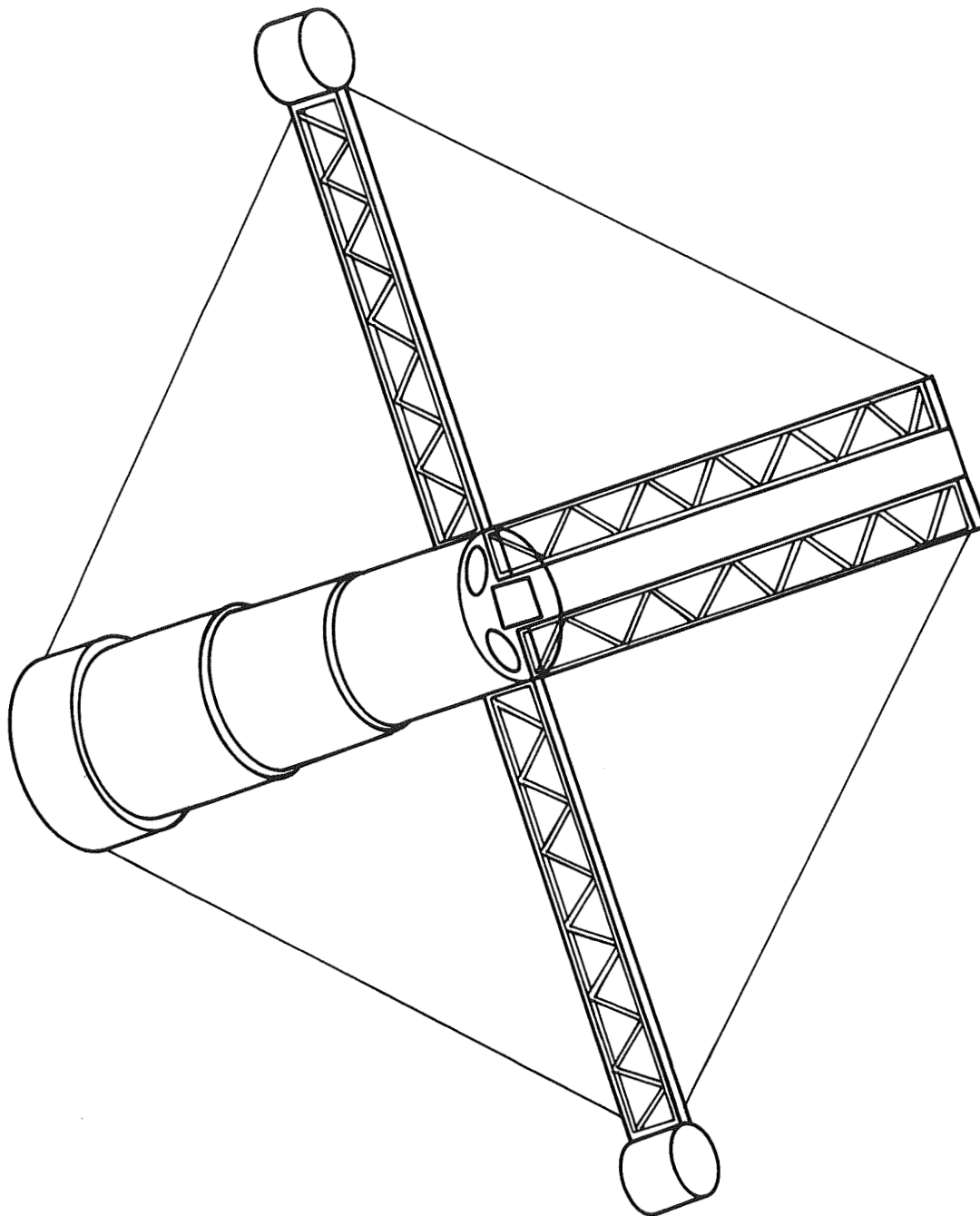


Figure 254. Two-Meter Telescope Modified for Michelson Interferometer

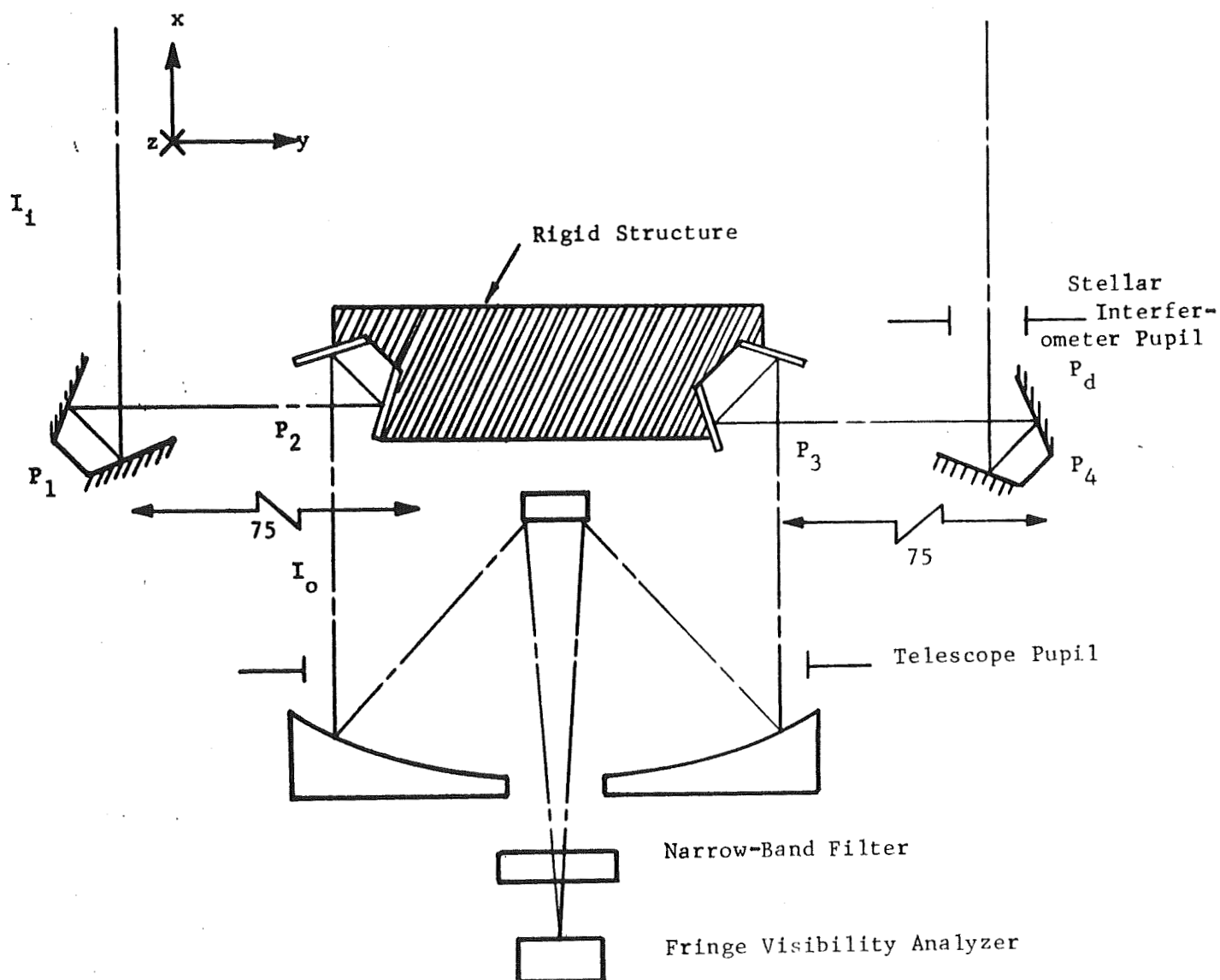


Figure 225. Stellar Interferometer Utilizing Prisms

$$\bar{P}_2 \cdot \bar{P}_1 = \bar{I} \quad (2)$$

where $\bar{I}$ is the identity matrix

By calculation one can determine that pairs of penta prisms (as in Figure 255) or pairs of roof penta prisms have the property required by Equation (2). However, this property in itself is of no consequence since two properly oriented mirrors of the Michelson stellar interferometer have the same property as Equation (2). The advantages of the prism systems are realized when one introduces errors into various elements of the system and then notes the change in system performance.

Before presenting results of this analysis, a brief discussion of errors for a ground-based Michelson stellar interferometer will be presented.

Errors for a Ground-based Michelson Stellar Interferometer

Polarization - Since the polarization perturbation caused by the atmosphere is on order of arc-minutes, it is excluded as a source of error from all interference experiments.

Mechanical Disturbances Within the Instrument - The problems of both "high" frequency vibrations and structural bending and rotation of the elements in Michelson's experiment were circumvented by using a very stiff (and heavy) structure for positioning the remote mirrors. This luxury is unacceptable in a space mission. Also, it can be inferred from discussions of increased baselines for orbiting stellar interferometers that a major source of error is expected to result from the displacement and rotation of the remote mirrors.* Compensation for this error is the motivation for developing the design of Figure 255.

The mirror angular alignment tolerance of the classic Michelson interferometer is a fraction λ/d , where d is the entrance pupil (typically the diameter of the remote mirror). Linear motion parallel to the optic axis will, in general, introduce vignetting. As will be discussed, the interferometer has the additional constraint that both optical path lengths must be equal within, typically, tens of hundreds of wavelengths. Thus, angular tolerance is on the order of a fraction of an arc-second and linear dimensional tolerance as small as 0.001 inch can be expected.

* Factors such as gravity gradient may introduce significant torques on the structure.

Alignment Tolerances

The discussion of alignment tolerances for the proposed orbiting system requires several assumptions:

- 1) Each penta prism has no intrinsic errors.
- 2) Prisms P_2 and P_3 are oriented within a rigid structure such that all mirror normals are coplanar.
- 3) Apertures of P_2 and P_3 are limited sufficiently large to obviate vignetting.
- 4) Only errors caused by translation on rotation of remote prisms are significant.

Although realization of these tolerances is by no means trivial, the technique of utilizing prisms has transferred the difficulty of maintaining two mirrors separated by over 100 feet aligned within arc-seconds (ground-based instrument) to the problem of maintaining alignment of the mirror surfaces of the individual prisms and the alignment of P_2 to P_3 to typically arc-seconds (orbiting instrument).^{*} The resulting trade of increased complexity for relaxed tolerances in the alignment of the remote mirrors (Prisms P_1 and P_4) is thus justified.

The remaining factor to be considered in the system error trade-off is the bandwidth of the narrow band filter. Brown^{**} shows that the fringe visibility is increased for the case when small errors exist between the path lengths of the two interferometer "arms". For example, an optical bandwidth of 100Å (Figure 255) requires that the total optical path lengths through the two arms of the instrument may not vary more than 1 or 2 waves. This is increased to more than 1000 waves if a 1Å narrow band filter is utilized.[†] This is feasible, however, since the development of the solid spacer Fabry-Perot filters which were fabricated for the ATM Hydrogen-Alpha Telescope. Assuming a 4-percent system efficiency (including filter, PMI, optics), 10^6 photoelectrons/second are detected from a 6000°K 1M star if a 1Å bandpass is used.[†] There is an obvious trade-off between increased optical bandwidth and fainter-colder stars.

^{*} Preliminary analysis of a similar optical system (Perkin-Elmer Engineering Report No. 8949) verifies this conclusion.

^{**} RH Brown, Annual Review of Astronomy & Astrophysics (1968), Annual Reviews, Inc. Palo Alto, Calif.

[†] Wolfe, W.L.: Handbook of Military IR Technology. U.S. Printing Office, Washington, D.C., 1965, p. 114

Conclusion

A complete error analysis including both fabrication errors of the individual prisms and structural errors is required to verify the system design. Assuming positive results from this analysis, the proposed technique provides an **auxiliary** experiment which will provide significant new data on stellar diameters with an acceptable change in the basic LTEP design.

EXPERIMENT NO. 36 - CRYOGENIC SPACE STRUCTURE (77°K)

Experiment Objective

The objective of this experiment is to obtain and maintain a 77°K ambient environment for a large telescope structure and its optical system by passive means over long periods of time, while the earth orbiting system is receiving radiant energy from the sun and the earth. The experiment is designed to permit gathering of engineering data on the cold structure's performance while in earth orbit.

Experiment Justification

The orbital astronomy support facility (OASF) study^{*} has demonstrated the need for a 77°K environment for an infrared telescope to be used for astronomical observations. Justifications for orbital infrared astronomical observations and the 77°K temperature for the infrared telescope environment are presented here.

Figure 256 is a plot of the approximate depth of penetration for electromagnetic radiation incident normal to the atmosphere as a function of wavelength. The areas under the curve represent that altitude above the earth where the radiation has been diminished to 1/e of its extraterrestrial value. It is clear from the figure that the earth's atmosphere blankets radiation from the 25 μ to the microwave region, and that large portions of the 1 μ to 25 μ range are also blanketed. The situation in the infrared portion of the spectrum would not be so arduous for ground-based astronomical observations if the equivalent blackbody temperature of the atmosphere were not about 300°K for wavelengths longer than about 2.7 μ . At shorter wavelengths during daylight hours scattered sunlight begins to predominate, while at night, OH emission and airglow become important. Figure 257 summarizes this situation for idealized spectral radiances of the sun, emitting atmosphere, sunlit cloud, and sunlight-scattering clear sky. In the real world, the earth's atmosphere has the highest emissivity where there are strong absorption bands. Therefore, the 300°K sky curve of Figure 257 should be modified along the guideline that the radiance of the atmosphere corresponds to that of a 300°K blackbody when the atmosphere's emissivity = 1.0; at lower emissivities, the radiance is reduced appropriately. Figure 258 plots the emissivity of the earth's atmosphere as measured from a high altitude observing site at 4.62 μ . At this wavelength 86 percent of solar radiation at ground level is absorbed by a wing of H₂O and by N₂O components present in the atmosphere^{**}. It is clear from Figure 257 that the emissivity of the earth's atmosphere is substantial, even in "windows" from good observing sites.

^{*}Wolbers, H.W.: OASF Study (for NASA). Vol. 3, Douglas Aircraft Co., Santa Monica, California, 28 June 1968, DAC-58143.

^{**}Shea, L.: Handbook of Geophysics & Space Environment. Cambridge Research Laboratories, Office of Aerospace Research, USAF, 1965, p. 10.

L_0 Air equivalent at standard temperature and pressure (NTP) for which the radiation has been diminished to $1/e$ of the extra-terrestrial value

h Height above the earth

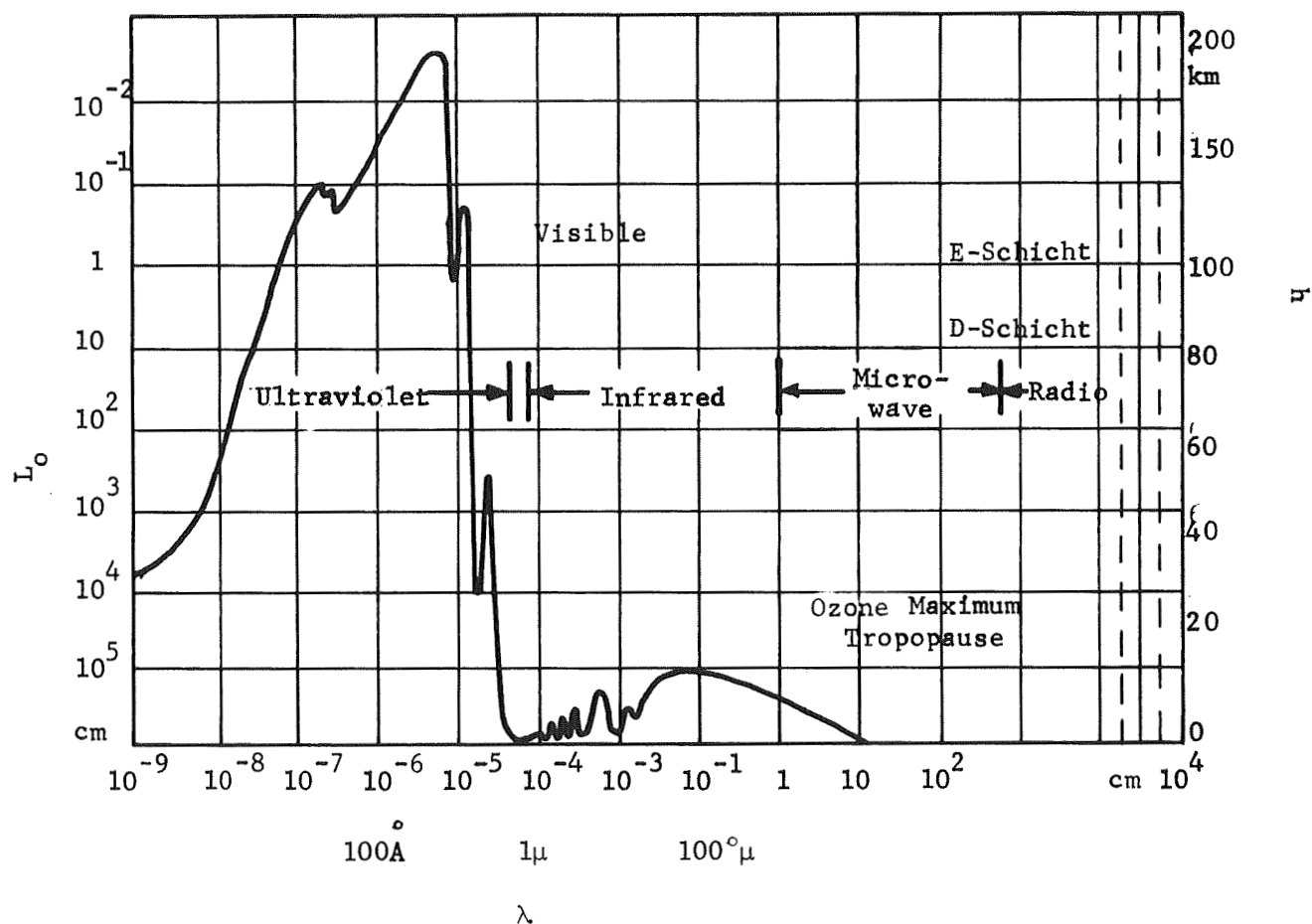


Figure 256. The Approximate Depth of Penetration of Radiation Incident Normal to the Atmosphere as a Function of the Wavelength*

*Siedentopf, H.: Naturwiss. Vol. 35, 1948, p. 289.

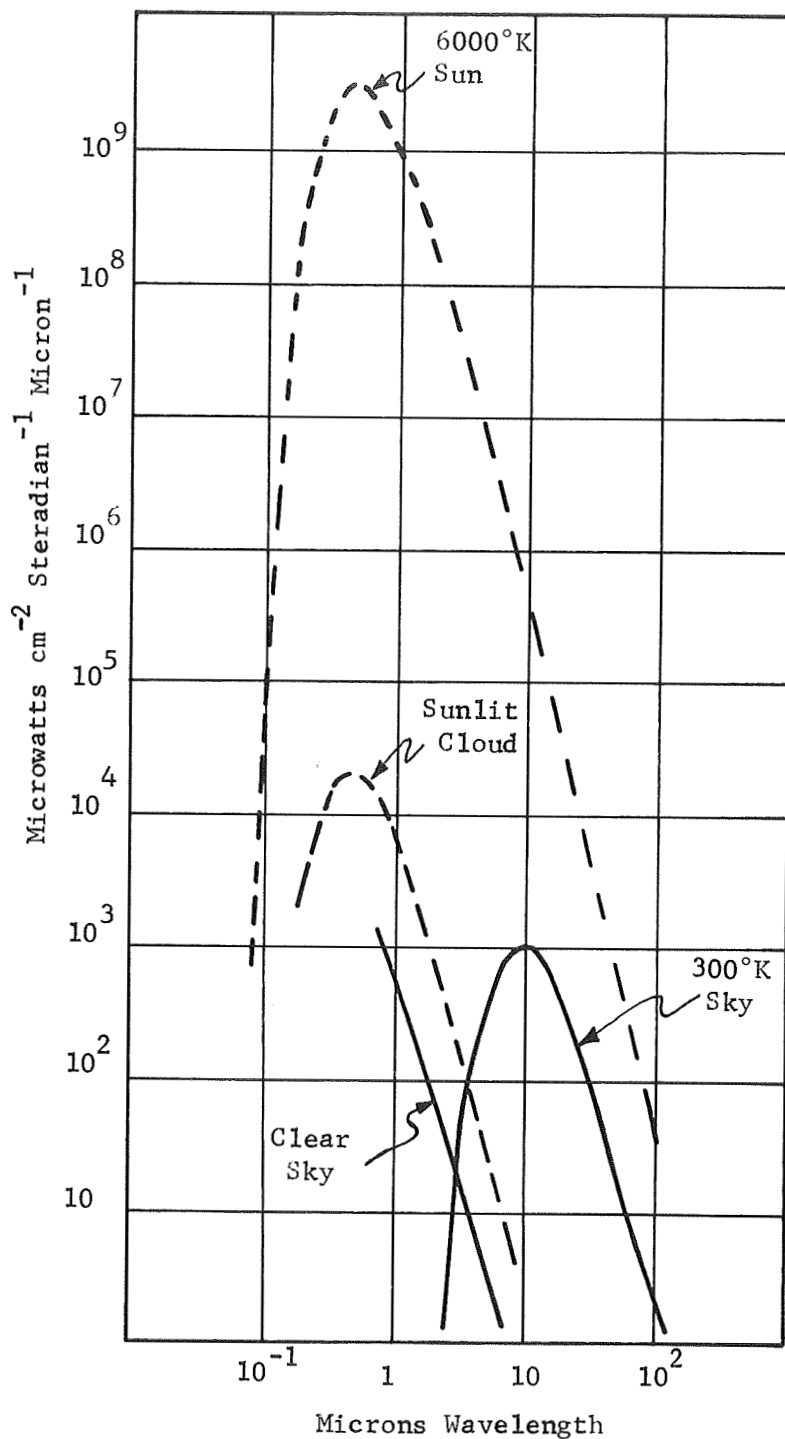


Figure 257. An Idealized Spectral Radiance of the Sun Emitting Atmosphere,* Sunlit Cloud, and Sunlight-Scattering Clear Sky.*

* Bill, E.E., Et al.: J. Opt. Soc. Amer. Vol. 50, 1960, p. 1313.

The effect of the emissivity of the earth's atmosphere on an astronomical observation from the surface of the earth at 10μ with a 1μ spectral bandwidth, will now be analyzed as an illustration of the above points. The astronomical object will be assumed to be the sun but located at the distance of the nearest known star to the earth (α Centauri) located at 4.3 light years. Assuming a 6000°K blackbody temperature for the sun, one can calculate that about $2 \times 10^{-1} \text{ W}/\mu$ of solar flux falls on 1 meter^2 of the earth's atmosphere at 10μ . At the distance of α Centauri, this is reduced to $5 \times 10^{-12} \text{ W}/\mu$. Assuming a 300°K atmosphere with an emissivity of 1.0 located a mean distance of 10 Km from the observing telescope, the total power received at the image plane from the atmosphere is $3 \times 10^{-3} \text{ W}/\mu$, for a 1-meter^2 aperture and 1-degree field of view. Thus we see that sky radiation completely swamps the stellar radiation at 10μ by the ratio of $(3 \times 10^{-3} / 5 \times 10^{-12}) = 10^9:1$. It is interesting to compare the received flux of our hypothetical sun located at α Centauri with the noise equivalent power (NEP) of the best infrared detector for 10μ , which is Ge(Hg) cooled to 30°K , for which the $\text{NEP} = 10^{-11} \text{ W}$ with a bandwidth of 1 Hz.

At this point, some desirable infrared astronomical observations will be discussed. Infrared imagery and broadband spectrophotometry of the planets will be of value, the latter being useful for determination of the amounts of water vapor and carbon dioxide, as well as other compounds, in planetary atmospheres. Red dwarf stars, which emit nearly all their radiation in the infrared, could constitute as much as 50 percent of all observable stars; therefore, an infrared survey of the celestial sphere is in order. Some detailed infrared observations of stars and stellar-like objects which should be made are:

- 1) Broadband spectrophotometry for improving bolometric magnitude data of the cool, late spectral class stars. This data will also provide a means to improve the law of interstellar reddening and provide accurate determinations of the ratio of total absorption to selective absorption.
- 2) High spectral resolution observations for data on the abundance determinations of molecules, since determinations made from the ground-state transitions are the most reliable. Also, the permitted and forbidden vibration-rotation transitions of molecules (organic molecules) occur in the near - infrared region, while permitted and forbidden pure rotation transitions occur in the far - infrared region and the millimeter wavelength bands. A detailed study of peculiar bright-line stars should lead to a discovery of new infrared forbidden atomic lines.

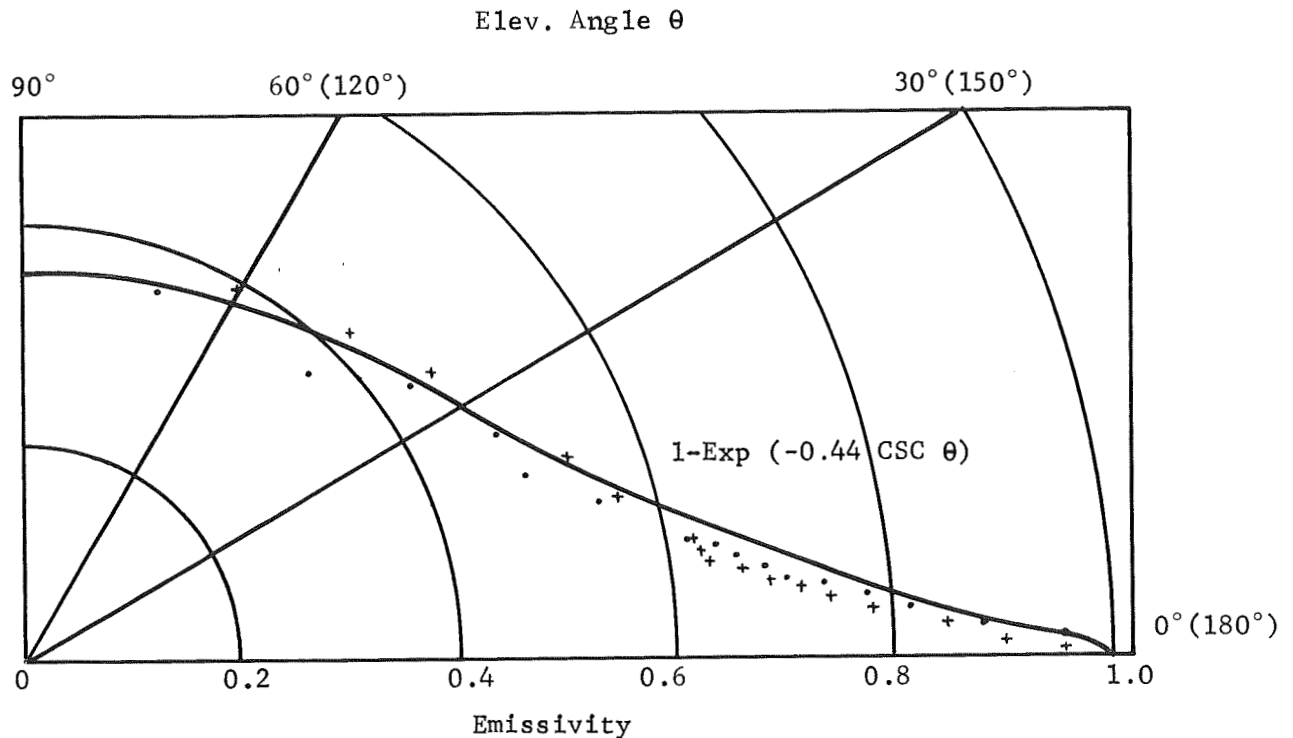


Figure 258. Comparison of Theoretical and Experimental Variation of Emissivity with Elevation Angle. (The experimental points are plotted as dots for the 0° to 90° elevation range and as crosses for the 90° to 180° range. The data were obtained at Sacramento Peak, New Mexico, 9200 ft above sea level, in August, at a wavelength of $4.62\mu^*$)

* Bill, E.E., et.al.: Journal of the Optical Society of America, vol. 50, 1960, p. 1313.

- 3) The detection of individual red stars in the galactic bulge should be attempted. Associated with a search for red dwarf stars is a selective high resolution spectroscopic survey of the interstellar medium in the infrared. The stars in the solar neighborhood account for only 38 percent of its total mass, the remainder being either in the form of stars too faint or too cool to be observed from the surface of the earth, or in the form of mass in the interstellar medium. Atomic hydrogen in the interstellar gas can account for only 2 percent of the medium, and the amount of dust is insignificant, so it is possible that a large fraction of it could be in the form of molecular hydrogen. The rotational lines from the ground state of H_2 occur at 28.1μ , 16.9μ , and 12.0μ . Other non-hydrogen lines that might appear in the interstellar medium are the ionic lines of CII and SiII, atomic lines of OI and the rotational lines of CH, NH, and OH. Finally, infrared observations are necessary for objects that are located at the extreme edge of the observable universe. The spectral lines of objects at these distances are shifted into the infrared by the "red-shift", and observations there will provide an observational extension of the application of the $\Delta\lambda/\lambda$ relation over a much longer wavelength interval than is possible from ground-based observations. Also, at the very largest distances, approaching the radius of curvature of the universe, individual galaxies become too faint and numerous to be resolved and thus constitute a feebly glowing celestial background. This glow should be peaked in the infrared by the red-shift. The spectral and spatial distribution of this glow over the celestial sphere has a direct bearing on the validity of the various cosmological theories.

The desirability of a 77°K cold structure for an orbiting infrared telescope will now be presented. Figure 259 plots the spectral radiance for a 300°K and 77°K blackbodies. Figure 260 plots the spectral radiance for a 77°K body that has an emissivity of 0.01. This would be the typical emissivity of highly polished gold-coated optics for an infrared telescope. The radiation background from a telescope with a 1-meter² primary mirror and a 50-meter focal length will now be determined for the 300°K and 77°K cases. A 1-mm² detector located at the focal point of the telescope will receive radiance from the primary mirror which is radiated over a 4×10^{-10} steradian solid angle. The spectral radiance of a 300°K primary mirror at 10μ , according to Figure 259, upon correcting for the 0.01 emissivity factor, is $10^{-1} \text{ Wm}^{-2}\mu^{-1}\text{sr}^{-1}$. Hence, the total energy at the detector radiated by the primary mirror is $4 \times 10^{-11} \text{ W}/\mu$. For a 77°K primary mirror the corresponding background radiation at 10μ is $8 \times 10^{-17} \text{ W}/\mu$. Thus, cooling the telescope to 77°K reduced the background radiation level by a factor of about 10^5 . We saw previously that the radiation received from a star such as the sun at 10μ ,

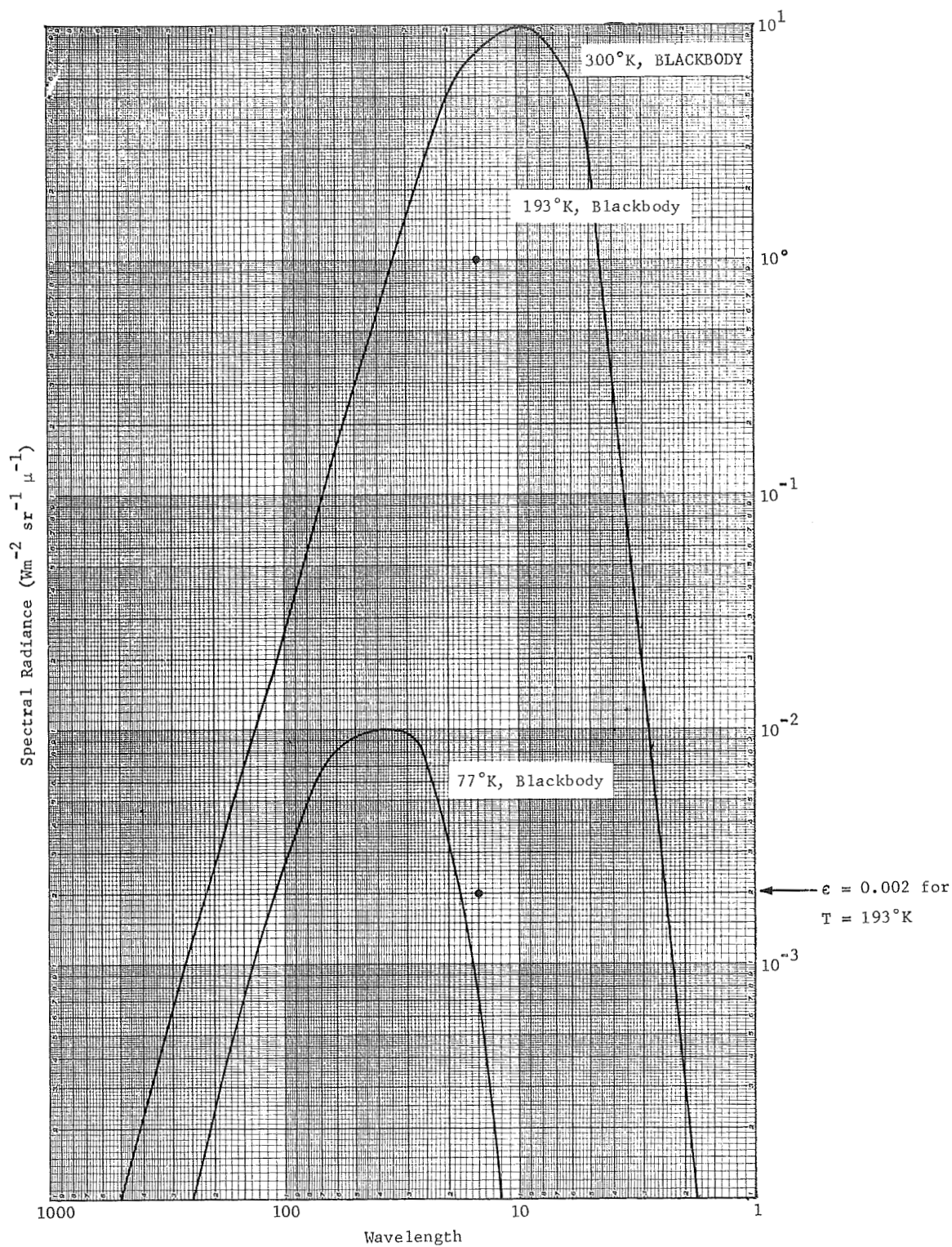


Figure 259. 300°K, 193°K, 77°K Blackbodies

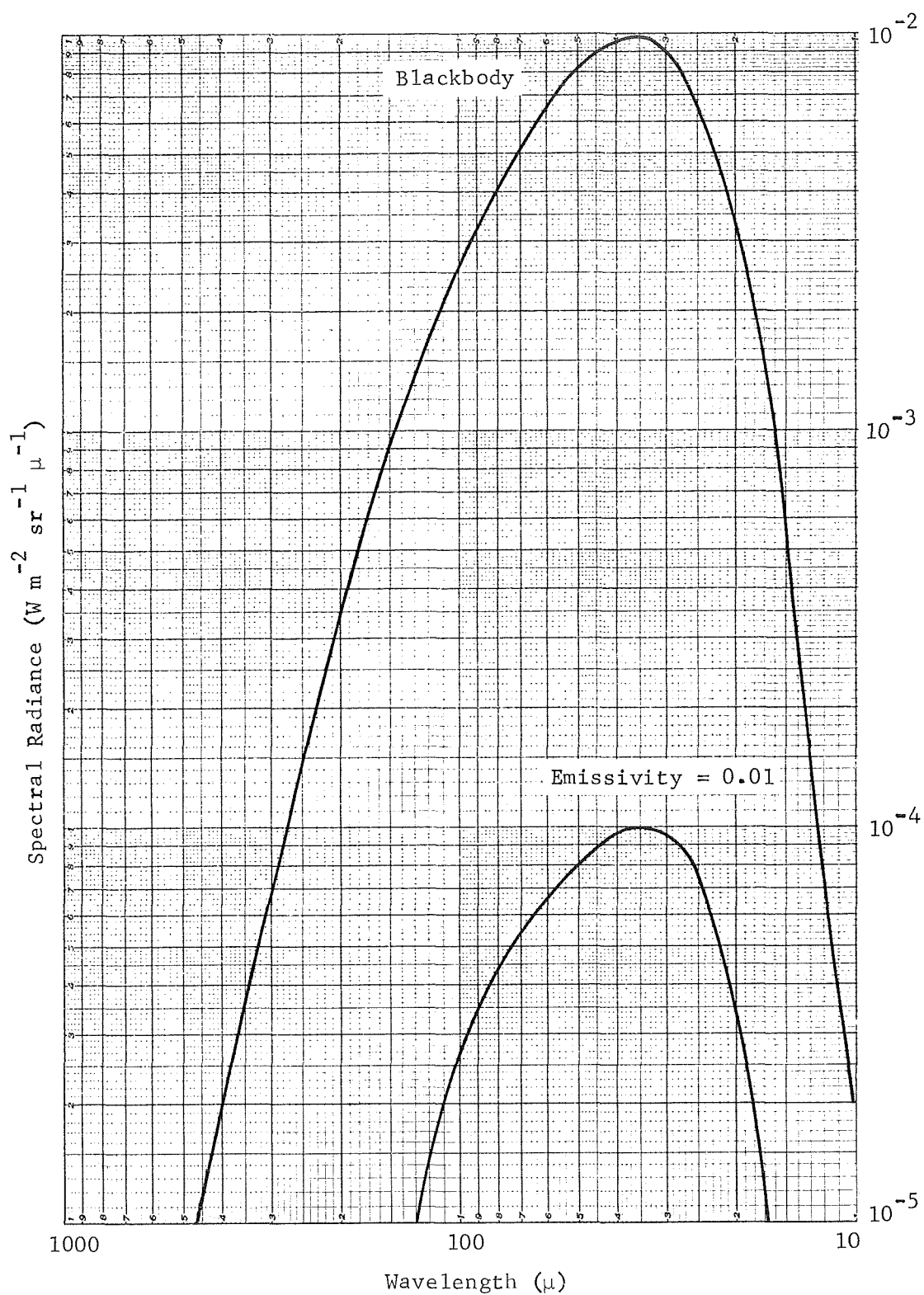


Figure 260 . Spectral Radiance at 77°K

located at the distance of α Centauri is about $5 \times 10^{-12} \text{ W}/\mu$, for a telescope with a 1-meter² aperture. Therefore, the background radiation of a 1-meter² 300°K primary mirror exceeds the energy received from the star by a factor of 10, while the 77°K telescope is only limited by the background noise generated in the detector, which we saw was equivalent to about 10^{-11} W for the best detector available for 10μ .

Technology Deficiencies

Achieving cryogenic temperature in the optics and structure of a space telescope is an exceedingly difficult thing to do in a large aperture telescope. Because of this, it is more difficult to extend the observational spectrum in the direction of the infrared region than it is in the direction of the ultraviolet. If it were not for the radiation from the sun and earth, this problem would not be nearly so difficult. The problem is obviously eased if the sources of heat can be reduced. Flying in a synchronous orbit where the solid angle subtended by the earth is much smaller would be of advantage. A means for reducing ambient solar radiation might be flying a station-keeping object (such as one of the echo balloons) so that the telescope could operate in its shade. While this might be of advantage, it would be somewhat impractical. For this reason such a configuration has not been studied.

Another approach to the problem is to decrease the emissivity of the reflecting optics so that their contribution to the background infrared radiation is reduced. Dielectric film coatings may be of use provided the restricted bandpass is commensurate with the astronomy experiments. The principal constraint in coating development is the dearth of measuring instruments for the emissivity and reflectivity range required. For example, in theory, gold should reflect 0.998 of the incident radiation leaving an emissivity of 0.002. There is no instrument which can reliably measure at these levels. This needless to say, inhibits any meaningful research on coatings in this reflectance range. However, if this emissivity could be accomplished (and the scattering were zero), Figure 259 shows the peak of the gray body curve for a mirror at 193°K is lower than a 77°K blackbody.

Another implementation which would improve the signal to noise by rejecting at least part of the telescope background is to chop the entire telescope aperture. The signal caused by telescope background is then unchopped and the signal processing electronics can therefore discriminate against the background signal. The chopper could take the form of a venetian blind with slats rotating about their longitudinal axes, and covering the entire aperture. Obviously, such a chopper would be large and cumbersome, and would have to be removed when other types of experiments or observations were being made.

A remaining alternative is brute force active cooling of the structure and optics. This could be done with mechanical refrigeration or by sublimating solids or evaporating liquids such as argon or nitrogen respectively, to accomplish the cooling. These techniques (needless to say) create immense engineering and supply problems and are to be avoided, if at all possible, through reliance on orbital shading or coating techniques.

It is also obvious that technological deficiencies in the area of detectors must be corrected before there will be great payoff in infrared astronomy. For these reasons the cryogenic structure experiment is not presently seriously considered for the Large Telescope Experiment Program as configured on a low-earth-orbit Saturn Workshop or shuttle type of space vehicle. Breakthroughs in coating technology, detector technology, or signal processing technology could yield much improved performance in an uncooled large space telescope. Hence, the above decision should be re-examined periodically.

EXPERIMENT 37 - APODIZATION AND SUPERRESOLUTION TECHNIQUES

The criterion commonly used in judging the quality of an astronomical telescope is the intensity of the center of the image formed by a point source of the far field. It can be shown that for a telescope with a primary mirror of a given area, the unobscured circular aperture illuminated by a uniphase wavefront is optimum. Introducing a central obscuration or phase perturbations will reduce the intensity of the center point. For some specialized applications, the astronomer is more interested with other qualities of the image such as:

- (1) Reduced radius of first dark ring
- (2) Asymmetrical image
- (3) Reduced intensity of Airy rings
- (4) Large portion of energy in central disk
- (5) Image for which position of first dark ring may be continuously varied
- (6) Absence of dark ring along a radial direction

The technique for generating images compatible with requirements (1) and possibly (5) is commonly referred to as superresolution*. Because of conservation of energy, movement of the position of the first dark ring toward the center displaces energy from the central disk to the rings of the image. Also, Wilkins** has shown that as the first dark ring is moved toward the center, the maximum intensity in the disk rapidly approaches zero with the resultant rise in energy in the rings. However, it will be shown that increases in resolution from $1.22\lambda/D$ (the classical definition of resolution of the circular aperture[†]) to approximately $0.7 \times (1.22\lambda/D)$ may be obtained for special conditions where the variables λ and D are the wavelength and pupil diameter, respectively.

Requirements (3) and (4) are obtained by apodization^{††} (Greek for "to take away the feet"). Note that these requirements are the reverse of the super-resolution requirements. Apodization tends to move the position of the first dark ring away from the maximum and transfers energy of the rings into the central disk.

* Wolf, E.: Progress in Optics, III. North-Holland Publishing Co., Amsterdam, 1961, p. 122.

** Wilkins, J.E.: J. Opt. Soc. Amer. Vol. 40, No. 4, 1950, pp. 222-225.

† Jenkins, F.A., White, H.E.: Fundamentals of Optics. McGraw-Hill, New York, 1957, p. 304.

†† Jacquinot, P.: Apodization. Progress in Optics III. McGraw-Hill, New York, 1957, p. 304.

Requirements (2) and (6) are obtained by introducing a nonsymmetrical pupil function. A common technique is to introduce a rectangular stop in the system which produces an image which has low energy densities along the image axes which correspond to the diagonals of the pupil stop.*

For the purpose of this report, object pairs will be classified into two exclusive sets;

Set A $I_1 \geq I_2 > 0.1 I_1$ (binary stars)

Set B $I_2 \leq 0.01 I_1$ (star with planet)

where I_1 , I_2 are apparent intensities of the object pair elements.

The two classes of problems which will be analyzed in this section can now be rigorously defined. The superresolution techniques will be used by astronomer-scientists to investigate binary stars of Class A whose angular separation is less than the Rayleigh criterion resolution of the instrument. The apodization techniques will be used for Class B objects whose angular separation is somewhat larger than the telescope resolution and whose intensity ratio is less than 1:100. This ratio is typical of the photometry expected from the larger planets of a stellar system when compared with the parent star. The limitation to planetary investigations such as this is set by the faint limit of the telescope.

Experiment Objective: Superresolution

The objective of the superresolution experiment is to compare the alternate techniques discussed in Chapter 3 for precisely measuring the orbits of binary stars. These measurements are important for determining relative masses of the binaries. The measurements of stellar masses are critical to astronomers developing models of stellar structure and stellar evolution. The alternate techniques for precisely determining binary orbits are as follows:

- (1) Post exposure processing of images obtained from diffraction-limited optical system. Either the digital maximum likelihood process or the analog-matched filter process appears feasible.
- (2) Displacement of opposing segments of segmented mirror to generate an asymmetrical image.

* Jenkins, F.A., White, H.E.: Fundamentals of Optics. McGraw-Hill, New York, 1957, p. 304.

- (3) Generation of a superresolution image by laterally displacing the center segment of a 7-segment primary mirror. This technique has the advantage that within the limits of the technique, the first dark ring can be placed at any predetermined position.

Equipment Requirements - Superresolution

No additional optical hardware for the LTEP is required to conduct this experiment. Alternative (2) above would most likely require the active participation of an astronaut since there is a definite possibility that the two opposing segments which are tilted could in turn image other unwanted stars into the portion of the image plane of interest. Therefore, the segments should either be covered or removed.

Alternative (3) above does not require direct participation of the astronaut. A trivial addition to the Active Optics control electronics would be required to step the center segment through a predetermined sequence. Alternative (3) also has the advantage that it operates on all point images in the corrected field-of-view.

Experiment Objective: Apodization

The objective of this experiment is to verify the applicability of energy redistribution apodization techniques to large orbiting telescopes. The detection of planets orbiting neighbor stars is extremely difficult not because of the expected angular separation, but because of the large difference in intensities between the two sources. Using the solar system as a typical model, separations exceeding 10 arc-seconds can be expected between the interrogated planet and the parent star.

Remembering the inverse square law of irradiance versus separation, the planet has a chance of detection if it were to have a diameter 0.1 that of the star (which is the case for Jupiter) and be in an orbit equal to that of Venus.

Equipment Requirements.

The required variation of the optical system is demonstrated in Figure 261. The telecentric collimating lens which images the LTEP exit pupil at infinity is required. Since the alignment of the axicon is critical, an active control will be required. Astronaut participation will be required both for the alignment functions and previewing of candidate stars.

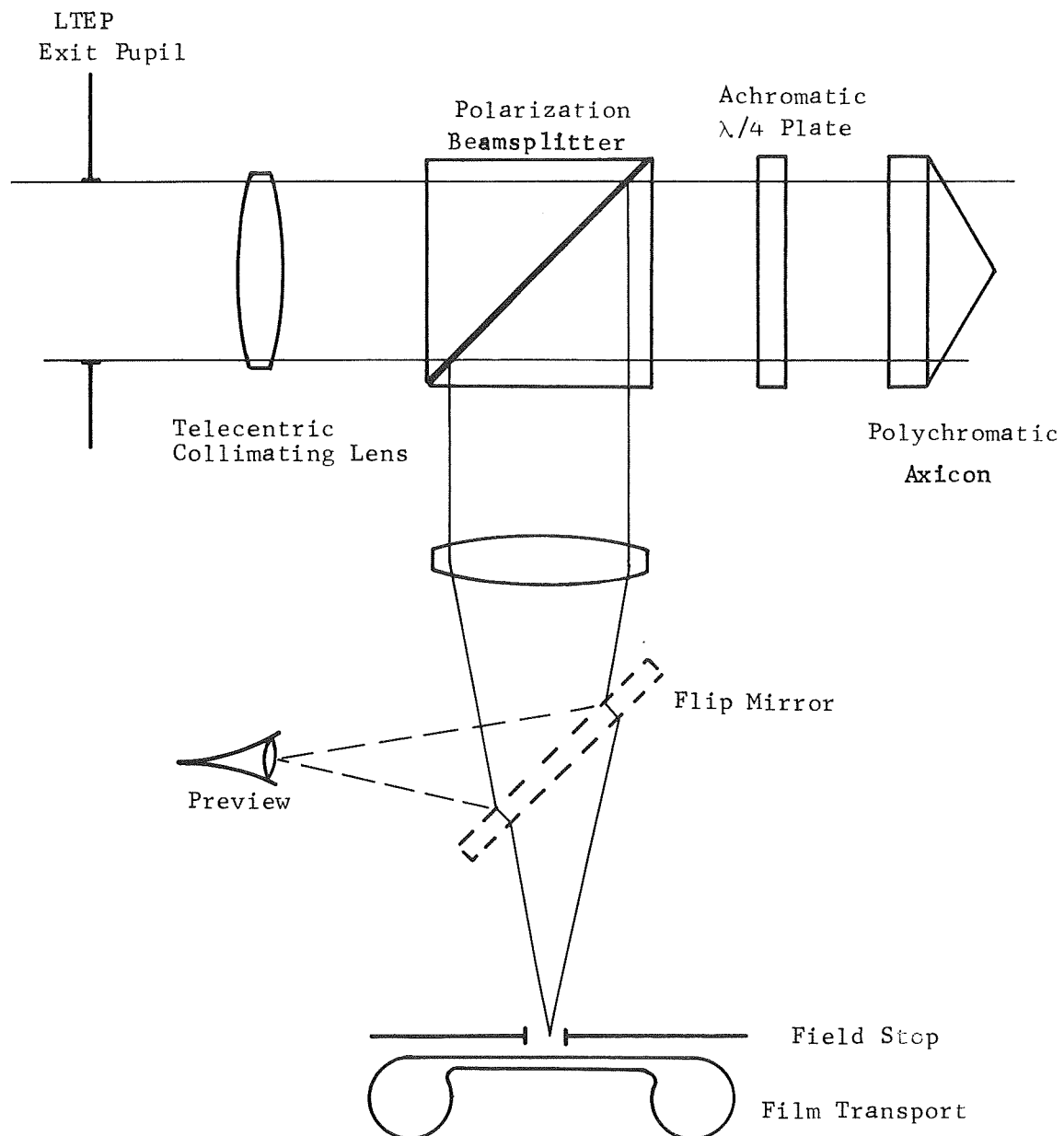


Figure 261. Axicon Optical Schematic

CHAPTER 14 - SUPERRESOLUTION AND APODIZING WITH SEGMENTED ACTIVE OPTICS

SUPERRESOLUTION TECHNIQUES UTILIZING SEGMENTED OPTICS

The orbiting of the LTEP telescope will yield to astronomers a quantum jump in resolution of almost two orders of magnitude for selected sources in the ultraviolet region. Nevertheless, in the scientific community the question immediately arises whether there may be techniques for extending the resolving power to even better values. This section will consider the singular problem of "resolving" binary point sources (stars) somewhat closer than $1.22\lambda/D$, the Rayleigh criterion. The accurate measurement of the orbits (i.e., separation) of binary stars is critical in the measurement of stellar masses. This information has long been of concern to astrophysicists who are studying the theory of stellar structure and stellar evolution.*

This section will consider the applicability of superresolution techniques to the LTEP. The term superresolution is defined as the capability of moving the first dark ring of the diffraction image closer to the central maximum.

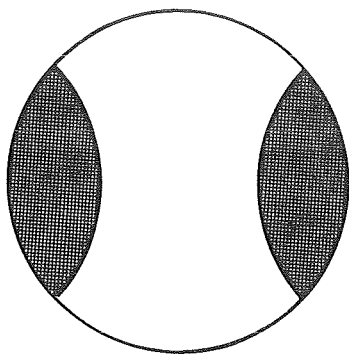
Recalling that the Rayleigh criterion requires that two sources are resolved if the maximum of one source is superimposed on the first zero of the other source, we will artificially increase the "resolution" of the system by super-resolution techniques to be presented in this section. One must treat with care any claims that the true resolving power in general terms is increased by superresolution techniques. A more probing discussion of this point is presented in the conclusion.

The information generated in this section is also valuable in reaching the final design decision of the shape and number of primary mirror segments. Thus, one of the purposes of this section is to present a catalog of various point-spread functions that are easily obtained from the segmented primary mirror. Hopefully, additional applications for the "non-classical" images will be brought to the attention of the project by interested readers.

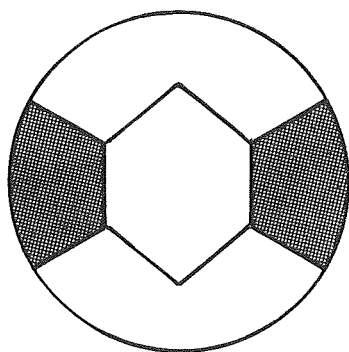
SUPERRESOLUTION TECHNIQUES

During an LTEP interview of Dr. Fredricks, (University of Virginia) a classic superresolution technique (Figure 262a) utilizing opaque disks in a telescope pupil was discussed. The analogy of this pupil function with the pupil function

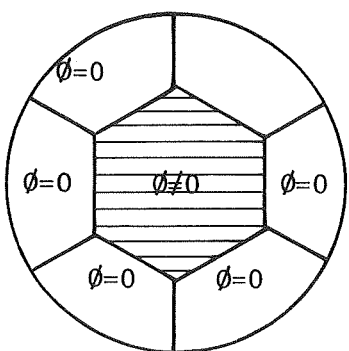
* Hess, H.H: Scientific Uses of the Large Space Telescope. Nat'l. Acad. of Sciences, Washington D.C.



(a) Aperture Occulted by
Two Disks



(b) LTP Primary Mirror with
Two Opaque Segments



(c) LTP Primary Mirror Simulating
Pupil Phase Plate

Figure 262. Superresolution Pupil Functions

of the proposed LTP segmented mirror (Figure 262b) suggests a superresolution technique that could be easily implemented. This technique would require physically removing two opposing segments or tilting them with the servo control to a suitable orientation. An alternate technique requires displacement of the center segment by some phase ϕ to form a contiguous surface which has a phase step. (Figure 262c.) The special case of a pupil containing a disk phase plate which generates superresolution pupil functions has been analyzed by Wilkins.*

The Fraunhofer diffraction theory was utilized to derive the point spread functions of the apertures shown in Figure 262. Slight variations of these pupils were also analyzed. Isophote plots together with isometric views of the images were generated by computer solution of the Fraunhofer integral.

ANALYSIS OF THE LTP MIRROR WITH TWO OPAQUE SECTIONS

The Fourier integral governing Fraunhofer diffraction can be expressed as **

$$U_{(p,q)} = \iint_{-\infty}^{\infty} G(\xi, \eta) e^{-ik(p\xi + q\eta)} d\xi d\eta \quad (1)$$

where the pupil function G is given by

$$\begin{aligned} G(\xi, \eta) &= \text{constant (C) at points in the aperture} \\ &= 0 \text{ at points outside the aperture} \end{aligned}$$

The wave number k is equal to $2\pi/\lambda$, and the integral (1) extends over the entire (ξ, η) plane (plane of the aperture). The intensity $I(p, q)$ is equal to the square of the modulus of $U(p, q)$;

$$I(p, q) = |U(p, q)|^2 \quad (2)$$

It has been shown by Born and Wolf that the constant C appearing in the pupil function is equal to $\frac{1}{\lambda} \sqrt{\frac{E}{D}}$, where E is the total energy incident on

the plane of observation and D the area of the clear aperture. In addition, the intensity $I_0 = |U(0, 0)|^2$ at the center of diffraction pattern $p = q = 0$ is given by

$$I_0 = \frac{ED}{\lambda^2} = C^2 D^2 \quad (3)$$

* Wilkins, J.E.: Journal of Opt. Soc. Amer., vol. 40, no. 4, 1950, pp. 222-225.

**Born, M., and Wolf, E.: Principles of Optics, Pergamon Press, Limited, London, England, 1959, p. 386.

First the contribution of the central hexagon with no central circular obscuration to the $U(p,q)$ diffraction integral will be computed. The region of integration in the ξ, η plane for this case is depicted in Figure 263. The central hexagon may be separated into three components, that is, the rectangular region (2)-(3)-(5)-(6) and the two triangular regions (1)-(2)-(6) and (3)-(4)-(5). The contribution from the rectangular region denoted $U_r(p,q)$ may be expressed via Equation (1) as:

$$U_r(p,q) = C \int_{-\frac{\sqrt{3}s}{2} - s/2}^{\frac{\sqrt{3}s}{2} + s/2} \int d\xi d\eta e^{-ik(p\xi + q\eta)} \quad (4)$$

Performing the indicated integration yields

$$U_r(p,q) = C\sqrt{3}s^2 \operatorname{sinc}\left(\frac{ksp}{2}\right) \operatorname{sinc}(ks\sqrt{3}q) \quad (5)$$

where $\operatorname{sinc} x = \frac{\sin x}{x}$

The contribution from the two triangular regions on the left and right of the central hexagon, denoted $U_{t\ell}(p,q)$ respectively, may be expressed as:

$$U_{t\ell}(p,q) + U_{tr}(p,q) = C \int_{-s}^{-s/2} \int_{-\sqrt{3}\xi - \sqrt{3}s}^{\sqrt{3}\xi + \sqrt{3}s} d\xi d\eta e^{-ik(p\xi + q\eta)} + C \int_{s/2}^s \int_{\sqrt{3}\xi - \sqrt{3}s}^{-\sqrt{3}\xi + \sqrt{3}s} d\xi d\eta e^{-ik(p\xi + q\eta)} \quad (6)$$

Since the pupil function for the two triangular regions is such that $G(\xi, \eta) = G(-\xi, \eta)$ we can evaluate the contribution from the two triangular regions by computing twice the real part of the second integral in Equation (8). Performing the η integration, substituting the proper limit, and taking the real part of the resultant yields

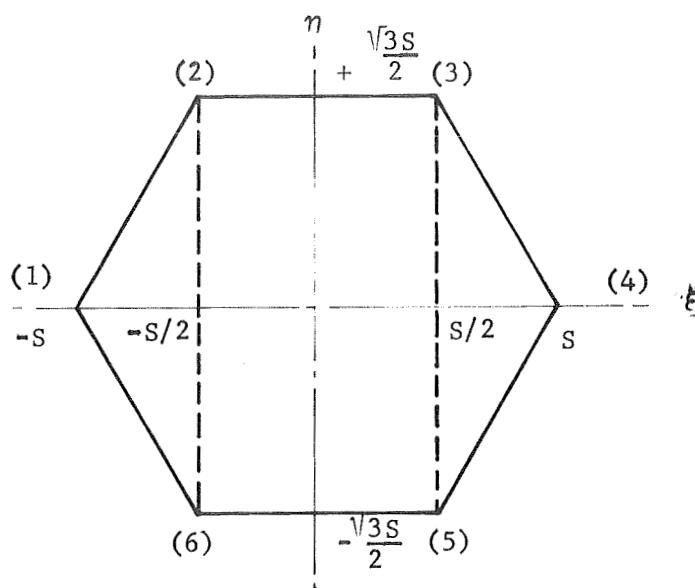


Figure 263. Central Hexagon Nomenclature

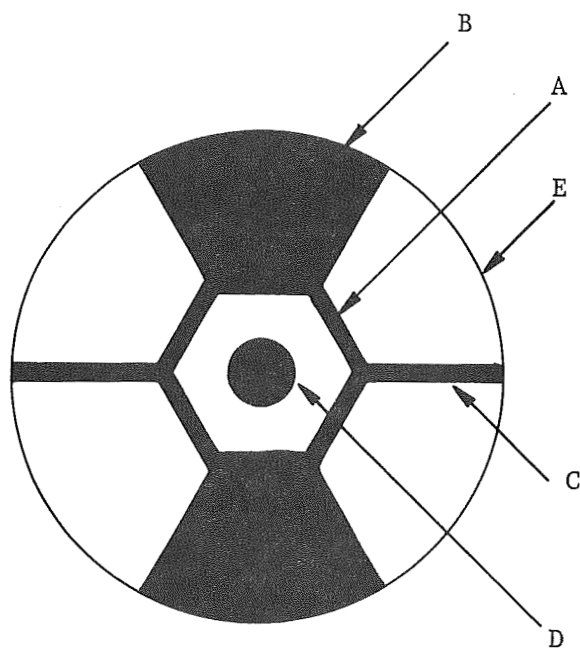


Figure 264. Nomenclature for Aperture with 5-Segments

$$U_{tl}(p, q) + U_{tr}(p, q) = \frac{-4\sqrt{3}C}{k^2(3q^2 - p^2)} \left[\cos\left(\frac{kps}{2}\right) \cos\left(\frac{\sqrt{3}sqk}{2}\right) - \right. \\ \left. - \frac{p}{\sqrt{3}q} \sin\left(\frac{kps}{2}\right) \sin\left(\frac{\sqrt{3}sqk}{20}\right) - \cos(kps) \right] \quad (7)$$

The electric field distribution in the Fraunhofer diffraction pattern of a single hexagon aperture is obtained by the following summation

$$U_H(p, q, s) = U_r(p, q) + U_{tl}(p, q) + U_{tr}(p, q) \quad (8)$$

The effect of the two missing segments on the Fraunhofer distribution (Area B, Figure 264) is obtained by inserting proper limits into the Fraunhofer diffraction integral (Equation 1). Because of the circular symmetry, the Fraunhofer diffraction of the two missing mirror segments will be derived in polar coordinates. The general equation for Fraunhofer diffraction in polar coordinates is readily derived as

$$U(w, \psi) = \int_0^{2\pi} \int_0^\infty G(p, \theta) e^{-ik\rho w \cos(\theta - \psi)} \rho d\rho d\theta \quad (9)$$

where

$$\begin{aligned} \rho \cos \theta &= \xi \\ \rho \sin \theta &= \eta \\ G(p, \theta) &= \text{constant } C \text{ at points in aperture} \\ &= 0 \text{ at points outside aperture} \end{aligned}$$

By inspection, the Fraunhofer diffraction integrals for the upper segment and lower segment are:

$$U_{us}(w, \psi) = \int_{-\pi/3}^{2\pi/3} \int_{\frac{\sqrt{3}s}{2\cos(\theta - \pi/2)}}^R e^{-ik\rho w \cos(\theta - \psi)} \rho d\rho d\theta \quad (10)$$

$$U_{ls}(w, \psi) = \int_{4\pi/3}^{5\pi/3} \int_{\frac{\sqrt{3}s}{2\cos(\theta - \pi/2)}}^R e^{-ik\rho w \cos(\theta - \psi)} \rho d\rho d\theta \quad (11)$$

Rotating the reference frame of Equations (10) and (11) by $\pi/2$ and $3\pi/2$, respectively, to obtain symmetrical limits on the θ integration and performing the ρ integration.

$$U_{us}(w, \psi) = \int_{-\pi/6}^{\pi/6} d\theta' \frac{e^{-ik\rho w \cos(\theta' - \pi/2 - \psi)}}{[-ikw \cos(\theta' - \pi/2 - \psi)]^2} [-ik\rho w \cos(\theta' + \pi/2 - \psi) - 1] \quad (12)$$

$\begin{array}{c} R \\ \downarrow \\ \rho = \frac{\sqrt{3}s}{2\cos\theta'} \end{array}$

$$U_{ls}(w, \psi) = \int_{-\pi/6}^{+\pi/6} d\theta'' \frac{e^{-ik\rho w \cos(\theta'' + 3\pi/2 - \psi)}}{[-ikw \cos(\theta'' + 3\pi/2 - \psi)]^2} [-ik\rho w \cos(\theta'' + 3\pi/2 - \psi) - 1] \quad (13)$$

$\begin{array}{c} R \\ \downarrow \\ \rho = \frac{\sqrt{3}s}{2\cos\theta''} \end{array}$

where

$$\begin{aligned} \theta' &= \theta - \pi/2 \\ \theta'' &= \theta - 3\pi/2 \end{aligned}$$

$$\int_A^B e^{\alpha x} x dx = \frac{e^{\alpha x}}{\alpha^2} [\alpha x - 1] \Big|_A^B \quad (13a)$$

Recalling the trigonometric identities

$$\begin{aligned} \cos(\alpha + \pi/2) &= -\sin\alpha \\ \cos(\alpha + 3\pi/2) &= \sin\alpha \end{aligned} \quad (14)$$

Equations (12) and (13) may be simplified. The sum of the Fraunhofer diffraction integrals gives the net electric field resulting from the two apertures* (Babinet's Principle);

$$U_{us}(w, \psi) + U_{ls}(w, \psi) = \int_{-\pi/6}^{\pi/6} d\theta \frac{e^{ik\rho w \sin(\theta - \psi)}}{[k^2 w^2 \sin^2(\theta - \psi)]^{-1}} [-ik\rho w \sin(\theta - \psi) - 1] +$$

$\begin{array}{c} R \\ \downarrow \\ \rho = \frac{\sqrt{3}s}{2\cos\theta} \end{array}$

Recalling the exponential relations

$$\sin \alpha = \frac{e^{i\alpha} - e^{-i\alpha}}{2i}; \quad \cos \alpha = \frac{e^{i\alpha} + e^{-i\alpha}}{2} \quad (16)$$

*The dummy variables θ' and θ'' are contracted to θ for the ensuing discussion.

Equation (15) may be regrouped into the following simplified form

$$U_{us}(w, \psi) + U_{ls}(w, \psi) = \left| 2 \int_{-\pi/6}^{\pi/6} d\theta \rho^2 \text{sinc} \{k\rho w \sin(\theta - \psi)\} \right| + \left. 2 \int_{-\pi/6}^{\pi/6} d\theta \rho^2 \frac{\cos \{k\rho w \sin(\theta - \psi)\}}{[k\rho w \sin(\theta - \psi)]^2} \right|_{\rho = \frac{\sqrt{3}s}{2\cos\theta}}^R \quad (17)$$

For the special case at the center of the Fraunhofer field ($w = 0$)

$$U_{us}(0, \psi) + U_{ls}(0, \psi) = 2 \int_{-\pi/6}^{\pi/6} d\theta \left\{ R^2 - \frac{3s^2}{4\cos^2\theta} \right\} + 2 \int_{-\pi/6}^{\pi/6} d\theta \lim_{w \rightarrow 0} \frac{\cos(kRw \sin(\theta - \psi)) - \cos\left(\frac{ks\sqrt{3}}{2\cos\theta} w \sin(\theta - \psi)\right)}{k^2 w^2 \sin^2(\theta - \psi)} \quad (18)$$

the limit of the kernel of the second integral is evaluated via l'Hopital's Rule with the result

$$U_{us}(0, \psi) + U_{ls}(0, \psi) = 2 \int_{-\pi/6}^{\pi/6} d\theta \left\{ R^2 - \frac{3s^2}{4\cos^2\theta} \right\} + 2 \int_{-\pi/6}^{\pi/6} d\theta \lim_{w \rightarrow 0} \left\{ \frac{-R^2 \sin(kRw \sin(\theta - \psi))}{2wks \sin(\theta - \psi) R} \right\} + \frac{\left(\frac{13s}{2\cos\theta}\right)^2 \sin \frac{ks\sqrt{3}}{2\cos\theta} w \sin(\theta - \psi)}{\frac{s\sqrt{3}}{2\cos\theta} \times 2 wks \sin(\theta - \psi)} \quad (19)$$

Noting the limit

$$\lim_{x \rightarrow 0} \text{sinc}(x) = 1, \quad (20)$$

the Fraunhofer integral at $w = 0$ is

$$U_{us}(w, \psi) + U_{ls}(w, \psi) = 2 \int_{-\pi/6}^{\pi/6} d\theta \left\{ R^2 - \frac{3s^2}{4\cos^2\theta} - \frac{R^2}{2} + \frac{3s^2}{8\cos\theta} \right\} \quad (21a)$$

$$= 2(R^2\theta - \frac{3s^2}{4} \tan\theta) \Big|_0^{\pi/6} \quad (21b)$$

$$= \frac{\pi R^2}{3} - \frac{\sqrt{3}s^2}{2} \quad (21c)$$

An effective check on the validity of all previous mathematics is obtained by noting that Equation (21c) gives the area of the two truncated pie-shaped segments, as expected from the basic Fraunhofer equation and Equation (3).

The effect of the two horizontal segment separations (Area C of Figure 264) is derived by substituting the following limits on Equation 4.

$$U_s(p, q) = C \int_{-\ell/2}^{\ell/2} \int_{\Xi}^{P_1} d\xi d\eta e^{-ik(p\xi + q\eta)} + C \int_{-\ell/2}^{\ell/2} \int_{-\Xi}^{-R} d\xi d\eta e^{-ik(p\xi + q\eta)} \quad (22)$$

where $\Xi = s + \frac{2\ell}{\sqrt{3}}$

Using the techniques of previous derivation, the Fraunhofer contribution of the two horizontal segment separations is

$$U_s(p, q) = 2c(R - \Xi) \ell \text{sinc}\left(\frac{k\ell q}{2}\right) \sin\left(\frac{k(R - \Xi)p}{2}\right) \cos(kp(R - \Xi/2)) \quad (23)$$

The last two contributions to the Fraunhofer patterns are from the circular periphery and the circular central obscuration which is defined by the size of the secondary mirror and/or the size of the circular hole in the primary mirror. Placing the proper limits on Equation (9) and applying the standard integrals of the Bessel Function*, the Fraunhofer distribution for the periphery is

$$U_R = C\pi R^2 \frac{2J_1(kRw)}{kRw} \quad (24)$$

where $J_1 =$ first order Bessel function

* Born, M., and Wolfe, E.: Principles of Optics. Pergamon Press, Limited, London, England, 1959, p. 386.

By analogy, the contribution of the central obscuration of radius a is

$$U_a = \pi a^2 \frac{2J_1(kaw)}{kaw} \quad (25)$$

The Fraunhofer intensity distribution from the modified hexagonal primary mirror with two missing (or misaligned) segments will now be calculated. Repeated use of Babinet's principle will be utilized in the following Fraunhofer summation. For example, the effect of the central hexagonal obscuration (A, Figure 264) is obtained by subtracting the Fraunhofer integral of the hexagon (Equation (8)) with side $s' = s + 2\ell/\sqrt{3}$ from the same integral with side s . The intensity of the aperture of Figure 262b is thus

$$I = K |U_R + U_H(p, q, s) - U_H(p, q, s') - U_s - U_a|^2 \quad (26)$$

The normalization factor K was arbitrarily defined as

$$K = [\pi(R^2 - a^2) - 2(R - \frac{1}{2}\ell)]^{-2} \quad (27)$$

This definition of K forces the central maximum of the point-spread function of a circular aperture with both mirror segment separations and central obscuration to have a value of 1.0. This permits direct calculation of the loss in relative intensity for the superresolution techniques discussed.

ANALYSIS OF APERTURE OCCULTED BY TWO DISPLACED SYMMETRIC DISKS

The apodization scheme discussed is shown in Figure 262a. Substituting alternate limits on Equation (10), the Fraunhofer diffraction integral for the area A of Figure 265 is

$$U_{uc}(w, \psi) = \int_{\theta_1}^{\theta_2} \int_{\rho_{min}}^R e^{-ik\rho w \cos(\theta - \psi)} \rho d\rho d\theta \quad (28)$$

Performing the co-ordinate rotation and preliminary integration as required by Equation (12) thru (17), the Fraunhofer diffraction integral may be written by inspection

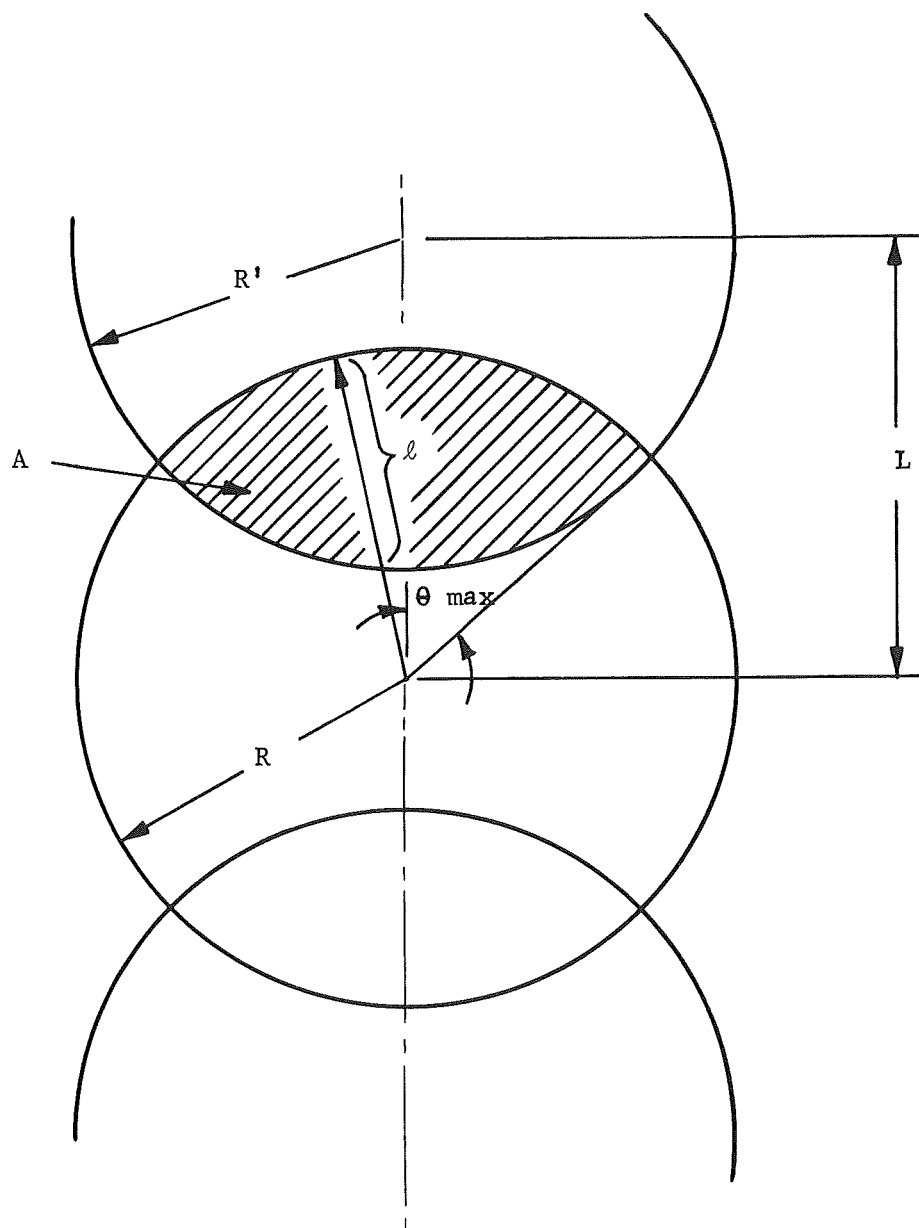


Figure 265. Nomenclature for Circular Pupil Obscured by Symmetric Discs

$$U_{uc}(w, \psi) + U_{lc}(w, \psi) = \left| 2 \int_{-\theta_{\max}}^{\theta_{\max}} d\theta \rho^2 \operatorname{sinc} \{k\rho w \sin(\theta - \psi)\} + \right. \\ \left. 2 \int_{-\theta_{\max}}^{\theta_{\max}} d\theta \frac{\rho^2 \cos \{k\rho w \sin(\theta - \psi)\}}{[k\rho w \sin(\theta - \psi)]^2} \right| \begin{matrix} R \\ \rho_{\min} \end{matrix} \quad (29)$$

The tasks of deriving functions for $\rho_{\min}$ and $\theta_{\max}$ remain, however. The expression for $\rho_{\min}$ will be derived first. The geometry of the rotated reference frame ($\theta \equiv \theta'$) is shown in Figure 266. Applying the cosine law to triangle ABD,

$$\cos \theta = \frac{\rho_{\min}^2 + L^2 - R^2}{2\rho_{\min} L} \quad (30)$$

Solving the resulting quadratic equation for $\rho_{\min}$

$$\rho_{\min} = L \cos \theta - \sqrt{L^2 (\cos^2 \theta - 1) + R^2} \quad (31)$$

The value $\theta_{\max}$ is determined by setting $\rho_{\min} = R$ in Equation (31) and solving for $\theta_{\max}$ with the following result

$$\theta_{\max} = \cos^{-1} \left(\frac{R^2 + L^2 - R^2}{2RL} \right) \quad (32)$$

The far field intensity distribution for the aperture of Figure (3) is obtained with the following operation

$$I = K' |U_R - U_{uc} - U_{lc} - U_a|^2$$

where $K' = [\pi(R^2 - a^2)]^{-2} \quad (33)$

ANALYSIS OF SIX-SEGMENT MIRROR WITH TWO OPAQUE SECTIONS

The Fraunhofer diffraction integral of the two truncated "pie"-shaped opaque segments in the centrally obscured aperture in Figure 267 is written by inspection of Equation (17)

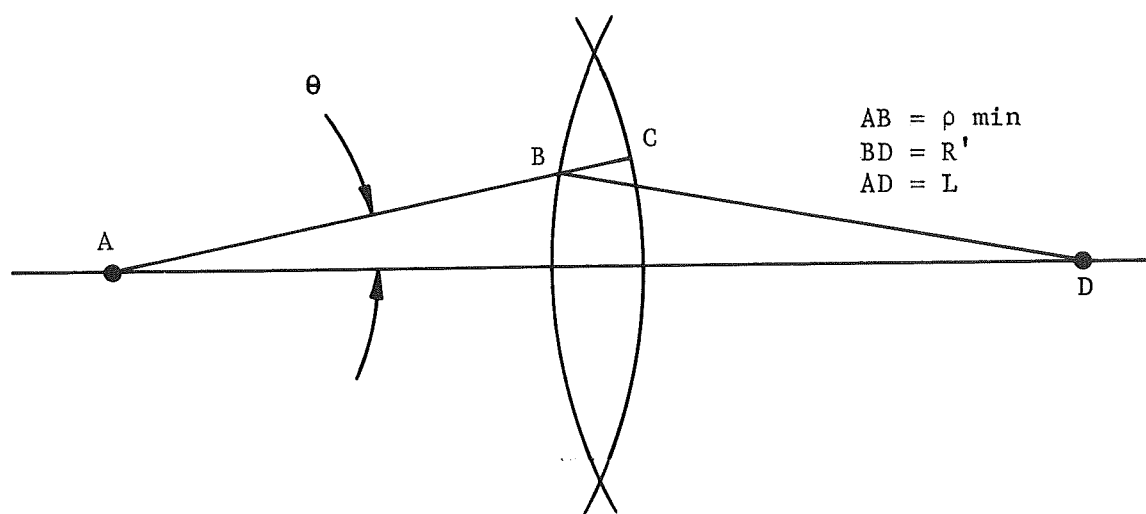


Figure 266. Geometry for Symmetrical Overlapping Disks

$$U_p = \left| 2 \int_{-\pi/6}^{\pi/6} d\theta \rho^2 \left\{ \text{sinc} \left(k \rho w \sin(\theta - \psi) \right) + \frac{\cos(k \rho w \sin(\theta - \psi))}{(k \rho w \sin(\theta - \psi))^2} \right\} \right|_{\rho=a}^{\rho=R} \quad (34)$$

The image intensity distribution for the aperture in Figure 267 is

$$I_p = K'' \left| U_R - U_a - U_p \right|^2 \quad (35)$$

where $K'' = [\pi(R^2 - a^2)]^{-2}$

The special case for the aperture in Figure 267 with no central obscuration is accommodated by substituting $a = 0$ into Equation (34).

EXPERIMENTAL RESULTS FOR SUPERRESOLUTION COMPARISON

Isophote plots together with isometric views of one quadrant of the superresolution images are presented in Figure 268 to 276. The Fraunhofer integral was solved by an SDS 9300 computer for the intensity at a point in Cartesian image space. This value was converted to a number pair $P(x,y)$

$$P(x,y) = 100 - 10 \log_{10} (I(x,y) / I(0,0)) \quad (36)$$

Thus the resultant isophote plot will give the image intensity in terms of decibels (dB), where 100 (contracted to 00 in the computer plot) is the maximum intensity; 90 is 10^{-1} . $I(0,0)$, 80 is 10^{-2} . $I(0,0)$ and so forth. Each isophote of the figures represents a locus of equal intensity where $I_n = 10^{-n} \times I(0,0)$, $n = 0, 1, 2, \dots$

The physical dimensions chosen for the pupils were obtained from the preliminary design of the OTES.* Wherever possible, variables such as wavelength and diameter were normalized to permit application of the resulting experimental results to various systems with minimal effort (Table 32). The scale of w is given for the isophote plots for dimensional reference.

* Wischnia, H.F.: Optical Technology Apollo Extension System (OTES) Phase A Study. Perkin-Elmer Engineering Report No. 8900, October 1967.

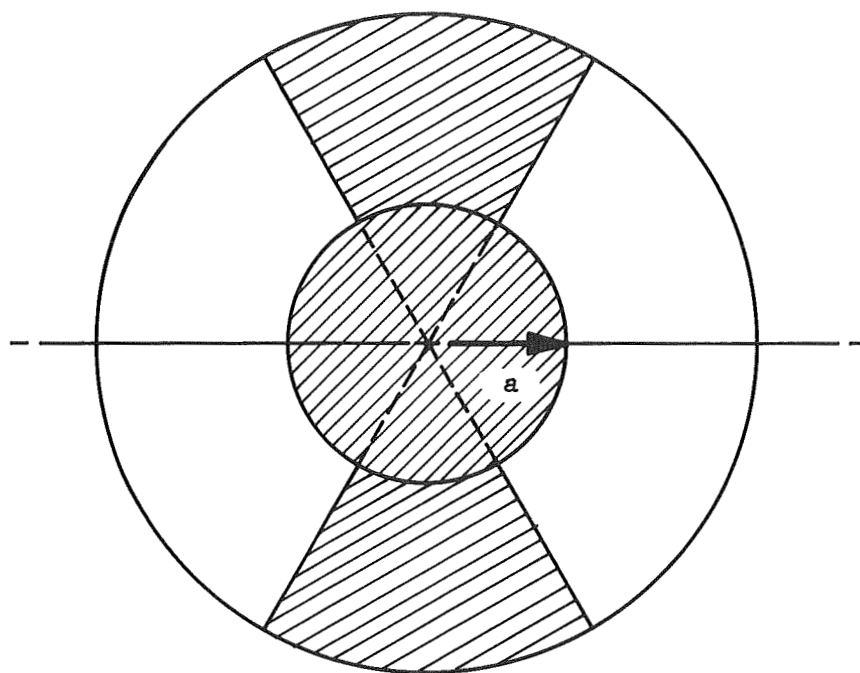


Figure 267. Four-segment Pupil with Central Obscuration

TABLE 32
LIST OF VALUES FOR SUPERRESOLUTION
ISOPHOTE PLOTS

<u>Parameter</u>	<u>Variable</u>	<u>Units</u>	<u>Value</u>
Wavelength	λ	m	1
Separation of Disk Center	L	m	$0 < L < R + R'$
Radius of Pupil	R	m	1
Radius of Occulting Disk R'	R'	m	$0 < R' < \infty$
Radius of Central Obscuration	A	m	0.2
Length of "Spoke" in Figure	H	m	$R - S - \frac{2\sqrt{3}\ell}{3}$
Length of Side of Central Hexagon	S	m	0.5
Separation of Mirror Segments	ℓ	m	0.001

Figure 268 represents the unobscured circular segmented aperture. As discussed in OTES Preliminary Report,* the effect of the separation between segments has negligible effect on the point spread function.

Figure 269 represents the above aperture with the addition of a properly scaled central obscuration. In general, the introduction of the central obscuration reduces the peak intensity and reduces the radius of the first dark ring.** Thus the central obscuration has some superresolution tendencies. The reduction of intensity between Figures 268 and 269 is not evident because of the normalization technique utilized (Equation (27)). All remaining isophote plots will be normalized with respect to Figure 269.

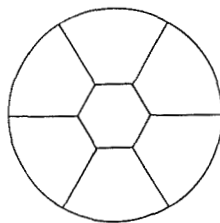
Figures 270 and 271 represent the point spread of the seven segment primary mirror with two segments removed. To isolate the effect of the central obscuration, isophotes for both the obscured and unobscured apertures were plotted. Comparison of these plots demonstrate that the introduction of the central obscuration will increase the energy in the first ring with slight changes in the central intensity and position of first dark ring.

* Wischnia, H.F.: Optical Technology Apollo Extension System. Preliminary Report, Perkin-Elmer Engineering Report No. 8500.

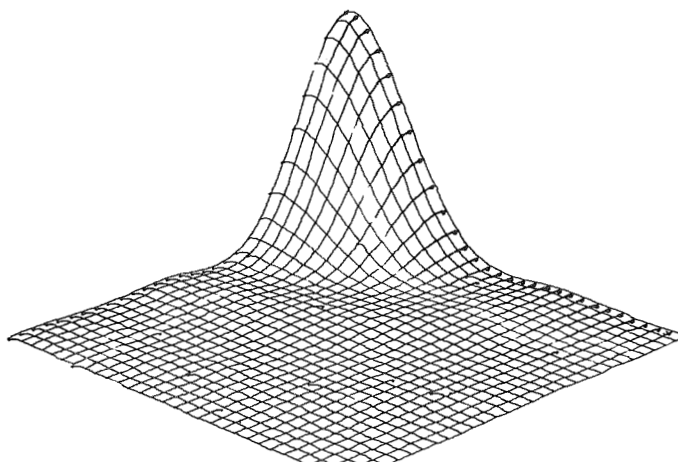
** Born, M., and Wolfe, E.: Loc. Cit.

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ISOPHOTES

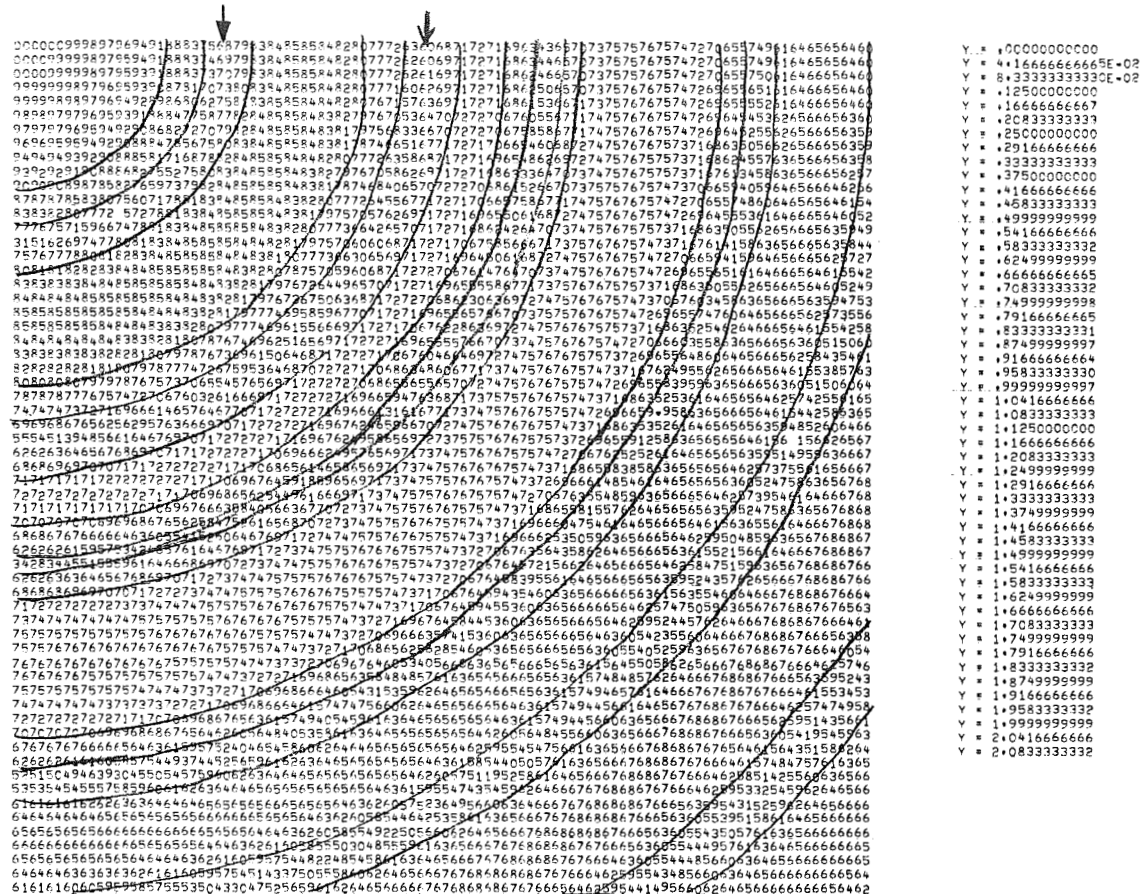


PUPIL



ISOMETRIC

Figure 268. Non-Obscured 7 - Segment Primary Mirror

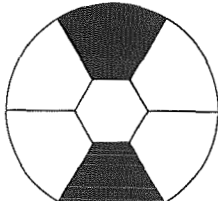
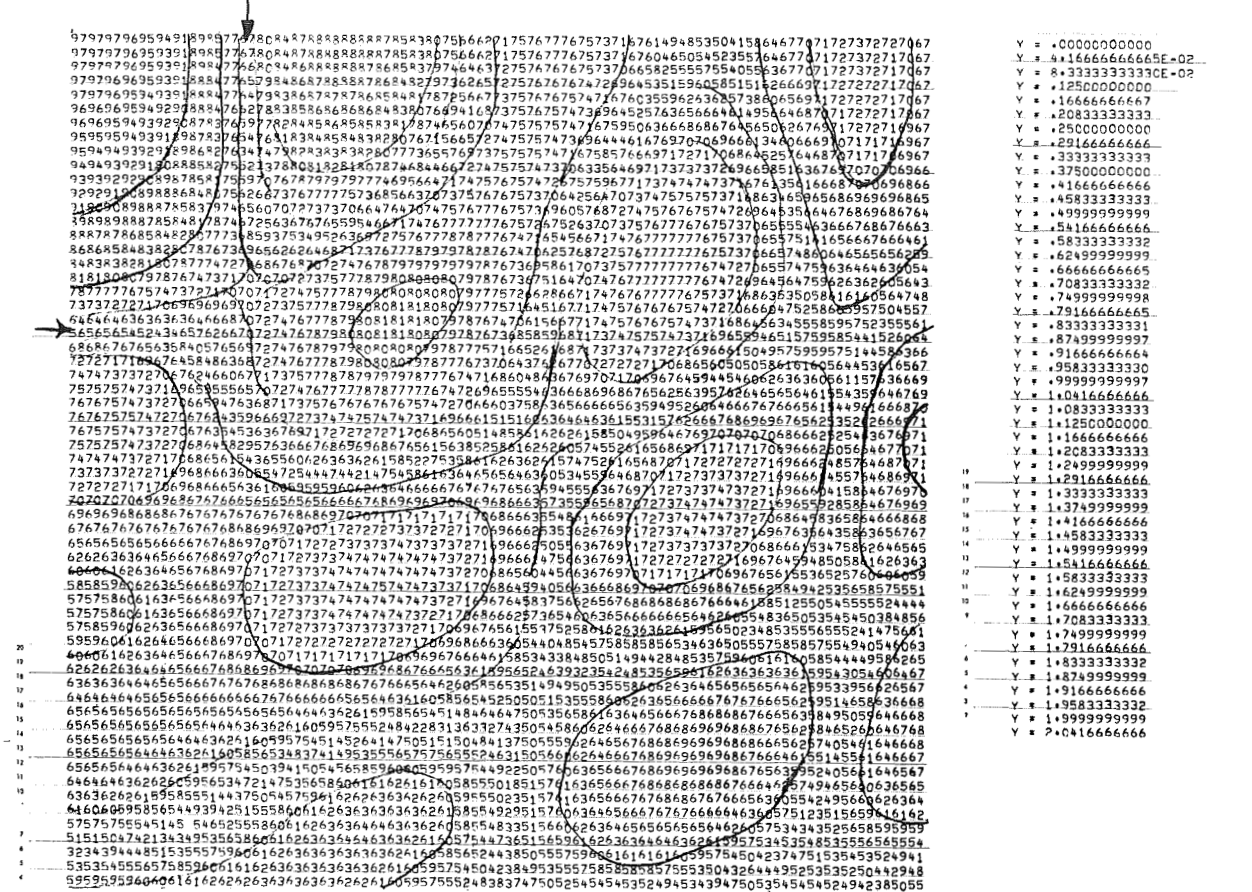


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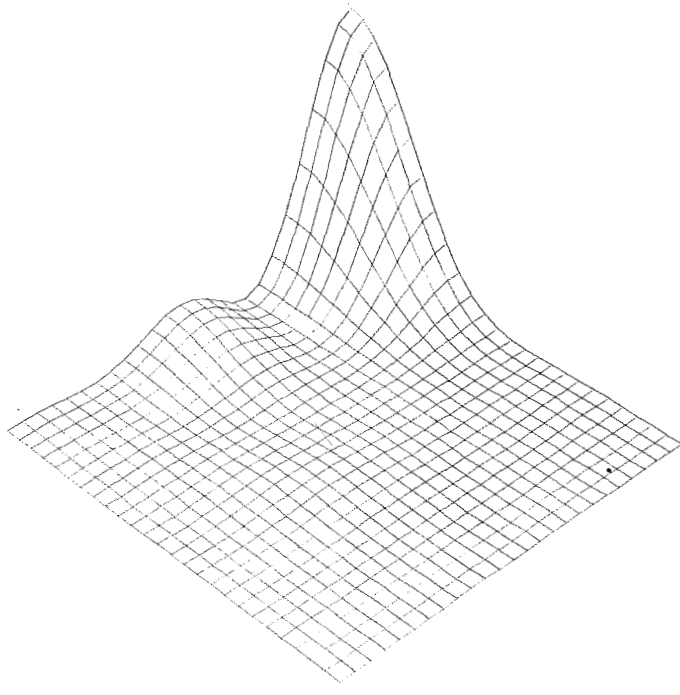
PUPIL

ISOMETRIC

Figure 269. Centrally Obscured 7-Segment Primary Mirror



PUPIL

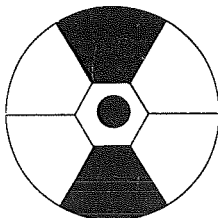


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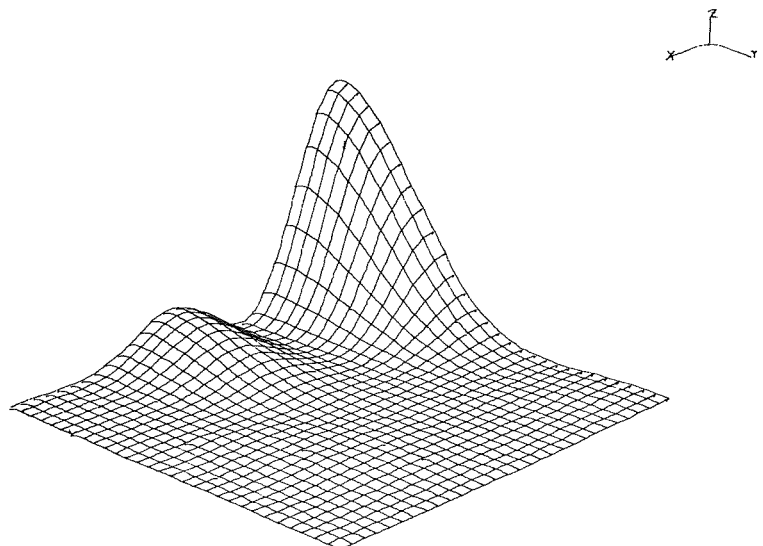
Figure 270. 7-Segment Mirror with 2 Opposing Segments Removed

[illegible]

ISOPHOTES



PUPIL



ISOMETRIC

Figure 271. 7-Segment Mirror with Central Obscuration and 2 Opposing Segments Removed

The comparison of 6-segment verses 7-segment mirrors has been contemplated during the progress of this program. In support of this comparison, Figures 272 and 273 were plotted to determine the point spread of the obscured and unobscured 6-segment apertures. Examining the point-spread functions, there is a striking similarity between all images generated by 6- and 7- segment mirrors. Thus, from the standpoint of imagery, the 6- and 7- segment mirrors are nearly equivalent.

The point spread obtained by using the Wilkins phase screen* is shown in Figures 272, and 275. As before, the unobscured aperture is included for reference. The value of ϕ' represents the phase delay (advance) expected from the phase screen being positioned in a pupil. Since one has double path considerations when light is reflected from a mirror, a shift of the central segment by $\lambda/4$ is equivalent to a phase change $\phi' = \pi$. Figures 274 and 275 demonstrate that the Wilkins screen technique gives superresolution comparable to that of the techniques in Figures 270 to 273, but with two distinct advantages:

- 1) The image is symmetrical which allows one to utilize the superresolution gain in all radial directions and not just in two opposite positions and,
- 2) The problem of tilting two opposing segments such that they do not reflect light from other light sources into the area of the image plane is obviated.

An additional advantage is demonstrated in Figure 276. By varying ϕ' , the position of the first dark ring can be positioned at any a priori point in the region $0.7 \rightarrow 1$ where 1 represents the radius of the unperturbed aperture. The special case of $\phi' = \pi/4$ was plotted in Figure 276.

As mentioned in the introduction, Dr. Fredericks has discussed the superresolution technique of two opposing circular disks. To generate a basis for comparing the alternate superresolution techniques, the point spread for the two-obscuration disk case was also plotted. Figure 277 gives the relationship for the central intensity and position of the first dark ring as the disk separation is varied. This curve graphically shows the catastrophic reduction in intensity for the central maximum as one takes the concept of superresolution to point sources much closer than λ/D . As two representative points on this curve, Figures 278 and 279 were plotted for values of $L = 1.3$ and $L = 1.6$, respectively. The utilization of this technique for the LTEP is doubtful because of the requirement for extra hardware at one of the relay pupils.

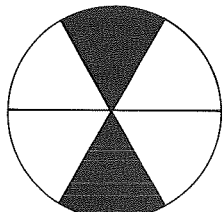
The position of the minor and major axes of all the superresolution pupils analyzed in this chapter is shown in Figure 280 as a bar chart. Note that a number of techniques will yield a significant reduction in the minor radius of the first dark ring. The easiest technique to implement and thus the preferred approach is the Wilkins screen.

Before we discuss advantages/disadvantages of the techniques discussed, the often used (and overworked) term "resolution" should be examined in greater detail. The commonly used Rayleigh criterion for resolution states that two point sources are resolved if the maximum illuminance of one image falls on the first minimum of the second image. This rule is a completely arbitrary

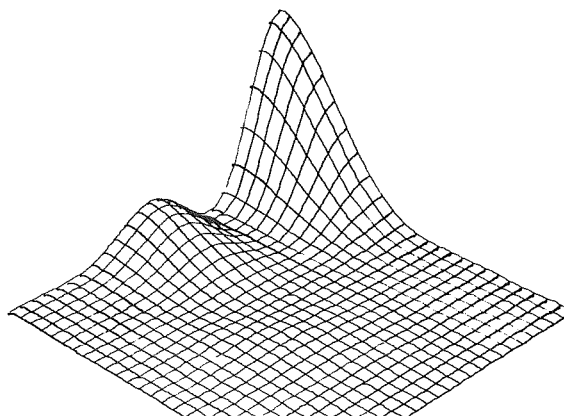
* Wilkins, J.E.: Journal of Opt. Soc. Amer., Vol. 40, No. 4, 1950, pp. 222-225.

Y = 000000000000
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Y = 333333333333E-02
Y = 125000000000
Y = 1.666666666667
Y = 208333333333
Y = 250000000000
Y = 291666666666
Y = 333333333333
Y = 375000000000
Y = 416666666666
Y = 458333333333
Y = 499999999999
Y = 541666666666
Y = 583333333333
Y = 624999999999
Y = 666666666665
Y = 708333333332
Y = 749999999998
Y = 791666666665
Y = 833333333331
Y = 874999999997
Y = 916666666666
Y = 958333333330
Y = 999999999996
Y = 1.041666666666
Y = 1.08333333333
Y = 1.125000000000
Y = 1.166666666666
Y = 1.208333333333
Y = 1.249999999999
Y = 1.291666666666
Y = 1.333333333333
Y = 1.374999999999
Y = 1.416666666666
Y = 1.458333333333
Y = 1.499999999999
Y = 1.541666666666
Y = 1.583333333333
Y = 1.624999999999
Y = 1.666666666666
Y = 1.708333333333
Y = 1.749999999998
Y = 1.791666666665
Y = 1.833333333332
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Y = 1.958333333332
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ISOPHOTES



PUPIL

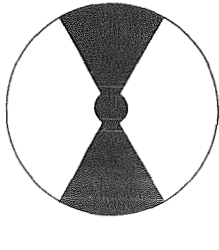


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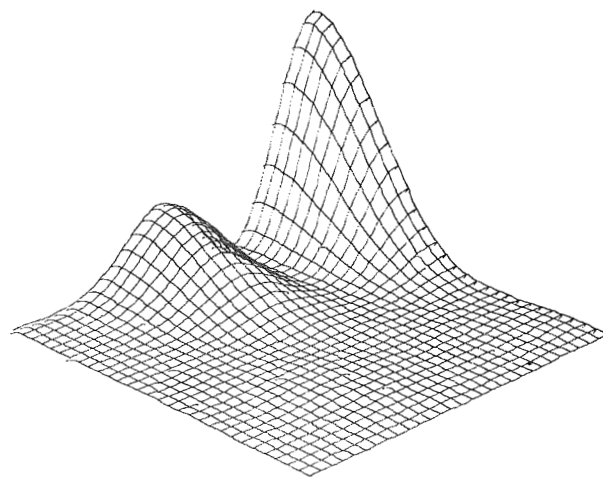
Figure 272 . 6-Segment Mirror with 2 Opposing Segments Removed

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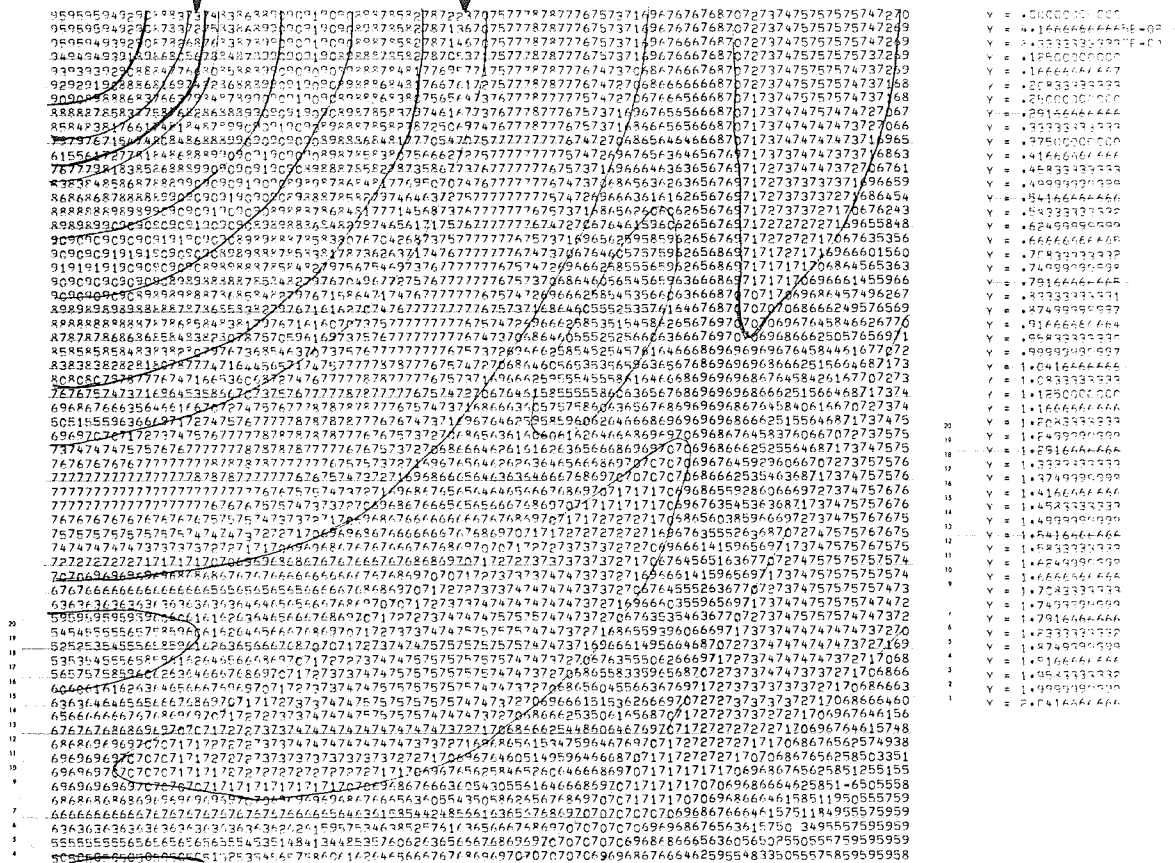


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Figure 273. 6-Segment Mirror with Central Obscuration and 2 Opposing Segments Removed

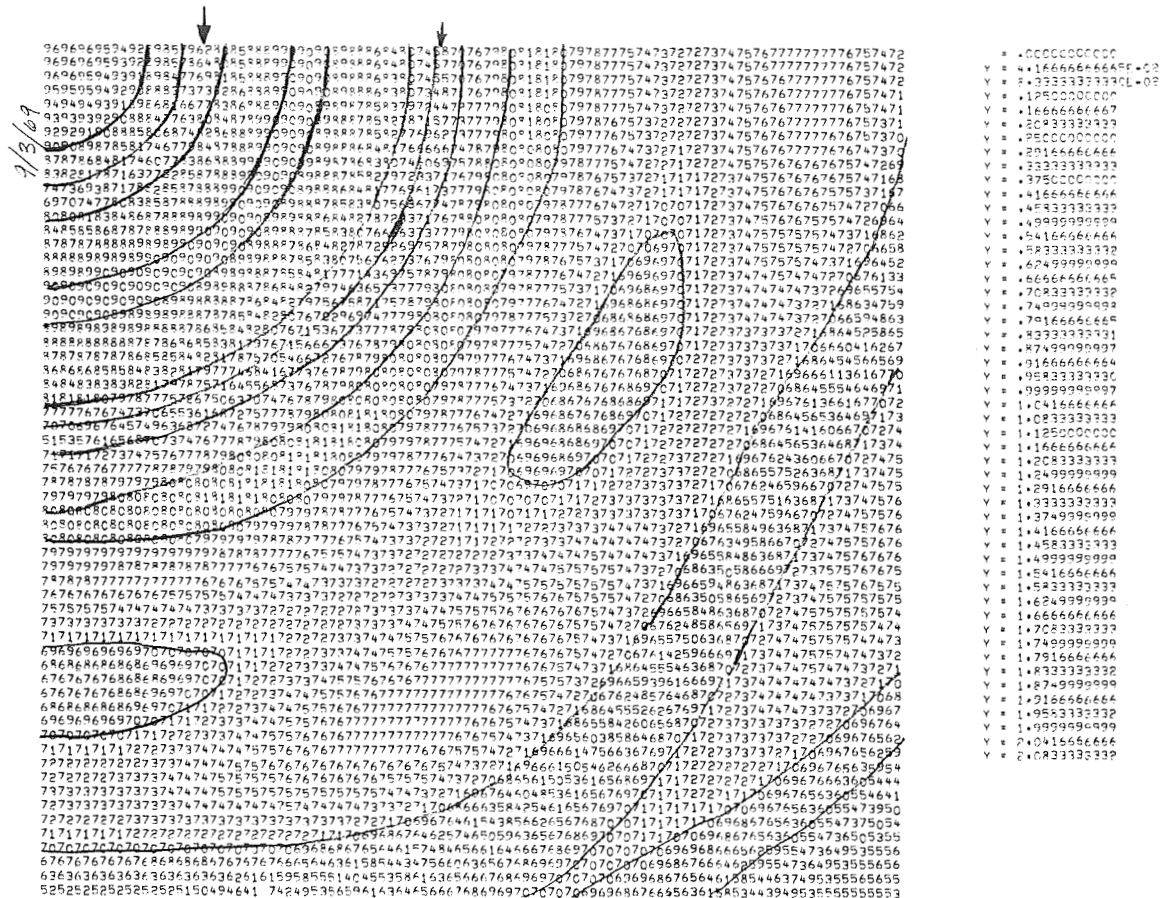


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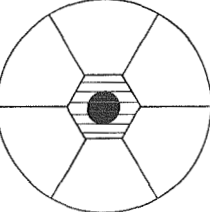
PUPIL

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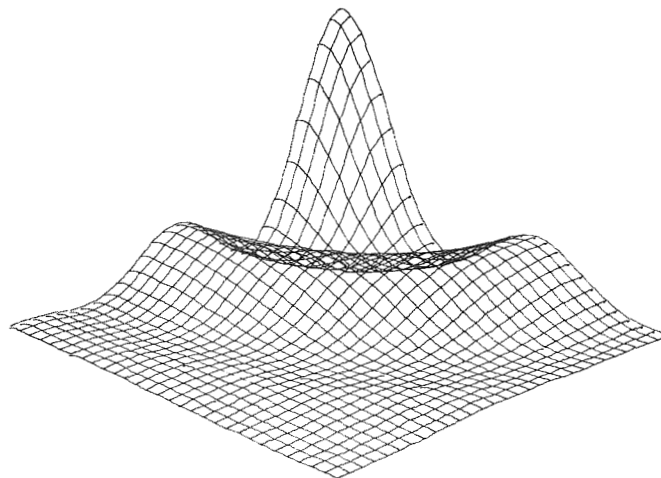
Figure 274. 7-Segment Mirror Simulating the Wilkins Screen



ISOPHOTES

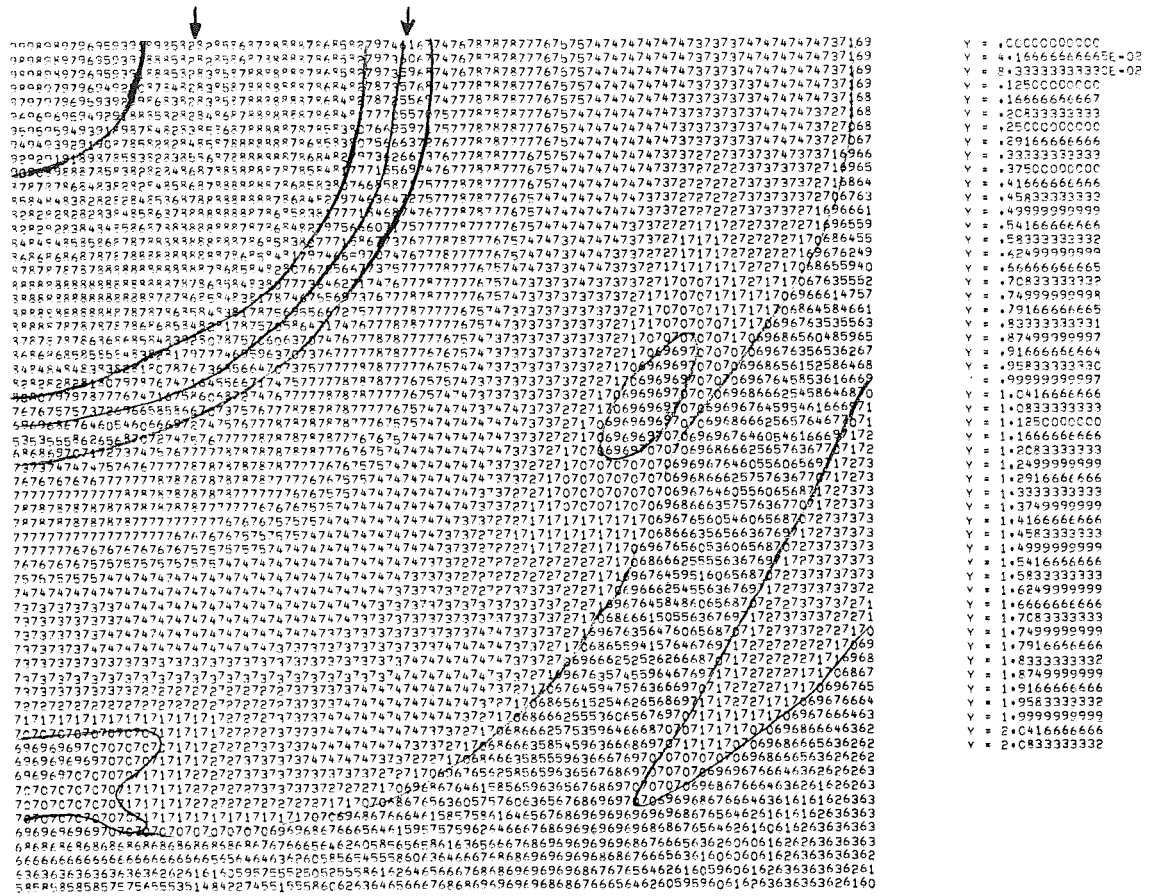


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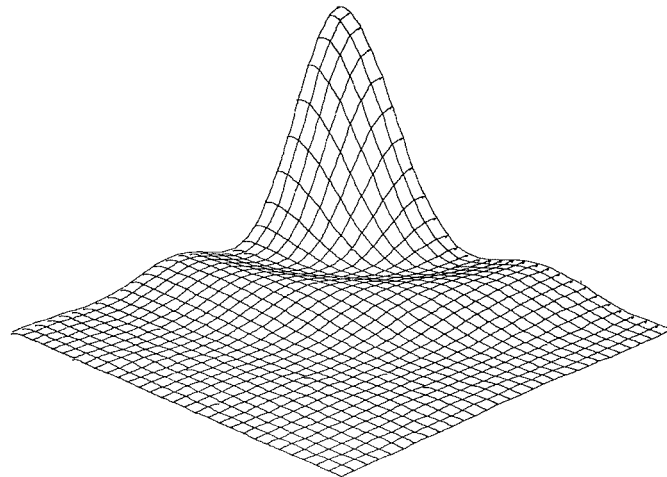
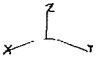


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Figure 275. Centrally Obscured 7-Segment Mirror Simulating the Wilkins Screen



ISOPHOTES



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Figure 276. 7-Segment Mirror with Central Obscuration displaced by $\pi/4$

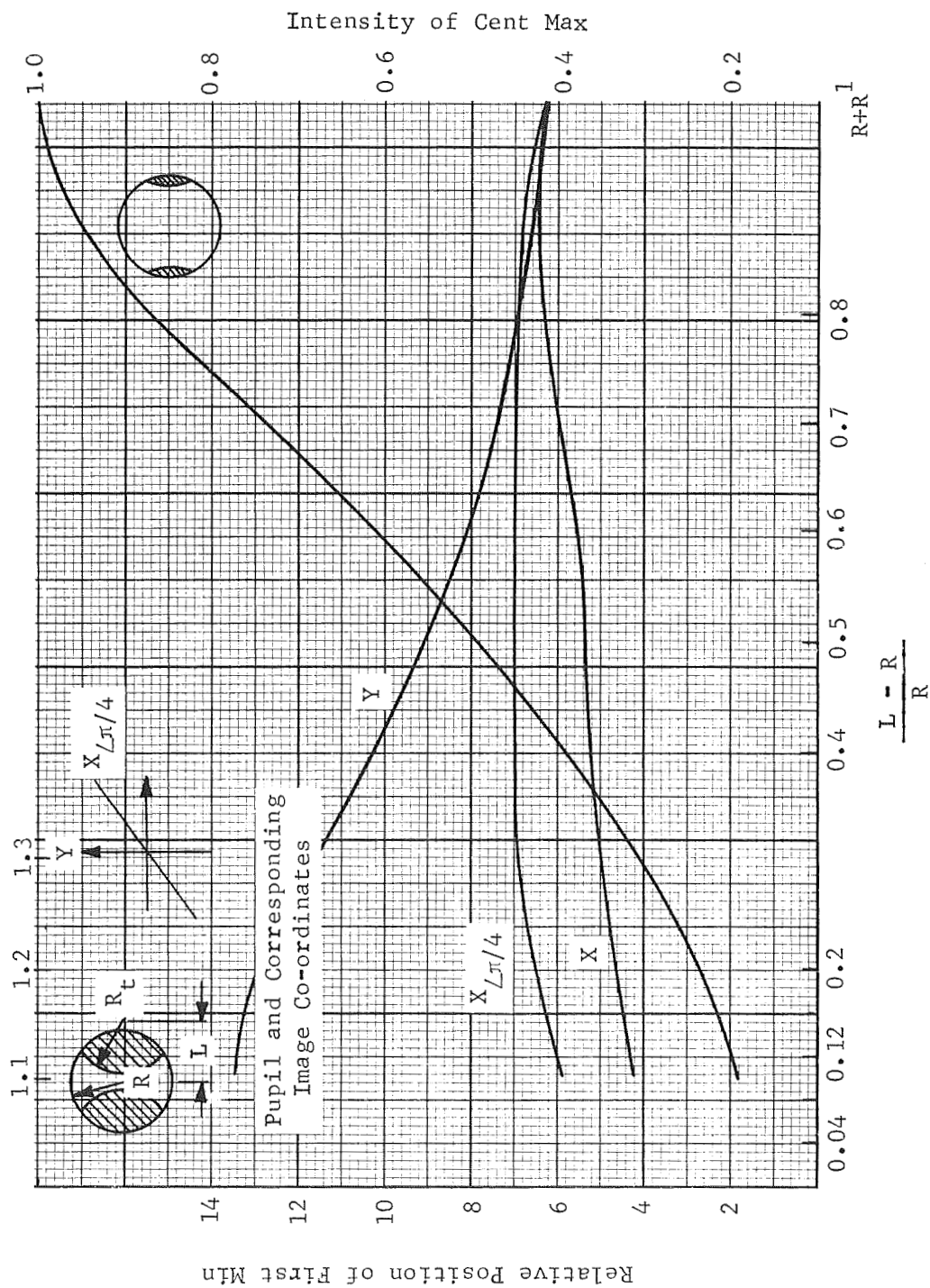
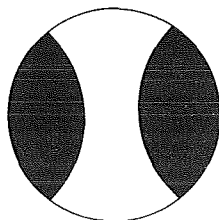


Figure 277 . First Zero and Central Intensity for Aperture Obscured by Symmetric Disks

[illegible]

Y = 1.0000000000000000
Y = 1.1666666666666666
Y = 1.3333333333333333
Y = 1.5000000000000000
Y = 1.6666666666666666
Y = 1.8333333333333333
Y = 2.0000000000000000
Y = 2.1666666666666666
Y = 2.3333333333333333
Y = 2.5000000000000000
Y = 2.6666666666666666
Y = 2.8333333333333333
Y = 3.0000000000000000
Y = 3.1666666666666666
Y = 3.3333333333333333
Y = 3.5000000000000000
Y = 3.6666666666666666
Y = 3.8333333333333333
Y = 4.0000000000000000
Y = 4.1666666666666666
Y = 4.3333333333333333
Y = 4.5000000000000000
Y = 4.6666666666666666
Y = 4.8333333333333333
Y = 5.0000000000000000
Y = 5.1666666666666666
Y = 5.3333333333333333
Y = 5.5000000000000000
Y = 5.6666666666666666
Y = 5.8333333333333333
Y = 6.0000000000000000
Y = 6.1666666666666666
Y = 6.3333333333333333
Y = 6.5000000000000000
Y = 6.6666666666666666
Y = 6.8333333333333333
Y = 7.0000000000000000
Y = 7.1666666666666666
Y = 7.3333333333333333
Y = 7.5000000000000000
Y = 7.6666666666666666
Y = 7.8333333333333333
Y = 8.0000000000000000
Y = 8.1666666666666666
Y = 8.3333333333333333
Y = 8.5000000000000000
Y = 8.6666666666666666
Y = 8.8333333333333333
Y = 9.0000000000000000
Y = 9.1666666666666666
Y = 9.3333333333333333
Y = 9.5000000000000000
Y = 9.6666666666666666
Y = 9.8333333333333333
Y = 10.0000000000000000



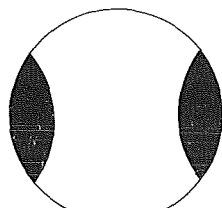
A 3D wireframe plot of a function. The surface features a prominent, sharp peak at the back center and a saddle point towards the front left. The surface is rendered with a grid of lines, showing its curvature and depth.

ISOMETRIC

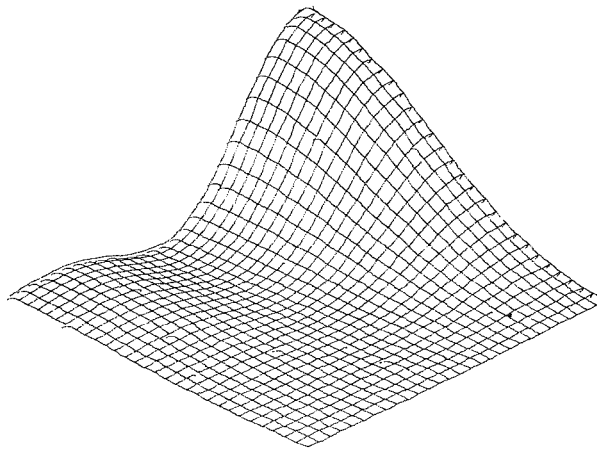
Figure 278 . Circular Aperture Obscured by Symmetric Disks
($L = 1.3$)

[illegible]

ISOPHOTES



PUPIL



ISOMETRIC

Figure 279. Circular Aperture Obscured by Symmetric Disks ($L = 1.6$)

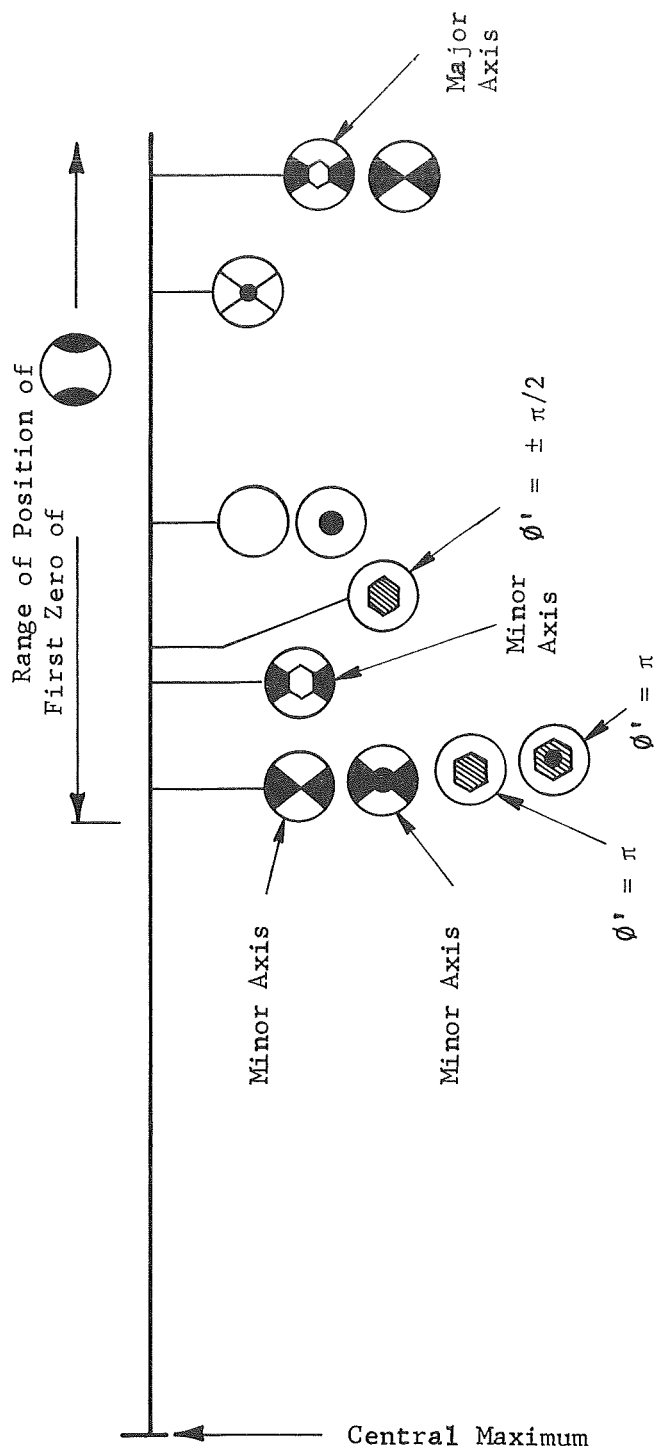


Figure 280. Relative Position of First Dark Ring for Superresolution Pupils.

definition and should not be considered as a "law of physics". Rayleigh himself emphasized this when he conceived it. Quoting him:^{*} "this rule is convenient on account of its simplicity and it is sufficiently accurate in view of the necessary uncertainty as to what is actually meant by resolution."

An alternate resolution criterion proposed by Sparrow^{**} states that two point sources are resolved when the second derivative of the total illuminance distribution vanishes on axis. The relative gain in resolution realized by the superresolution techniques previously discussed are realized when one considers either the Rayleigh or Sparrow resolution criteria.

The problem of resolving two point sources will now be considered in the unreal world of no noise. In papers which considered resolution in an entirely new perspective, Di Francia[†] considered the resolution problem from an information theory point of view. This new approach was required to answer some embarrassing dilemma. One of these is of extra interest to the resolution problem being considered. In a mathematical and noiseless sense, two objects can be distinguished one from the other at any separation as long as the spatial frequency spectra of the two objects are not everywhere identical within the passband of the optical system.^{††} This implies that with proper sensors and low noise, two-point resolution much better than $1.22\lambda/D$ is obtainable from any optical system which is near diffraction limited.

Another dilemma occurs when one considers the following question: "How is it possible to resolve point sources much closer than λ/D when the MTF theory states that the circular aperture has a spatial frequency cutoff of λ/D ?" The upper frequency cutoff applies only to the case where the available information is uniformly distributed over the whole object space. For the superresolution case, we are not interested in the entire object space, but only in a single image. The a priori knowledge in this case is tremendous. In fact, we know a priori all but one bit of information about the object space, the one bit of information being the single-binary star decision. The problem is how to maximize the probability of making the correct decision.

Aberrations of a diffraction-limited optical system never increase the system MTF.[#] Since all the superresolution techniques discussed in this report are actually aberrations in the strict sense of the word, we can say categorically that the MTF of all the superresolution systems are less than optimum. This has the consequence for the theoretical case of white gaussian noise that the signal-to-noise ratio of a binary double star decision is equal to the integral over all spatial frequencies of the product of the MTF and the difference

^{*} Lord Rayleigh: Collected Papers. Vol. 3, Cambridge University Press, Cambridge, 1902, p. 84.

^{**} Sparrow, C.: Astrophysics Journal. Vol. 44, 1916, p. 76.

[†] De Francia, T.: J. Opt. Soc. Amer., Vol. 59, 1969, and Vol. 54, 1964.

^{††} Harris, J.L.: J. Opt. Soc. Amer., Vol. 54, p. 606, 1964.

[#] Goodman, J.W.: Introduction to Fourier Optics. McGraw-Hill, New York, p. 122.

between the point-spread functions of the two point sources.* This statement applies only to the case where the unknown image is compared on a point-by-point basis with the theoretical image. The parallel problem of image restoration by digital analyses of images on a point-by-point basis was analyzed by Helstrom.**

The analog technique corresponding to the above digital procedure is to use an optical matched filter procedure.† The techniques have been demonstrated in the laboratory, but an experimental program must be undertaken to determine the accuracy of this alternate approach when applied to the problem at hand.

After the cursory review of an area in which the theoretical physicists placed great emphasis, we will re-examine the technique of superresolution in the more practical sense of making a decision in the "real world" of binary stars of unequal intensities, nongaussian grain and background noise, film nonlinearities, nonstationary images, and most important, non-ideal deciders such as the eye. Astronomers traditionally have made subjective decisions on photographic plates on the basis of visual examination. Using the eye as a sensor, there is a definite advantage in perturbing the image by moving the first zero toward the central maximum (i.e., superresolution). This is adequately demonstrated by the great interest shown in developing techniques for superresolution.†† The eye has the capacity of detecting small irregularities in shape and locating zero-intensity points, but is relatively poor in detecting small changes in illuminance between physically separated images unaided by auxiliary equipment. The advantage of the superresolution techniques are now obvious since they "perturb" the image in such a way that binary images are more easily "resolved" by the eye.

The most important result of this previous discussion should now be obvious to the reader. The optimum technique for detecting binary stars with separations less than the Rayleigh limit is determined by both the noise characteristics and the detector. To the author's knowledge, there are no practical systems in existence that are designed to detect binary stars using the sophisticated

* Harris, J.L.: J. Opt. Soc. Amer., Vol. 54, 1964, p. 606.

** Helstrom, C.W.: J. of Opt Soc. Amer., Vol. 57, 1967, p. 297.

† Goodman, J.W.: Introduction to Fourier Optics. McGraw-Hill, New York, p. 122.

†† Barakat, R.: J. Opt. Soc. Amer., Vol. 52, 1962, p. 264.

Barakat, R.: J. Opt. Soc. Amer., Vol. 52, 1962, p. 276.

An excellent bibliography of these techniques are included in Wolf, E.: Progress in Optics, Vol. II, Interscience, New York, 1961.

maximum likelihood probability tests presented in the mathematical models of Harris* and Helstrom.** Assuming that sufficient interest exists in the astronomical community for a capability of resolving beyond the classical limit, the mathematical techniques discussed should be reduced to practice by developing sophisticated detection techniques.

The alternative of taking the more classical approach of introducing controlled aberrations which permit a higher probability of correct detection by visual means also has significant merit. At the very least, the techniques discussed in this section should be tried in space (or ground-based simulation) in an attempt to derive relative figure of merits for detecting binary stars by post detection processing (à la Harris and Helstrom) or by superresolution.

APODIZATION USING ENERGY REDISTRIBUTION TECHNIQUES

The primary interest of the planetologist utilizing the LTEP will be the location and cataloguing of planets around neighboring stars. The major problem in locating planets does not arise from inadequate resolution of the LTEP. For example, the orbital radius of a planet the size and orbit of Saturn which is in orbit about α Centauri will subtend up to 7 arc-seconds as viewed from earth orbit. When comparing this value with the LTEP resolution of 0.05 arc-second, one notices that a significant number of stars may be analyzed in the search for orbiting planets.

The photometry calculations are not nearly so encouraging. Assuming an albedo of 0.5[†], the energy ratio at maximum separation of Saturn referenced to the sun is approximately 10^{-7} :1. For a centrally obscured aperture with 25 percent obscuration by area, the intensity of the first and second rings are approximately 0.09:1 and 0.013:1. A potentially hard problem has been made dramatically harder because of the effect of the central obscuration which transfers significant amounts of energy out of the central maximum into the surrounding ring structure.

It thus appears improbable that the LTEP viewing an identical model of the solar system located at a range of 4 light years could detect the planet Saturn. However, both an increase in the size of the interrogated stellar planet by a factor of 10 and a decrease in its orbit to the equivalent of 1 AU will generate a new radiance ratio of 10^{-3} :1. This is still orders of magnitude below the energy ratio of the rings of the Airy pattern to its maximum. Apodization schemes for reducing the energy surrounding the central maximum are presented in this section. It will be shown that under some special conditions, the LTEP utilizing auxiliary optics should be able to detect planets orbiting neighboring stars.

* Harris, J.L.: J. Opt. Soc. Amer., Vol. 54, 1964, p. 606.

** Helstrom, C.W.: J. Opt. Soc. Amer., Vol. 57, 1967, p. 297.

† Levy, E.C.: SRL Design Manual. Space Tech. Labs, 1961.

The classical apodization schemes require the pupil function to be altered by inserting an absorbing mask in one of the telescope pupils. In a classic paper, Luneberg* considered the effect of placing variable density filters in a pupil to generate an aberration free wave front of variable intensity. This technique dramatically reduces the energy in the image rings and thus allows the imaging of pairs of objects with significantly different intensities (e.g., a planet around a star). The reduction of elegant mathematics to practical application is yet to be fully implemented. Also, the average transmission of the masks is typically 0.1 which in turn requires that exposure times are significantly extended. Frieden** has raised the possibility for an "active" pupil (i.e., one for which there is a gain in light flux) which would allow for quasi-perfect imagery without any loss in total illumination. The implementation of this technique presents formidable problems if polychromatic light (as compared with monochromatic light required for laser amplification) is utilized by the telescope optical system.

The following section proposes a technique to redistribute, rather than absorb the energy in the entrance pupil to obtain the required pupil function. This has the distinct advantage that it is a nearly lossless apodization technique. The optical element which performs the energy redistribution is a polychromatic version of the conical axicon which is shown in Figure 281 along with its tunnel diagram. To make the mapping completely general, it is assumed that a central dark zone of radius b exists in the device's exit pupil.

General Theory of Axicon Mapping

The values for the extreme rays in Figure 281 were obtained by the following constraints and definitions:

$$a = \frac{\text{entrance pupil obscuration radius}}{\text{entrance pupil radius}} = \frac{r}{R} \quad (1)$$

Noting the tunnel diagrams, one deduces that at the axicon exit pupil

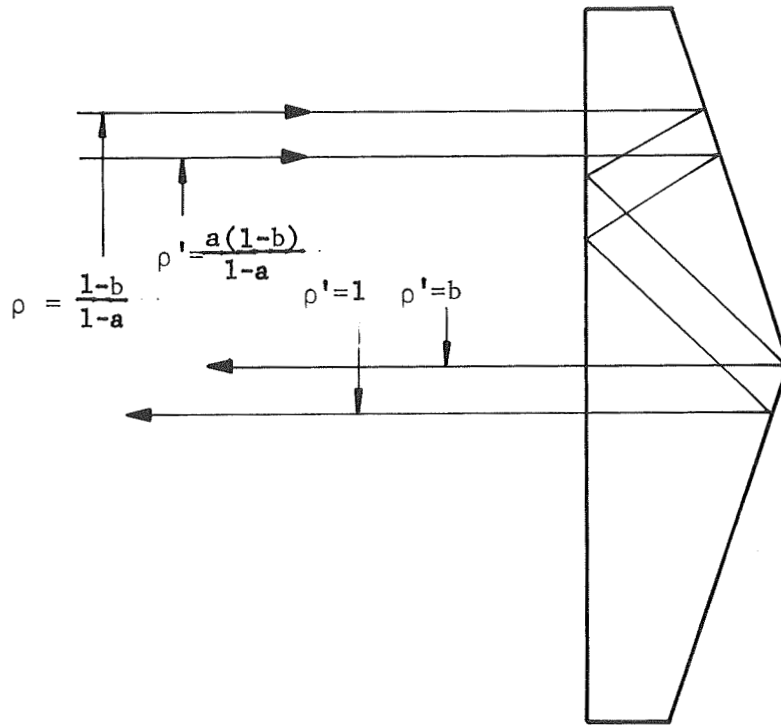
$$r - R = 1 - b \quad (2)$$

Solving the simultaneous equations yields.

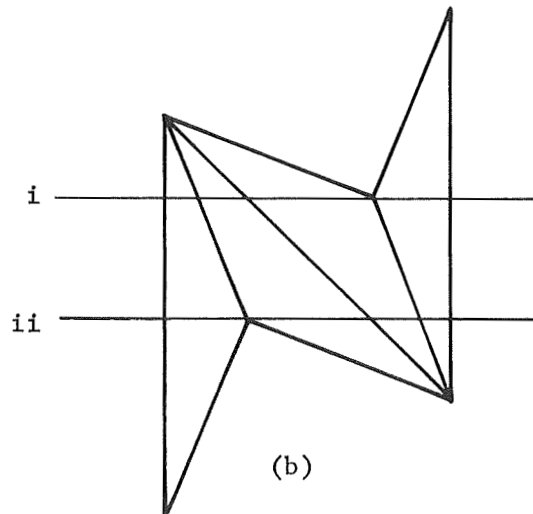
$$r = \frac{a(1-b)}{1-a} \quad (3)$$

* Luneberg, R.K.: Mathematical Theory of Optics. Brown University, 1944.

** Frieden, B.R.: A Pupil Coating for Quasi-Perfect Image Formation. Tech. Rpt. No. 21, University of Arizona, Oct. 20, 1967.



(a) Achromatic Axicon Showing Extreme Rays



(b)

Figure 281. Tunnel Diagram

$$R = \frac{1 - b}{1 - a} \quad (4)$$

The element utilizes total internal reflection at the first surface and is polychromatic for collimated light since there exists no refractive deflections in the ray trace. Noting the two rays i and ii, a ray in the center of the entrance pupil (ii) will be mapped into the outer portion of the exit pupil and vice versa. The intensity output from this mapping will be referred to as palindromic.

Two techniques for duplexing the input and output beams of the achromatic axicon are presented (Figure 282). The first technique utilizes a folding mirror with an elliptical hole in the center. The minor axis of the ellipse is equal to the diameter of the central obscuration in the exit pupil. This technique has the advantage that it is independent of the state of polarization of the telescope output, but has the disadvantage that it introduces a loss of transmitted power in addition to that lost by the truncation at the entrance pupil if the area of the exit pupil central obscuration is less than 25 percent of the unobscured clear aperture. This is not a serious limitation since the central obscuration of many two-mirror telescopes is of this order. (The central obscuration of the LEP is 4 percent.)

A second technique utilizes polarization (Figure 282b). Assuming that the telescope input is linearly polarized in the "p" state relative to the beamsplitter polarizing coating,* the incident light is transmitted by the polarization beamsplitter.** The properly oriented quarter wave plate† converts this light to circular polarization on the first pass and subsequently to "s" polarization after the second pass thru the plate. Thus, the returning light is reflected by the polarizing coating and the input and output of the folded axicon and achromatic axicon are properly duplexed.

If the semi-cone angle of the achromatic axicon is α , total internal reflection will occur at the front surface if

$$2 \alpha > \pi - \sin^{-1} \frac{1}{n} \quad (5)$$

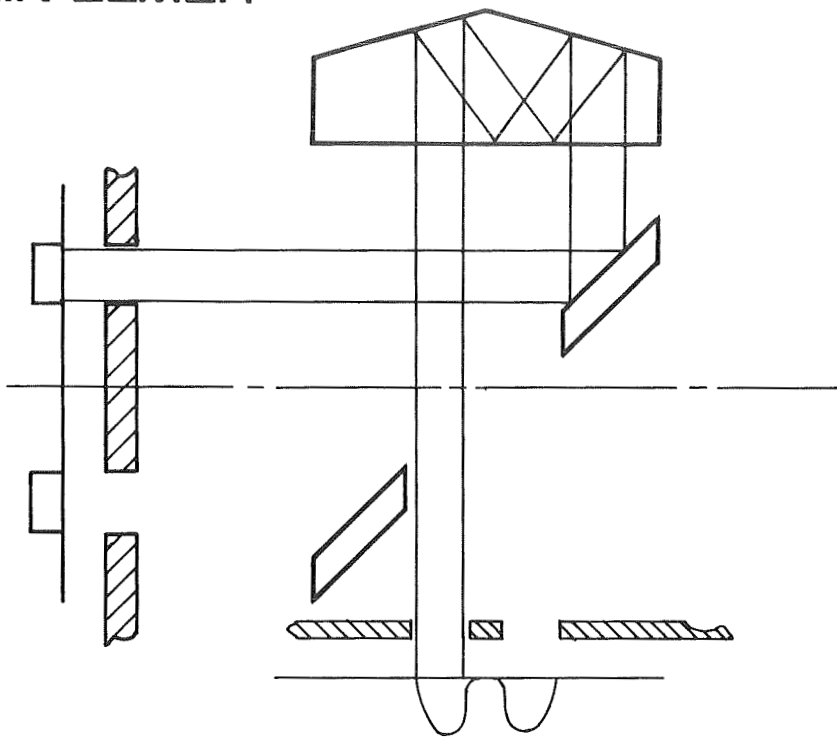
where n = index of refraction

Since the input and output beams must be parallel and collimated, the angle α is 22.5° which infers that n must have a value greater than $\sqrt{2}$.

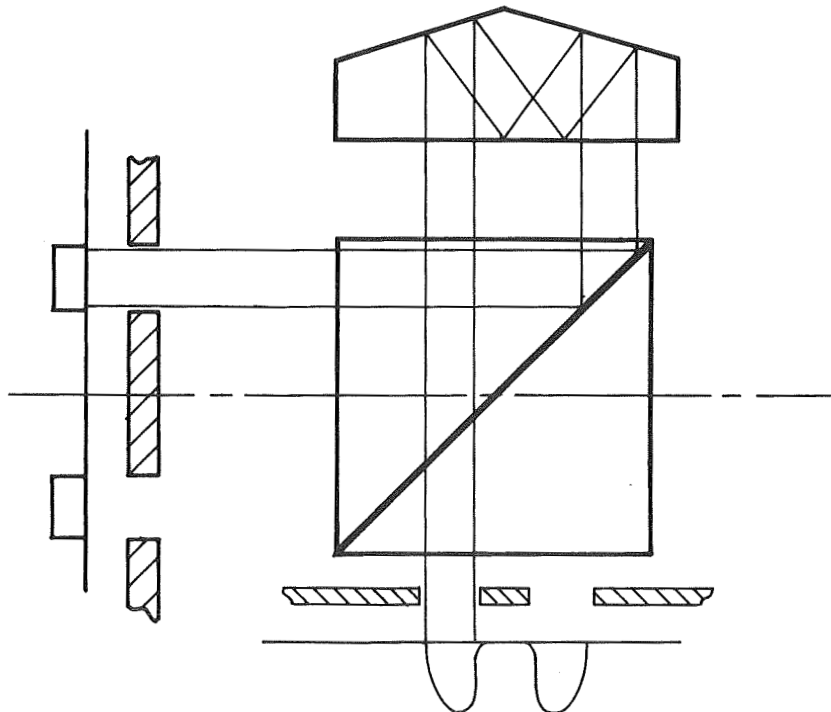
* if the source is not polarized in the "p" state, it is sufficient to insert the polarization beamsplitter in the beam as shown since the polarizing beamsplitter automatically chooses the required polarization and rejects the unwanted polarization.

** Austin, R.: Personal Communication.

† McIntyre, C.M.: Achromatic Wave Plates, J. Opt. Soc. Amer., Vol. 58, No. 12, pp. 1575-1580.



(a) Spatial Multiplexing



(b) Polarization Multiplexing

Figure 282. Multiplexing Technique for Energy Redistribution Apodization

The cone height for the achromatic axicon can be shown to be *

$$\ell = \frac{\sqrt{2}R}{4} \quad (6)$$

As expected, the dimensions of the achromatic axicon are independent of the index of refraction.

Output Intensity Distribution in The Exit Pupil

The intensity distribution of the light incident on the entrance pupil is

$$f(\rho) = k \quad (1)$$

where k = normalization constant

The normalization constant is computed by the following criterion:

$$\int_0^{2\pi} \int_0^R f(\rho) \rho d\rho d\theta = 1 \quad (2)$$

where a = central obscuration radius
 R = entrance pupil radius

Doing the necessary integration, the constant becomes

$$k = [\pi(R^2 - a^2)]^{-1}$$

The axicon maps the optical energy in an annulus of radius ρ in the entrance pupil of radius R , into an annulus of different radius in the exit pupil as shown in Figure 283.

The energy in an annulus of thickness $d\rho$ is

$$E(\rho) = 2\pi K \rho d\rho \quad (3)$$

Since the energy in an annulus of thickness $d\rho$ is conserved during the mapping, the energy in the output annulus of radius in the palindromic distribution is

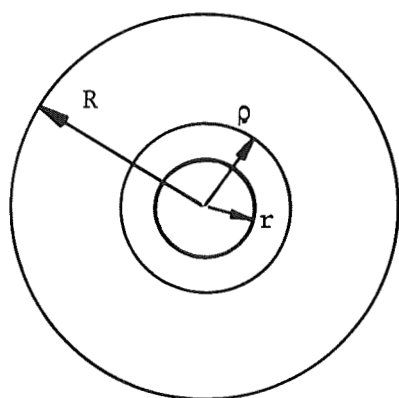
$$E(\rho') = 2\pi f_p(\rho') \rho' d\rho' \quad (4)$$

where ρ' = radial dimension in exit pupil

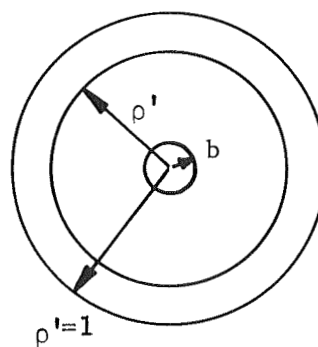
The relationship between the entrance and exit pupil variables ρ and ρ' , respectively, will now be examined. The linear domains of the variables are

$$r = \frac{a(1-b)}{1-a} < \rho < R = \frac{1-b}{1-a}; \quad b < \rho' < 1 \quad (5)$$

*Peters, W., Ledger, A.: To be published in App. Optics.



Annulus in Entrance Pupil



Annulus in Exit Pupil

Figure 283. Palindromic Mapping Geometry

Because of the linear relationship between ρ and ρ' ,

$$\rho' = \alpha\rho + \beta \quad (6)$$

where α , β are constants to be determined.

Applying the conditions of Equation (6) to Equation (5), the simultaneous equations result:

$$1 = \alpha a \frac{1-b}{1-a} + \beta \quad (7)$$

$$b = \alpha \frac{1-b}{1-a} + \beta \quad (8)$$

Solving these equations,

$$\alpha = \frac{-(1-b)(1-a)}{(1-b)(1-a)} \quad (8)$$

$$\beta = 1 + \frac{a(1-b)}{1-a} \quad (9)$$

$$\begin{aligned} \rho' &= 1 + \frac{a(1-b)}{1-a} - \rho \pi \\ &\equiv \Xi - \rho \end{aligned} \quad (10)$$

The phase term π denotes that there is a reversal in the direction in the radial dimension during this mapping.

Equating Equation (4) to Equation (3), the palindromic distribution is found to be

$$f(\rho') = \frac{K(\Xi - \rho')}{\rho'} \quad (11)$$

The effectiveness of the apodization scheme will be considered in terms of the normalized illuminance ratio as defined by Barakat.*

* Barakat, J. Opt. Soc. Amer., Vol. 52, 1962, pp. 264-275.

$$I(\rho') = \left| \frac{D(\rho')}{D_{Ai}(0)} \right|^2 \quad (12)$$

where $D(\rho') \xleftrightarrow{2DFT^*} F(\rho)$

$$F(\rho') = \text{E-field in exit pupil (complex)}$$

$$|D_{Ai}(0)|^2 = \text{illuminance at center of far field for lossless aberration-free Airy aperture uniformly illuminated.}$$

Thus $I(0) = 1$ is the maximum attainable value of the normalized illuminance ratio.

In polar coordinates, the expression for the complex amplitude in the Fraunhofer plane is**

$$D(\rho') = \int_0^{2\pi} d\theta \int_b^1 F(\rho') J_0(k\rho' w) \rho' d\rho' \quad (13)$$

For the condition when the input (and thus the output) beams are parallel to the axicon axis (Figure 281) the exit pupil amplitude distribution is simply

$$F(\rho') = f(\rho')^{1/2} \quad (14)$$

The square root operation is legitimate for this special case because the axicon appears to be a plane parallel plate of glass. This is easily verified by noting the tunnel diagram. Thus no phase variations are expected across the aperture and square root operation is justified.

Phase Perturbation of Off-Axis Images

The tunnel diagram for the achromatic axicon which is presented in Figure 284 will be used to analyze the effect of phase front tilt on the operation of the device. In general, the tunnel diagrams are used to "fold" plane surfaces about lines lying in the plane of an interface between media of different refractive indices. Thus, in this general application for plane surfaces the tunnel diagram model may be used to trace all rays transmitted through

* 2 DFT is the two dimensional Fourier transform (i.e., the Fraunhofer diffraction integral).

** Barakat, J. Opt. Soc. Amer., Vol. 52, 1962, pp. 264-275.

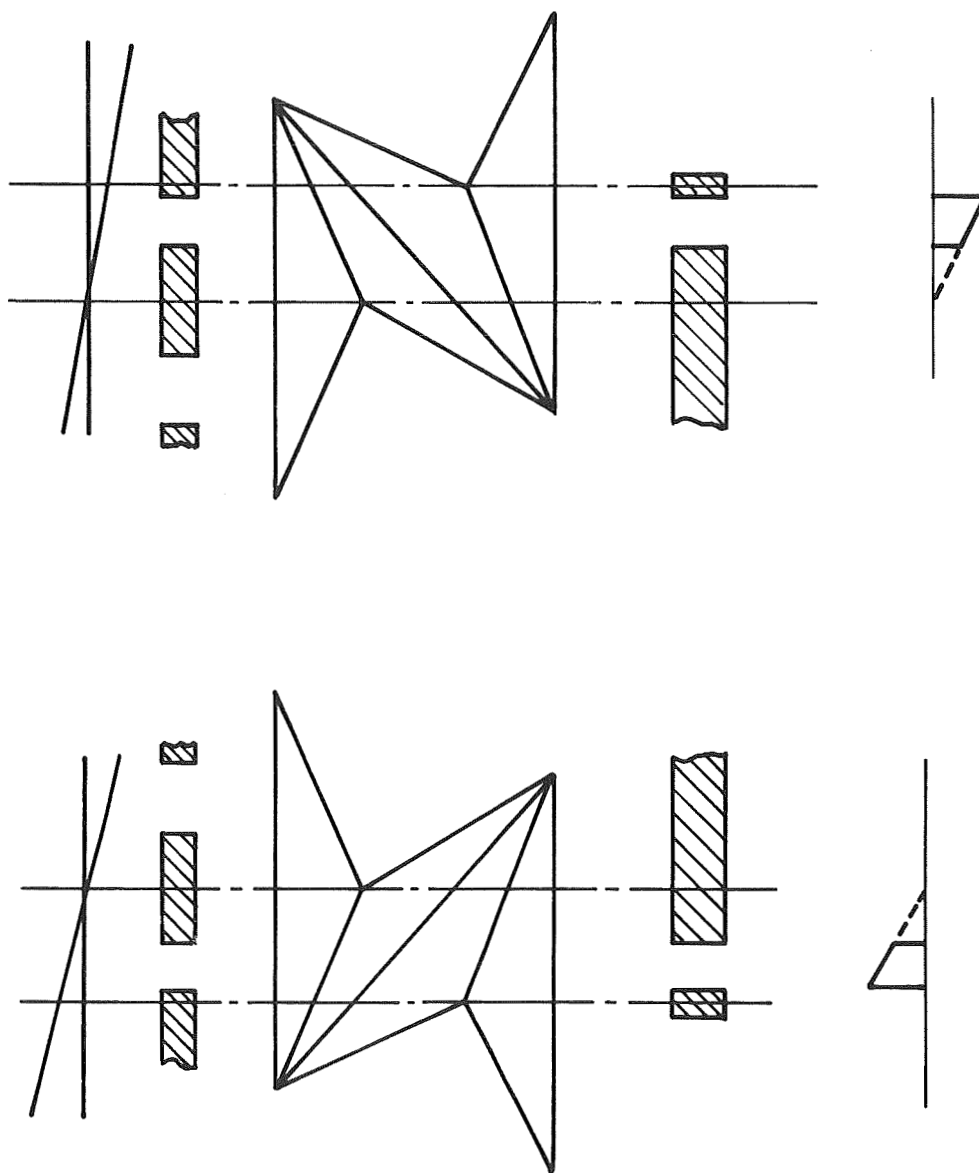


Figure 284. Geometry for Off-axis Performance

the functional model. Additional constraints must be applied if we consider the tunnel diagram with conical surfaces.

Since the intersection of two tangent cones in the relative positions of Figure 284 is a straight line, a given tunnel diagram is applicable for only one radial line (i.e., $\theta = \text{constant}$, $\rho' = \text{positive variable in range } b \leq \rho' \leq 1$).

Assuming the centrally obscured entrance pupil is illuminated by a uniform tilted wavefront whose normal vector is at an angle β to the axicon optical axis, the equation for the input is:

$$F_{in}(\rho) = K e^{j\beta \rho \cos \theta} \quad (1)$$

where $\beta = \frac{n\lambda}{2R}$

$n = \text{number of wavelengths tilt from bore sight.}$

To simplify the mathematics, it is assumed that the intensity distribution of the output is not significantly changed when the value of β is small (typical values of β are $\approx 10^{-5}$ radians for a 5-cm radius axicon).

An effect of this perturbation is to misalign the optical axis of axicon and the center of the output distribution by

$$3.4 \ell_A \beta = \frac{3.4 \cdot R \cdot \sqrt{2}\beta}{A} \approx 1.2R\beta \quad (2)$$

$\approx 1\mu$ for the above example

The exit pupil E-field for an off-axis source is thus

$$E(\rho') = f(\rho') e^{j\Theta(\rho', \theta, \beta)} \quad (3)$$

The phase term Θ is derived by the following procedure:

$$e^{j\beta \rho \cos \theta} \quad \begin{matrix} \text{axicon} \\ \text{mapping} \end{matrix} \quad e^{j\Theta(\rho', \theta, \beta)} \quad (4)$$

which infers with a 2π ambiguity

$$\beta \rho \cos \theta = \Theta(\rho', \theta, \beta) \quad (5)$$

$$-\beta(\Xi - \rho') \cos \theta = \Theta(\rho', \theta, \beta) \quad (6)$$

Substituting into Equation (3), the aberrated pupil function is

$$E(\rho') = \left(\frac{\Xi - \rho'}{\rho'} \right)^{1/2} e^{-j\beta (\Xi - \rho') \cos \theta} \quad (7)$$

where

$$b \leq \rho' \leq 1$$

Note that the phase term may be broken into two parts: $\beta \rho' \cos \theta$ which, as expected, is the identical tilt introduced in Equation (1) and $\beta \Xi \cos \theta$, an aberration term which is not one of the Seidel aberrations.

A disturbing feature of this aberration is the discontinuity in the region $\rho' = 0$ for $\theta \neq \pm \pi/2$, $b = 0$. A value $\beta \approx 1$ corresponds to the wavefront tilt between an on-axis source and off-axis source if the sources are separated by one Rayleigh resolution element.

Substituting Equation (7) into the Fraunhofer integral, the far field pattern at a point ω, ψ for an off-axis source is:

$$U(\omega, \psi) = \int_0^{2\pi} d\theta \int_b^1 d\rho' \sqrt{(\Xi - \rho')} \rho' e^{-j[k\rho' \omega \cos(\theta - \psi) - \beta(\Xi - \rho') \cos \theta]} \quad (8)$$

Using the trigonometric identities, Equation (8) is modified to

$$\begin{aligned} U(\omega, \psi) &= \int_0^{2\pi} d\theta \int_b^1 d\rho' \sqrt{(\Xi - \rho')} \rho' e^{-j[k\rho' \omega \cos \theta \cos \psi + K \rho' \omega \sin \theta \sin \psi - \beta(\Xi - \rho') \cos \theta]} \\ &\equiv \int_b^1 d\rho' \sqrt{(\Xi - \rho')} \rho' \int_0^{2\pi} d\theta e^{-j[A \cos \theta + \beta \sin \theta]} \end{aligned} \quad (9)$$

where

$$\begin{aligned} A &= k\rho' \omega \cos \psi - \beta(\Xi - \rho') \\ B &= k\rho' \omega \sin \psi \end{aligned}$$

The creation of the dummy variables ϕ , D is required where *

$$\begin{aligned} A &= D \cos \phi \\ B &= D \sin \phi \end{aligned}$$

which infers that

$$D = (A^2 + B^2)^{1/2}$$

* Technique suggested by J. Bartas

Substituting into (9)

$$\begin{aligned}
 U(\omega, \psi) &= \int_b^1 dp' \sqrt{(\Xi - p')\rho'} \int_0^{2\pi} d\theta \exp-j \{ D[\cos\theta \cos\psi - \sin\theta \sin\psi] \} \\
 &= \int_b^1 dp' \sqrt{(\Xi - p')\rho'} \int_0^{2\pi} d\theta \exp-j[D\cos(\theta + \psi)] \\
 &= 2\pi \int_b^1 dp' \sqrt{(\Xi - p')\rho'} J_0(D)
 \end{aligned}$$

where $J_0(D)$ is the Bessel Function of first kind and

$$D = [(kp'w\cos\psi - \beta(\Xi - p'))^2 + (kp'w\sin\psi)^2]^{1/2}$$

Substituting $\beta = 0$ yields the on-axis image previously discussed.

Conclusions

Figure 285 gives the isophotes* for the on-axis image formed by an axicon. The value $b = 0.095$ was chosen to minimize the maximum intensity outside the first zero by a trial-and-error technique. The value of $r/R = 0.5$ (the central obscuration ratio) was chosen as typical of the obscuration of two-mirror telescopes (although not for the LTP configuration). This choice also permits one to use either of the multiplexing techniques previously discussed. The isophote plot shows the motivation for considering energy redistribution techniques for apodization. The maximum intensity outside the first zero (71 dB) is 27 dB = 500 times below the maximum intensity (98 dB). This is an improvement of $0.09/0.002 = 45$ times over the original image.

The effect of the aberration term $\beta \Xi \cos\theta$ will now be examined. As expected, it is zero for on-axis. For comparison, a value $n = 1.22$ corresponds to a tilt in the input wavefront equal to the minimum angular separation in object space of two point sources just resolvable via the Rayleigh criterion.

Figure 286 shows the image of a point source translated somewhat less than one resolution element off axis. Note that the image is not badly perturbed but does show some asymmetry. The heavy cross superimposed on Figures 285 through 289 denote the axis of symmetry for the axicon output and the arrow denotes the position of the first minimum in the on-axis image.

*

Refer to the previous section for discussion on interpreting isophote plots.

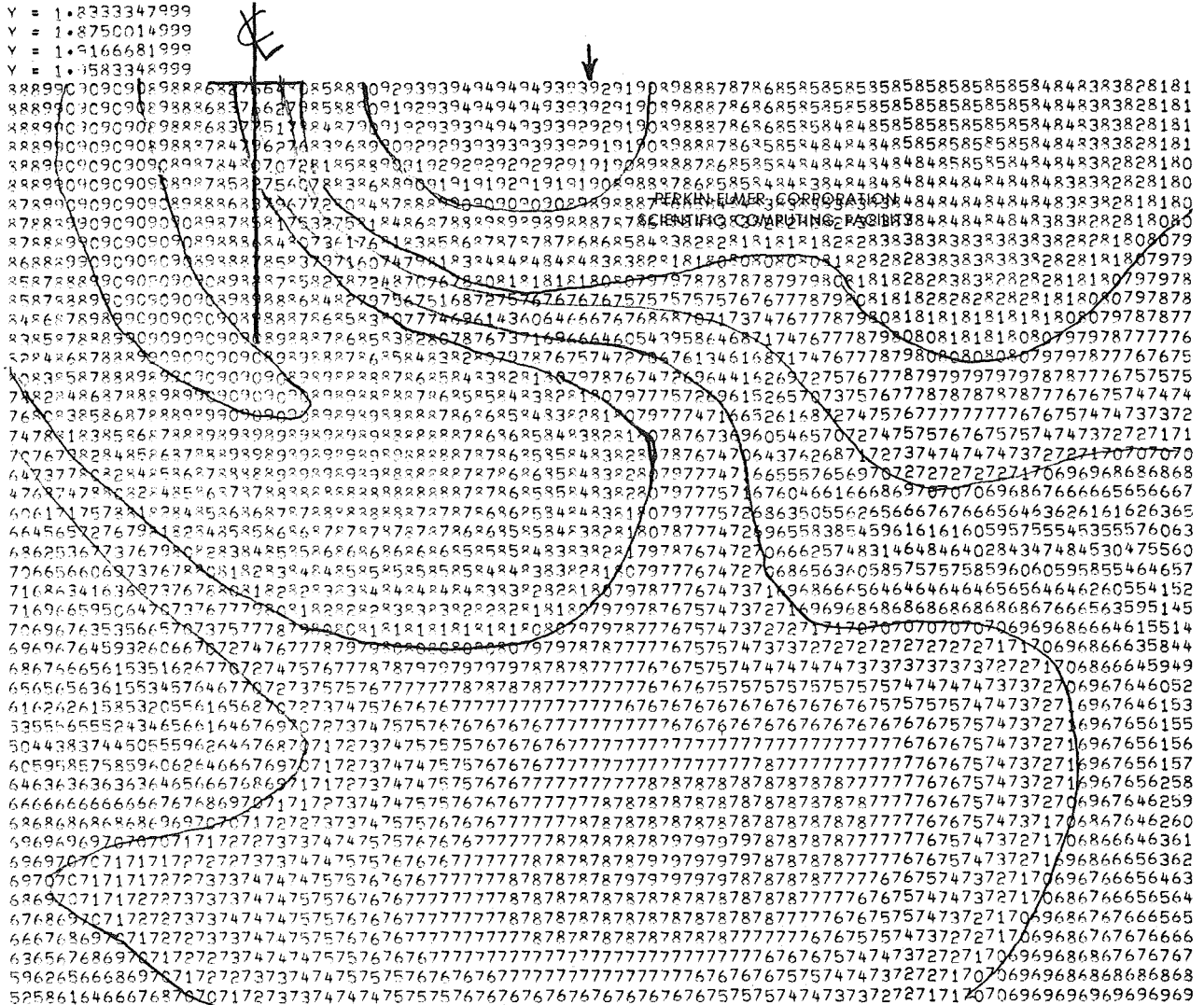


Figure 286. Off-Axis Image with $n = 1$

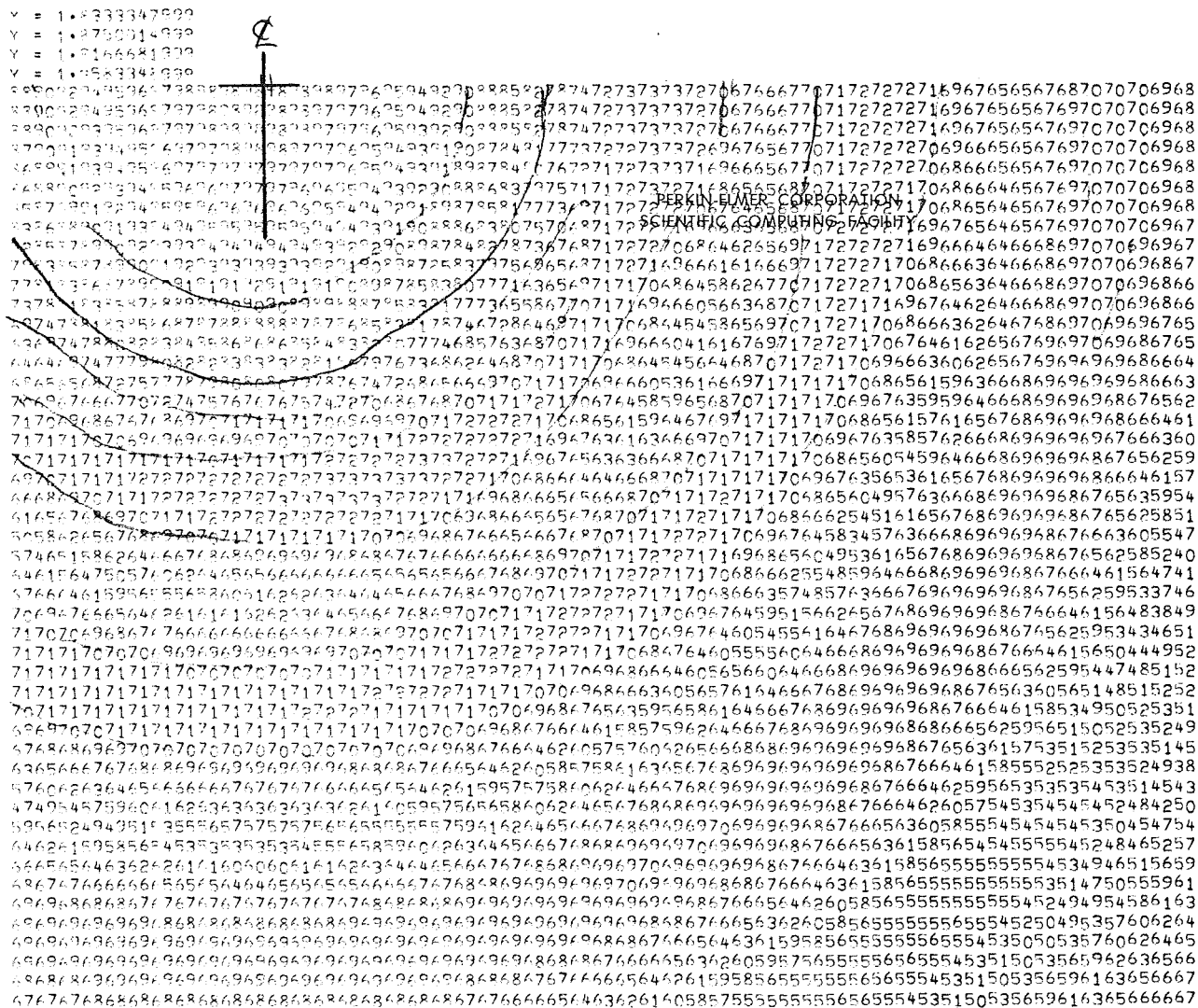


Figure 289. Superposition of Point Image of Relative Intensity 0.01 and Displacement $n = 2$ onto On-Axis Image

Figures 287 and 288 show off-axis images for point sources displaced 1.7 times and 2.5 times the Rayleigh resolution limit, respectively. As discussed in the Superresolution technique study, the central intensity decreases as a strong function of the introduced aberration. As will be demonstrated shortly, this is the main limitation of the energy redistribution apodization technique.

The ultimate test of the effectiveness of the technique being discussed is analyzing the isophotes of a star-planet or star-faint companion pair. From the previous analysis of Figures 286-288 it is obvious that one should take great care in aligning the axicon axis with the star center. This will guarantee that the nominal 27 dB reduction in the rings will actually be obtained. This condition is assumed for Figure 289 with the planet displaced by $n = 2$ (1.7 Rayleigh resolution elements). An intensity ratio of 100:1 was required to obtain a discernible change in the isophote plots. Note this is approximately a 2.5-times larger radiance ratio than one would first assume by analyzing Figure 285. As mentioned previously, this is a result of the aberration term.

Utilization of the technique for values of $n > 2.5$ is not practical. This limitation on the angular separation of the two point sources is not catastrophic. Considering that the angle corresponding to $n = 2$ is 0.01 arc-second, a significant number of stars at distances far beyond α -Centauri can be interrogated provided the telescope faint limit for the planet is not exceeded. Also, further study may well yield techniques for reducing the off-axis aberration and allow a more universal application of the apodization technique.

Assuming that the Solar system is typical of the planetary systems of neighboring stars, one can expect severe problems in using photometry to detect existence of extrasolar planets. As discussed previously a planet the size of Saturn in an orbit of 1 AU will have an apparent magnitude of 17M (10^7 -times) less than the parent star. Without modifications, the energy redistribution technique discussed in this section will detect energy ratios of approximately 5M (10^2 times). If the proposed technique is to be successful, it is required that the diameter of the planet be greater than 500 earth diameters if the LTP is to detect its existence. This number is disappointing indeed, but it should not be summarily dismissed.

The apodization technique will also have possible applications in detecting faint companions. As in the above discussion, the two sources must have apparent magnitudes within 5M of each other and separations on the order of the diffraction limit of the telescope.

CHAPTER 15: TELESCOPE THERMAL CONSIDERATIONS

INTRODUCTION

The thermal problem in space telescopes may be approached in three basic ways:

1. "Proper" material selection - if the materials used in the various telescope components are thermally insensitive there is, a priori, no thermal problem.

2. Thermal Isolation - If the telescope can be thermally isolated from its environment, the telescope temperature will remain at a preset baseline level and again, once the telescope is operational, it will remain operational and no thermal problem will occur.

3. Thermal accommodation - if the various thermal influences on the telescope are completely accommodated by, say, a perfect space conditioning system then the telescope can be maintained at a preset temperature condition and no thermal problem will exist in the telescope.

Unfortunately, the foregoing three approaches are idealized, non-attainable limits in the real world. Therefore, it is necessary to make design compromises and/or to use the various approaches in combination to develop an acceptable total telescope system. Usually optimum materials are used in conjunction with extensive superinsulation and the entire telescope is designed to promote thermal diffusion to even-out temperature gradients and/or excursions. We consider the three fundamental approaches to the telescope thermal problem on an individual basis recognizing that a multi-faceted attack on the problem may be needed to meet the optical error budget allowed for thermal effects.

In this immediate section of the LTEP report we evaluate the potential of currently available methods to cope with the design complexity of a large spaceborne telescope system.

Other sections of the report contain extensive studies of two advances in current technology which may provide optimum solutions in two critical telescope design areas. In Chapter 16, Chrysler Corporation investigates the feasibility of adapting the heat pipe principle to the thermal control of a two-meter orbiting telescope. In Volume II of this Study Report, Lockheed Missiles and Space Company reports on the results of a thermal design based on Optical Solar Reflector (OSR), multilayer insulation, optical black interior coating, and a sun shield cap. The analysis covers six different LTEP usage modes and three different orbital altitudes and various orbital inclination angles.

THERMAL DESIGN OBJECTIVES

The thermal design effort for any telescope is concerned mainly with the preservation of critical optical tolerances of the telescopes. It is also necessary that the electrical and mechanical elements operate in a suitable thermal environment.

THE OBJECTIVES OF THE THERMAL DESIGN EFFORT ARE AS FOLLOWS:

- PROVIDE A SUITABLE OPERATING TEMPERATURE.
FOR ALL TELESCOPE COMPONENTS AND SUBSYSTEM
- PREVENT IMAGE DEGRADATION DUE TO EXCESSIVE
THERMAL GRADIENTS IN THE TELESCOPE OPTICAL
SYSTEM

The critical optical thermal tolerances fall generally into three classifications:

1. A large peripheral gradient in the Cassegrain section of the telescope tube will "bend" the optical path and thus result in pointing error. (A pointing error will misalign the entire experiment package.)
2. Thermal gradients in large primary mirrors will compromise diffraction-limited performance because they will become warped.
3. Excessive temperature excursions of, or gradients in, optical or mechanical elements (windows, mirrors, or spacer rods) will cause a focal shift.

A major effort of the thermal design is to keep the foregoing thermal effects within specified limits.

Thermal transients must be included in any evaluation of the effects of temperature gradients and excursions of critical optical tolerances. The following sections consider the foregoing classes of problems in greater detail.

THERMAL TOLERANCES

It is more meaningful in discussing thermal tolerances if an existing system can be referred to, especially if the existing system analysis is substantiated by test data. The basis for much of the following will be the Hydrogen-Alpha telescopes fabricated for the ATM program and the thermal-vacuum tests that were carried out to establish the figure variations of the OAO 0.8-meter mirror. While these geometries do not duplicate the 2-meter LTEP configuration, conclusions drawn from the programs should correspond to those anticipated in future LTEP programs.

THE HYDROGEN-ALPHA TELESCOPES

The Hydrogen-Alpha telescopes, one of which is shown schematically in Figure 290, use both active and passive thermal control. CerVit* proved to be the optimum mirror material and is used along with Invar spacer rods. An active temperature control is used in the Fabry-Perot filter assembly.

Internal surface emissivities are governed by optical considerations. External surface emissivities are governed by thermal considerations. The low tube emissivities of 0.05 result in minimum peripheral gradients. The emissivities of the heat generating component surfaces provide each component with the means to dissipate its own generated heat by radiation without relying on conduction to other areas of the telescope system.

It is apparent that all three basic approaches were used in the H-Alpha program to achieve the thermal design objectives.

ANALYTICAL PROCEDURE

Standard finite difference methods are used for the solution of most of the temperature problems in the thermal design study of the telescopes. The finite difference method consists of idealizing the temperature field as an equivalent thermal network and then solving the network equation matrix by a series of finite steps.

Computer Programs

1. CINDA (Chrysler Improved Numerical Differential Analyzer). The TRW version (CINDA) adapted for Univac 1108 computer is used presently by Perkin-Elmer for most of the thermal calculations.

2. View Factors - The CONFAC program is used to some extent. Many view factors are calculated using standard references such as Hamilton and Morgan (NACATN 2836). View factors less than 0.01 (for both nodes) are generally neglected.

Nodal Geometry for the Total Telescope System

The nodal arrangement for the ATM H-Alpha telescope is shown in Figure 291. Tables of network constants are available for any interested reader.

This report presents only the network for the total system. Other networks modeling subsystems or components were used for component and subsystem thermal investigations.

* A product of Corning Glass Works, Corning, N.Y.

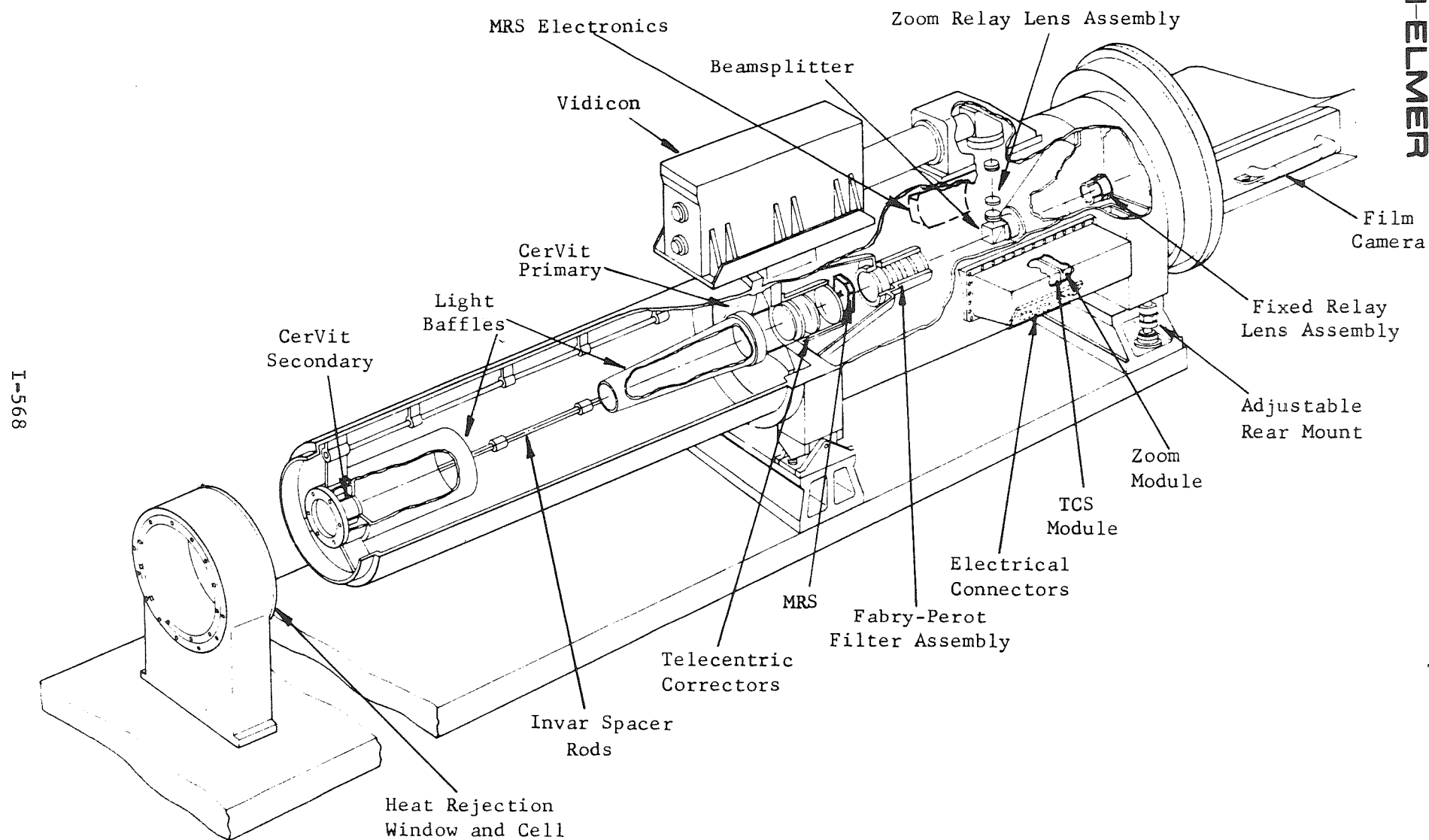
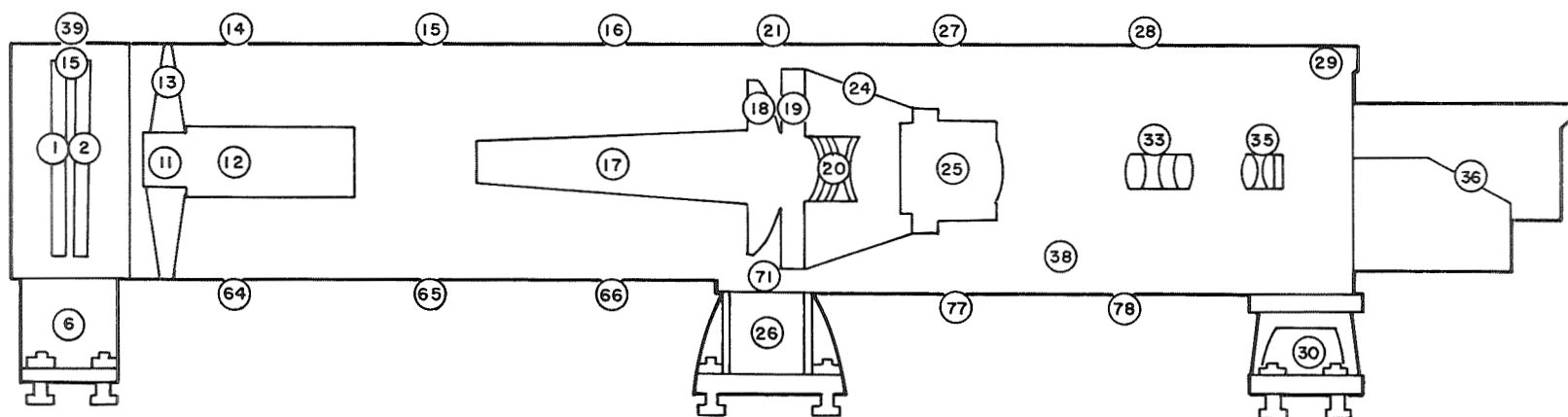


Figure 290. HCO-Hydrogen Alpha Telescope - Artist's Concept

I-568

I-569



- 40 Spar Insulation
- 41 ATM Experiment
- 42 Front Wall
- 43 Canister
- 44 Space
- 46 Rear Wall
- 47 Spar
- 51 E-Box Base Plate
- 52 TCS Electronics
- 53 Zoom Electronics

Figure 291. ATM H α Nodal Geometry

TRANSIENTS

Pseudo-Steady State

The Hydrogen-Alpha telescope will reach a pseudo-steady state condition (after which time variations of temperature will be repetitive) a few hours after systems operation begins. The front housing will generally undergo the most severe cyclic temperature variations of any system component. (See Figure 292.) Behind this area, the cyclic variations attenuate very rapidly. At the Cassegrain mid-section, the cyclic variations are not discernable on a plot with a realistic temperature scale.

It is reasonable, therefore, to refer to the steady-state temperature of a node even if, theoretically, the temperature undergoes a small cyclic variation (of 1/100 of a degree or so) within a critical element. The foregoing statement assumes, of course, that the telescope design will provide a suitable attenuation of the thermal gradients in the critical optical areas of the telescope.

SYSTEM PERIPHERAL GRADIENTS

A plot of the peripheral temperature gradients in the Cassegrain sections of the two telescopes is shown in Figure 293. The HCO-H α peripheral gradient peaks at about 0.22°F at the mounting plane. The maximum local ATM-H α peripheral gradient is 0.11°F at design point conditions. Variations in canister temperatures are attenuated by about 100:1, e.g., a change of 1°F in the canister changes the local peripheral gradient by 1/100°F, or less. The peripheral gradients are of the order of 0.01°F/inch of diameter.

HYDROGEN-ALPHA DESIGN RESULTS

The results of the optical-thermal analysis of the HCO-H α and ATM-H α telescopes are summarized in Table 33. The specified optical temperature tolerance limits are compared with the computed temperature variations. In all cases, the calculated temperature variations are within the allowable temperature tolerances.

Several comments are appropriate:

- The pointing error due to peripheral gradients will be no larger than 1 arc-second.
- Due to the small size of the H-Alpha primary mirror, the figure variation with temperature gradient is insignificant. Therefore, no thermal tolerance based on figure error is listed.
- The most significant thermal effect on the H-Alpha telescope optical systems was the possible focus shift. However, the temperature control is sufficiently precise so that active focus positioning systems are not required.

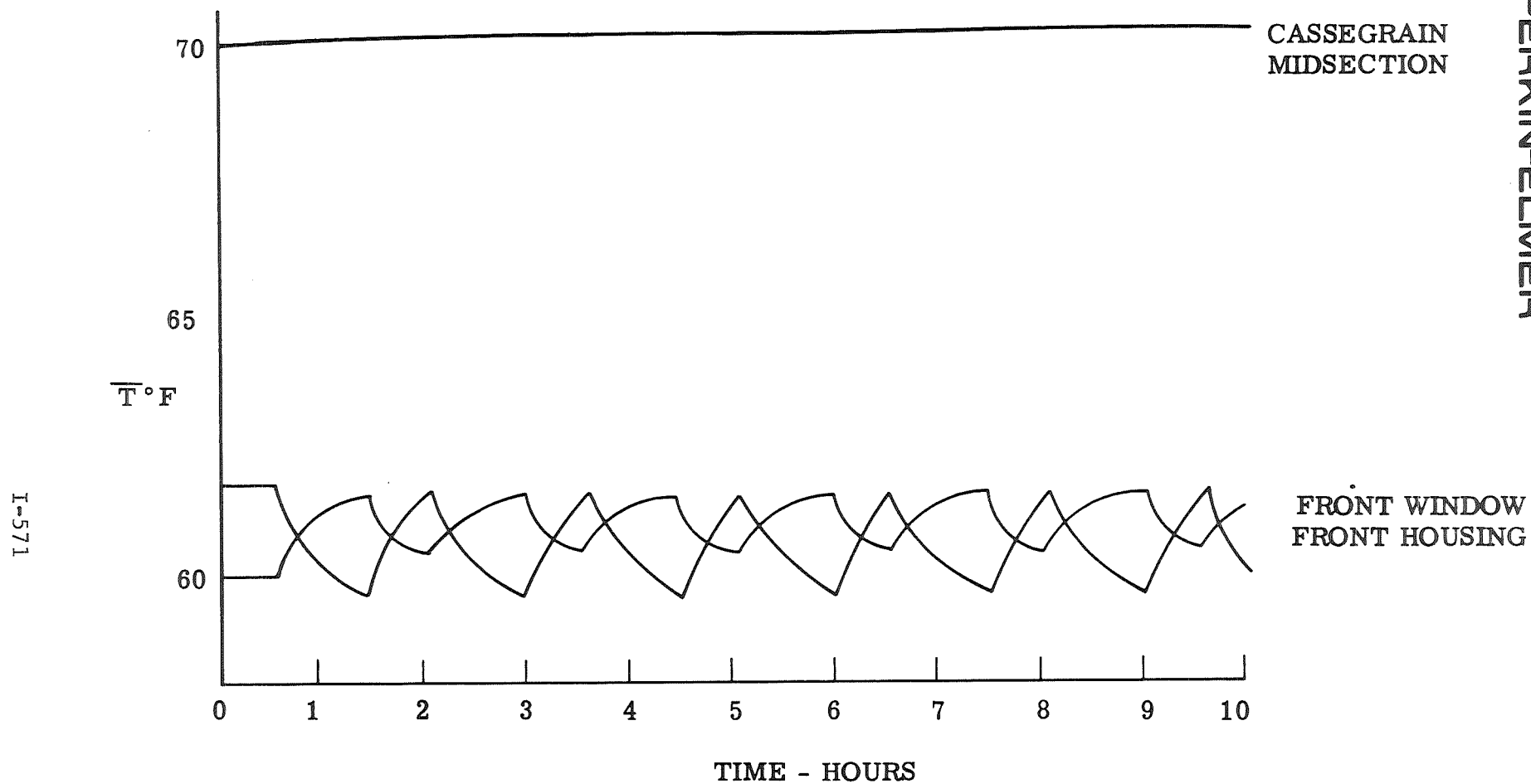


Figure 292. ATM-H α Initial Transient

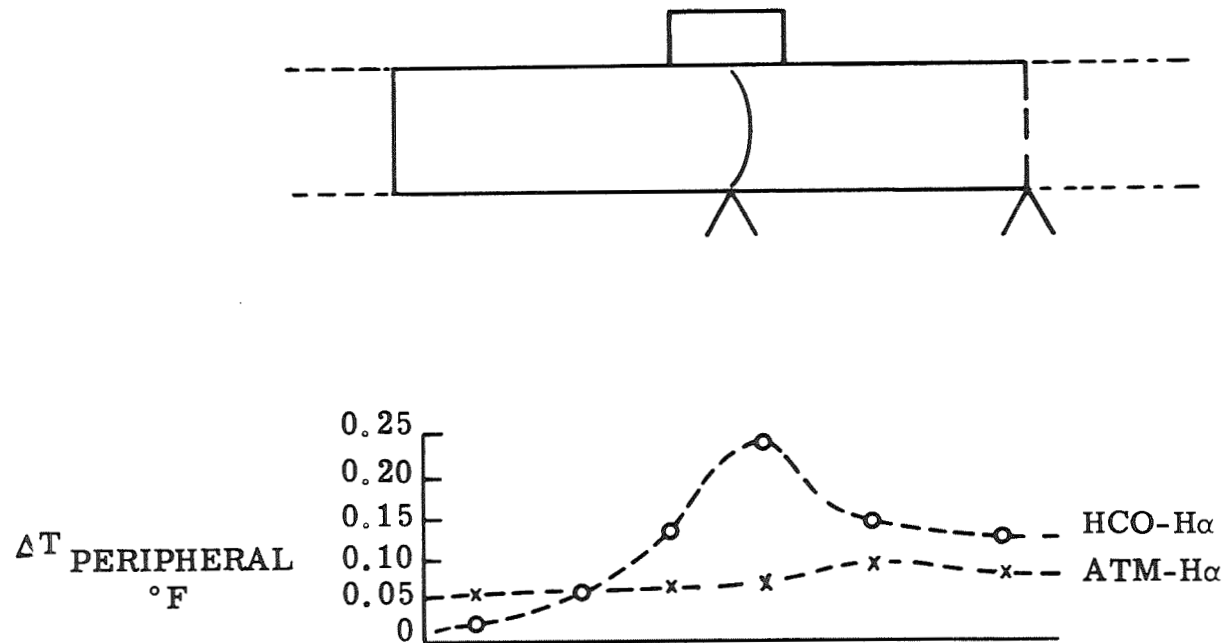


Figure 293. Cassegrain Section Peripheral Temperature Gradients

TABLE 33. SUMMARY OF H-ALPHA TELESCOPE THERMAL TOLERANCES

<u>ELEMENT OR ASSEMBLY</u>	<u>ΔT OR GRADIENT</u>	<u>EFFECT</u>	<u>TOLERANCE</u>	<u>CALCULATED</u>
Heat Rejection Window	Radial Gradient	Focus Shift	2.5°F	1.0°F
Invar Spacer Rods (Cassegrain)	Change in Temp.	Focus Shift	±6°F	±4°F
Primary Mirror	Axial Gradient	Focus Shift	1.5°F	0.5°F
	Change in Temp.	Focus Shift	±50°F	±4°F
Secondary Mirror	Axial Gradient	Focus Shift	2.5°F	0.2°F
Fabry-Perot	Radial Gradient	λ_0 Shift	0.5°F	0.15°F
	Change in Temp.	λ_0 Shift	1.0°F	(0.2°F)
Telescope Tube (Cassegrain Sector)	Peripheral	Pointing Error (5 Arc-Sec/°F)	0.5°F	0.20°F

Note: Tolerances listed are only those considered critical to the optical performance of the telescope.

PRIMARY MIRROR GRADIENTS

Several years ago The Perkin-Elmer Corporation initiated a program to evaluate the effects of thermal disturbances on diffraction-limited mirrors in space-borne telescope systems. In addition to the acquisition of specific data and deformation guidelines, the program had two other objectives:

- The development of an analytical model that could be used to predict accurately the thermoelastic deformation of primary mirrors.

- The refinement of experimental techniques to measure accurately the resultant deformations with errors of less than 0.1°C and 0.01 λ for thermal gradients up to 10°C.

All three objectives were achieved for the 0.8-meter mirror tested. Some general design guidelines and a brief summary of proven experimental and analytical techniques are noted below. The experimental and analytical techniques are discussed first.

EXPERIMENTAL TECHNIQUES

Test Facility

A vertical test tank, operated under vacuum, is well suited as the test chamber. An absolute pressure of less than 1 millimeter Hg will permit precise figure measurement tests without the degrading effects of air turbulence. An air bag mirror support system can be employed to minimize mechanical deflections caused by the weight of the mirror and to simulate the gravity-free conditions encountered in a space environment.

Heat input patterns similar to those anticipated for a spacecraft are imposed upon the test mirror. These thermal inputs can be produced by a number of individually controlled electric heater pads located at the mirror periphery and back surface. Provision could be made to simulate solar heating if desired.

Temperature Sensing Instrumentation

The temperatures at the mirror surfaces are measured by the use of thermocouples and thermistors mounted directly on the mirror surfaces. Each test mirror can be monitored with about 100 five-mil, or smaller, copper-thermocouples and its differential temperatures measured to an accuracy of better than 0.1°C.

Figure 294 is a photograph of the back face of the OAO-C mirror with the temperature sensors in place. Only about a third of the total number of sensors mounted on the mirror are visible. The experimentally measured thermal contour map and the sensor locations are shown in Figure 295.

Displacement Sensing Instrumentation

A scatterplate interferometer^{*}, used to measure mirror surface deformation, is mounted at the top of the test tank close to the mirror center of curvature. A small corner cube attached to the center of the mirror will permit monitoring the separation distance between the mirror front surface and the scatterplate by means of a ranging interferometer^{**} attached to the same framework as the scatterplate head. Figure 296 shows the general relationship of the mirror, interferometers and mounts.

* Publication of The Perkin-Elmer Corporation: New Developments in Interferometry. Symposium held at Norwalk, Connecticut, Perkin-Elmer Corporation, Norwalk, Connecticut, September 21, 1967.

** Minkowitz, S. and Smith-Varrig, W.R.: The Perkin-Elmer Model INF-1 Laser Interferometer. Perkin-Elmer Publication, 1967. A summary description of the operation of the scatterplate interferometer is also given in Appendix E.

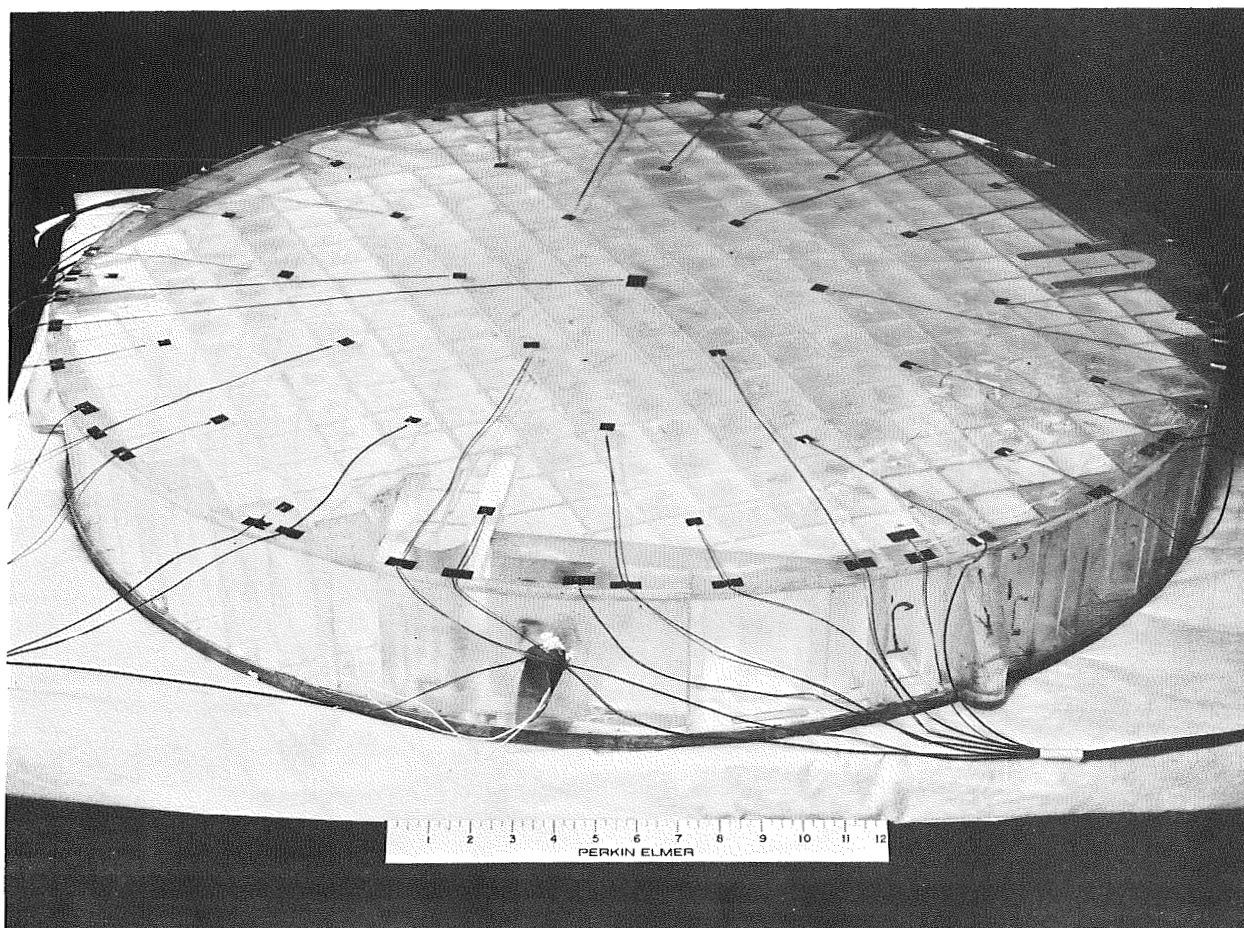


Figure 294. Sensors on Back of Mirror

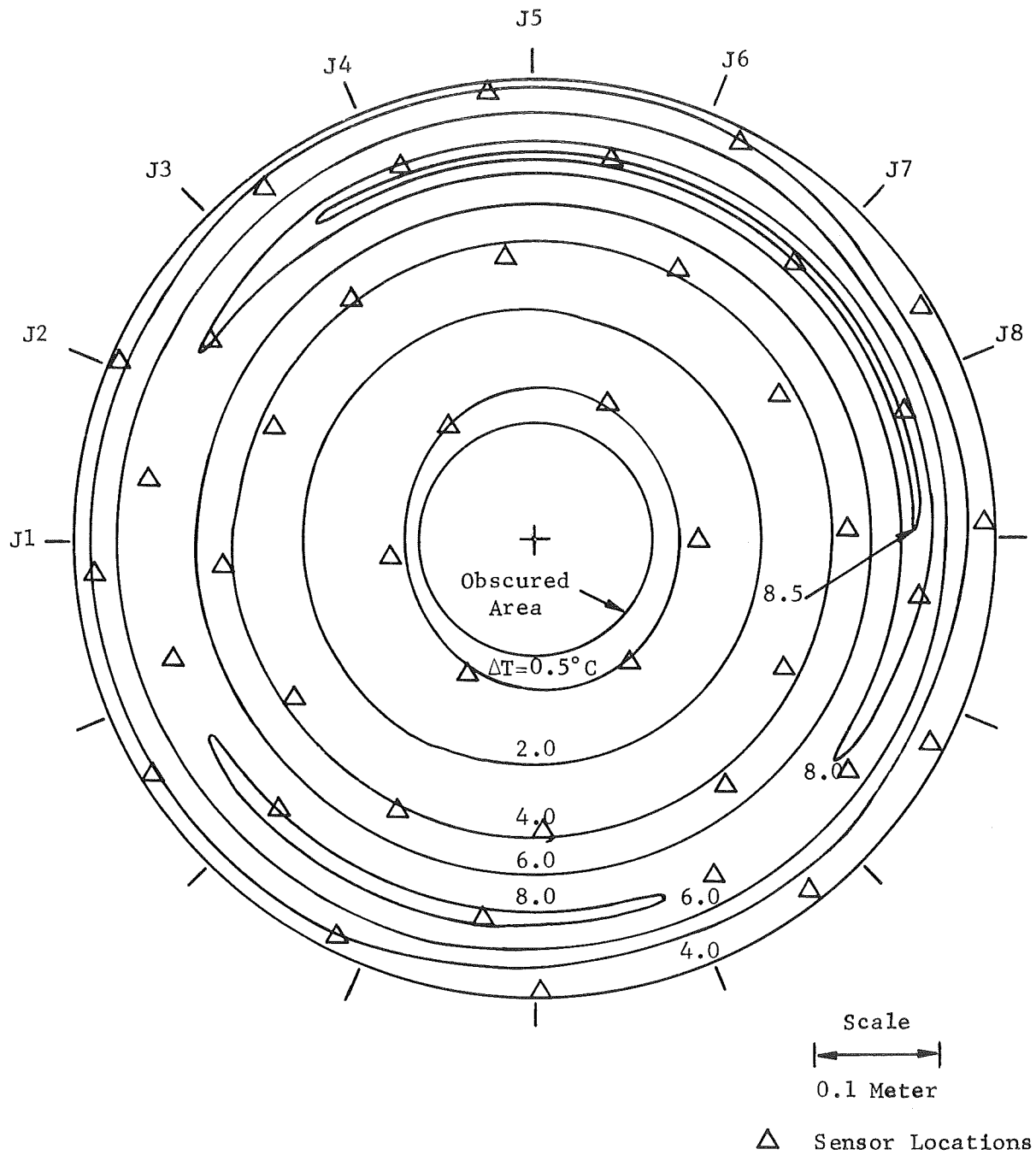


Figure 295. Thermal Contour Map of Back Face of OAO-C Mirror with (Parabolic) Radial Gradient Impressed on the Mirror

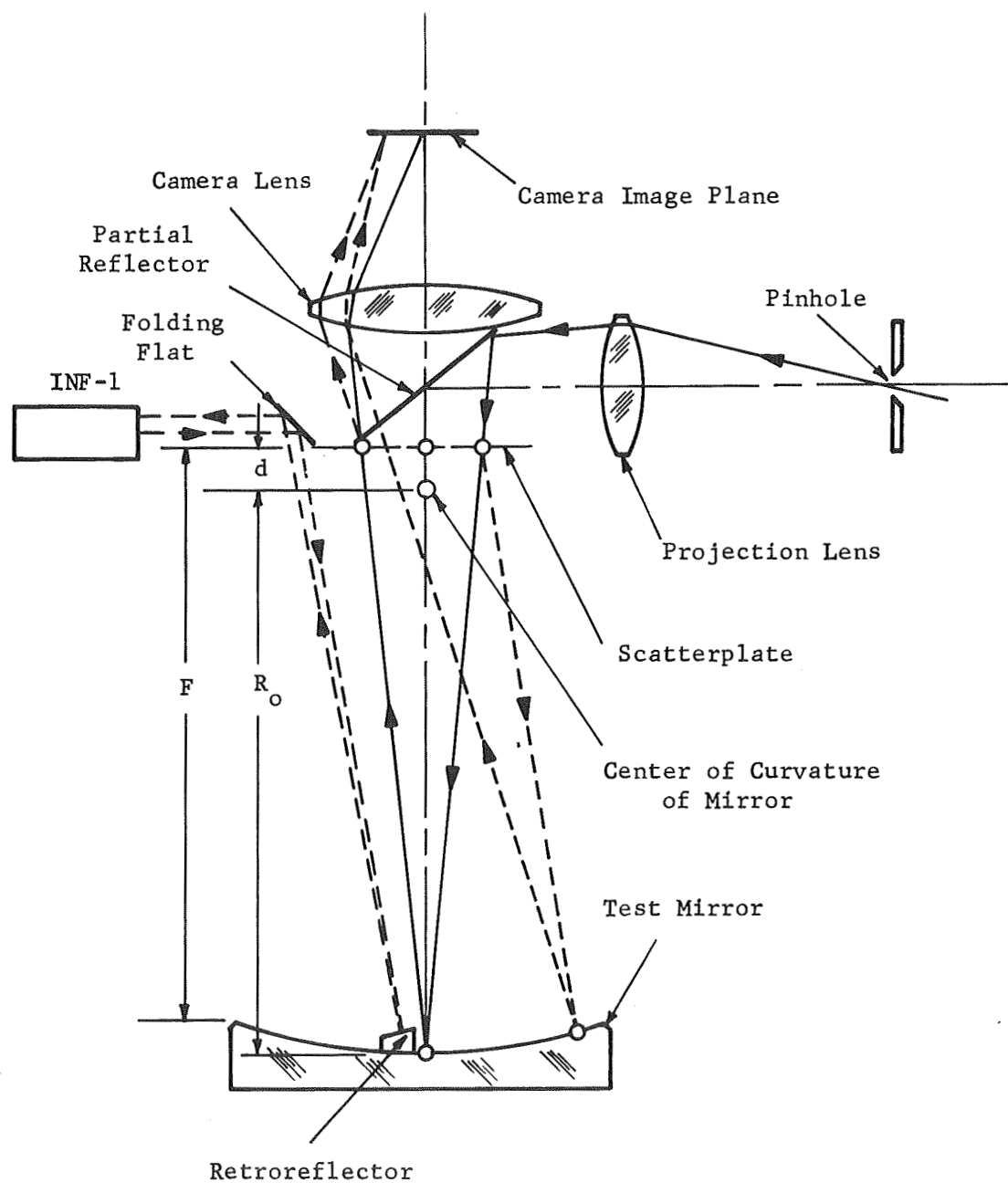


Figure 296. Optical Schematic of Test Mirror and Interferometers

Burch Scatterplate Interferometer

The technique of scatterplate interferometry is illustrated schematically in Figure 377 in Appendix E.

An example of the resulting interference pattern is shown in Figure 297. As described below, the radial position of these fringes is measured and a computer then generates a contour map of the errors in the surface (Figures 298 and 299). The computer program is used to determine the rms figure tolerance of the best fitting parabola; to evaluate the Optical Path Differences (OPD) across the mirror surface; and to optimize the distance between the plane of the scatterplate and the mirror center of curvature.

Path Length Laser Interferometer

The change in mirror radius of curvature as a function of thermal changes in the mirror itself is quite important, since the first-order effect in any rise or drop in the temperature of a mirror is a focal shift due to a slight change in curvature of the mirror surface.

The scatterplate interferometer can be used to determine the position of best focus for the mirror; however, a means for correcting for variations of the test tank length with changing temperature, pressure, or other external effects must be provided. This may be accomplished to an accuracy of $\lambda/2$ with a laser ranging interferometer. Only measurements relative to some initial or starting condition are necessary since, in all cases, the focus shifts are referred back to a reference mirror condition at the initial isothermal state. A path length laser interferometer such as the Perkin-Elmer INF can be mounted to the same reference bulkhead as the scatterplate, and a corner cube is mounted to the test mirror. This arrangement permits continuous monitoring of the optical path difference between the mirror and scatterplate.

A detailed description of the INF-1 ranging interferometer is given in Minkowitz.*

Data Processing

Displacement data, recorded in the form of a scatterplate interferogram such as seen in Figure 298, consists of a large number of dark and light "concentric" circles on the surface of the mirror. The fringe pattern locations are determined precisely; the location data are fed to a computer than scans the data for errors and adjusts readings to allow for mirror tilt and displacements. The program then computes and prints out the equation of the "best-fit" parabolic surface (least residual OPD's) and the rms figure value, and traces the OPD profiles of the eight mirror diameters (relative to the best fit surface) as shown in Figures 298 and 299.

* Minkowitz, op. cit. (See reference on page 574.)

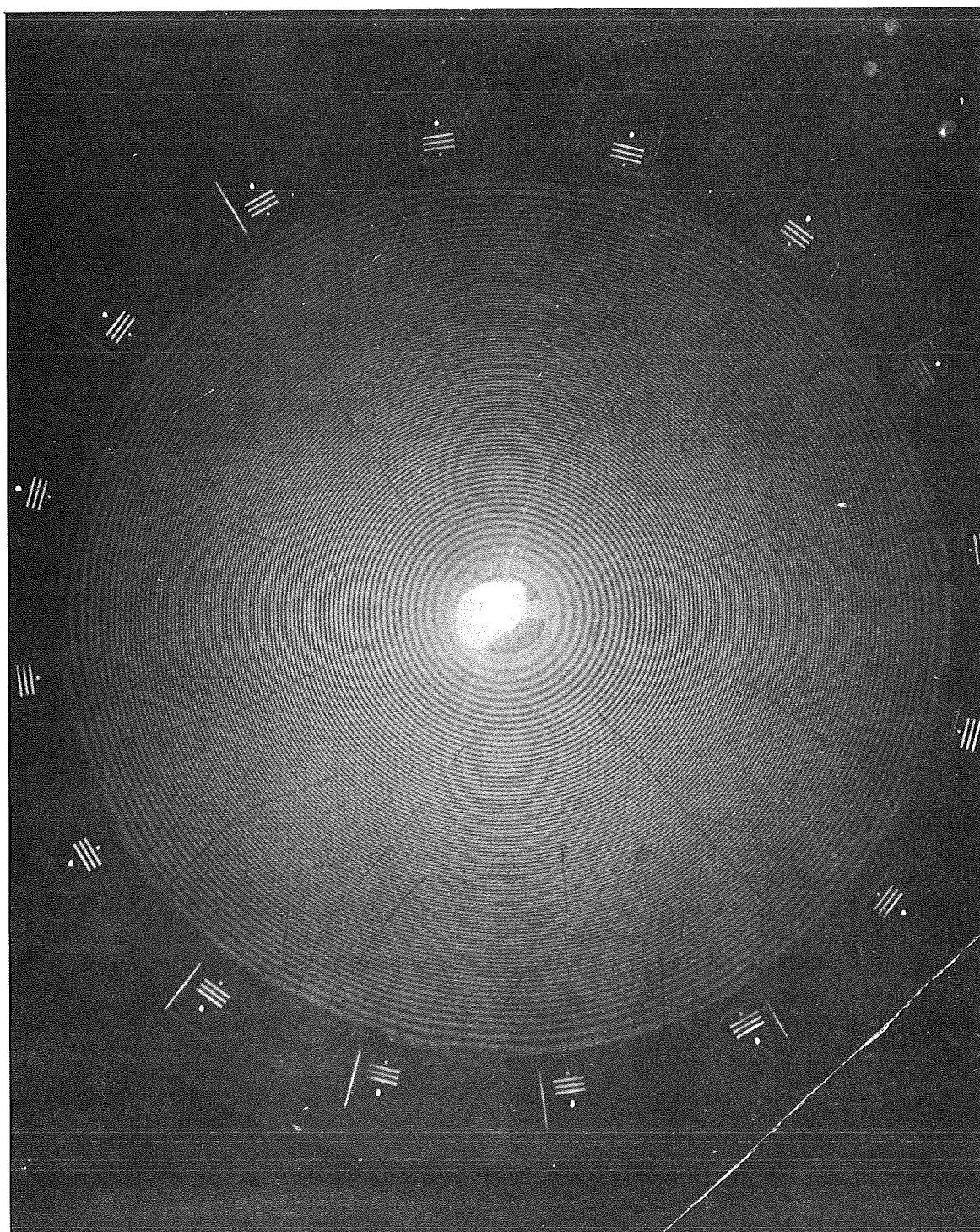


Figure 297. Interferogram of a Scatterplate

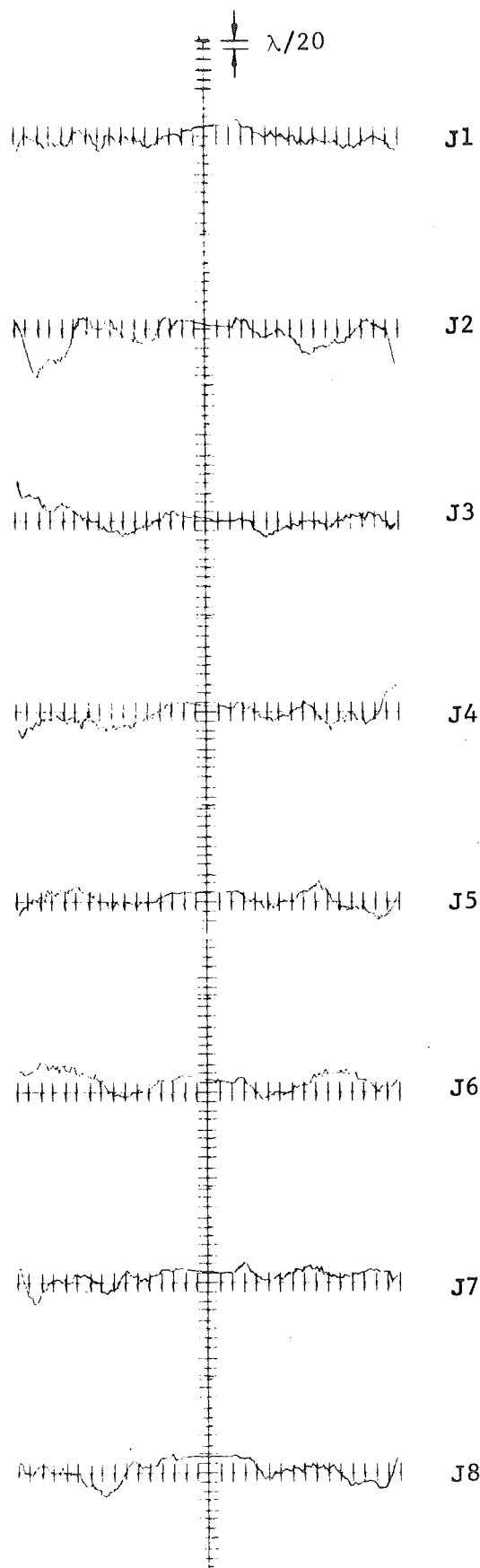


Figure 298. OPD Plot of Perkin-Elmer Mirror with Radial Gradient

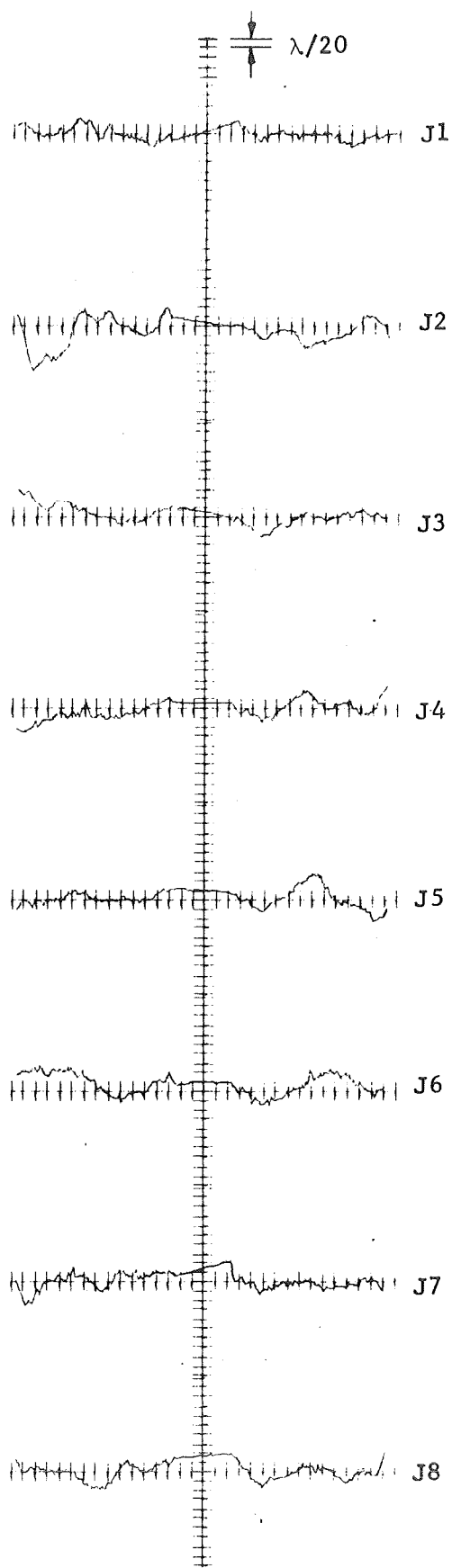


Figure 299. OPD Plot of Perkin-Elmer Mirror in Isothermal Condition

A reference interferogram is taken with the mirror in an isothermal condition prior to each test. A quantitative assessment of the effects of their thermal gradients is obtained by a comparison of the OPD figure deformations and best-fit parabola between the reference and the heated conditions.

Analytical Model

The analytical model is defined using the simplest approach that can lead to a set of results representative of the actual physical condition. Some of the potential methods for representing the mirror that could have been considered, in order of increasing complexity, are:

Membrane
Thin Plate
Thick Plate
Thin Shell
Thick Shell
Multi Shell

Since the membrane analogy cannot deal with axial temperature gradients, thin plate theory has been used and, thus far, has provided an acceptable mirror model.

Solution of Equations

The complex mirror structure is considered as a flat plate composed of an isotropic material with "effective" elastic constants. The thermoelastic deformation equations for the analytical model,* based on thin plate theory, have been programmed for computer solution.

Analytical predictions have been limited herein to deformations resulting from axi-symmetric temperature gradients. Non-axi-symmetric temperature disturbances do not lend themselves to simple generalizations without extensive qualifications. A summary of the postulated gradients and the resulting displacement vector components follows:

TABLE 34. TEMPERATURE GRADIENTS AND RESULTING DISPLACEMENTS

<u>Temperature Gradient</u>	<u>Displacement</u>	<u>Comment</u>
Radial	Axial W_1	See Figure
Radial	Radial U_2	See Figure
Axial	Axial W_3	See Figure
Axial	Radial U_4	See Figure
Axial	Axial W_5	See Figure

* Boley, B. and Weiner, J.H.: Theory of Thermal Stresses. Wiley, New York, 1962.

The radial displacements, U_2 and U_4 , are considered only as they imply an axial displacement, at a fixed radius, due to the curvature of the mirror. In the case of a parabolic surface, the associated axial displacement is

$$W = -\frac{r}{R} u$$

The total displacement W_T at a point is a summation of these five contributions

$$W_T = \sum_{i=1}^5 W_i$$

Once a thermal profile has been hypothesized and the point-by-point deflections are evaluated, a best-fit parabola is determined for the resulting mirror profile. The computer also calculates the rms degradation from curvature due to the temperature profile.

Theoretical Results

The usefulness of the technique is illustrated in the predicted deformations for the 0.8-meter OAO-C fused silica eggcrate mirror. Specifically, as summarized in Table 35, the (a) and (b) sketches show negligible rms deformation for uniform front-to-back temperature differences. The focal shift, however, is in excess of 40×10^{-6} meters/ $^{\circ}\text{C}$. Thus, the mirror, while bending under the influence of an axial gradient, tends to maintain a parabolic shape.

Temperature gradients increasing in the radial direction show a greater focus shift than do temperature gradients of the same magnitude decreasing radially. (See sketches (c) and (d).) The same statement applies to parabolic temperature profiles as shown in sketches (f) and (g). Both the focus shift and the rms degradation exhibit an essentially direct correlation with the magnitude of the temperature perturbation. (See sketches (d) and (e).)

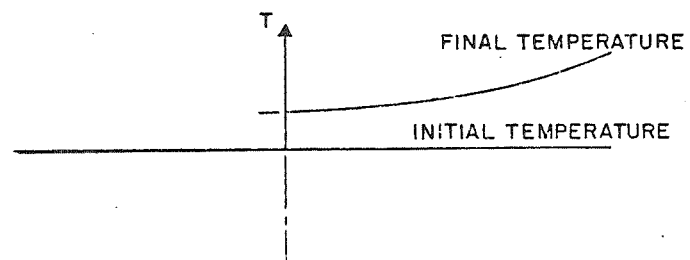
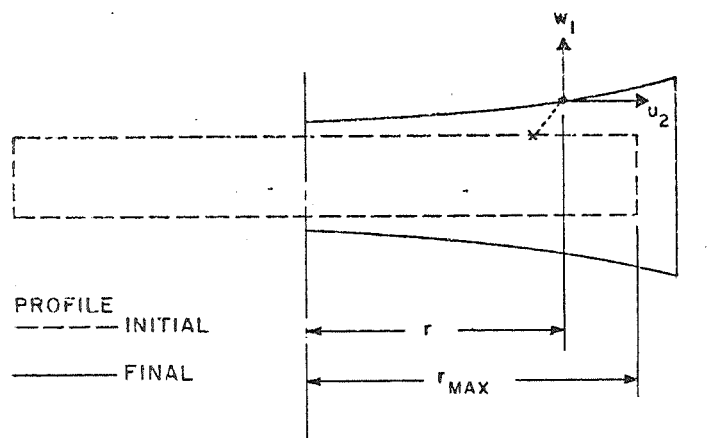
ANALYTICAL MODEL EVALUATION AND COMPARISON

A thermal contour map is shown in Figure 301. Measured temperature data points from both the back and front mirror faces are fed to the computer in conjunction with the thermoelastic deformation program and the mirror configuration and material constants.

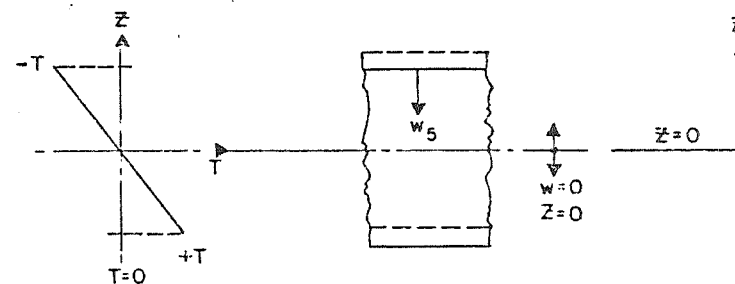
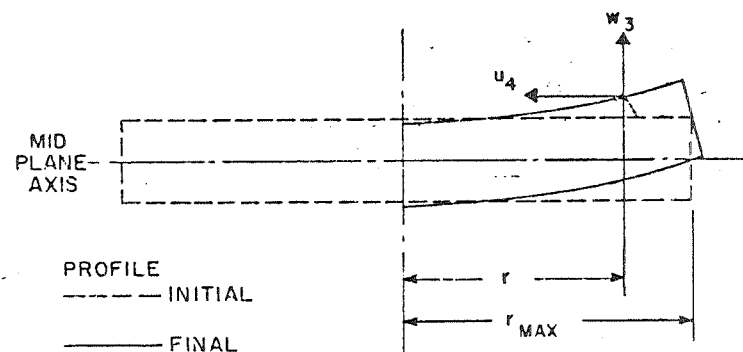
The computer uses a Lagrangian interpolation procedure to interpolate the temperature data as required, and calculates the component deflections and the total axial deflection.

The computer subsequently refits a best-fitting parabolic surface to the deformed surface by means of a least squares analysis, and brings out the residuals. In addition, it computes the rms degradation from the ideal surface and the change in radius of curvature of the mirror.

A comparison of experimental and analytical results for the OAO-C mirror for several thermal profiles is shown in Table 36.



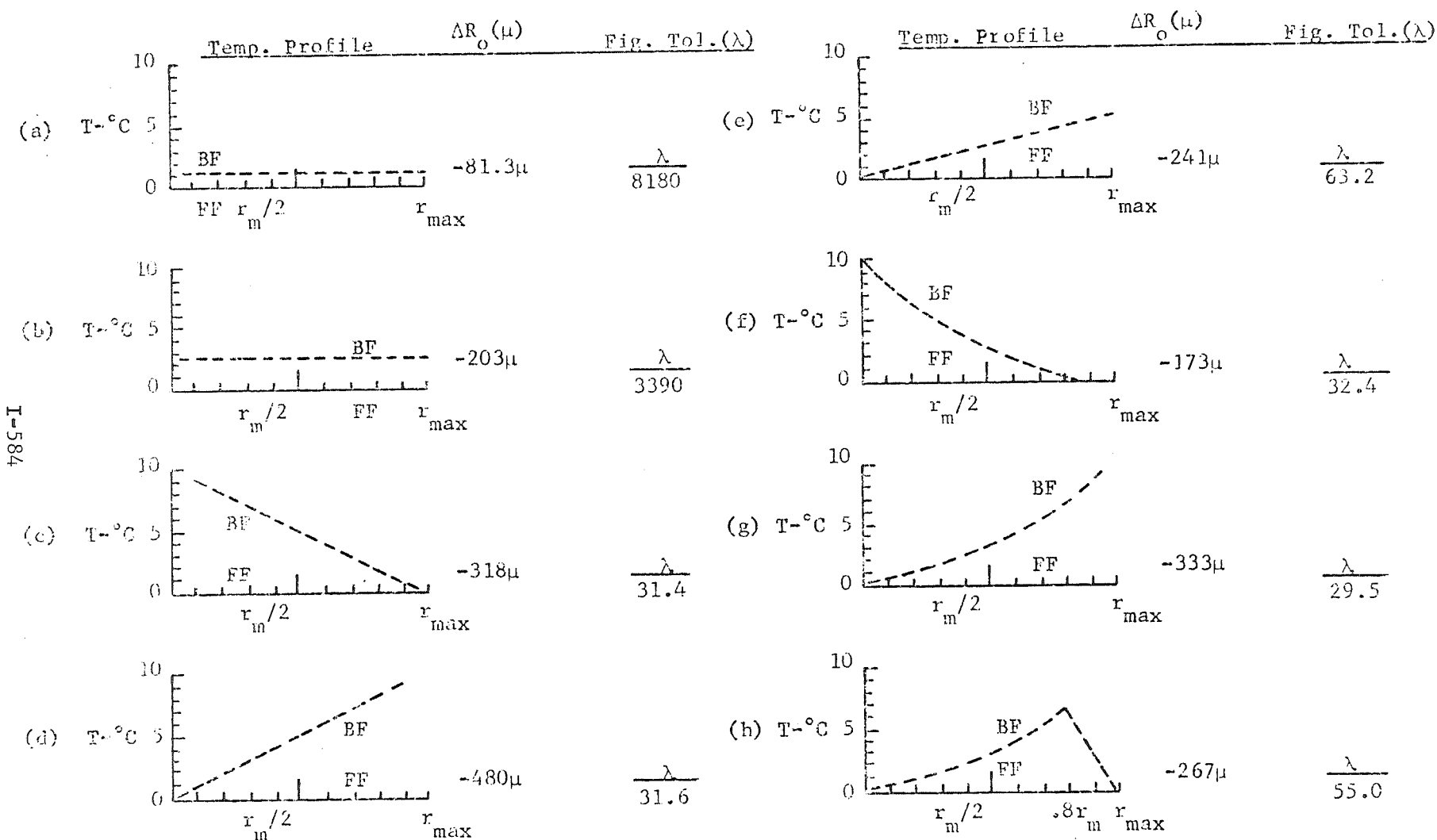
a. Radial



b. Axial

Figure 300. Temperature Gradients

TABLE 35 . SUMMARY OF PREDICTED THERMOELASTIC DEFORMATION
FOR THE OAO-C MIRROR



LEGEND

BF = BACK FACE $r_m = 0.4$ METERS $\nu = 0.03$ $R_o = 5.4$ METERS
FF = FRONT FACE $t = 0.1$ METERS $\alpha = 5.5 \times 10^{-7} ^{\circ}\text{C}^{-1}$ $\lambda_{\text{LASER}} = 0.6328 \times 10^{-6}$ METERS

I-585

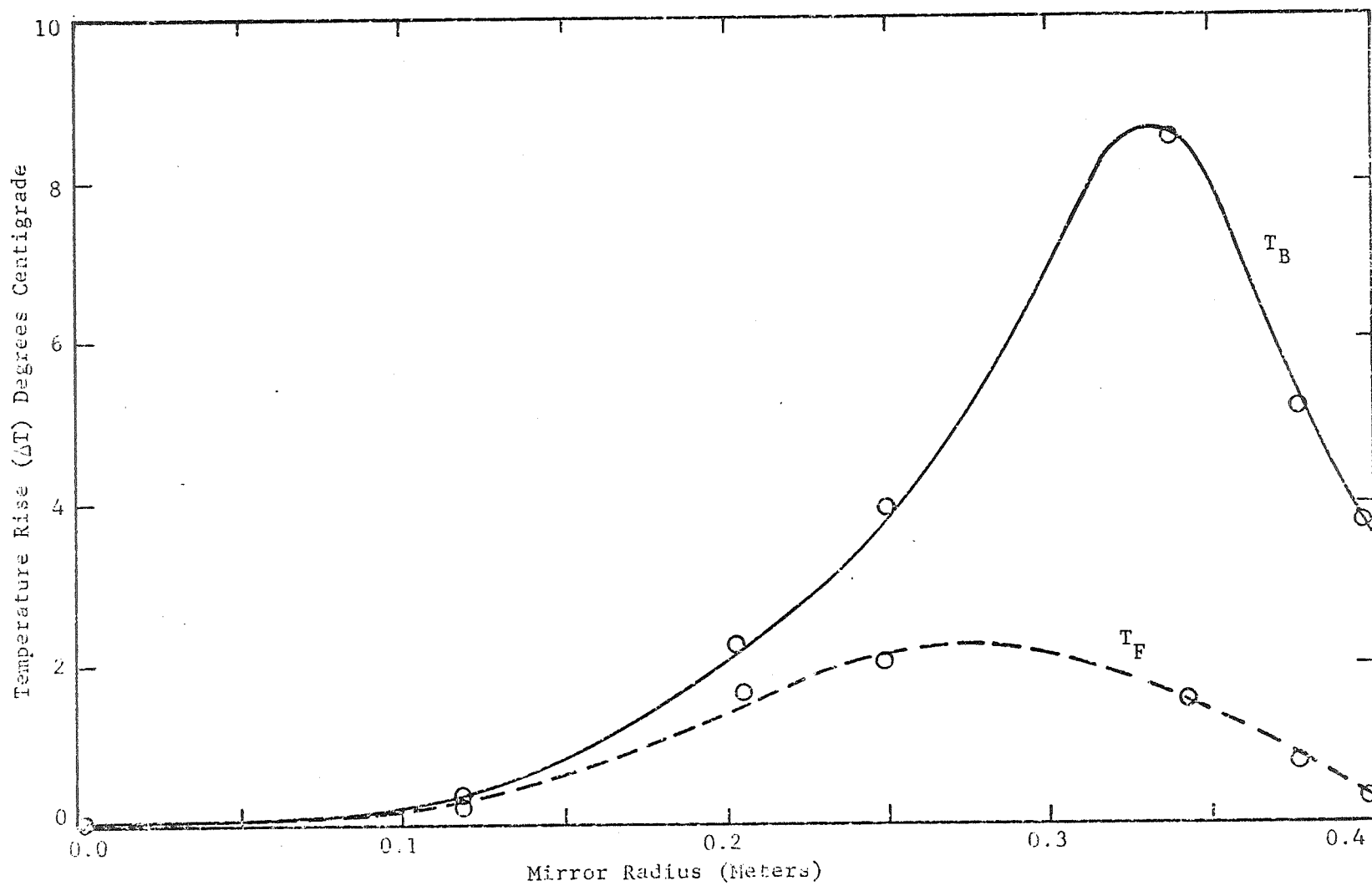
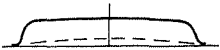
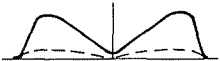
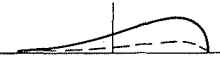


Figure 301. Temperature Profile of Mirror Sector Running from Center to Edge

TABLE 36. COMPARISON OF EXPERIMENTAL AND ANALYTICAL RESULTS

Temperature Distribution			Thermally-Induced RMS Deformation (Waves) $\lambda = 0.6328$ Micron		Focus Shift (Microns)	
Profile	Gradient	Max ΔT	Experimental	Analytical	Experimental (2)	Analytical
1 	Axial	0.8°C 2.8°C	0.006 0.017	0.010 0.013	- 40 -160	- 35 -130
2 	Radial (Central) Heaters)	3.0°C 7.0°C 12.0°C	0.011 0.015 (1) 0.025	0.013 0.028 0.053	- 75 -100 (1) -150	- 5 - 10 - 15
3 	Radial (Para- bolic; Edge Heaters)	8.0°C	0.021	0.021	-180	-140
4 	Radial (Linear)	8.0°C	0.022	0.021	-280	-200
5 	Lateral	5.0°C 11.5°C	0.020 0.028	No Data No Data	-125 -125	No Data No Data

 Mirror Rear Surface
 Mirror Front Surface

(1) Central mirror area unscattered observed during experiment due to laser light.

(2) Corrections for test tank length changes. (50 microns/°C) result in excellent agreement.

MIRROR DEFORMATION RESULTS

The following conclusions have been reached as a result of the program. Note that the conclusions are believed generally valid even though experimental verification is available only for the 0.8-meter eggcrate fused silica mirror.

1. Mirror surface deformations due to temperature changes are small. The rms changes due to various temperature gradients as large as $1^{\circ}\text{C}/\text{cm}$ are generally much less than 0.04λ .

2. Focus shifts of 250 microns for a $0.5^{\circ}\text{C}/\text{cm}$ axial temperature gradient are observed despite the compensating effect of accompanying radial gradients (due to edge effects, etc.).

3. An area-weighted average of the axial gradient can be used to predict the probable change in focus assuming the relationship

$$\Delta f = 2\alpha f^2 (\overline{\Delta T}/T)$$

4. Measurement errors were less than 0.1°C and 0.01λ on a relative basis.

5. The theoretical model, based on elementary thin plate theory, agrees well with experimental results.

MIRROR MATERIAL CONSIDERATIONS

Dimensional Stability

In order for an optical system to record the maximum information about an object under observation the optical system must be "diffraction limited". In a reflective system, diffraction-limited performance requires that each element of the surface be within approximately $\pm 0.033\lambda$ (λ = wavelength) of the desired geometrical surface, i.e., the peak-to-peak deviation should be less than 0.06λ . For visible light (λ = 5000 angstrom units), this means the surface contour must be correct to its geometrical form to within 0.6 microinches or 40 atom layers.

For the large aperture mirrors it is very difficult to manufacture and test the mirror surfaces to these stringent specifications. Once manufactured into useful elements, the materials of construction must be dimensionally stable to maintain the surface shape or figure under the operating environment. As operating requirements become more critical, whether due to size or resolution, the influence of dimensional stability becomes an increasingly important factor.

Types of Dimensional Instabilities

There are two basic types of dimensional instabilities. The first is thermally induced while the second is caused by the spontaneous release of stored energy.

The thermally-induced instabilities can be subdivided into thermal gradient and isothermal cases. The thermal gradient case is created by a differential thermal load, as, for example, that produced by solar heating of a mirror. This, in turn, heats the rest of the optical system by radiation and/or conduction. Generally, the thermal energy eventually flows into the mirror mount (which may be considered as a heat sink). The thermal gradient through a mirror associated with this heat flow causes the mirror to deform. Alleviation of deformations caused by thermal gradients can be realized by using materials with low thermal expansivity and/or high thermal conductivity.

The isothermal case refers to situations where no thermal gradient exists, but the temperature throughout the system has changed homogeneously from the nominal operating or figuring temperature. Degradation in the performance of an optical system will occur if the thermal expansion coefficient is not homogeneous over the entire volume of the mirror. This isothermal degradation has been observed in some materials to the extent that the performance of a mirror manufactured and tested at room temperature is drastically reduced when operated at an elevated or reduced temperature. Figure 302 presents interferograms of several beryllium samples with different manufacturing histories. The samples were equilibrated at the indicated temperatures prior to taking the interferograms. Thermal deformations may be reversible, i.e., the mirror returns to its original shape when the thermal load is removed, or irreversible depending on the amount of deformation.

The second type of dimensional instability is the temporal relaxation of stored chemical and/or mechanical energy. Stored energy may be associated with improper thermal processing history, phase changes, or mechanical working, e.g., grinding. In general, the release of stored energy will result in a shape change which is large with respect to the optical tolerances discussed earlier. Temporal instabilities have degraded the performance of some optical systems beyond usefulness in time spans far shorter than the design lifetime of those systems.

Materials Selection

The specifications for many optical systems limit the choice of mirror materials to those which have (a) the requisite dimensional stability in a changing thermal environment, (b) are known to exhibit a high degree of temporal dimensional stability, (c) have previously been fabricated (including polishing) to the desired tolerances, and (d) are commercially available in requisite sizes. The major technical factors pertaining to the selection of mirror materials are outlined in the following sections.

Criteria for Mirror Materials

In making a comparison of mirror materials, it is not possible to define a single figure of merit derived from the material properties that allows an absolute choice to be made from existing mirror materials. The comparison made

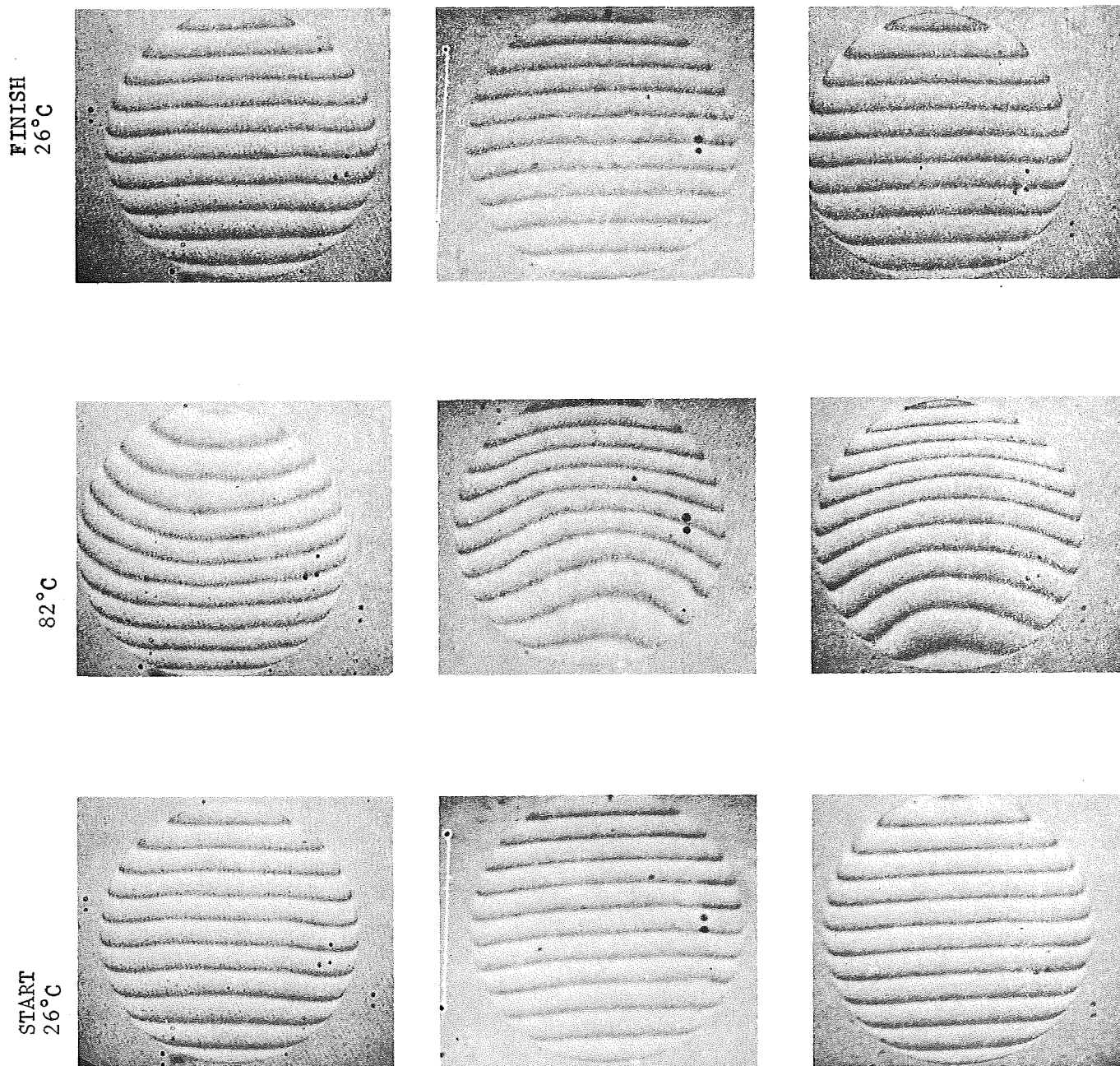


Figure 302. A Review of The Dimensional Stability of Beryllium as an Optical Material

here uses four criteria for mirror materials based upon imbalances in the mirror support structure, radiant dissipation of heat from the mirror, conductive dissipation from the mirror and the thermal diffusivity of the material.

The criterion for deformations of a mirror blank due to its support structure is the term E/ρ , where E is the modulus of elasticity and ρ the mirror density. This term is chosen from the equation for the deformation of a simple beam supported at both ends.

The amount of thermoelastic distortion accompanying a given temperature distribution is an extremely complex analytical problem for any but the simplest physical configurations and temperature distributions. However, in situations where radiation rather than conduction plays the dominant role in dissipating the heat from anomalous temperature regions, and where a time-dependent situation exists so that the volumetric heat capacity of the material is important, then $1/Cp\alpha$ is a suitable mirror material criterion. For a dynamic situation involving conduction as the main heat transport mechanism then $K/Cp\alpha$ appears to be a suitable criterion.

The thermal diffusivity K/Cp is analogous to the reciprocal of the time constant of an electrical circuit and a large value of thermal diffusivity indicates that the mirror can adjust to a new thermal environment in a relatively short time.

Mechanical Considerations

The weight and deformation of a mirror due to gravity or inertial loads are important considerations in the design of optical systems. Table 37 gives the stiffness-to-weight ratio (E/ρ), mirror thickness and the weight of a 1-meter diameter mirror (1 meter was selected as a typical segment dimension) in the form of a right circular disk which will not deflect more than 0.06λ in a one-G gravity field when mounted on three equispaced point supports 35 cm from the center. The minimum thickness for each material has been computed consistent with the assumed model. The weights given in Table 37 for solid mirrors are conservative, of course, because weight reductions can be realized through the use of lightweight (i.e., hollow) structures. However, while the weight of a specific mirror can be reduced significantly by ingenious design, the relative weight for different materials will be about as shown in Table 37.

On the basis of stiffness-to-weight ratio, beryllium is outstanding, being a factor of 2.4 better than silicon which is second best in this regard. Beryllium also has a yield strength of 40,000 psi (not shown) which is considerably higher than the 24,000 psi yield strength of silicon or the tensile strength of fused silica which varies between 7,000 and 10,000 psi.

TABLE 37. WEIGHT AND THICKNESS OF SOLID MIRRORS 1 METER IN DIAMETER
WITH A SELF WEIGHT DEFLECTION OF 0.06 MADE OF SELECTED
MATERIALS

<u>Material</u>	<u>E/ρ cm x 10</u>	<u>Thickness cm</u>	<u>Weight</u>	
			<u>In Pounds</u>	<u>Relative to Beryllium</u>
Pyrex	3.0	29.2	1140	2.91
Fused Silica	3.4	27.4	1050	2.68
ULE	1.5	28.5	1090	2.78
CerVit	3.7	26.1	1130	2.89
Beryllium	16.6	12.4	391	1.00
Aluminum	2.73	30.6	1420	3.63
Silicon	4.9	22.8	945	2.41
Invar	2.3	33.3	4500	11.50

Thermal Considerations

Thermal gradients in a mirror can seriously deform the optical surface. To demonstrate this we shall analyze the mathematically simple case of a thermal gradient through the thickness of a flat mirror which causes the mirror to become convex when viewed from the hot side. By assuming the undeformed surface to be a perfect plane and the deformed surface to be approximately a spherical section, the sagitta becomes a direct measure of the deformation.

Mirrors of one meter diameter require the thickness tabulated in Table 38 to meet the deflection requirement. The thermal gradients shown in Table 38 will be developed in these mirrors if the mirror views the sun and absorbs 5 percent of the incident solar radiation of 0.14 watts/cm^2 . A temperature gradient induces a varying thermal dilation along the thickness of the mirror resulting in the sagittas shown in Table 39.

TABLE 38. THERMAL GRADIENT AND DEFLECTION OF 1-METER DIAMETER MIRRORS EXPOSED TO NORMAL INCIDENT HEAT FLOW OF 7×10^{-3} WATTS/CM²

<u>Material</u>	<u>Thermal Gradient (°C/cm)</u>	<u>Sagitta ($\times 10^{-6}$ cm)</u>
Pyrex	6.4×10^{-2}	401.00
Fused Silica	5.5×10^{-2}	34.50
ULE Glass	5.5×10^{-2}	2.06
CerVit	5.5×10^{-2}	2.07
Beryllium	4.5×10^{-4}	6.29
Aluminum	3.2×10^{-4}	8.42
Invar	6.3×10^{-3}	3.99
Silicon	5.1×10^{-4}	1.45

In terms of immunity to thermal distortion, silicon appears to be outstanding, having a sag value that is a factor of 10 less than that of aluminum and beryllium, and a factor of 30 better than that of fused silica. The coefficient of expansion of the type of Invar used in the comparison does not compensate sufficiently for the low heat conductivity to provide any obvious advantage.

The lifetime of a thermal transient is determined by the thermal diffusivity, D , of the mirror material. The magnitude of the thermal distortion is proportional to the ratio α/D . From Table 38 it is apparent that silicon, CerVit, and ULE are superior to the other materials with regard to deformations caused by thermal transients. On the other hand, metals achieve thermal equilibrium in less than one-hundredth of the time required for a dielectric material.

In situations where radiation is the chief heat transport mechanism as might be the case for very large glass eggcrate structures, the relative values of $1/C\rho\alpha$ indicate fused silica to be more advantageous than Pyrex by a factor of 10. The use of $1/C\rho\alpha$ as a mirror criterion does not appear justified in the case of other mirror materials.

The data in Table 39 provides a reasonably good comparison if the operating temperature range is close to that over which the coefficient of expansion was measured. In spaceborne systems, the operating temperature of mirrors and associated structures is often far below the nominal temperatures implied in Table 39 and, as a result, the coefficient of expansion can differ markedly. Both fused silica and silicon are such examples. The coefficient of expansion decreases with temperature and, in both cases, crosses zero and becomes negative. Figure 303 shows the variation of coefficient with temperature for a typical sample of fused silica (Englehard Industries, Inc., Hillside, New Jersey) and for a single crystal of silicon. A number of devitrified glass materials such as Ultra Low Expansion glass (ULE) and CerVit have recently become available. These materials can be tailored to hug the zero coefficient of expansion line over an extended temperature range and do not exhibit the severe scattering of earlier materials. A curve for one type of CerVit is also shown in Figure 303.

In conclusion, it is worth noting that, if a nominal operating temperature range is centered so as to correspond to the zero crossing for the expansion coefficient of fused silica or the zero crossing for one of the newer glass materials with a low coefficient, then the expansion coefficient at each end of the operating temperature range is likely to be reduced to about $1.0 \times 10^{-7}/^{\circ}\text{C}$. Even this low value does not provide adequate compensation for the very low thermal conductivity of glass when compared with silicon. At ambient temperatures, silicon offers more immunity to thermoelastic deformation by a factor of 5 than a glass having an expansion coefficient of $1.0 \times 10^{-7}/^{\circ}\text{C}$. At lower temperatures, the advantages of silicon become even more pronounced.

TABLE 39. THERMAL PROPERTIES OF SELECTED MATERIALS

<u>Material</u>	<u>Thermal Diffusivity $D, \text{cm}^2/\text{sec.}$</u>	<u>Thermal Expansion $\alpha, \times 10^{-6}/^{\circ}\text{C}$</u>	<u>$D/\alpha \times 10^{-3}$ $^{\circ}\text{C cm}^2/\text{sec.}$</u>
Pyrex	0.0045	3.2	1.44
Fused Silica	0.0080	0.5	14.50
ULE Glass	0.0080	0.05	155.00
CerVit	0.0080	0.10	74.00
Beryllium	0.4650	12.4	37.50
Aluminum	0.9200	23.9	38.20
Invar	0.0320	1.3	26.30
Silicon	1.000	2.3	427.00

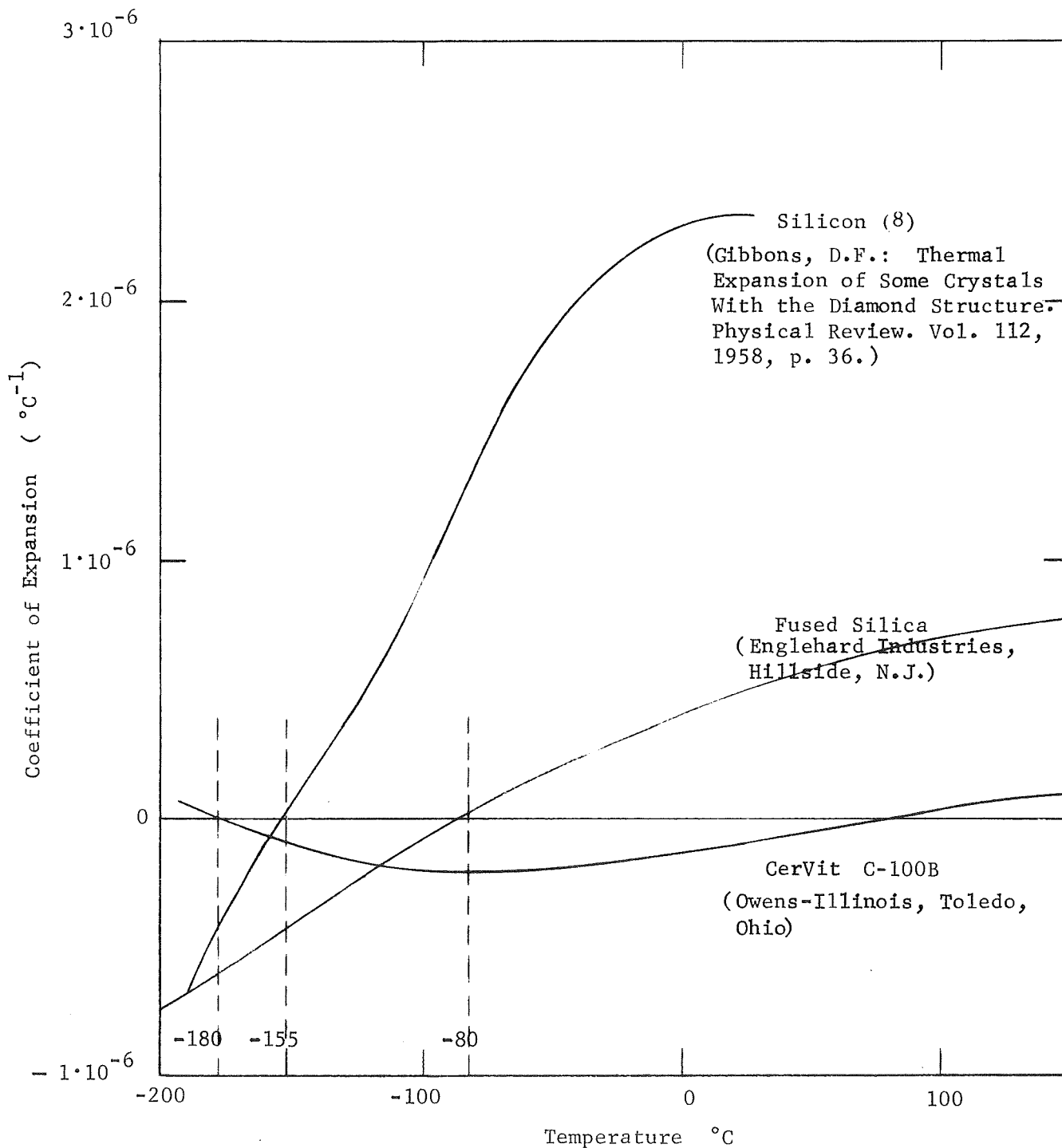


Figure 303. Coefficient of Expansion - Variation with Temperature

A BRIEF SUMMARY OF OPTICAL MATERIALS

The potential optical materials mentioned in the preceding section may be divided into two broad classes -- metals and dielectrics. A summary of the physical properties of potential mirror materials is included in Table 40.

Metals

The favorable properties of beryllium described previously were recognized and several mirrors of commercial vacuum hot-pressed beryllium have been manufactured. Initially, very erratic results in performance were reported and in some cases the optical surfaces of mirrors were observed to distort out of specification overnight in the optical polishing shop.

Presently, beryllium can be polished by conventional optical techniques although the time and labor efforts required to achieve a given surface specification is considerably greater than for fused silica. Figure 304 compares the reflectivity of bare beryllium, aluminum-coated beryllium and aluminum-coated glass over the spectral range 0.35 to 40 micrometers. Two points are significant. First, aluminum-coated beryllium has almost the same reflectivity in the visible portion of the spectrum as aluminum on glass. The few percent decrease is due primarily to the residual porosity in the beryllium. Second, in the far infrared portion of the spectrum, uncoated beryllium has the same reflectivity as aluminum so no coatings will be necessary for infrared applications.

Isothermal surface deformations of numerous beryllium samples have been measured over the range 0° to 80°C. Additional research is required to ascertain the behavior of beryllium in the 0°C to -80°C region.

Dielectric Materials

Several years ago the accepted material for high performance optical mirrors was fused silica. Two methods were available commercially at that time for manufacturing lightweight structures -- direct fusion and "soldering" employing a glass with a low melting temperature. New dielectric materials with a thermal expansion coefficient about an order of magnitude smaller than fused silica are under development by at least two manufacturers. The dimensional stability of fused silica is often used as a standard for comparison with these new materials.

Measurements indicate that fused silica, ULE, and CerVit are temporally stable to better than 0.003λ for periods of weeks. The dimensional stability of fused silica during thermal cycling also appears to be below the limit of instrument sensitivity. ULE and CerVit, however, show significant instabilities as the temperature is increased from 20°C to 90°C. In the case of ULE the instability appears to be directly attributable to chemical inhomogeneities. At the present time, it is not clear whether the instability observed in CerVit is due to bulk inhomogeneities or inappropriate surface preparation. It may be presumed, however, that technological evolution will produce improved dimensional homogeneity in both classes of materials.

TABLE 40
PHYSICAL PROPERTIES OF MIRROR MATERIALS

MATERIAL	DENSITY (ρ) gm/cm ³	MODULUS OF ELASTICITY (E) Newton/cm ²	THERMAL CONDUCTIVITY (K) Cal/cm sec °C	SPECIFIC HEAT (γ) Cal/ gm °C	COEFFICIENT OF EXPANSION (α) °C ⁻¹	E/ ρ	1/C ρ	K/C ρ	K/C ρ	YIELD STRENGTH psi
Fused Silica	2.2	7.0x10 ⁶	0.0033	0.188	0.55x10 ⁻⁶	3.18x10 ⁶	4400x10 ³	14.5x10 ³	0.008	7.800*
Pyrex-7740	2.35	6.8x10 ⁶	0.0027	0.250	3.20x10 ⁻⁶	2.89x10 ⁶	530x10 ³	1.44x10 ³	0.0045	
Aluminum	2.70	6.9x10 ⁶	0.53	0.215	23.9x10 ⁻⁶	2.56x10 ⁶	72x10 ³	38.2x10 ³	0.92	
Beryllium	1.82	28.0x10 ⁶	0.38	0.450	12.4x10 ⁻⁶	15.4x10 ⁶	99x10 ³	37.5x10 ³	0.465	40,000
Invar (36% Ni)	8.0	14.8x10 ⁶	0.026	0.095	1.30x10 ⁻⁶	1.85x10 ⁶	1000x10 ³	26.3x10 ³	0.034	
Silicon	2.33	13.0x10 ⁶	0.39	0.168	2.33x10 ⁻⁶	5.56x10 ⁶	1065x10 ³	427x10 ³	1.0	23,800
ULE	2.213	6.74x10 ⁶	0.00313	0.183	0.5x10 ⁻⁷	3.05x10 ⁶	49500x10 ³	155x10 ³	0.0079	
CerVit	2.5	9.23x10 ⁶	0.0040	0.217	1.0x10 ⁻⁷	3.7x10 ⁶	18400x10 ³	74x10 ³	0.008	

*The yield strength of glasses varies widely in tension due to the propagation at cracks. As a result a maximum working tensile stress is generally kept to about 1000 psi.

** At 20°C

*** All properties except those for silicon were taken from Scott, R.M.: Optical Engineering Applied Optics. Vol. 1, July, 1962, p. 387. Properties of silicon were obtained from Ballard, S.S., McCarthy, K.A., Wolfe, W.L.: State of the Art Report, Optical Materials for Infrared, Instrumentation. University of Michigan, Report Nos. 2389-11-S, January, 1959, and Supplement 2389-11-S, April, 1961.

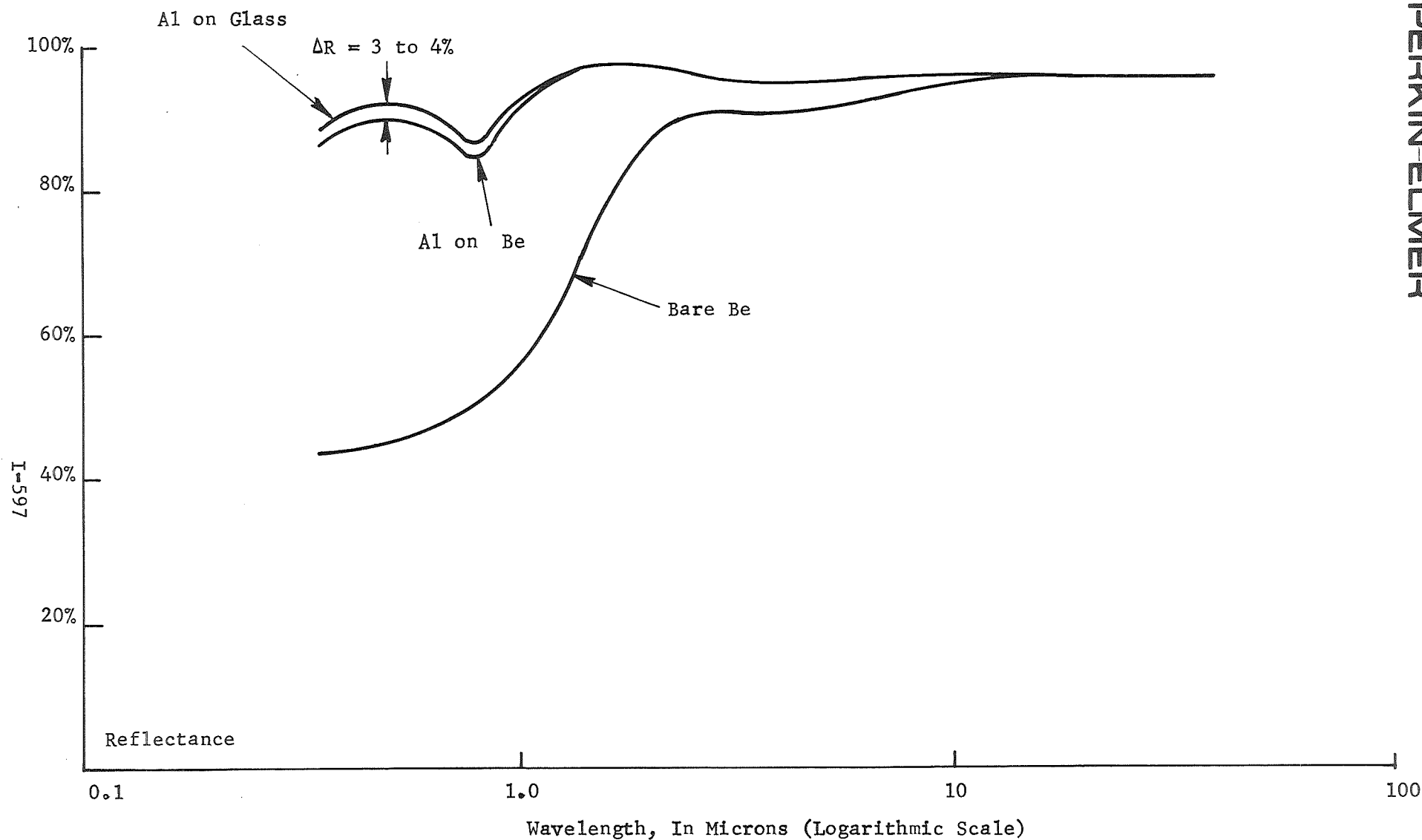


Figure 304. Reflectivity of Polished Beryllium, Aluminized Beryllium and Aluminized Fused Silica. The decrease in reflectivity of aluminized beryllium in the visible is due to scatter, not absorption.

Pure silicon has the high thermal diffusivity of a metal, as well as the low coefficient of expansion of the glass. Further, the material has a fundamental stability theoretically equivalent to that of the quartz materials so that a mirror, once fabricated, will not creep or warp into a distorted shape after a few years.

In the periodic table, there are two materials, diamond and silicon, that are most likely to combine the desirable thermal characteristics of a metal with the dimensional stability of fused silica. Diamond has obvious economic drawbacks; but silicon has already found considerable application as an optical material in the infrared region. Compared to fused silica, silicon has nearly twice the elasticity-to-weight ratio, three times the yield strength, and thirty times the immunity to thermal distortion. Even when compared to beryllium it appears to have advantageous thermal properties, being more immune to thermal distortion by a factor of eleven. High degree of isotropy and dimensional stability are indicated by the cubic crystal lattice of silicon and a substantial amount of experimental data that indicates that the dislocations in silicon remain immobile even under the influence of fairly high stresses at temperatures below 500°C.

The fabrication of large silicon structures is complicated by the very high freezing point (1420°C) and the extreme reactivity of molten silicon. Electrically active impurities such as boron, phosphorus, and aluminum have a marked influence on the optical properties of silicon. Optical quality material generally calls for concentrations of these impurities of less than 100 parts per billion and sometimes less than 20 parts per billion. Since even a small bit of impurity can contaminate a melt, extreme precautions in the handling of a silicon charge are required. These include etching of the crucibles with high purity reagents followed by rinsing with triply distilled water and the strict avoidance of any contact with human hands. To avoid oxygen contamination, silicon is usually melted in a vacuum or inert atmosphere. Fused silica appears to be the only useful crucible material but even fused silica is dissolved by molten silicon resulting in an increased oxygen content, as well as impurities such as boron, which are contained in the fused silica crucible. The oxygen can be detected by an absorption band at 9 microns. It apparently affects the degree of crystal perfection and the amount of strain in the final structure, although this is also dependent on the annealing process.

Since silicon expands about 10 percent on freezing, it cannot be cast in a conventional manner. Various techniques have been developed that allow the solidification zone to be moved from one side to the other or from top to bottom on a silicon casting. The step from the presently fabricated 12-inch diameter mirror to 40-inch diameter optics requires further development of these techniques or alternatively, fusing 12-inch castings together in a manner similar to that used for making large eggcrate mirrors of fused silica.

A two-phase project has been initiated to develop this material for future space telescope projects. Phase I of the project investigates the mirror material characteristics including the polishing techniques for a 12-inch mirror prior to the start of the Phase II efforts which would work towards a 40-inch silicon mirror.

The fundamental work required before logical initiation of the Silicon Mirror Project has been completed at Perkin-Elmer. A 12-inch silicon mirror was successfully polished, and the stability, resistance to thermal deformation, and polishing techniques established that there is the highest confidence that the 25-inch and 40-inch silicon mirrors will prove successful.

SUMMARY

The foregoing sections show that an analytical, experimental, manufacturing and testing capability has been developed in the thermal area. We believe that present thermal technology has the ability to:

- Provide sufficient design data to design, build and test large telescope optical components.
- Analytically predict and experimentally verify the effect of thermal gradients and excursions on optical components.
- Design a thermal system for a telescope that will maintain optical tolerances well within stringent limits.

CHAPTER 16 . HEAT PIPE FEASIBILITY FOR THE LTP*

INTRODUCTION

The heat pipe is a device that exhibits an effective thermal conductivity many times greater than the best known metal conductors. In its simplest form (shown below), the heat pipe consists of a pipe, a wick, and a working fluid.

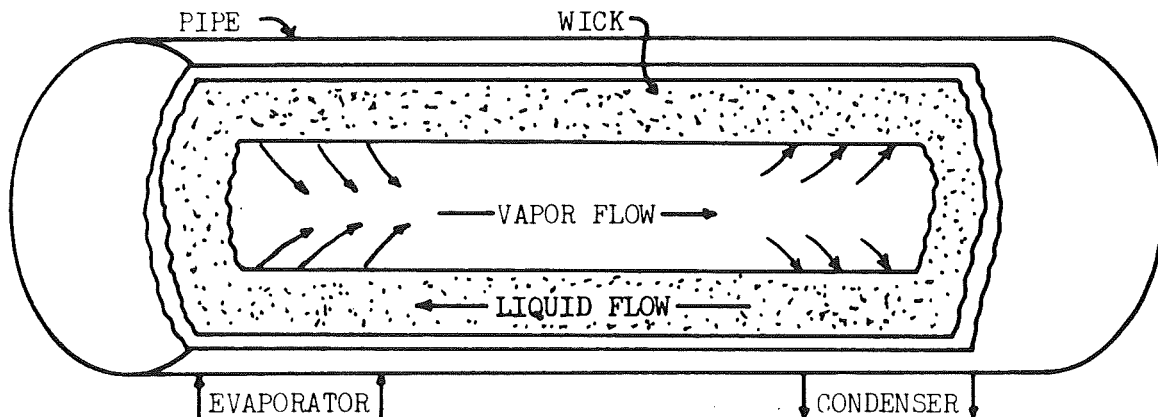


Figure 305. Basic Heat Pipe

The application of heat at one end (the evaporator section) of the pipe vaporizes the working fluid. This creates a small pressure difference in the work space and forces the vapor to flow toward the cooler end of the pipe (the condenser section). Upon reaching the cooler section of the work space, the vapor condenses, giving up its latent heat. The liquid is then returned to the evaporator section by the capillary forces in the wick.

The heat pipe has undergone considerable study and development since its rediscovery in 1964. However, the emphasis has been on high temperature applications with low temperature applications practically ignored. Consequently, a considerable amount of time was expended during this study in defining candidate heat pipe configurations, working fluids, and wicking materials that could be adapted to low temperature operation. In the thermal control of the two-meter telescope, it is required that the optical system be

* Ehrhardt, L.L., Jr. and Hartley, L.L.: Large Telescope Experiment Program, Final Report. Submitted to the Perkin-Elmer Corporation in fulfillment of sub-contract 62503-PN. Chrysler Corporation Space Division, Louisiana, October 28, 1969.

maintained at a uniform temperature in the neighborhood of -112°F (-80°C). Discussions and analyses contained in subsequent sections are directed toward the low temperature application of the heat pipe to the thermal control of a two-meter orbital telescope.

TECHNICAL STUDIES

System Requirements

The thermal control of an orbiting telescope requires the maintenance of temperature gradients and temperature uniformity with much greater precision than has previously been required for satellite and spacecraft. For example, the temperature of a satellite or spacecraft must be maintained within a few degrees Fahrenheit, while a telescope such as the one considered in this study requires control to fractions of a degree. Thermal control must be maintained while the telescope is subjected to varying heat loads resulting from the particular orbit, telescope orientation, and duty cycle.

The most common approaches to the thermal control of an orbiting telescope have involved the use of super-insulation and selected thermal control coatings. Super-insulation is used to isolate the telescope interior from the asymmetric environment resulting from solar and earth emitted and reflected radiation, and thermal control coatings are used to reduce the heat flux into the system. As an example, a cylinder with its longitudinal axis normal to the solar vector can have circumferential gradients in excess of 200°F .^{*} This, of course, can produce correspondingly high gradients on the interior of a telescope. The use of 1/4 in. to 1/2 in. of super-insulation in conjunction with a thermal control coating with a low α/ϵ (α - solar absorptivity, ϵ - infrared emissivity) in the order of 0.2 can reduce the circumferential gradient in the telescope to a value in the range of 10 to 20°F . This passive control approach, however, must take into account the following considerations:

- 1) The thickness of super-insulation required to reduce the telescope and mirror gradients to acceptable levels will result in non-controlled low temperatures on the interior of the telescope and the optics. These temperatures can vary from the range of -200 to the range of -100°F to -150°F , depending upon telescope orientation.**
- 2) Degradation of the thermal control coating, which is likely for the long lifetime planned, will result in eventual increased temperature gradients.

^{*} Wischnia, H.F.: Optical Technology Apollo Extension System. Perkin-Elmer Engineering Report No. 8900, October 1967.

^{**} Ibid., and Chrysler Corporation Space Division: Final Technical Report Optical Technology Apollo Extension System Phase A. Vol. V, Section V, Chrysler Corporation Space Division, October 1967.

- 3) The thermal inertia of the insulation will also result in longer stabilization times during transition from power off to power on modes, or changes in orientation.

These considerations, while not designed to negate the consideration of such passive control systems, do serve to emphasize the difficulties encountered when precise temperature control is attempted by passive means.

Operating Environment

The near-earth orbit of the telescope will result in its exposure to direct solar radiation, and to earth-emitted and reflected radiation. Additionally, the telescope will at times be in the shadow of the earth where it will be subjected to only the earth-emitted radiation. Since the heat pipe is a closed system with no external energy input, and no expendables, the design need not be concerned with periodic variations in the incident radiation. Instead, the design will be dictated by the extremes in the radiation environment experienced during the lifetime of the mission. The heat pipe must be designed to accommodate the maximum heat load it will experience, and the fluid must be selected to avoid freezing on the cold side of the telescope. In the heat pipe design presented later, this rationale was used to select the heat pipe configuration, working fluid, and wicking material.

In order to select a maximum heat load for the heat pipe design, it was assumed that the surface in contact with the heat pipe would be held at -112°F . In addition, the worst case was selected as the case where the longitudinal axis of the telescope is normal to the solar vector. Equation (1) was then solved to determine the maximum net heat absorbed by the surface. Since the surface temperature is assumed constant at -112°F , this absorbed heat load must be removed by the heat pipe.

$$\alpha_s F_s S - \epsilon \sigma T^4 = Q_{\text{abs.}} \quad (1)$$

where

α_s = solar absorptivity

F_s = form factor for direct solar radiation

ϵ = emissivity of telescope surface

σ = Stefan-Boltzmann constant, $0.1713 \times 10^{-8} \text{ Btu/hr-ft}^2\text{-}^{\circ}\text{R}^4$

T = temperature of surface, $^{\circ}\text{R}$

$Q_{\text{abs.}}$ = heat load to be removed by heat pipe, Btu/hr-ft^2

Solution of Equation (1) for a solar absorptivity, α_s , of 0.18, and an emissivity, ϵ , of 0.9, results in a maximum heat load of 57.2 Btu/hr-ft^2 . Since $Q_{\text{abs.}}$ varies from this maximum value at the point where the cosine of the angle between the surface normal and the solar vector equals 1, to 0 Btu/hr-ft^2 , where the cosine equals $0(90^{\circ})$, the integrated value of $Q_{\text{abs.}}$ will be approximately 36.5 Btu/hr-ft^2 . This value was used in subsequent analyses.

Telescope Requirements

Two basic requirements exist for the telescope. The first and foremost is that temperature gradients in the optics be as small as possible. Otherwise thermal deformations could be detrimental to the telescope's performance. Since thermal deformation is dependent on both temperature and temperature gradient, it is necessary to relate the temperature gradient to the average temperature. For the fused quartz mirror considered in this study, it was desired that an average mirror temperature of $-112^{\circ}\text{F}(-80^{\circ}\text{C})$ be maintained with thermal gradients not to exceed $0.9^{\circ}\text{F}(0.5^{\circ}\text{C})$.

The second requirement is that the telescope structure, and any internal mechanism, must either be protected from large circumferential temperature variation, or specially designed to compensate for those variations. Figures 306 and 307, taken from Perkin-Elmer Report No. 8900, referred to earlier, show the transient temperatures at four circumferential locations of the two-meter telescope tube as determined during a previous study. These curves show the influence of solar incidence angle, β , on temperature distribution around the telescope. The temperatures were computed for one orbit and thus do not represent equilibrium conditions.

The high effective thermal conductivity of the heat pipe results in its ability to isothermalize surfaces subjected to wide variations in thermal load. It is this unique attribute that has stimulated the interest in applying the heat pipe principle to the thermal control of orbital telescopes. A reduction of the circumferential temperature gradients in the telescope tube to approximate an isothermal surface would subject the internal optics to a more uniform environment.

Heat Pipe Design Considerations

As previously discussed, a heat pipe consists of a containment vessel, a working fluid, and a wick. The design of the heat pipe, and its associated components, is a function of the particular application planned. Once the maximum heat load and operating temperatures are selected, then the design becomes a matter of selecting the configuration, working fluid, and wicking material compatible with the requirements. However, since all of the factors are interrelated, the basic equations that describe a heat pipe's performance must be utilized to show the interrelationships. These equations are developed in this section.

The basis for most of the heat pipe design presently in use is the theory developed by Cotter.* This theory equates the pressure drop in the liquid phase (ΔP_L) and vapor phase (ΔP_V) to the difference between the capillary pumping pressure (ΔP_C) and the pressure difference resulting from the gravity field (ΔP_g):

*Cotter, T.P.: Theory of Heat Pipes. Los Alamos Scientific Laboratory Report LA-3246-MS (TID-4500).

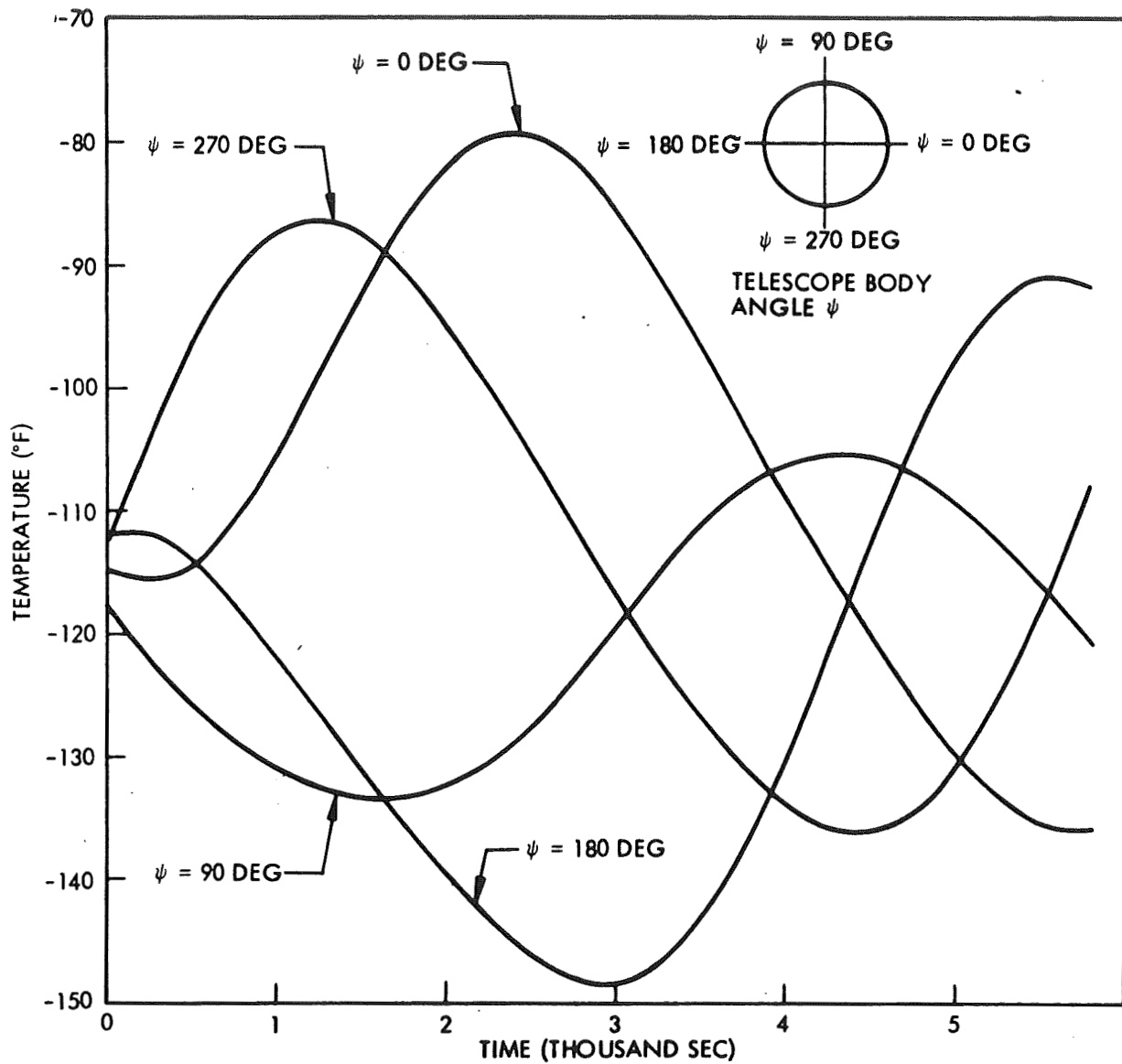


Figure 306 . OTES Section 1 Temperature History, $\beta = 0$ deg. 1 Orbit
(Perkin-Elmer Engineering Report No. 8900)

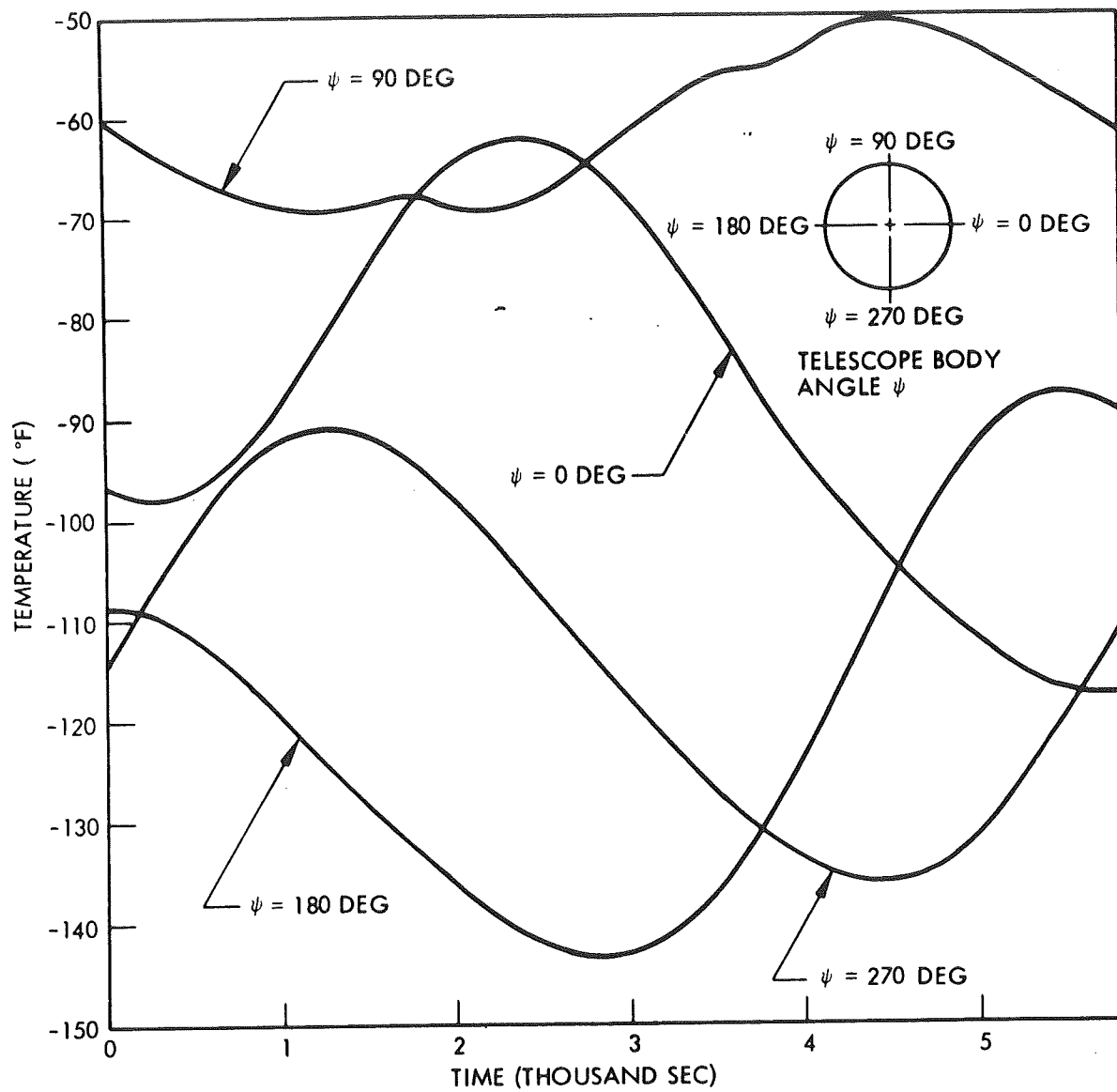


Figure 307. OTES Section 1 Temperature History, $\beta = 52$ deg, 1 Orbit
(Perkin-Elmer Engineering Report No. 8900)

$$\Delta P_c - \Delta P_g = \Delta P_L + \Delta P_v \quad (2)$$

For most applications the vapor pressure drop is negligible, and for this application there is also a zero gravity field. Therefore, Equation (2) reduces to:

$$\Delta P_c = \Delta P_L \quad (3)$$

The capillary pumping term, ΔP_c , can be expressed as

$$\Delta P_c = 2\sigma \left(\frac{\cos \theta_e}{r_e} - \frac{\cos \theta_c}{r_c} \right) \quad (4)$$

where

- σ = surface tension of liquid, lb_f/ft
- θ_e = liquid contact angle at evaporator
- θ_c = liquid contact angle at condenser
- r_e = effective radius of wick pore at evaporator, ft
- r_c = effect radius of wick pore at condensor, ft

The pressure drop in the liquid consists of the viscous losses and is defined by

$$\Delta P_L = \frac{\mu L' \dot{m}}{\rho K A} \quad (5)$$

where

- μ = viscosity of liquid $\text{lb}_f\text{-hr}/\text{ft}^2$
- L' = effective wick length, ft
- $\dot{m}$ = liquid mass flow rate, lb/hr
- ρ = density of liquid, lb_m/ft^3
- K = wick permeability, ft^2
- A = total cross sectional area of wick, ft^2

Substituting Equations (5) and (4) into Equation (3) and solving for mass flow rate yields

$$\dot{m} = \left(\frac{\rho K A}{\mu L'} \right) 2\sigma \left(\frac{\cos \theta_e}{r_e} - \frac{\cos \theta_c}{r_c} \right) \quad (6)$$

The maximum liquid flow rate will occur when the liquid contact angle in the condenser is a maximum, i.e., 90 degrees and the liquid contact angle in the evaporator is a minimum, i.e., 0 degrees. Equation (6) then yields

$$(\dot{m})_{\max} = \frac{2\sigma\rho KA}{\mu L' r_e} \quad (7)$$

In a heat pipe the heat transfer process is such that the heat transferred through the evaporation process predominates to the point where all other forms can be neglected. Therefore, the heat transfer rate can be expressed as

$$Q = \dot{m} h_{fg} \quad (8)$$

where Q = heat transfer rate, Btu/hr

h_{fg} = latent heat of vaporization, Btu/lb_m

Substituting Equation (7) into Equation (8) yields the following expression

$$Q_{\max} = \frac{2\rho\sigma K A h_{fg}}{\mu L' r_e} \quad (9)$$

Equation (9) defines the maximum total heat input that can be transferred by a heat pipe of a given configuration. The only quantity in the equation that cannot be readily determined is r_e , effective radius of the wick pore at the evaporator. However, when the evaporation rate is a maximum, that is, the contact angle, θ_e , equals 0° , the radius of curvature of the liquid-vapor interface, R_e , equals the effective pore radius, r_e . This effective radius of curvature is also the minimum possible. If the porous passages are treated as a single capillary of equivalent diameter, the surface tension acting upward will be balanced by the force of the column of liquid of length l_m . A hydrostatic pressure balance can be written as follows:

$$\rho g l_m = \frac{2g_o \sigma}{R_m} \quad (10)$$

where g is the acceleration due to gravity and g_o the proportionality constant in Newton's Second Law. Substituting Equation (10) into Equation (9) and grouping terms

$$Q_{\max} = \left(\frac{\rho h_{fg}}{\mu} \right) \left(\frac{KA}{L'} \right) \left(\frac{l_m g_o}{\sigma g_o} \right) \quad (11)$$

In Equation (11), the first term is a dimensional parameter which involves only fluid properties, while the other terms relate to the capillary pumping and viscous loss characteristics of the wick. This equation is used in the subsequent sections to evaluate candidate fluids and wicks, as well as to define a conceptual design of the heat pipe.

Working Fluids

The choice of a working fluid is largely dictated by the desired operating temperature of the heat pipe. In this case, since the desired operating temperature of the heat pipe was -112°F (-80°C), a search was initiated for fluids which had boiling points below 0°F . The search resulted in the fluids shown in Table 41. Water is included in this and subsequent tables for comparison. From Table 41, a second list was prepared based on the following criteria:

- a. Boiling point in the vicinity of -112°F .
- b. Freezing point low enough to avoid freezing of the fluid on the cold side of the telescope.
- c. Vapor pressure of fluid.
- d. Safety in handling.

The second group of fluids is shown in Table 42. Ammonia and nitrogen are included in this list because of previous use for heat pipe applications.

This smaller group of fluids was then evaluated for relative performance as a heat pipe fluid using the liquid transport factor, N . This factor is the first term shown in Equation (11) and repeated here for clarity

$$N = \frac{\rho \sigma h_{fg}}{\mu} \quad (12)$$

where

$$\begin{aligned} \rho &= \text{liquid density, lb}_m/\text{ft}^3 \\ \sigma &= \text{surface tension of liquid, lb}_f/\text{ft} \\ h_{fg} &= \text{latent heat of vaporization, Btu/lb} \\ \mu &= \text{viscosity of liquid, lb}_f\text{-hr}/\text{ft}^2 \end{aligned}$$

Table 42 shows the liquid transport factor for each of the fluids calculated at the fluid's normal boiling point. Since the parameters comprising N are a function of temperature, it is necessary that the value be determined over the anticipated operating range. Figure 308 shows the factor for the various fluids. The significant feature of Figure 308 is that all candidate fluids

TABLE 41. CANDIDATE WORKING FLUIDS

FLUID	FORMULA	MELTING POINT (°F)	BOILING POINT (°F)
Acetylene	C_2H_2	-114.	- 98.
Ammonia	NH_3	-107.9	- 28.3
Argon	A	-309.	-303
Carbon Monoxide	CO	-340.	-314.
Chlorine	Cl_2	-150.7	- 28.5
Dihydrodiborance	B_2H_6	-264.8	-134.5
Ethane	C_2H_6	-297.7	-126.9
Ethylene	C_2H_4	-272.5	-154.8
Fluorine	F_2	-367.	-305.
Methane	CH_4	-296.4	-260.
Methyl Chloride	CH_3Cl	-144.	-11.5
Nitrogen	N_2	-345.8	-320.4
Nitrous Oxide	N_2O	-130.3	-127.3
Oxygen	O_2	-360.4	-296.9
Perchloryl Fluoride	ClO_3F	-231.	- 52.3
Propane	C_3H_8	-305.	- 43.7
Propylene	C_3H_6	-381.	- 52.6
Refrigerant 12	CCl_2F_2	-247.3	- 22.
Refrigerant 13	$CClF_3$	-294.8	-114.
Refrigerant 14	CF_4	-298.6	-196.7
Refrigerant 22	$CHClF_2$	-256.	- 41.7
Refrigerant 23	CHF_3	-247.	-115.7
Refrigerant 503(59.9% R13, 40.1% R23)	---	-247	-127.6
Water	H_2O	+ 32.	+212.
Xenon	X_e	-170.	-162.

TABLE 42 . PROPERTIES OF SELECTED FLUIDS *

FLUID	FORMULA	MELTING POINT (°F)	BOILING POINT (°F)	DENSITY (lb _m /ft ³)	HEAT OF VAPORIZATION (Btu/lb _m)	SURFACE TENSION (lb _f -ft)	VISCOSITY (lb _f -hr/ft ²)	LIQUID TRANSPORT FACTOR (Btu/hr-ft ²)
Ammonia	NH ₃	-107.9	- 28.3	42.6	689.3	22.8 E-4	1.77 E-9	3.145 E+10
Nitrogen	N ₂	-345.8	-320.4	50.3	85.3	6.1 E-4	0.98 E-9	0.265 E+10
Propane	C ₃ H ₈	-305.8	- 43.7	36.3	182.7	10.2 E-4	1.18 E-9	0.570 E+10
Refrigerant 12	CCL ₂ F ₂	-252	- 21.62	92.8	71.0	11.1 E-4	2.1 E-9	0.348 E+10
Refrigerant 13	CCLF ₃	-294	-114.6	95.0	63.9	10.3 E-4	1.85 E-9	0.340 E+10
Refrigerant 23	CHF ₃	-247	-115.7	89.9	104.7	11.1 E-4	1.80 E-9	0.588 E+10
Refrigerant 503	(1)	-247	-127.6	94.5	74.21	13.0 E-4	1.82 E-9	0.501 E+10
Water	H ₂ O	+32	+212	59.8	970.4	40.3 E-4	1.65 E-9	14.170 E+10

(1) Chemical Formula - 59.9% Refrigerant 13
40.1% Refrigerant 23

(2) All properties at normal boiling point (14.7 psia)

* Chrysler Corporation Space Division: Final Technical Report Optical Technology Apollo Extension System
Phase A. Vol. V, Section V, Chrysler Corporation Space Division, October, 1967.

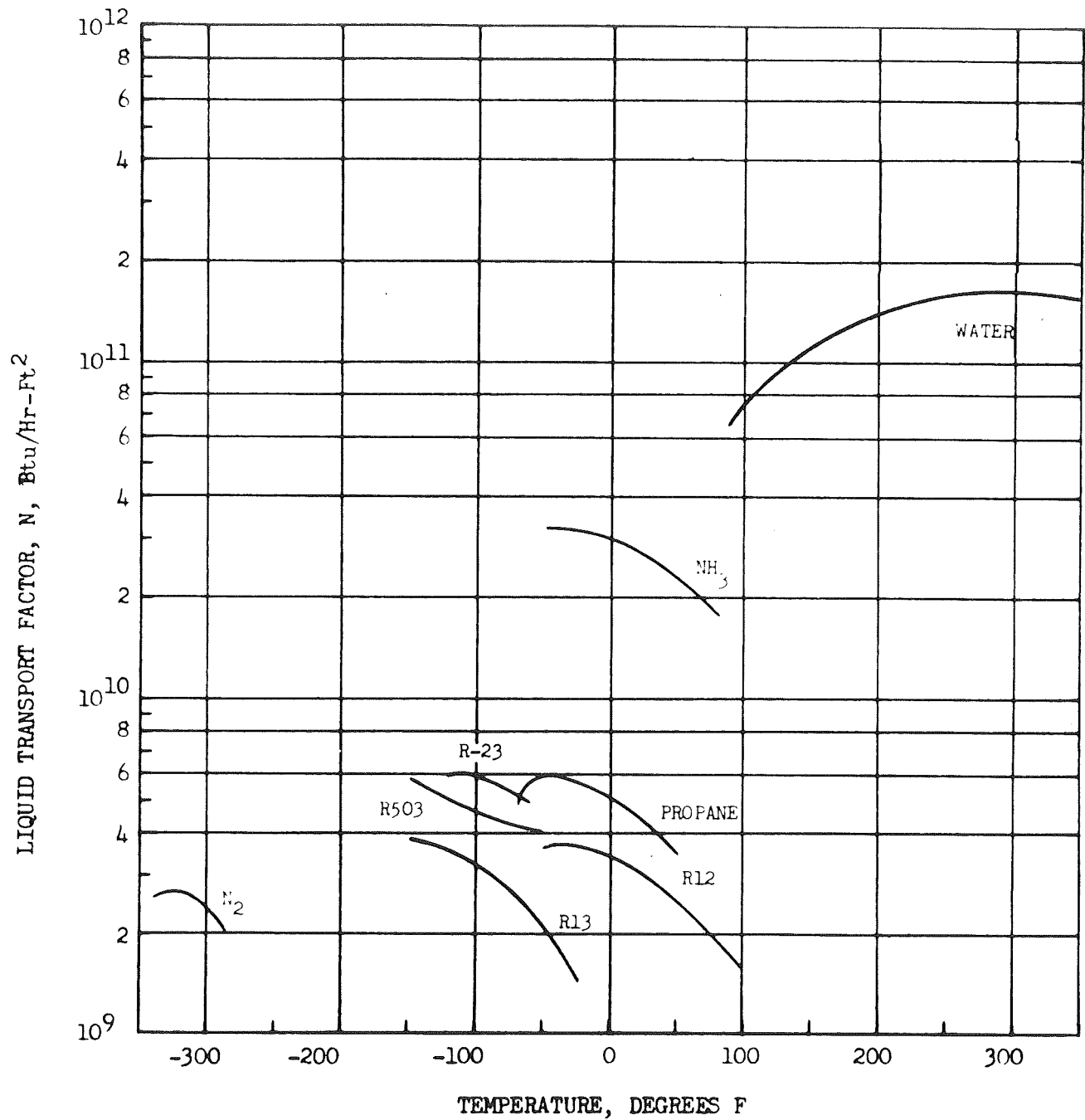


Figure 308. Liquid Transport Factor for Candidate Fluids

tend to cluster in the range of $N = 3 \times 10^{+9}$ to $N = 6 \times 10^{+9}$ Btu/hr-ft², while N for water has a value approximately one hundred times greater. Since the maximum heat transfer ability of the heat pipe is a direct function of the liquid transport factor, a water heat pipe can transfer approximately 25 times more heat than a Refrigerant 23 heat pipe of the same diameter and length, assuming all other factors equal.

As a result of this analysis, Refrigerant 23 was selected as offering the best performance in the proximity of -112°F. It has the highest value of N , a low freezing point, and offers very good compatibility with most common construction materials.

Wicking Materials

A considerable amount of research has been accomplished on wicking materials for heat pipe applications. Most of the research, however, has been accomplished with fluids that are liquids at room temperature. Equation (11) contains two parameters which relate to wick performance. These are the permeability of the wick (K) and the wicking height (l_m), both of which must be experimentally determined. Table 43 presents data from Langston and Kunz* on the performance of various wicking materials with water and Freon 113. No published data was found on wick performance with the candidate fluids previously identified. However, it should be noted that permeability is independent of the working fluid, and the values shown for permeability in Table 43 can be used in this study. The quantity l_m , defined as the maximum height that a liquid will rise in a vertical screen due to capillary action is a function of the particular fluid used. It is expressed in Langston and Kunz* as

$$l_m = \frac{4g_o \sigma_L \cos \theta}{g \rho_L D} \quad (13)$$

where σ_L = surface tension of liquid
 θ = liquid-vapor contact angle
 ρ_L = density of liquid
 D = pore diameter of wick

The ratio of the equilibrium heights for different fluids with the same wick can be determined from

$$\frac{l_{m1}}{l_{m2}} = \frac{\sigma_{L1}}{\sigma_{L2}} \frac{\rho_{L2}}{\rho_{L1}} \frac{(\cos \theta)_1}{(\cos \theta)_2} \quad (14)$$

* Langston, L.S., and Kunz, H.R.: Liquid Transport Properties of Some Heat Pipe Wicking Materials. ASME Prepring 69-HT-17, August 1969.

TABLE 43. TYPICAL WICK PROPERTIES*

WICK TYPE	FLUID	POROSITY	MEDIAN PORE DIAMETER, MICRONS	PERMEABILITY, $K, \text{ FT}^2$	WICKING HEIGHT, h_m INCHES	PERFORMANCE FACTOR, $K h_m$, FT^3
150-297 μ Nickel Powder	Water	65.8%	770	2.94 E-9	9.71	2.38 E-9
297-841 μ Nickel Powder	Water	54.0%	~ 80	0.87 E-9	>16.0	1.16 E-9
297-841 μ Nickel Powder	Water	69.9%	>80	3.22 E-9	6.90	1.85 E-9
50 Mesh Nickel Screen	Water	62.5%	>100	7.15 E-9	1.91	1.14 E-9
10 μ Sintered Nickel Fiber	Water	86.8%	43.8	0.472 E-9	>15.90	0.626 E-9
10 μ Sintered Nickel Fiber	Water	82.5%	35.0	0.362 E-9	>16.0	0.482 E-9
10 μ Sintered Nickel Fiber	Water	68.9%	26.5	0.163 E-9	>16.0	0.217 E-9
15 μ Sintered Nickel Fiber	Water	88.0%	40.7	0.331 E-9	>15.7	0.433 E-9
76 μ 430 S.S. Fiber	Water	82.2%	>80.0	12.5 E-9	5.32	5.54 E-9
297-841 μ Nickel Powder	Freon 113	54.0%	~ 80	0.87 E-9	>7.01	0.508 E-9
297-841 μ Nickel Powder	Freon 113	69.6%	>80	3.22 E-9	2.02	0.543 E-9
10 μ Sintered Nickel Fiber	Freon 113	86.6%	43.8	0.472 E-9	4.20	0.165 E-9

*Langston, L.S., and Kunz, H.R.: Liquid Transport Properties of Some Heat Pipe Wicking Materials.
ASME Preprint 69-HT-17, August 1969.

Since the maximum wicking height will occur when the liquid vapor contact angle is 0 degrees, the cosine term can be dropped from Equation (14). While this will indicate the theoretical ratio of equilibrium heights, experimental data, such as that shown in Table 43, will vary considerably from theoretical values. This variation in l_m can result from differences in the cleaning process, experimental procedure, and contact angle hysteresis.

It should be pointed out that heat pipe performance is very sensitive to fabrication procedures. Wick performance is dependent on the degree of wick compaction, which affects the wick permeability, and the cleanliness of the wick, which affects its wicking ability. This makes it imperative that the performance of a particular wick-fluid combination be established experimentally very early in the design phase of a project.

Configurations

In selecting potential configurations, two basic approaches are available. The heat pipe can be used to isolate the interior of the telescope from the environment, or it can be used to remove temperature gradients from the optics and the interior of the telescope.

The basic philosophy followed in selecting configurations for investigation was to furnish an isothermal surface for the optics to view. Figure 309 shows the variation in the mirror form factor with distance from the mirror. As shown in Figure 309, more than 95 percent of the view from the mirror to the telescope wall comes from nodes less than four radii (two diameters) from the mirror (for equal temperatures). Therefore, temperatures in this section of the tube will have a dominant influence on mirror temperature. The form factor out the end of the telescope is very small, and consequently, has a very small influence on mirror temperature.

Studies reported on in this section were made to identify the various configurations and the trade-offs, advantages, and disadvantages of each approach. Selection of a particular configuration for further development will be reported in the section on conceptual design.

Configuration Number One

Figure 310 shows one approach that has been investigated. A series of heat pipes are wrapped around the periphery of the telescope tube, thus reducing the surface to a series of bands or belts. The heat pipes transfer heat from the hot side and reject the heat on the cold side, thus reducing the circumferential gradient. An analysis was made to show the reduction that could result from the approach.

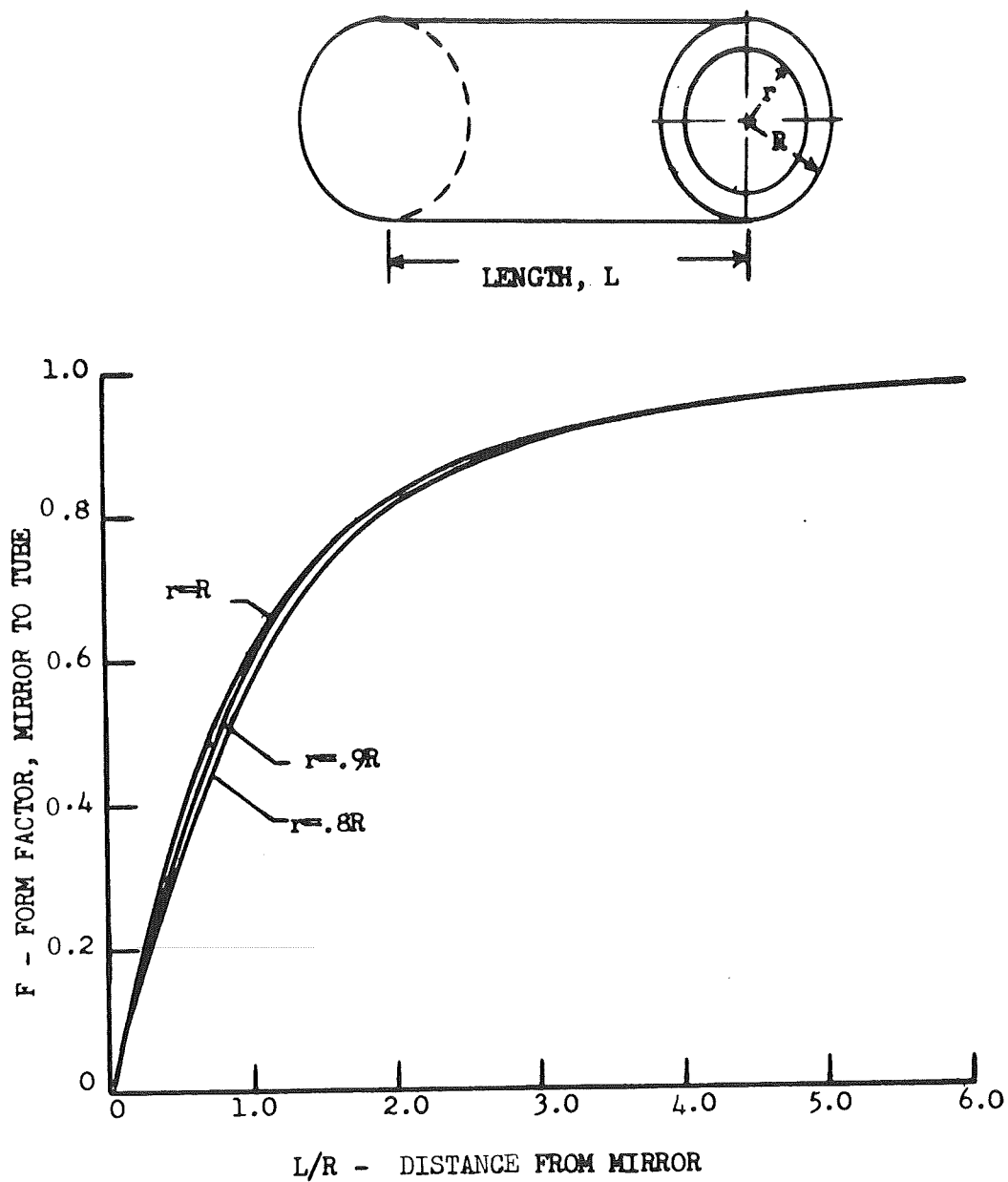


Figure 309. Mirror to Telescope Tube Form Factor

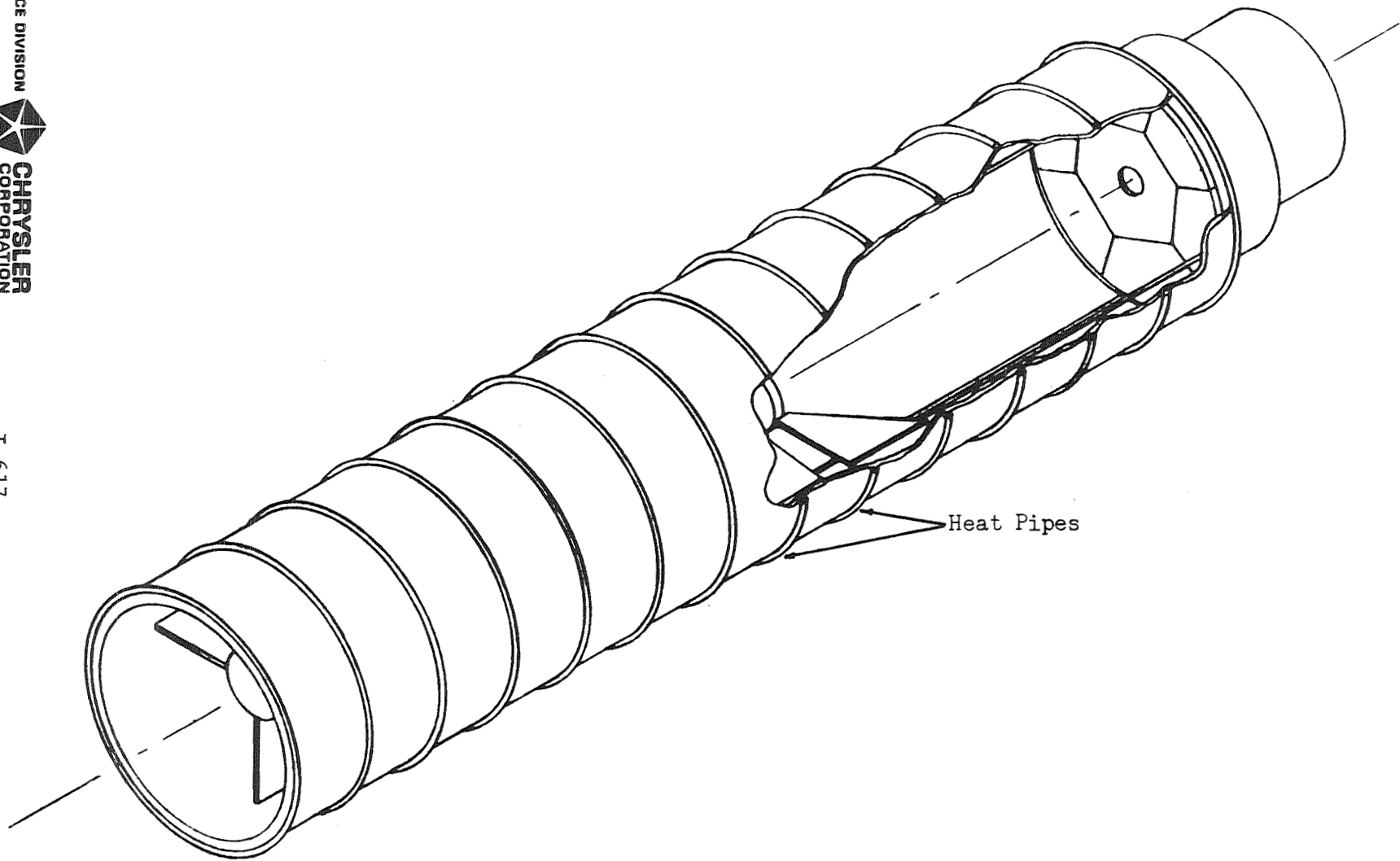


Figure 310 . Configuration Number One. Circular Heat Pipes Around Telescope Tube

Referring again to Figure 307, the temperatures shown at approximately 2750 seconds were used in this analysis. The heat pipes, operating at -112°F , are placed around the telescope, spaced 8 in. apart. Assuming equilibrium conditions, $\epsilon = 0.8$, no circumferential conduction, no internal radiation, and an aluminum skin thickness of 0.02 in., the reduction in circumferential temperature gradient was calculated. Resulting temperatures shown below in Figure 311 are at the mid-point between the heat pipes.

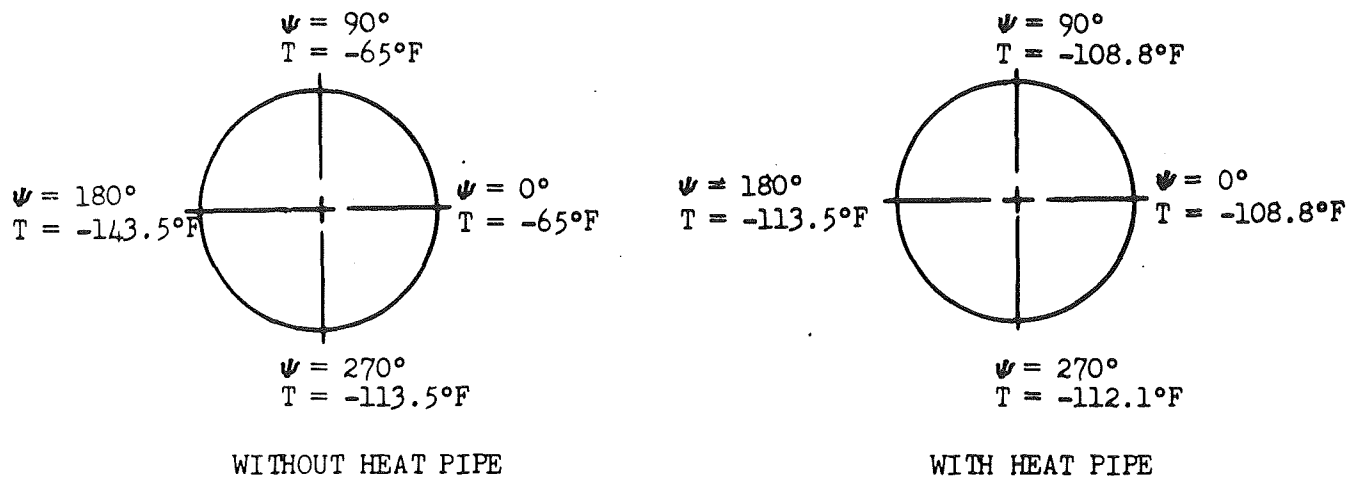


Figure 311. Temperature Gradient Reduction Resulting From Circular Heat Pipe

Putting the heat pipes closer together would further reduce the gradients, as would increasing the skin thickness.

Using configuration one, the center of each band would be at the heat pipe operating temperature with gradients toward the edge of the band. Longitudinal gradients, i.e., between heat pipes, can be reduced by increasing the thickness (δ) of the cylindrical surface, or by reducing the width (L) of the bands. An analysis was conducted to illustrate the effects of varying these two parameters. In the analysis it was assumed that the heat pipes (at $T = -112^{\circ}\text{F}$) were separated by a distance L (see Figure 312) and in contact with an aluminum band of thickness δ . It was further assumed that one side of the aluminum skin, with $\alpha/\epsilon = 0.2$, was exposed to solar radiation with the other side being adiabatic. Results, presented in Figure 313 show the maximum steady state temperature difference between the center and edge of the band as a function of L and δ . As shown, the circumferential heat pipe does have the potential of effecting significant reductions in the thermal gradients.

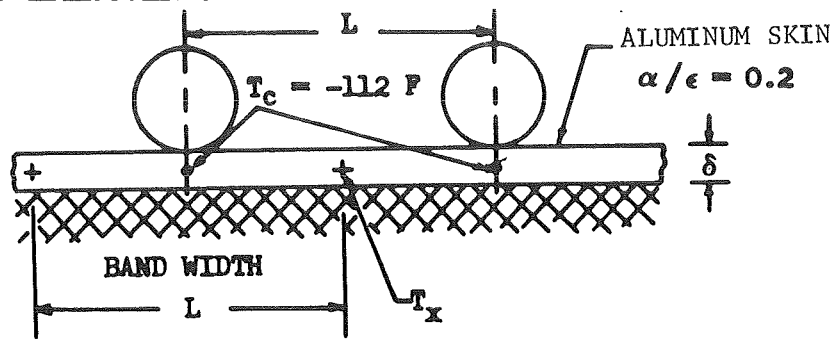


Figure 312 . Analysis Model

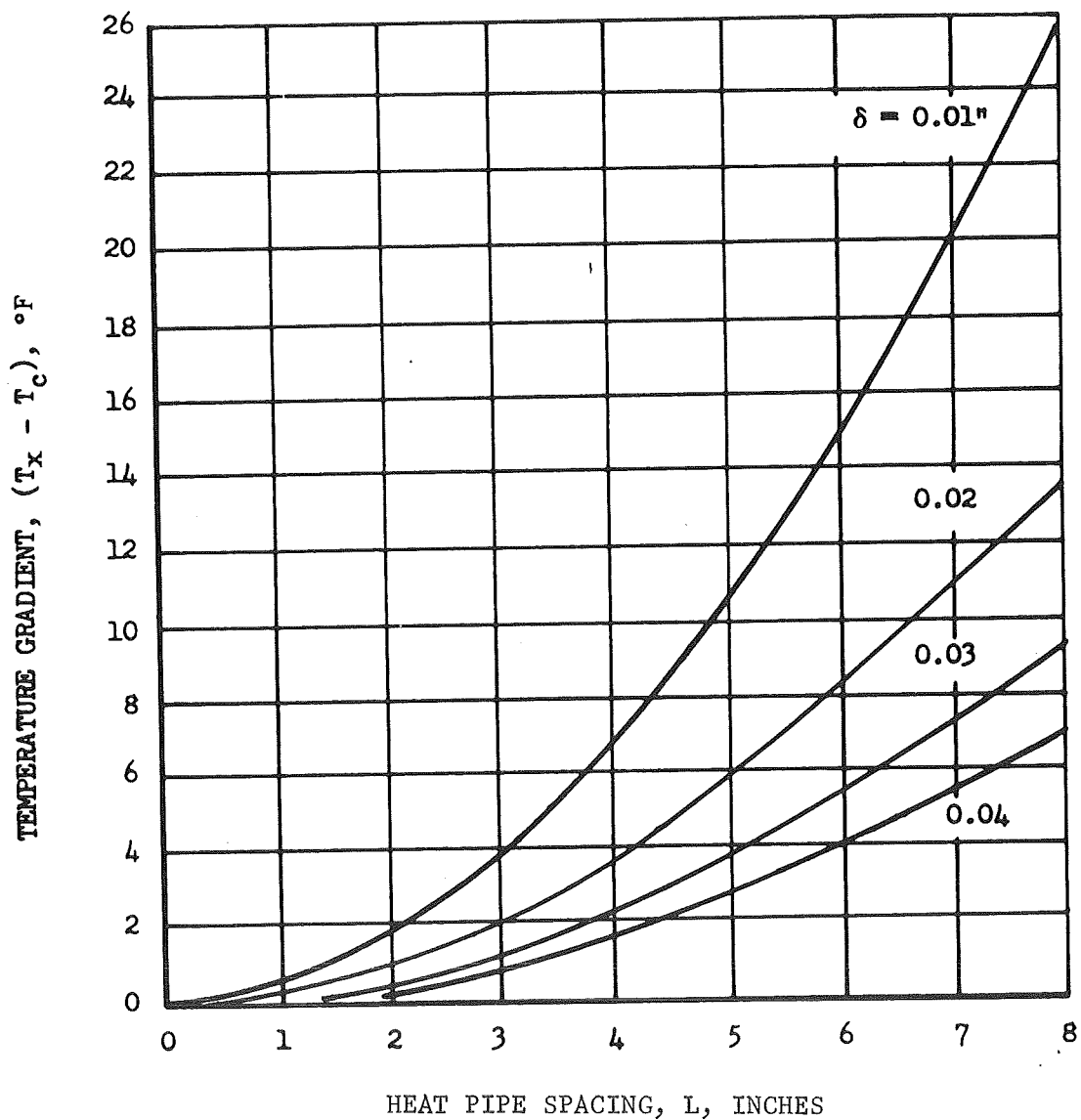


Figure 313 . Temperature Gradient as a Function of Heat Pipe Spacing (L) and Telescope Skin Thickness (δ)

For the circumferential heat pipe design approach, design considerations might dictate the location of the heat pipes on the inside periphery of the outer telescope tube to avoid meteoroid puncture. Additionally, because of the previously discussed form factor relationship between the tube and the mirror (Figure 309), the heat pipes should be closer together at the mirror end of the tube. In the conceptual design section of the report, a configuration is analyzed in detail where the heat pipe or vapor chamber affords protection only in the portion of the telescope tube adjacent to the primary mirror. Those results confirm the effect shown in Figure 309.

Configuration Number Two

Figure 314 shows a dual heat pipe - vapor chamber approach. Longitudinal heat pipes, attached to the inside of the telescope wall, are used in conjunction with a vapor chamber located behind the primary mirror. The longitudinal heat pipes would reduce the longitudinal and the circumferential gradients in the telescope tube, and additionally, would furnish a nearly isothermal surface for radiation interchange with the front of the primary mirror. The vapor chamber is removed a short distance from the back of the primary mirror. This would prevent the vapor chamber from introducing any disturbances into the optics due to the momentum of internal movement of fluid (considered to be very small). The rear face of the primary mirror and the face of the vapor chamber would be coated to promote good radiation interchange between the two. A common working fluid would be used in both the longitudinal heat pipes and the vapor chamber, thus resulting in the front and rear faces of the primary mirror being exposed to the same temperature.

While this dual approach does show promise, it would be more complicated than configuration number one. It might also interfere with the internal structure of the telescope and the contained instruments. However, it does use the heat pipe and vapor chamber in the more familiar configurations which have undergone much development work.

Configuration Number Three

Figure 315 shows an approach which combines some of the features of the previous two configurations. In this case, an annular vapor chamber structure is in contact with the telescope tube. This annulus could either be on the interior or exterior of the telescope, or it could be an integral part of the telescope tube. With this annular configuration, the wick structure covers all surfaces on the inside of the vapor chamber, with radial wick spokes connecting the inner and outer shell. This configuration departs radically from the traditional heat pipe configuration and consequently, would require more development. A similar configuration, however, has been successfully employed in a heat pipe designed to cool an electron tube.*

* Basculis, A., and Dixon, J.C.: Heat Pipe Design for Electron Tube Cooling. ASME Preprint 69-HT-25, August 1969.

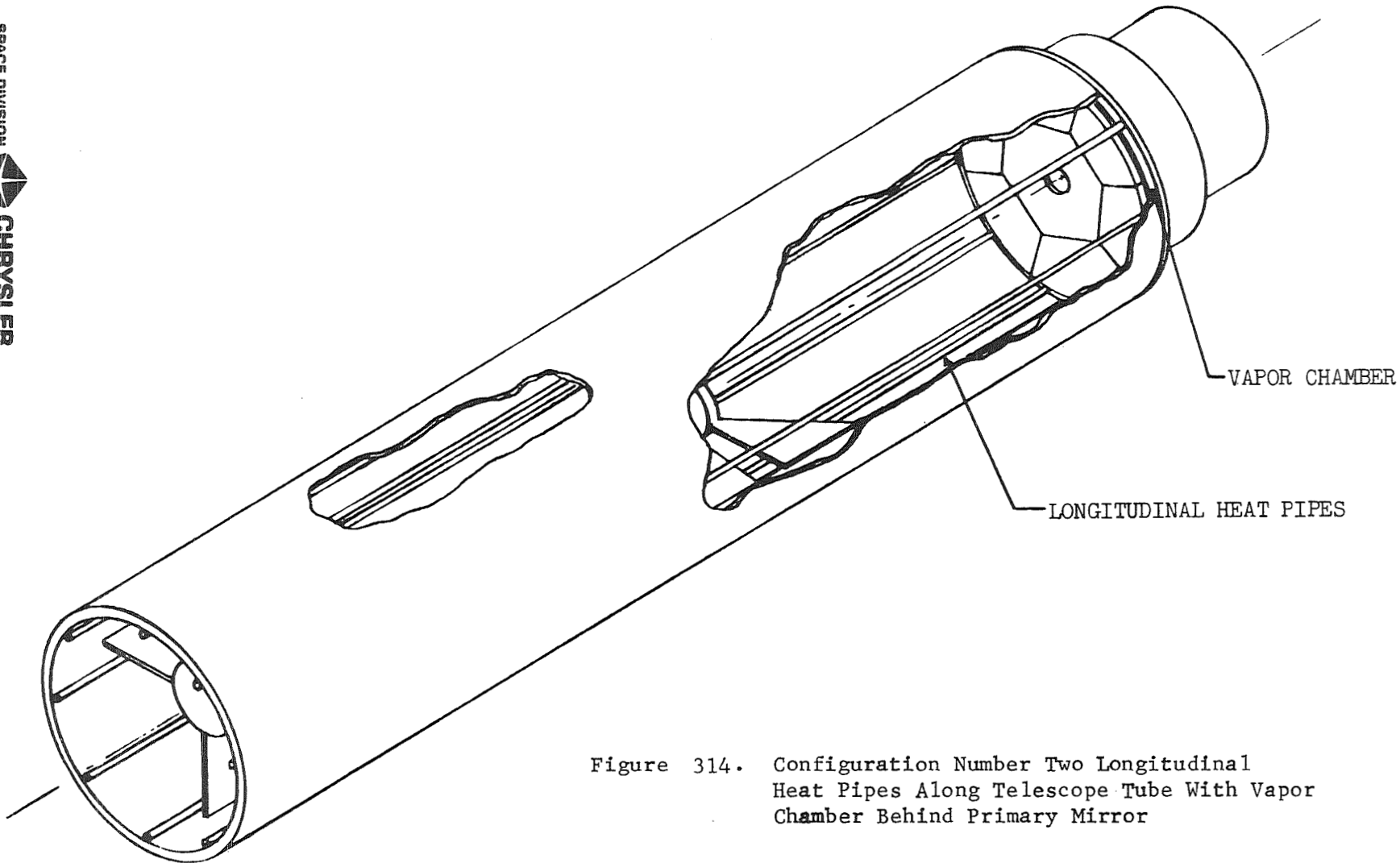
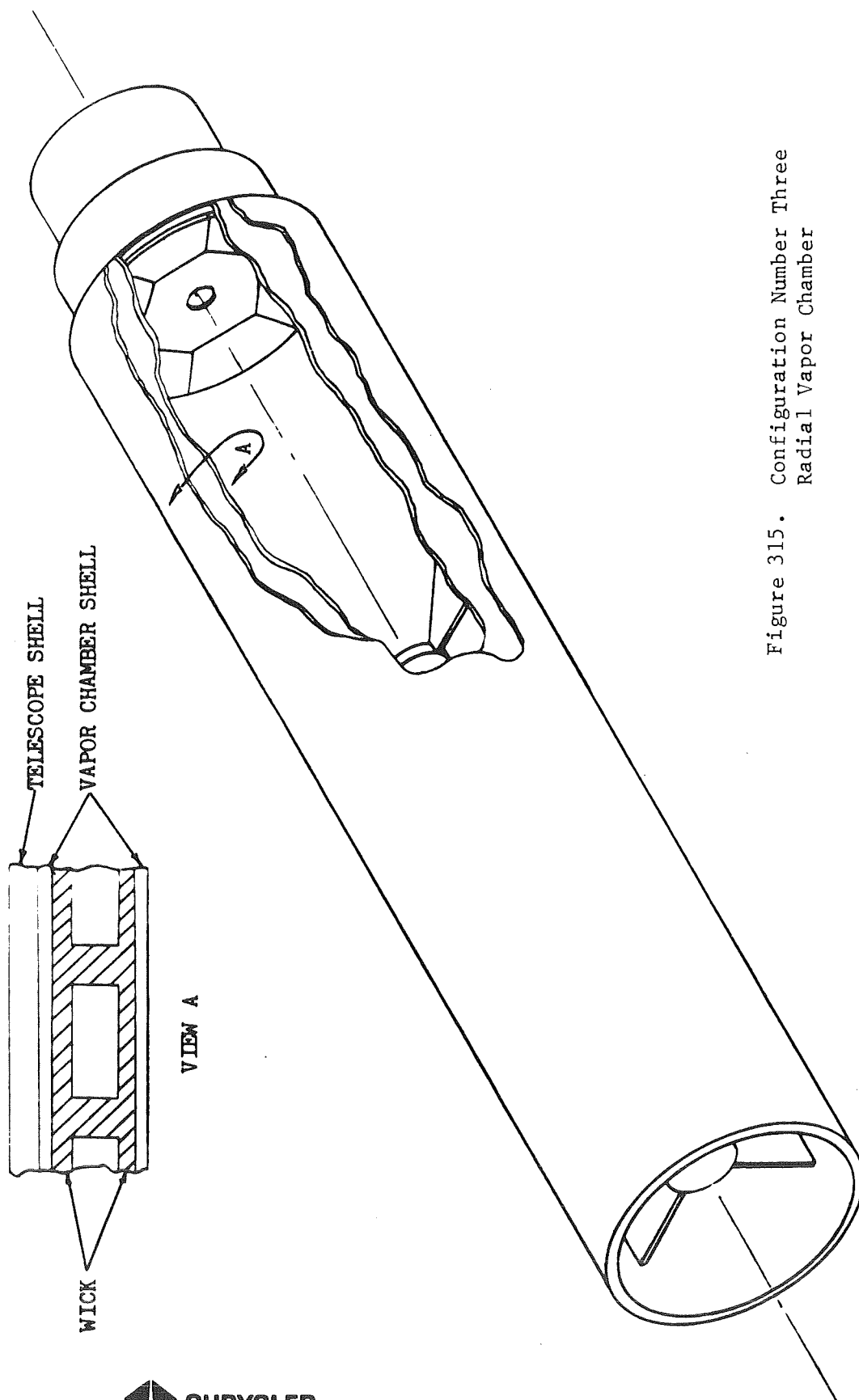


Figure 314. Configuration Number Two Longitudinal Heat Pipes Along Telescope Tube With Vapor Chamber Behind Primary Mirror



Conceptual Design

Previous sections of this report have presented the results of the research into potential heat pipe configurations, working fluids, and wicking materials. In this section, the results of this research will be used to develop a conceptual heat pipe configuration.

Heat Pipe Configuration

Earlier, Refrigerant 23 was selected as the fluid offering the best performance within the desired temperature range. Accordingly, it was used in this section to arrive at a conceptual definition of the heat pipe configuration. In addition, the analysis conducted in support of the three candidate configurations indicated that significant reductions in the thermal gradients could be achieved through the use of the circular heat pipes around the periphery of the telescope. Since this is the least complex approach, it was chosen as the candidate configuration for development in this section.

The equations presented earlier will now be used to arrive at a heat pipe diameter and wick thickness.

Equation (11) relates the maximum heat transfer capability of a heat pipe to the working fluid and wick-fluid characteristics as follows:

$$Q_{\max} = \frac{\rho \phi h_{fg}}{\mu} \frac{KA}{L'} \frac{\rho \ell_m g}{\sigma g_o} \quad (11)$$

If the fluid properties for Refrigerant 23 at -112°F are substituted into Equation (11), an expression is obtained which shows the relationship between $Q_{\max}$ and the heat pipe physical characteristics. This equation is

$$Q_{\max} = 48.4 \times 10^{13} \frac{KA \ell_m}{L'} \quad (15)$$

where

- K = wick permeability, ft^2
- ℓ_m = wicking height, ft
- A = cross sectional area of wick, ft^2
- L' = effective wick length, ft

The effective wick length, L' , is normally taken as the distance from the midpoint of the evaporator to the midpoint of the condenser. In this case L' would have a value of 14.92 feet. Therefore, using this value for L' , and a ratio of wick inside to outside diameter of 0.75, the following expression is obtained:

$$d_o = 3.0 \times 10^{-7} (Q_{\max} / K \ell_m)^{1/2} \quad (16)$$

where d_o is the outside diameter of the wick. Equation (16) was then solved for

various values of $Q_{\max}$ and the quantity of $K\ell_m$ to show the effect of heat transfer rate and wick properties on the heat pipe design. The results of this analysis are shown in Figure 317. Since the wick o.d. is effectively the same as the heat pipe i.d., heat pipe i.d. is shown on the abscissa of Figure 317.

For a three-inch spacing between the heat pipes, the previously calculated heat flux of 36.5 Btu/hr-ft² would equal a heat transfer rate of 136.5 Btu/hr which the heat pipe would have to remove. Referring again to Figure 317, it can be seen that for heat transfer rates of this magnitude, values of $K\ell_m$ of less than 2.1×10^{-10} ft³ result in a heat pipe diameter that exceeds the three inches allotted for spacing between the pipe. In order to maintain the heat pipe diameter and consequently its weight, within reasonable limits, the heat pipe diameter probably should not exceed 1.5 inches. This diameter would require a $K\ell_m$ value = 7.6×10^{-10} ft³. The wick performance data shown in Table were used in conjunction with Equation (14) to determine theoretical values of $K\ell_m$. This was done using Refrigerant 23 and the wick data listed in Table. Only one wick was found to have a $K\ell_m$ value that exceeded the minimum value of 7.6×10^{-10} ft³. The 76-micron stainless steel fiber wick had a $K\ell_m$ value of 8.57×10^{-10} ft³. Using this value, a heat pipe inside diameter of 1.44 inches was determined. Figure 318 shows a heat pipe design for this application. The stainless steel wick is 0.18 in. thick, and is bonded to the inside wall of the stainless steel heat pipe. The relatively high vapor pressure of Refrigerant 23 (686 psia at 78°F) dictates the selection of stainless steel tubing. This permits the charging of the heat pipe at ambient conditions. The heat pipe is formed into a 57-inch diameter hoop to fit around the exterior of section 1 of the telescope. The heat pipe would be constructed from a straight section of tubing bent into a hoop with overlapping ends to facilitate construction and installation on the telescope. The weight of the heat pipe, including fluid, would be approximately 35 pounds.

The single greatest driver in determining the heat pipe size in this case is the long distance between the evaporator section and the condensor section. Previous equations have shown that with all else equal, the diameter of the heat pipe varies as the square root of this distance, L' . If more detailed studies at a later time indicate a larger heat load than was used in this study, it will be necessary to seek some means of improving the wick performance in order to maintain the heat pipe at its present diameter. The improved wick performance could be realized as a result of improvements in basic wick performance, or as a result of a different wick configuration. One such configuration is shown below.

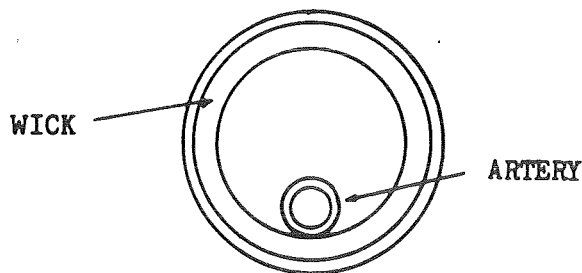


Figure 316. Heat Pipe With Artery Wick

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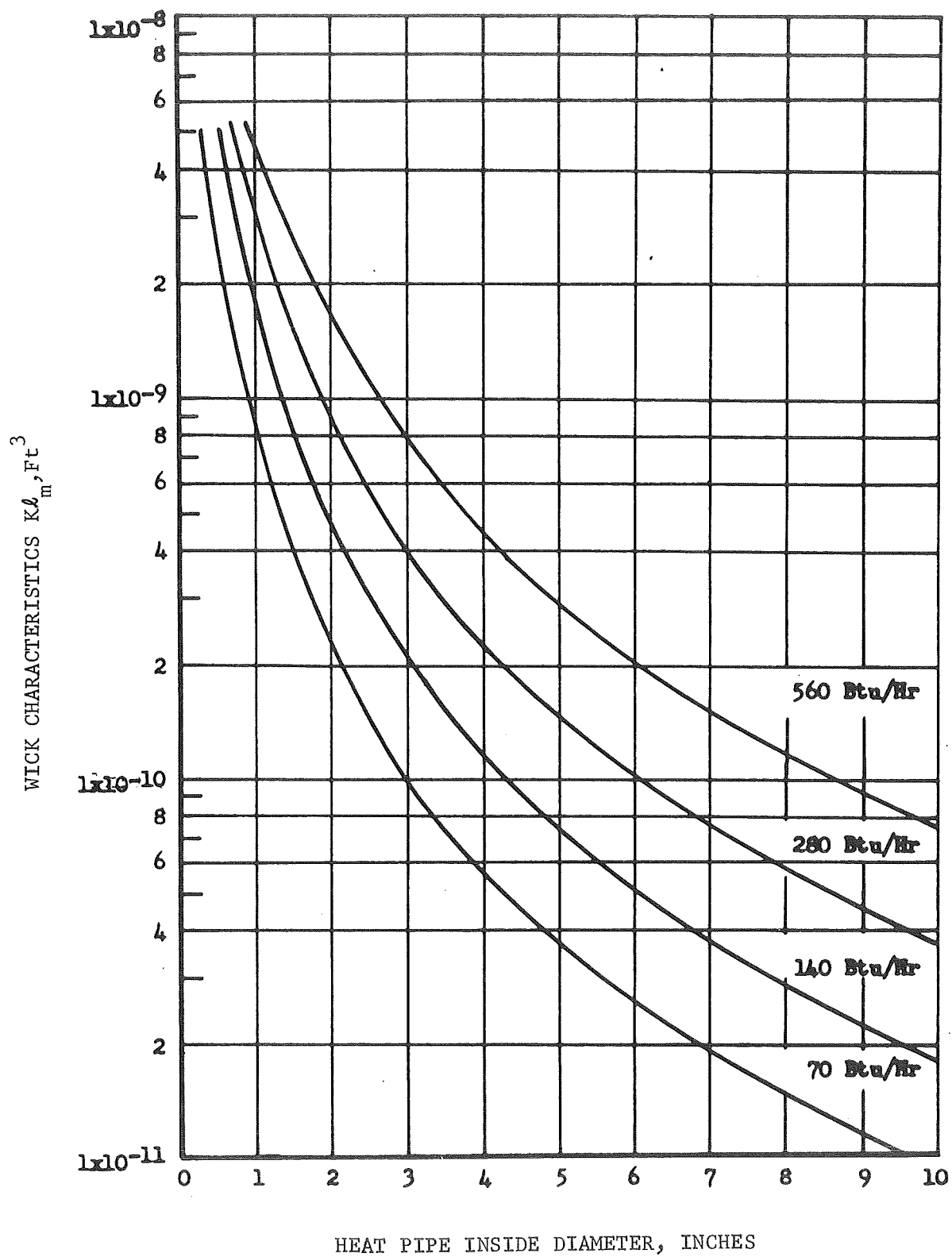


Figure 317 . Effect of Wick Characteristics and Heat Load on Heat Pipe Diameter

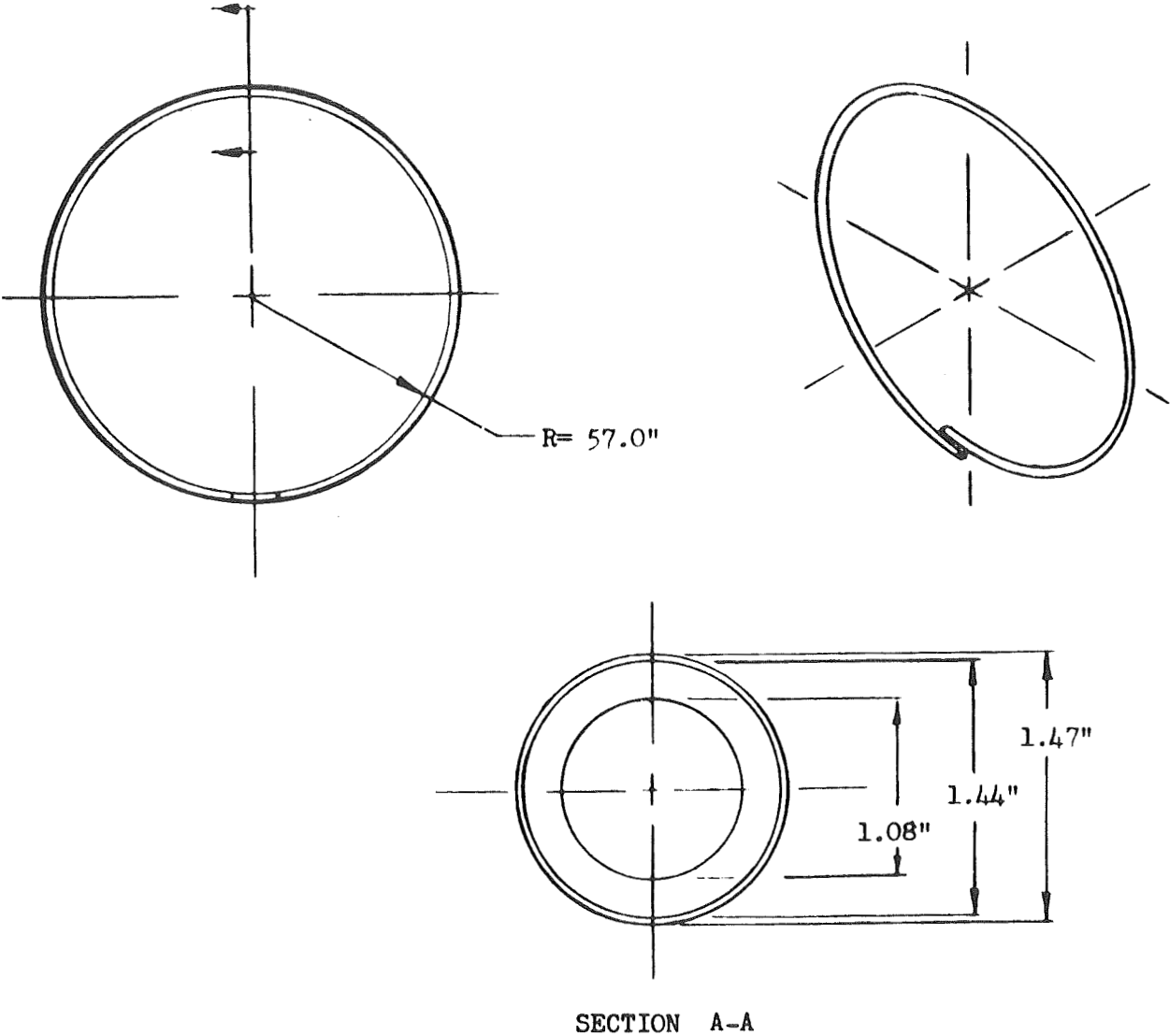


Figure 318. Heat Pipe Design

The preceding design shown consists of a standard wick around the inside periphery of the heat pipe, with a separate, low resistance passage bonded to it. This separate low resistance passage is called an artery. The usual heat pipe wick must return the fluid to the evaporator in the same wick in which both evaporation and condensation are occurring. With the artery, the fluid condenses in the wick along the heat pipe wall, flows into the artery, and is transported back to the evaporator region. The greater efficiency resulting from the smaller pressure loss in the returning liquid would result in a smaller wick area required for a given heat load, and consequently, a smaller diameter heat pipe.

System Design

Previous investigations into applying heat pipes for telescope thermal control have considered the deployment of the heat pipes along the entire length of the telescope tube. This type of heat pipe arrangement is, however, applicable only to telescopes that have rigid, non-sectioned tube walls. A review of the two-meter telescope concept presented in Perkin-Elmer Report No. 8900 revealed that the telescope tube is composed of six sections (see Figure 319). Furthermore, while in the stowed configuration, the five extension sections of the spar are contained within a 59-inch section that is fixed. This telescope concept, therefore, limits the heat pipe deployment length to 59 inches. Thus, in studying the two-meter mirror temperature and temperature gradients, a special analysis was needed to determine the effect of a confined heat pipe deployment area. In the section on configuration, the implication was that a deployment length of twice the tube diameter would be sufficient to insure a uniform environment over the mirror. In referencing Figure 319, the 59-inch length is equivalent to an L/R of only 1.1.

In performing the two-meter mirror analysis, a constant temperature was assumed for the entire tube area protected by the heat pipe. This would be the case if a vapor chamber were used. This assumption greatly simplifies the analysis while introducing only a very small error, since the three-inch spacing discussed in the previous section would result in only a few degrees difference between heat pipe temperature and mid-span temperature. Therefore, throughout the remainder of this section, the portion of the telescope spar protected by the heat pipes will be termed a vapor chamber.

In analysis, two telescope tube temperature distributions were used. These two distributions, shown in Figure 320, approximate the temperatures at 2750 and 4550 seconds in Figure 307. It was assumed that these tube temperatures remain unaltered in the areas not covered by the vapor chamber. Therefore, in a steady state analysis, the mirror temperature is affected by the radiant interchange between the mirror and the fixed tube temperature distribution and the radiant interchange between the mirror and the constant temperature (-112°F) vapor chamber. Other factors affecting the analysis were the adiabatic constraint imposed on the aft side of the mirror and the assumption that the mirror could be adequately defined by 18 nodes, three in the radial direction and six

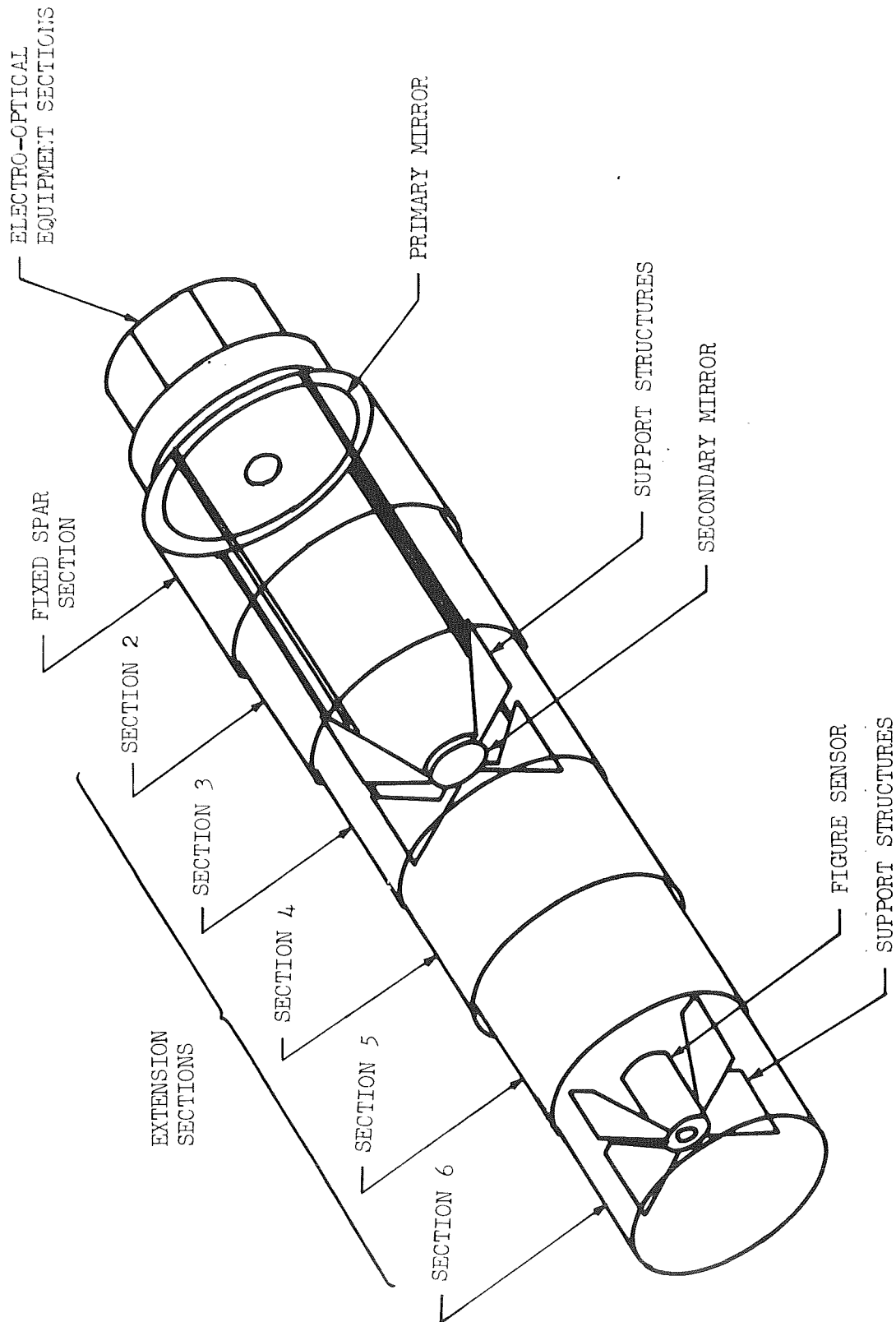


Figure 319 . Thermal Otes Spar

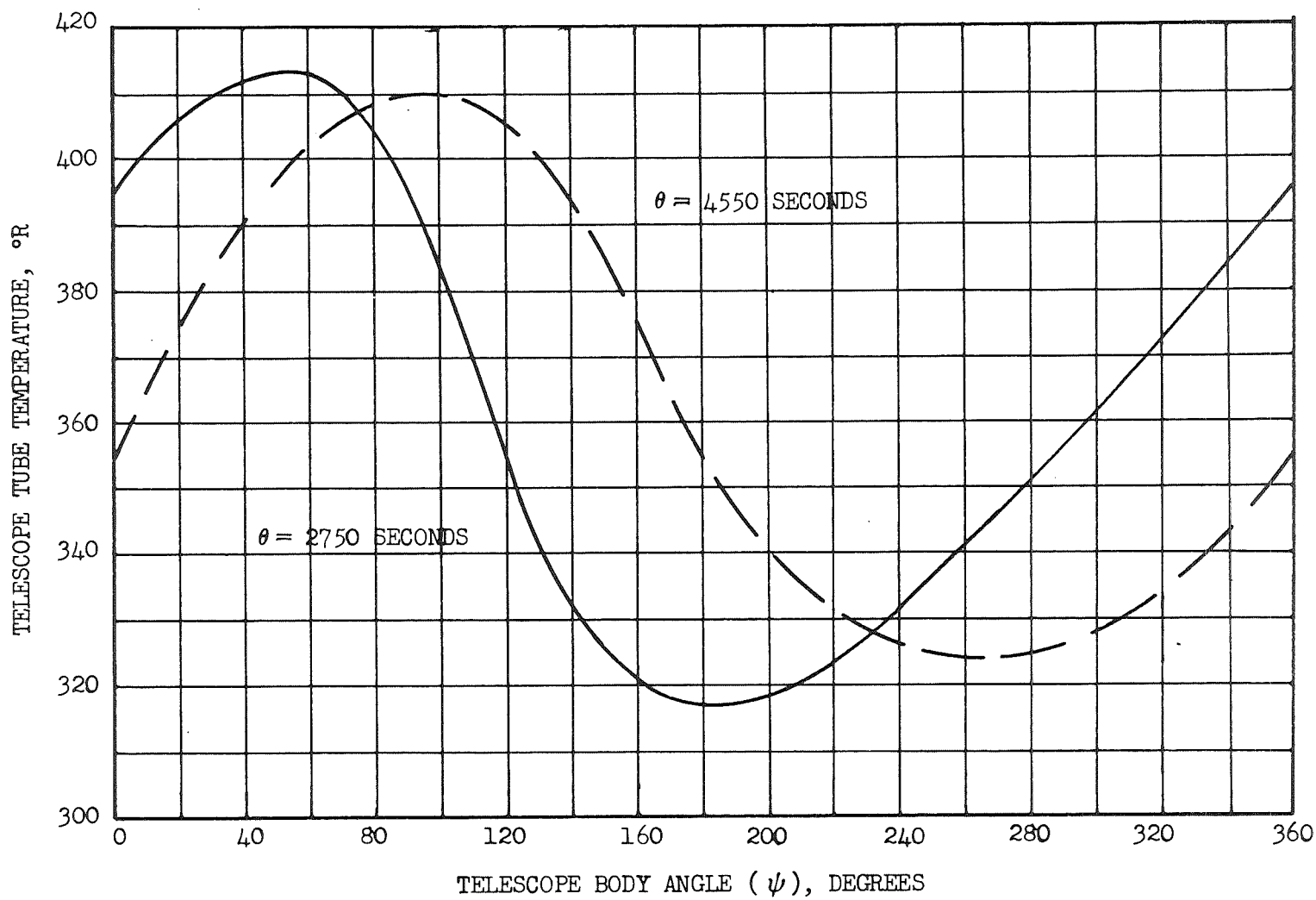


Figure 320 . Telescope Tube Temperature Distribution

circumferentially (see Figure 320). Similarly, the telescope tube beyond the vapor chamber was divided into six equal circumferential nodes. Therefore, the average radiating temperature of the tube T_T , was

$$\frac{1}{6} \sum_{n=1}^6 T_n^{4 \cdot 0.25} \quad (17)$$

where T_n represents one of the six tube temperatures. In order to determine the average mirror temperature for a given vapor chamber length, the following equation was derived:

$$\bar{T}_m = F_{m-vc} T_{vc}^4 + (1 - F_{m-vc}) \bar{T}_T^{4 \cdot 0.25} \quad (18)$$

In this relation,

$\bar{T}_m$ = the average mirror temperature,

T_{vc} = the vapor chamber temperature,

$\bar{T}_T$ = the average radiating temperature of the tube beyond the vapor chamber, and

F_{m-vc} = the form factor from mirror to vapor chamber.

In order to determine the mirror temperature and temperature gradients, the radiation interchange between each tube node (including the -112°F vapor chamber) and each of the 18 mirror nodes was computed. Additionally, circumferential and radial heat conduction within the mirror was computed. In the analysis, vapor chamber lengths of 0, 24, 50 and 140 inches were considered at a temperature of -112°F .

Mirror temperatures resulting from the analysis using the telescope tube temperature distribution at 2750 seconds are shown in Table 44 as a function of vapor chamber length. Also shown is the average computed mirror temperature T_{avg} . Figure 322 is a radial plot of these mirror temperatures for each of the vapor chamber lengths. In this figure both radial and circumferential gradients can be observed. As a further aid in visualizing the effect of vapor chamber length on the mirror temperature gradient, Figure 323 was developed. In this plot the radial temperature drop (T_c , at the center, minus T_e , at the edge) at the six circumferential locations was plotted versus vapor chamber length. As expected, the results show that the mirror ΔT is reduced with the addition of a vapor chamber. Furthermore, the mirror ΔT would become nonexistent if the vapor chamber extended along the entire length of the telescope tube. However, the results show that even if the vapor chamber could be extended to be the same length as the telescope tube, it would be impractical. An optimum, vapor chamber length was found to be approximately 32 inches. This length vapor chamber could

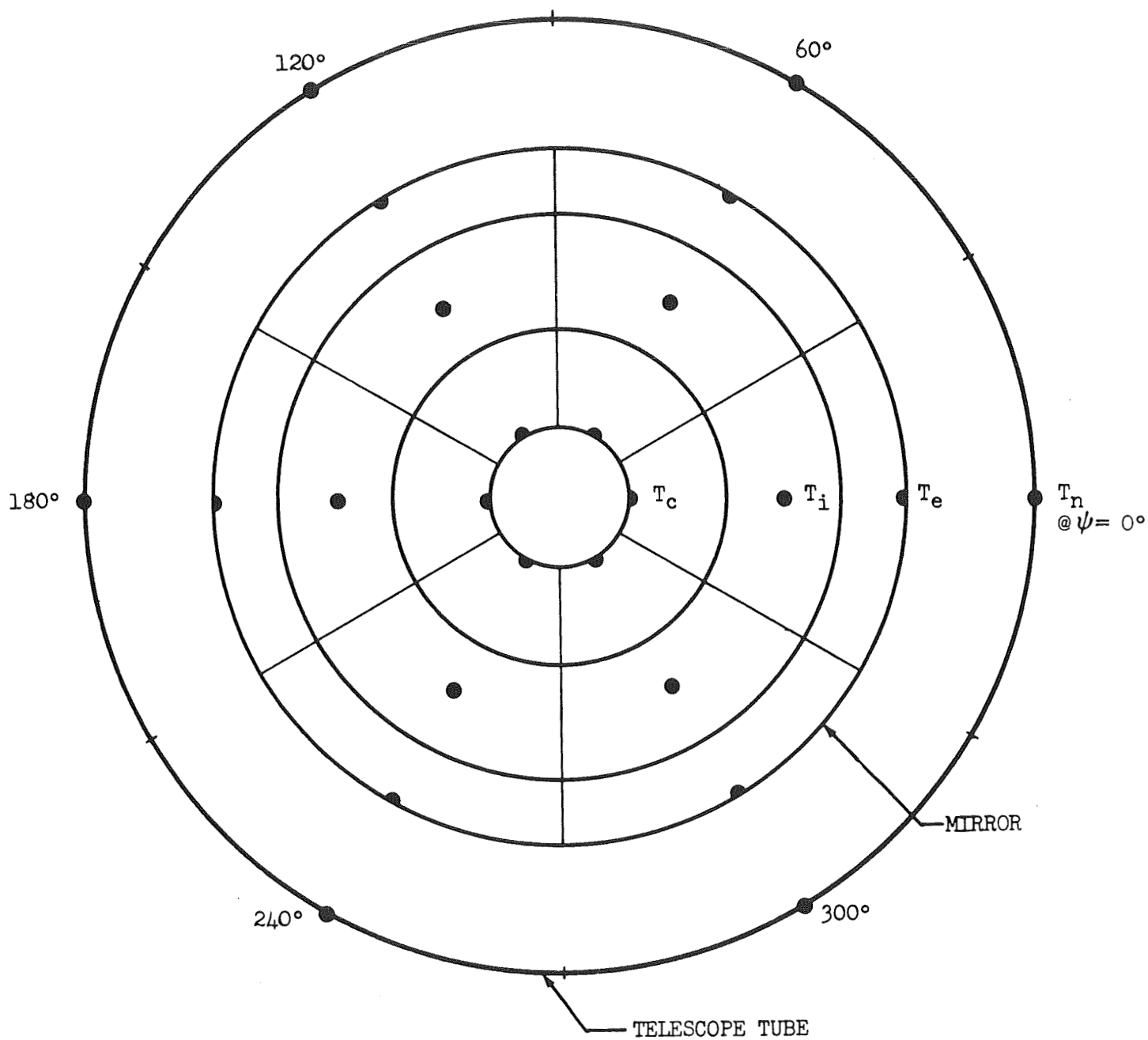


Figure 321 . Model of 2-m Mirror

TABLE 44. MIRROR TEMPERATURE AS A FUNCTION OF VAPOR CHAMBER LENGTH ($A = 2750$ SECONDS)

VAPOR CHAMBER LENGTH- INCHES	CIRCUMFERENTIAL ANGLE - DEGREES	TUBE TEMPERATURE BEYOND VAPOR CHAMBER DEGREES F	T_E	MIRROR TEMPERATURES - T_i	T_c	DEGREES F $T_c - T_e$	T_{avg}
0	0	-65	-92.4127	-92.5111	-92.9906	-0.5789	-93.447
	60	-47	-92.2233	-92.3649	-92.9606	-0.7373	
	120	-105	-93.5214	-93.5021	-93.4549	0.0665	
	180	-143.5	-94.5553	-94.4402	-93.9163	0.6390	
	240	-128.5	-94.4598	-94.3655	-93.9029	0.5570	
	300	-99	-93.5096	-93.4980	-93.4616	0.0480	
24	0	-65	-98.6753	-98.6530	-98.6810	-0.0058	-98.829
	60	-47	-98.6734	-98.6571	-98.6781	-0.0047	
	120	-105	-98.8584	-98.8377	-98.7873	0.0711	
	180	-143.5	-99.0467	-99.0251	-98.8950	0.1516	
	240	-128.5	-99.0499	-99.0217	-98.8943	0.1505	
	300	-99	-98.8643	-98.8425	-98.7896	0.0747	
50	0	-65	-104.0412	-104.0093	-103.8992	0.1420	-103.859
	60	-47	-104.0593	-104.0233	-103.9025	0.1568	
	120	-105	-103.8720	-103.8616	-103.8320	0.0399	
	180	-143.5	-103.7077	-103.7172	-103.7648	-0.0571	
	240	-128.5	-103.7174	-103.7246	-103.7661	-0.0487	
	300	-99	-103.8708	-103.8601	-103.8309	0.0399	
140	0	-65	-109.9717	-109.9597	-109.9043	0.0674	-109.857
	60	-47	-109.9747	-109.9621	-109.9052	0.0695	
	120	-105	-109.8592	-109.8583	-109.8546	0.0046	
	180	-143.5	-109.7450	-109.7553	-109.8039	-0.0589	
	240	-128.5	-109.7454	-109.7555	-109.8036	-0.0582	
	300	-99	-109.8568	-109.8561	-109.8585	0.0033	

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28 2-M Mirror Radial Gradients for
-112°F VC of Various Lengths
Tube Temp @ 2750 Sec

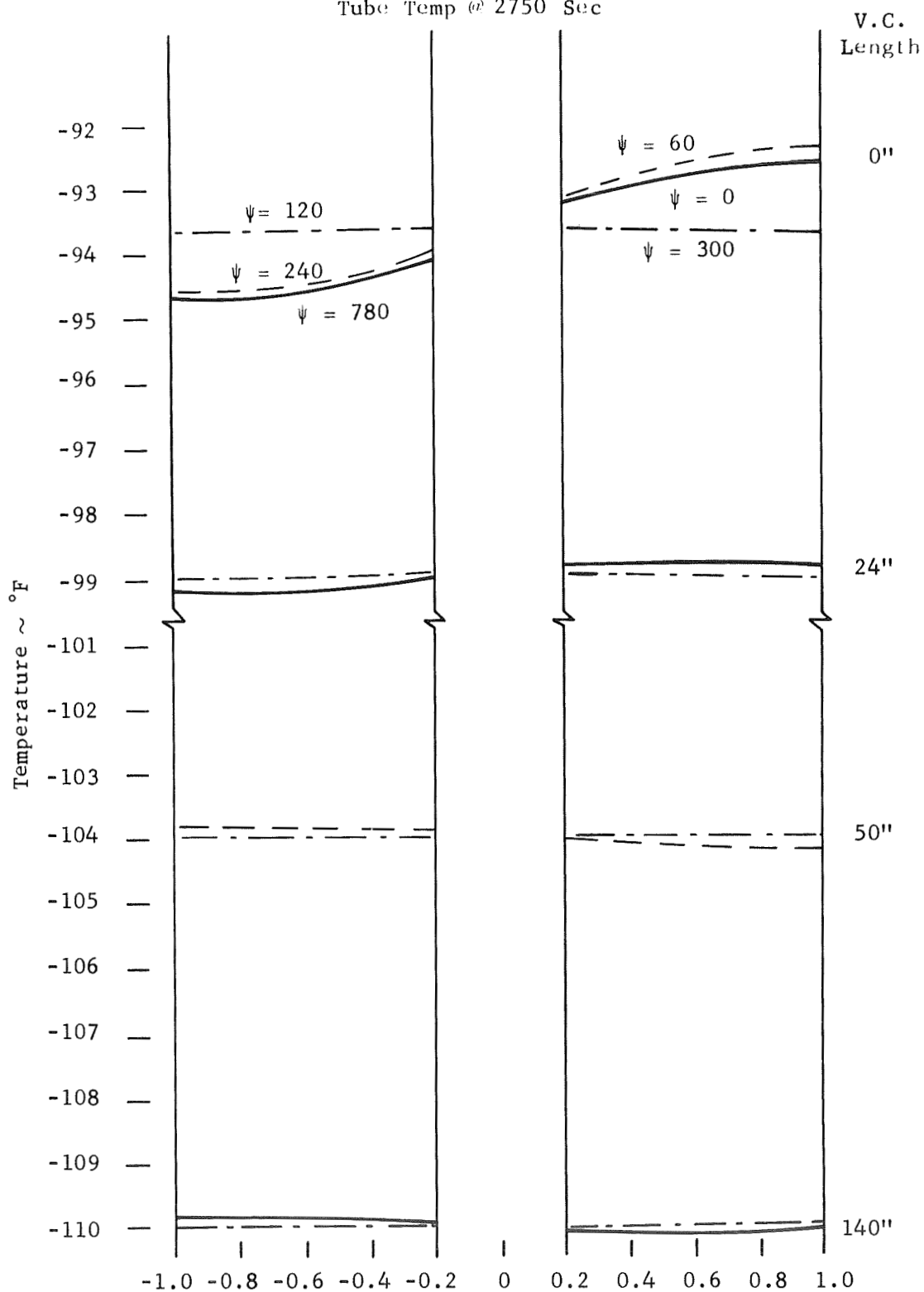


Figure 322. Mirror Radial Temperature Gradient (@ $\theta=2750$ sec)

I-633

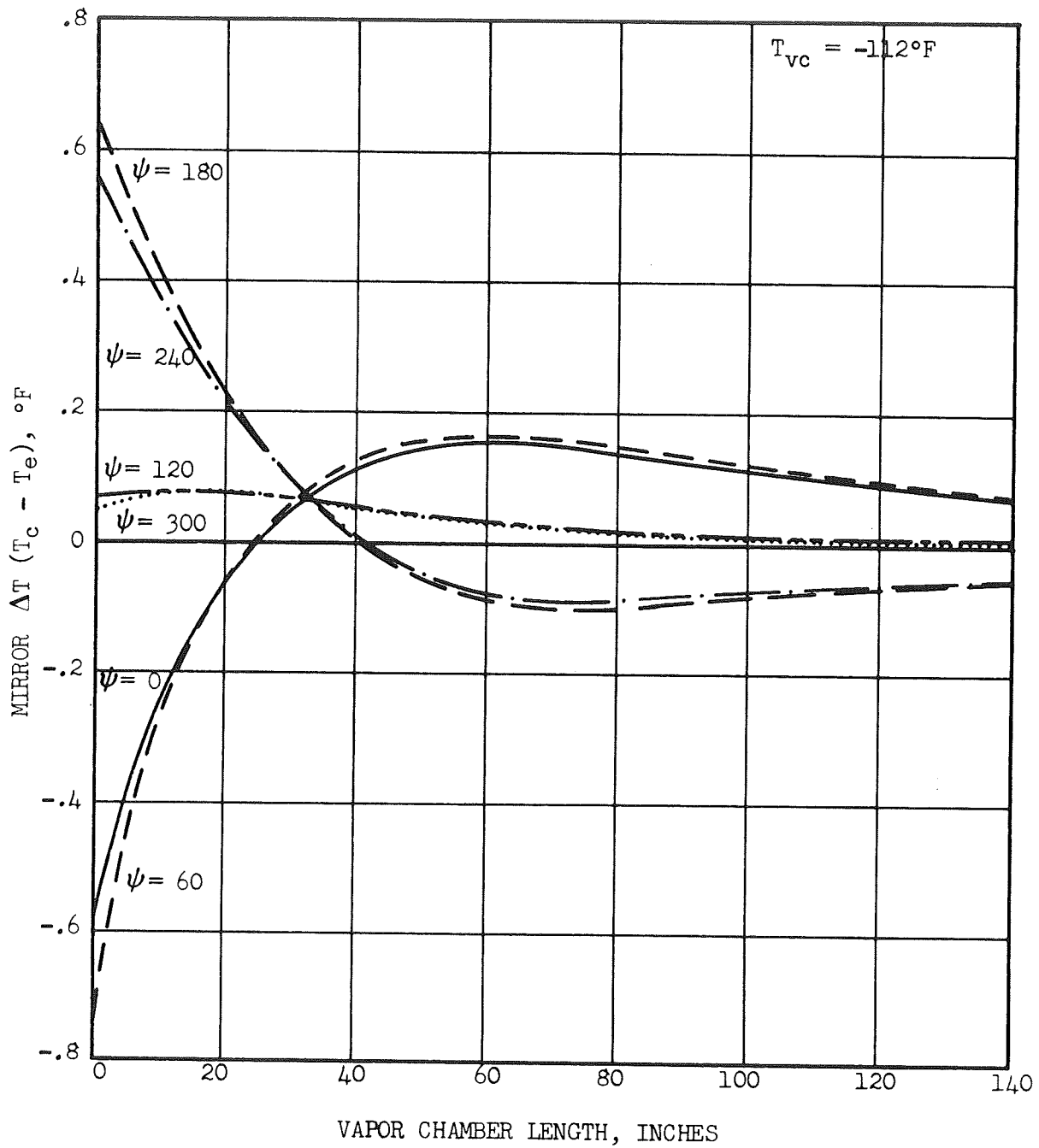


Figure 323. Mirror ΔT (@ $\theta = 2750$ Sec) vs. Vapor Chamber Length

maintain a radial temperature drop of 0.08°F with practically no circumferential gradient. A plot of the form factors from the mirror nodes to the tube nodes reveals that the form factor distribution is more nearly constant for a vapor chamber length of 32 inches than for any other length. This shows up particularly well on a polar coordinate plot. To determine this optimum vapor chamber length for a given geometry without plotting the form factors, a form factor differencing technique could be employed. This would involve knowing the form factors from a sector of the mirror to all of the nodes on the telescope tube for various vapor chamber lengths. With this information the difference between the form factor to the tube node at some ψ - location and the form factor to the tube node at the $\psi+180$ location could be computed for all $180 > \psi \geq 0$. The optimum vapor chamber length could then be found by determining the smallest vapor chamber length that results in satisfying the expression

$$\sum_{180 > \psi \geq 0} \left[F \left(\begin{array}{cc} \text{mirror} & \text{tube node} \\ \text{section } j & \text{at } \psi \end{array} \right) - F \left(\begin{array}{cc} \text{mirror} & \text{tube node} \\ \text{section } j & \text{at } \psi+180 \end{array} \right) \right] = 0. \quad (19)$$

Naturally, for a very long vapor chamber length, i.e., $L/R > 4$, Equation (19) could be satisfied. However, as previously mentioned, a length of this magnitude is often impossible because of design limitations. For this analysis the optimum vapor chamber length computed using Equation (19) was 31.987 inches.

Results obtained from the telescope tube temperature distribution at 4550 seconds (from Figure 320) are presented in Table 45, and Figures 324 and 325. These results are similar to those discussed for the telescope tube temperature distribution at 2750 seconds. As in the previous analysis the ΔT crossover in Figure 325 occurs at a vapor chamber length of approximately 32 inches. The value of the mirror ΔT for the 4550-second tube temperature distribution at the optimum vapor chamber length is 0.07°F .

Additional analyses revealed that the 32-inch vapor chamber length is unique to the mirror-telescope tube geometry analyzed and is independent of the vapor chamber temperature and average telescope tube temperature. The chamber and tube temperatures do, however, affect the magnitude of the mirror ΔT and, according to Equation (18), also affect the average mirror temperature. A plot of the average mirror temperature as computed from Equation (18) for both telescope temperature distributions is contained in Figure 326. Also shown in this figure are the average mirror temperatures as computed in the analysis and the form factor from the mirror to the vapor chamber. To achieve an average mirror temperature of -112°F using a vapor chamber length of 32 inches would, according to Equation (18), require vapor chamber temperature of approximately -146.3°F for either of the tube temperature distributions analyzed. Operating at this temperature could, according to Figure 308, require the selection of Refrigerant 503 as the working fluid.

TABLE 45. MIRROR TEMPERATURE AS A FUNCTION OF VAPOR CHAMBER LENGTH ($t = 4550$ SECONDS)

VAPOR CHAMBER LENGTH - INCHES	CIRCUMFERENTIAL ANGLE - DEGREES	TUBE TEMPERATURE BEYOND VAPOR CHAMBER DEGREES F	T_E	MIRROR TEMPERATURES - DEGREES F				$T_{avg.}$
				T_i	T_c	$T_c - T_e$		
0	0	-105.5	-94.1834	-94.1629	-94.1026	0.0808		
	60	-58	-93.0060	-93.1204	-93.6322	-0.6262		
	120	-55	-92.9670	-93.0891	-93.6233	-0.6563		
	180	-105.5	-94.1644	-94.1450	-94.0929	0.0715		
	240	-134	-95.0979	-94.9984	-94.5261	0.5718		
	300	-132	-95.0920	-94.9951	-94.5281	0.5639		-94.085
24	0	-105.5	-99.4402	-99.4203	-99.3697	0.0705		
	60	-58	-99.2585	-99.2355	-99.2640	-0.0054		
	120	-55	-99.2565	-99.2329	-99.2625	-0.0060		
	180	-105.5	-99.4359	-99.4162	-99.3674	0.0686		
	240	-145	-99.6145	-99.5930	-99.4699	0.1455		
	300	-132	-99.6159	-99.5942	-99.4708	0.1451		-99.407
50	0	-105.5	-104.2244	-104.2150	-104.1891	0.0353		
	60	-58	-104.3994	-104.3669	-104.2566	0.1428		
	120	-55	-104.4037	-104.3704	-104.2577	0.1460		
	180	-105.5	-104.2276	-104.2180	-104.1905	0.0372		
	240	-134	-104.0764	-104.0844	-104.1270	-0.0506		
	300	-132	-104.0765	-104.0843	-104.1266	-0.0501		-104.216
140	0	-105.5	-109.9498	-109.9492	-109.9468	0.0029		
	60	-58	-110.0615	-110.0498	-109.9960	0.0655		
	120	-55	-110.0630	-110.0511	-109.9966	0.0664		
	180	-105.5	-109.9522	-109.9514	-109.9479	-0.0043		
	240	-134	-109.8433	-109.8531	-109.8993	-0.0560		
	300	-132	-109.8423	-109.8521	-109.8988	-0.0565		-109.950

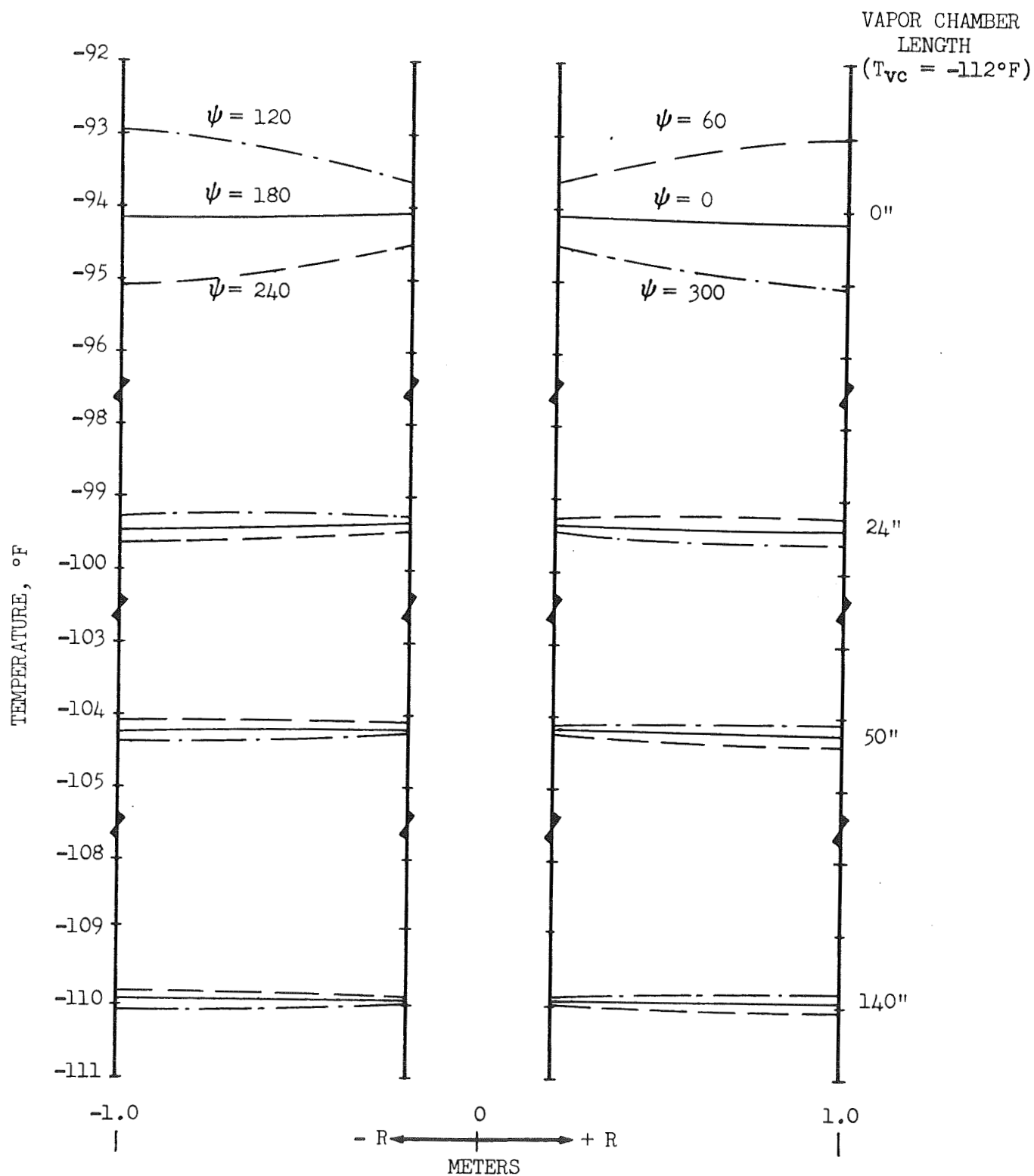
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CHRYSLER
CORPORATION

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Figure 324 . Mirror Radial Temperature Gradient (@ $\theta = 4550$ Sec.)

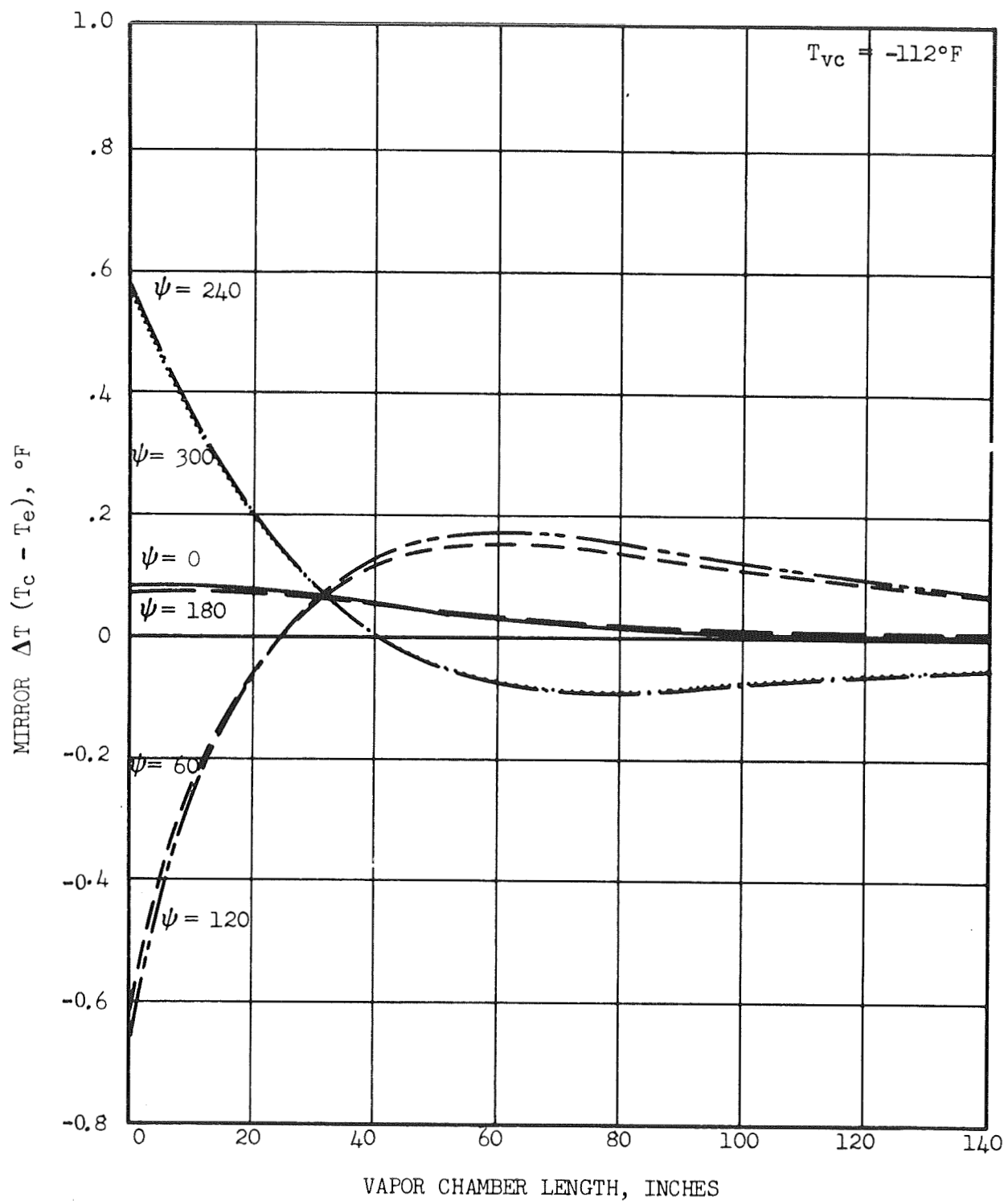


Figure 325. Mirror ΔT (@ $\theta = 4550$ Sec.) vs. Vapor Chamber Length

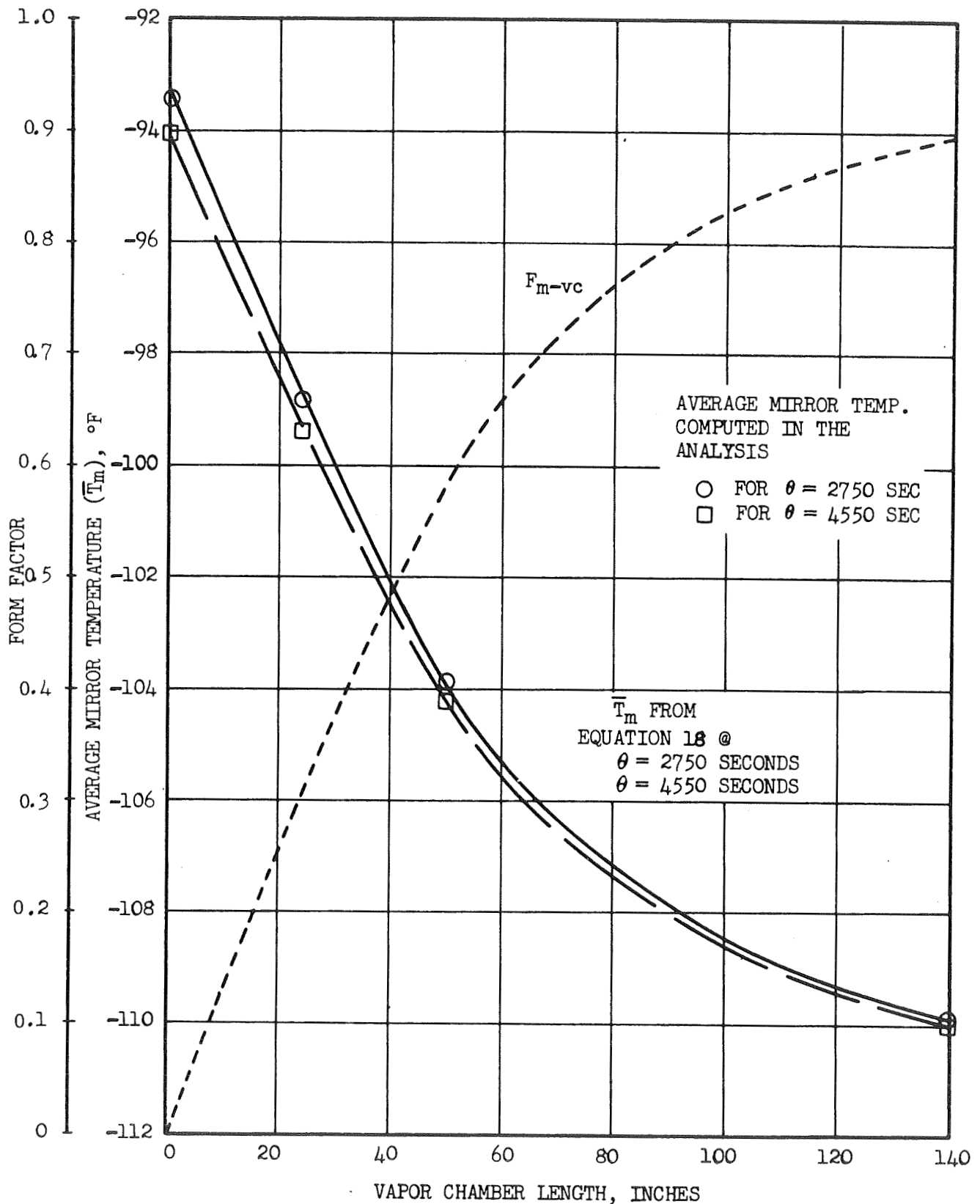


Figure 326. Average Mirror Temperature and Form Factor to -112°F Vapor Chamber

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CONCLUSIONS AND RECOMMENDATIONS

The following is a summary of the major conclusions and recommendations resulting from the accomplishment of the study.

Conclusions

- The heat pipe or vapor chamber offers a means of significantly reducing the thermal gradients in an orbiting telescope.
- Very small temperature gradients can be achieved in the primary optics through the use of heat pipes or vapor chambers on a small portion of the telescope tube adjacent to the optics.
- Circular heat pipes located around the circumference of the telescope tube offer the most practical approach for thermal control of the telescope.
- The heat loads in conjunction with large wick lengths required for circular heat pipes require highly effective wicks for reasonable heat pipe diameters.
- All working fluids considered for application in the low temperature range ($\approx -112^{\circ}\text{F}$) offer significantly lower performance than the more common working fluids such as water or ammonia.
- An average mirror temperature of -112°F will require a heat pipe temperature well below -112°F .

Recommendations

- Telescope design, operating conditions, and thermal constraints should be sufficiently defined to allow a more detailed analysis.
- More detailed studies should include the development of a mathematical model to link the basic heat pipe theory with an accurate thermal model of the telescope system.
- Additional research should be directed toward developing more efficient wicking designs for the low temperature applications discussed herein.
- Development efforts should be directed toward further identifying heat pipe performance characteristics using fluids, wicks and configurations that are suitable for low temperature applications.

CHAPTER 17 - NON-SPACE EXPERIMENTS

There is a considerable amount of non-space experimental work which is invaluable prior to final design and fabrication of a large telescope for space usage. This is the design development and breadboard testing which necessarily precedes the final detail design for the telescope. Some of the work is already underway in existing supporting research and technology programs. This section is concerned with new experimental programs necessary before the LTP can progress to the final design and fabrication stages.

Specifically, the programs which can be carried out on the ground are:

- The fabrication and testing of a full-scale two-meter active optics primary mirror, and the adaption of the figure sensor to provide automatic optical alignment capability for a telescope system.
- Fabrication and testing of the image mover optical and mechanical components in order to determine actual performance data.
- An extension of the theoretical and experimental thermal vacuum testing and measurements of large primary mirror deformations in the face of thermal gradients. This work should be extended to include large silicon and beryllium mirrors. Realistic simulations of space telescope thermal profiles and temperatures should be attempted.
- Operation of the Free Float pointing experiment in a vacuum chamber with a realistically simulated optical error sensor working in conjunction with a 12th-magnitude simulated star. The objective is to demonstrate Free Float pointing to an accuracy of 0.01 arc-second or better.
- Space qualification of a compact He-Ne laser system and peristaltic actuator for the figure sensor package, which is needed to control actively the primary mirror figure and secondary mirror alignment.
- Construction of a small (1-m) ground-based astronomy telescope utilizing active optics techniques in order to obtain operating experience among the astronomers.
- Additional development and testing of high-reflectance ultraviolet dielectric film coatings, since these coatings offer the possibility of more than doubling the photon transmission of the telescope in the vacuum ultraviolet.
- Development of signal processing techniques to extend the faint limit of the telescope. These techniques include pulse-height discrimination techniques to enhance photodetector signal to noise, development of photometers with much smaller photocathodes to restrict

dark current, development of instrumental techniques for reducing sky background. Each of these requires study prior to breadboarding, but the potential pay-off is equivalent to doubling the size of the telescope aperture.

- Development of ultraviolet films for reduced susceptibility to energetic radiation and consequent reduction in the weight of shielding. (Shielding in the present ATM weighs 7000 lb, more than the weight of the ATM experiment canister. It should be possible to reduce this shielding weight substantially by film conditioning and/or in-orbit processing).
- Development of grating technology to permit the ruling of focusing gratings with a uniformly varying blaze angle. This will permit marked increases in the optical efficiency of low-f-number-focusing gratings and again is equivalent to increasing telescope aperture.
- A program of laboratory astrophysics to achieve an extensive library of vacuum ultraviolet spectra. This is needed to identify and correlate spectral data obtained by the telescope.
- Development of infrared detectors to take advantage of space astronomy (See Chapter 3 on Astronomical Utilization).
- Further development of image tubes to improve resolution, dynamic range, and spectral range.

TWO-METER ACTIVE OPTICS MIRROR SYSTEM

Some of the most critical problems involved in developing the technology for a large diffraction-limited orbiting telescope have been solved in principle. Specifically, an approach to solving the problem of maintaining the figure of a two-meter or larger telescope is provided by the Active Optics Techniques, whereby, diffraction-limited operation has been obtained in laboratory experiments for mirror sizes up to 30 inches in diameter. It is now appropriate to extend the laboratory work to include both the control of a two-meter primary mirror and to the control of the secondary mirror alignment in two directions orthogonal to the optical axis and in two angles around these axes.

The key components that are to be fabricated and tested for this laboratory work are:

- A primary mirror of full two-meter aperture figured to the required hyperboloidal surface figure with errors less than $\lambda/50$ rms. This type of aspheric surface is required for one of the best telescope designs available today, namely, the Ritchey Chretien.
- A system for active control of the mirror using interferometric figure sensing to achieve an overall primary mirror figure of $\lambda/50$ rms.
- An active telescope alignment system that automatically aligns the primary and secondary mirrors in position and angle.

Fabrication and Optical Testing

The primary mirror requires advanced fabrication techniques for the large active optics mirror. Off-axis precision optical elements are usually fabricated by first cutting them from a circular parent blank and then re-blocking them in the parent blank so that fabrication can proceed effectively on a rotationally symmetric piece. Such a procedure economically limits the practical size of off-axis optics that can be produced. Where larger off-axis optics are required, the only alternative is to fabricate the required surfaces directly.

Traditionally, large primary mirrors have been fabricated by conventional, or classical, processes that rely heavily on the skills of the optician, augmented by carefully executed test methods. Newly developed technologies are emerging which will substitute for skills that appear to be obsolete. These recent technologies have come about through several years of directly applicable development based on the early recognition that Perkin-Elmer would be required to fabricate mirrors of this complexity and accuracy.

Optical measurement is generally acknowledged to be one of the major tasks during the figuring of a precision large-aperture primary. This is largely due to the effort required in data acquisition, reduction, and interpretation, and in the subsequent determination of the corrective action required to produce the desired surface. The level of effort for testing increases as both the precision and the asphericity of the optic being fabricated increase.

Two separate approaches are available for measuring the optical figure of the mirror. Both are interferometric and have the requisite intrinsic accuracies to measure surface contours of the quality desired. The first is based on center of curvature interferometric testing, either with or without a null lens, with interference being obtained against a reference spherical wavefront. The second is an approach that uses holographic interferometry for in-process measurement of the off-axis segments. The strength of hologram interferometry is that the burden of data reduction can be reduced significantly because the presentation of surface errors is in such a form that the optician can easily assess his own progress and directly determine the course of required corrective action.

Figure 327 shows the anticipated testing configuration. A point source is located beyond the prime focus of the mirror so that a convergent beam is produced by the mirror. A photographic plate is mounted to intercept the beam at a point where it is reasonably small. A second beam, the reference beam, from the same laser source also falls upon the photographic plate.

The interference produced between the two beams falling upon the photographic plate produces a hologram. In this manner a master hologram will be generated using the section of the aspheric mirror that has been finished and whose quality has been verified by scatterplate or other testing techniques. Once the master hologram has been generated, the finished mirror section will not be needed during the testing of the other mirror sections except for occasional verification of the alignment of the interferometer. The hologram in conjunction with the reference beam will reconstruct a wavefront identical to the wavefront produced by the finished aspheric section. A different off-axis mirror section is then placed in the interferometer and, when the wavefront from this mirror is superimposed upon the reconstructed wavefront of the finished section, interference fringes will be produced that will be indicative of the difference between the two wavefronts. Figure 328 and 329 show interferograms produced in such an interferometer. The only significant difference between the setup that produced these interferograms and the proposed interferometer is size. The off-axis parabola used for these interferograms had approximately a 4-inch aperture. In Figure 328 the hologram-reconstructed wavefront and the line wavefront are identical. Straight fringes have been introduced, to aid in interpretation of the interferogram, by a shearing movement of the hologram. Figure 329 shows a parabola that has a zonal error of approximately $1/2\lambda$.

The previously anticipated problem areas for the implementation of hologram interferometry are as follows:

- Accurate replacement of hologram after processing
- Proper positioning of test piece
- Interpretation of interferograms
- Limitations of photographic emulsion
- Metrology of setting up the interferometer.

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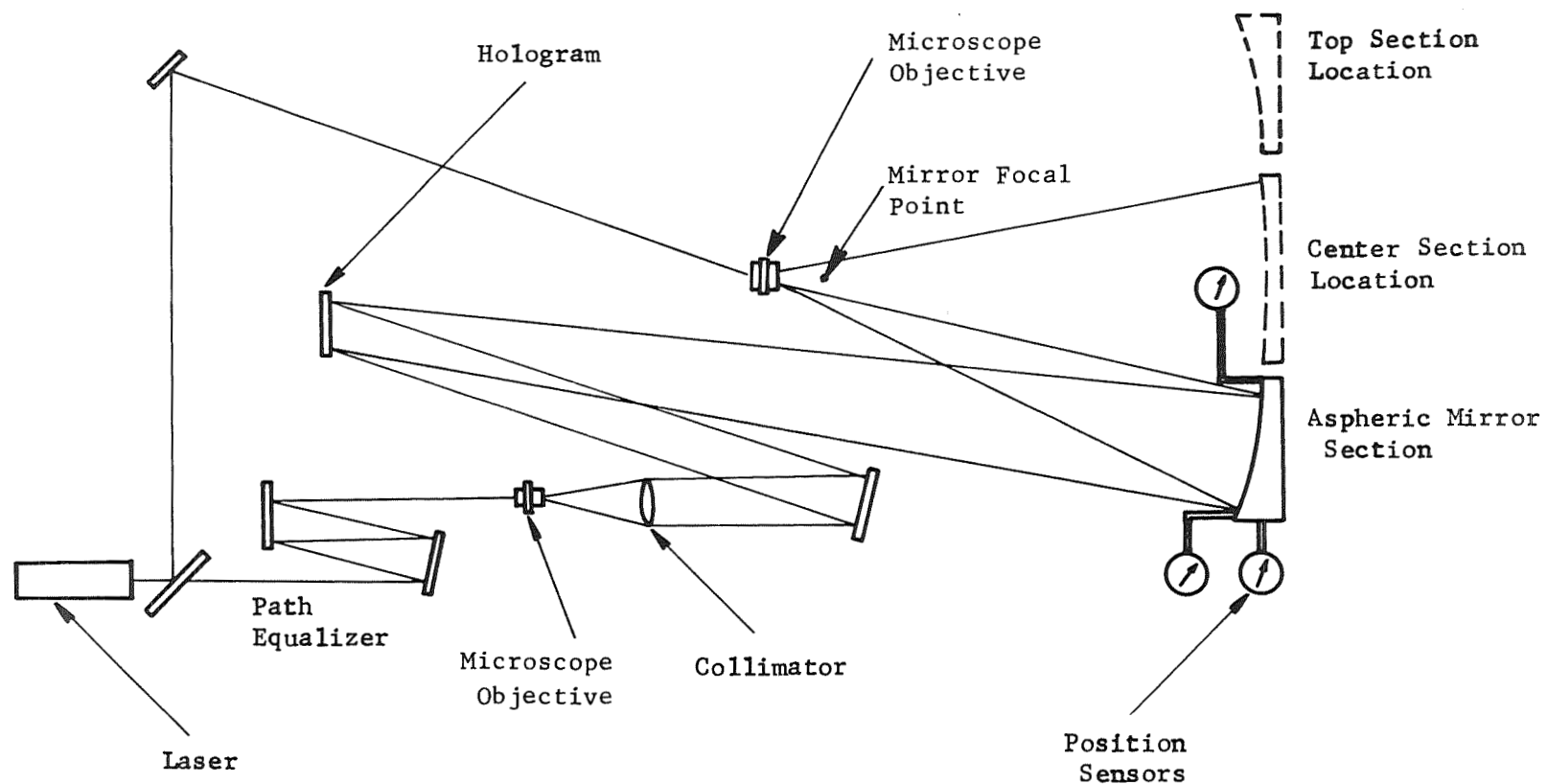


Figure 327. Test Configuration

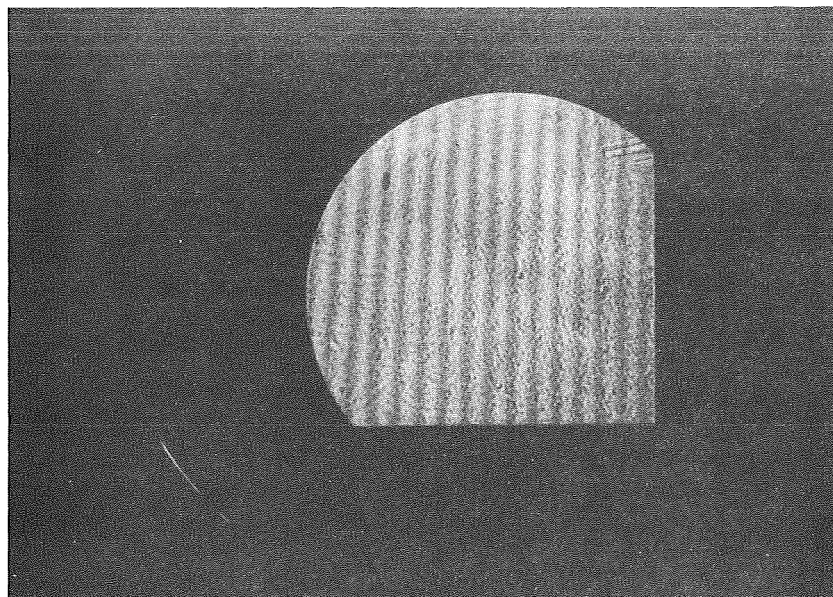


Figure 328. . Hologram Interferometer Testing

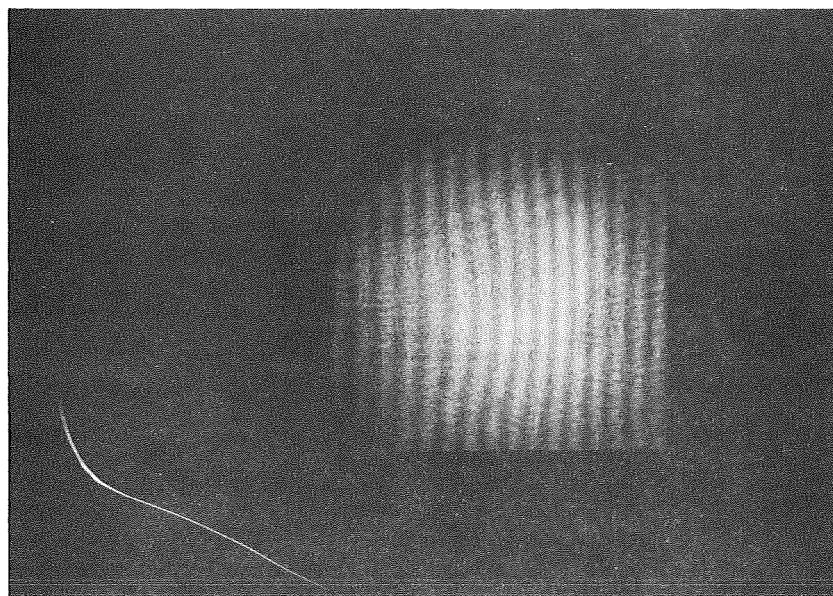


Figure 329. Hologram Interferometer Testing

All of these areas have been investigated during the course of other work and the results indicate that the aspheric segments can be tested to the figure of the finished segment to an accuracy of approximately $\lambda/30$.

Hologram interferometry has the advantage of producing interferograms with relatively few fringes and of low complexity. It is expected that direct interpretation of the interferogram will be possible and will allow a short response time between testing and polishing. During the final polishing stages, when higher precision is required, it may be desirable to use computer interpretation.

Active Positioning of Mirror Segments

1. The Modified Twyman-Green Phase-Measuring Interferometer

One goal of the figure sensor is to measure primary mirror surface figure error and provide a real-time output. The modified Twyman-Green phase-measuring interferometer is a proven means of accomplishing this goal. It is a two-beam interferometer capable of measuring both spherical and aspherical surfaces. (See Figure 330).

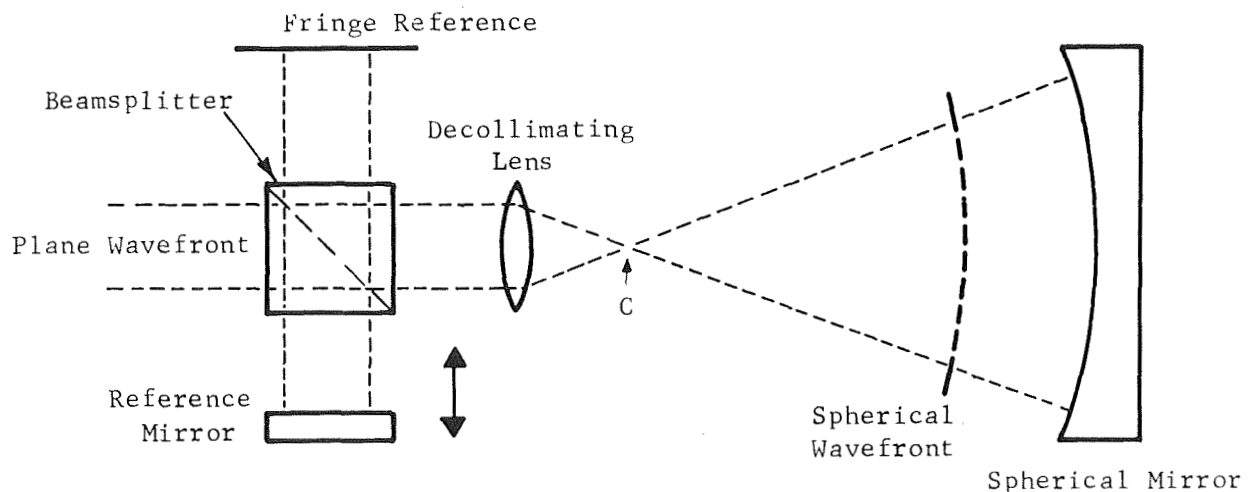


Figure 330. Modified Twyman-Green Phase-Measuring Interferometer, Optical Schematic

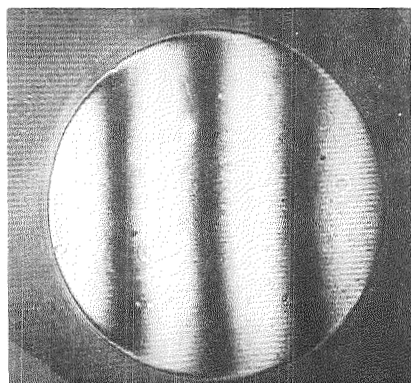
A plane coherent wavefront enters a beamsplitter and forms two exciting fronts or interferometer beams. One beam of the interferometer, the reference arm, is reflected from a plane reference reflector; the second beam, the test arm, is converted by a lens into a diverging spherical wavefront. The figure sensor is so located that the diverging wavefront is concentric about the center of curvature of the primary mirror under test. If the primary mirror has a spherical surface and is aligned so that its center of curvature (C) is located at the center of divergence of the wavefront, the light will be reflected back exactly upon its incident path. It will then pass back through the lens and again become a plane wavefront. Thus, for a perfect spherical mirror, interference takes place at the beamsplitter between two coplanar wavefronts. When there are surface irregularities on the mirror under test, or when it is misaligned, the return wavefront will not be perfectly spherical and, hence, will not be coplanar with the reference beam at the point of interference. This results in fringe patterns.

The advanced figure sensor proposed for the Active Optics Telescope system employs a slight modification of this technique. The decollimating lens is removed from the interferometer test arm and placed between the laser and beamsplitter as shown in Figure 331. This reduces errors resulting from the optical aberrations and figure errors due to the decollimating lens, since the decollimating lens is no longer in one interference beam path.

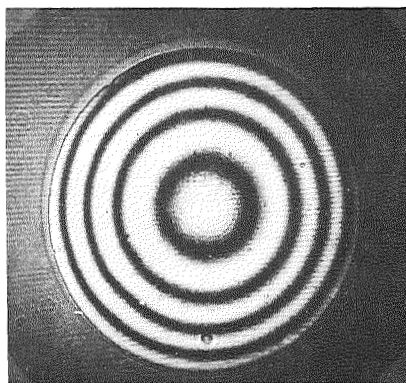
Figure 331 shows actual photographs of different types of fringe patterns obtained with this type of figure sensor. For large tilt misalignment, the interference wavefronts are plane but not parallel, producing a fringe pattern consisting of straight lines. For large axial misalignment, interference takes place between a plane wavefront and a slightly spherical wavefront, producing a circular fringe pattern. Small localized figure errors produce localized phase shifts in the fringe pattern.

If these fringe patterns were to be converted electronically into analog outputs representing surface figure errors, any type of photodetector would have to rely on measuring the variation in fringe intensity. This is a difficult measurement to make accurately and reliably, particularly when the reflectivity of the mirror varies over its surface.

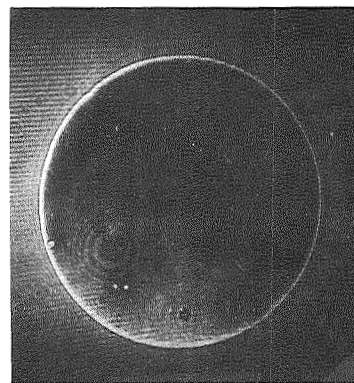
To avoid this problem, the phase-measuring technique is used. The phase-measuring interferometer, identical in optical configuration to the modified Twyman-Green, analyzes the fringe pattern electronically to permit real-time error sensing. Figure 332 is a schematic of a simplified version of the interferometer, with coherent illumination and with plane reflecting surfaces in each interference beam path. If both reflectors are perfectly plane and parallel to the wavefront, interference will occur between two plane parallel wavefronts and produce a fringe pattern having uniform intensity over its entirety. If the reference reflector is translated at constant velocity along the optical axis normal to the wavefront, the fringe pattern intensity will vary through maximum and minimum levels in a sinusoidal fashion with identical phase over the entire pattern. When two photodetectors are placed in the fringe plane, one at a position arbitrarily selected as a reference and one at a spot corresponding to a spot of unknown figure on the test surface, they convert the cycling fringe pattern



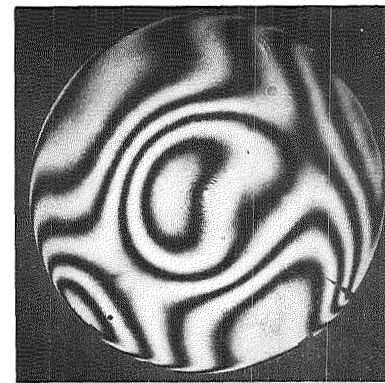
Tilt Error



Axial Error



Correct



Astigmatism

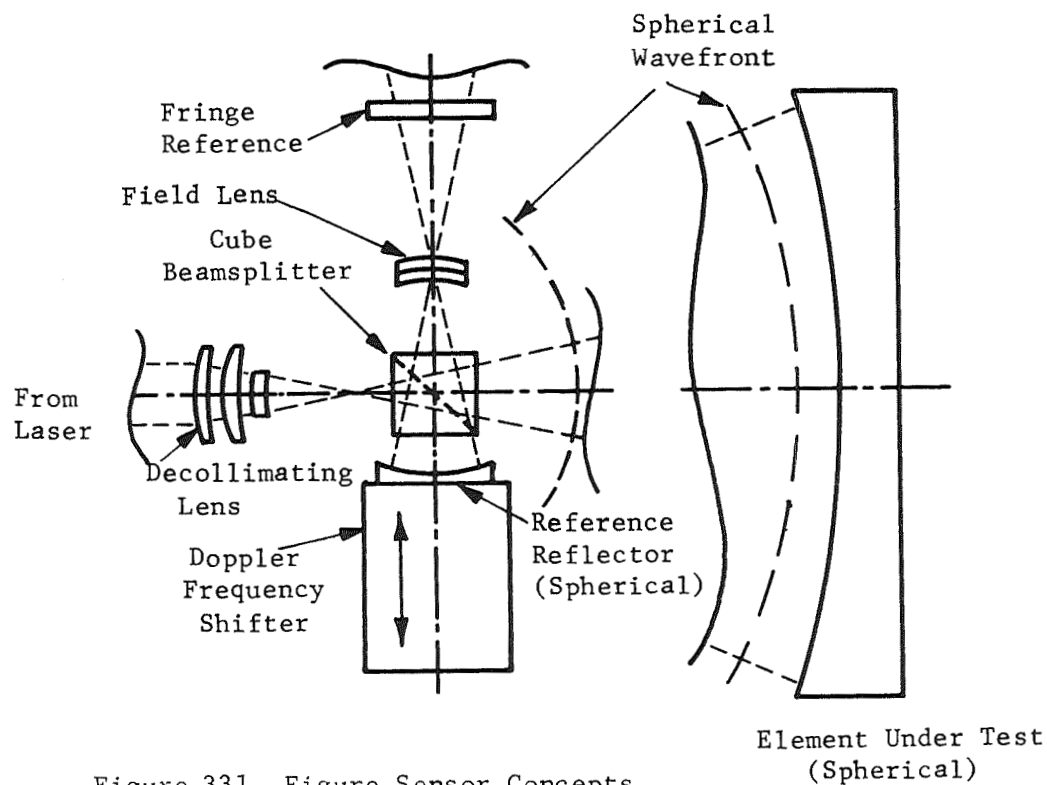


Figure 331. Figure Sensor Concepts

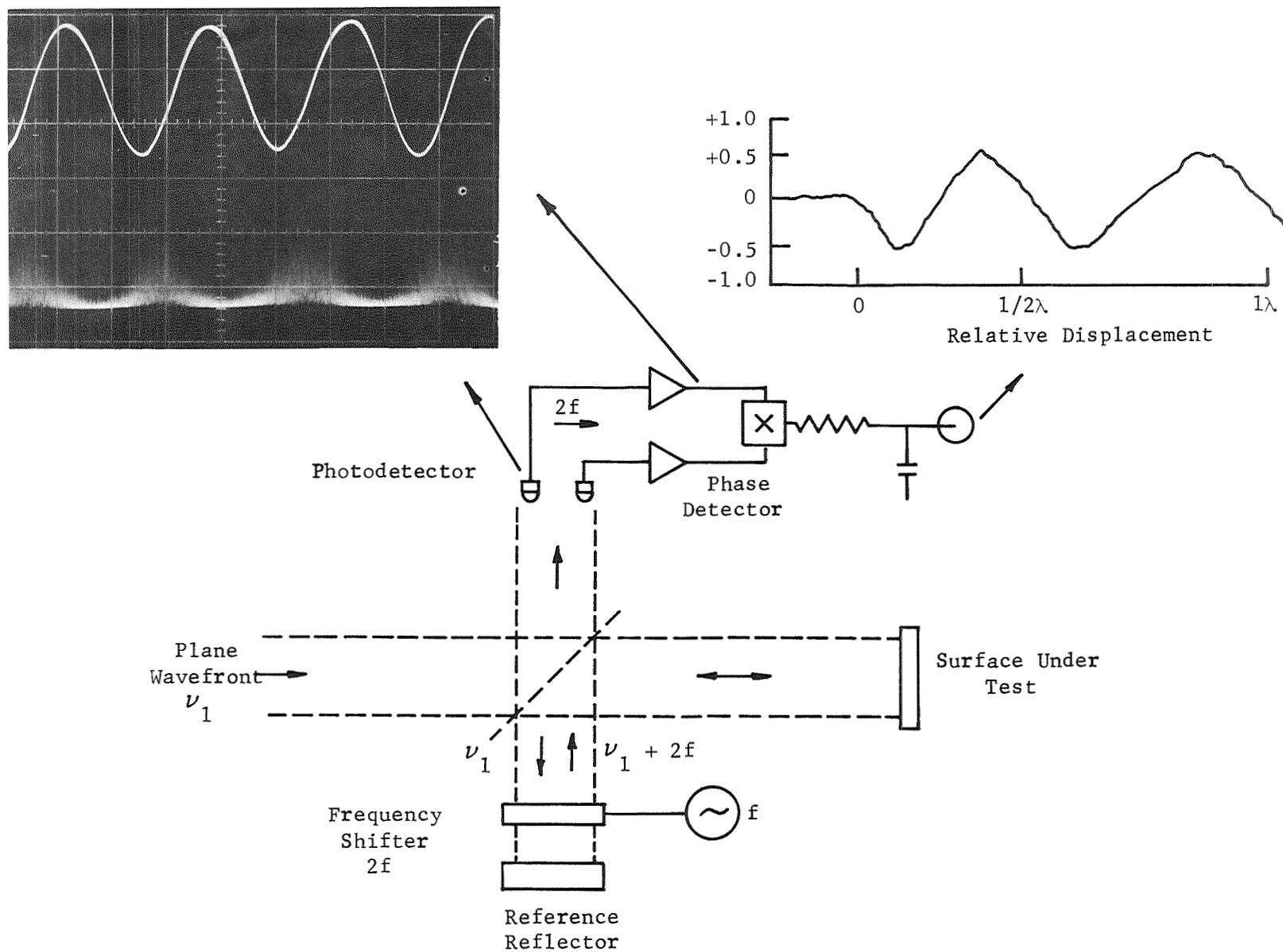


Figure 332. A Phase-Measuring Interferometer Concept

into two cycling electronic voltages or carriers. If a small portion of the test surface is high in relation to the rest of the surface, there will be a relative phase shift of the electronic signal generated from the fringe pattern corresponding to that point in the test surface. Thus, an optical figure error is converted to a phase shift of an electronic signal.

In a practical implementation of this concept, linear translation of the reference arm is regarded as the cause of a continuously increasing phase shift in the reference beam path. Continuous increase in phase of a carrier is the same as a fixed change in frequency. Thus, an optical frequency shifter is used in the reference arm of the phase-measuring interferometer. The device that translates the reference reflector to modulate the intensity at the fringe plane is referred to as a Doppler frequency shifter. It is a solid-state mechanism that utilizes a piezoelectric cylinder to generate linear motion. This mechanically rugged device has no moving parts and is driven with high-voltage triangular waves that result in constant velocity sweeps of the reference reflector forward and aft.

The phase-measurement approach has several advantages: first, the error signal has sense, that is, the sign of the figure error can be recognized from the sign of the phase error; second, an electronic phase shift can be readily detected to an accuracy of a few degrees; and third, it is independent of amplitude. A surface departure from spherical of $1/200$ wavelength produces $1/100$ of a fringe, which in turn results in a 3.6-degree electrical phase shift. An electrical phase shift of 3.0 degrees and less is well within the sensitivity of the electronic phase detector state-of-the-art.

The trace in the left-hand corner of Figure 332 is a plot of a photo-sensor output, where the high frequency noise is caused by photon discreteness (photon noise). The upper part of this same trace is the same photosensor output as it appears when filtered. The phase detector circuit measures the electrical phase difference between two points being measured on the test element and converts the difference into a dc voltage. This dc voltage therefore becomes a direct real-time signal output representing test mirror surface figure errors. In the upper right-hand area of Figure 332 is a plot of the phase detector dc output voltage as a function of the relative displacement between the two points being sensed on the mirror.

Diode photodetectors are suitable devices for detecting small and stationary portions of the fringe pattern and are useful for deriving continuous mirror figure errors for the primary and secondary mirror actuators. However, if the mirror surface figure is to be determined everywhere in fine detail, a very large number of detectors will be required. Another disadvantage of diode photodetectors is that photodiode output data is ambiguous for errors exceeding one-quarter of a wavelength and cannot be directly used for coarse alignment.

These disadvantages can be eliminated by incorporating a Vidisector camera (image dissector) to examine fringe plane data. An image dissector* has high spatial resolution and can observe up to 700 points or more in the fringe plane.

* Vidicon cameras are not suitable for this application because they are integrating detectors and the fringe pattern intensity modulation (the carrier) would be lost.

Image dissectors can continuously scan points in succession or dwell indefinitely on a single point. The continuous scanning feature is a useful tool for automatic coarse alignment. High frequency response and high sensitivity are also advantages of this device.

Active Telescope Alignment and Control

The optical tolerances for a two-meter Ritchey-Chrétien telescope using an $f/2$ primary are listed on Figure 333 and show that active control of these telescope parameters is required. As the primary and secondary mirrors are controlled with reference to the figure sensor, it is possible to transfer some of the degrees of freedom of the primary to the figure sensor. Thus, the axial and lateral translation and two degrees of tilt of the primary mirror relative to the figure sensor are most conveniently managed by tilting and translating the figure sensor with respect to the primary.

Secondary Alignment

The front surface of the secondary mirror cannot be used directly for alignment to the phase measurement interferometer using the present figure sensor scheme. An alignment surface must therefore be generated on the back of the secondary mirror to intercept a portion of the test wavefront from the figure sensor. The problems of the determination of the optimum figure of this alignment surface, the interference technique for sensing misalignment, and the data reduction technique for producing alignment signals are presently being investigated and experimentally verified at Perkin-Elmer under NASA contract. The design employed to implement the proposed secondary control system will draw on the results of this program. Several novel methods are being considered. A relatively straightforward approach consists of an almost parabolic secondary alignment surface that returns the wavefront from the figure sensor back through the same null corrector used to accommodate the aspheric primary mirror, such that it returns to a focus at the focal point of the interferometer decollimating lens when the secondary is aligned properly. The image of the secondary at the output of the interferometer is monitored to determine the phase of the returning wavefront from several different points of the secondary alignment surface. Three points that do not lie on a straight line provide information to determine tilt orientation of the secondary mirror, another point provides information about axial translation, and two additional points can provide information about the two degrees of lateral translation.

Primary Lateral Alignment

The techniques of alignment of the primary mirror in tilt and axial translation have already been developed in the previous Active Optics programs. Because of the relative sizes of the primary mirror and the figure sensor unit, it is much more convenient to tilt the figure sensor than to translate the primary mirror to correct for lateral mispositioning of the primary with respect to the secondary mirror. For the same reason, requisite tilt and axial translation of the primary mirror, with respect to the figure sensor, are also corrected at the figure sensor. This leaves only segment actuation to be accomplished at the primary mirror so that the primary mirror, wide field correction elements, and focal plane instrumentation can be fixed to a common frame with a minimum number of mechanical displacement devices.

Primary 2.0 Meters Diameter
Secondary 0.4 Meter Diameter

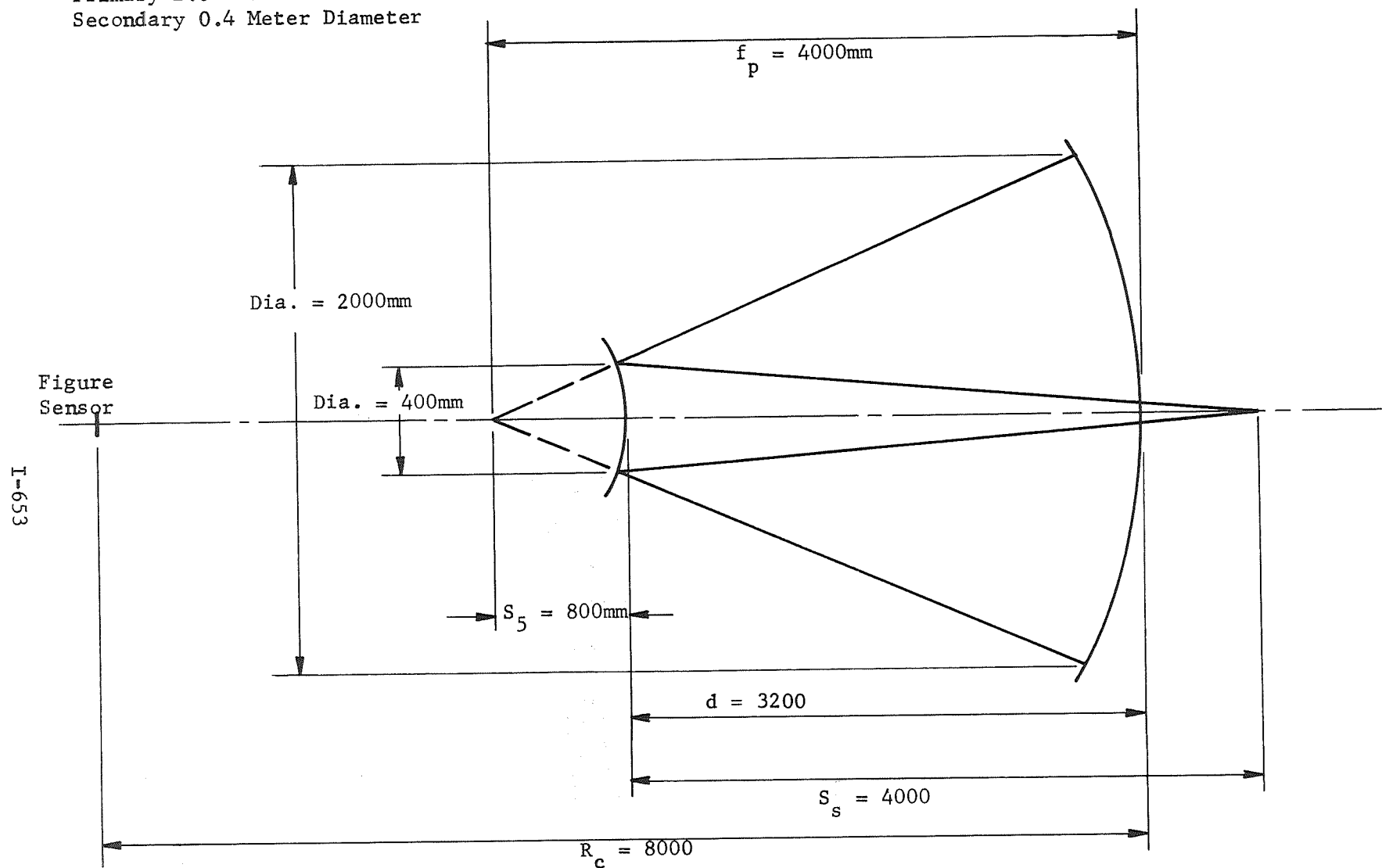


Figure Sensor

- 2 Lateral Translation- $\lambda/6$
- 1 Axial Translation- $\lambda/2$
- 2 Rotation- 2.4×10^{-5} Rad.

Secondary Mirror

- 2 Lat. Transl. $\pm 20\mu$
- 1 Axial Transl. $\pm 1\mu$
- 2 Rotation- 5×10^{-5} Rad.

Primary Mirror

- Rotation
- 14 (2 per Segment)
- $\pm \lambda/50$

Figure 333. Basic Optical Schematic

FREE FLOAT POINTING EXPERIMENT

The primary goal of this experiment is to provide a laboratory demonstration of the required 0.01 arc-second angular pointing accuracy using the free float body point guidance technique. This demonstration is based upon previous experimental work which included the development of:

1. A test setup that realistically simulated the weightless environment in space (in 3 of the 6 degrees of freedom) for a mass representing the telescope experiment package;
2. Magnetic pushers for controlling the fine pointing of the telescope;
3. Capacitive sensors for detecting errors in the displacement of the telescope with respect to the spacecraft;
4. Chain link transformers for supplying power to the telescope that require no wiring, brushes, or other electrical or mechanical connections to effect the transfer;

and resulted in an angular pointing accuracy of 0.15 to 0.30 arc-second. The setup and results of the experiment are described briefly below.

Status of Existing Technology

The main goal of the completed experiment was to prove out the control systems for three of the possible six degrees of freedom (three angular and three translational) controlled by the pointing system. The yaw rotational axis and the longitudinal and lateral translational degrees of freedom were selected for the test. The majority of tests were focused on the angular axis and the associated centering control. (Two degrees of freedom - one angular and one translational - can be controlled with a single set of magnetic pushers.) Proving out a combination of an angular and associated centering axis verifies nearly all of the critical pointing system concepts. A full scale, six-degree of freedom test would be more complete only in that an evaluation of the effects of axis-to-axis cross-coupling would be possible (although the difficulty of the test would be greatly magnified).

The prime concern in devising the experiment configuration was to develop a test device that would realistically simulate the weightless conditions of outer space. To accomplish this, a means of suspension had to be developed in which uncontrollable disturbance forces and torques were an order of magnitude less than those known to exist in the space environment. The approach used was to develop a frictionless surface (describing a plane exactly at right angles to the gravity vector) on which a mass representing the experiment package (telescope) could be supported. It was concluded that the best technique for generating a frictionless surface was to support the simulated experiment package on a cushion of air (Figure 334). The only friction from such a device is the shearing force (viscous force) between the air pads and the table, but forces of this nature are very small and weightless conditions in one plane can be closely simulated.

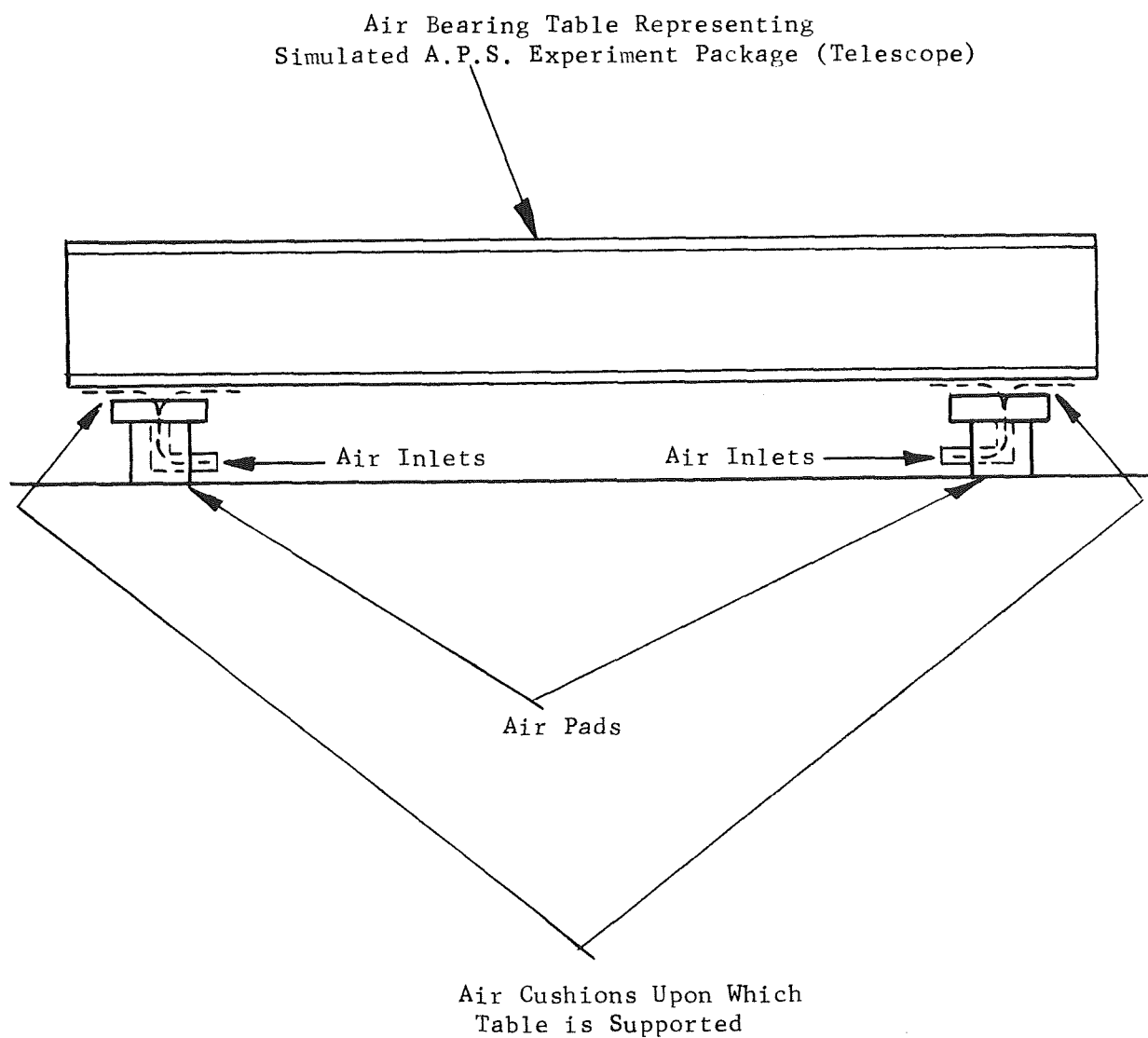


Figure 334. . Air Bearing Table Concept
I-655

Having established an environment for the simulated experiment package which is decoupled from gravity in one plane, the remainder of the experiment was directed towards proving the magnetic suspension concepts. The first goal was to determine whether or not the system could be easily stabilized and remotely controlled. It was also necessary to determine whether the magnetic pushers, capacitive sensors and chain link transformers, developed during this program, could perform simultaneously and acceptably without creating unexpected detrimental effects. A performance evaluation of the system was therefore undertaken with the major test effort directed at:

1. Determining angular and linear displacement errors caused by purposely applied torque and force disturbances intended to simulate those expected in space.
2. Evaluating the magnitude and effects of cross-coupling between the angular yaw axis and its associated centering control.
3. Acquisition performance in the yaw and lateral centering control.
4. Separate evaluation of the special components' performance when used in the system.

The weight and mass of the air bearing table representing the experiment package were less than that anticipated in the actual system. Part of the reason for using a reduced version of the system was the availability at Perkin-Elmer of a horizontal vacuum optical test chamber. The table was designed to fit into this chamber for future evaluation work, particularly a demonstration of 0.01 arc-second pointing. The decision to reduce the table size was also influenced by the fact that reduced mass actually represents "worst-case" conditions in terms of servo performance in the presence of noise. The small test bed would respond more to noise and/or high-frequency electrical and mechanical test-bed disturbances than would a system with a mass equal to that of the actual telescope.

Breadboard versions of the special electrical-mechanical components required for this system concept were constructed as part of this effort. These included the magnetic "pushers" which are attached to the telescope and set up a magnetic field which, when varied, effectively "pushes" against the spacecraft to control fine pointing of the telescope. Displacements of the telescope with respect to the spacecraft are detected by capacitive sensors which were also built for this test program. The power for these components is transferred from the spacecraft by means of chain link transformers that require no wiring, brushes, or other electrical or mechanical connections to effect the transfer.

The pointing system experiment can be considered as four separate but functionally complete subsystems. These systems are:

- Pneumatic Self-Centering Air Pad System,
- Automatic Leveling System,
- Pointing and Centering Servo Loops,
- Signal and Command Links.

The system experimental setup is shown in Figure 335. The test results obtained from the experimental pointing system itself are summarized separately. The air cushion was generated by a pneumatic system consisting of four air pads located beneath 8-inch diameter optical flats mounted to the table. The mirrors were spaced symmetrically, 1 and 2 feet from center, at the corners of the table. The system is shown schematically in Figure 336. Air is fed into the base of a pad and forced out through the pad orifices into the recess area. Before liftoff, that is, before the mirror breaks free of the top surface, pressure build up in the recess chamber. When the pressure reaches a level sufficient to develop a force capable of lifting the mirror and its associated load, the table rises to a height, air cushion thickness, of 0.0010 inch. The 0.0010-inch film thickness was an optimum design goal based on a trade-off between pad lifting power and, perhaps one of the most important design considerations, viscous friction.

The air bearing table had to be leveled or maintained perpendicular to the gravity vector to better than 0.005 arc-second in order to eliminate bias currents or forces in the magnetic pushers during operation. For this reason, a table-leveling system was incorporated that consisted of a fine and coarse mechanical leveling adjustments and an ultra-fine piezoelectric leveling adjustment. The results of the tests are described in Chapter 10 and repeated in part here. The initial tests performed with the experimental pointing system were directed at an examination of errors created by disturbances from the test setup itself. Possible test bed disturbances can arise from unknown vibrations in the air bearing table, excessive building vibrations, room disturbances from air mass movements or large temperature variations, and internal disturbances from the system electronics, such as power supply transients. The tests involved noise measurements in the yaw and centering axes. Data was specifically obtained on angular noise, linear displacement noise, and magnetic pusher coil current variations which are a direct measure of force. For the first test, the table was balanced and the pointing system placed in the low gain or normal operating mode; the yaw axis or pointing errors were then recorded over a period of 100 minutes (typical time for one orbit). An azimuth alignment theodolite was used to monitor yaw axis motions independent of the pointing system capacitive sensors. This theodolite is an optical instrument capable of consistently measuring angular deviations as low as 0.10 arc-second for long periods - 24 hours or more over a 10-Hz bandwidth. The instrument was placed outside the room containing the table. Typical chart recording samples from this test run are shown in Figure 337.

The first two traces of Figure 337 are pointing errors sampled at 3730 seconds and again at 6085 seconds into the test run. The third sample is a plot of errors with the experimental pointing system and the air bearing table turned off. This later trace provides a measure of the inherent theodolite noise, atmospheric disturbances and the building vibrations occurring between the air bearing table and the alignment theodolite. In some cases, such as sample No. 1, there is no distinguishable difference between the noise measurements with the pointing system active or inactive. The worst-case short-duration (15 second) peak-to-peak angular errors are equivalent to 0.077 arc-second in both instances. However, referring to sample No. 2 and other portions of the

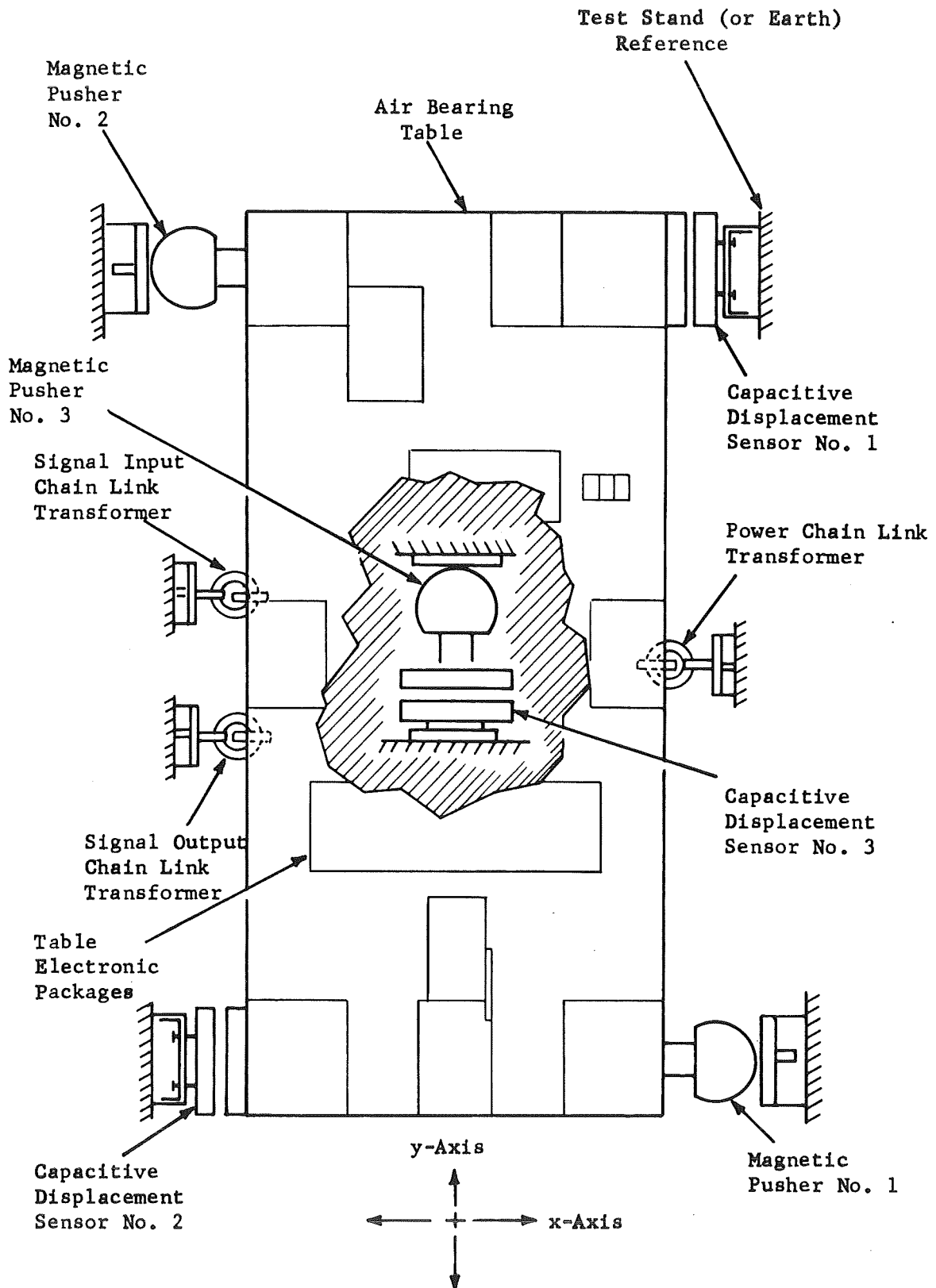


Figure 335. Pointing System Experimental Setup

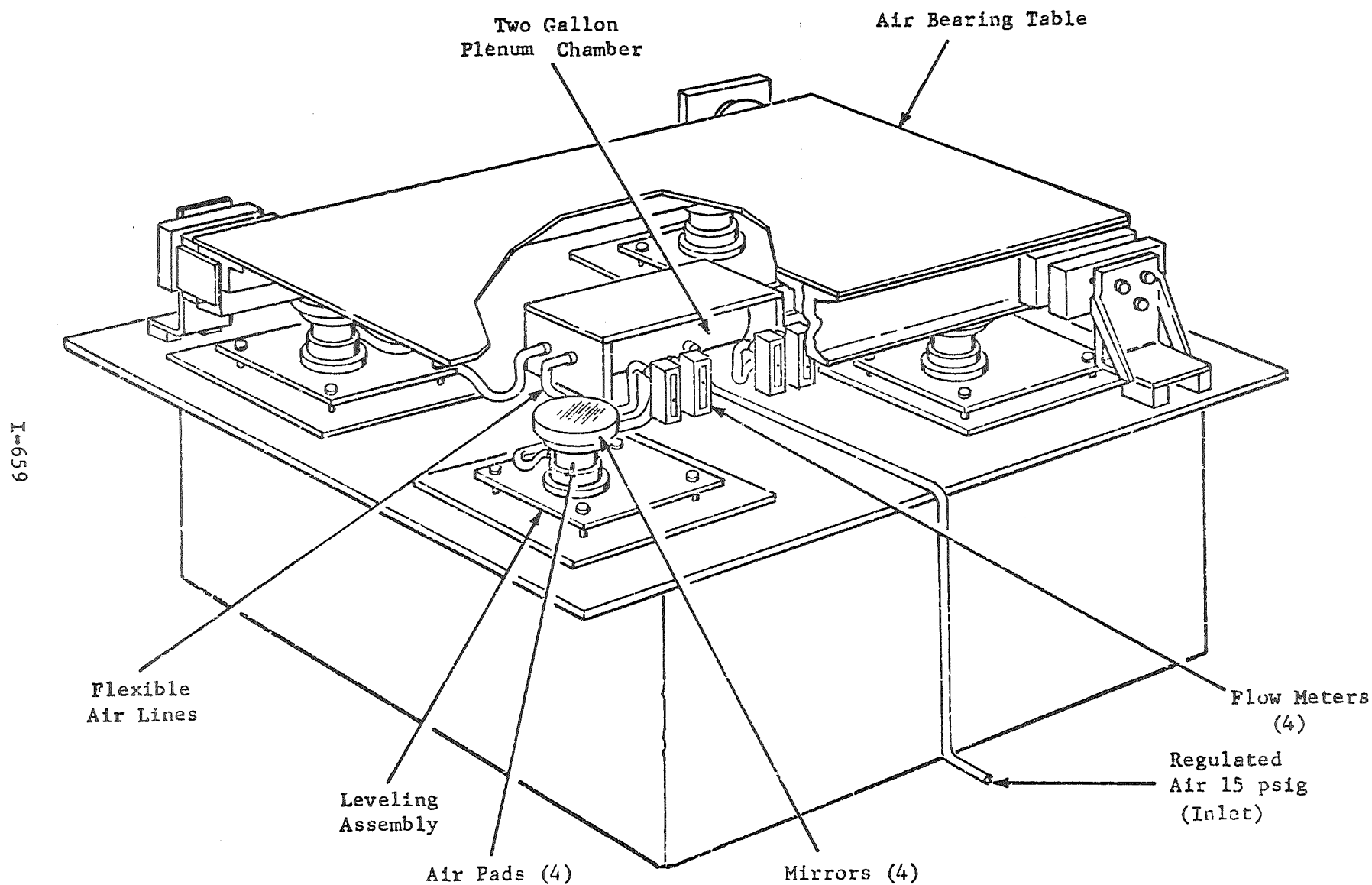
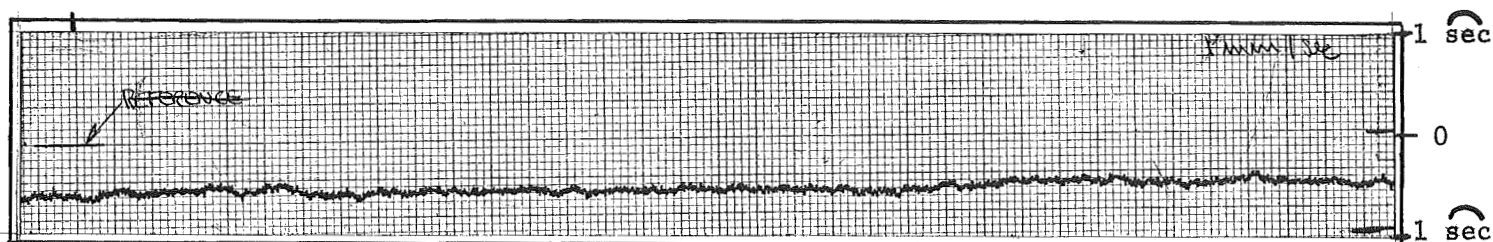


Figure 331. Self-Centering Air Pad System

Yaw Axis Pointing Errors
(Sample No. 1)

3730 Seconds
Into Run

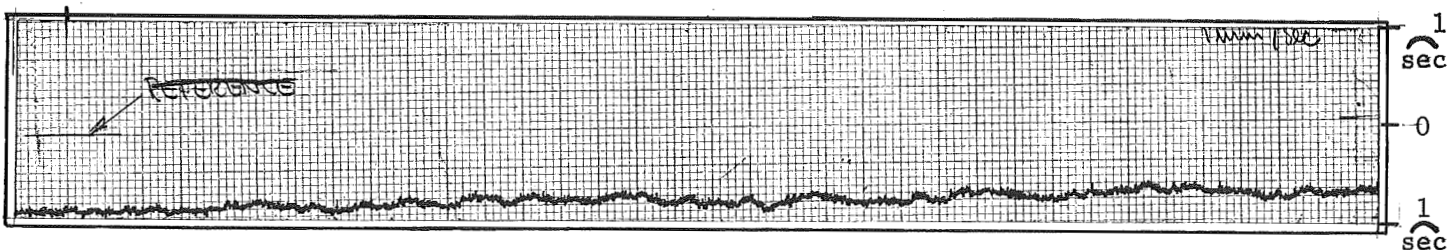
Calibration: 13 mm/Arc-Second
1 mm/Sec



Yaw Axis Pointing Errors
(Sample No. 2)

6085 Seconds
Into Run

Calibration: 13 mm/Arc-Second
1 mm/Sec



Yaw Axis Pointing Errors
(Sample No. 3)
(System Turned Off)

6400 Seconds

Calibration: 13 mm/Arc-Second
1 mm/Sec

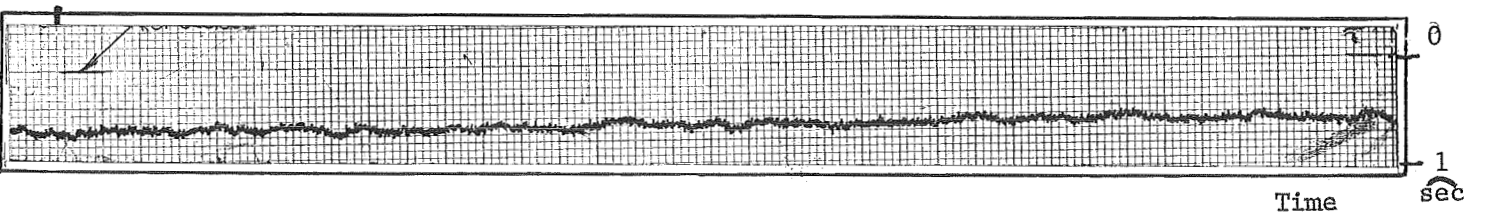


Figure 337.. Long Term Stability and Vibration
Yaw Axis Angular Error Measurements

test run not shown, pointing system errors were detectable and noise errors 0.10 to 0.15 arc-second (peak-to-peak) were measured. Again, these latter measurements included pointing system error plus the theodolite noise, building vibrations, and also power line variations. The pointing system was sensitive to random power supply fluctuations and/or 400-Hz line variations, and these electrical disturbances contributed to the pointing errors. In the final analysis, for errors of 0.15 arc-second (peak-to-peak) or less, it was difficult to determine the true yaw axis pointing errors. Nevertheless, the yaw axis experimental goals in terms of jitter were more than satisfied for this phase of the program. Pointing errors of 1 to 3 arc-seconds (peak) were initially considered to be an optimistic design goal and performance beyond this goal was left to future experimental programs.

Long-term drift was noticeable, amounting to 1×10^{-4} arc-seconds/second (time) or less. A comparison of samples No. 1 and No. 2 (Figure 337) clearly indicates the drift rate. The drift was attributed to temperature variations, since the rate of drift decreased with an increase in the amount of time that the room containing the air bearing table was left undisturbed.

The yaw-axis pointing servo loop performance was evaluated in terms of angular pointing errors for known simulated torque disturbances. The torque disturbances were simulated electrically by feeding sinusoidal voltage into the dc pusher amplifiers. The angular errors were measured with the azimuth alignment theodolite and cross-checked using the capacitive sensors. Two levels of torque were used - 0.0075 pound-foot (100,000 dyn -cm) and 0.015 pound-foot (200,000 dyn -cm) for a range of input frequencies. The results of these tests are shown in Figure 338. Errors were undetectable at the nominal 20,000 dyn -cm, 2200-second period torque disturbance levels, since these only produce pointing errors of 0.001 arc-second, which was beyond the capability of the instruments used in this phase of the experiment.

Cross-coupling from the x-axis displacement servo loop into the yaw-axis pointing loop was measured by disturbing the displacement axis and recording pointing errors. Displacement disturbances ranging from 0.025 to 0.20 inch (peak) at a frequency of 0.021 radian/second were fed electrically into the x-axis servo. The pointing errors were measured with the azimuth alignment theodolite with results as shown in Figure 339.

To improve cross-coupling of the centering servos into the angular pointing servos, an independent angular sensor, such as an optical sensor must be used.

The next step is an effort directed at achieving the 0.010 arc-second (rms) pointing accuracy-- the ultimate goal of the pointing system. Such an effort would involve a progression of efforts beginning with some refinements of the existing breadboard and finishing with a system that is equipped with an optical sensor capable of 0.01 arc-second measuring accuracy. Suggested refinements to the existing experimental system include:

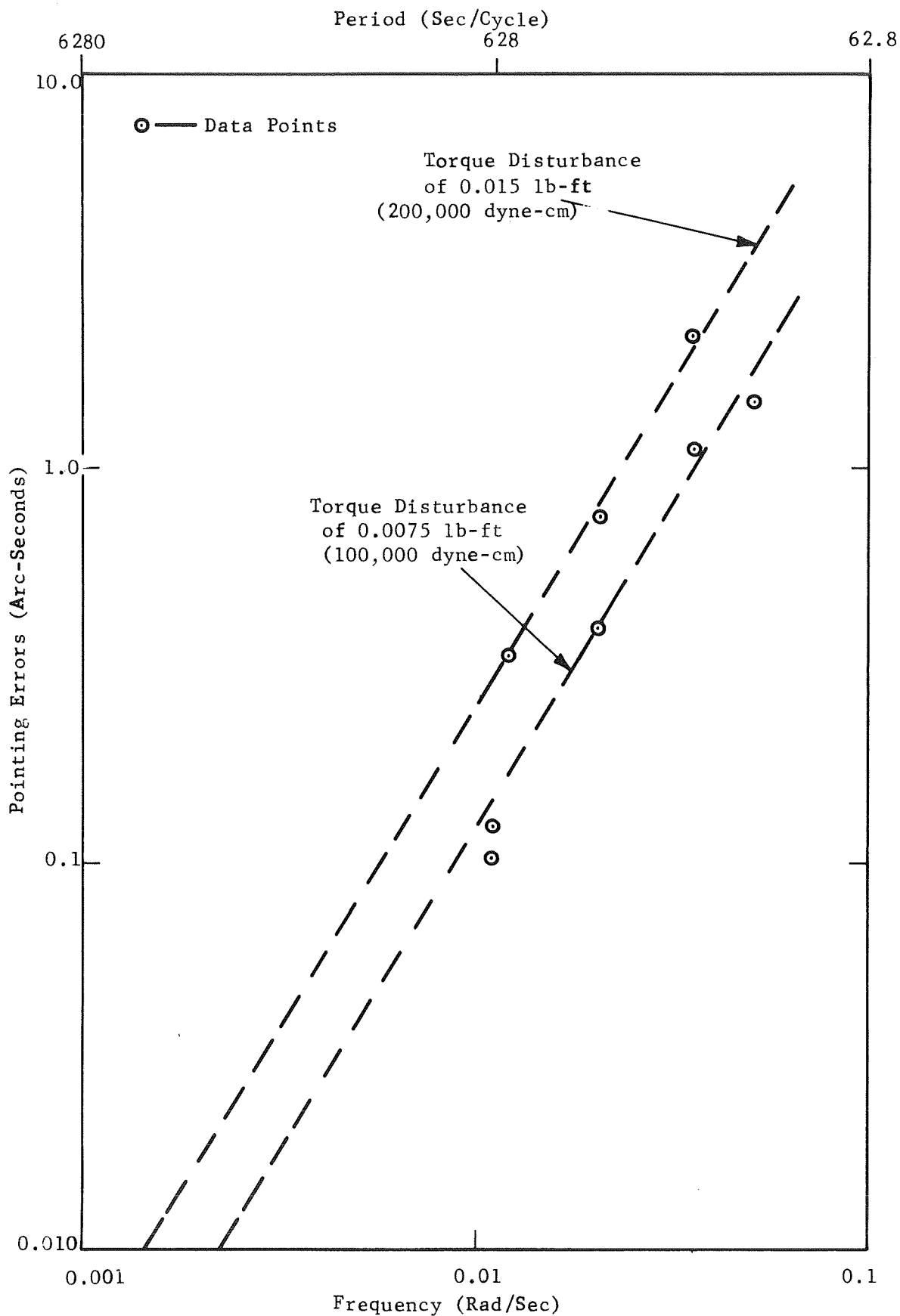


Figure 338. Pointing System Angular Errors Versus Torque Input Disturbances

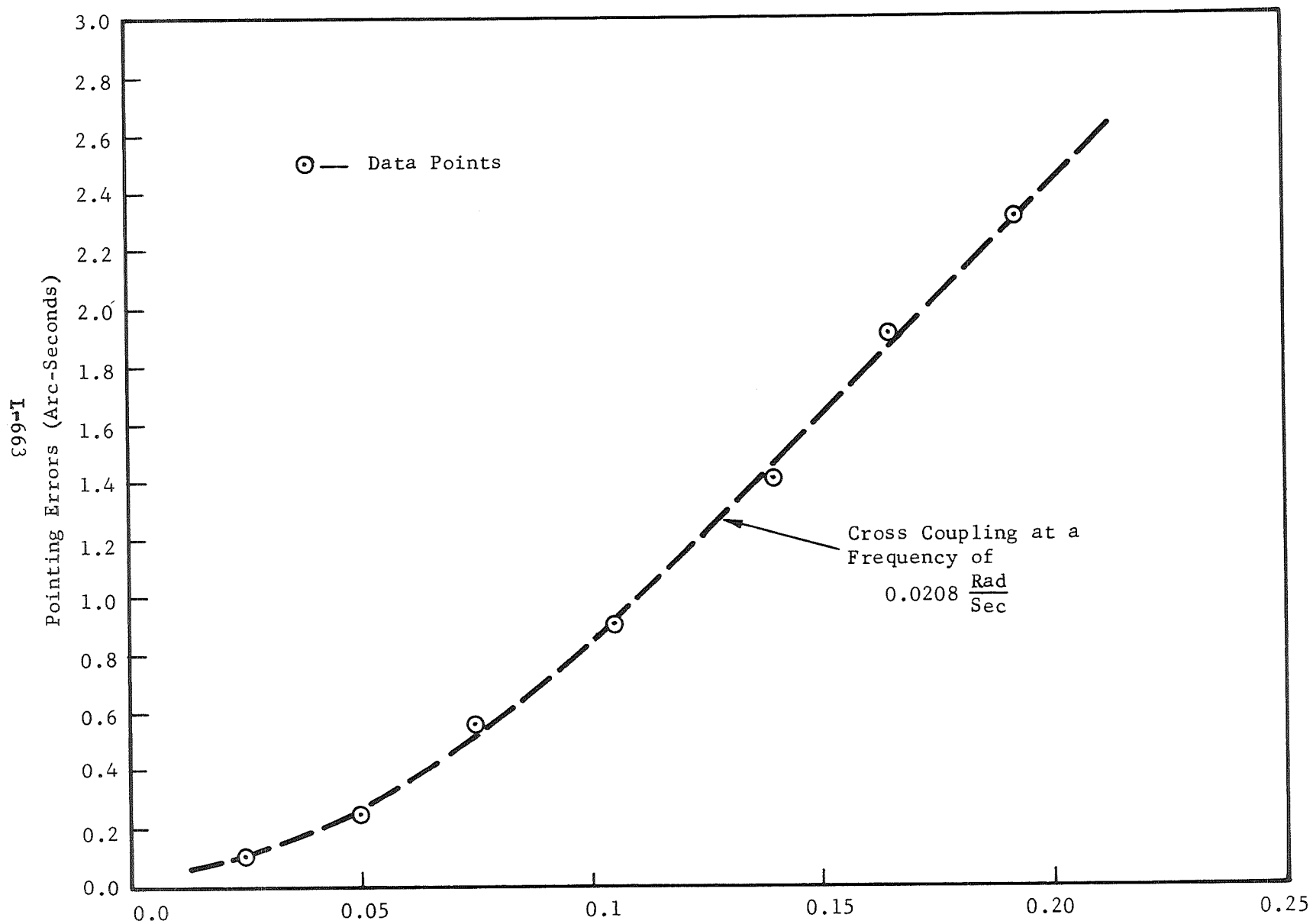


Figure 339.. Pointing Errors Due to Cross Coupling From the X-Axis Displacement Servo

- Improved electrical power system.
- Improved capacitive sensors to minimize further the effects of temperature changes.
- Improved dc torquer amplifiers to increase their efficiency.
- Expanded data link facilities.
- Use of a controlled environmental chamber such as a vacuum chamber.
- Improved vibration isolation. This would involve the design of special vibration isolators, perhaps with precise servo control or utilizing an auxiliary air suspension system to minimize building vibrations.
- Improved angular measuring instruments, capable of accuracy of 0.01 arc-second.
- A completely automatic leveling system that would eliminate the manual fine-coarse mechanical leveling adjustments.
- A means of simulating the outer spacecraft motions to verify further the isolation concept. An automatic, remotely controlled subsystem that would position the capacitive sensors and pushers would be necessary.

Experiment Hardware

In order to demonstrate on the ground the required pointing accuracy of 0.010 arc-second (rms), it is necessary to provide a floating platform which is isolated from external disturbances. Preliminary, yet rather detailed, experimental results in which a free floating platform was developed are reported in this section. This experiment demonstrated pointing to 0.10 arc-second and included recommendations for refinements of the system in order to achieve the desired 0.010 arc-second pointing accuracy. The refinements include improved isolation from external disturbances, an optical sensor capable of measuring 0.001 arc-second, and the inclusion of a subsystem representing the spacecraft.

The improved isolation must reduce the external disturbances to an order of magnitude less than those disturbances expected in space. This is required so that the angular pointing error may be measured in the presence of known disturbances, representing those expected in space, deliberately introduced through the subsystem simulating the spacecraft. Isolation from all sources of disturbance must be considered. These sources include:

1. Internal Vibrations - control of the vibrations introduced by the air bearings, the automatic leveling system, and other internal systems must be provided.
2. External Vibrations - chiefly building vibrations
3. Electrical noise in control loops
4. Air flow
5. Temperature gradients

This required isolation, although not completely straightforward, is feasible.

In order to demonstrate pointing errors of 0.01 arc-second, it is necessary to have an instrument capable of measuring errors less than that. This is to ensure that the results are not instrument limited. Relatively straightforward extensions of existing auto-collimators developed at Perkin-Elmer will be capable of measuring angular errors of 0.001 arc-second. This requires that the optical sensor is contained within the same disturbance-isolated environment as the free floating platform.

Provision for simulation of the spacecraft requires the construction of a framework surrounding the free floating platform. This framework must be movable in a controlled manner to simulate spacecraft motions.

The complete system will consist of:

1. A table representing the telescope package, suspended on a cushion of air to generate a frictionless surface in the horizontal plane. Mounted on this table, in addition to the required electronics, is an optical sensor to detect the angular position of the source (at which the telescope is pointing) and to provide the error signals for pointing at the source to within 0.01 arc-second;
2. A frame surrounding the simulated telescope package representing the spacecraft. This frame is programmed to move in order to simulate spacecraft motions;
3. An optical sensor capable of 0.001-arc-second resolution for independently monitoring the angular position of the simulated telescope package with respect to the source;
4. A controlled environmental chamber, such as a vacuum chamber. This will house the complete system for isolation from external disturbances. The vacuum chamber may be hard mounted to the floor if building vibrations permit. Otherwise it must be isolated from external vibrations.

Isolation

The required isolation can be achieved by placing the entire system, including the measuring instruments, inside a vacuum chamber, which may be isolated from building vibrations. The requirements on such a chamber are that it be large enough to accommodate the system and that the pump rate be capable of handling the leakage from the air pads. The air pads designed in the previous free float experiment operate from a 15-psig supply pressure. Thus, they may be used in a vacuum chamber by venting the air pad inlets to the atmosphere. A 15-psig (one atmosphere) pressure differential between the vacuum chamber and standard atmospheric conditions is sufficient to lift the table. The flow rate (0.50 scfm maximum per pad) was minimized so that the leakage would be below the capability of standard vacuum pumps.

A suitable horizontal vacuum chamber is available at Perkin-Elmer and is shown in Figures 340 and 341. This chamber is 5 feet in diameter by 32 feet long. The pump capability is 140 CFM. As can be seen in the photographs, the chamber is presently isolated from building vibrations using pneumatic serva-level isolators. As pointed out, later these are not adequate for the 0.01-arc-second free float pointing experiment. An isolation system which does have the necessary low spring constants can be made by floating the entire vacuum chamber on mercury, if necessary. A three-point support is best and each support would consist of a leg with low cross-sectional area with a sphere at the end (Figure 342). The buoyant force is $F_B = \rho V$, where ρ is the density of mercury and V is the volume of the support immersed in the mercury. The volume is

$$V = \frac{4}{3} \pi r^3 + A z$$

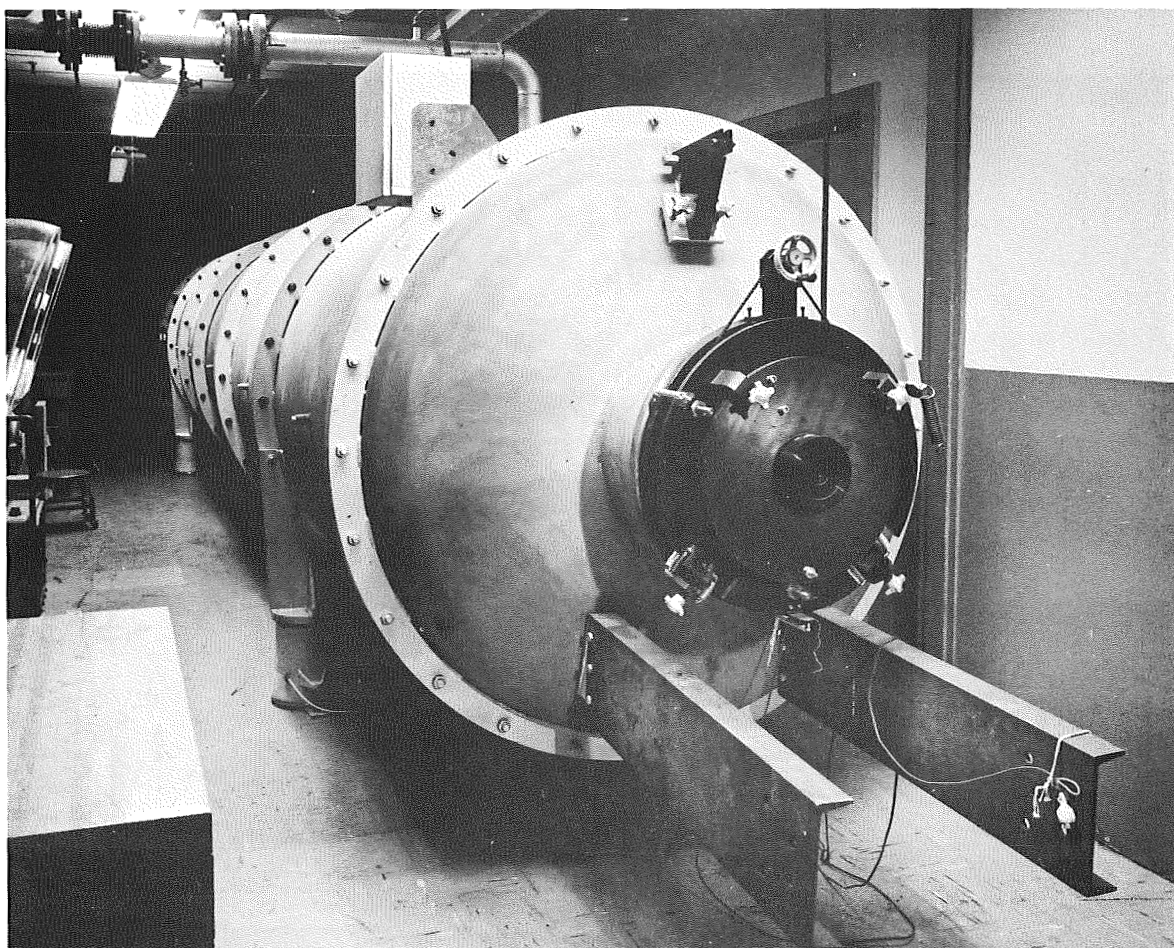


Figure 340. Perkin-Elmer Vacuum Facility

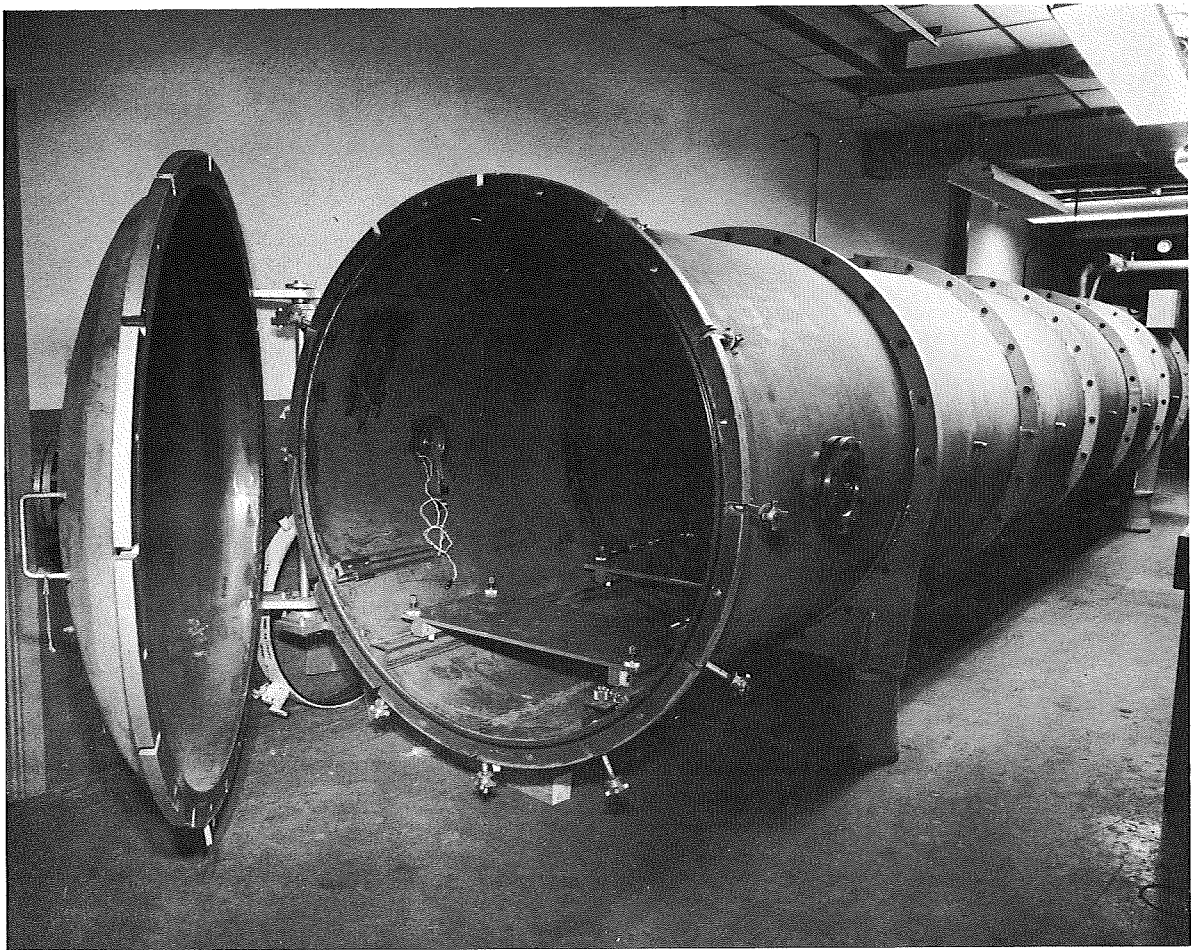


Figure 341. Perkin-Elmer Vacuum Facility

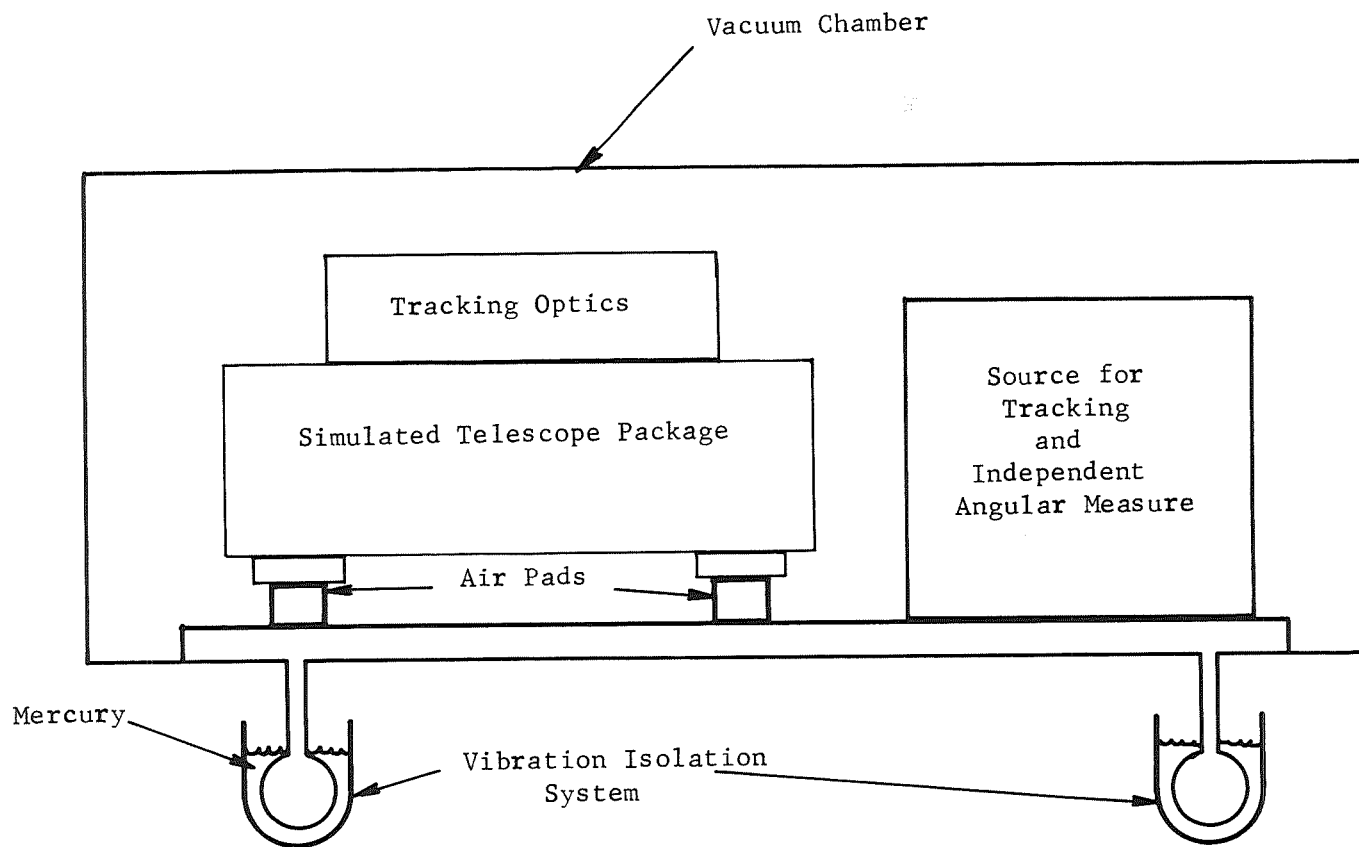


Figure 342 . Schematic of 1/100 Arc Second Pointing Experiment

where r = radius of the sphere
 A = cross-sectional area of the leg
 z = length of leg immersed in mercury

$$F_B = \frac{4}{3} \pi r^3 \rho + \rho A z$$

The effective spring constant is given by

$$K_{\text{eff}} = \frac{d F_B}{d z} = \rho A$$

and can be varied by changing the cross-sectional area of the supporting leg structure. The magnitude of the radius of the sphere and the cross-sectional area necessary can be estimated from the total weight supported and the natural frequency desired. The weight of the free floating platform is 535 lb. Allowing another 500 pounds for the associated equipment and approximately 2500 lb for the vacuum chamber, the total weight is near 3500 lb. Each of the three legs must support 1/3 of this or 1,200 lb. Since the sphere is the bulk of the volume, the radius of the sphere must be

$$\begin{aligned} \frac{4}{3} \pi r^3 &= \frac{1,200 \text{ lbs} \times 453.6 \text{ g/lb}}{13.55 \text{ g/cm}^3} \\ &= 4.017 \times 10^4 \text{ cm}^3 \\ r &= 21.3 \text{ cm} = 8.4'' \end{aligned}$$

From $\omega_n = \sqrt{\frac{k}{M}}$

where $f_n = \frac{\omega_n}{2\pi}$ is the natural freq.

$k = \rho A$ is the effective spring constant

and M = mass

$$\begin{aligned} A = \omega_n^2 M &= \frac{\omega_n^2 \times \frac{1,200 \times 453.6}{980 \text{ cm/sec}^2}}{13.55} \\ &= 41 \omega_n^2 \text{ cm}^2 \text{-sec}^2 \\ &= 1618 f_n^2 \text{ cm}^2 \text{ sec}^2 \end{aligned}$$

For $f_n \leq 0.1 \text{ Hz}$
 $A \leq 16.18 \text{ cm}^2$

This corresponds to 480 psi which is well below the yield strength of any materials which might be used for support. Note that the chamber must be restrained from moving in the horizontal plane. This may be accomplished using thin wires which will not transmit vertical motions to the chamber.

It must be carefully noted that changes in position of the center of gravity (due to movement of the free floating platform or the spacecraft simulation frame) will result in a redistribution of forces on the mercury isolation pads. This would cause a tilting of the entire chamber such that the free float platform is no longer perpendicular to the gravity vector.

In order to maintain the horizontal components of gravity below 1×10^{-5} pounds (a requirement derived in the previous free float experiment), the table must be level to 0.005 arc-second. This means that the entire vacuum chamber must be held level to 0.005 arc-second during operation. As the center of gravity of the table moves, the load on the isolation pads changes and the vacuum chamber becomes unbalanced. A simple calculation shows that for motions of 5×10^{-4} inch (the peak displacement observed during operation in the previous free float experiment) the proposed isolation system would allow the table to tilt approximately 23 arc-seconds. This is several orders of magnitude above the allowable tilt. This problem can be met by including an additional servo which controls either counterweights to maintain the c.g., or plungers in the mercury pads to compensate for the unbalance. It is an interesting circumstance that in order to measure the pointing in one axis to 0.01 arc-second, the entire test bed must be maintained to 0.005 arc-second in the other two axes.

Pointing

The error signals for pointing the free float platform to 0.01 arc-second in one axis may be derived utilizing the optical system shown in Figure 343. A collimated beam representing the guide star is focused onto the apex of a wedge-shaped image divider. The wedge is formed by the intersection of two planes and may be made quite sharp to minimize scattered light. At this apex, the light is divided into two beams, each of which then falls on a photomultiplier tube. The field lens images the entrance pupil onto the photo-cathodes of the multiplier photo tubes to minimize the effects of non-uniformity. The output of the two photomultipliers is compared, and the difference amplified, to derive an error signal for pointing the free float table. A linear approximation to the discriminant of this detector is shown by the dark line in Figure 345. Note that saturation occurs at an angle of $1.22 \lambda/D$ where D is the diameter of the entrance pupil. The slope of the discriminant may be expressed in terms of volts/unit angle.

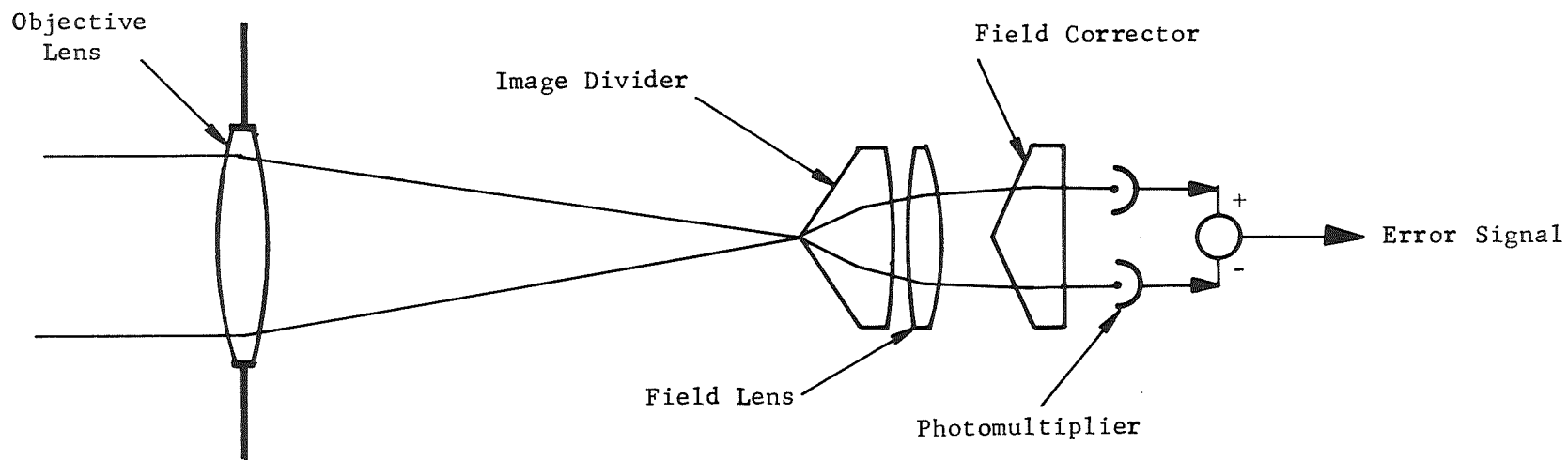


Figure 343 . Schematic of Tracking Optics

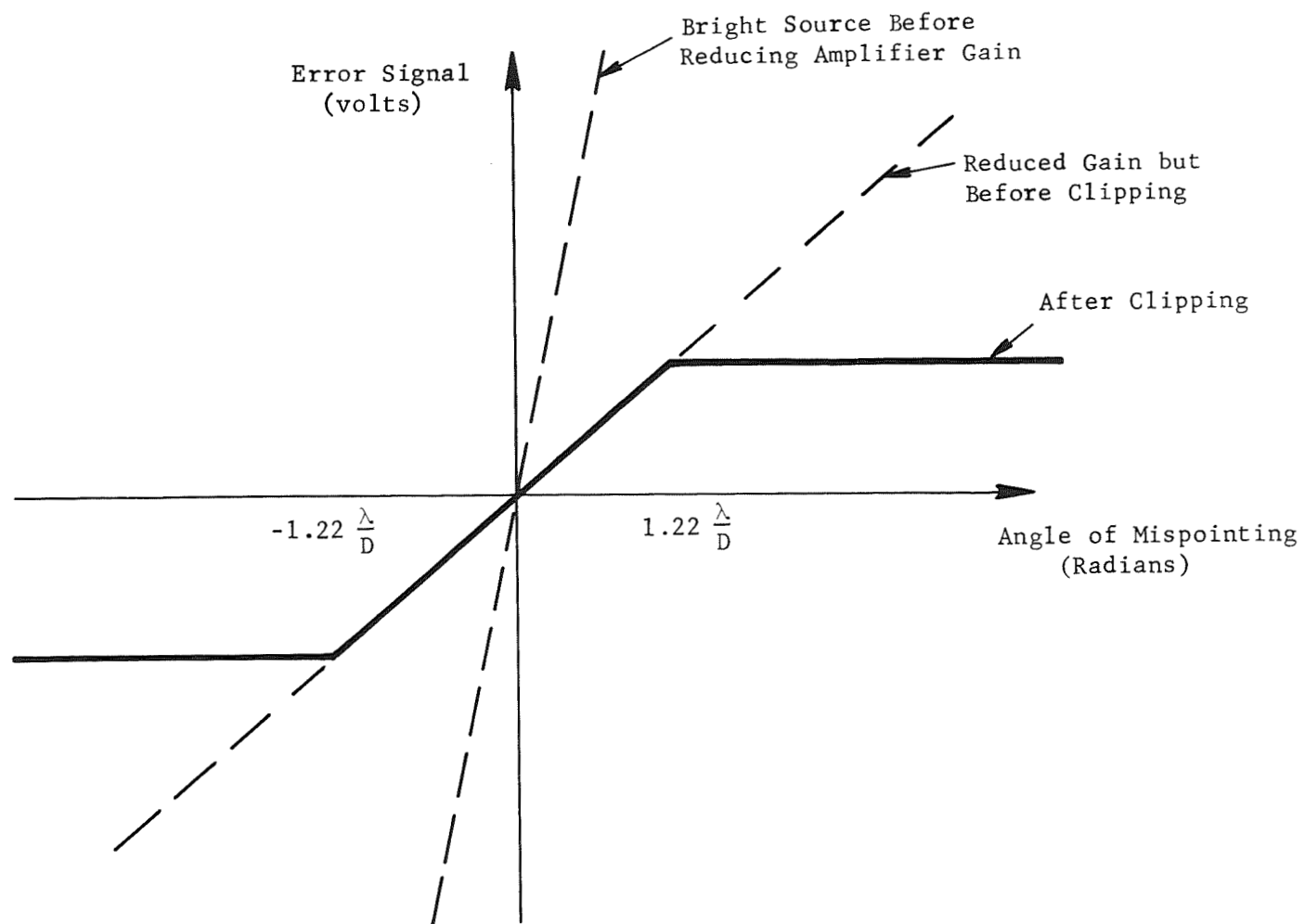


Figure 344. Derivation of Tracking Discriminant for Simulation of 2-Meter Discriminant

It is impractical to duplicate the telescope on the experimental platform because of the large optics involved. However, it is possible to simulate the tracking parameters using manageable-size optics. The pointing error is given by $1.22 \frac{\lambda}{D} \left(\frac{S}{N} \right)^{-1}$ (Perkin-Elmer Corporation Engineering Report No. 7846).

The slope of the discriminant is determined by the size and intensity of the image at the photomultipliers and the gain in the following stages. To obtain the desired slope (identical to that in the actual telescope configuration), both the intensity of the source simulating the star, and the amplification in the stages following the photomultiplier may be varied. Since the aperture of the objective lens is small relative to the primary aperture of the large telescope, the angle at which saturation occurs will be large relative to that in the 2-meter case. This saturation may be simulated electronically by using a clipping circuit following the photomultipliers which clips the signal at a level corresponding to the saturation level desired. The appropriate slope is obtained by increasing the signal level to the point where the signal-to-noise ratio is very large compared to that expected when tracking a guide star in space. To compensate for this, the amplification following the detectors is decreased to maintain the desired slope. Noise simulating the noise level in space may be introduced into the system after clipping. The noise could be generated in a photomultiplier monitoring a low light level. Figure 345 is a block diagram of the system designed to simulate the tracking parameters of the 2-meter telescope.

Laser Autocollimator with 0.001-Arc-Second Sensitivity

The Dual-Axis Laser Autocollimator (Figures 346 and 347) is an interferometric system designed to monitor changes in angles about two axes while providing high sensitivity, large dynamic range, and very high frequency response. The autocollimator includes no moving components.

The system currently being produced has a digital read-out in units of one-tenth of one arc-second, and has exhibited a dynamic range in excess of four thousand arc-seconds (1.1 degrees). The angular acceptance range is directly related to the separation of the system package from the optical flat which is being monitored. The present range of approximately $\pm$ one-half degree is obtained with a separation of 1/2 inch. Greater separations will rapidly reduce this operating range because of optical beam displacement and associated vignetting. This system is velocity limited in tilt at the rate of twenty thousand arc-seconds per time second, determined by the frequency response of the signal processing electronics. The system does not determine a unique zero angle position, but rather monitors with the above operating characteristics any changes from an arbitrarily selected zero position. If the angle change exceeds the system's angular acceptance range, the zero setting is lost and the system will no longer indicate zero when returned to the initial position. However, if care is taken to assure that operation is maintained within the angular acceptance range, the very high frequency response of the system precludes any loss of zero position.

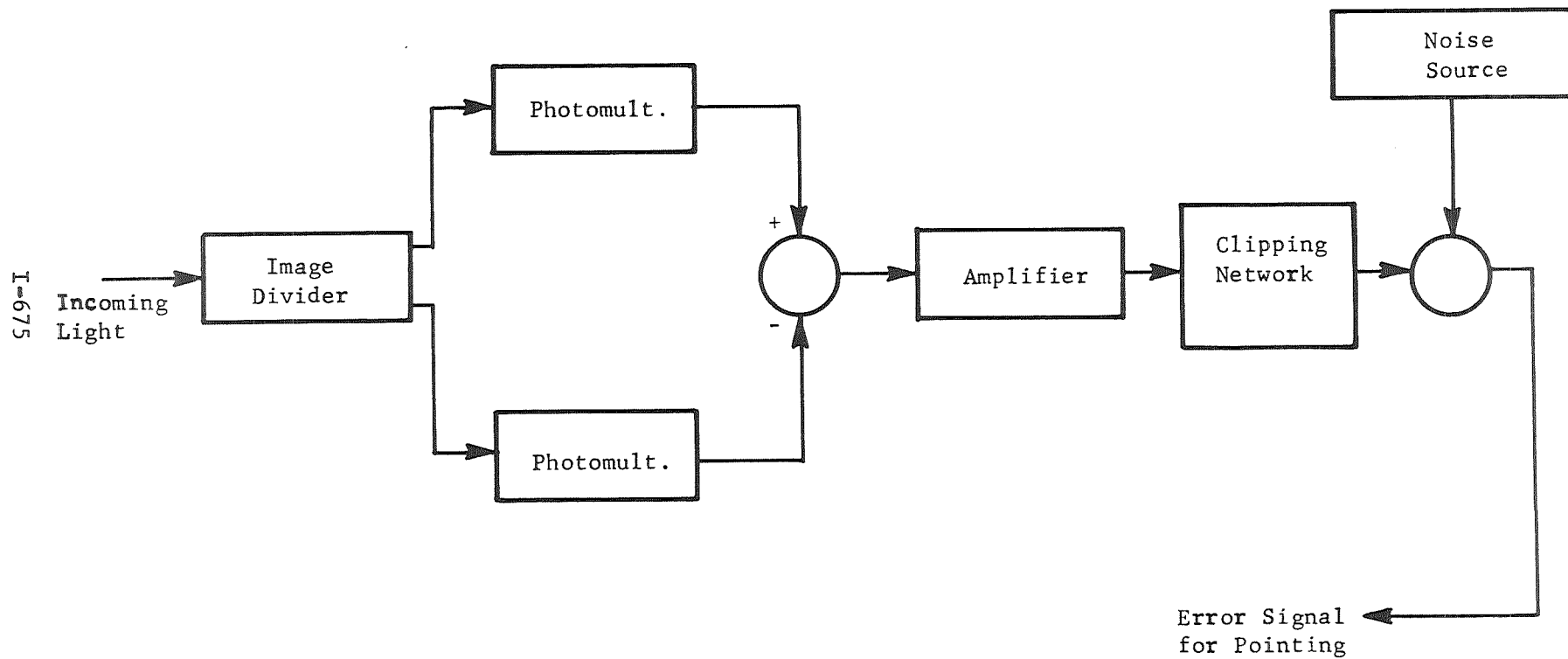


Figure 345 . Block Diagram of Tracking System

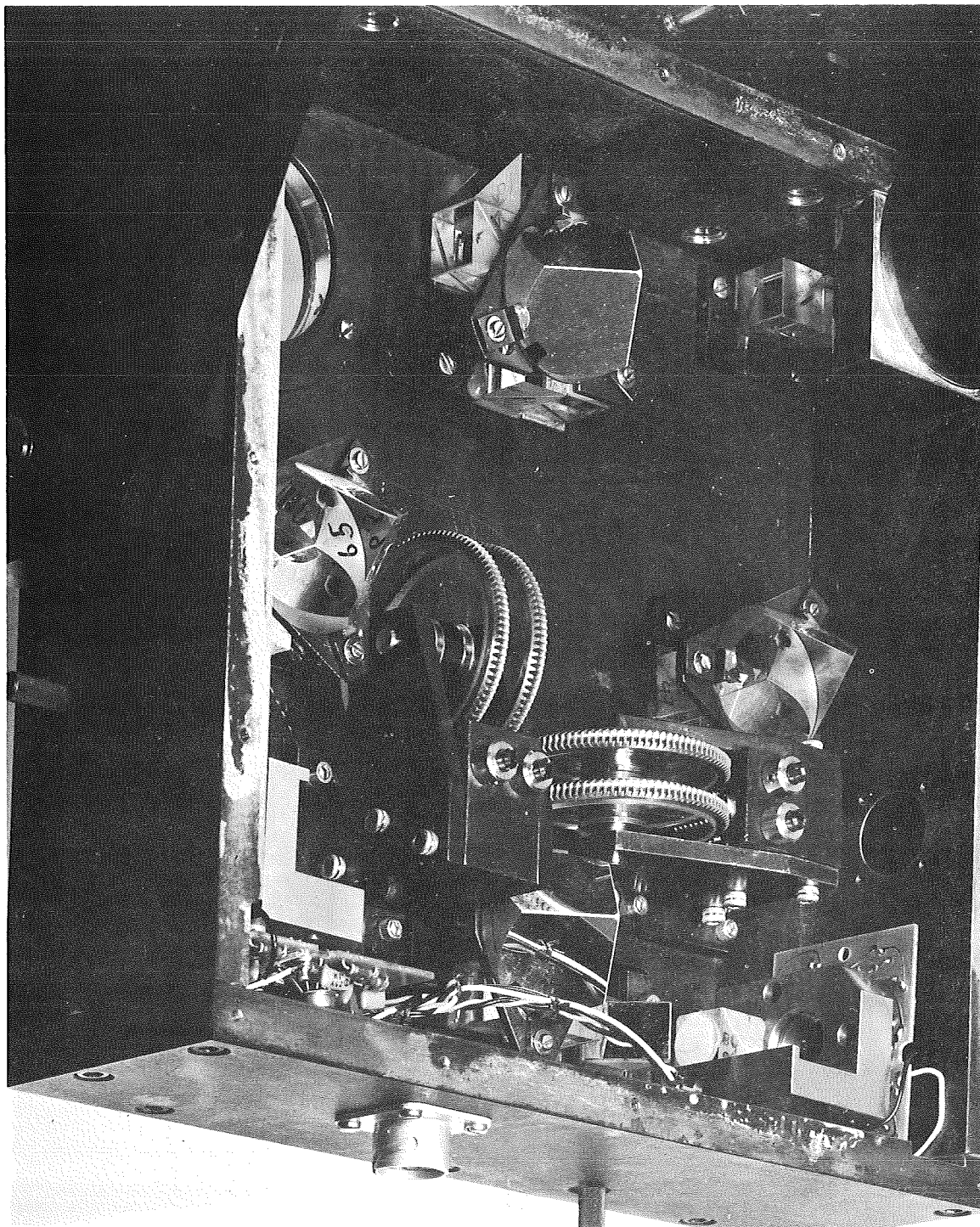


Figure 346. ATM Dual-Axis Autocollimator
(Cover Removed)

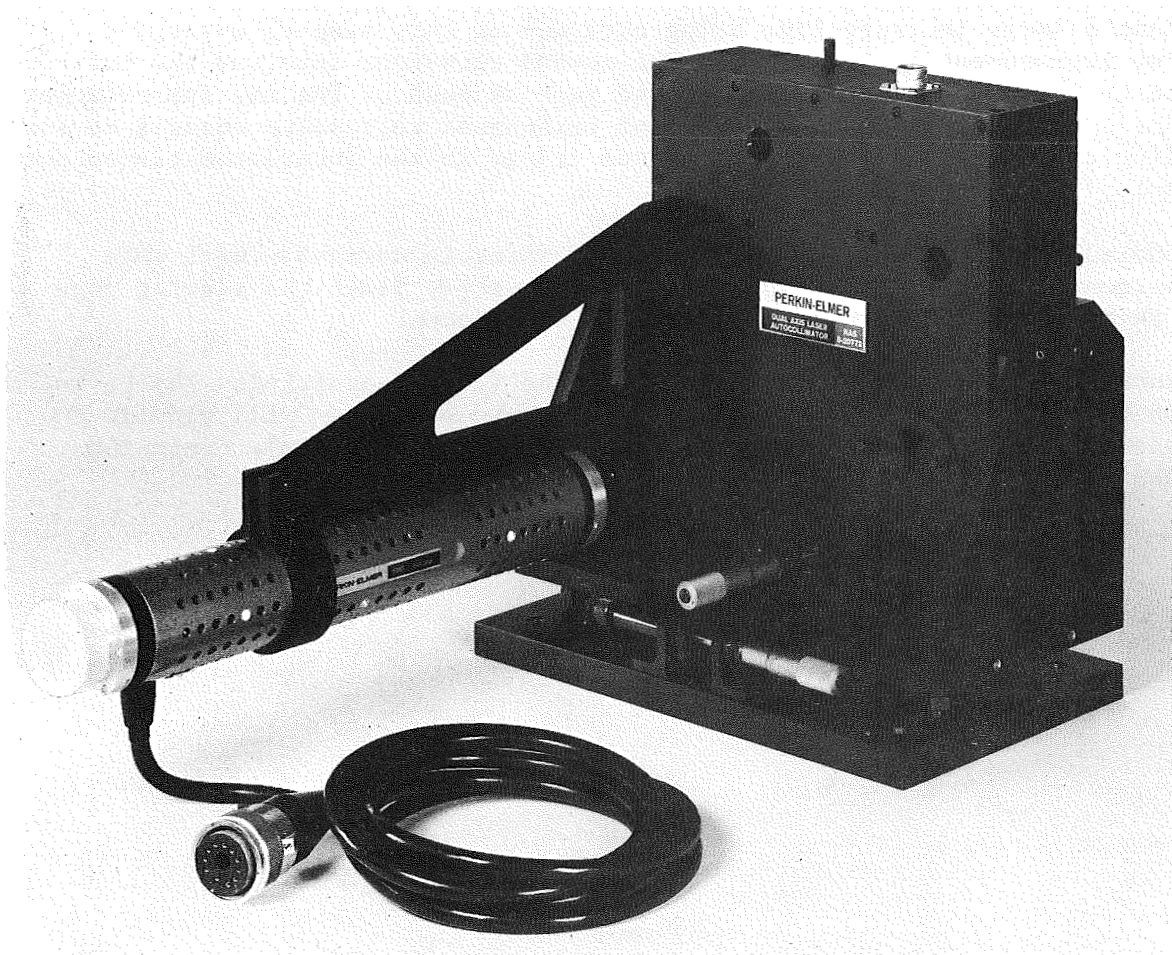


Figure 347. ATM Dual-Axis Autocollimator

The system that has been constructed is the first fully operating system of this design produced. There have been identified several modifications that can be incorporated to increase the system performance in terms of angular resolution and angular acceptance range.

The angular acceptance range can theoretically be doubled for the existing hardware by redesigning and rebuilding the optical detector packages for sensing relative light levels rather than absolute light levels.

The angular resolution can be improved by two techniques. The first is by increasing the physical dimension of the unit. Practical limitations prevent a large increase from being obtained in this manner; perhaps a factor of two improvement is practical. The second technique involves the introduction of an optical frequency shifter in one path of each of the two interferometer channels and utilizing phase detection techniques to resolve changes corresponding to a very small fraction of one fringe in the interferometer output image.

Potentially an improvement of a factor of fifty is available with this technique. However, this technique would severely limit the slewing rate and therefore the operating bandwidth of the system.

By combining the above two improvements, the system sensitivity may be improved to on the order of 0.001 arc-second. This autocollimator will provide the necessary sensitivity for independently monitoring the angular pointing of the free float table.

THERMAL VACUUM EXPERIMENTS

In order to achieve diffraction-limited performance in a large optical system in space, it is mandatory that the primary mirror does not undergo excessive thermoelastic deformations. This implies that an optimum thermal design must minimize thermal gradients in the mirror. Unfortunately, analyses are only available for predicting mirror distortion in the presence of certain simple temperature gradients and there are no solutions to the general problem and no adequate measurements have been made which simulate the behavior of large mirrors under realistic space environment conditions. Therefore, the most important ground-based experiments to undertake are detailed experimental and theoretical investigations of the figure of a mirror when subjected to a controlled thermal environment properly simulating space conditions.

The optimum method of using mirror materials is to operate them around the temperature where the coefficient of expansion is zero and consequently the thermal testing should include measurements around the zero coefficient temperature range.

In proposing a program of thermal vacuum experiments, it is realized that the simplest experimental approach done is to make measurements upon existing mirrors as was done at room temperature in an initial experimental program at Perkin-Elmer where measurements were made upon the 32-inch eggcrate OAO-C mirror fabricated for the Princeton Experimental Package. Similar experiments are planned for the 36-inch Stratoscope II mirror in the near future. One drawback to this approach is that both mirrors are composed of fused silica whereas a thorough thermal vacuum experimental program should include mirrors fabricated from some of the newer mirror material candidates such as #7971 ULE*, CerVit** and Silicon.

ULE is a glass developed by Corning and is composed of approximately 93 percent SiO_2 and 7 percent TiO_2 . The glass is formed by the pyrolytic decomposition of SiCl_4 and TiCl_4 . The metals are introduced into the flame by saturating oxygen with the chlorides before they enter a series of burner nozzles above a rotating crucible.

CerVit is a partially devitrified glass with which crystallization heat treatment can be accomplished to produce a coefficient of expansion that is nearly an order of magnitude less than pure fused silica. By optimizing the heat treatment process, a coefficient of thermal expansion identical to that of ULE can be obtained. It should be noted that this is not the commercial grade of CerVit, which has an appreciably higher coefficient of thermal expansion.

The properties of silicon and the reasons why it is a strong candidate for space-borne mirrors are presented later.

* ULE, a product of Corning Glassworks, Corning, N.Y.

** CerVit, a product of Corning Glassworks, Corning, N.Y.

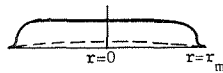
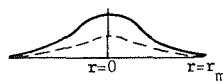
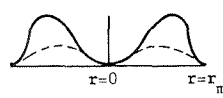
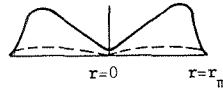
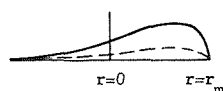
The experimental part of the thermal vacuum testing of space mirrors can be accomplished using the following facilities and measuring techniques:

- A vertical test tank operated under vacuum together with an air bag support for the mirror.
- An array of heater pads placed on the side and back surface of the mirror to produce controlled thermal inputs.
- An array of thermistors and thermocouples mounted on the mirror surface to measure temperature gradients.
- A scatterplate interferometer placed at the center of curvature of the mirror.
- A method of measuring the distance of the interferometer to the mirror such as a ranging interferometer.
- Data reduction equipment to generate the equation of the best fit parabola and the rms figure value.

The analytical work uses thin plate theory and axisymmetrical temperature gradients as a starting point. The use of these simple approaches has provided surprisingly good agreement for the OAO mirror between analytical and experimental results as can be seen in Table 46.

It is necessary to extend both experimental and analytical results to unsymmetrical temperature gradients and different mirror materials to assess the effect of thermal gradients upon rms figure and focus changes.

TABLE 46 . COMPARISON OF EXPERIMENTAL AND ANALYTICAL RESULTS

Temperature Distribution			Thermally-Induced RMS Deformation (Waves)		Focus Shift (Microns)		
	Profile	Gradient	Max ΔT	$\lambda = 0.6328$ Micron Experimental	Analytical	Experimental	Analytical
1		Axial	0.8°C 2.8°C	0.006 0.017	0.010 0.013	- 40 -160	- 35 -130
2		Radial (Central Heaters)	3.0°C 7.0°C 12.0°C	0.011 0.015 0.025	0.013 0.028 0.053 } (1)	- 75 -100 -150	- 5 - 10 - 15 } (1)
3		Radial (Parabolic; Edge Heaters)	8.0°C	0.021	0.021	-180	-140
4		Radial (Linear)	8.0°C	0.022	0.021	-280	-200
5		Lateral	5.0°C 11.5°C	0.020 0.028	No Data No Data	-125 -125	No Data No Data

(1) See Discussion

— Mirror Rear Surface
 --- Mirror Front Surface

I 89-I

IMAGE MOVER FABRICATION AND TEST EXPERIMENT

The image mover assembly which is used for guidance in the vidicon imaging system module must be optimized in both the optical and thermal-mechanical design. A workable configuration of the image mover is shown in Figures 348 and 349 .

Design Considerations

Since the sole function of the image movers and telescope corrector elements is to form images of auxiliary stars upon an image dividing prism in the fine pointing error sensor, they are considered part of the pointing system. Consequently, only those variables which affect the stability and precision of pointing of the telescope during an exposure are important in the design of the image mover subsystem. Thus, there is no requirement for light transmission in the far ultraviolet region since the fine error sensor photo-multiplier is insensitive to the far ultraviolet spectrum.

From the standpoint of the pointing system, the design requirements for the image movers are as follows:

(1) The aberrations in the image produced at the image-dividing prism must be small enough so that the energy transfer rate across the image-dividing edge is not degraded significantly (10 percent or less) from that of an ideal diffraction image.

(2) The position of the center of the image on the image-dividing prism must not move by more than a small fraction of 0.01 arc-second during an exposure (which may exceed 90 minutes). Motions of this kind can be caused by motions of the corrector lenses and image movers as a unit, motions of the folding prisms, and relative motions of the segments of the "flip" mirror. Therefore, the thermal and mechanical designs of these optical parts must be such that single-orbit changes in the thermal environment are incapable of causing motions or tilts that exceed this number.

In addition to these performance requirements, there are overall design requirements set by the space environment, the space and power available, the thermal design, etc. The design given here was made for the Princeton OAO - F vehicle and is included here to outline the steps that must be considered in order to complete a detailed design for the LTEP instrument section.

Anticipated Thermal Environment

Effectively, the image movers transfer an arbitrarily positioned bundle of light to a fixed position in the $f/10$ focal plane. To accomplish this, there must be a unique correspondence between the mapping of the points in the original focal plane and the fixed point in the $f/10$ focal plane. Small biases introduced between the earth and space environments can be calibrated out, but in the space environment the mapping must be simply a function of the setting of the image mover.

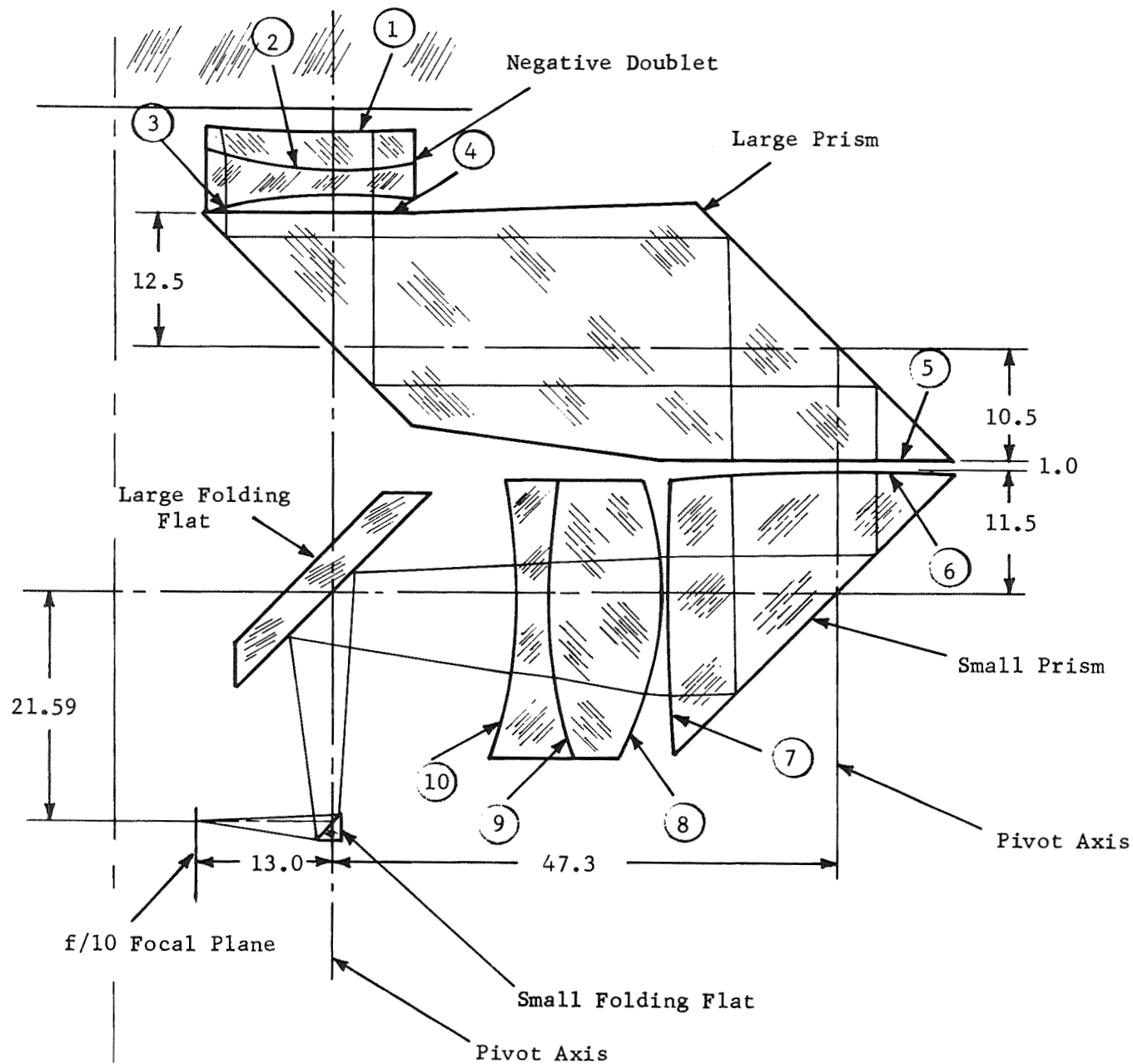


Figure 348 . Image Mover Optical Elements

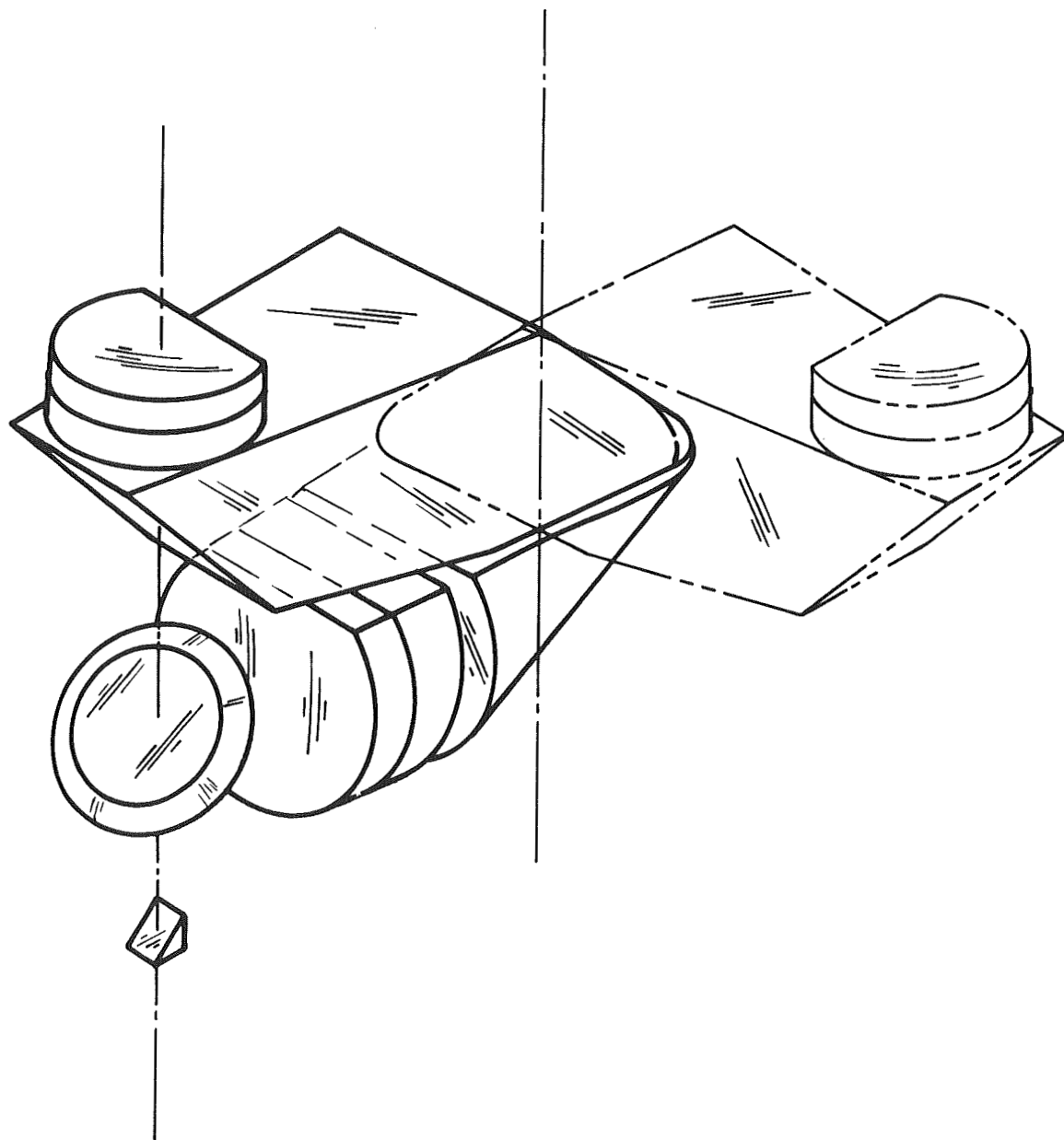


Figure 349 Image Mover Pivoted About Both Axes

Of the possible conditions encountered in the space environment that could disturb this unique mapping relationship, it is apparent that the most consequential is the changing thermal environment. As the average temperature changes and the temperature differential from one point to another changes, there will be corresponding changes in optical alignment, in the physical size of the optical elements, and in the optical properties of the elements (for example, the index of refraction of the glass). Each of these changes must be studied with regard to pointing system performance. The philosophy adopted in this study is to assume "worst-case" temperature gradients, identify those effects (e.g., alignment changes) which can degrade or move the guide star image, and then compute the magnitude of the effect caused by the assumed worst-case condition. If the sum of the effects yields a negligible reduction in pointing system performance, then the thermal and mechanical design is adequate.

In general, the problem requires that the effects of motions of each and every optical element in six degrees of freedom be investigated, as well as all combinations of motion, including gross motions of the image mover assembly and the corrector lenses. It is obvious, however, that many of these motions will have no effect (e.g., rotation of a folded mirror in its own plane). Equally obvious, only some of the possible motions will occur for conceivable temperature changes. We shall then formulate the problem which is to be investigated in terms of important motions of optical elements as well as probable motions.

The following paragraphs are intended to define the specific problems which were investigated and the associated results. Specifically, the anticipated worst-case thermal environment, thermally induced motions of the various optical elements, and gradients in the glass (and the assumptions made) are all defined. Finally, the mechanical design problems and the resulting design concepts are given.

The thermal environment which the image movers experience is a function of the experiment package heat balance design, the location of power-dissipating components in the experiment package, and the expected programmed orbits of the vehicle. As stated earlier, the thermal design of the experiment package will be completely passive, such that the components contained within the package will be required to operate satisfactorily over a temperature range of about 30°C between extreme orbit attitudes. The thermal time constant of the primary mirror and drum assembly is many times larger than the orbital period and, therefore, temperature variations in the image mover during an orbit will be negligibly small. Computations for the primary show gradient changes within the hot orbit to be 0.10°K or less (24-hour albedo variation). Gradient changes within the backing plate will be considerably smaller because of shielding by the primary mirror.

The system thermal analysis computed steady state temperature distributions at various stations in the APS. The primary mirror and image mover assembly are considered to be one, well-coupled thermal mass. As the analyses show, the temperatures computed for the primary strongly reflect the temperature of the mirror backing plate which is synonymous with the casting containing the image movers. Correspondingly, the transient thermal analysis is directly applicable.

We will compute the maximum transverse, axial and radial gradients expected in the image movers and the changes in these gradients that take place during an observation just after a programmed orbit change. Although the internal heat sources (coarse guidance package and image mover drive motors) can introduce temperature gradients in the structure, they produce a negligible effect on the image movers.

From Figure 350 it can be seen that the image movers are integrally mounted within the casting which serves as the basic structure for the primary mirror cell as well as the conducting backing plate for the mirror. The function of this casting is to provide an isothermal enclosure for the image movers as well as a stable heat sink for the primary mirror. This casting is represented by node 3 in the system thermal analysis.

Consider the hot orbit where there is a 0.3°K temperature difference between nodes 0 and 3. A 0.03°K drop will occur between the center and edge of the backing plate. The radial gradient in the structure will then be $1.5 \times 10^{-3}^{\circ}\text{K}/\text{inch}$ and the maximum axial gradients can be computed in a manner similar to that employed in the primary mirror study. The maximum steady state axial gradient for the primary was found to be 0.16°K assuming an effective thermal conductivity for quartz of 1.2×10^{-2} watt $\text{inch}^{-1}\text{K}^{-1}$. The conductivity for aluminum is 4 watt $\text{inch}^{-1}\text{K}^{-1}$ or 300 times larger than the quartz. The axial gradient will then be about $5 \times 10^{-4}^{\circ}\text{K}$. The worst-case transverse gradient can be approximated by assuming that there is a 6°K gradient across the APS due to the uneven solar heating of the OAO when in the cold orbit. If the insulation blanket reduces this gradient by a factor of 10 (normal superinsulation is good for 50 to 100 times attenuation), the transverse gradient will be 0.6°K or 0.015°K per inch in the image mover structure. The maximum steady state temperature gradients that will exist in the image mover mounting drum are:

$$T_{\text{axial}} = 1.25 \times 10^{-4}^{\circ}\text{K}/\text{inch}$$

$$T_{\text{radial}} = 1.5 \times 10^{-3}^{\circ}\text{K}/\text{inch}$$

$$T_{\text{transverse}} = 0.007^{\circ}\text{K}/\text{inch}$$

A similar analysis was performed for the gradients incurred for the grazing orbit case, but these were significantly lower than the values given above. All these gradients are extremely small and, as will be seen, have no deleterious effect on the image mover performance. Of greater significance in degrading the image quality will be the temperature change in the image mover between the hot and the cold orbit (32°K) and the resultant change in index of refraction of the optical elements during this temperature change.

The change in temperature gradients can also be computed. We found that $\frac{dT_R}{dt} = 0.13 e^{t/\tau} ^{\circ}\text{K}/\text{hr}$ for the backing plate during the cool down of the APS after an orbit change from a hot to a cold orbit. These values can be used

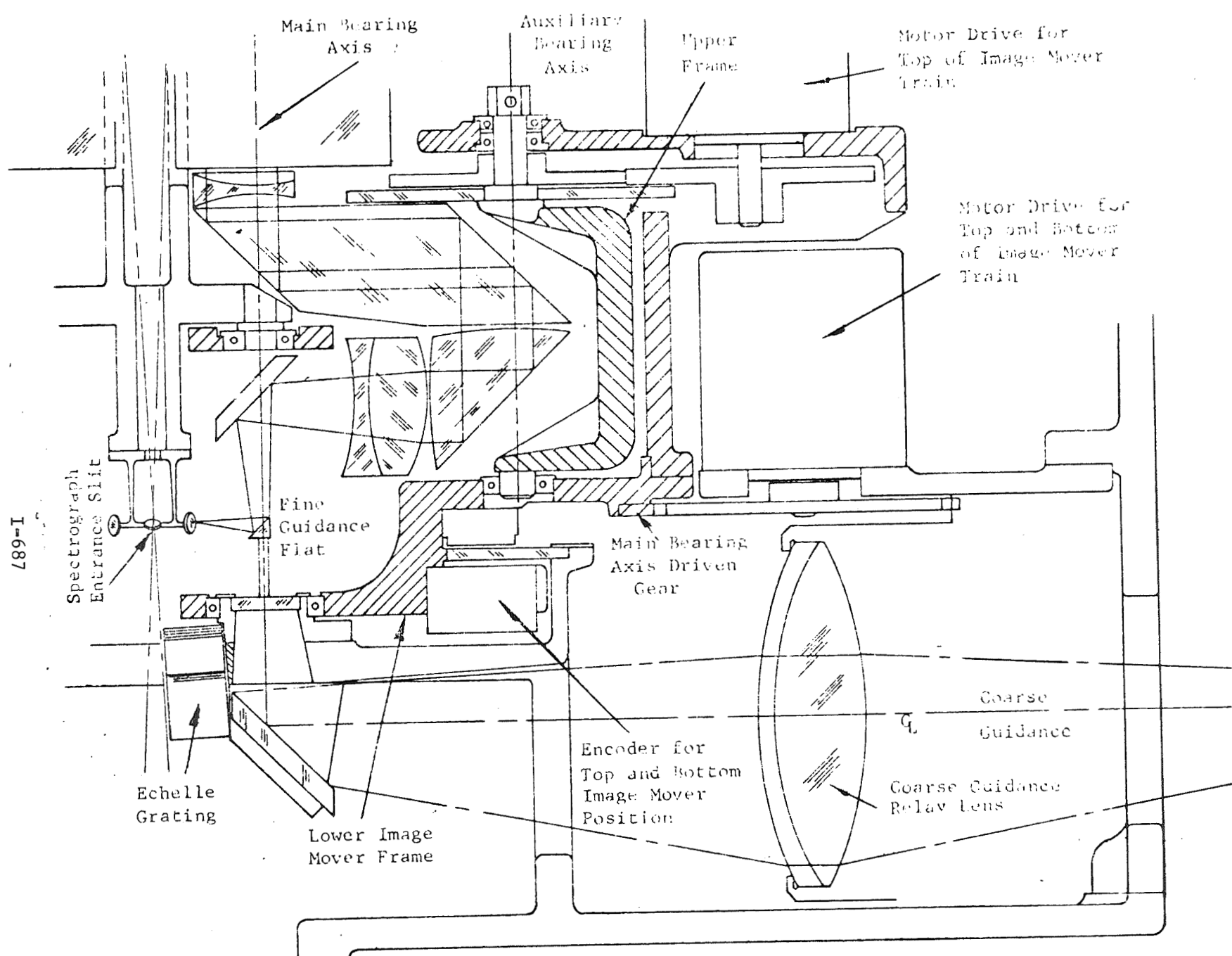


Figure 350 Image Mover Mechanical Design Layout

to compute worst-case radial gradients and changes in radial gradients. Similarly, the axial gradient during this transient period is seen to be

$$\frac{dT_A}{dt} = 0.008 e^{-t/\tau} \text{°K/hour}$$

The maximum axial and radial gradients are then,

$$\Delta T_{\text{axial}} = 0.008 \text{°K}$$

$$\Delta T_{\text{radial}} = 0.13 \text{°K}$$

and the maximum change in gradient (at initial orbit change) is,

$$\Delta(\Delta T_{\text{axial}}) = 4 \times 10^{-5} \text{°K per hour}$$

$$\Delta(\Delta T_{\text{radial}}) = 6 \times 10^{-4} \text{°K per hour}$$

Within an orbit, the gradient changes will be very small because of the thermal isolation of the image mover afforded by the primary, the OAO, and the back of APS.

Gradients caused by the image mover drive motors will be negligible. Assume that the motors dissipate 2 watts but operate for a maximum of 30 seconds during the 180-degree slewing which is maximum travel; then the approximate temperature rise in a motor will be 0.2°K which will be quickly conducted into the structure. The temperature rise in the guidance package tubes can also be computed by assuming that they are radiatively and conductively coupled to the casting. We find that a 0.5-watt heat source located as shown will raise the temperature of the structure; however, the gradients will be small and will not change because of the steady heat source supplied by the coarse guidance packages.

The gradients listed above will be used to compute tolerance for the image movers. Note that the only gradients that are significant are the changes in gradient ($\Delta \cdot \Delta T$) during an exposure.

Effects of Thermal Environment

Effect of Thermal Gradients on Positions of Image Mover Optical Elements - It is the purpose of this analysis to delineate the linear and rotational displacements of the various image mover elements resulting from constant thermal gradients. Based on these results the corresponding optical pointing degradation will be determined.

Both radial and axial gradients were considered to provide the results shown in Table 47. (See Figure 351.)

TABLE 47. TILTS AND DISPLACEMENTS RESULTING FROM AXIAL AND RADIAL GRADIENTS IN THE IMAGE MOVERS

Element	Axial Temperature Gradient		
	Δx (mm)	Δy (mm)	θ (radians)
Negative Lens & 1st Prism	1.562×10^{-6}	0.0641×10^{-6}	-0.0434×10^{-6}
2nd Prism	1.155×10^{-6}	-0.1168×10^{-6}	-0.0827×10^{-6}
Positive Lens	0.564×10^{-6}	-0.0362×10^{-6}	-0.0491×10^{-6}
1st Folding Mirror	0	0.01034×10^{-6}	0
2nd Folding Mirror	0	-0.00827×10^{-6}	0
	Radial Temperature Gradient		
	Δx (mm)	Δy (mm)	θ (radians)
Negative Lens & 1st Prism	-36.83×10^{-6}	35.5×10^{-6}	1.64×10^{-6}
2nd Prism	67.31×10^{-6}	26.2×10^{-6}	0.64×10^{-6}
Positive Lens	20.8×10^{-6}	12.83×10^{-6}	0.52×10^{-6}
1st Folding Mirror	5.97×10^{-6}	0	0.52×10^{-6}
2nd Folding Mirror	4.77×10^{-6}	0	0.47×10^{-6}

* Based on worst-case computed temperature gradients

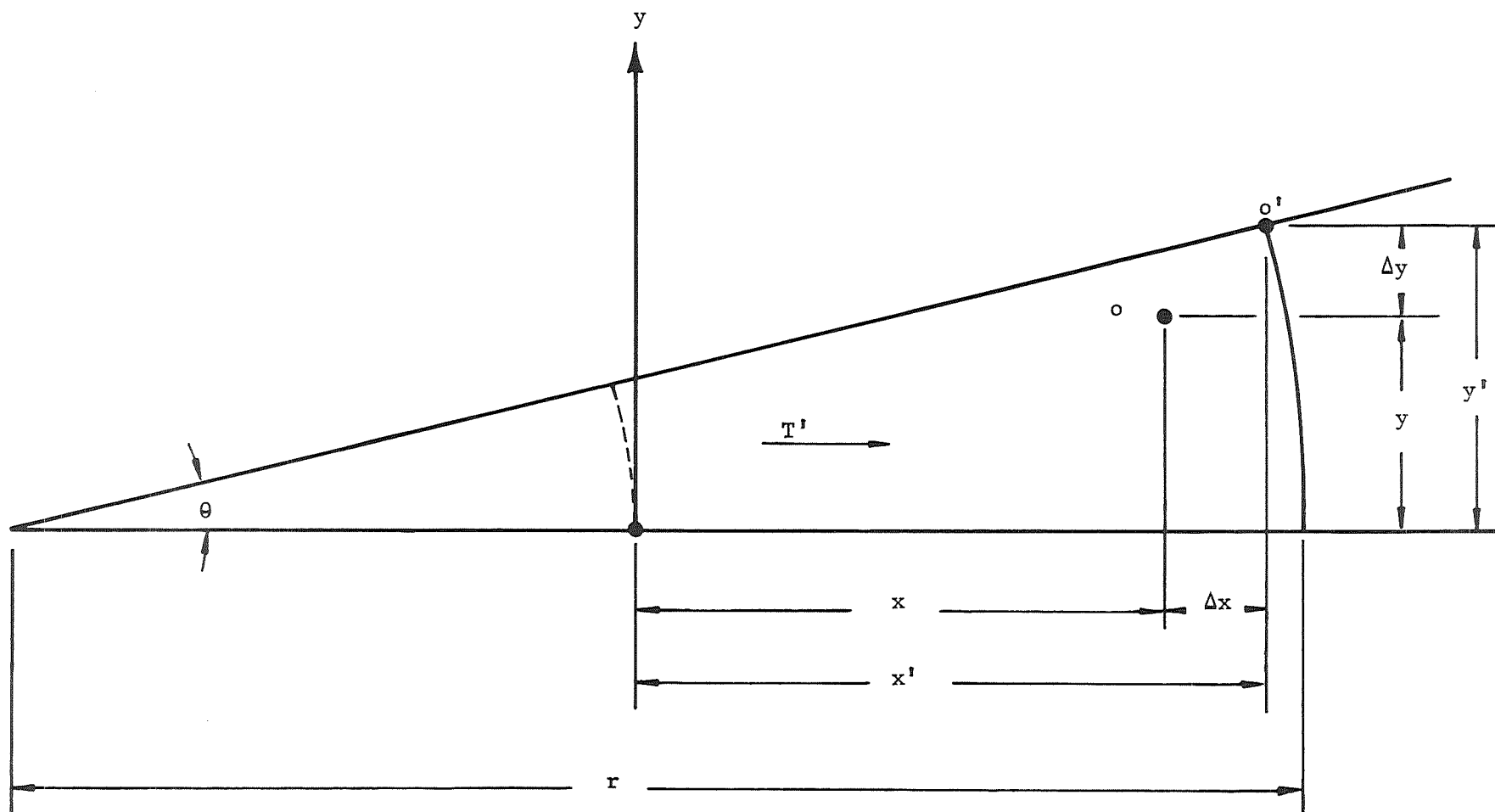


Figure 351. Displacement of an Arbitrary Point in Image Mover Element

Assume that the various optical elements are connected to a plate whose coefficient of expansion is α and that the linear and rotational displacements of each glass element are identical to those of a point on the plate corresponding to the centroid of that element. The displacement will be measured with respect to an arbitrary reference point located on the image mover rotation axis and midway between the two radial optical paths. Figure 351 illustrates this arrangement.

Radial Gradient - An unconstrained square plate subjected to a radial temperature distribution deforms as shown:

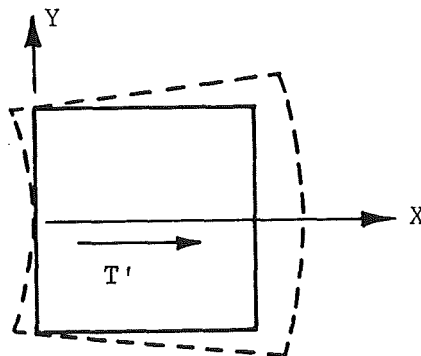


Figure 352 . Radial Temperature Deformation

It is assumed that the temperature at the origin does not change.

Taking x to be the radial direction and y to be the axial direction (positive towards the primary), the above sketch describes the case of a radial gradient, with the temperature increasing linearly with radius.

Figure 351 details the displacements of a point O to O' due to a gradient T' . It is evident that:

$$x' = x + \frac{\alpha x^2 T'}{2} - \frac{y'^2}{2r}$$

$$y' = y + \alpha xy T'$$

$$\theta = \frac{y'}{r} = \frac{y}{r - (x - \frac{\alpha x^2 T'}{2})}$$

If terms involving α^2 are neglected, it may be shown that

$$r = \frac{1}{\alpha T'}$$

Table 47 lists the results for each element for a radial gradient of $0.40^{\circ}\text{K}/\text{inch}$. This gradient was computed for an earlier thermal design and is 6 times greater than the expected gradient.

Axial Gradient - An unconstrained square plate subjected to a linear temperature distribution in the y direction deforms as shown below:

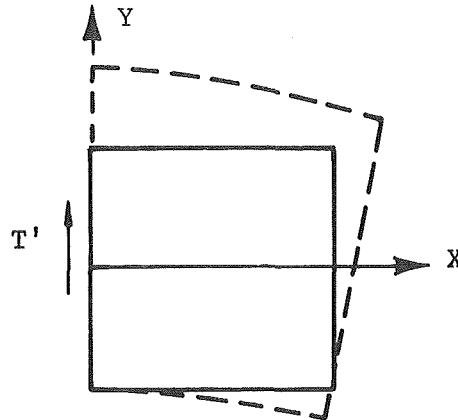


Figure 353 . Linear Temperature Deformation

Comparison with the sketch in (a) shows that, in this case:

$$\Delta x = \alpha T' (xy)$$

$$\Delta y = -\alpha T' \left(\frac{x^2 - y^2}{2} \right)$$

$$\theta = -\alpha T' (x)$$

Table 47 lists the results for each element for an axial gradient of $0.03^{\circ}\text{K}/\text{inch}$. Here again the gradient is in excess of the anticipated gradient because earlier design numbers were chosen. It should be noted that the resulting tilts and displacements are far smaller than mechanical assembly tolerances normally encountered in optical assembly operations.

Effects on Optical Performance - It is obvious from the displacements and tilts presented in Table 47 and Figure 354 that these factors are extremely small and of no significance in degrading performance of the image movers. An exaggerated example was chosen which had displacements 100 times larger than anticipated (10^{-2} mm versus 10^{-4} mm) and tilts which were 1000 times greater than expected (10^{-3} radians versus 10^{-6} radians). Figure 355a is a plot of system aberration for the F, C, and D lines. The plots in Figure 355a show a symmetrical pattern which crosses zero at the center, the plots in Figure 355b are displaced downward by 7 microns but are still symmetrical and all cross at nearly the same point. The effect of a 7-micron shift in the image at a focal length of 10 meters ($f/10$, 1-meter aperture system) is an offset error in apparent star position of 0.7×10^{-6} radians or 0.14 second which is too small to be of any significance. In actuality, the shift will probably be

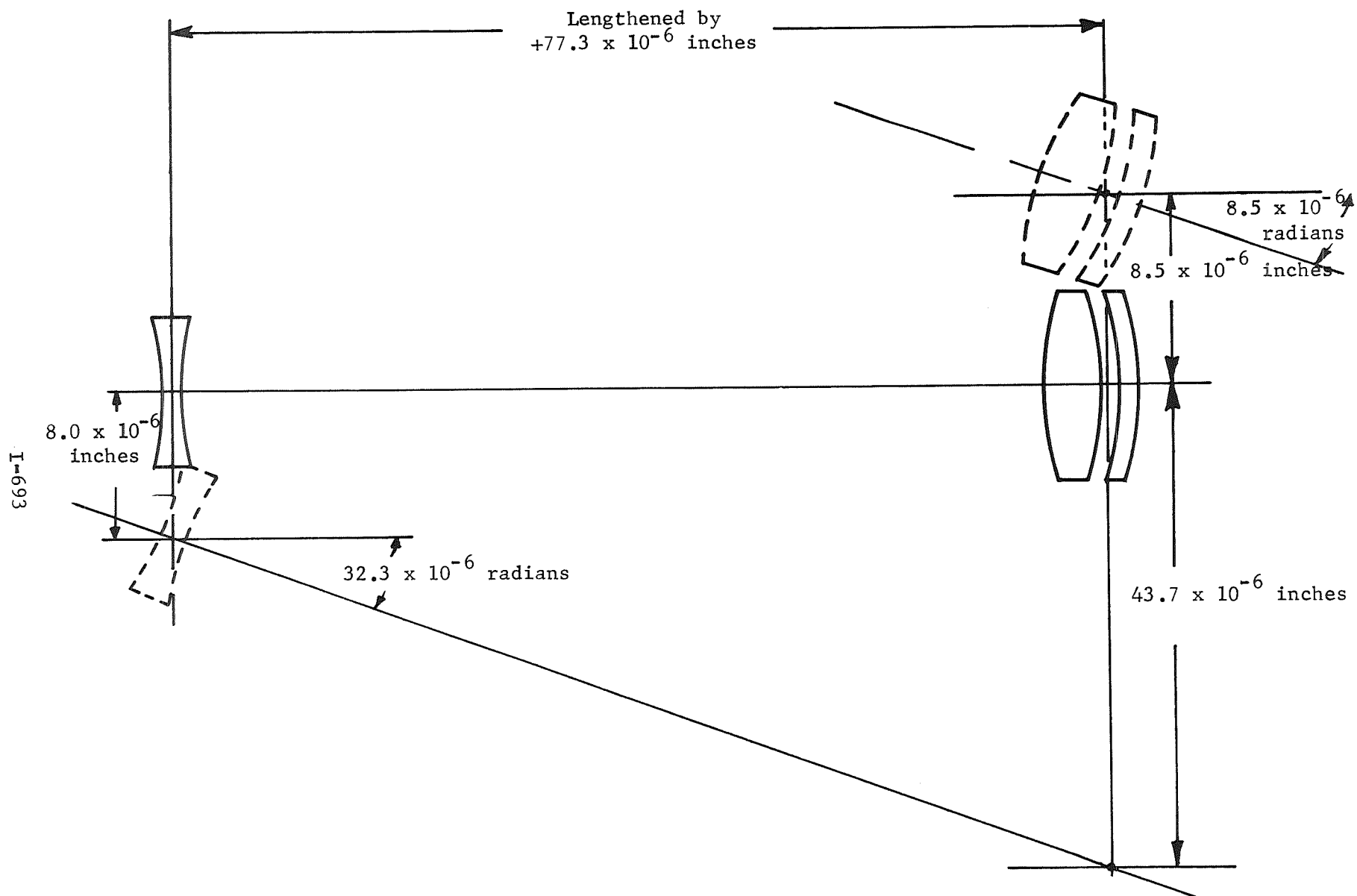
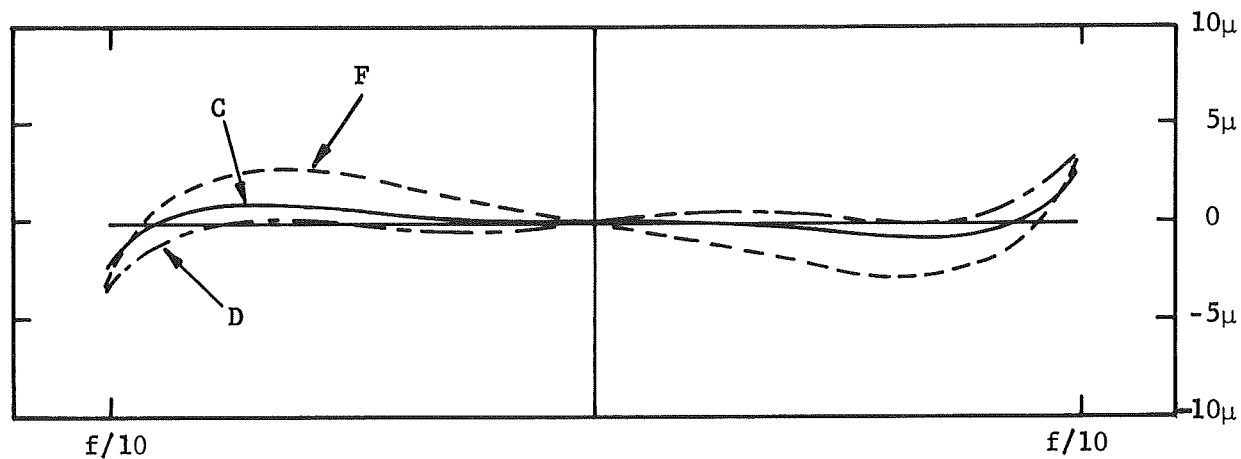


Figure 354 Worst-Case Tilts and Displacement Due to Temperature Gradients

(a) Nominal System



(b) With Gross Decentrations
& Tilts

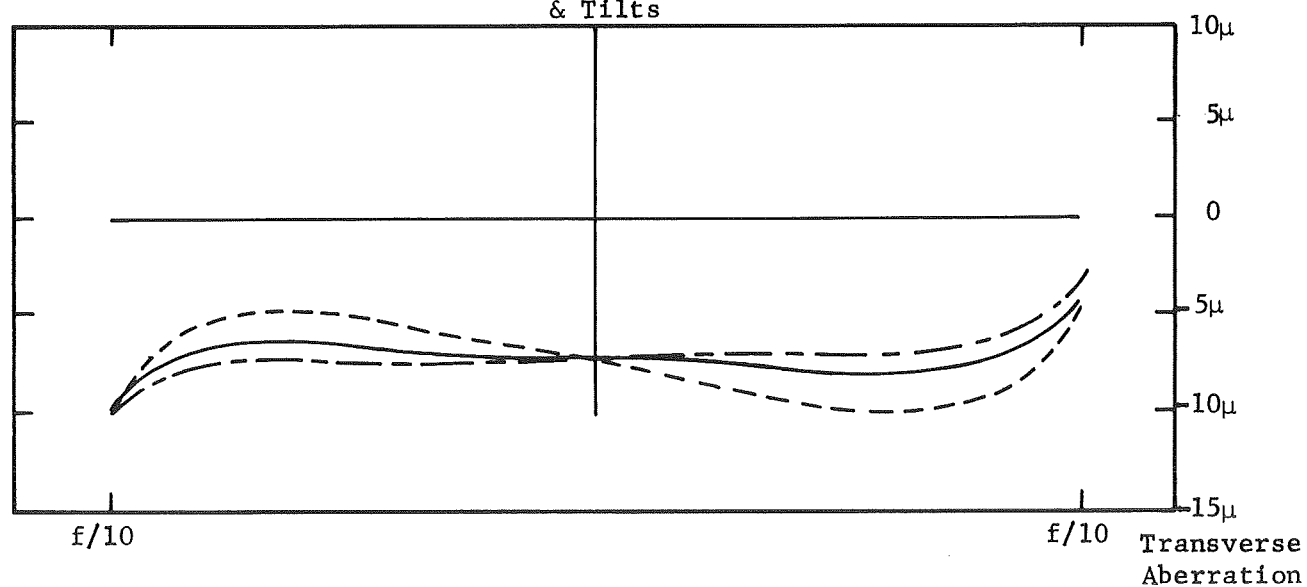


Figure 355 . Transverse Aberration for Perfect System
(a) and for System Having Gross Tilts and
Decentrations (b)

two to three orders of magnitude less than that computed, because of the gross values used. Note also, that the gradients chosen are worst-case static gradients which will change during an observation by a much smaller amount than that shown. It is therefore evident from the preceding discussion that thermal effects are negligible on the major aspects of image mover performance. However, an analysis of index changes in the glass as a function of temperature was performed that indicated some changes in focus as a result of this effect. The following is a discussion of this investigation.

Effects of Temperature Gradient on Opto-Mechanical Performance - Since the index of refraction of glass is a function of the temperature, it is possible that the imagery will be degraded by varying optical indices in the various glass elements of the image mover system. In addition, the temperature gradients will cause differences in the length through which the light rays pass in different parts of the various lenses and prisms. The problem was investigated by computing the optical path difference through the entire image mover optical system when worst-case temperature gradients are assumed to exist in the glass elements. The total effects of the length changes and the index of refraction changes were then combined and expressed in terms of optical path differences as shown in the following equation:

$$\text{OPD} = t \left[(n - 1) \alpha + \frac{dn}{dT} \right] T$$

where

OPD = optical path difference
t = the thickness of glass
n = the index of refraction of the glass
 α = the coefficient of expansion
T = the absolute temperature

The worst-case gradient was found to be 0.015°K/inch, and it was assumed to increase laterally across each lens or prism.

The optical path change (mechanical) between the lenses was of the order of 0.1 micron, about 2/3 of which was axial movement which would result in a shift of the image movers (Figure 354). The actual shift would amount to less than 0.02 arc-second, so that there would be no change in acquisition. The actual gradients would be more than a factor of 2 smaller than assumed in this calculation.

It was therefore concluded that the image movers were adequately stable from this "mechanical" point of view.

The next area is one that will be termed "Optical". Differences in temperature will yield differences in length and differences in the index of refraction. We may combine the terms from the above equation which are dependent on the glass used, in the form:

$$\Gamma = (n - 1) \alpha + \frac{dn}{dT}$$

so that:

$$OPD = t\Gamma\Delta T$$

where Γ is a characteristic of the glass used. For most glasses, Γ lies within the range from 1 to 11 times 10^{-6} , so that we may assume a value of 10^{-5} as a worst-case situation with no attempt to athermalize the elements by balancing the Γ 's. Thus, we may write:

$$OPD = t\Delta T \times 10^{-5}$$

Assume, for convenience, an axial gradient of 1°K per inch, or $0.04^\circ\text{K}/\text{mm}$. The temperature change across the first prism will be 0.8°K , and the path length through it radially will be of the order of 50 mm. Thus, the OPD for the two extreme rays will be:

$$10^{-5} \times 50 \times 0.8 = 40 \times 10^{-5} \text{ mm} = 0.4 \text{ micron}$$

In actuality the axial gradient will be more like 0.008°K and the resulting OPD will be $\lambda/150$.

This change across the 20-mm width would be equivalent to:

$$\frac{3.3 \times 10^{-3}}{20 \times 10^3} = 1.67 \times 10^{-7} \text{ radians} = 0.03 \text{ second}$$

Consider next the second prism. The path through this at right angles to the gradient is less than 20 mm, so that the angular shift is only 2/5 of the previous one, or 0.012 arc-second. The remaining element with light rays orthogonal to the gradient is the lens, with a thickness of roughly 15 mm, so that the shift would be 15/50 of the first, or 0.01 arc-second.

Thus, we have a shift in the top (near the primary) element of 0.03 arc-second and in the other arm a shift of 0.02 arc-second. These shifts will add or subtract according to the position of the image mover, so that the shift for a $0.008^\circ\text{K}/\text{inch}$ axial gradient will range from 0.01 arc-second to 0.05 arc-second. There is approximately a 200:1 reduction in these angles because of the ratio of focal length; therefore, the maximum effect is of the order of 10^{-4} arc-second for the assumed axial gradient. Because of the differences in the radial and axial path length of the rays in the glass, the effect of a radial gradient would be less than half of this. Since these effects are much less than the previous ones, it is safe to assume that no aberrations are added to the image.

At the operational temperature range of 30°K (257°K for hot orbit, 227°K for cold orbit) the gradients expected in the image mover will have a negligible effect on the optical performance. However, some compensation for the focal shifts resulting from the variation of refractive index with temperature may be necessary.

Mechanisms

The mechanical problem inherent in the image mover concept is essentially the implementation of small, precise structures and mechanisms in severely limited space in a manner compatible with the required launch and orbital environments. The problem areas of particular significance include the following:

1. The image mover assembly must be as compact as possible because of the optical design and required performance characteristics of the overall system. The available dimension along the telescope axis is restricted by the back focal length from the corrector lenses to the f/10 focal plane. Further, the requirement for a 3-arc-minute guidance field, limitations on the speed of the image mover elements, and the position of the telescope's exit pupil combine to require that the total path length through the image movers be as short as possible.

2. The nature of the system is such that enclosure of the image mover mechanisms in hermetically sealed chambers would be exceedingly cumbersome. In the absence of such enclosures, the image mover mechanisms must operate in the hard-vacuum environment. This is a severe restriction on the design of rotating or translating devices.

3. The performance requirements dictate that the image mover positions be adjustable in small increments (~ 1.0 arc-second), be known accurately (~ 1.0 arc-second), and be stable during an exposure (to ~ 1.0 arc-second). The scale factor in the correct focal plane is such that 1.0 arc-second corresponds to 0.0022 inch.

4. As indicated earlier, the thermal characteristics of the image mover mechanisms must be carefully considered. The interface between the optical elements and their supporting members must be compatible with the anticipated temperature extremes, and the entire mechanism must be insensitive to thermal perturbations of alignment or position.

Recommended Mechanisms - The recommended mechanical and optical arrangement of the image movers is indicated by Figure 350. The configuration shown represents the results of design efforts which have not as yet included detailed layout work or selection of specific components. However, an attempt has been made to recognize the key problem areas, to select practical and realistic hardware, and to indicate components compatible with the current state of the art.

As previously indicated, the function of each image mover is to relay an image from any selected point in the guidance field of the telescope to a fixed point adjacent to the on-axis image. The device shown accomplishes

this function with an optical system carried by two rotating arms. The lower arm rotates about a fixed axis parallel to that of the telescope, while the upper arm rotates about a second axis carried by the outboard end of the lower arm and also parallel to the telescope axis.

At any given position, a negative lens at the outboard end of the upper rotating arm intercepts and collimates the converging beam of light which emerges from the last corrector element and would otherwise come to a focus in the corrected focal plane. A prism folds the collimated beam along the upper arm and then down the axis connecting the two arms. A second folding prism directs the beam along the lower arm and through a positive lens where it is refocused. A third fold directs the converging bundle down the fixed axis about which the lower arm rotates. A small folding flat diverts the central 1.0 arc-minute diameter portion of the beam in a manner such that it comes to a focus at the desired point adjacent to the on-axis object star image, from which point it is relayed by the microscope to the fine guidance system. The remainder of the beam, 3.0-arc-minute outer-diameter, is split into four separate bundles by a dividing prism. These bundles are then relayed to the coarse guidance system, the details of which are not herein considered.

The folded axes of the negative and positive lenses are both coincident with the arm rotation axes, so arm rotations do not alter the optical characteristics of the image movers.

The position of each arm is indicated by a directly coupled digital shaft encoder consisting of an accurate glass code disk, a radioactive or solid-state gallium arsenide light source, and an appropriate sensor.

The upper arm is driven with respect to the lower arm and the lower arm is driven with respect to the telescope structure by geared stepping motors. In each case, the arms rotate on precision ball bearings designed for compatibility with the hard-vacuum environment. The detent characteristics of the stepping motors assure maintenance of the arm positions.

Features

- o The optical path length through an image mover's optics is constant for any position of that image mover.
- o The first and second 90-degree folds may be accomplished with a one piece prism. The use of such a prism, in conjunction with the use of a prism for the third fold, results in the optical

path between the negative and positive lenses being almost entirely through glass. This reduces the divergence of the bundles corresponding to the edges of the 3-arc-minute guidance field, and hence reduces the required diameter (and speed) of the positive lens group. A one-piece prism in the upper arm also permits the efficiency of internal reflection, simplifies the initial alignment procedures, and reduces the number of optical elements to be mounted.

- The use of a dual-rotation system permits identical or nearly identical drive mechanisms and encoders for each of the two motions.
- The field coverage of each image mover is somewhat greater than could be obtained under similar circumstances with the original rotation-translation approach.

The principle limitation on field coverage is imposed by the limited rotation of the lower arm before interference occurs with the light bundle reflected by the microscope objective mirror. (The outline of this bundle at the point where it crosses the telescope axis is indicated by dotted lines in Figure 350. Determination of the precise limiting angles of the lower arm requires detailed layout to a degree not warranted by the present status of the design, but it is evident that the field coverage will be qualitatively as shown in Figure 356. This figure assumes that, as shown in Figure 350, the distance from the negative lens axis to the upper arm rotation axis is identical to the distance between the two rotation axes on the lower arm. The shaded area is inaccessible. It is evident that the coverages of the two image movers overlap, and that coordination or interlocking is necessary to prevent interference between the two. It is also evident that, for an angular range from the lower arm of less than ± 90 degrees, a "blind" area divides the accessible portion of the field into two parts. The relative size of the inaccessible areas may be adjusted to some degree by altering the length of the upper arm. It is plain, however, that the final design of the system should include among its objectives a lower arm angular range as close as possible to ± 90 degrees.

The bearings must be of good precision and must be compatible with the hard vacuum environment. These requirements are not unique, and a number of possible solutions are available. Among the most promising, at least on the basis of preliminary evaluation, are ball bearings designed specifically for vacuum environments by several manufacturers.* These bearings are outwardly conventional with steel races and balls, but feature phenolic retainers

* e.g., "Bartemp" bearings - Barden Co.

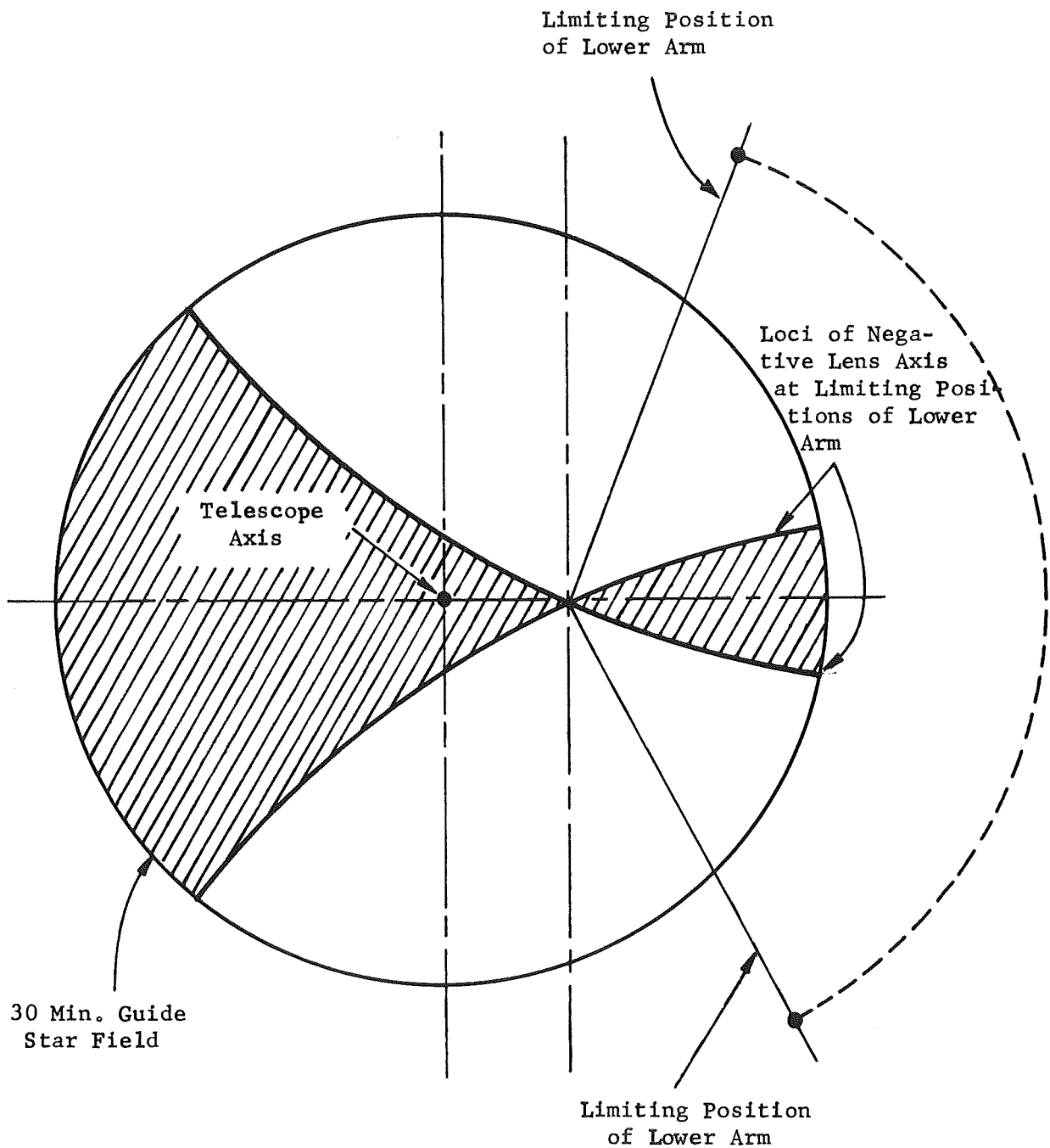


Figure 356. . Image Mover Field Coverage

impregnated with appropriate solid lubricants. In operation, vacuum welding of balls and races is prevented by transferal of microscopic quantities of solid lubricant from the retainer to the balls.

Careful additional development effort should be performed in this area since directly applicable performance data are quite limited. It does appear, however, that the bearing problem is amenable to satisfactory solution.

In order to enable telemetry of the image mover positions to the ground station, some means of encoding the angular positions of the upper and lower image mover arms is required. Proposed operating techniques require that the image mover positions be known to about 1.0 arc-second in the focal plane. In terms of the angular positions of the image mover arms, which are shown as about 2.0 inches long, 1.0 arc-second corresponds to arm rotations of 3.7 arc-minutes. On this basis, a design objective was established of implementing digital encoding devices with a resolution on the order of 2^{14} bits per revolution (1.3 arc-minutes). It was also assumed that the encoder output should be absolute as opposed to incremental. Because gear train accuracies on the order of 1.0 arc-minute in the small sizes necessary for this application are of borderline feasibility, it was further determined that every effort should be made to couple the encoding devices directly to the pivot axes.

A brief survey of the current state of the art in miniature shaft encoders indicated that high resolution is best accomplished by optical encoders. It was also evident that resolution of 2^{14} bits per revolution could be obtained with an encoding disk small enough in diameter to be incorporated in the available space, but that the physical configuration of standard, commercially available units would not permit direct coupling to the pivot axes. It is thus recommended that, as shown in Figure 350, the image movers incorporate custom-designed shaft encoders based upon precision glass encoding disks mounted directly to each rotation axis. For maximum ruggedness and reliability, these encoders should utilize either radioactive light sources or solid-state gallium arsenide light sources and silicon detectors, currently offered by several encoder manufacturers.

Note that, on the pivot for the lower arm, the encoder disk is on the fixed side of the pivot and is partially cut away, while the light source and detector rotate with the arm. This arrangement is dictated by the need to avoid obstruction of the f/10 spectrograph beam.

The selection of a means for driving the arms is governed by the need to move the image movers in increments corresponding to about 0.5 arc-second in the telescope field, i.e., a reasonably small fraction of the spectrograph entrance slit dimension. Position changes of 0.5 arc-second correspond to image mover arm rotations of approximately 2.0 arc-minutes.

A survey of available techniques showed that small stepping motors with appropriate reduction gearing provide a practical means of accomplishing such motions. These motors are available in small sizes and with angular steps of from 2 to 45 degrees. The latter value would require a gear reduction ratio of $(45 \times 60)/2.0 = 1350$ to produce 2.0-arc-minute arm motions. A major portion of this reduction might well be accomplished with a harmonic drive stage, which offers the advantage of large reduction ratios in a compact package.

It is inescapable, of course, that inaccuracies in the gear reduction train will lead to cumulative errors in excess of the encoder resolution previously described, particularly since the gears must be small and their design must include consideration of the vacuum environment. These errors are of no consequence, however, if the operating procedure is such that the image mover is instructed to "home" on specified encoder readings. Such a technique could utilize on-board electronics or ground station computing capabilities, and in either case would be quite straightforward.

An important question is that of how the positions of the two rotating arms, once set, are reliably maintained. A variety of caging or clamping techniques to be activated once the desired position is obtained were investigated. However, to utilize such an approach the driving mechanism would have to be disengaged or backed off when the clamping devices were actuated, and both actions would have to be accomplished without altering the desired position. The mechanical problems involved in implementing such devices in the limited available space are unnecessarily complex. The preferable alternative is to utilize the detent characteristics typically featured by small stepping motors. The detent may be either mechanical or magnetic, but in either case would be ample to securely lock the arms in position, particularly since there is no plausible source of consequential forces to move the arms. This approach does, of course, require that all gear-train backlash be preloaded out.

The geared stepping motor would almost certainly be a specially designed unit, since there does not appear to be a stock item which fulfills all of the necessary requirements. As with the encoders, however, an effort has been made to select performance characteristics and sizes which are compatible with the current state of the art for such devices.

The complete image mover assembly is housed in and supported by an enclosure consisting of a cylindrical housing roughly a foot in diameter and closed at its aft end by a stiffened bulkhead or plate. This plate supports the hollow pylon which carries the coarse guidance dividing prism and the lower bearing for the lower image mover arm rotation axis. The upper bearing for that axis is carried by a beam which traverses a diameter of the cylindrical housing. The cross-section of this beam would be nonuniform and designed for maximum bending and torsional rigidity consistent with the available space. A central hole through the beam permits passage of the $f/10$ object-star optical beam. Similarly, cut-outs in the walls of the cylindrical housing prevent obstruction of the microscope optical beams.

The structural housing which encloses and supports the image movers also serves as an effective transition member between the primary mirror supports at its forward end and the rest of the instrument package at its aft end. As indicated elsewhere, this approach is consistent with the thermally advantageous concept of multiple chambers, each at a different nominal temperature.

The material for the image mover arms should provide the closest possible match to the thermal coefficient of the optical elements supported by the arms. On this basis, titanium is a probable selection.

Given a reasonable thermal match between the arms and the optical elements, the mounting of those elements is considerably simplified. It is considered highly probable that adhesive bonding to small circular pads will prove to be a completely satisfactory means of mounting the various elements, as indicated by Figure 350. As with any nonadjustable assembly, of course, such a technique requires careful control of initial alignment.

Error Analysis

Consideration of the errors inherent to the image mover mechanisms herein described leads to two pertinent questions:

- 1) How accurately can an image be positioned at any given set of coordinates?
- 2) What is the geometrical stability of the mechanisms during an exposure?

For successful operation, the answer to the latter question must be less than 0.01 arc-second. In the absence of consequential dynamic forces, the principal perturbations will be thermal. As described elsewhere in this report, worst-case approximations of possible thermal effects provide a considerable degree of confidence in the stability of the image movers.

The accuracy with which the image movers can be initially positioned depends on the resolution of the encoder and upon the mechanical precision of the various moving components. As previously indicated, the desired accuracy of 1.0 arc-second in the object field corresponds to 0.0022 inch in the f/11 focal plane and to arm rotations of slightly less than 4 arc-minutes.

The glass encoder disks can be made to extremely high geometrical accuracies, on the order of an arc-second or less, so that no consequential error will be contributed by the disk itself. The accuracies with which the disks may be read are typically plus or minus one resolution element which, in the case of a 2^{14} encoder disk, would be ± 1.3 arc-minutes, corresponding to ± 0.32 arc-second in the object field.

An additional source of error is eccentricity of the encoder disks with their respective rotation axes. Assuming that the mechanical centering of the disks and the run-out of the bearings may be held to a net eccentricity of 0.0002 inch and that the outermost encoder track is at a radius of 1.0 inch, a peak angular error of about ± 0.7 arc-minute results, corresponding to about ± 0.18 arc-second in the object field.

The principal remaining sources of inaccuracy are errors in locating the lower rotation axes and errors in knowing the lengths of the two arms. Assuming that each of these parameters may be determined with ± 0.001 inch, the resulting errors in each case will be ± 0.45 arc-second in the object field.

Although a detailed study has not been made of the various ways in which the error sources considered above might vectorially add at different places in the field, it is evident that the capability of the system is compatible with the desired 1.0 arc-second positioning axis. This conclusion is substantially reinforced by the observation that all of the mechanical error sources considered above would be highly repeatable and hence their effects can be calibrated.

Implications to Thermal Design

In order to minimize thermal gradients in the image movers, it was decided to mount them in a drum-type casting of aluminum. A central section of the casting (12-inch diameter) would be designed to isolate the image movers from the remainder of the experiment package. This compartment would also be used to mount all the critical optical components such as the folding flats, the spectrograph predisperser and echelle grating, and the imaging microscopes. In this manner, all the key optical components are tied closely together in a near isothermal compartment.

It is also desirable to have a thermal design for the primary mirror cell such that the mirror is thermally coupled to a conducting backing plate which aids in minimizing transverse gradients and reducing thermal gradients at the mirror mounting points. This backing plate will tend to spread heat around at the back of the primary and thereby offer some thermal advantages of a metal mirror.

Heat-dissipating components in the experiment package are almost completely confined to a rear section of the package. The plan in the thermal design is to insulate the primary from these heat-generating components and to use the back end of the experiment package as the heat sink surface. A buffer compartment or cold chamber in back of the primary would help in this thermal isolation of the primary. The drum configuration can also be effectively used as the buffer zone or cold chamber to isolate the primary from the electronics in the instrument section.

The specific advantages of mounting the image movers in this drum configuration are listed below:

- It thermally and mechanically couples all the critical optical components.
- It can be used as a conducting metal backing plate for the primary mirror.
- It can be used as the mirror cell for mounting the primary and for mechanically mounting the key optical elements to the telescope tube.
- An effective buffer zone between the primary and instrument package electronics is also achieved.

Conclusions and Recommendations

The preceding analysis demonstrates the feasibility of the image mover concept and its application to the LEP. By controlling the thermal environment of the image movers, possible degradation in optical performance due to temperature gradients and changes in temperature gradients can be eliminated. However, it will be necessary to provide a mechanical and optical design which compensates for focal shifts caused by changes in refractive index as a function of temperature. This requirement was known and anticipated at the beginning of the study. What had to be defined and analyzed was a satisfactory mechanical design and a suitable thermal design to guarantee performance during long exposure periods. The preceding discussion and analyses satisfy these initial requirements. The next step is to build a breadboard model of the image mover and evaluate its performance.

PHOTOGRAPHIC SYSTEMS IN SPACE ENVIRONMENT

The use of a photographic data recording subsystem is included in this study for two reasons. Photographic systems offer a larger field coverage than the vidicon and afford the possibility of extending the range of the spectrometer coverage down to $800\text{\AA}$, whereas the vidicon coverage diminishes rapidly beyond $1100\text{\AA}$. The presence of an astronaut who is trained in engineering or astronomy affords in-orbit analysis and decision-making capability after the photographic material is processed and examined. Subsidiary advantages made possible by the use of photographic recording are larger field coverage, higher resolution of low-contrast objects, continued operation in the event of TV data link failure (or if power available becomes marginal), and increased knowledge of potentialities of photographic systems in larger orbiting telescopes.

These advantages will not be realized without solving certain problems, however. Foremost among these is the requirement to shield the photographic media from the effects of Van Allen Belt radiation. Not studied here but perhaps equally important are the effects of Van Allen Belt radiation on the SEC Vidicon. The effect of radiation environment on the photographic system are discussed later in this section.

Also included in this section is an exploration of the photographic systems available from the standpoints of 1) the spectral coverage desired and 2) the resolution capabilities of the optical system. Recommendations for photographic systems to be used with the spectrometer and image camera are made.

Scope of Photosensitive Recording Materials Study

In this report the consideration of permanent recording materials will be limited to Eastman Kodak silver halide systems. Silver halide systems have already enjoyed considerable success as recording media for outer space applications. A search for other systems, which might functionally replace silver halide systems and afford performance improvements, may be conducted in a future phase of this program.

No photographic medium is known which competes with silver halides for sensitivity, although many have higher resolving powers.* However, the sensitivity of silver halide systems to Van Allen Belt radiation may force a search for other recording media.

Simplified Nature of Silver Halide Photochemical Reactions**

The following is a discussion of the mechanism by which radiation exposures lead to silver deposits in the photographic process. The final step of image production in the silver halide photographic process consists of reducing silver ions to metallic silver by a developing solution via the reaction,

* Unconventional Photographic Systems. SPSE Symposium 1967. Washington, D.C.

** Mees, C.E. Kenneth: The Theory of the Photographic Process. Revised Edition, The Macmillan Company, 1954.



The metallic silver deposits form the opaque part of the recording (or negative) while the remaining constituents of the emulsion (after it has been processed) form the transparent part of the recording.

Most unprocessed photographic emulsions consist mainly of silver halide crystals, sensitizing dyes, and gelatin (which keeps the other components colloiddally suspended). At normal temperatures some of the silver ions in the silver halide crystal lattice are bounced out of their positions by molecular kinetic energy and may be regarded to behave like a "gas" of ions wandering about with thermal energies within the lattice structure.

This "gas" of ions plays an important role in the deposit of metallic silver. At low temperature extremes the molecular kinetic energies are too low to liberate silver ions from the lattice structure and the "gas" of ions is virtually nonexistent.

The primary function of light in the production of metallic silver deposits (which constitute the image) is to generate free electrons. These free electrons drift about the lattice structure until they are captured by a loose silver ion or until they find a silver deposit or a silver sulfide impurity, in which they tend to become trapped. The electric field produced by this negatively charged deposit (or impurity) attracts loose silver atoms. Under low temperature extremes there is a shortage of free silver ions and the growth of silver deposits is inhibited. Although the generation of free electrons via the photoelectric effect still proceeds under low temperatures, the electrons are repelled by the existing negatively charged silver deposits, thus making their future union with a silver ion unlikely even when the emulsion is later returned to normal temperatures.

The role of the sensitizing dyes is to absorb light quanta at wavelengths not easily absorbed by silver halides. The energy absorbed by the dyes is transferred to the silver halides by thermal means. Silver halides are photosensitive primarily to ultraviolet light; but, with an appropriate choice of sensitizing dyes, the emulsion can be made equally photosensitive in a combination of spectral regions. This is how emulsions are made panchromatic.

The production of silver deposits solely by light exposures is a very slow process because it requires at least one photon to produce at least one free electron for each silver atom deposited, and there can be light absorption inefficiencies as well as other inefficiencies along the way. For this reason, the emulsion is attacked by a reducing solution, or developer, for a controlled period of time. If the developing time is sufficiently long, all the silver halide in even an unexposed emulsion can be reduced to silver deposits.

In emulsion areas exposed by light, the larger silver deposits in exposed silver halide grains serve as more attractive traps for the free electrons liberated by the reduction process of the developer. Thus, the attraction of loose silver ions to the silver deposits and their conversion into metallic silver is accelerated. Because the emulsion transmittance depends on the fraction of its silver deposited, the reduction process (development) is intentionally stopped before most of the unexposed (by light) silver halide grains have been reduced to silver. Thus, a degree of opacity dependent on the light exposure is produced in the emulsion and unexposed emulsion areas are very transparent while exposed areas take on an opacity which increases with exposure. This explains why the adjustment of development time to exposure is a common practice in photographic processing.

The increase in graininess with development time can be explained by the non-linear increase or acceleration with time of the reduction process within a single silver halide grain. This acceleration exaggerates small differences between adjacent unexposed silver halide grains so that one may be almost fully reduced while the reduction process in the adjacent one has only begun.

The fixer arrests the reduction process, absorbs the unreduced silver halide and sensitizing dyes from the emulsion which causes transparency in unexposed areas.

Photographic Emulsion Fogging by Van Allen Belt Radiation*

A solution to the problem of radiation damage to film is reduced to one of design of adequate shielding. Since the protection needed varies with the optical performance requirements imposed on the film, radiation protection must consider vulnerability of photographic film as well as sources of radiation. In some applications, for example, the increase in graininess will impose a greater restriction on the permissible radiation exposure than will the increase in density.

The high energy trapped protons appear to present the most serious hazard with respect to photographic film. Depending on the shield thickness, protons having energies below a given value will be completely stopped while higher energy protons will be degraded in energy as they traverse the shield. To provide adequate shielding, one must not only consider the exposure dose, but also, because of the spectral sensitivity of the photographic film, the emergent proton spectrum. For example, for sufficiently low specific ionization the grains will not develop whereas for sufficiently high specific ionization all grains along the path of the particle are developable and the grain density reaches a saturation value independent of particle energy. For thick shields ($>20 \text{ gm/cm}^2$) the production of secondary particles must also be considered.

* Rossi, Bruno: High Energy Particles. Prentiss-Hall, 1952.
Mees, C.E. Kenneth: The Theory of the Photographic Process. Revised Edition, The Macmillian Company, 1954.

The most frequent result of the interaction between high energy particles and matter is the liberation of electrons. When a charged particle traverses matter, it can produce large electric and magnetic field fluctuations in the vicinity of its path. These field fluctuations can, in turn, cause considerable excitation and ionization in nearby molecules.

The electrons liberated by this interaction (between fast particles and a photographic emulsion) can find their way to silver deposit nuclei within silver halide crystals. Also, the molecular excitations produced by fast particles can tear a larger than usual supply of silver ions loose from their crystal lattice positions. With this increase in liberated electrons and loose silver ions, the size of silver deposits is increased within the silver halide crystals hit by fast particles. It is not surprising, then, that continued exposure to radiation from the Van Allen Belt can fog films.

The proportional silver halide content is of great importance in judging the sensitivity of a given emulsion to fast charged particles. This is because

- a. A relatively high number of electrons per unit volume makes silver halides more vulnerable to excitation by charged particles than other emulsion components with lower electron densities.
- b. Larger silver halide concentrations provide more silver ions to be knocked out of their crystal lattice positions by charged particles.

Larger loose silver ion concentrations in the presence of larger free electron concentrations build silver deposits. Sensitizing dyes, although more sensitive than silver halides to light in certain spectral regions, are less sensitive than silver halides to charged particles. The reciprocal of the silver halide concentration is a factor of merit by which different films can be compared for resistance to Van Allen Belt radiation fogging.

Balancing the Resolution, Exposure, and Spectral Sensitivity Requirements of Photographic Materials

According to the Rayleigh criterion for the resolution of point sources, the angular resolving power (θ) of an optical system is given by

$$\theta = 1.22 \frac{\lambda}{D} \quad (1)$$

where λ is the wavelength of light and where D is the diameter of the optical system entrance pupil. The linear separation (δ) of two star images in the focal plane (which are separated by the angle, θ , in object space) is obtained by multiplying Equation (1) by focal length (f) as follows:

$$\begin{aligned} \delta &= f\theta \\ \delta &= 1.22\lambda \frac{f}{D} = 1.22\lambda F \end{aligned} \quad (2)$$

where $F = \frac{f}{D} = \text{f-number}$

The Rayleigh criterion for the resolution of line images is similarly given by $\theta = \lambda/D$ and $\delta = \lambda F$. Thus, the approximate number of lines per millimeter resolving power at the image plane is given by

$$\frac{1}{\delta} = \frac{1}{\lambda F} \quad (3)$$

The telescope f-number is 200 and its spectral range is the 4000Å band from 2000Å to 6000Å. Future designs may permit an all-reflective microscope which would afford coverage down to 800Å. Substituting these values in Equation (3) gives a resolving power of $\frac{1}{1.22 \times 0.4 \times 10^{-3} \times 200}$, or about 10 ℓ/mm .

Because the limiting resolving power of the telescope is somewhat higher than the Rayleigh criterion resolving power, and because the emulsion should not limit the system resolution, at least 20 ℓ/mm resolving power is required in recording the telescope output--preferably even under low contrast operating conditions. Low contrast resolving powers exceeding 20 ℓ/mm should not be accepted for the telescope operational mode at the expense of light sensitivity. Although the resolving power design goal for the spectrometer calls for more than a 50-percent modulation transfer function at 20 ℓ/mm (which is sufficient to obtain a system essentially limited by the vidicon), it is hoped that a higher resolving power will eventually be achieved in order to utilize the higher resolving powers attainable with photographic emulsions.

Exposure time may be long, frequently exceeding 2000 seconds, in order to make recordings of dim celestial details at $f/200$ with the telescope, or of weak spectral lines at $f/20$ with the spectrometer. Because the sensitivity to light of most emulsions falls off quite rapidly as exposure times exceed ten seconds (Table, 48 A1), the use of emulsions specially treated against reciprocity failure at long exposures is highly desirable. Emulsion cooling is another way of arresting reciprocity failure, since low temperatures shift reciprocity failure properties toward long exposure.* This technique will not be explored at this time.

The required spectral ranges of operation are 800Å to 3000Å for the spectrometer and 2000Å to 6000Å for the telescope. The gelatin present in ordinary photographic emulsions is highly light-absorbing below about 2200Å (Table 48, A4). Two methods of sensitizing emulsions below this wavelength are:

- a) Using emulsions with practically no gelatin.
(These are called "Schumann" emulsions)
- b) Coating the emulsion with a fluorescent substance, which converts the light into longer wavelengths which expose the emulsion.

* Hiltner, W.A., ed.: Astronomical Techniques. University of Chicago Press, 1962, p. 392.

These categories of Kodak emulsions will be examined for use with the instrument package: plates and films (103a and IIa), ultraviolet coated spectroscopic plates and films (103a and IIa), and "Schumann" emulsions (SWR, DC-3, SC-5, and SC-7). Advantages and disadvantages of these emulsion categories are presented in Tables 48, 49, 50, and 51, with comments and explanations of each advantage and disadvantage given after each table.

TABLE 48
ADVANTAGES AND DISADVANTAGES OF SPECTROSCOPIC PLATES
AND FILMS (103a and IIa)

<u>ADVANTAGES</u>	<u>DISADVANTAGES</u>
A1) Relatively low reciprocity failure	A6) Relatively high sensitivity to Van Allen Belt radiation
A2) Relatively high abrasion tolerance	A7) Diminishing spectral sensitivity below 2500Å
A3) Available in quite convenient forms	A8) Bimat processing not recommended
A4) Good spectral sensitivity available between 2500Å and 7000Å	A9) Should not be hard vacuum environment for long time periods
A5) Acceptable resolving power	
A1) At present Types Ia, IIa, IIIa and 103a are the only Kodak emulsions specially designed for long exposure times. Most other emulsions have considerable reciprocity failure at exposure times above ten seconds. (See reciprocity characteristics of 103, 103a, II, and IIa, IIIa and Ia plates given on page 31d of the handbook, "Kodak Plates and Films for Science and Industry," an Eastman Kodak publication, Kodak Park, Rochester, New York, 1967. See, also, the reciprocity failure curves for various aerial films in another Kodak publication, the "Manual of Physical Properties of Kodak Aerial and Special Sensitized Materials.")	
A2) 103a, Ia, IIa, and IIIa are normal emulsions which are protected by suspension in gelatin. By comparison, "Schumann" emulsions are highly sensitive to abrasion and contamination because they have almost no gelatin. See C6. (Anti-abrasion gelatin overcoating can be applied after exposure before or after development.)	
A3) 103a and IIa are supplied on a large variety of plates and on 35mm and 70mm film. Although some of the IIa spectral sensitivity classes are not normally supplied in the film, discussion with staff members of Eastman Kodak, Rochester, New York, indicates that some might be made on special order. (Ref. "Kodak Plates and Films" handbook.)	

- A4) Spectral sensitivity curves for 103 and 103a in the handbook, "Kodak Plates and Films for Science and Industry," show that about 0.03 ergs per square centimeter are required to produce a density of 0.6 above gross fog in 103a film between 2500Å and 7000Å. Below 2200Å, gelatin is highly light absorbing. (See "UV Sensitization of Photographic Plates with Sodium Salicylate" by Rinda Allison and Jay Burns, JOSA, May, 1965, Volume 55, page 574.) Beyond 7000Å good emulsions sensitivity is not available for 103a. From page 31d of the "Kodak Plates and Films" handbook, IIA has practically the same sensitivity as 103a. But because, from page 43d of the handbook, IIA is not available in the U spectral sensitivity class, the high spectral sensitivity range of IIA is 2500Å to about 6700Å. The telescope spectral range of interest is 2000Å to 6000Å. From spectral sensitivity curves, p. 26d, Ia has practically the same sensitivity as 103 and 103a, but its high spectral sensitivity range is from 2500Å to about 6700Å. Emulsion IIIa is available in only one sensitizing class, which is sensitive from 4500Å to 5500Å. Also, according to the spectral sensitivity curve on page 26d, IIIa requires about 0.05 ergs per square cm to produce a density of 0.6 above gross fog.
- A5) The high contrast resolving power ranges are given in the "Kodak Plates and Films" handbook as 56 to 68 ℓ/mm for 103a and Ia, as 69 to 95 ℓ/mm for IIA and 136 to 225 ℓ/mm for IIIa. Low contrast resolving powers are, typically, from two to three times lower than high contrast resolving powers. (See "Manual of Physical Properties of Kodak Aerial and Special Sensitized Materials" for high and low contrast resolving powers of aerial films.) High contrast measurements were made with test object contrast (TOC) ratios of 1000:1, and low contrast measurements were made on TOC ratios of 1.6:1. Ratios given are luminance ratios of the lines to the spaces on bar type resolution targets having equal line and space widths. Low contrast resolving powers of 103a and IIA appear to be consistent with the 20 ℓ/mm requirement set for the telescope output recording medium earlier in this part. Type IIA emulsion is recommended because of its higher resolving power. This added resolution may be required when the Van Allen Belt radiation granularity increases are considered later in this chapter.
- A6) During communication with Eastman Kodak, Perkin-Elmer was verbally informed that 103a and IIA have a relatively high total silver content when compared with aerial films. This high total silver content induces greater fogging by Van Allen Belt radiation, discussed earlier.
- A7) The gelatin in the emulsion begins to absorb light just below 2500Å (See Reference in B1).

- A8) Perkin-Elmer was verbally informed by Eastman Kodak (see A6) that Bimat processing is not recommended for 103a and IIa emulsions because of their relatively high total silver content.
- A9) Prolonged exposure to vacuum of 103a and IIa causes excessive emulsion dehydration and the associated emulsion contraction and curling. Emulsion contraction causes a high fog level, but this effect can be eliminated by soaking the emulsion in water prior to processing. Curling effects can be minimized by the use of estar film base where possible, or by the use of plates instead of film. Storage of emulsions in a suitable environment when not being exposed is recommended. Ref: Eastman Kodak contacts, (see A6).

TABLE 49

ADVANTAGES AND DISADVANTAGES OF ULTRAVIOLET COATED SPECTROSCOPIC PLATES
AND FILMS (103a AND IIa)

<u>Advantages</u>	<u>Disadvantages</u>
B1) Uniform spectral response possible between 800Å and 3000Å	B7) Plates are not supplied coated. Customer must apply coatings.
B2) May be used in hard vacuum	B8) Some coatings must be removed with a special solution prior to processing. (ethylene chloride or cyclohexane)
B3) Relatively high abrasion tolerance	B9) Bimat processing not recommended
B4) Relatively low reciprocity failure	B10) Spectral sensitivity relatively low
B5) Coatings do not appreciably reduce inherent film resolving powers	B11) Resolving power relatively low
B6) Available in quite convenient forms	
B1) Ref: "UV Sensitization of Photographic Plate with Sodium Salicylate" by Rinda Allison and Jay Burns, J. Opt. Soc. Amer., May, 1965, Volume 55, page 574.	
B2) Ibid.	
B3) Ibid.	
B4) 103a and IIa, which have good reciprocity failure properties (item A1, Table 5-I), may be ultraviolet coated.	
B5) Same reference as item 1 above.	
B6) All forms of 103a and IIa can be ultraviolet coated. See item A3, Table 48 for available forms of 103a and IIa.	

- B7) Ref: "Kodak Plates and Films for Science and Industry" handbook.
- B8) Ibid.
- B9) Bimat processing is not recommended for reasons given in item A8, Table 48
- B10) Spectral sensitivity is low by comparison with SC-5 and SC-7 (item C3, Table 50). Reciprocity failure of SC-5 and SC-7 may negate this advantage at long exposure times. (item C5, Table 50).
- B11) Resolving power may be too low if Van Allen Belt radiation increases granularity too much (see page 718). Also, resolution demands of the spectrometer are higher than the resolution demands of the telescope. Resolution capabilities of coated emulsions are the same as those discussed in item A5, Table 48.

TABLE 50
ADVANTAGES AND DISADVANTAGES OF "SCHUMANN" EMULSIONS
(SWR, DC-3, SC-5, AND SC-7)

<u>Advantages</u>	<u>Disadvantages</u>
C1) Relatively low sensitivity to Van Allen Belt radiation	C4) Resolving power may be slightly lower than desired
C2) Higher tolerance to vacuum	C5) Reciprocity failure at long exposures
C3) Relatively high sensitivity in the ultraviolet region	C6) Very low abrasion tolerance. Handling is very difficult. Bimat processing not recommended
	C7) Available in very limited forms.
C1) During the communication with the Eastman Kodak research laboratories, it was found that because silver halides are highly absorbing in the ultraviolet region all ultraviolet light absorption takes place in a very thin surface emulsion layer. For this reason "Schumann" emulsions are very thin layers of silver halide with a very low total silver content. An earlier part of this section discusses the increase in sensitivity to Van Allen Belt radiation caused by a high total silver content. Staff members of Eastman Kodak supplied information about thin emulsion properties of SWR, DC-3, SC-5, and SC-7.	

C2) Because of the thinness of "Schumann" emulsions (see item C1), curling and shrinkage problems are less severe than in the case of ordinary emulsions. Soaking in water for a few seconds prior to development is still advisable in order to preclude fogging.

C3) The following data were obtained from the Kodak research laboratories:

DC-3 Type: "Medium grain size emulsions giving good definition and high contrast. Ultraviolet sensitivity is close to that of SWR film. Sensitivity to visible light is slightly higher."

SC-5 Type: "High sensitivity (about 8 times that of DC-3 film above $170\text{\AA}$). Fills a need for very high sensitivity requirements at an acceptable graininess."

SC-7 Type: "Very high sensitivity (at least 10 times that of DC-3 above $170\text{\AA}$). Average contrast. Long development."

A relative comparison of SWR film and 103-0, coated ultraviolet plates is given on page 21d of the "Kodak Plates and Films" handbook. The comparison shows SWR to be more sensitive below about $1200\text{\AA}$ and 103-0 ultraviolet plates to be more sensitive about $1200\text{\AA}$.

C4) No formally published resolving power data was found for "Schumann" emulsions; however, some pertinent data was obtained from some of Perkin-Elmer's work connected with Aerobee sounding rocket probes during the past two years. 20-micron-diameter high-density spot images were produced on SWR film in the laboratory. This suggests a high contrast resolving power of about $50\ell/\text{mm}$ for SWR. Also, 10-micron-diameter line images were produced in flight on SC-5 film from star track images. This suggests approximately $100\ell/\text{mm}$ high contrast resolving power for SC-5. GAEC measured the resolving power of SC-7 film at roughly 50 cycles per millimeter. Van Allen Belt radiation may increase granularity and reduce resolving power (see page 718). Spectrometer resolution demands are discussed earlier and exceed the resolution demands of the telescope.

C5) Kodak "Schumann" emulsions are not specially treated against reciprocity failure at long exposure times like 103a and IIa. If reciprocity failure characteristics of these "Schumann" emulsions turn out to be similar to most films (item A1, Table 48), then the sensitivity advantage of "Schumann" emulsions over ultraviolet coated 103a and IIa may disappear at long exposure times. According to Fowler*, the reciprocity failure of SWR and SC-5 should be insignificant. But according to Kodak, an indication of the reciprocity failure characteristics of SWR appears in the following table:

*Fowler, W.K., Rense, W.A. and Simmons, W.R.: Film Calibration for Rocket UV Spectrographs. Applied Optics. Vol. 4, No. 12, December 1965, p. 1597.

TABLE 51. RECIPROCITY FAILURE OF SWR

<u>Exposure Time</u>	<u>IXT (Relative)</u>	<u>Density</u>	<u>Density Decrease</u>
1/2 sec	64	0.52	0.28
64 sec	64	0.24	
1 sec	128	0.72	0.32
128 sec	128	0.40	

Reciprocity failure of SWR at 2000 seconds exposure time is likely to be much more pronounced than the failure shown in Table 51. The values in Table 51 were deduced from unpublished "Time Scale Curves on SWR Film" obtained from Eastman Kodak. The investigation of reciprocity failure curve shifts toward long exposures with temperature reductions might be a very important step in deciding whether "Schumann" emulsions should be used rather than ultraviolet coated IIA films or plates.

- C6) Low abrasion tolerance exists because of the absence of gelatin in "Schumann" emulsions. Bimat processing was discouraged by Kodak for this reason.
- C7) SWR is available only on plates and on 35mm by 25 foot 3 inch unperforated rolls. Ref: "Kodak Plates and Films" handbook, pp. 3d and 4ld. DC-3, SC-5, and SC-7 are supplied only in unperforated 35mm by 180mm film strips.

The use of these films to record the spectrometer output will require bulky holding devices in addition to those used to handle the emulsions that record the direct telescope imagery. The design of separate processing equipment may also be required.

Radiation Environment

High energy particles and the accompanying secondary emission not only fog film, but also increase granularity by exposing individual film grains in a random fashion. This increase in granularity is more severe than the increase normally associated with higher densities. A reduction in resolving power should, therefore, be expected with increased fast particle exposures. The use of IIA film is, therefore, recommended because it has a higher resolving power than 103a film.

Figure 357 gives a very crude relationship between granularity and resolving power obtained by comparing the granularity data of various Kodak aerial films. The resolving power in Figure 357 falls off at the approximate rate of 0.64 ℓ/mm per 0.001 increase in granularity at low contrast conditions, and at approximately 1.5 ℓ/mm per 0.001 increase in granularity at high contrast conditions.

As can be seen in Figure 358, apart from the increase in graininess directly due to the roentgen dose, there is an additional increase in graininess due to the increase in image gross density needed to produce the density above gross fog required for image detection.

A thorough study of the radiation environment and shielding requirements in a 300-nautical mile altitude satellite with a 30-degree orbital inclination has been conducted by the Grumman Aircraft Engineering Corporation (GAEC). Results of this study indicate that the artificial radiation belt in the region of the South Atlantic anomaly is responsible for nearly all of the radiation. This is true in spite of the fact that only about 16 minutes of the 96 minute orbital period is spent in this region, and that only about half of the orbital passes made by the satellite pass through the anomaly.

Based on calculations of the average number of silver halide grains per unit area and the average grain size, the sensitivity of some typical Eastman Kodak photographic films is expressed below in terms of the number of rads of 1MeV gamma rays to produce a density change of 0.20 above background fog.

Type SC-7	1.0 rad
Type 649-0 and 649-F	200 rad
Type I-N, M and Z	0.50 rad

The first type is an ultraviolet ($7\text{\AA}$ to $5000\text{\AA}$) film used in rocket experiments. It is fast relative to spectrographic films; the exposure index is 1.6 and has a resolution of 50 cycles/mm at high contrast. It can be used in a hard vacuum at zero degrees centigrade. The second type is a standard ultraviolet to visible spectrographic film ($2500\text{\AA}$ to $6500\text{\AA}$) which cuts off due to gelatin absorption of ultraviolet at about $2500\text{\AA}$ to $2000\text{\AA}$. It is very slow--about 600 times the first film exposure time in the ultraviolet region. It has a very high resolution of 2000 cycles/mm. The third is a standard infrared film with 75 cycles/mm resolution and is quite fast. It must not be allowed to get much above freezing temperatures or else a high fog level results. This is a very rough agreement with Figure 359 (an unpublished Kodak data sheet) which shows that a 0.5 roentgen (0.42 rad) 1-MeV gamma ray exposure raises the background fog level of type ID₂ spectroscopic film by 0.33 density units.

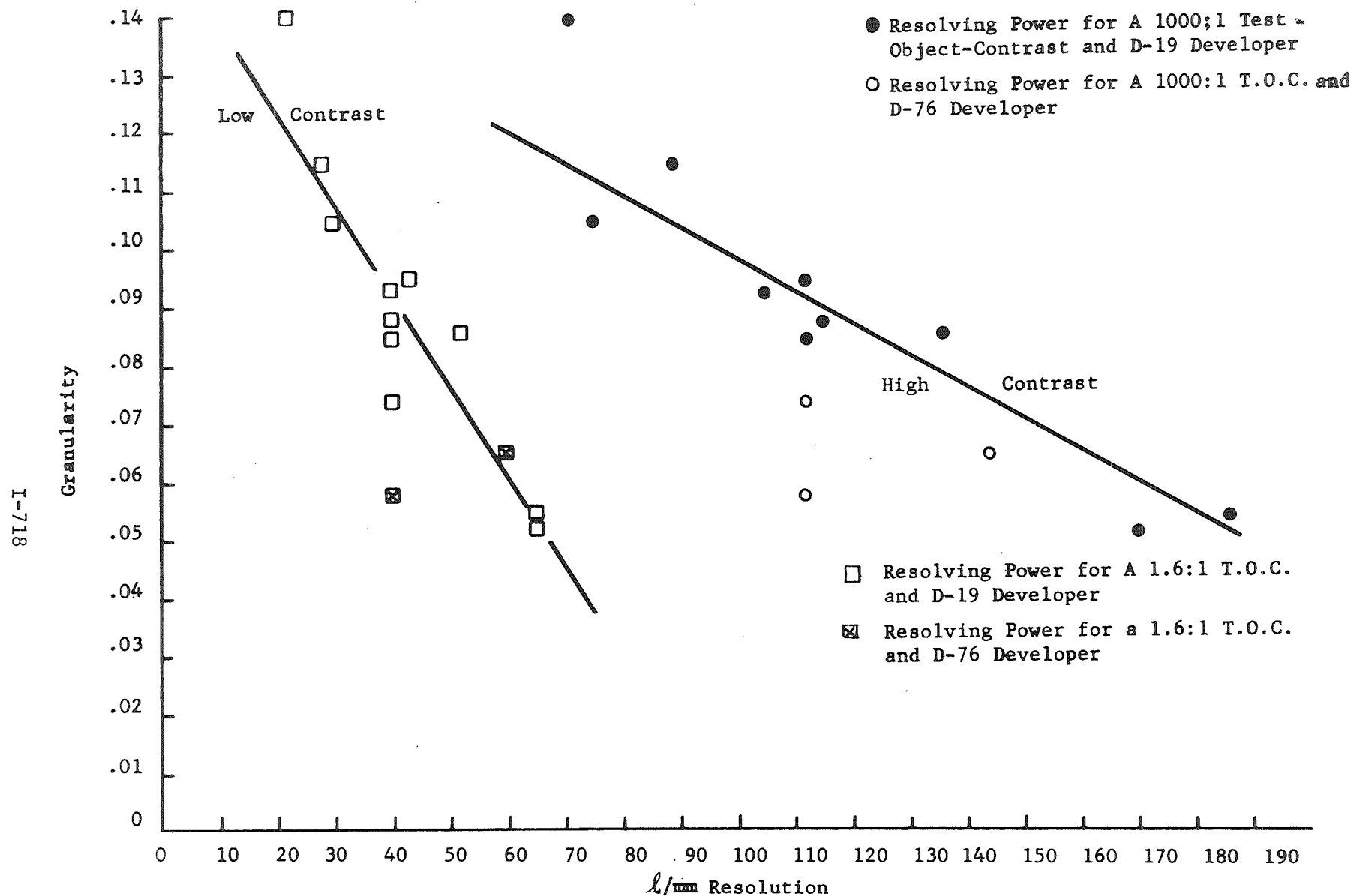


Figure 357 Granularity Vs. Resolving Power for Various Aerial Films From the "Manual of Physical Properties of Kodak Aerial and Special Sensitized Materials"

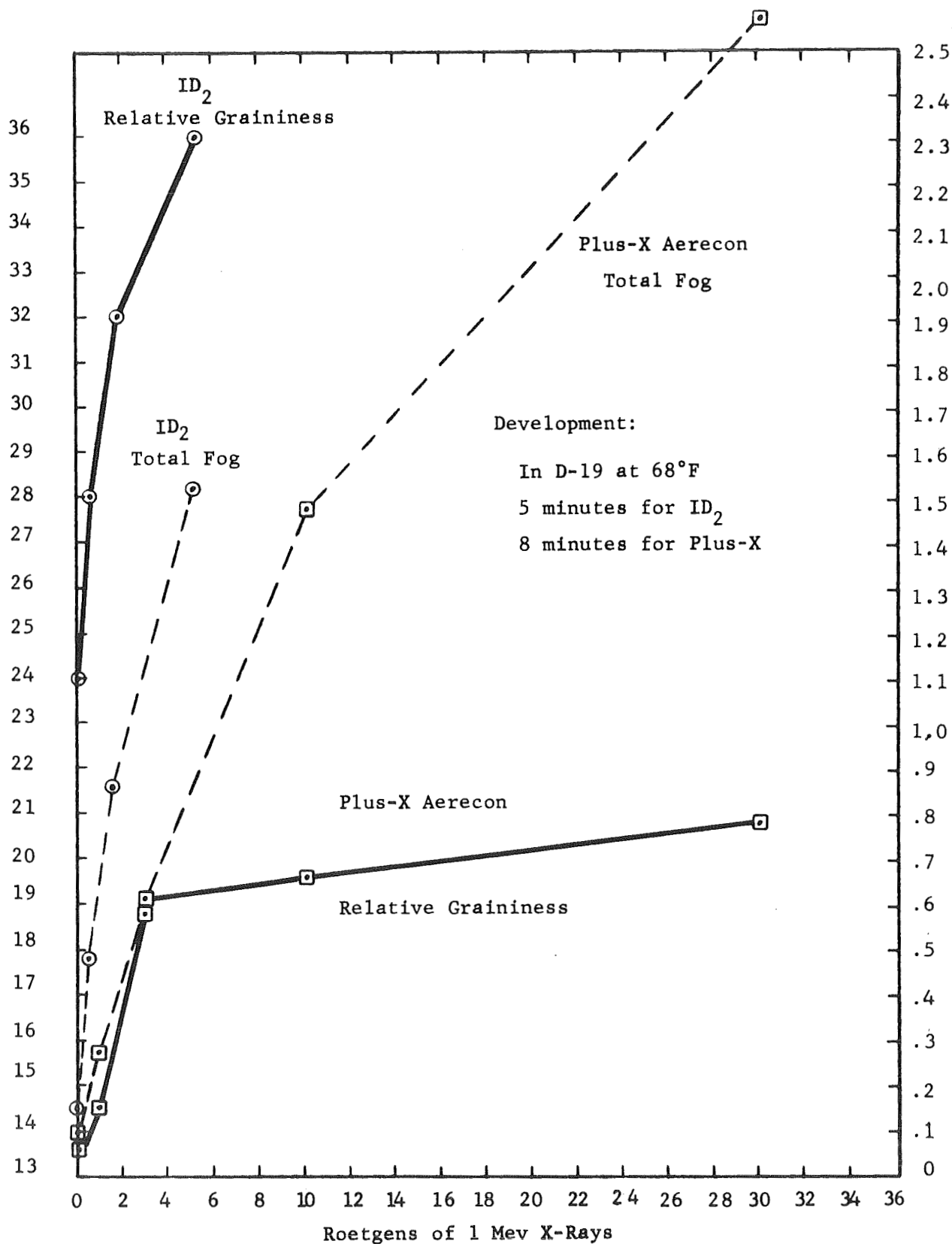


Figure 358 Relative Graininess and Total Fog as a Function of X-Ray Dose
I-719

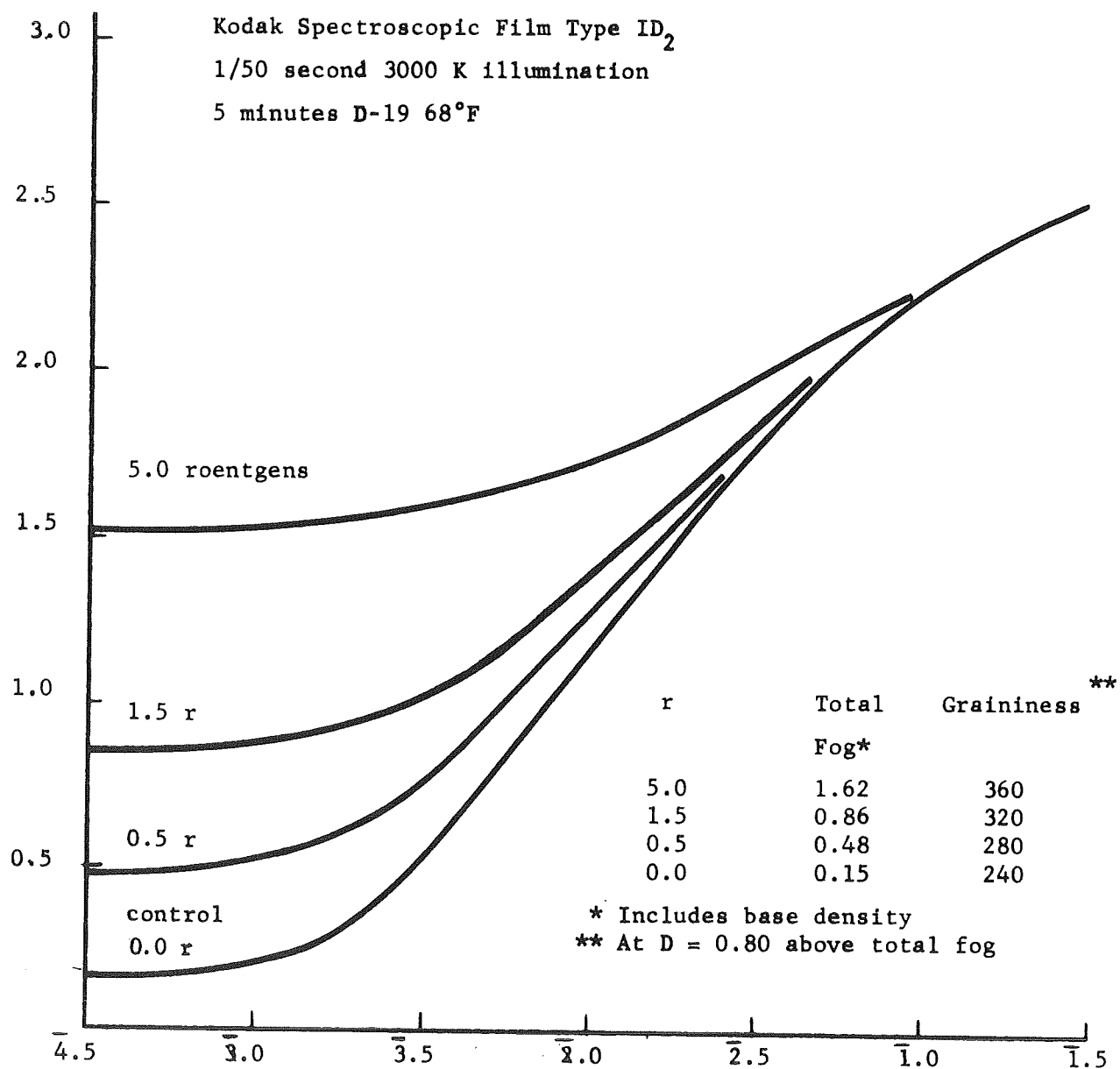


Figure 359 Log Exposure (meter-candle-seconds)

The rise in total fog with particle exposures decreases the gamma of the characteristic curve. (See Figure 357.)* This gamma reduction, in turn, decreases contrast and reduces the relative sensitivity of the film. (Relative sensitivity is the reciprocal of exposure in ergs/cm² required to produce a density of 0.6 above gross fog.)

Figure 358 is a plot (for two films) of relative graininess and total fog as a function of exposure in roentgens of 1 MeV gamma rays.* Note that, regardless of the gamma ray exposure, graininess was measured at a 0.80 density above gross fog, so that the indicated graininess increases with gamma ray exposure are independent of graininess increases due to density increases alone. The required 0.80 density was attained by the superposition of gamma ray exposure and exposures by 3000°K illumination.

According to Figure 358, the inherent film graininess is a rough measure of film sensitivity to gamma rays. The same conclusion can be reached for other films.** Type ID₂ Spectroscopic film was chosen for presentation in Figure 358 because its reaction to fast particles is likely to be greater than that of IIA-D spectroscopic film, for which suitable data was not found. (Although IIA-D is not normally available in film, discussion with staff members of Eastman Kodak, Rochester, New York, indicates that some might be made on special order). Under standard developments, the granularity of ID₂ film is about 0.14, whereas the granularity of IIA-D film is about 0.76.† As a result of the communication with Eastman Kodak, Kodak will try to obtain suitable data for 103a and IIA films.

* Corney, G.M.: Effective Characteristic Curves of Certain Gamma-Ray-Fogged Aerial Photographic Films. Unpublished, Eastman Kodak Research Library, Kodak Park, Rochester, New York.

** Corney, G.M.: Gamma-Ray Sensitivity of Several Black and White Negative Photographic Films. Photographic Science and Technique, Series II. Vol 2, No. 14, November 1955, pp. 146-148.

Corney, G.M.: Effective Characteristic Curves of Certain Gamma-Ray-Fogged Aerial Photographic Films. Unpublished, Eastman Kodak Research Library, Kodak Park, Rochester, New York.

Lewis, Jack C., and Harold V. Watts: Effects of Nuclear Radiation on the Sensitometric Properties of Reconnaissance Films. Technical Report AFAL-TR-65-113, Air Force Avionics Laboratory Research and Technology Division, Air Force Systems Command, Wright-Patterson AFB, Ohio, U.S.A., April 1965.

† Kodak Plates and Films for Science and Industry, handbook

According to the "Kodak Plates and Films" handbook, type I film is almost twice as grainy as type IIA under recommended development. Kodak published data on SC-7 film indicates that it is grainier than SC-5 and DC-3 films. Because gamma ray sensitivity of film rises with inherent granularity (see Figure 358), the gamma ray sensitivities of IIA and SC-5 should be less than type I and SC-7, respectively. For the purpose of estimating shielding requirements, a one rad radiation dose will be considered to be the maximum gamma ray tolerance level of film.

The artificial radiation belt consists of charged particles (mostly protons and electrons) "trapped" by the earth's magnetic field. Neutrons are unaffected by magnetic fields and cannot be trapped; mesons are unstable and short lived; and heavy atomic nuclei are very few in number by comparison with protons and electrons. A given number of rads of gamma ray exposure will be assumed to be equivalent to the same number of rads of proton or electron exposure.* Cobalt 60 gamma rays have been found** to have practically the same effect on film as any gamma rays having a 1 MeV energy or greater.

Figure 360, plotted from results of the Grumman study, gives the shielding requirements as a function of the number of days to acquire a 1-rad dose from protons or electrons (including secondary emission from the shielding). Figure 359 translates points from Figure 361 into the shielding weight needed to shield a cylindrical volume eight inches in diameter and four inches high. The film is assumed to remain within this volume and within the spacecraft. This assumption is valid if the film is removed only when away from the vicinity of the South Atlantic anomaly. The spacecraft is assumed to provide about 4 grams per square centimeter of aluminum shielding. This corresponds to an aluminum thickness of about 1.5 centimeters. It is possible that a place in the spacecraft exists (such as between cryogenic supply bottles) where the equivalent shielding is greater than 4 grams per square centimeter. Film storage in such a place could considerably reduce the required weight of the additional shielding needed.

The inflection in the curves of Figure 360 at about 4 grams per square centimeter of shielding marks a change in the predominant source of the accumulated radiation dose. When the shielding is less than about 4 grams per square centimeter, the radiation dose rate from electrons predominates because the number of electrons trapped in the radiation belt exceeds the number of trapped protons. When the shielding exceeds about 4 grams per square centimeter, the radiation dose rate from protons predominates because, in the Van Allen

* Lewis, Jack C., and Harold V. Watts: Effects of Nuclear Radiation on the Sensitometric Properties of Reconnaissance Films. Technical Report AFAL-TR-65-113, Air Force Avionics Laboratory Research and Technology Division, Air Force Systems Command, Wright-Patterson AFB. April 1965.

** Corney, G.M.: The Effect of Gamma-Ray Exposure on Camera Films. Photographic Science and Engineering, Vol. 4, No. 5, September-October, 1960, pp. 291-296.

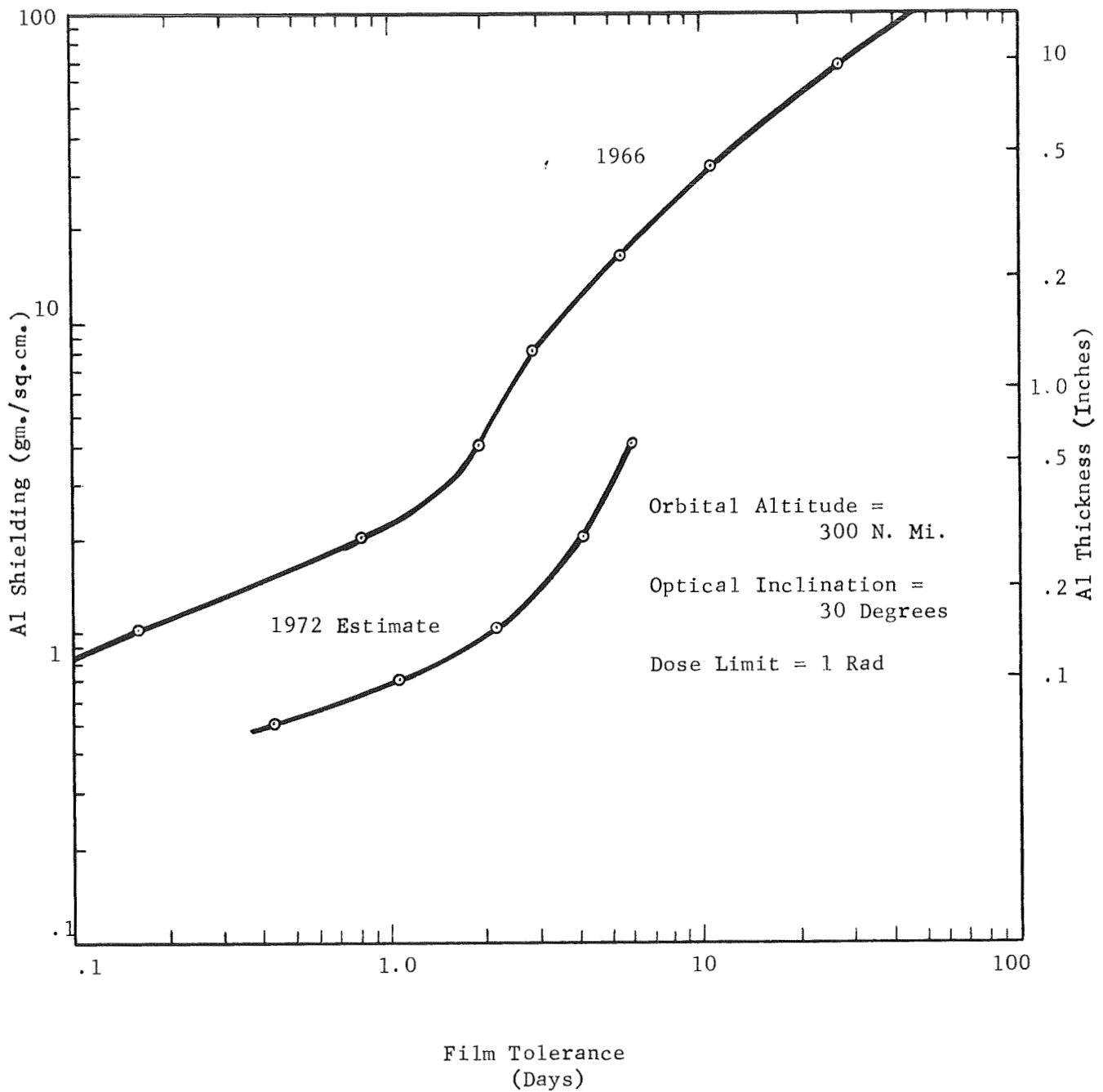


Figure 360 Shielding Requirements as a Function of Number of Days Exposure

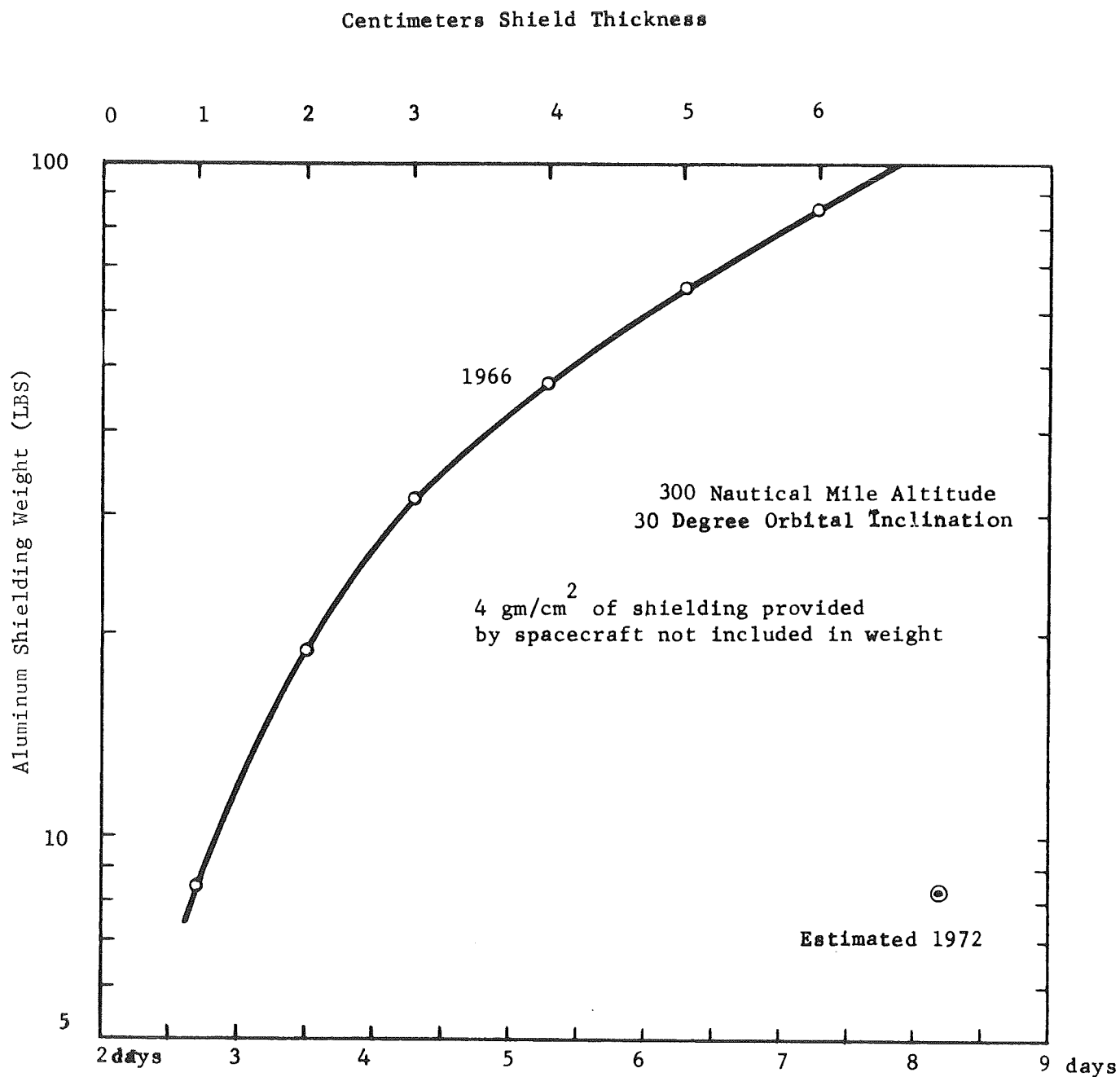


Figure 361. Shielding Weight Needed to Shield a Cylindrical Volume (8 inch Diameter by 4 inches) from Doses Exceeding one Rad as a Function of Days Exposure.

Belt, the penetrating power of protons generally exceeds that of electrons. This higher proton penetrating power exists in spite of the relatively large collision cross section of trapped protons because they generally have much higher kinetic energy than trapped electrons.

Alternate means of protection of photographic film from the effects of ionizing radiation should be studied for their practicality and feasibility. For example, it is known that the latent image produced by charged particles fades in time. Latent image fading depends on many parameters including emulsion composition, flux type and specific ionization of the exposing particles and storage conditions of atmosphere, humidity and temperature. Exposure produced by high specific ionization particles are more stable than those by low. Thus, techniques for latent image eradication must account for the differential fading between various types of charged particles. Fading rate appears to be enhanced by storage in an atmosphere containing increased amounts of oxygen or hydrogen peroxide. Perhaps the greatest influence on fading is exerted by emulsion moisture content, which in turn depends on atmospheric humidity. Other methods to minimize the formation of a latent image include low temperature storage prior to use.

In summary, the main problems of using photographic emulsions in space are the deleterious effects of Van Allen Belt radiation and reciprocity failure (low light sensitivity) at long exposure times. Both problems are indirectly aggravated by the telescope's F/200 convergence cone-imagery. Because the image light intensity is inversely proportional to the square of the f-number, the intensity at F/200 is 100 times dimmer than that at F/20. In order to record images at very low light levels, it is necessary to use fast emulsions (which are, unfortunately, most sensitive to Van Allen Belt radiation) at very long exposure times (sometimes exceeding 2000 seconds). Although the spectrometer operates at F/20, it also suffers from low light level recording problems due to high dispersion and deteriorating surface reflectivity of reflective optical elements in the far ultraviolet spectral region.

Kodak type IIa spectroscopic emulsion is recommended for recording imagery with the telescope because of its high light sensitivity at long exposure times and its adequate resolving power. Kodak 103a has slightly higher sensitivity at long exposure times but a lower resolving power. Other Kodak emulsions suffer from severe reciprocity failure at long exposure times.

The two kinds of photographic systems considered for recording the spectrometer output are special ultraviolet sensitive "Schumann" emulsions with practically no gelatin (gelatin is opaque to ultraviolet light below 2000Å), and regular IIa-0 emulsion coated with a special fluorescent material which converts ultraviolet light into light which penetrates gelatin and sensitizes the emulsion. The advantages and disadvantages of these films are summarized in Tables 49 and 50.

The primary advantages of IIa coated emulsions are that they have a fairly uniform spectral response and that they are available in the same forms as uncoated IIa. This makes it possible to use the same processing, transport,

and other handling mechanisms for both the telescope and spectrometer. If "Schumann" emulsions are used with the spectrometer, then the extra weight and bulk of additional handling mechanisms must be considered because of the limited forms of availability of "Schumann" emulsions. Also, coated IIA-0 is likely to be more light sensitive at very long exposure times than "Schumann" emulsions -- unless special techniques (such as refrigeration during exposure, which will be the natural environment of film in space anyway) are proven and used to arrest reciprocity failure in "Schumann" emulsions.

The chief advantage of "Schumann" emulsions are a higher (if the reciprocity failure problem is solved) light sensitivity coated IIA-0, especially in the far ultraviolet spectral range, and a lower sensitivity to Van Allen Belt radiation. A possible disadvantage is the necessity of refrigerating Kodak "Schumann" emulsions (i.e., SWR, DC-3, SC-5, and SC-7) during storage.

Assume the usable telescope format dimensions to be 70 by 70 millimeters, and the usable spectrometer format to be 70 by 80 millimeters. The use of 70-millimeter film for both applications appears to be an attractive possibility because film handling mechanisms are lighter and less bulky than plate handling mechanisms. Unfortunately, Kodak DC-3, SC-5, and SC-7 are supplied only in 35-millimeter by 180-millimeter unperforated film strips. Kodak SWR is supplied only on plates and in 35 millimeter by 25-foot, 3-inch unperforated film rolls. Kodak IIA film (coated and uncoated) can, however, be made available in 70-millimeter film rolls.

Bimat processing is not recommended by Kodak for either "Schumann" emulsions or IIA emulsions. The use of wet processing equipment in orbit is, therefore, indicated.

It is desirable that the system resolving power not be lowered by the recording emulsion. The resolving powers of ultraviolet coated IIA film and "Schumann" emulsions might be a bit lower than desired in this case of the spectrometer, especially because 1) Van Allen Belt radiation increases granularity, and 2) optimistic estimates of the spectrometer resolving power may be realized. The use of less grainy (and, unfortunately, less light sensitive) emulsions than IIA might be considered if the reciprocity failure problem can be solved.

Practically all the radiation dose in a 300-nautical mile altitude, 30-degree inclination orbit comes from the trapped high speed protons and electrons in the Van Allen Belt. The radiation dose is sufficiently severe to make a 30-day survival in orbit of unprocessed 103a, IIA, SWR, DC-3, SC-5, or SC-7 emulsions unfeasible. Figure 361 shows that 3 days' survival requires about 12 pounds of aluminum shielding and that 7 days' survival requires about 80 pounds of aluminum shielding. The volume shielded was assumed to be cylindrical (8-inch diameter by 4 inches). It should be pointed out that these figures are crude estimates based on a 1966 space radiation environment. On the basis of the estimated 1972 space radiation environment, only 8 pounds of shielding will be required for 8 days' survival in space. This estimated dose decrease

is partly due to an expected leakage of high speed protons and electrons from the Van Allen Belt and partly due to higher solar activity expected in 1972. High solar activity heats the atmosphere and raises the trapped radiation belt partly away from the 300-nautical mile altitude orbit, thus decreasing the dose rate.

Van Allen Belt radiation not only fogs film but also increases granularity and reduces resolving power. The latter effect is especially deleterious because of the marginal resolution of emulsions considered for use in the spectrometer.

The use of finer grain emulsions would both increase resolving power and reduce the required shielding weight for a given survival time. Associated with finer grain emulsions are reduced inherent light sensitivity and severe reciprocity failure at long exposure times by comparison with 103a and 11a.

HIGH RESOLUTION HIGH EFFICIENCY ECHELLE SPECTROGRAPH

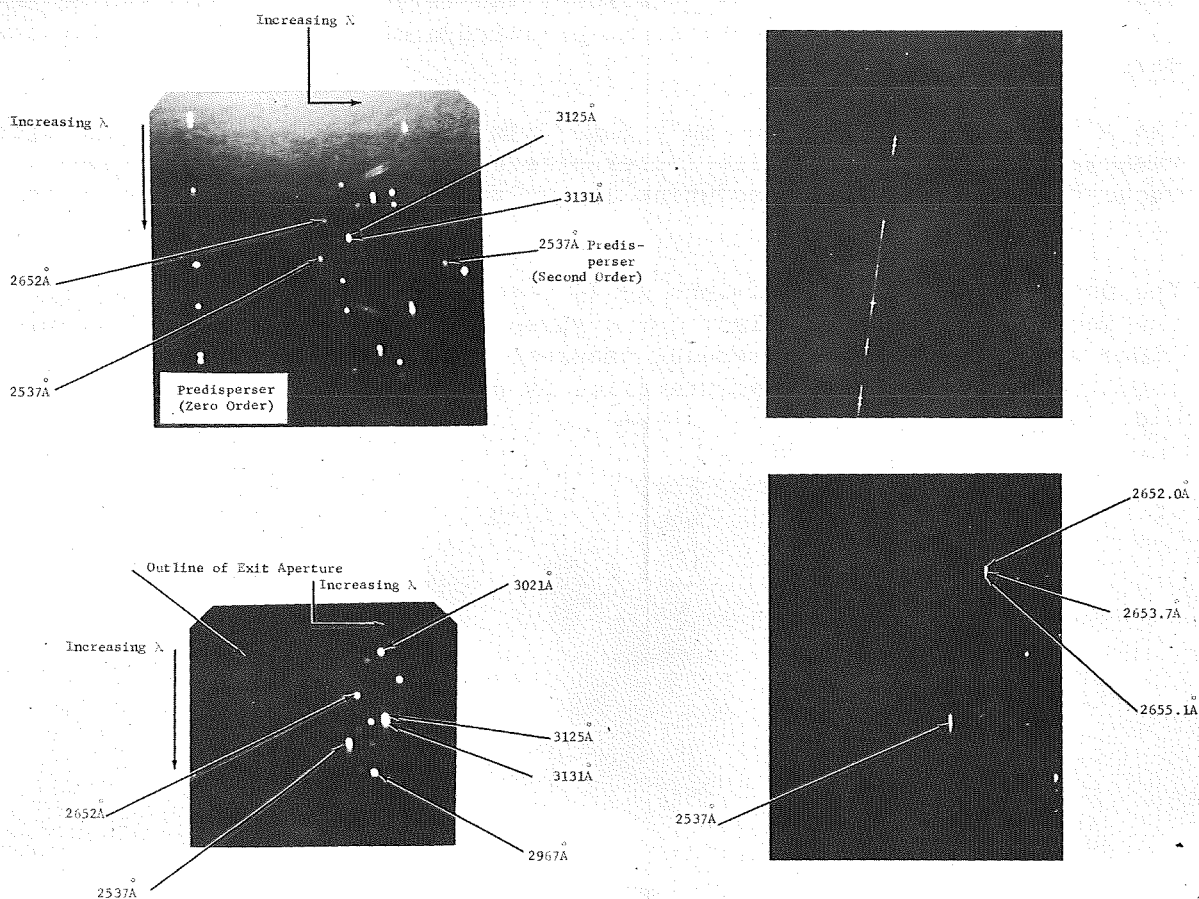


Figure 362. Echelle Spectrograms

Experiment Objective

The objective is to construct a two-element vacuum ultraviolet echelle spectrograph capable of overall optical efficiencies upward of 10 percent at 1000Å and a resolution capability of 0.1Å at this wavelength and demonstrate this capability in the laboratory. The two elements are a high-dispersion echelle grating and an order - separating and focusing pre- or post- disperser.

A two-element echelle spectrograph (Figure 363) has been built and tested for Princeton University*, and the efficiency was found to be only 2.85 percent at 1600Å. Neither efficiency nor spectral resolution was measured at 1000Å, and no attempts at measuring resolution were made at any wavelength (although

*Grosso, R. D.: Measurement of Efficiency of Two Element Echelle Spectrograph. Perkin-Elmer Report No. 9608. April 2, 1969.

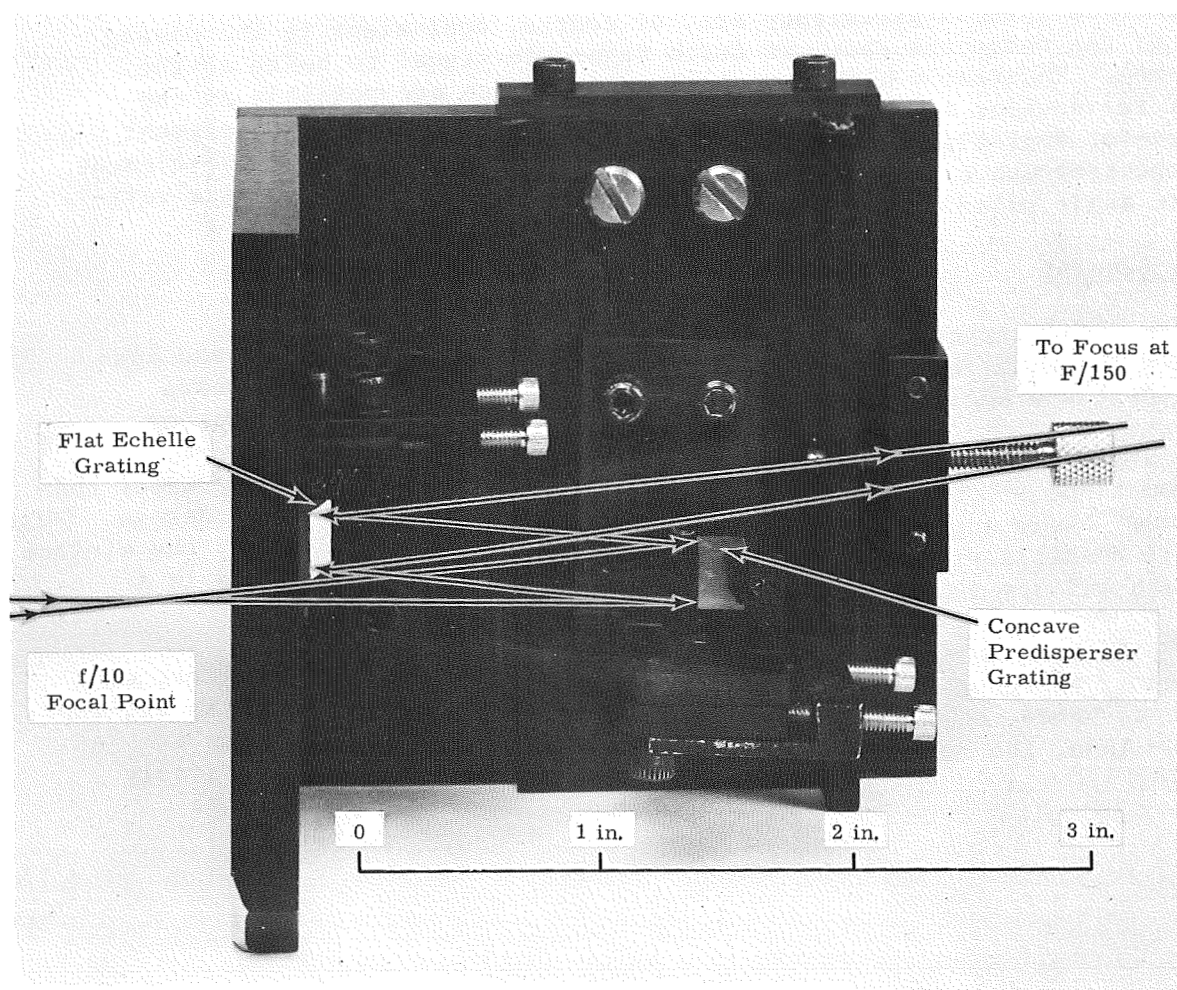


Figure 363. Two Element Echelle Spectrograph

the 2653.7Å and 2655.1Å lines of mercury were clearly resolved establishing that the desired scale was achieved and resolution capability exists. See Figure 1). A follow-on study* showed that much higher efficiencies are possible (40 percent, neglecting reflection losses) if a variable angle blaze can be ruled on the concave predisperser grating. Furthermore, the additional theoretical work shows that the low efficiency measured (see Perkin-Elmer Report No. 9608) is all that is to be expected for the particular non-variable blaze used in the concave grating. (Figure 364 shows the theoretical efficiency for an f/10 (input) focusing grating for various uniform grating blaze angles. Superimposed are the measured efficiency points.)

Experiment Justification

Improvements in optical efficiency are, of course, equivalent to increasing the size of the telescope aperture (with regard to signal to noise at the focal plane). Inherently higher optical efficiencies are possible as the number of reflections is reduced; hence, the motivation for a two-element vs a three- or four-element echelle spectrograph. The only way a two-element echelle spectrograph can work with the highest efficiency is to have a variable blaze angle ruled on the concave or focusing grating.

Technical Benefit

Figure 365 illustrates what is meant by blaze angle on a grating and also illustrates the need for a variable blaze angle. Note that on a concave grating, the blaze angle is correct only over a small part of the grating

The blaze angle on a concave grating is presently limited to angles greater than the maximum slope of the optical surface. In the case of Princeton design, (Figure 366) the "optimum" blaze angle is 0.774 degrees, but because of the above, the minimum blaze angle possible with existing ruling engines is 2 degrees. This is a problem with all low f-number diffraction gratings.

Figure 367 shows a variable blaze concave grating. Note that each facet of the grating is at the same angle relative to the incoming light rays as all the other facets; hence, the energy reflected in the diffraction direction is maximized. Thus, the optical efficiency of a focusing grating is radically increased.

*Markle, D A : Theoretical Efficiency of the Princeton Two-Element Echelle Spectrograph. Perkin-Elmer Engineering Report No. 9746. September, 1969. This and the first footnoted report were supported by NASA under Contract No. NGR-31-001-044.

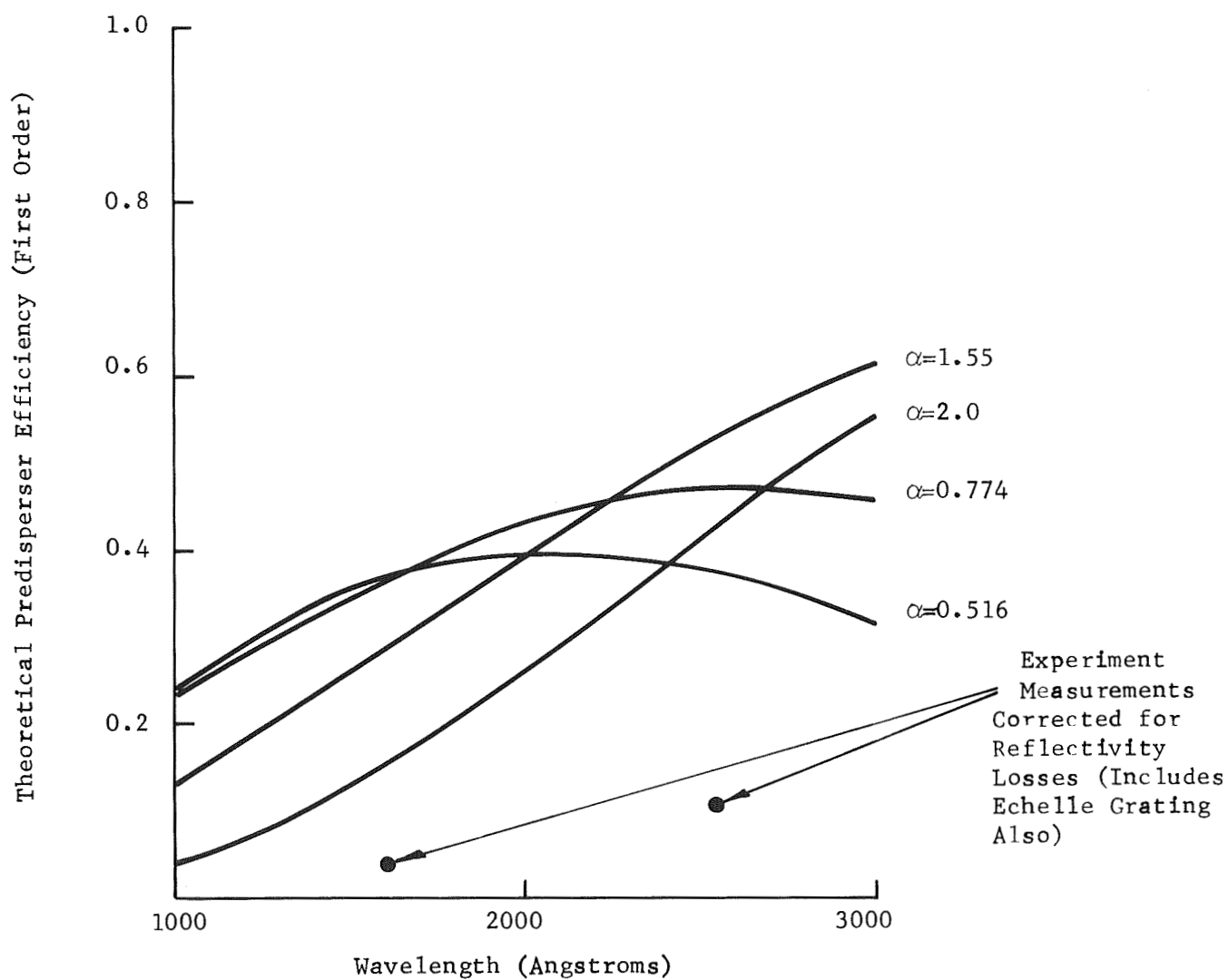
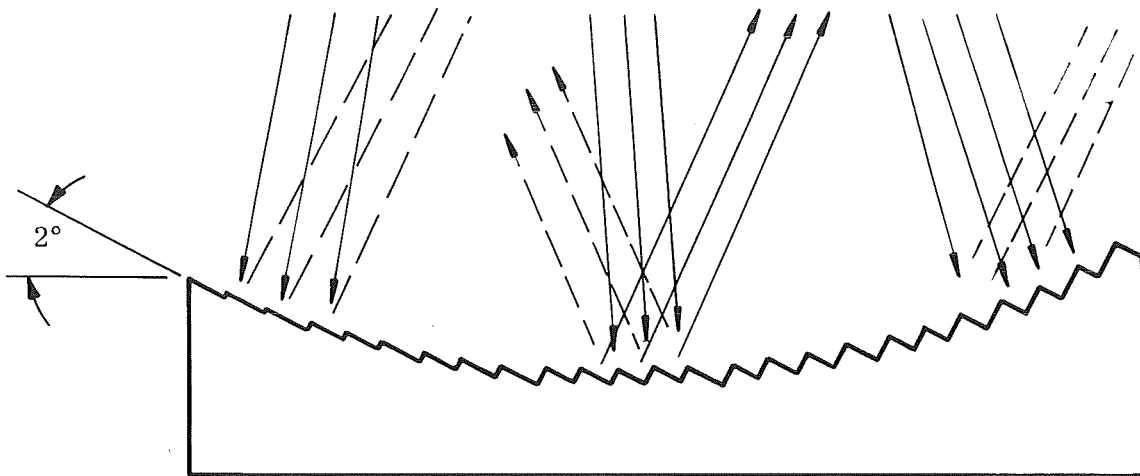
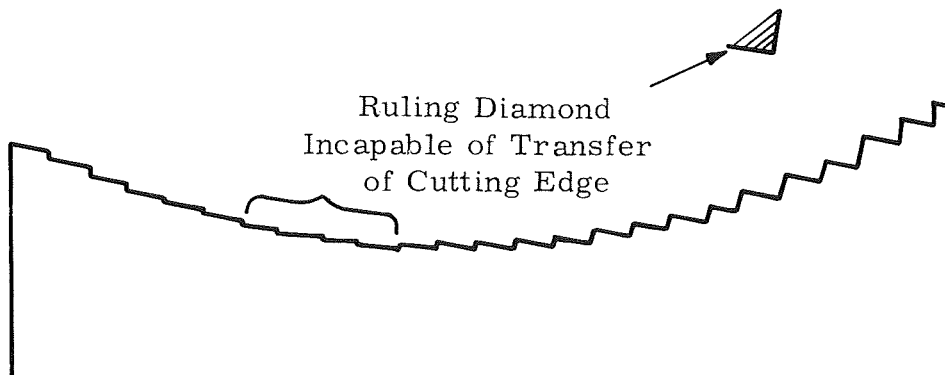


Figure 364. Effect of Blaze Angle (α) on Predisperser Grating Efficiency (f/10 System)

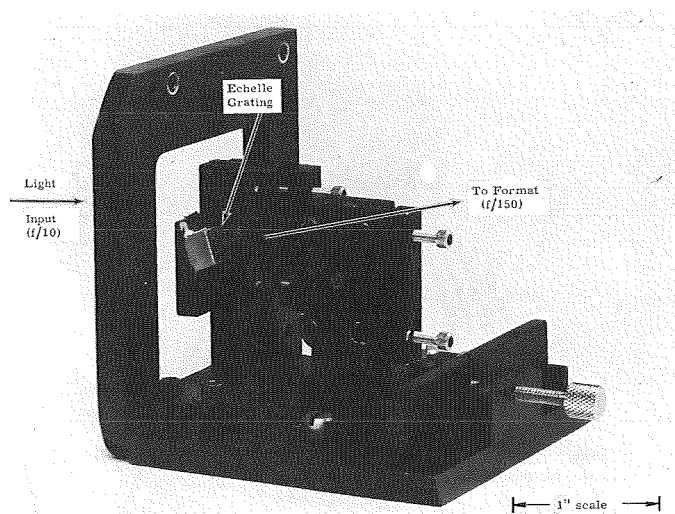


Possible Blaze
(Blaze Angle (α) More Than Slope
of Blank Everywhere)

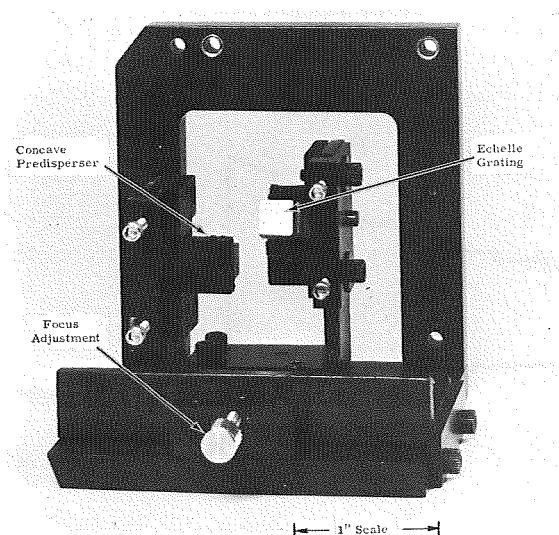


Impossible Blaze
(with Present Ruling Engine)
(Blaze Angle (α) Less Than Steepest Slope)

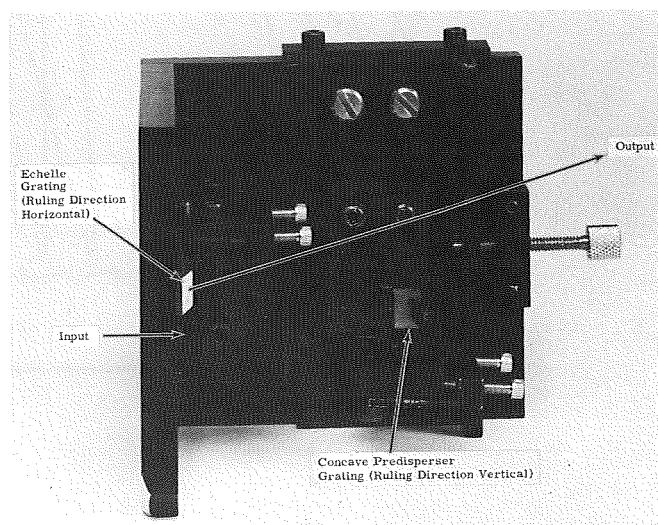
Figure 365, Illustrating Impossibility of Achieving
Design Blaze Angle



View From Format Side



View Showing Echelle Grating



Top View

Figure 366 . Breadboard Grating Assembly Two-Element Echelle Spectrograph- Princeton University

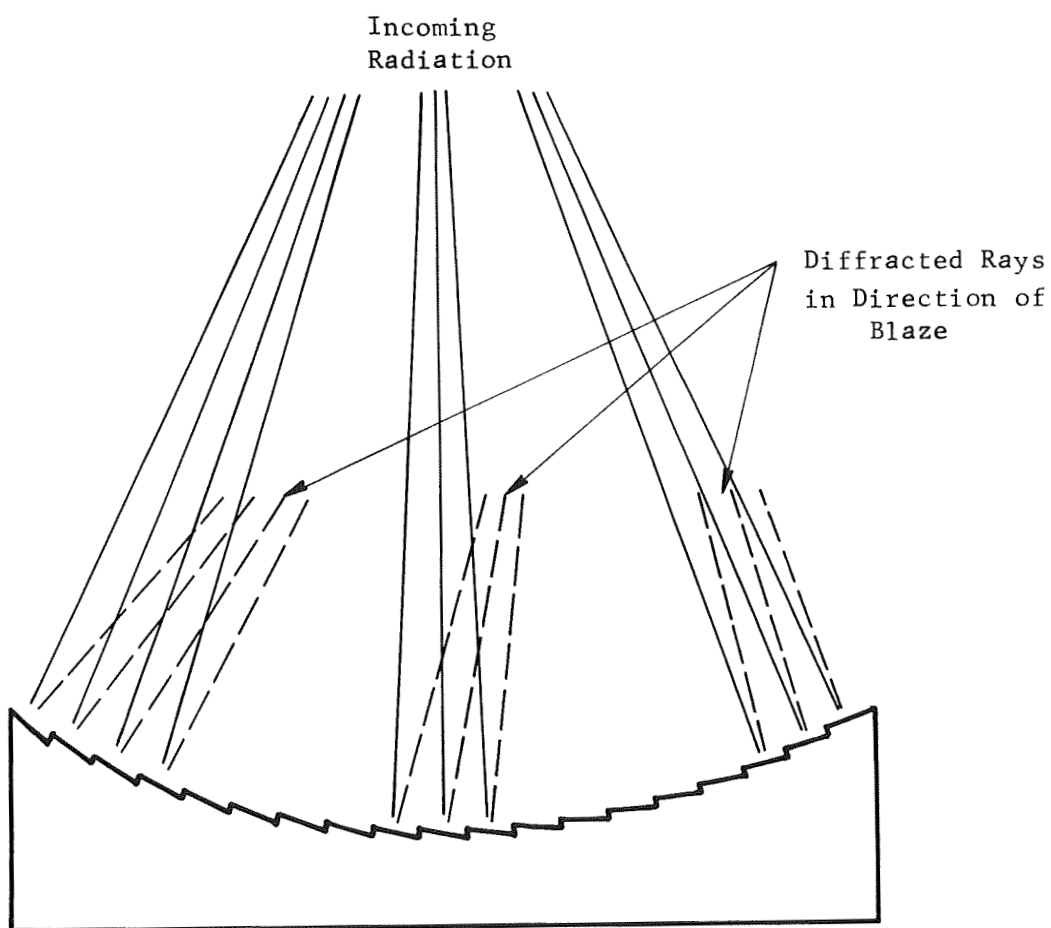


Figure 367. Variable Blaze Focusing Grating

Basic Experiment Hardware

- Ruling engine with servo-controlled ruling dismount to vary blaze angle (new development)
- Set of properly ruled diffraction gratings
- Ultraviolet source of adequate intensity at 1000⁰A
- Vacuum ultraviolet detectors (SEC vidicon and ultraviolet photomultipliers)
- Vacuum test bed (in existence, see Figure 368).

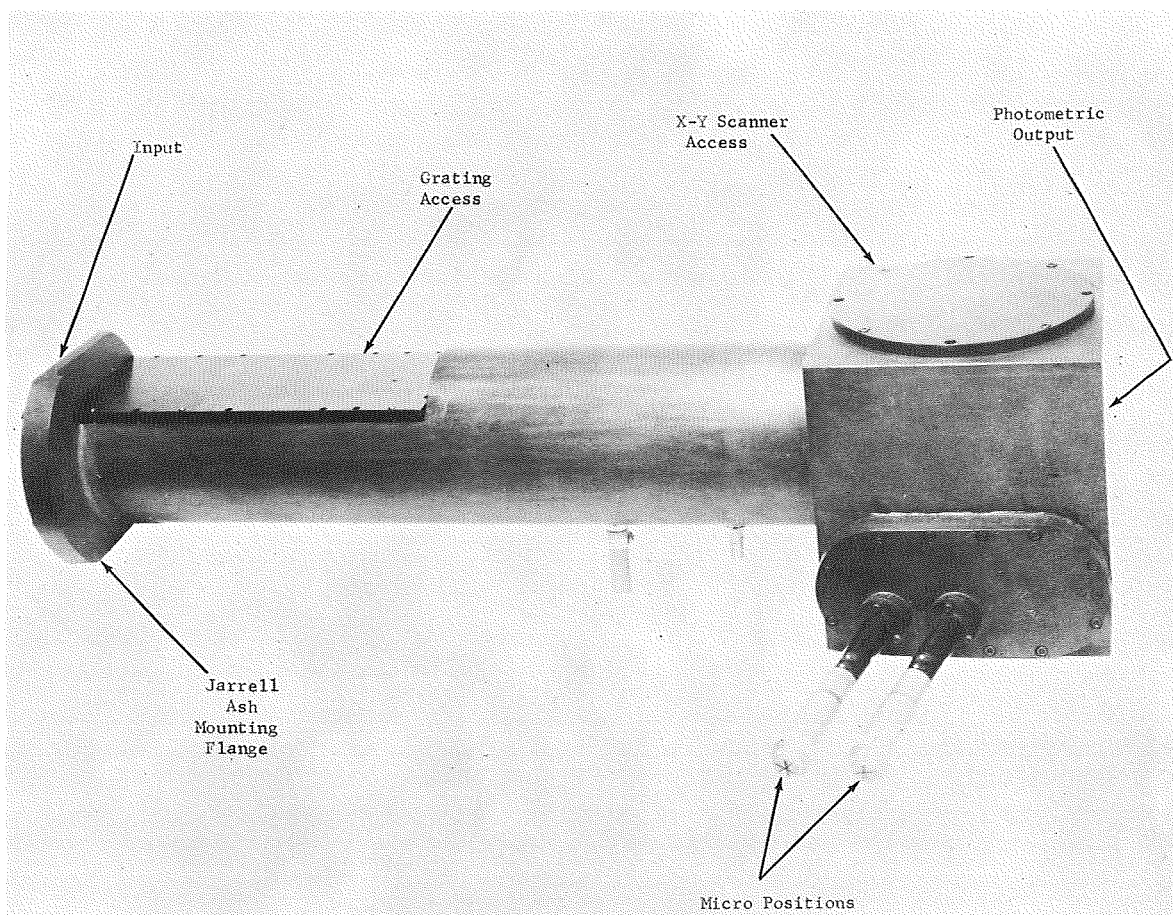


Figure 368. Experimental Breadboard System

CHAPTER 18. RESOURCES ANALYSIS

The Large Telescope Experiment Program is subdivided into four prime development areas; these are:

- Telescope Development
- Sensor and Instrument Development
- Operations Development
- Accessories for Large Space Telescope

Included under the Telescope Development Program are: the development of the primary mirror, secondary mirror, alignment techniques, pointing techniques and contamination control. The sensor and instrument development program encompasses the "behind-the-mirror" scientific instrumentation and experiments. The Operations Development Program includes the data processing research such as image enhancement, Fourier Spectroscopy data analysis and synthetic aperture research, manned operations research and unmanned operations research. In the manned operations research, human factor design criteria for EVA and IVA astronaut inputs are developed. In the unmanned operations research the astronomers' analysis modes and in-orbit program alterations leading to design criteria are developed. In the fourth and last developmental area, accessories for The Large Space Telescope, development of large span stellar interferometers and/or synthetic devices leading to super-resolution capability are developed. Figure 369 shows a time oriented PERT schedule for development in the four technology areas. The Funding Requirements for meeting this schedule are shown in Figures 370, 371, and 372. In addition, the spacecraft development and mode-particular requirements are included in these charts.

As previously indicated, either of the piggyback approaches or the independent concepts are considered feasible. Further, the basic 2-meter telescope configuration will readily adapt to these various operational approaches. Thus, the basic decision as to the optimum program implementation need not be completed prior to continued effort on the system. Phase B effort can proceed on the basic configuration with equal applicability to the ultimate mode of implementation. A programmatic selection will be required at the end of Phase B effort (i.e., late CY 1970). This is particularly true if the earliest flight date (July 1974) is selected for the telescope launch. Early initiation of development of the basic 2-meter telescope configuration with the Propulsion/Support Module is recommended.

The phase definitions and a summary tabulation of these costs is presented in Table 52.

In the paragraphs which follow, the immediate steps needed to implement the program are detailed.

TELESCOPE DEVELOPMENT PROGRAM

The telescope development program may be further subdivided into the following categories:

1. Mirrors Research
2. Telescope Pointing Development
3. Alignment Techniques
4. Contamination Control

Mirror technology is under development and the next step is to build a full scale 2-meter active optic aspheric mirror with alignment of the secondary mirror and figure sensor incorporated into the figure sensor implementation. Experiments in both segmented active primary and deformable primary mirrors will be done so that by mid 1972 a stage II deformable mirror concept will be reached. By this time an engineering model will exist in a test chamber the design of which is based on a significant rocket launch and space operation parameters so that final design could commence.

In parallel with development of the active optic principle, material research will be conducted having two objectives in mind. The first concerns material stability and other mechanical parameters. The second objective is research into maximizing the reflectance of the coatings on the primary mirror. Full scale mirrors will be subjected to vibration and thermal vacuum testing in a configuration sufficiently like that of the active optic primary mirror to prove definitively which mirror material should be used in the LTP technology telescope. Experiments with electrostatic shielding in coating overcoats will be conducted to determine whether or not the susceptibility to contamination can be reduced. The possibility of in-orbit refurbishment of the coatings will also be further investigated.

In the pointing development program the vacuum Free Float demonstration outlined in the nonspace experiments will be carried through. Photographs of the facility that already exists for carrying out these experiments will be found in that section.

The physical facilities or buildings for accomplishing the telescope development program are described in the final part of this section. A significant optical resource not currently in existence is a diffraction-limited test flat of sufficient size to test a two- (or five) meter telescope. Similarly, test spheres of adequate size to test secondary mirrors must be fabricated. A study should be made to determine the best means for testing the space telescope; then the test metrology equipment must be manufactured. Manufacturing such large test optics to the precision dictated by a large space telescope will require advances in optical technology.

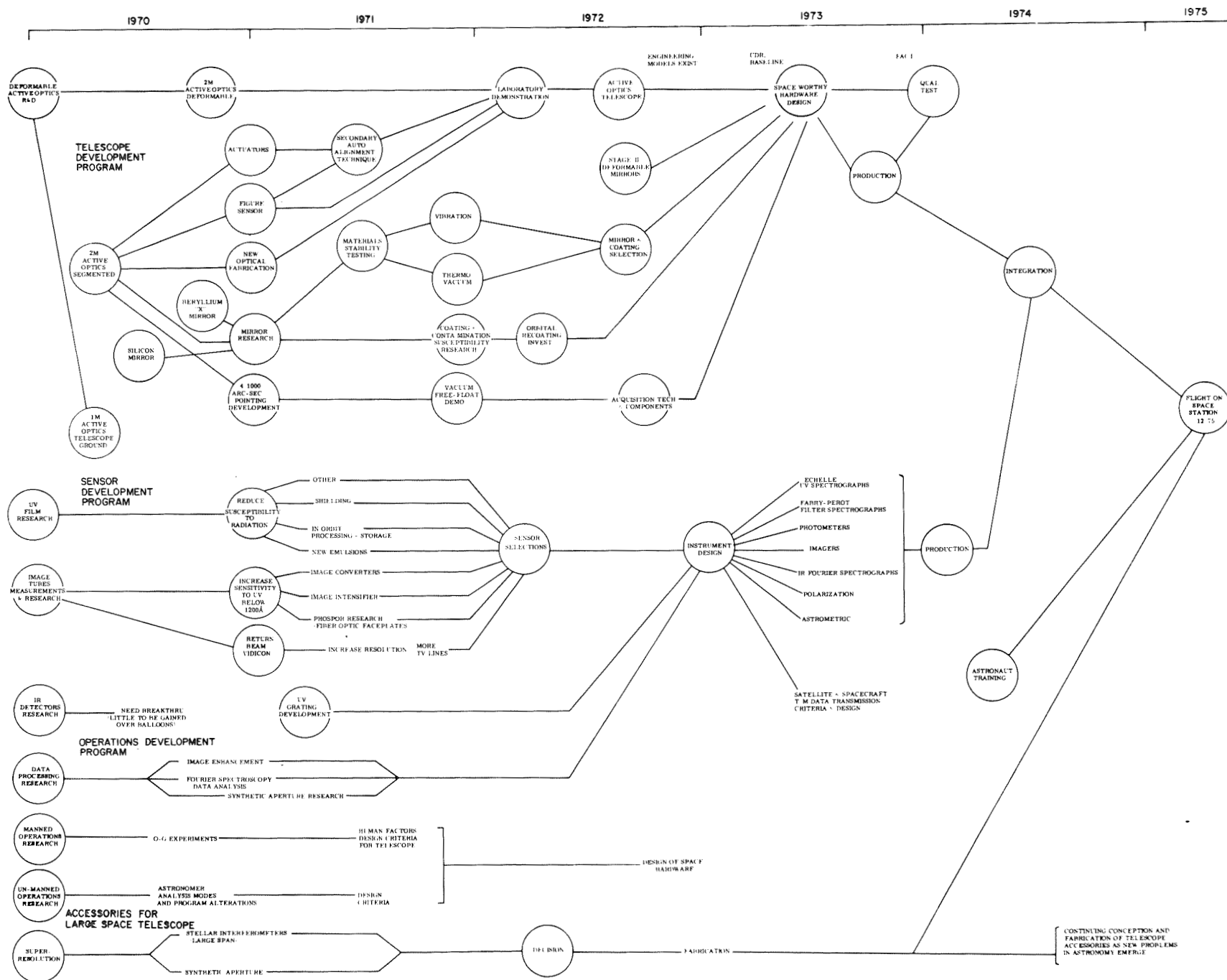


Figure 369. PERT Schedule

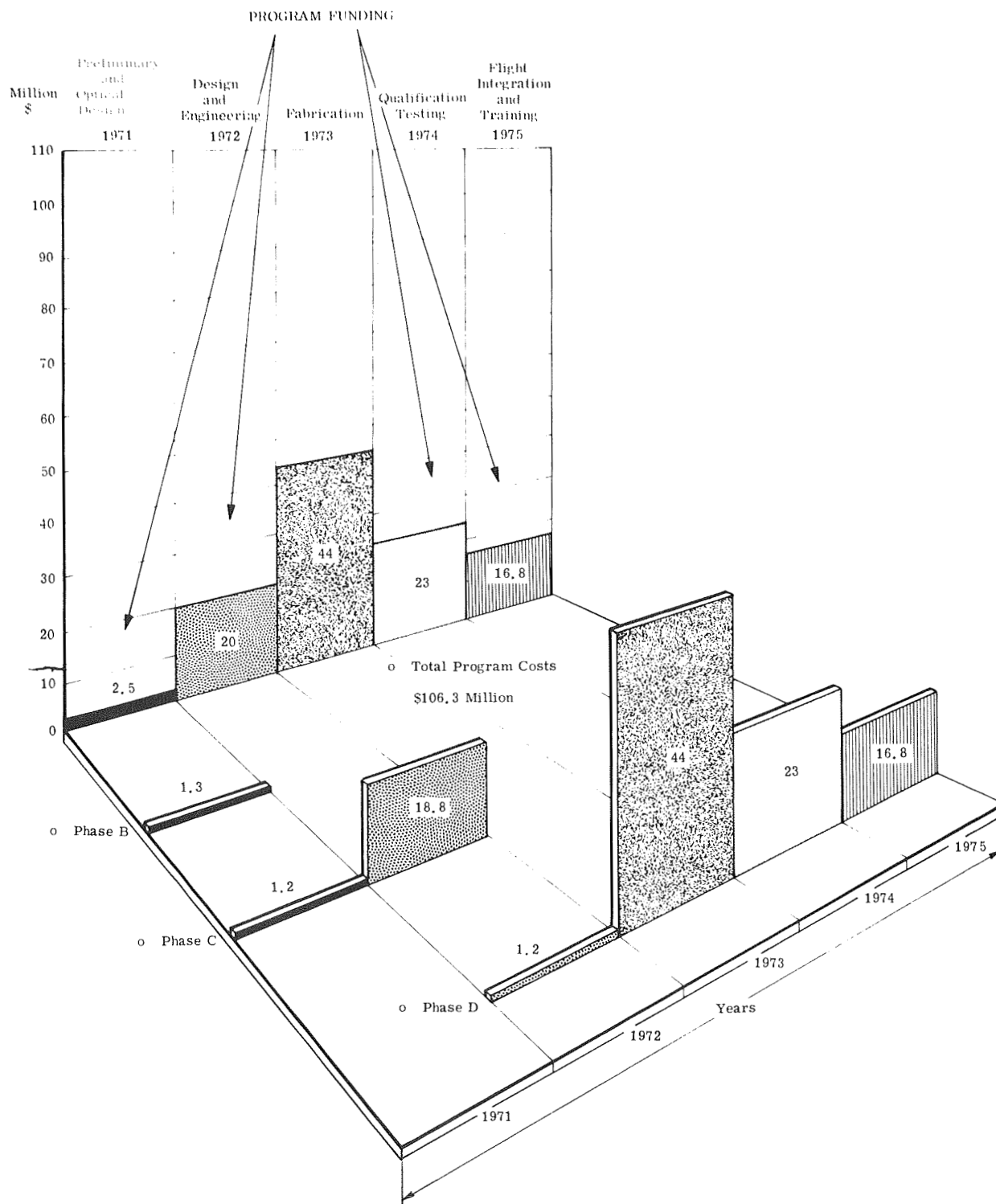


Figure 370. Funding Requirements for Titan IIIC - Unmanned LTEP and Rendezvous LTEP

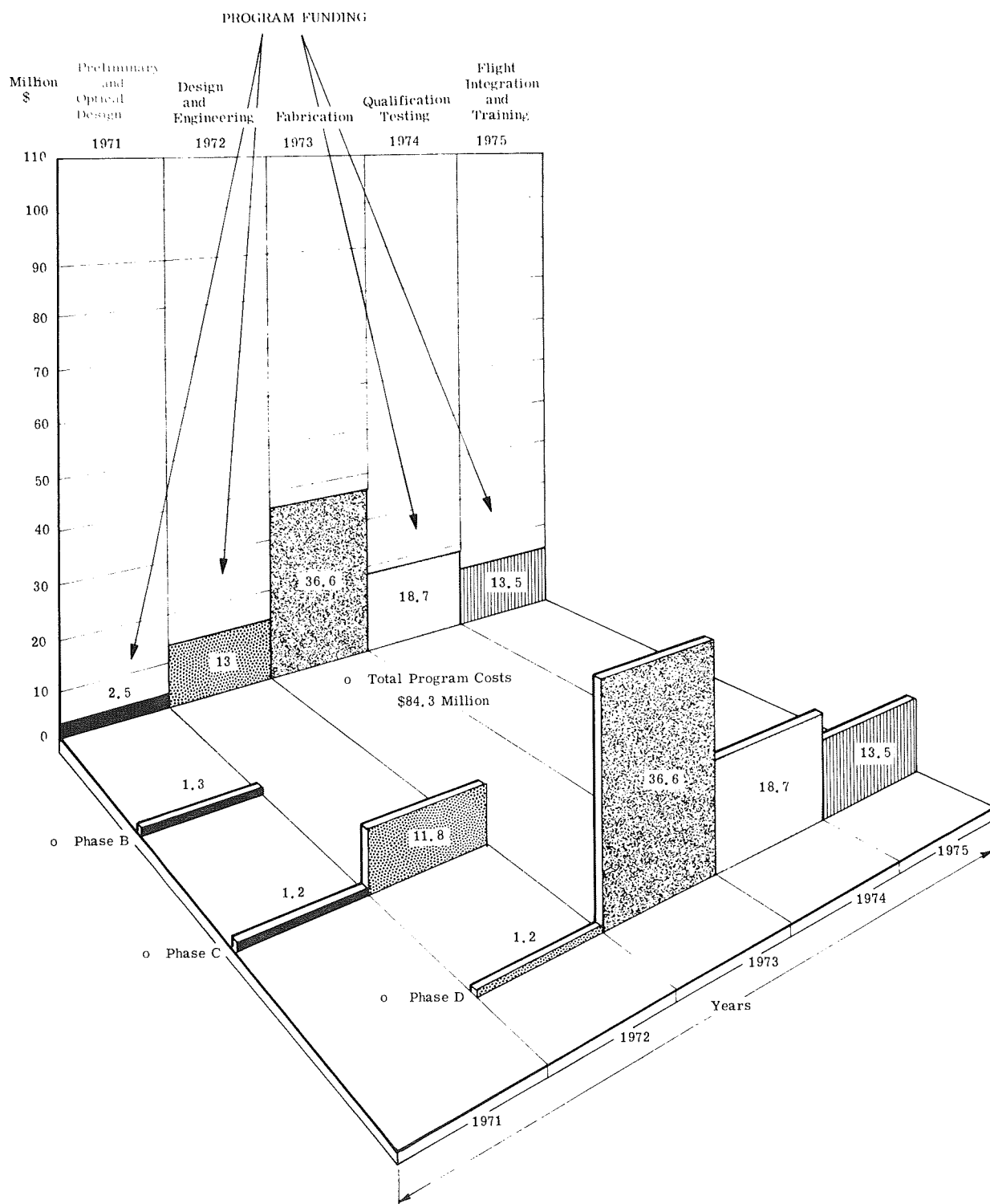


Figure 371. Funding Requirements for Sky Lab-LTEP

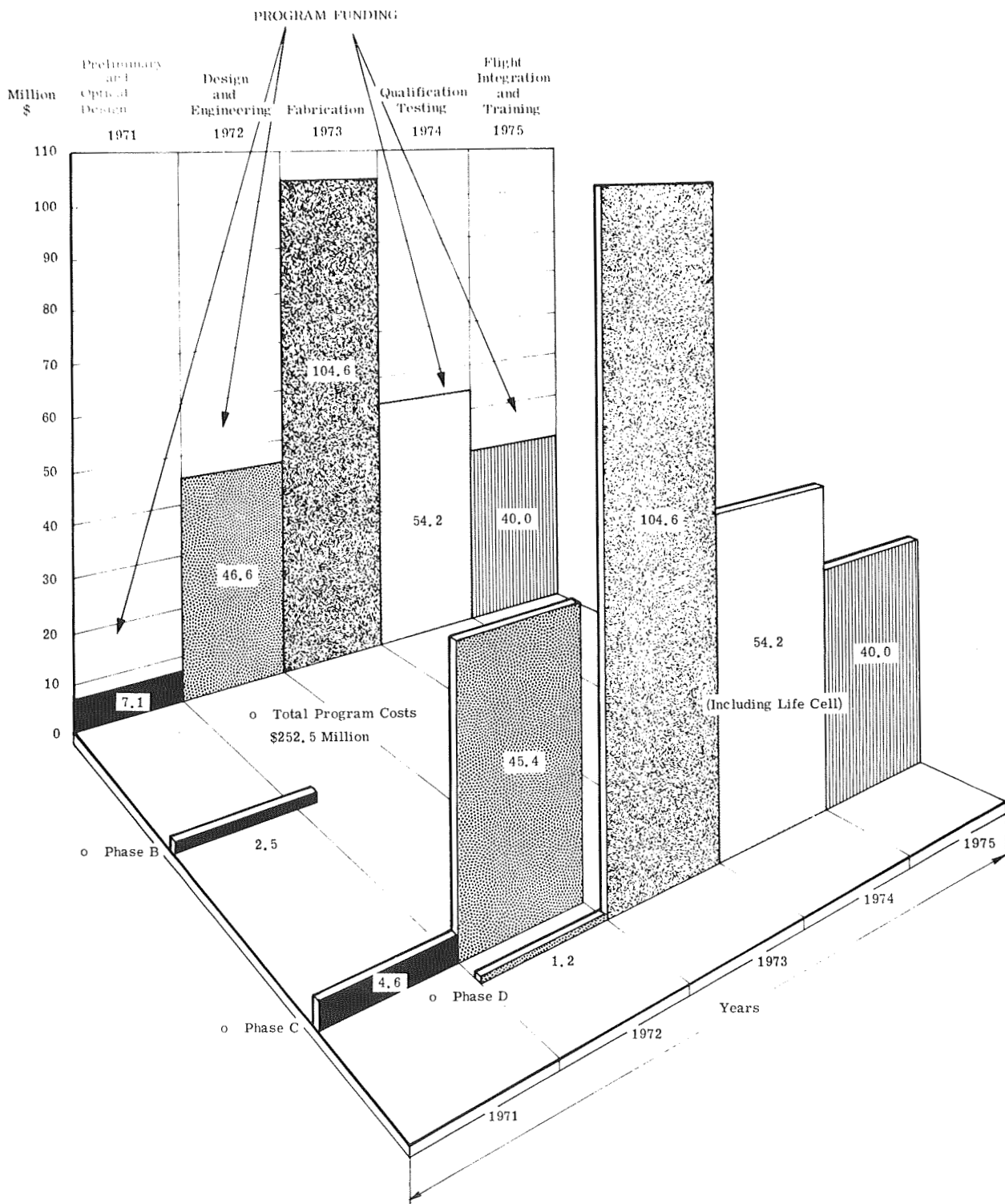


Figure 372. Funding Requirements for Saturn IB-LTEP (With Motel)

POINTING DEVELOPMENT PROGRAM

There will be two aspects to the pointing development program, at least initially. The first will be concerned with space-qualified transfer lens system and the second with additional development of the freefloat magnetic suspension technique. Again, the technical description is contained in the body of the study. The engineering tasks involved are as follows:

Mirror Research

Experimental data will be obtained using the scatterplate and/or other appropriate interferometers on an existing diffraction-limited mirror. Specifically, RMS and point-by-point figure data will be taken in the presence of thermal gradients. Controlled thermal disturbances will be programmed to produce: (a) axisymmetric temperature profiles, (b) lateral temperature profiles and (c) simulated direct solar radiation.

Analytical models of the mirror, based on the analysis of the thermo-elastic deformation of the mirror in the presence of the thermal disturbances of item 1 above, will be developed and experimentally checked. Numerical and computer correlation of the experimental figure data with predictions from the analytical mirror model will be carried out. Emphasis will be on: (a) RMS figure degradation, (b) focal point changes and the need for refocusing and (c) determination of aberrations assignable to astigmatism and coma.

Pointing Development, Transfer Lens

The task shall be to design the transfer lens mechanical assembly to achieve a spaceworthy design capable of surviving launch, and of long-term operation in a hard vacuum.

Ground tests shall be performed with the unit in a preferred orientation. A complete set of electronics as required for functional operation shall also be fabricated and tested. However, only those portions of the electronics which must be located on the mechanical assembly shall be of space-qualified design, two candidates in this regard being the position sensor excitation source and high-input impedance amplifiers.

The mechanical assembly modifications will feature required caging mechanism(s), motors designed for minimal mass and stray dc field, the simplified suspension arrangement featuring BeCu flexure strips, and the elimination of position sensor flex wires. The present approach using flex lead motor coil connections shall be improved also. It is intended that performance and configuration of the mechanical assembly shall be as required for the LTEP equipment, so that the deliverable item will be directly usable on that program.

Moreover, qualification testing shall involve the launch environment requirements for Saturn V since this is a representative and most probable launch vehicle.

When all of these engineering tasks have been completed, a breadboard and prototype of the essential components of the LTEP will exist late in 1972. Based on the technological knowledge developed through this supporting research and developmental activity, the final design of the LTEP telescope can commence. In approximately one year the paper design should be ready for the critical design reviews. Upon NASA approval, fabrication of the spaceworthy telescope can commence. The developmental testing in this phase of the program can be minimized because of the earlier prototype breadboard development in the laboratory. The final flight versions will be quality tested during the late -'73- early -'74 period. Integration of the telescope system with the sensors and instruments can be accomplished late in 1974. Total systems testing and integration with the spacecraft will be accomplished in the balance of 1974 and 1975 for a launch in the very last part of 1975.

SENSOR AND INSTRUMENT DEVELOPMENT PROGRAM

As indicated in the Technology Development Schedule of Figure 369, the selection of the "behind-the-mirror" instruments is delayed to a point as late in the program as is possible. This is done for two reasons. One is that astrophysics is a very rapidly developing science and the investigations of prime interest today are nearly certain to be altered, superceded or downgraded as information is gained from the OAO series, from the SAS series, and from Aerobee rockets. The second reason for postponing the decision as to the complement of scientific instrumentation is that new instrumental techniques are bound to be developed in the next few years, and astrophysicists must become familiar with them so that they might be most expeditiously exploited in the design of the scientific payloads for the LTEP.

ULTRAVIOLET DIFFRACTION-LIMITED TELESCOPE

The early results from OAO II indicate that the young blue stars are ten times brighter in the ultraviolet region than expected, and that the Andromeda Galaxy contains many more hot blue stars than was suggested by extrapolations from the visible. The question of whether the nuclei of galaxies (such as the Seyfert Galaxies) consist of glowing clouds of gas, single mammoth star-like objects, or systems of densely packed stars which collide and explode will require the ultimate in resolving power, and the OAO results suggest that the important information may be in the ultraviolet portion of the light which comes to us. Every advance in resolving power will give us more information on these questions, as we strive to explore and understand the structure of the galaxies. It is certain, therefore, that an ultimate objective of a large space telescope will be to achieve the diffraction limit at 1000Å.

Construction of an ultraviolet-diffraction-limited optical telescope is well beyond the present state of the optical fabrication art. Achievement of the "diffraction limit" (e.g., $\sim \lambda/50$ rms optical figure tolerance) at 1000Å implies figuring the optics to $\lambda/250$ at visible wavelengths. For large monolithic primary mirrors, this is clearly not possible, and for space operation, all of the thermal, alignment, and focus tolerances are tightened up by a factor of 5. In the waning days of 1969, there are debates as to whether it is really necessary to go to the complexity of active optics for telescopes of 2- or 3-meter aperture for diffraction-limited telescopes at visible wavelengths. Only the proponents of active servo-controlled deformable optical primaries are willing to continue talking when the subject of achieving the diffraction limit at 1000Å is addressed.

As formidable as the problems are in improving our optical skills by the necessary factor of 5, an even more difficult problem is that of reducing the scatter of the far ultraviolet light so that diffraction at the aperture is the phenomenon which defines the performance of the telescope. All of the by-now familiar curves which show reflectivities of 20 percent at 800Å - 900Å imply that the other 80 percent of the light flux is dissipated in some fashion or other, and sad experience shows that much is scattered rather than being simply absorbed. Much can be achieved by limiting the spectral range of reflectance in multi-layer thin film coatings so that both reflectance and absorption are improved with a corresponding reduction in scattering. This technology is just starting its research in the vacuum ultraviolet, and needs additional support in order to achieve the payoffs required for a large, diffraction-limited ultraviolet space telescope.

While it is not to be expected that the NASO telescope of the '80s will achieve diffraction-limited performance in the far vacuum ultraviolet, the technology required for the ultimate achievement of this goal should continue to be nurtured.

In planning the resources necessary for the telescope experiment program, present-day experience can be used as a guide and, of course, the representative scientific instruments studied in the body of this report form the basis for this planning. Although it is realized that as techniques and astrophysical knowledge accelerate in their accumulation and some of the instruments studied will be set aside, they form the basis for planning extrapolations.

Modified Echelle - Module A

The tasks required to design, develop, fabricate and test this instrument are as follows:

1. Preliminary engineering and optical design to perform ray trace, aberration study and minimization, and calculate focal plane intensity profiles
2. Ruling of gratings, breadboard and test with TV in vacuum ultraviolet
3. Execution of final design, design reviews, mockup (including image movers and fine error sensors), structural design, engineering model fabrication and development testing
4. Fabrication of six units for prototype, thermal-vacuum testing, vibration testing, qualification testing, an engineering functional unit, and a flight unit.
5. Preparation and execution of test program to verify performance in vacuum ultraviolet and with main telescope, and qualification of equipment for its space usage. Where astronaut participation is planned, the training would be accomplished in this phase

When combined with the Product Assurance, Configuration Management, Program Management and other necessary supporting functions, the estimated costs for this program would be approximately \$4 million if executed over a two-year period. These costs are exclusive of mission support, Principle Investigator expenses, and Ground Support Equipment.

f/10 Film Imagery, Module C

f/50 Electronic Imagery, Module F

These packages are of approximately equal complexity, with the costs for the mechanisms needed to handle the film being about equal to the costs of the extra optics needed to form the f/50 images and the image movers (not needed in the f/10 imagery package). The same sequence of tasks would be necessary to accomplish the fabrication and qualification testing of these instruments. Because there are more mechanisms which must be designed to work in the cold, hard-vacuum environment, more funds should be allocated to the developmental testing phase of the project. Therefore, the projected cost for Modules C and F is \$4.5 million each.

Princeton Experiment Package - Ultraviolet Echelle and f/200 imagery, Module D

This module shares many features in common with the other modules, such as the image movers and fine error sensors. For the purposes of estimating, it is assumed that interchangeable designs have been developed and that these costs have already appeared in a previous module. The principle new item in Module D is the all-reflective microscope. While the module contains much complex equipment, there are few mechanisms requiring unique development. Again, the task sequence will be the same as in the previous group, and the costs will be approximately equal to that of Module A, under the assumptions of an already developed pointing error sensor and image mover assembly. Therefore, the estimated cost of Module D is \$4 million. This would be substantially increased if the module were designed and developed as a separate entity, without the benefit of the somewhat standardized error sensor and image mover designs mentioned above.

Rowland Circle Ultraviolet Spectrometer, Module E

Advantage can be taken of the experience gained in the OAO-C program which contained such a spectrometer. As such, the design effort for the LTEP would consist mainly of repackaging to fit within the equipment bays of the telescope and qualification testing of the new package. This effort would be substantially less than in the development of new instrumentation, and is estimated to require \$3 million.

Estimate Sensor and Instrumentation Development Costs

Exclusive of Supporting Research and Technology costs and the costs of mission support, Principle Investigator expenses, and ground support equipment, the costs for the instrumentation development are summarized as follows:

Module A	Modified Echelle Spectrograph	\$4 million
Module C	F/10 Film Imagery	4.5 million
Module D	Princeton Experiment Package	4 million
Module E	Rowland Circle Spectrometer	3 million
Module F	Electronic Imagery	4.5 million

The time phasing of these expenditures is shown in Figure 373.

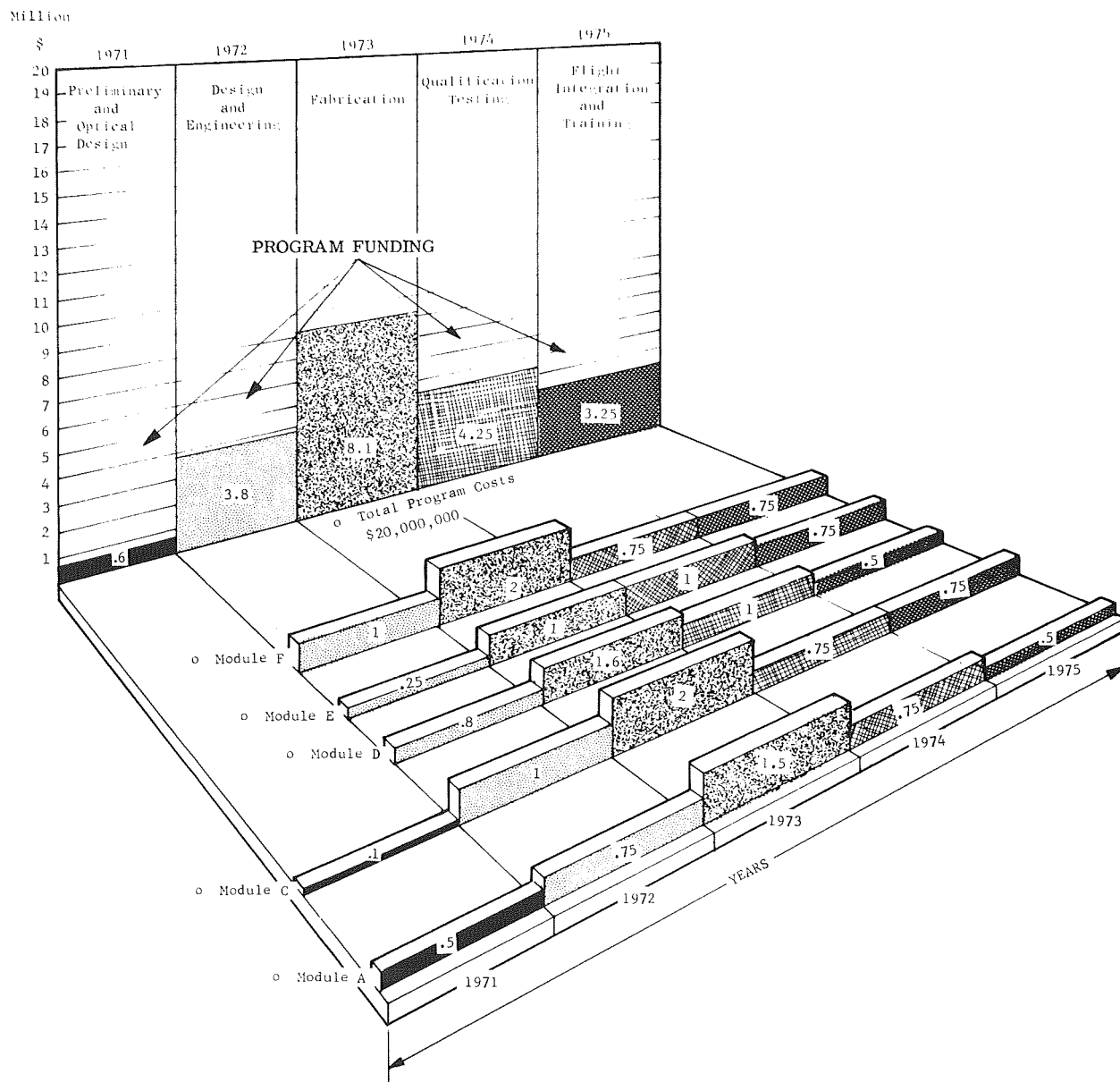


Figure 373. Funding Requirements for Experiment Modules

PHASED PROJECT PLANNING PHASE RELATIONSHIPS

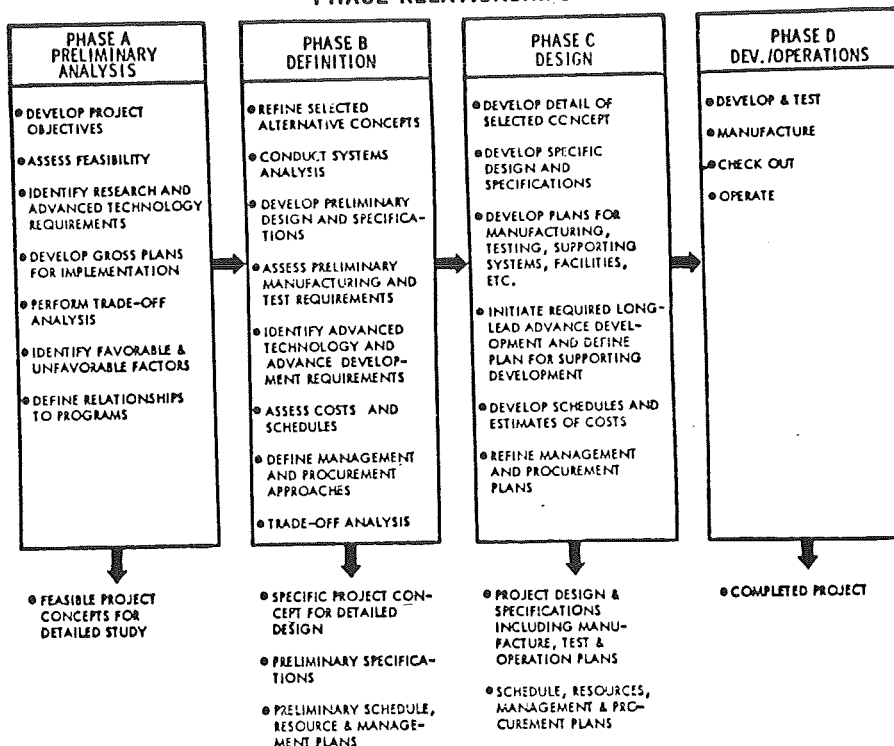


TABLE 52. FLIGHT PLAN RESOURCES (1970)

		Sky Lab-LTEP	Titan IIIC Unmanned LTEP & Rendezvous LTEP	Saturn IB LTEP (With Motel)
Phase A	Invested	\$1 Million	Same	Same
Phase A/B Transitional	In Process	\$.27 Million	Same	Same
Phase B Required In Process	1.25 Million 0.27	1.3 Million 1 Million	\$ 1.3	\$ 2.5
Phase C		13 Million	\$ 20	\$ 50
Phase D Telescope Life Cell		70 Million AAP	\$ 85 \$ 0	\$ 70 \$ 130 Million

Excludes

NASA Costs

Range Costs

Integrator Costs

SRT

Launch Vehicle Costs

Environmental Facilities at MSFC+KSC

FACILITY PLANNING

The facility planning identifies the significant LTEP-unique facilities assumed to be beyond industry's funding interests and added to the government's facility inventory. Facilities not specifically identified are considered to be industry-provided and are not considered to be of major proportion. No significant facilities are required for the concept development phase since the facility requirements will be met by industry capability. The facilities identified in this section are those required to develop the LTEP equipment in the project development phase and, in turn, used to conduct the project operation phase.

The significant program facilities identified for this program are:

- System Development Complex (New)
- Optical Development Complex (New)
- Attitude Control and Rendezvous Development Complex (New)
- Scientific Instrument Integration Complex (New)
- Structural Development Facilities (Existing)
- Training Facilities (New)
- Mission Control Complex (Some Addition)
- KSC Facilities (Existing)

Geographical and common functional subsystem entities were considered in assembling the above facilities. The system development complex was considered to be location sensitive due to the constraints of surface transportation to KSC of the complete telescope. Also, the optical development complex should be located to allow proper transportation of the primary mirror to the system development complex, commensurate with its size and delicate nature.

Of the five new complexes, the system development complex is considered the largest requirement and, correspondingly, the most expensive. This requirement stems from the traditional conservative requirement of proving full system operation on the ground under simulated space operating conditions prior to actual flight. To provide a test complex that will simultaneously provide thermal space environment, seismic isolation, vacuum space environment, very accurate stellar simulation to demonstrate the optics to their design limit, and a gimbal test system to allow the LTEP fine guidance system to be demonstrated to the design limit of ± 0.01 arc-second accuracy will entail a very large resource expenditure.

System Development Complex

The complex shown in Figure 374 is used to assemble, check, and test the LTEP at the system level. Primary facility areas are:

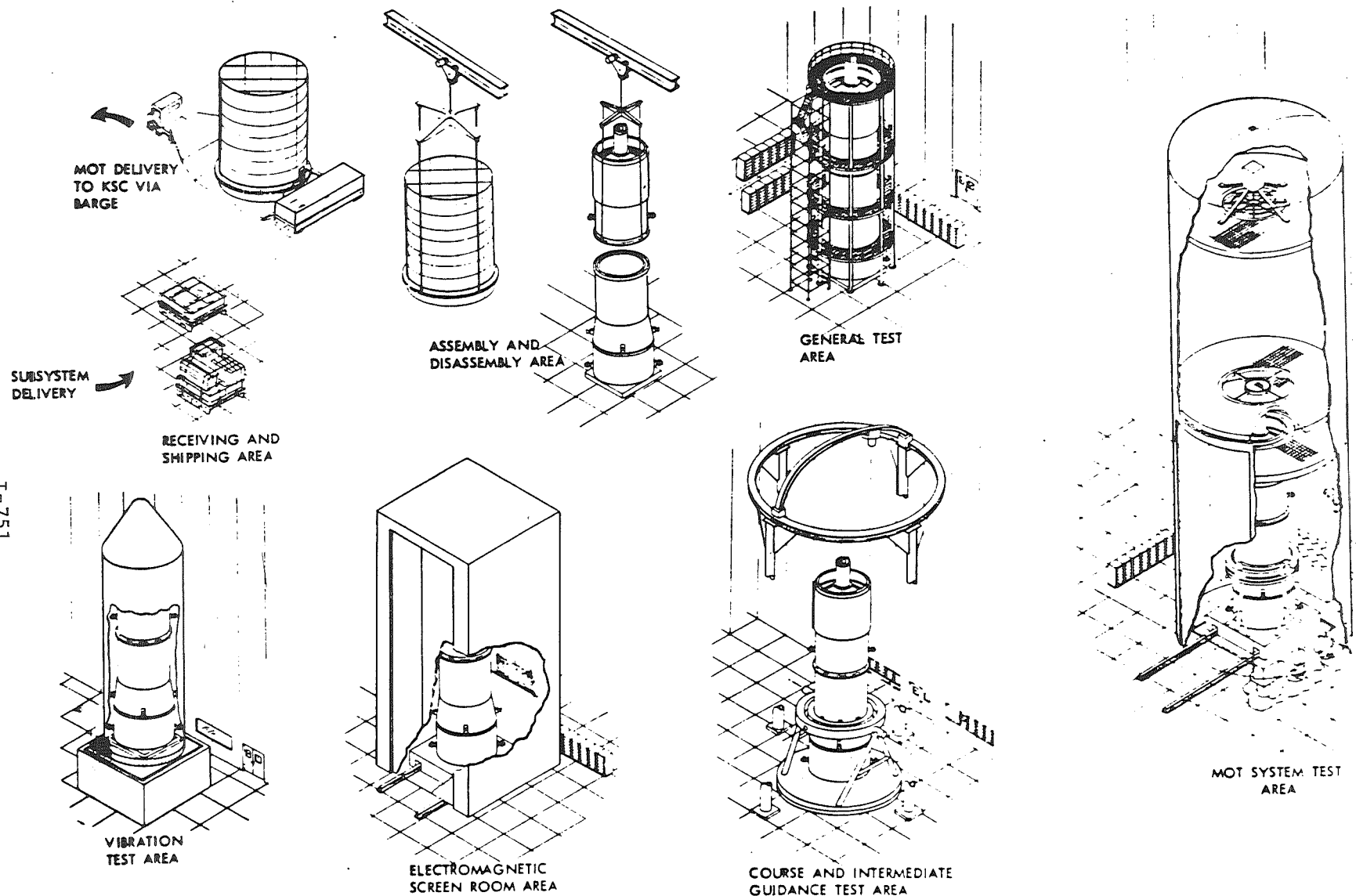


Figure 374. System Development Complex

- Receiving and Shipping
- Assembly and Disassembly
- General Test
- Electromagnetic Screen Room
- Vibration Test
- Acoustical Test
- Coarse and Intermediate Guidance Test
- System Test

The coarse and intermediate guidance test area involves a mechanical gimbal system to support the LTEP; it is capable of allowing the operation of reaction control jets and contains a large travel stellar simulator.

The system test area involves a vacuum chamber in which the LTEP will be suspended on a highly sensitive gimbal test system supported by a seismic base. Within the chamber, looking down at the telescope, will be a test telescope (of the same size but greater accuracy than the LTEP) for stellar simulation. The chamber will be cryogenically cooled to simulate space and will have a solar simulator to simulate the sun's radiation on the side of the telescope.

For optimum program efficiency, this facility should be one integrated building complex. For this study, it is assumed to be located in the Michoud area in Louisiana. This assumption was made for two reasons: first, the primary mirror can be shipped by waterway to this location from the major industrial areas of the country. Water shipment is preferred because of the mirror size, and it would keep the dynamic transportation loads minimized. The second reason for the site selection was to avoid a duplication of this facility at KSC. The philosophy used in the development planning was that after the factory completed the LTEP, it would be shipped to KSC as intact as practicable and in a manner that would not materially reduce the confidence established in the factory. The telescope must be shipped in a manner that keeps the dynamic transportation loads to a minimum and maintains a controlled atmosphere environment. Therefore, water transportation, using the Saturn IC barge or an equivalent, was selected to ship the LTEP by water from Michoud to KSC.

Optical Development Complex

This optical subsystem complex, which is shown in Figure 375, is used in converting the LTEP mirror blanks into finished mirrors and then to integrate these mirrors with the secondary-mirror alignment assembly, the folding-mirror assemblies, and the secondary-mirror interchange assembly. The principal facility areas are:

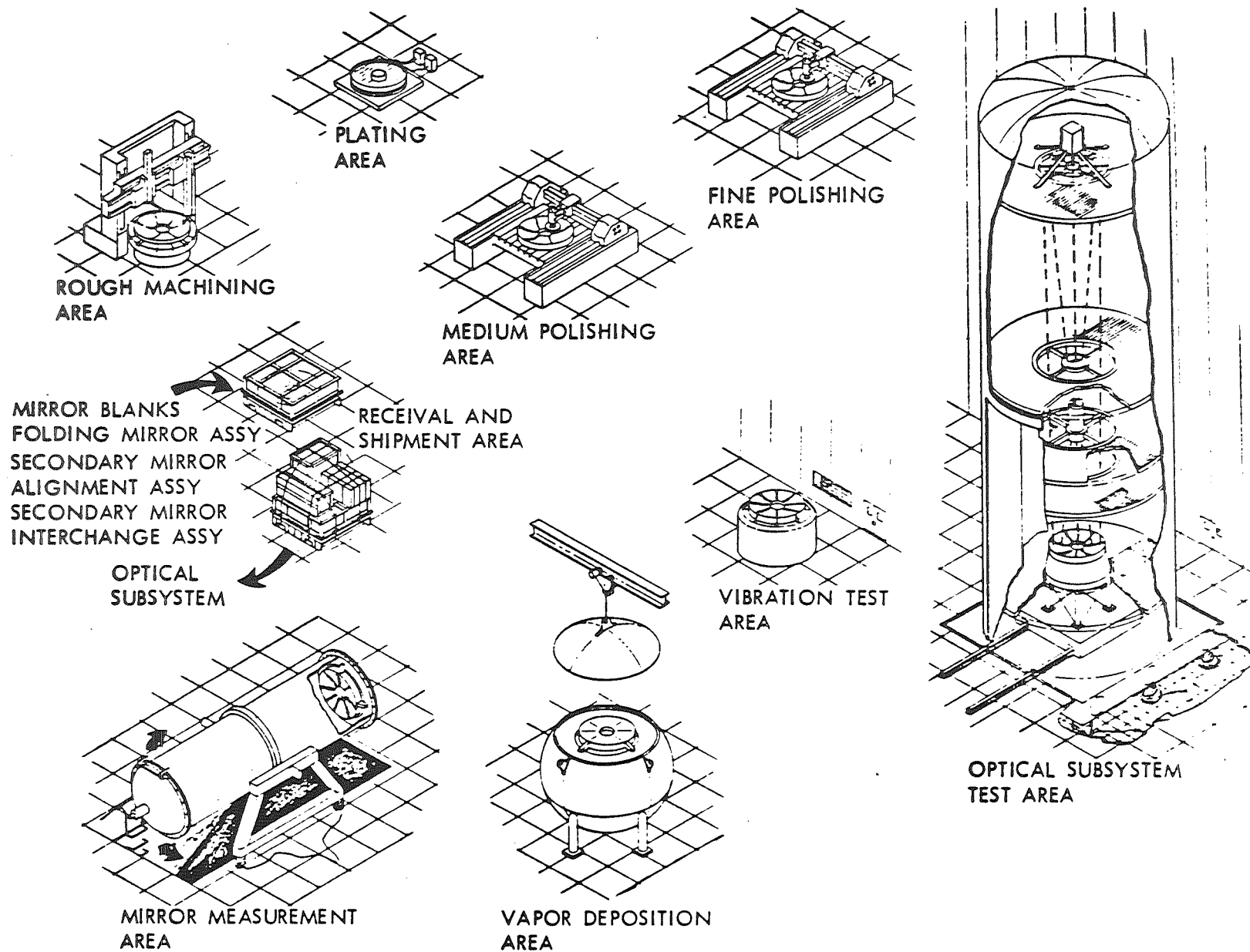


Figure 375. Optical Development Complex

- Blank rough machining
- Blank nickel plating (required only for beryllium blank)
- Blank medium polishing area
- Blank fine polishing area
- Blank optical measurement (including a vacuum chamber with low-temperature capability)
- Vapor deposition area
- Optical subsystem test
- Vibration test
- Receiving and shipment

The optical subsystem test area includes a seismic support of a large, long tubular low-vacuum chamber having low-temperature control. The LTEP optics components would be mounted in a vertical LTEP test fixture and directly overhead would be a test telescope looking at the LTEP optics so as to represent a stellar input. The test telescope would have primary and secondary mirrors of about the same size as those in the LTEP, but finished to a greater accuracy.

Since the LTEP optics subsystem has such very high accuracy requirements, an integrated single-building complex with the above principal areas is considered necessary. Design of the complex should include the necessary fabrication and quality control features to ensure product accuracy. The location of the facilities has not been identified in the study other than being near water transportation routes leading to the LTEP system development complex.

Attitude Control and Rendezvous Development Complex

This facility, as shown in Figure 376, is used to develop the attitude control and rendezvous subsystem except for the propulsion assembly. The principal areas of this complex are the coarse and medium guidance test area and the fine guidance test area.

The coarse and medium guidance test area contains an LTEP test fixture on which are mounted the coarse and medium guidance equipment and the rendezvous electrical and electronic equipment. This simulated LTEP is mounted in a vertical position on a mechanical gimbal system that allows a large angular movement. A simulated stellar source is mounted above the simulated telescope. This stellar source can be moved over a spherical segment area to demonstrate guidance tracking ability. The reaction jets are operated to produce typical LTEP operations.

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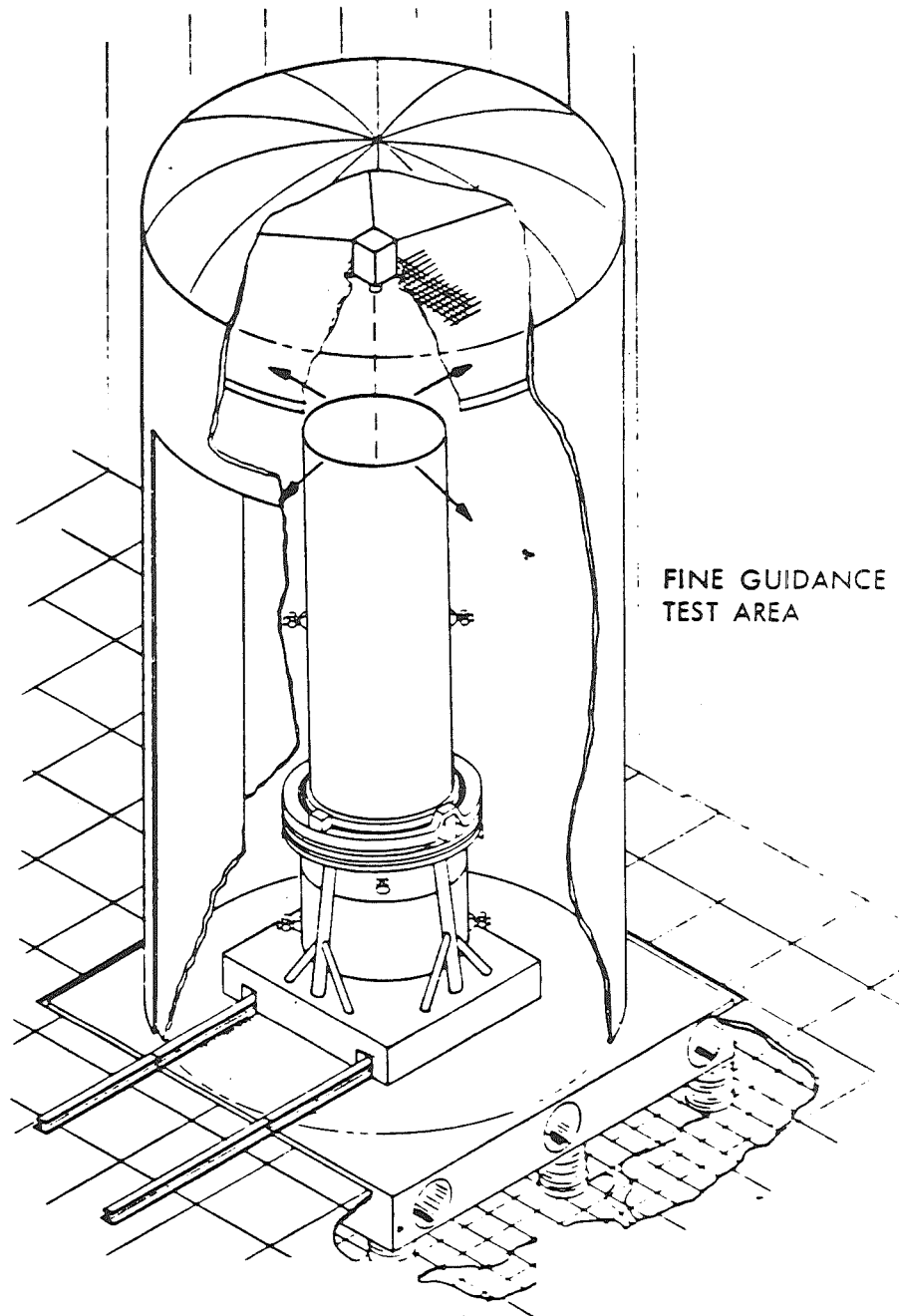
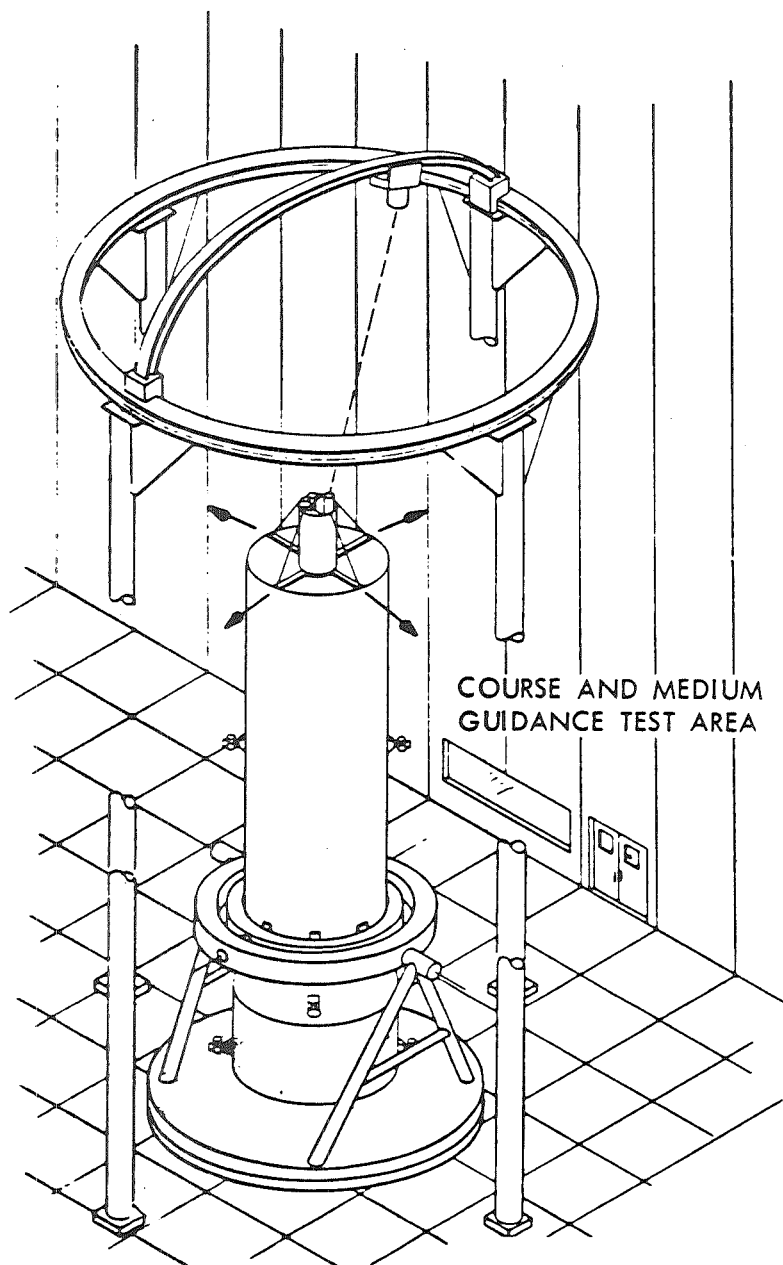


Figure 376. Attitude Control and Rendezvous Development Complex

The fine guidance test area also has an LTEP test fixture on which is mounted the fine guidance system. A movable stellar source is mounted above the LTEP simulator and feeds directly into the fine guidance sensor system. The simulator is mounted on a test gimbal suspension system that provides a small travel and is located in a test chamber. This suspension system is, in turn, mounted on a seismic support base. The test chamber should be capable of providing a low vacuum and expected orbital thermal conditions. This complex may be located anywhere in the United States on the assumption that the LTEP subsystem equipment is readily transported with proper packaging.

Scientific Instrument Integration Complex

This facility, shown in Figure 377, is required to ensure that the instruments are developed to a common integrated operational concept. The subsystem test area contains a simulated LTEP cabin and a telescope image simulator mounted in a test chamber. The instruments are mounted on the platen in the cabin. The chamber is mounted on a seismic base and capable of providing vacuum and low-temperature conditions. Also included in the facility are separate areas for vibration test, receiving and shipment, general test, and processing of the scientific instrument data.

Structural Development Facilities

These facilities will be needed early in the project development phase and before the system development complex facilities will be available. Existing facilities at MSC could be used, as could other existing aerospace industry facilities, for structural development. The significant requirements are for a dynamic test facility capable of testing the full-scale LTEP structures, and a vacuum chamber capable of simulating thermal and solar environment for the testing of the fullscale LTEP thermal model. No additional facilities for these functions are considered required.

Training Facilities

The facilities shown in Figure 378 are principally the brick and mortar for the part task training complex and the mission simulator training complex. The training equipment will have been developed and produced as a part of the project program and then assembled and checked for training use in these buildings.

Mission Control Complex

Some facility additions are visualized for this complex. An experiment control and data facility will be tied to the mission control center by communications. A rather extensive data center is visualized, considering the type of mission and the duration of 5 years or more. The complexity of the experiment control center will depend on whether or not the telescope is remotely controlled from the ground. In the preliminary system concept used in this study, the telescope is not controlled from the ground. Therefore, this control center will perform a manned advisory operation for mission control. Some minor equipment modification may be required in the existing mission control and engineering data center.

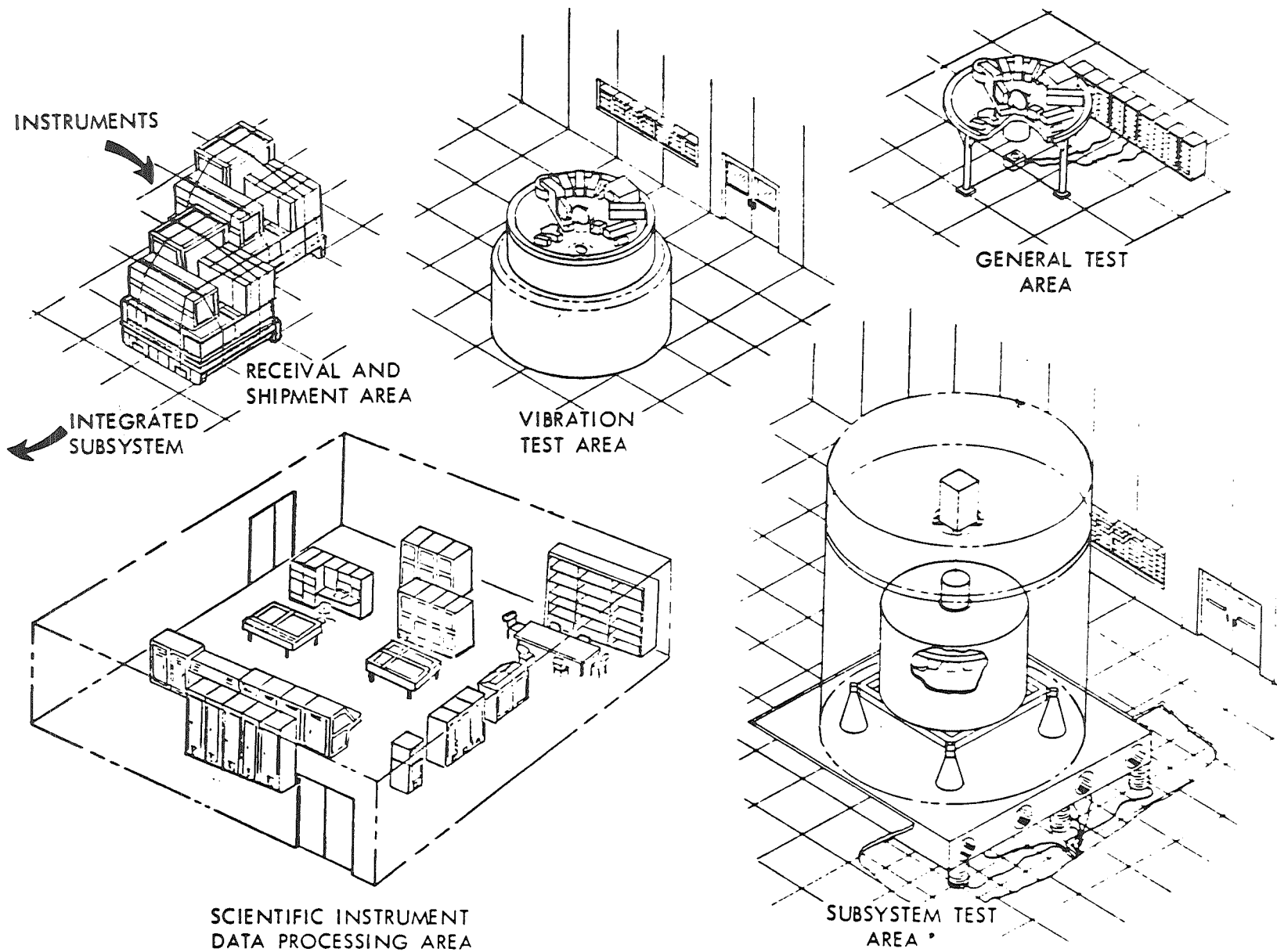
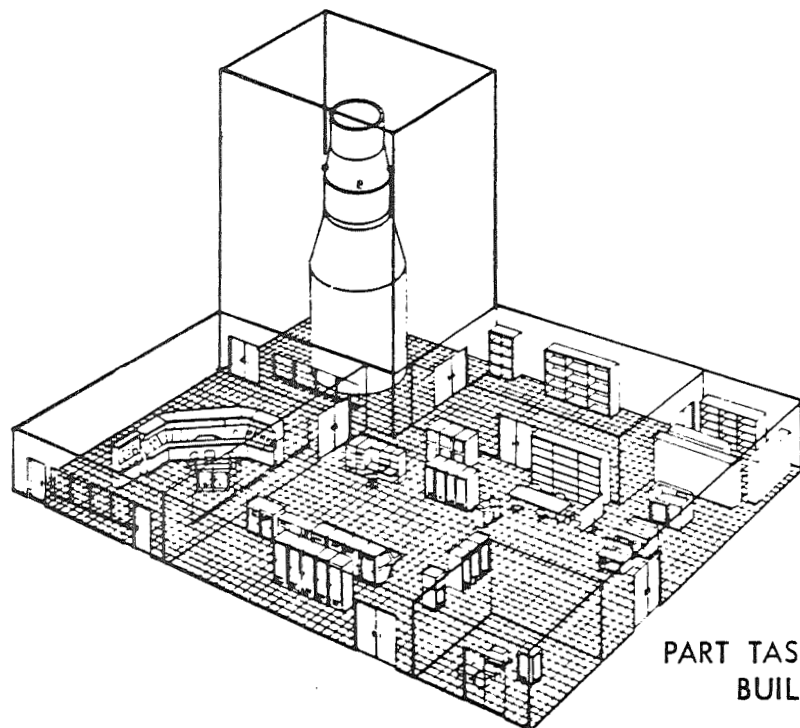
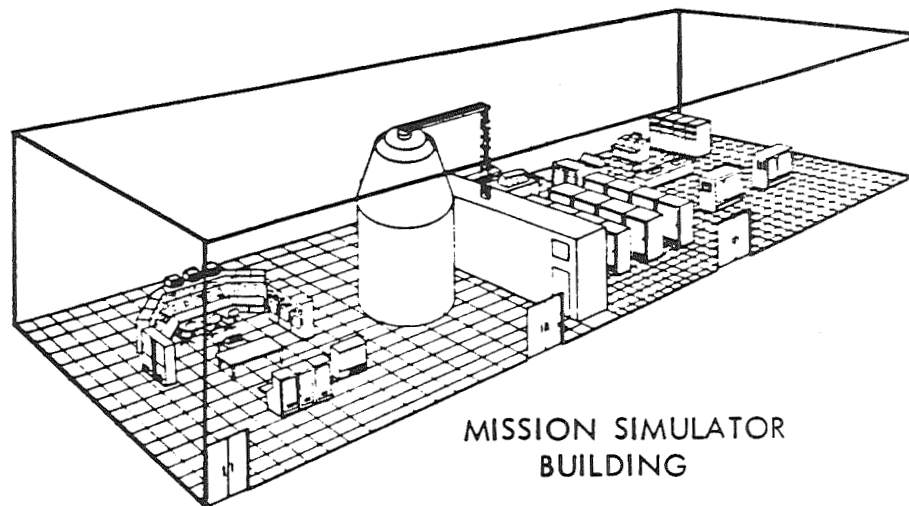


Figure 377. Scientific Instrument Integration Complex

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PART TASK TRAINER
BUILDING



MISSION SIMULATOR
BUILDING

Figure 378. Training Facilities

KSC Facilities

The Apollo and Saturn V facilities at KSC were reviewed as to their capability to handle the LTEP. From this review it was determined that these existing facility complexes would adequately handle the LTEP, particularly in regard to length and weight as illustrated below:

	<u>Height (feet)</u>	<u>Weight (tons)</u>
LTEP (Launch Mode with Shroud)	62	15

	<u>Hook Height (feet)</u>	<u>Hoist Capability (tons)</u>
--	-------------------------------	------------------------------------

Existing Facilities Characteristics

● Manned Space Operation Bldg. (MSOB)	85	25
● Vertical Assembly Bldg. (VAB)	140*	250
● Mobile Launcher	100*	12 to 25

*Hook height above Saturn V

APPENDIX A - IMAGE TUBES

IMAGE TUBES FOR LTEP SPACE ASTRONOMY

The LTEP telescope configuration contains a package designed for electronic imaging at an f/50 focal plane and thus requires a tube whose resolution is better or at least equal to the diffraction-limiting resolution of the telescope. Since one can express the limiting resolution as $\sim \lambda/F$ where F is the final f-number of the system, at 5000Å the tube requires a resolution of 40 lines per millimeter. At the Symposium for Photoelectric Imaging Devices held in London in September of 1965 it was reported that improvements in phosphor deposition enable image resolutions of 100 lines per mm to be obtained at the phosphor with slight degradation being introduced in the multiplication types of image tube. At the present time, four years later, the resolution of commercially available image tubes of various types can only approach this predicted resolution by a factor of one third as shown for comparison in Table A-1. The resolution of the tube required to match that of the telescope is shown in the second column but these f-numbers apply only to imaging and not to spectroscopic measurement of spectral line positions, which requires resolutions on the order of 10 times higher than those for imaging. Resolution is not the only deciding factor in the choice of an image tube since sensitivity, integration and storage capability are of paramount importance when attempting to detect very faint objects.

Conventional vidicon tubes are not suitable for astronomical observations from space since they are relatively insensitive, having no internal gain mechanism. They also cannot be used to integrate and store information and, for this reason, will not be considered in detail for the LTEP. The remaining types of tube shown in Table A-1 all have storage capability and have been used for ground-based astronomy experiments, and one of them, the SEC vidicon, has been space qualified on the OAO and Apollo programs.* No single tube combines the best available resolution and sensitivity with the ability to integrate and store information over long periods of time, but, presently, the most suitable tube for the LTEP is the Secondary Emission Conduction vidicon tube in combination with an intensifier front end. The limitations of image orthicons and return beam vidicons will be considered next before giving a detailed account of the selected SEC vidicon tube.

RETURN BEAM VIDICON TUBES

This type of image tube has a high limiting resolution of 50/mm but has a nominal storage time of 10 seconds. If longer times are required, a special target of polystyrene or quartz has to be evaporated onto the back of the photoconductor to provide a high resistance surface. Tubes of this type have been specially developed by RCA but are not available commercially.

* Watts, Raymond N.: Some Early Results from Celestcope. Sky and Telescope. May 1968, p. 280.

TABLE A-1
IMAGE TUBE COMPARISON DATA

	Resolution	Final f/no	Sensitivity (μ /f-c)	Dark Current	Storage Time	Integrate and Storage
Vidicon (RCA 8521)	30 1/mm	f/55	2.0	0.02 μ A	none	none
Vidicon (RCA 8480)	28 1/mm	f/60	0.05	0.02 μ A	none	none
Vidicon (4503A)	22 1/mm	f/10.0	10.0	0.01 μ A	none	none
SEC Vidicon WL-30691	20 1/mm	f/100	50.0	none	72 hrs	15 hrs
Image Orthicon	20 1/mm	f/100	200.0	none	40 min	40 min
R.B. Vidicon C74137	50 1/mm	f/33	0.21	0.02 μ A	15 min	15 min
Intensifier SEC	20 1/mm	f/100	12,500	none	72 hrs	15 hrs
RCA C21117B Silicon Diode sec	12 1/mm	8/160	2,500	--	100 sec	100 sec

The return beam mode is primarily used when external noise sources from load resistors or preamplifiers become dominant, since the return beam from the target is amplified by a factor of 200 by a five-stage dynode chain within the tube. A disadvantage of this type of tube is that at low light levels the amount of modulation of the return beam can be reduced by as much as 20 times because of the low target voltage, whereas the shot noise in the scanning beam remains constant.

IMAGE ORTHICON TUBES

Unlike the return beam vidicon, image orthicon tubes have been successfully used for ground-based astronomical observations.* Luedicke obtained integration times of 40 minutes but had to use a modified operational cycle because of

* W.T. Powers, R.S. Aikens: Image Orthicon Astronomy at the Deerborn Observatory. Applied Optics. Vol. 2, No. 2, February 1963, p. 157.

Livingston, William, C.: Resolution Capability of the Image Orthicon Camera Tube Under Nonstandard Scan Conditions. J. Soc. Motion Picture Television Engineers. Vol. 72, 1963, p. 771.

E. Luedicke, A.D. Cope, L.E. Flory: Astronomical Image - Integration System Using a Television Camera Tube. Applied Optics. Vol. 3, No. 6, June 1964.

problems arising from long write times followed by readout in a fraction of a second. During long write times spurious pulses and bursts of noise are integrated and can seriously affect image contrast. Also when readout occurs, some residual charge pattern is left on the target and subsequent scans must be made to collect all the available information. The presence of background light emitted by the cathode and being scattered through the tube towards the photocathode was eliminated on the measurements by turning off the read beam during the write phase and by only turning on the high voltage during the read cycle. The limitation on storage time was due to charge buildup starting at the edge of the target and was tolerable for write times of 40 minutes after which the whole target had to be wiped clean before the next write cycle.

SECONDARY EMISSION CONDUCTION VIDICON (SEC)

The SEC vidicon tube is not as sensitive as an image orthicon tube but has superior integration and storage capabilities (Table 53). It can be made more sensitive by adding an image intensifier section coupled by a fiber optic faceplate. Figure 371 shows a schematic drawing of a magnetically focused and deflected SEC vidicon. It consists of three basic parts, an electrostatically focused diode image section, a secondary electron conduction target and a readout section. The photocathode can be any of the standard types depending upon the spectral region chosen and is deposited into the curved inner surface of a fiber optic faceplate. This transfers the focused image on the plane faceplate surface through to the photocathode. Photoelectrons emitted from the photosurface are accelerated by an electrostatic lens and are focused onto the SEC target. This target provides an internal gain mechanism and consists of a 500Å - thick layer of Al_2O_3 followed by an equal thickness of aluminum to form a signal electrode. In the side of the target facing the reading gun is deposited a highly porous layer of KCl across which an electric field is maintained. High energy (8KeV) photoelectrons arriving from the diode image section penetrate the Al_2O_3 and Al layers and dissipate their energy in the KCl layer by producing secondary electrons. The secondary electrons travel through the KCl layer in the interstitial pores of the material and migrate toward the signal plate because of the internal electric field in the KCl material. This produces a positive charge pattern on the target surface focusing the reading beam and, since the target is of high sensitivity, because of its low density, the charge pattern can be stored for long periods of time. The reading beam discharges the target by returning the surface to gun cathode potential and this generates a current pulse which is capacitively coupled to the signal electrode.

The parameters in Table A-1 are those for the Westinghouse WL-30691 SEC tube and this tube can be directly coupled to the image intensifier tube WL-30677 to provide sensitivities far in excess of those of image orthicon tubes since the image intensifier can provide coupled electron gains of 100 to provide overall sensitivities of $50\mu A/ft-c$.

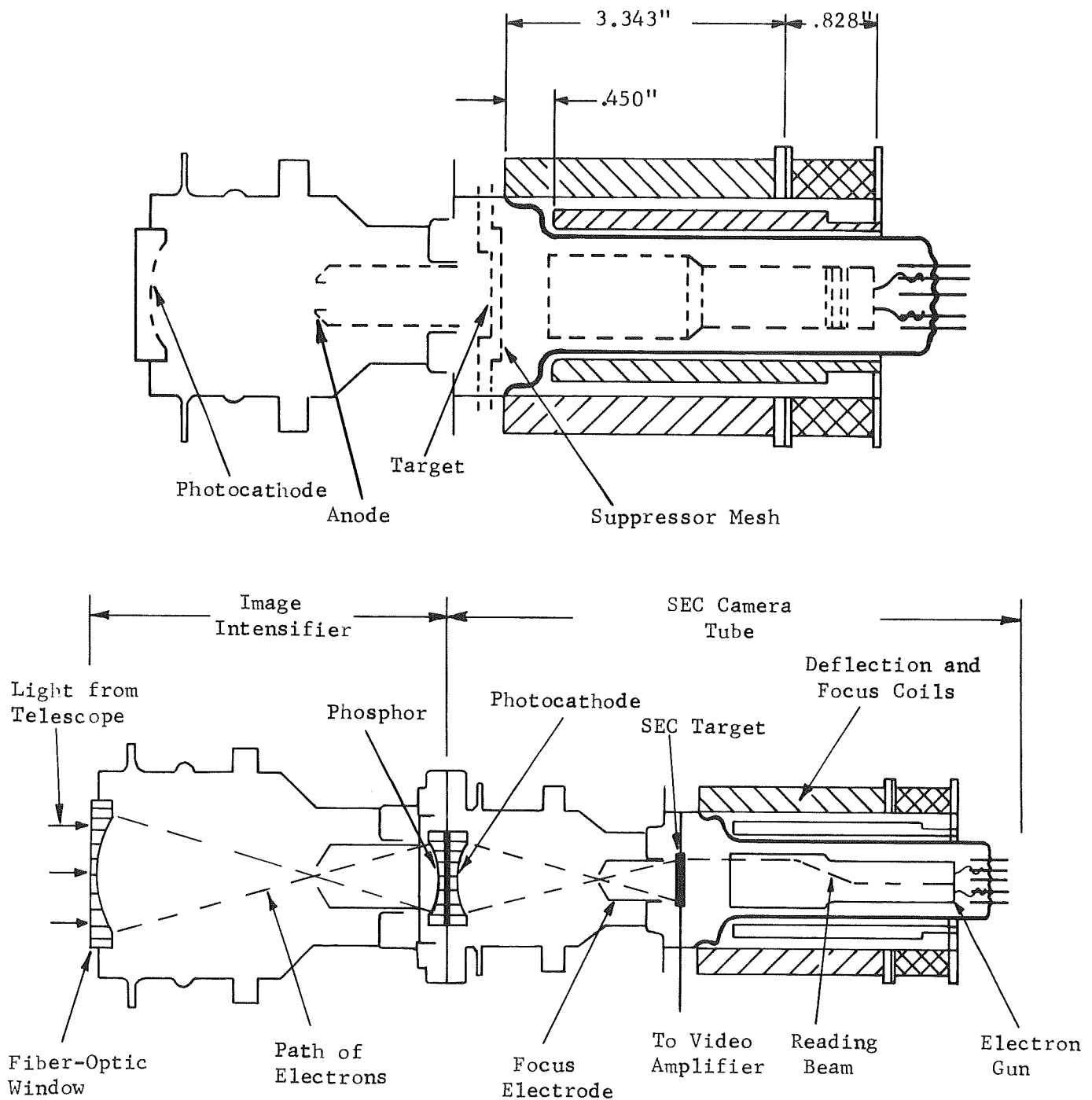


Figure A-1. Intensifier SEC Vidicon

Both the WL-30691 and the coupled version have been experimentally investigated using the 36-inch McDonald telescope by Green* who was able to detect 20-Mv stars against a night sky background. A comparison of the intensifier SEC (WL-30691 coupled to a WL-30677 intensifier) and the unaided WL-30691 was made with previously published data on image orthicons and photographic film.** All the published data, normalized to a 36-inch Cassegrain telescope aperture, is shown in Figure A-2. The limitation on limiting visual magnitude is due to the presence of night sky background and since the background expected in space will be much different, then the results reported by Green provide only a relative sensitivity comparison in the sense that the intensifier SEC image tube is 10 times more sensitive than the image orthicon and 100 times more sensitive than the SEC vidicon. Assuming that this type of tube or some future development (i.e., greater sensitivity, area, etc.) of the intensifier SEC vidicon is used for the LTEP electronic imaging package, then the optical configuration of the package must be altered to take full advantage of the 5.5 arc-minute diffraction-limited field size of the LTEP.

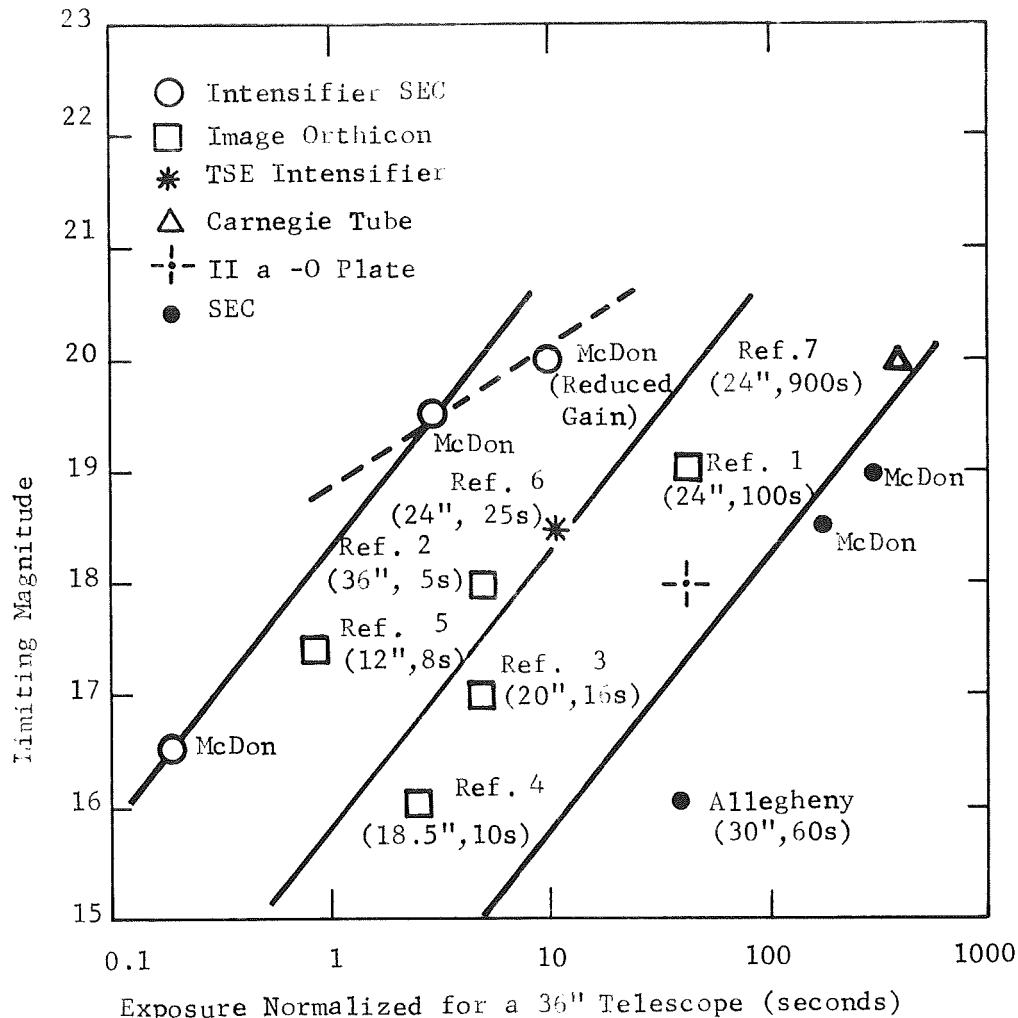
OPTICAL CONFIGURATION

In the previous optical design the final f-number was $f/50$ and now must be increased to $f/100$ to match the resolution of presently available tubes. This entails a different design of the all-reflective microscope with a magnification of 10 instead of 5. Two microscope designs providing diffraction-limited performance have been invented, having magnifications of 5 and 20[†] and a detailed design for a magnification of 10 is a natural extension of these and is not presented in detail since future image tube resolution improvements may be compatible with the previously proposed $f/50$ design. A more serious problem is that of covering the entire diffraction-limited field of the telescope since at $f/50$ the 5.5 arc-minute field corresponds to 138 mm (5.5 inches) and will be 11 inches in diameter at $f/100$. The most straightforward way is to develop 11-inch diameter intensifier SEC vidicon, but at present only 6-inch diameter SEC vidicons have been manufactured. However, there are several other techniques which can be used to enable the 11-inch field to be covered by a smaller diameter vidicon, for example:

*Green, M., Hansen, J.R.: Application of SEC Camera and Electrostatic Image Intensifiers to Astronomy. Presented at the Fourth Symposium on Photo-electronic Image Devices, Imperial College, London, September 1968.

**Cromwell, R.H.: Evaluation of Image Tubes for Use in Direct Photography of Astronomical Sources. Phd. Thesis, Technical Report No. 38, Optical Sciences Center, University of Arizona, April 1969.

[†]Details are not included since these designs invented by D.A. Markle and L. McCarthy are proprietary to Perkin-Elmer.



- Ref. 1 DeWitt, J.H. Jr.: Adv. Elec. and Electron Physics. Vol. 16, 1962, p. 419.
- Ref. 2 Livingston, W.C.: Adv. in Elec. and Electron Physics. Vol. 23, 1967, p. 347.
- Ref. 3 Powers, W.: Applied Optics. Vol. 2, 1963, p. 157
- Ref. 4 Hynek, J.A., Barton, G., Aikens, R., Powers, W.: Adv. in Elec. and Electron Physics. Vol. 16, 1962, p. 409.
- Ref. 5 Hynek, J.A., Bakos, G., Dunlap, J., Powers, W.: Adv. in Elec. and Electron Physics. Vol. 22B, 1966, p. 713.
- * Ref. 6 Wilcock, W.L., Baun, W.A.: Adv. in Elec. and Electron Physics. Vol. 16, 1962, p. 383.
- △ Ref. 7 Ford, W.K., Jr.: Adv. in Elec. and Electron Physics. Vol. 228, 1966, p. 697.

—+— Measured with 36" McDonald Telescope

● Measured with 36" McDonald Telescope

Figure A-2. Limiting Magnitudes for Several Electronic Readout Devices

- (1) Use of multiple tubes to increase field coverage.
- (2) Moving the image tube in x and y directions to cover the 11-inch format and use of a zoom type intensifier SEC vidicon.
- (3) Offsetting the image movers and using the body pointing of the telescope or angular motion of the secondary to change field position.
- (4) Offsetting the image movers and using an auxiliary transfer mirror in the package together with motion of the telescope to change and stabilize the field position.

MULTIPLE TUBES

It is possible to pack three or four SEC tubes in parallel to cover a correspondingly larger field but the gain in coverage is not greatly improved since the usable aperture of most image tubes is much smaller than the tube envelope.

MECHANICAL SCANNING

If a 6-inch aperture vidicon is mounted on an x-y stage, then mechanical motion of ± 2.5 inches will enable the entire field to be covered. The x-y stage does not have to be of high precision because of the large depth of field at the f/100 focus (± 1.6 mm) and, if reliable electrical connections (video output, intensifier and image tube power, zoom control voltage) can be provided, the mechanical scanning provides a simple method of achieving full field coverage. Many intensifier SEC vidicons also provide a zoom feature in either the intensifier or SEC section with a 2.5:1 zoom ratio and therefore enable a 250-percent higher resolution to be obtained over a correspondingly smaller field.

USE OF IMAGE MOVERS

An obvious method of achieving full field coverage is to move the complete telescope structure in the telescope gimbals or to move the outer structure if the telescope tube is operated in the free float mode. This is most simply achieved by moving the image movers and using the error signals thus produced to move the telescope structure for coarse pointing and also to control the secondary mirror for image stabilization.

IMAGE TUBE DEVELOPMENT

The most recent image tubes to be developed have the photoelectron sensing element replaced by a rugged and more sensitive array of silicon diodes. Usually this technique has been applied to standard vidicon tubes, but RCA has recently announced an SEC type of tube (No. C21117B) in which the normal KCl target is replaced by a silicon diode array. This tube is reported to

give a sensitivity equivalent to an SEC vidicon coupled by fiber optic face-plates to a front end intensifier tube. The new image tube is consequently lighter and shorter than the intensifier/SEC combination. However, a serious disadvantage is that the tube can only store or integrate for ~ 100 seconds using a specially developed antimony oxy-sulfide photocathode. The present resolution is 600 TV lines for a 24-mm picture height, i.e., 12 ℓ/mm . At present, the resolution limitation is in the preamplifier coupled to the tube and the resolution changes with illumination levels, i.e., 12 ℓ/mm at 10^{-5} ft-c to 2 ℓ/mm at 10^{-7} ft-c. The inherent resolution of the tube is at present limited by the front section electron optics and the scanning electron optics. The tube sensitivity of $3500\mu\text{A}/\text{ft-c}$ is slightly lower than the estimated $12,500\mu\text{A}/\text{ft-c}$ for the intensifier SEC vidicon but is far superior to the SEC vidicon performance. The increase in gain is attributed to an avalanching process which produces electron-hole pairs in the silicon diode, the avalanche being initiated by hot electrons accelerated from the photocathode to the diode array target. Approximately one electron-hole pair is produced for each 3.5 keV of electron energy and realizable gains of 2500 can be achieved in the target.

This type of tube is in the very early development stage and gains in resolution and sensitivity can be expected from future research into diode array image tubes.

SUMMARY

Although the future sensitivity and resolution of the diode array SEC vidicon tube will be improved and will provide a new concept in image tube research, the field is in its infancy and as a consequence the conventional SEC vidicon or intensifier SEC vidicon is the most suitable image tube for the design of the present instrument packages aboard the LTP. In any event, the choice of a particular image tube should also depend upon its intended application which can only be fully specified when the observer's object of interest is better defined.

APPENDIX B - THE ECHELLE SPECTROGRAPH

This Appendix gives some of the design details developed by Perkin-Elmer in the Advanced Princeton Satellite Study.

ECHELLE SPECTROGRAPH

The main task of this portion of the spectrograph design study was to determine the performance attainable using a two-element echelle spectrograph configuration. The design goal is a resolution ($\lambda/d\lambda$) of 10,000 over the 900Å to 3000Å spectral region.

The use of vidicons as the primary data transducers not only allows the possibility of imaging but also promises a large increase in the rate of acquisition of spectrophotometric data from weak stellar sources. In order to take full advantage of this large data capacity, it is necessary to fold the spectrum so that it covers as much of the sensitive surface of the vidicon as is feasible. Such an arrangement is provided by an echelle spectrograph.

Initially it was expected that the spectrograph would be used to cover the 1100Å to 3000Å spectral region and that a resolution of 10,000 would be adequate. Later, when the possibility of a manned mission was considered, the desired spectral range was increased to include the 800Å to 1100Å spectral region and an even higher resolution capability was suggested in order to take advantage of the increased potentialities of photographic film.

General Properties of an Echelle Spectrograph

Echelle gratings differ from more conventional gratings by having deep grooves, relatively widely spaced, as shown below in Figure B-1.

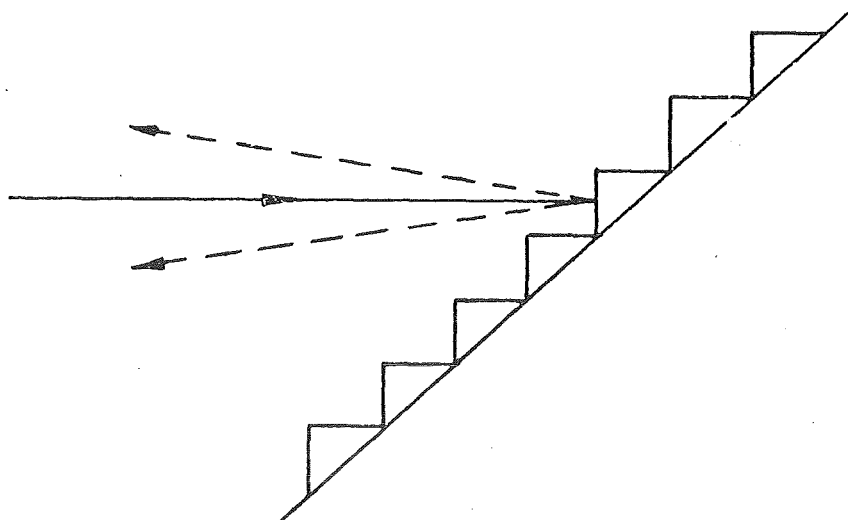


Figure B-1. Echelle Grating

When used at high angles of incidence the echelle grating provides high dispersion at high orders of diffraction. In the usual echelle spectrograph arrangement, a low dispersion predisperser and an echelle grating are arranged so that their dispersion directions are crossed. The high orders produced by the echelle are separated by the predisperser creating a folded spectrum resembling the lines of type on a printed page. Each line corresponds to a different order at the echelle grating.

The folded spectrum provides a substantial increase in the amount of spectral data that can be integrated with the SEC vidicon and readout in a single step. However, it is necessary to insure that each spectral resolution element covers a number of vidicon resolution elements so that a minimum of spurious detail is introduced by irregularities intrinsic to the vidicon. When the spectral source is diffuse, averaging is effected over the length of the entrance slit which is effectively 0.7 mm long and contains a narrow section 0.025 mm across at one end, and a wide section 0.1 mm across at the other. Point sources do not fill the spectrograph entrance slit and averaging of individual wavelengths does not occur unless the spectrum is smeared normal to the high dispersion direction. Optical aberrations will produce an astigmatic image.

The 0.7-mm effective length of the entrance slit sets the minimum line spacing on the spectrograph format at about 1.0 mm in order that some room be left to separate adjacent spectra. A compromise between the requirements for resolution and spectral coverage was made by setting the dispersion at 1000Å equal to 1.0Å/mm. This dispersion and a resolution of 10,000 correspond to about 10 resolution elements per mm which appears to be well within the capability of the vidicon assuming an image tube transfer function of 50 percent at a frequency of 10 lines per mm. In order to ensure that the vidicon is the resolution-limiting component, the design goal for the transfer function describing the performance of the spectrograph optics was set at above 50 percent at 20 lines per mm.

Spectrograph Design

The design of the spectrograph can be divided into two areas--one dealing with the optical properties of the system, with the goal being the definition of the shapes, spacing, and angular orientation of the optical elements so that a resolving power of 10^4 can be achieved. The second area deals with the mechanisms required to record and calibrate the spectral data on the vidicon image tube. Since the optical design is significantly affected by the technique used in recording the data, this area of design is discussed first.

Spectrograph Readout Technique

A question which had not been resolved in the previous studies is what mechanism is to be used for exposing the image tube to the spectral format.

The format produced by an echelle spectrograph is illustrated in Figure 374. The concave predisperser causes the dispersion in the vertical direction on the format, while the echelle grating causes the horizontal dispersion. Not shown is the second-order dispersion of the predisperser, which would overlap the first order. This is shown in Figure B-2 using a slightly different format. To separate the overlapping orders, it will be necessary to use a filter.

In reading out the information, three steps are required: filtering out the predisperser second order; shifting the format vertically; and shifting the format laterally. Shifting the format is considered rather than moving the image tube since the latter would be a much more complicated procedure, and, if the mechanism failed, it could leave the image tube in a position that could not be employed for direct photography.

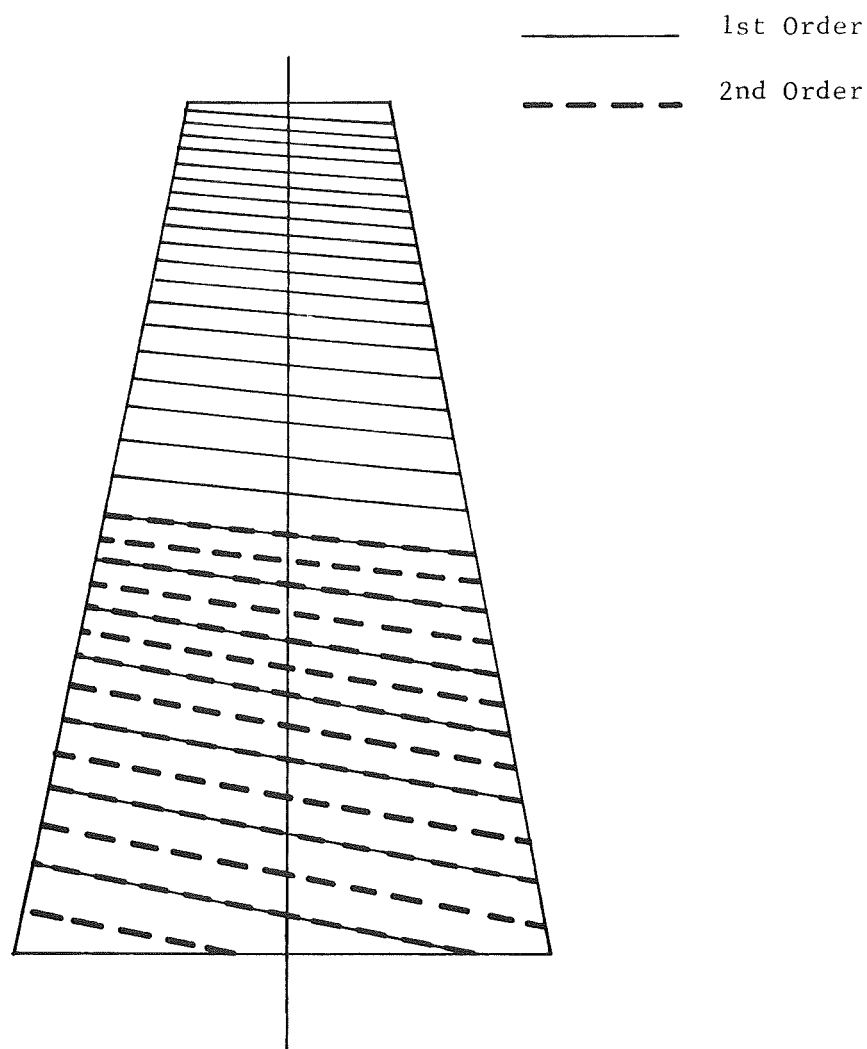
Figure B-3 shows a reasonable format using three exposures. This may be instrumented by deviating the format approximately 33 millimeters by the use of a weak quartz prism in two different orientations, the prism acting both to filter and shift the format for exposing the longer wavelength.

Three different techniques appear feasible. The first is simply to shift the format with a prism which, of course, should be done as far away from the focal plane as possible. Assuming that it is done approximately 500 mm away from the focal plane, the angular shift would be $\frac{33}{500} \sim 6.6 \times 10^{-2}$. This will amount to a tilt of between 2 and 3 millimeters across the format, which is within the depth of focus of an $f/60$ diffraction-limited system at 2000Å. Since the system is not diffraction-limited, this amount is tolerable. The addition of the prism will also modify the dispersion slightly. This technique is shown in Figure B-4.

A second technique would be to use a separate predisperser which could be rotated into place for the longer wavelengths. This would permit more efficient blazing, the use of different dispersions in the long and short wavelength regions, and a better balancing of aberrations, since there will be, in effect, two separate spectrographs, each optimized for different spectral regions. A weak prism would still be required, but in this case only for lateral displacement, and its deviation would be half that of the previous one.

It would be possible to use a prism near the slot to deviate the rays to a second fixed predisperser. This would not require mechanical movement of the predisperser (although it would require mechanical movement of a prism). Lateral movement could then be obtained with a second prism, in the same manner as before. Figure B-5 illustrates this possibility.

A final technique is possible, and that is to reduce the format so that it will fit on one image tube face, and simply insert a plane quartz filter in front of the focal plane when observing the longer wavelengths. This is,



Format For: 720 lines/mm, $r' = 600$ mm - Concave Predisperser
360 lines/mm, $\rho = 500$ - Plane Echelle
(For Definition of r' and ρ See Figure 1.9)

Figure B-2. Format Outline with Second Order Overlay

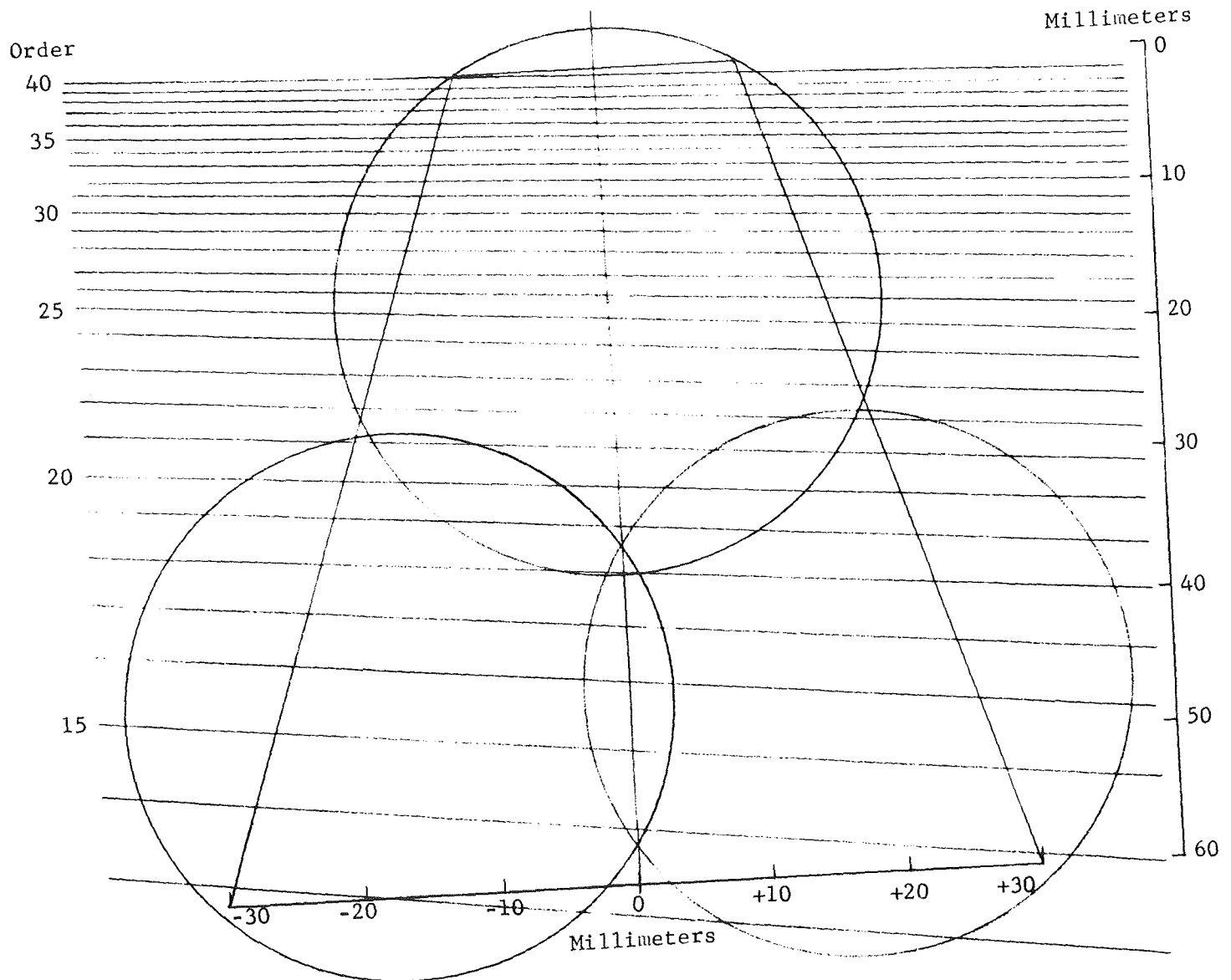


Figure B-3. Standard 3 Format Design
(Showing Presently Anticipated Photocathode Size)

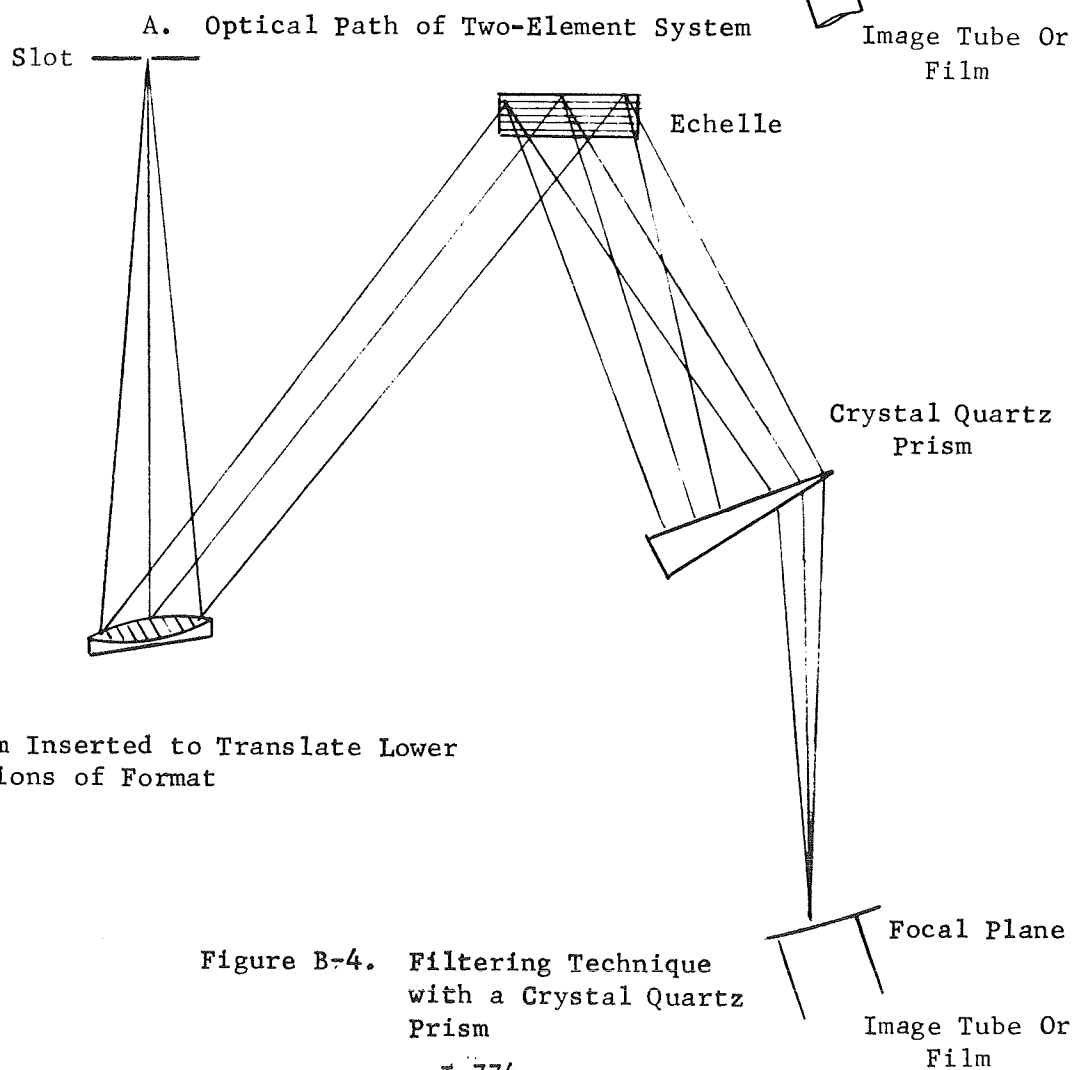
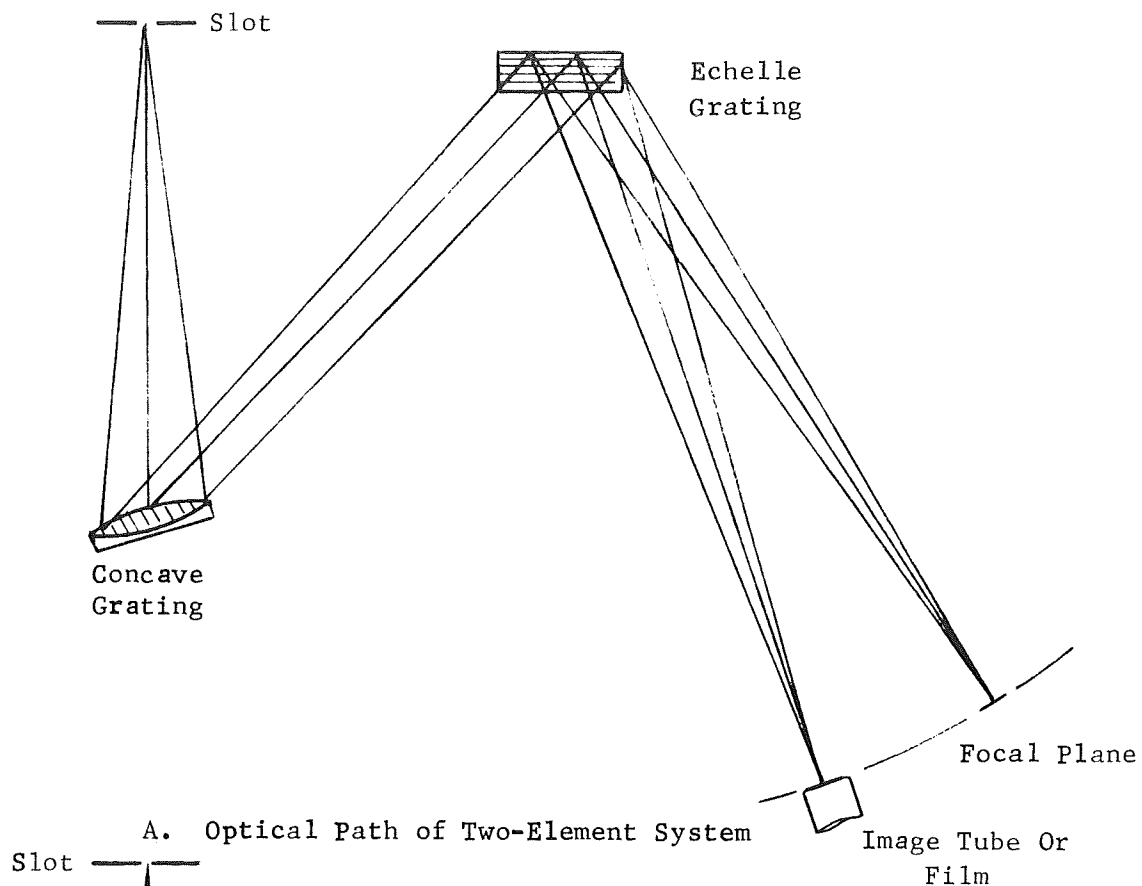
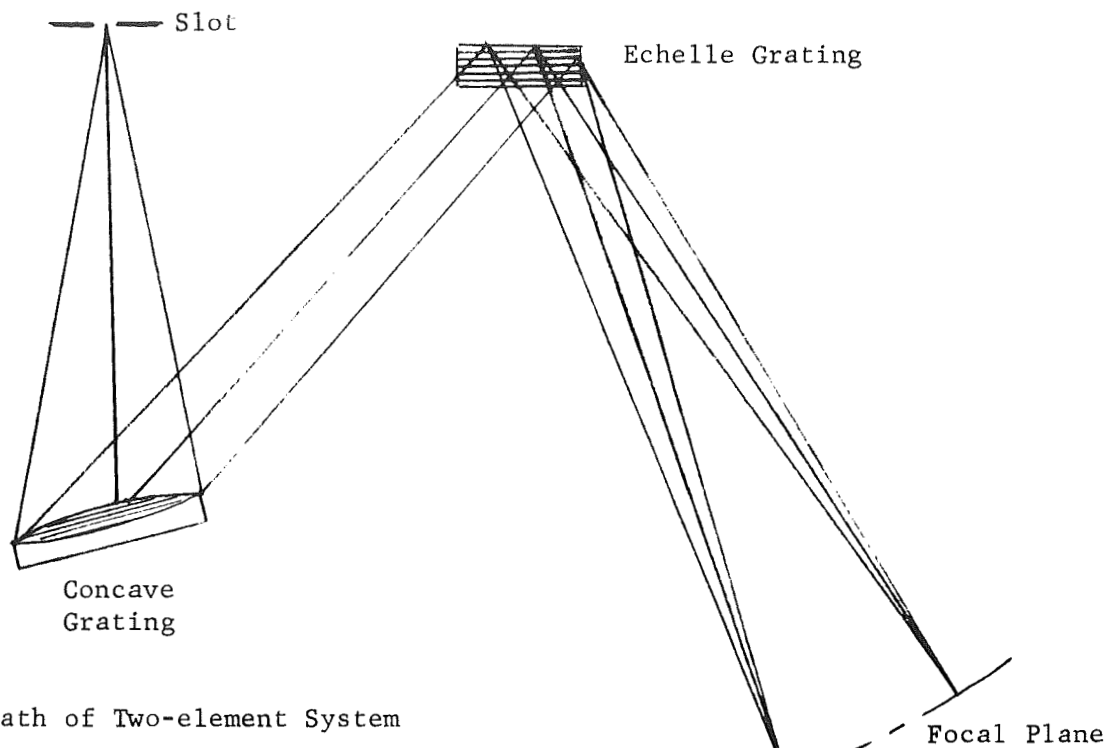
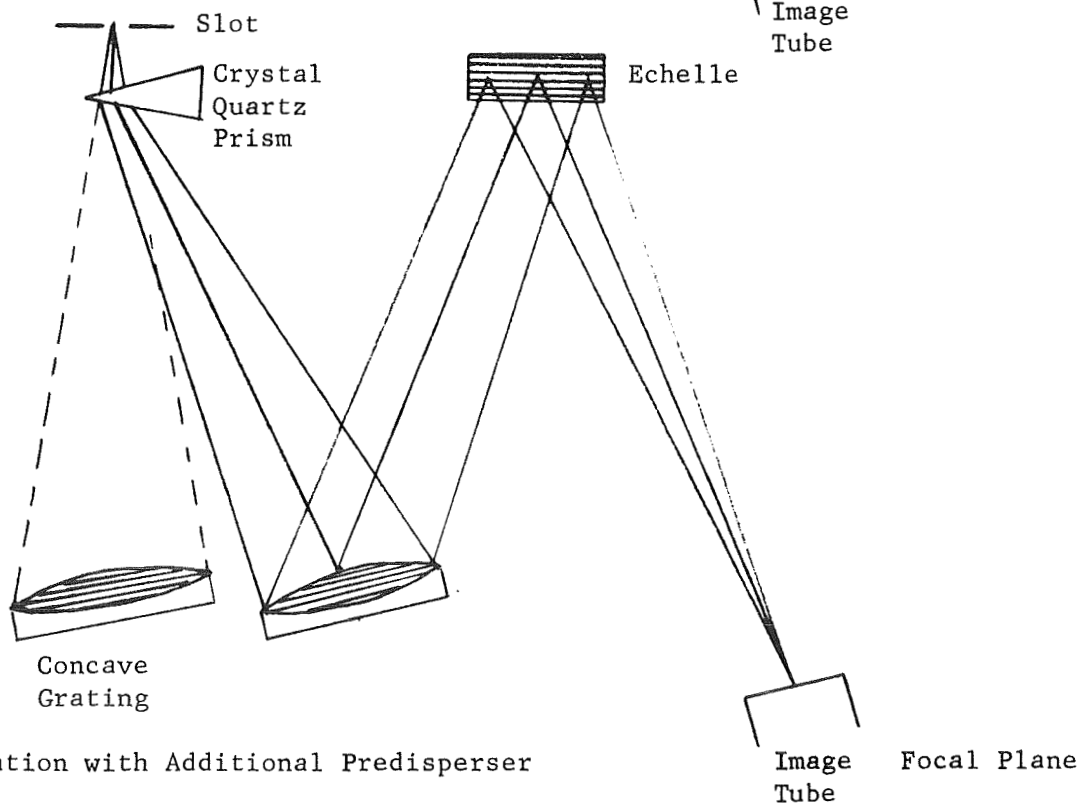


Figure B-4. Filtering Technique with a Crystal Quartz Prism



a. Optical Path of Two-element System



b. Configuration with Additional Predisperser

Figure B-5. Filtering and Spectrum Shifting Technique Using Separate Predisperser.

of course, the simplest solution in many respects, but is extremely difficult from the standpoint of packing the spectra close enough to include all the desired lines. For image tube work, if it is possible to neglect a number of wavelengths close to the 2400Å to 3000Å region and to neglect some of those shorter than the region past 1300Å, a one-format readout could be employed. This, of course, has the advantages of mechanical simplicity, a factor-of-3 decrease in exposure time, due to the three formats otherwise required, and decreased aberrations due to the decreased angular deviation. During astronaut participation, film could be used which would record the entire format.

Figure B-6 shows one of the early formats used, and is similar to Figure B-2, which illustrated the overlay of second order, with a modification using a decreased dispersion from the concave grating. The wavelengths lost are indicated approximately on this format. It should be noted that in this case the 2382Å and 2600Å lines of FeII and the 2852Å line of MgI are included. Comparing this format to the second order overlay shown in Figure B-2, we see that in all cases a filter will be required to eliminate the second order of the predisperser.

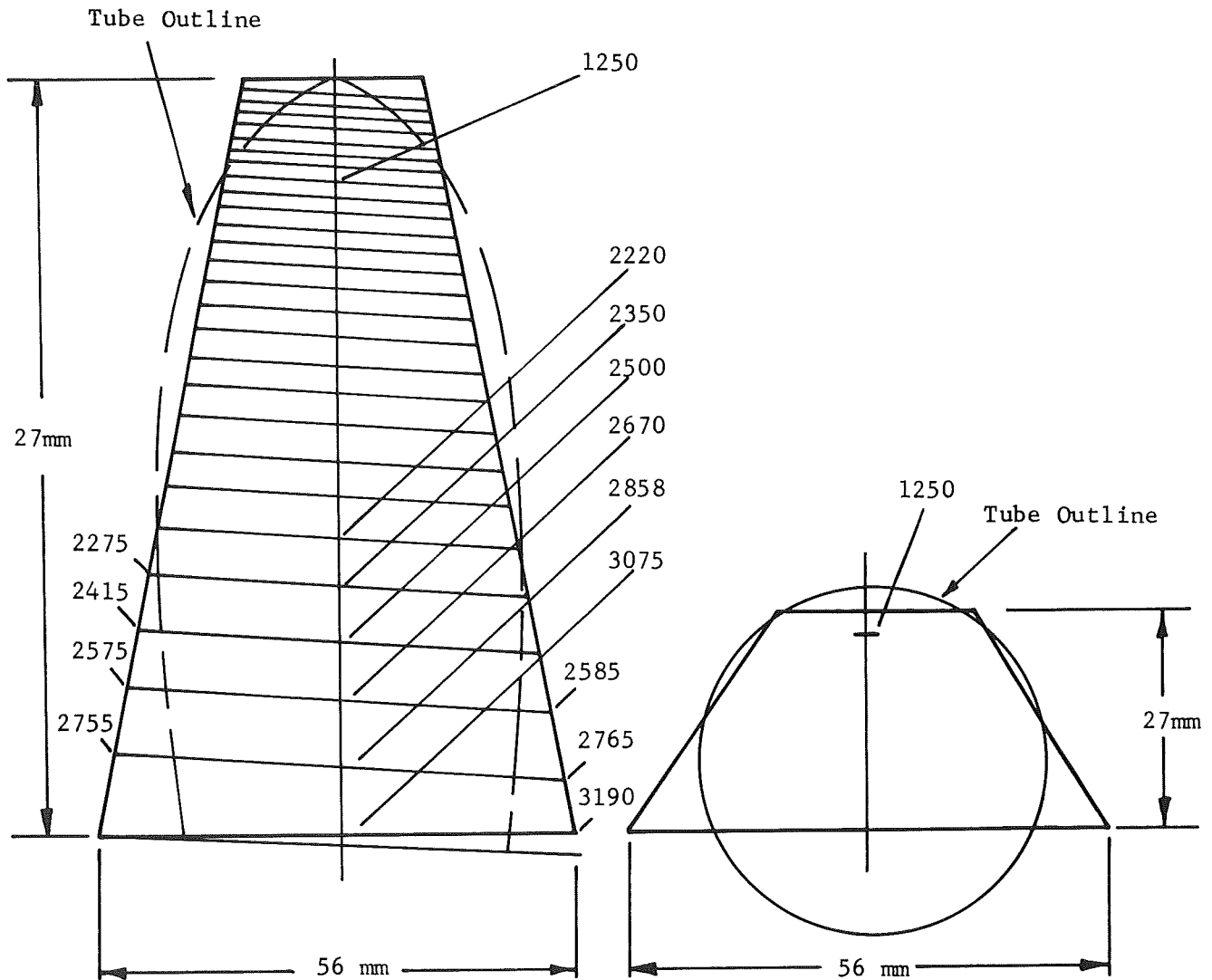
Calibration Techniques

The spectrograph is essentially a reimaging system in which the position of a monochromatic image of the star in the spectrograph focal plane is a function of wavelength. A diffraction-limited star image in the f/10 focal plane of the telescope, has a visual diameter of 5×10^{-3} mm. The image is magnified in the spectrograph by a factor of 6, so that at the f/60 focus it has a diameter of 3×10^{-2} mm, or 30 microns. The resolution of the image tube is 50-percent response at 20 TV lines/mm (10 cycles per mm) or 50 microns per TV line. The anticipated resolving power of film will be more than double this. Motion of the star (due to telescope pointing) must be held to 10^{-1} arc-second during an exposure so that the spectra will not be degraded. The position of the star must be known to the same accuracy to determine the absolute wavelength. For best wavelength calibration it may be necessary to use the full 10^{-2} arc-second stability of telescope pointing.

The magnification of 6 has been selected as the largest that can be conveniently fitted into the spacecraft. Larger magnification (slower system) would increase the dimensions but decrease the aberrations. Less magnification (faster system) would increase the aberrations significantly, while reducing the size. The value of 6 (f/60) was selected as a compromise value that would be representative of the final system.

The entrance slot has been discussed in Perkin-Elmer Engineering Report No. 8688. Effectively, the slot is a hole in the flip mirror at f/10 which relays light to the imaging microscopes and subsequently to the direct imaging

*Wissinger, A.B.: Advanced Princeton Satellite Study, Phase I, Section 4.1 Vol. III. Perkin-Elmer Engineering Report No. 8688, June 30, 1967.



Note: All Wavelengths in Å

Figure B-6. Format with Decreased Dispersion
From the Concave Grating to
Obtain Single Readout

image tube. Thus, this tube has a magnified field of view with a hole in the center where the slot is letting light through to the spectrograph from the object in the center of the field of view. Since this type of operation differs from that of the usual spectrographic slit, the term slot has been used.

The entrance slot will not, as it does in most astronomical spectrographs, define the system spectral resolution, since this is defined by the star image. However, in dealing with extended sources, it will define the spectral resolution. The factors involved in selecting the slot dimensions merit consideration, since this must be a compromise.

For direct imaging, the dimensions should be as small as possible to minimize the area of the field of view covered by the slot. For work on extended sources, the slot should be as small as possible for spectral resolution. On the other hand, for stellar work the slot should be large so that the star can enter it easily and small errors in positioning the image movers will not affect the spectrograph operation. A key-shaped slot would be a possibility, but would necessitate a much larger format, since with extended sources, the spectral lines would be lengthened to an image of the slot. This does not seem desirable since system resolution depends to a great extent on keeping all angles small and using as compact a format as possible. Maintaining the original format with a lengthened slot would cause overlapping of the various orders for extended sources. This might not be critical for emission line objects where the lines could be identified, and there would not be too many lines, but in the presence of a continuum the system output would be extremely difficult to resolve, if it could be resolved, at all. The obvious conclusion is that, if stellar spectroscopy is to be given the greatest weight, the slot should be of dimensions commensurate with the positioning accuracy of the image movers. If this accuracy is to ± 2 arc-seconds, then the dimensions should be slightly greater than this, or ± 0.05 mm, with 0.1 mm across as a minimum practical dimension. In this case, the resolution for nebulosities would be an order of magnitude lower, and overlap of orders would occur only slightly, although it would occur between adjacent orders because of astigmatism in the system. A practical compromise might be to use a slightly larger slot for convenience in setting, but to use a diaphragm or pinhole that could be rotated into place for operating with nebulosities, since in this case no increase in pointing accuracy would be required with a smaller slot because of the extended nature of the source.

Wavelength calibration in an absolute sense is difficult since the star acts as its own entrance slit and will be positioned to only ± 2 arc-seconds by the image movers. This corresponds to $\pm 5 \times 10^{-2}$ millimeter in the $f/10$ focal plane, and the uncertainty in position of its image in the focal plane of the spectrograph will be 600μ , or about 10 resolution elements. This uncertainty can only be compensated for by knowing the position of the star to 0.1 arc-second or better and applying a correction. This becomes a procedural matter which can be solved by knowing the position with respect to nearby stars which can have their positions measured relative to the slot of the spectrograph.

One possibility is to utilize the redundant image tube to transmit simultaneously direct images of the stars surrounding the program star (whose image would be passing through the slot in the flip mirror onto the spectrograph). The flip mirror would be in the position to relay the direct images to the image tube not being used for spectroscopy. By knowing accurately the position of the program star relative to its neighbors, and by obtaining the position of the edges of the slot (or reticle marks) relative to the neighboring stars, the position of the program star relative to the spectrograph optic axis can then be determined. The spectrograph wavelength calibration can be determined before flight as a function of image position in the flip mirror slot. It may be necessary to illuminate the flip mirror to make the slot visible.

Wavelength calibration is absolutely necessary, as is locating the physical position of the star in the slit, if one is to have absolute calibration to within the vidicon resolution. Because of the slot size, and the required close packing of the format, it does not seem feasible to present a comparison or reference spectrum at the same time as the spectrum of the star. However, between exposures, a reference source focused on a small mirror near the slot and displaced slightly in the direction of lines of the concave grating (as shown in Figure B-7), will make it possible to present a comparison spectra which is shifted in the vertical direction a few millimeters. Defining the reference light to be used for calibration was beyond the scope of this study. The reference light, of course, would have its own slit to define its spectra. Examination of the long wavelength end of the spectrum and the center of the format would indicate the wavelength in the center of the format, and could denote the wavelength for the bottom of the short wavelength format. This would also measure linearity of TV scan and by checking the standard formats it would then be possible to determine the offset introduced by the TV.

Because of the different format positions, calibration is a multi-step procedure whose repetition rate will depend on the stability of the system as observed in use. Placement of the calibration source in other positions than a slight displacement parallel to the concave grating lines will lead to a large modification of the aberrations which would invalidate the calibration. Intensity calibration may best be done using known stars instead of spectral lines, although the reference source spectra will provide a cross check.

This distribution of effective intensity across the format will depend on the distribution of light diffracted by the gratings, the reflectivity as a function of wavelength, the spectral sensitivity of the image tube, and the distribution of energy of the source.

Consider first the lateral variation across the format, caused by the echelle. This overall intensity distribution resembles that of a single slit, and theoretically depends on the width of the rulings. The envelope will vary as

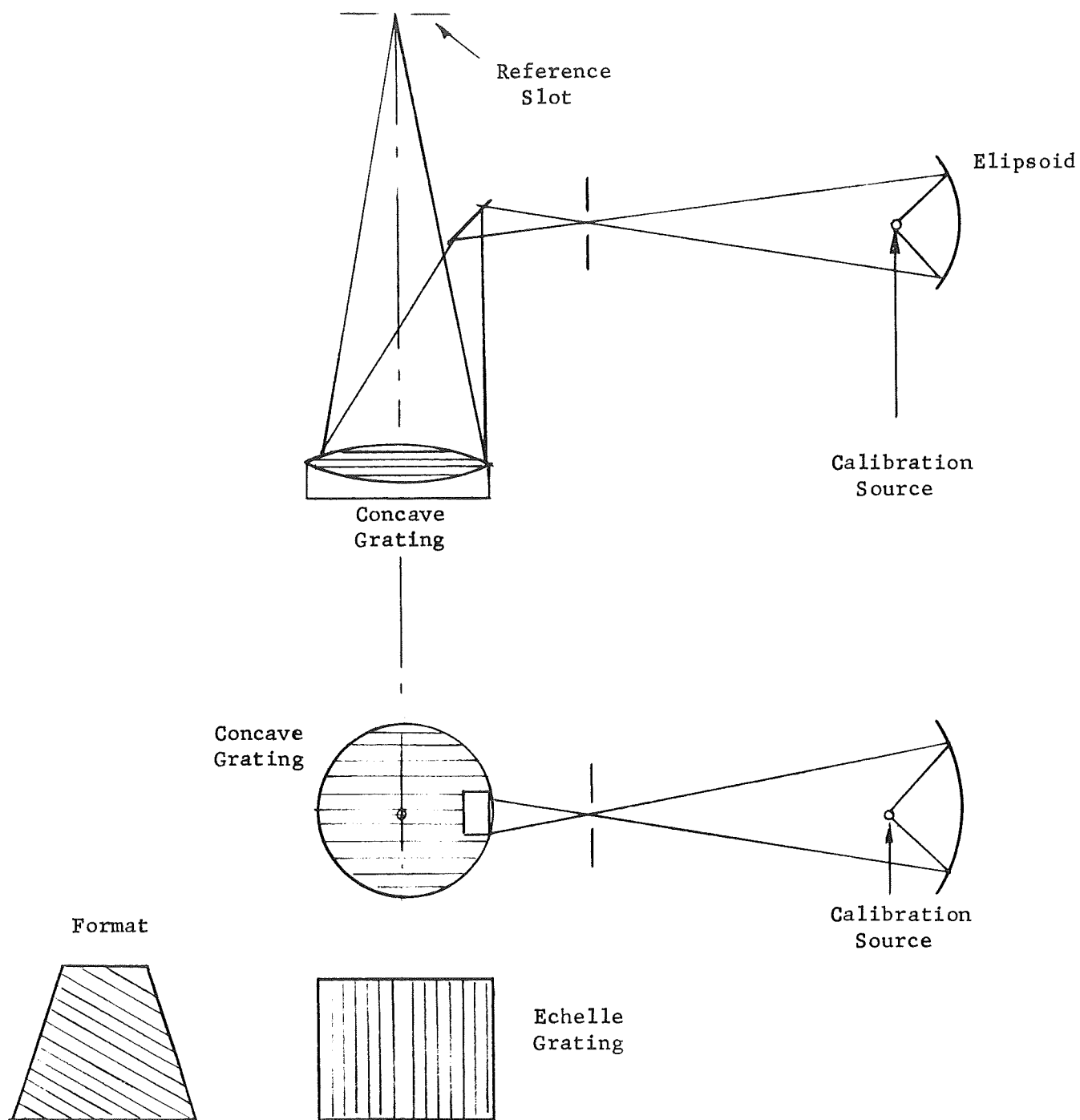


Figure B-7. Comparison Spectra Calibration Technique

$\frac{\sin^2 \beta}{\beta^2}$, where $\beta = \pi a \frac{\sin \theta}{\lambda}$, θ being the angle from the center of the format.

To obtain an approximate idea of where the half intensity points lie laterally, set $\frac{\sin^2 \beta}{\beta^2} = \frac{1}{2}$, so that $\frac{\sin \beta}{\beta} = \frac{1}{\sqrt{2}}$, and approximately $\beta = 1.39$, $\sin \beta = 0.984$.

Then $\frac{\pi a}{\lambda} \sin \theta = 1.39$, or $\frac{a}{\lambda} \sin \theta = 0.442$. Since the angles are not large, let $\sin \theta = \theta$, and as a result we have $\frac{a}{\lambda} \theta = 0.442$.

The important factor here is the ratio of $\frac{a}{\lambda}$, where a is the width of a ruling. It is apparent that contours of constant intensity broaden out with wavelength, so that they follow the proposed format. The angle θ has a maximum at 3000Å of approximately 0.1, or ± 0.05 . Thus for half intensity at the edge of the format we may write

$$\frac{a}{\lambda} \theta = \frac{a}{0.3} 0.05 = 0.442 \text{ micron}$$

$$a = \frac{0.442 \times 0.3}{0.05} = 2.65 \text{ microns}$$

If the ruling is narrower than 2.65μ, the $\frac{1}{2}$ intensity point will lie outside the format, and conversely, if it is wider, it will lie inside. It should be noted that the $\frac{1}{2}$ intensity points coincide with the points where redundant spectra appear, so that one would never need to go past them to obtain spectra. Gratings, in actual practice, are critically dependent on the blaze used, and this is an empirical matter.

Distribution of intensity due to the concave grating, in the vertical format direction, can be treated in much the same way. If a grating is blazed for a given wavelength, the distribution will tend to be narrower for shorter wavelengths, and broader for longer ones, because of the change in the ratio of $\frac{a}{\lambda}$. This value for the concave grating is about 0.05 at 1000Å, so if the blaze was for 1400Å, the 50-percent points for short wavelengths would lie around 1000Å and 1800Å. However, because of the decreased value of $\frac{a}{\lambda}$, the 50-percent point for the long wavelengths would actually lie near 3000Å. Thus again the intensity drops off to $\frac{1}{2}$ at the edges, but in this case the center is at 1400Å instead of at the center of the format. The extreme corners of the format then will have intensities roughly $\frac{1}{4}$ of the maximum, which occurs at the center line of the format in the region of 1400Å. Thus the edges near 1400Å will have intensities of $\frac{1}{2}$, and the maximum drop-off along the center line is $\frac{1}{4}$. Assuming the reflectivity is roughly constant to 1000Å, the intensities will be as shown in Figure B-8. These will be, of course, modified by the image tube, which will decrease the effective intensity radically short of 1300Å. This will not occur when a photographic detector is used.

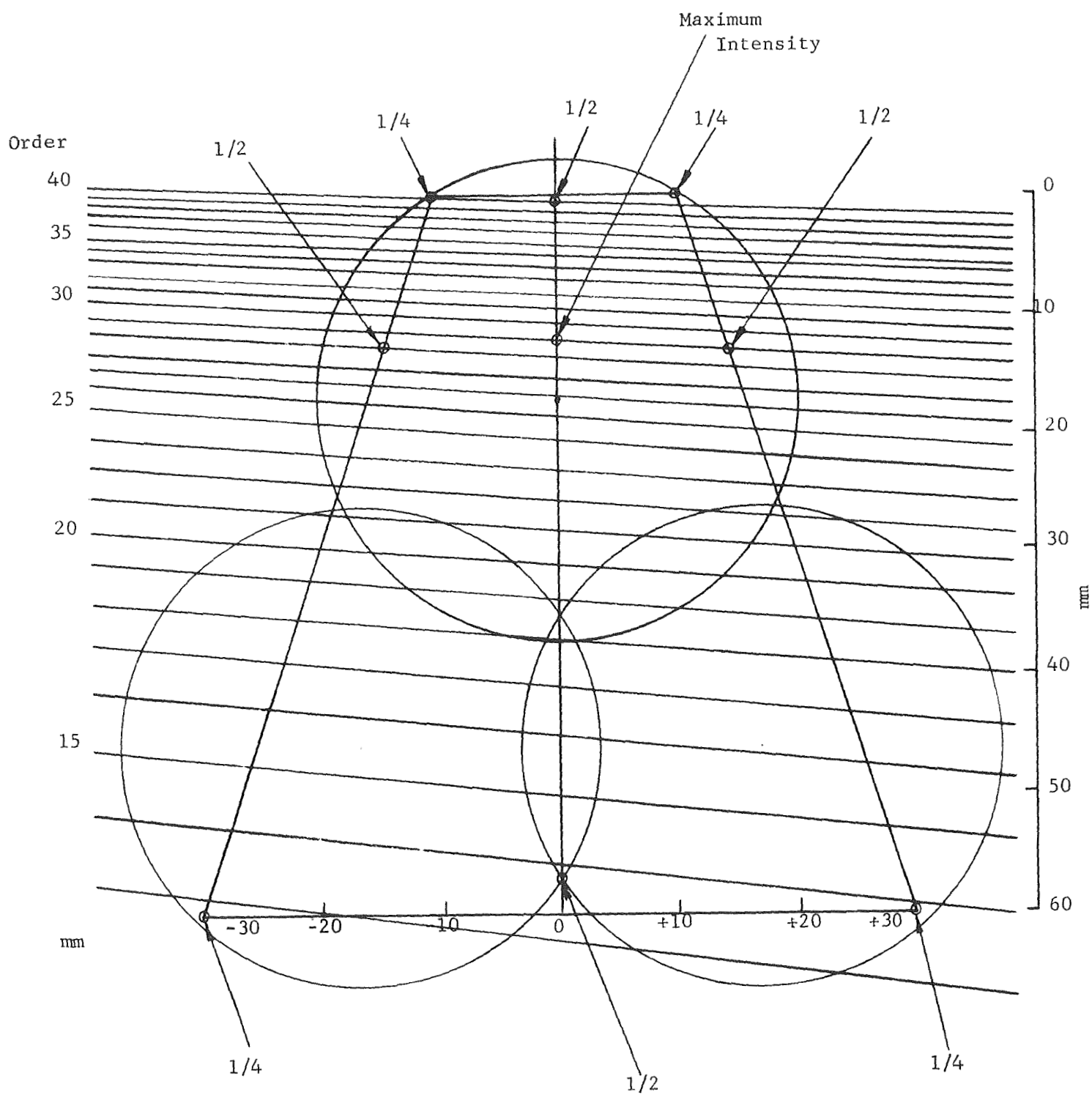


Figure B-8. Relative Intensity in Standard Three-Position Format

General Considerations

Although a vidicon readout has already been selected, it is instructive to compare the vidicon to a photomultiplier detector, because the power of the echelle-type spectrograph is illustrated. Also, the ultimate capacity of the image tube can be computed and then the percentage of the available capacity used by the proposed readout techniques can be computed. The word "efficiency" is used here to mean the length of time required to obtain a spectrum. For simplicity, we will deal in orders of magnitude, since, if two systems are within an order of magnitude of each other, a more detailed calculation would be required.

If we assume a 40-millimeter diameter for the vidicon surface or, roughly, a 30 x 30 millimeter format which has 10^3 mm^2 of area; and, if the resolution is 10^{-1} mm , then there are 10^5 resolution elements. If the closest spacing of orders of an echelle spectrograph format is 1mm, and the spacing doubles at the low order end of the format, 20 spectra can be placed on the tube face, or 6×10^3 of the 10^5 (6 percent) elements can be used, not counting the widening of the spectra. Thus for a 2000Å section of the ultraviolet spectrum at 0.1Å resolution, we need 20,000 elements and three or more frames must be used. If we design for a constant resolving power instead of constant resolution, then 10,000 resolution elements are needed and a three-frame readout becomes readily feasible.

Assume that a vidicon is 10^{-1} as efficient as a photomultiplier because of lower sensitivity. (This is favorable toward the vidicon, because the actual comparison might be more like 10^{-2} .) Assume also a 30-percent efficiency at each reflecting surface. A one-element spectrograph for 10,000 spectral elements using a photomultiplier might be taken as our standard. A two-element echelle reading out the 10^4 elements in one frame is then $10^{-1} \times 0.3$ times as efficient but has 10^4 times as many parallel readout elements, so overall it is $3 \times 10^{-2} \times 10^4 [=300]$ times as efficient. The efficiency of a four-element echelle would be of the same order of magnitude as a photomultiplier spectrometer unless it was read out in one exposure.

	<u>Detector</u> <u>Efficiency</u>	<u>Reflective</u> <u>Efficiency</u>	<u>Maximum</u> <u>Elements</u>	<u>1 Readout</u> <u>Relative</u> <u>Efficiency</u>	<u>3 Readout</u> <u>Relative</u> <u>Efficiency</u>	<u>15 Readout</u> <u>Relative</u> <u>Efficiency</u> *
1 Element PM	1	1	1	1	1	1
2 Element Echelle	10^{-1}	3×10^{-1}	10^6	300	100	20
3 Element Echelle	10^{-1}	2.7×10^{-2}	10^6	27	9	1.8

* Refer to readout scheme of Figure 23, Page 26 of Perkin-Elmer Engineering Report No. 8346(I).

Thus, the two-element echelle leads over all other possibilities using this simple case.

Another factor not discussed affecting relative efficiency is the height of the spectrum. This is irrelevant for a photomultiplier but, for example, if a spectral line is four scan lines high, the exposure/element is reduced to $1/4$, while the maximum gain in signal-to-noise ratio from four repetitions scans is $\sqrt{4}$. Thus, the signal-to-noise ratio is decreased by $\sqrt{2}$ or, in general, by $\sqrt{n}$ where n is the number of scan lines defining the spectral line. It is assumed that the spatial uniformity of the photocathode can be calibrated or nonuniformities neglected, and that the signal is weak enough for the detector to operate in a linear manner (not saturated).

During astronaut participation, film would be used with emphasis on shorter wavelengths. The reflectance of the surfaces is changing rapidly in this region and comparisons depend on the exact wavelength chosen. Film, though it has a low quantum efficiency, can be much larger than an image tube faceplate, so that a number of problems are eliminated. Further, it has higher resolution than an image tube. Any overall comparison must be considered only as an approximation, although it is clear that the two-element echelle retains its advantages. A final design would have to consider these factors and, for example, some use of astigmatism could be made to even out the intensity distribution across the format, simplifying exposure control.

Matching of Image Tube to Spectrograph

Use of the image tube leads to a number of problems which must be considered in the final system design. A system optical transfer function must be established with procedures for evaluating it. For the spectrograph, it is probably best to establish a one-axis (parallel to dispersion) monochromatic transfer function for each star image as a function of position (and wavelength) in the focal plane. If computed by a ray-trace program incorporating the diffraction relations, this yields a set of transfer functions as a function of wavelength. Together with the modulation transfer function of the image tube, it is then possible to establish a system modulation transfer function. Calculation of the signal as a function of wavelength and establishing the scattered light background then will yield the output signal-to-noise ratio, or resolution, for various exposures and wavelengths.

Other parameters of the image tube must also be known. For example, it is expected that the resolution will decrease toward the edge of its field. This strongly affects the problems of format, and hence the overall optical design. Further, the exposure latitude of the image tube must be known, as well as the effects of overexposure on resolution. Another consideration is the fact that the photocathode gives off reflections which have effects on scattered light in the system. Image tubes, as well as other tubes such as image orthicons, tend to have special operating problems which can only be discovered by actually using the device, and the existence of these must be uncovered.

APPENDIX C - USE OF VIDICONS IN ASTRONOMY

STELLAR PHOTOGRAPHY WITH THE VIDICON

The use of an image tube for point sources is essentially different from the use with extended sources. The extent to which it differs depends on a number of factors, and these must be considered separately from those for imaging-extended objects.

Several cases occur which we must consider. These are basically: 1) when the image tube is being used to reach the faintest limiting magnitude, 2) when it is being used to resolve close point sources and, 3) when it is being used for photometry. Characteristics of the image tube other than the sine wave response enter here and need to be specified. From the standpoint of this discussion, we are not interested in the details of these processes but only in how they reflect back on the optical specifications.

First, consider the output for a point image. Image growth is a well-known phenomenon and, although physically different from image growth in a photographic plate, it resembles it. Thus, over some range, we may approximate the image diameter with an expression of the form:

$$d = a + b \log (\text{exposure})$$

Or, two stars of differing magnitudes, Δm , will have diameters

$$d_1 - d_2 = 0.4 b \Delta m$$

and the constant b is known as the **astrogamma** of the detector.

Further, for a photographic plate, and similarly for an image tube, we know that for a given probability of detection, the density difference between the background and the star multiplied by the star's diameter is equal to a constant which is proportional to the granularity, or noise. Density is, of course, analogous to signal. If we increase the diameter by a factor of 2, however, we decrease the signal by a factor of 4, since the area increases as the square of the diameter. Thus, increasing the diameter of a star image decreases the detectability of faint stars, or, to say the same thing in another way, decreases the limiting magnitude. Thus, an increase in diameter by a factor **of 2.512 results in a loss of 1 magnitude** in the limiting magnitude for a given exposure. Obviously then, the most efficient system has some minimum image size, and, assuming that the limiting magnitude is set by the sky background, any image diameter larger than this will result in a loss in signal amplitude, detectability, and limiting magnitude.

Before stating that a certain maximum diameter of the image must be used, some of the other factors must be considered. Increasing the image diameter will decrease the astrogamma, so that images will not grow as rapidly with decreasing magnitudes. Further, it will increase the range over which the signal amplitude will vary, so that the normal gamma may be used instead

of the astrogamma. There is no obvious advantage in photometric work, but there might be some advantage in resolving faint objects near bright ones. Suppose that the diffraction limit (angular resolution) is kept fixed and that this sets image size. Increasing the focal length by a factor r , $f_2 = rf$, increases the diameter of a star by a factor r , decreases the signal by r^2 , and increases the separation of two stars by a factor r . In either case, to a rough approximation, each star will cover the same number of resolution elements, since within limits the area is set by the ability to discharge the image tube target with the scanning beam. Thus, there will be little apparent difference in size in the two cases, but there will be an increase in plate scale, enabling one to detect closer objects.

From the standpoint of point images, therefore, there is a tradeoff between the ability to resolve closer objects and the detectability and limiting magnitudes. If the image tube is assumed to have 50-percent response at 10 cycles per millimeter, then the optimum match for sensitivity (assuming no gain in sensitivity as the image size falls below a resolution element, as is probably the case at this resolution) would be to place the star within 1 resolution element, or 0.05 millimeter. Most of the energy in the $f/10$ focal plane of the primary telescope will be within 5 microns, so a magnification of 10, $f/100$, would yield a match with the tube. The present choice of $f/200$ will slightly decrease the limiting magnitude and detectability of point images and slightly increase the ability to resolve closer pairs. It is interesting to note that it would be possible to modify this situation somewhat by changing the number of scan lines, depending on the aim of the specific experiment.

From the standpoint of point sources then, it is apparent that the controlling factors reflecting back on the optical system are energy concentration and contrast. These then must be included in the optical specification. A number of other considerations are apparent, though, and information about the specific operation of the image tube is required for a final analysis. A number of requirements for data have arisen in this discussion, in addition to sine wave response, that are important.

For a television system, the system capability is strongly affected by contrast, and high frequency detail is frequently lost in the noise. Thus, the best performance of the system occurs when contrast losses are kept to a minimum. It has been proposed that the area under the actual modulation transfer function curve be kept within a certain percentage of the area for the theoretical case. The effects of contrast losses and system photometry suggest that this constraint should be tighter at low frequencies than at high; losses of energy that scatter energy to nearby points reduce the modulation in these areas, causing a loss of resolution that cannot be compensated for by a long tail on the modulation transfer function. From this standpoint, it might be commented that telescope baffling to reduce stray light can be as important in the final operation as the optical tolerances.

optical power is unity. This advantage is utilized in the present design with the exception that the field-correcting elements are assumed to be part of the image-moving system thus yielding equal f-numbers for the uncorrected on-axis image beam and the guide star beams emerging from the image movers.

SOME CONSIDERATIONS IN THE USE OF A TV CAMERA AS THE DETECTOR IN AN ASTRONOMICAL TELESCOPE

The proper matching telescope optical transfer function to a given type of detector has become relatively routine in the past several years*, particularly in the case where photographic film is the detector and the objectives of the experimenter can be specified in terms of a number (e.g., a limiting resolution in so many lines per millimeter in the focal plane). However, even in the case of film when the spectral coverage is extended into the vacuum ultraviolet region**, insufficient data is available concerning the transfer function of ultraviolet films to complete the matching of detector to optics as described in the first reference. Even less data of this nature is available for the SEC vidicon. However, reasonable approximations can be made on the basis of existing image orthicons. The discussion which follows explores the matching of the SEC vidicon to the APS telescope at two possible focal ratios; $f/200$ and $f/100$. In particular, the effect on signal-to-noise ratios in the video signal of various image intensities and exposure times is explored for the two focal ratios ($f/100$ and $f/200$), in this discussion.

Vidicon Response

It seems reasonable to assume that SEC vidicons will be available with 50-percent response at 10 cycles per millimeter. This is in line with the operation of a number of other types of tubes. For example, a standard 5820-image orthicon displays 50-percent response at about 5.5 cycles, a high sensitivity MgO target image orthicon has 50 percent at about 12 cycles, and vidicons go as high as 20 cycles at 50-percent response. Comparing the transfer characteristics for an SEC vidicon with that for diffraction-limited optics operating at $f/200$ shows negligible losses at $5000\text{\AA}$, while at $2000\text{\AA}$, although the losses are greater, there is an increase in limiting spatial frequency. Hence the primary limit on resolution is in the optics, not in the detector, at $f/200$.

However, the actual limiting resolution of the detector is set by the signal-to-noise ratio and the point of intersection of the detectivity curve with the transfer characteristic. Without better data on the response

* Scott, R., et.al.: The Practical Applications of Modulation Transfer Functions. Perkin-Elmer Symposium, March 6, 1963.

** Steiner, I.B.: Study of Far Ultraviolet Recording Media for Space Applications, Perkin-Elmer Report No. 8440, July 15, 1966.

of the SEC vidicon, actual photometric evaluations are difficult. However, by picking high signal and low signal cases, it is possible to compare f/100 and f/200 systems. (See Figures C-1 and C-2). In this high signal requirement, the f/200 operation is definitely preferable to the f/100 operation, although there is not much difference at 5000Å. In the low signal case, it makes no difference whether an f/100 or an f/200 system is used; and, in the very low signal case, an f/100 system would begin to be superior.

Whether or not one is in a strong signal situation depends on tube sensitivity. Assume, as is claimed in some papers, that the SEC vidicon is as sensitive as a 5820-image orthicon. This requires a highlight illumination of 10^{-2} to 10^{-3} foot-candle for full exposure, and the time constant involved is about 0.06 second, so that on the order of between 6×10^{-4} to 6×10^{-5} foot-candle-second is required. Typically, one might expect the surface brightness of a nebulosity (neglecting any nucleus if one existed) to be of the order of 10^{-3} to 10^{-4} foot-lambert, so with an f/200 system, the photocathode illumination is of the order of 10^{-8} to 10^{-9} foot-candle, neglecting losses. One hundred minute exposures then would yield exposures on the order of 6×10^{-5} to 6×10^{-6} foot-candle-second. This is close to minimum required for full exposure, but, if filtering is used, the exposure drops. We may conclude that we will be in either a moderate or a weak signal regime.

Since low contrast targets will be common, the low contrast behavior of the detector needs to be examined. This usually follows approximately a modulation for threshold detection curves similar to one for film. Not having data on the SEC vidicon, the response of a high resolution MgO target tube was calculated from published data. Figure C-3 shows the frequency for 10-percent modulation and the maximum frequency (in this case, plotted in cycles per 25 mm instead of cycles/mm). The shift in the $K_{\max}$ curve is due to its approaching the inherent tube resolution. Note that K_f , the frequency for 10-percent modulation, as well as $K_{\max}$, shift with illumination. At higher light levels and wider bandwidths, the resolution increases appreciably above that given. Note that at less than 10^{-4} foot-candle (at 30 frames/second) K_f becomes equivalent to 8 cycles per millimeter. From the modulation of threshold detection curves, this is a reasonable range of operation. (Note that the SEC vidicon would have to be operated in well over the maximum illumination of 1/30 s with 10^{-4} foot-candle, however.)

We may conclude that at f/200 it is possible to utilize the diffraction-limited operation of the telescope, but that a closer examination must be made to establish, without a doubt, that exposure will be adequate for low-contrast performance.

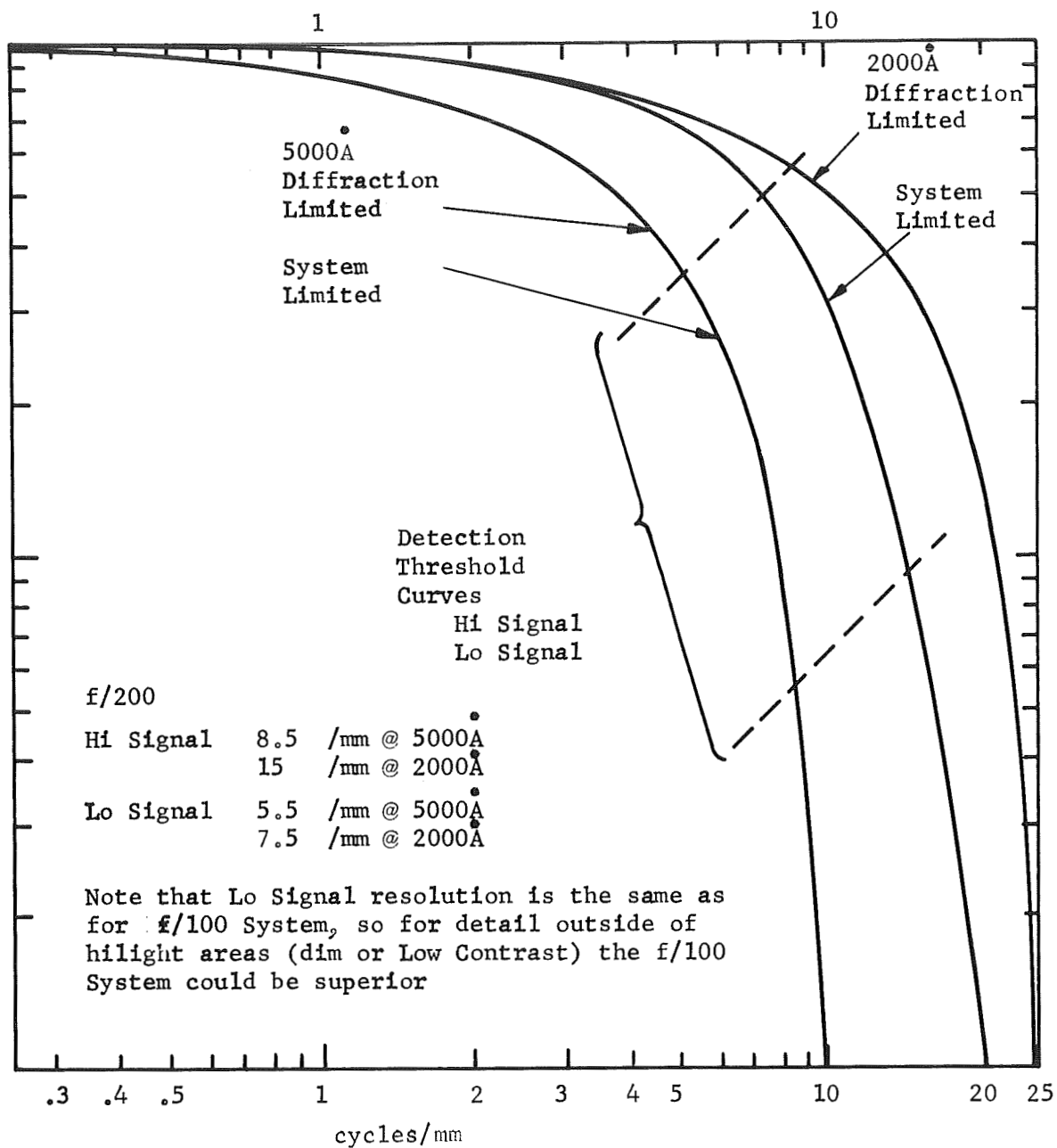


Figure C-1. f/200 Frequency Response

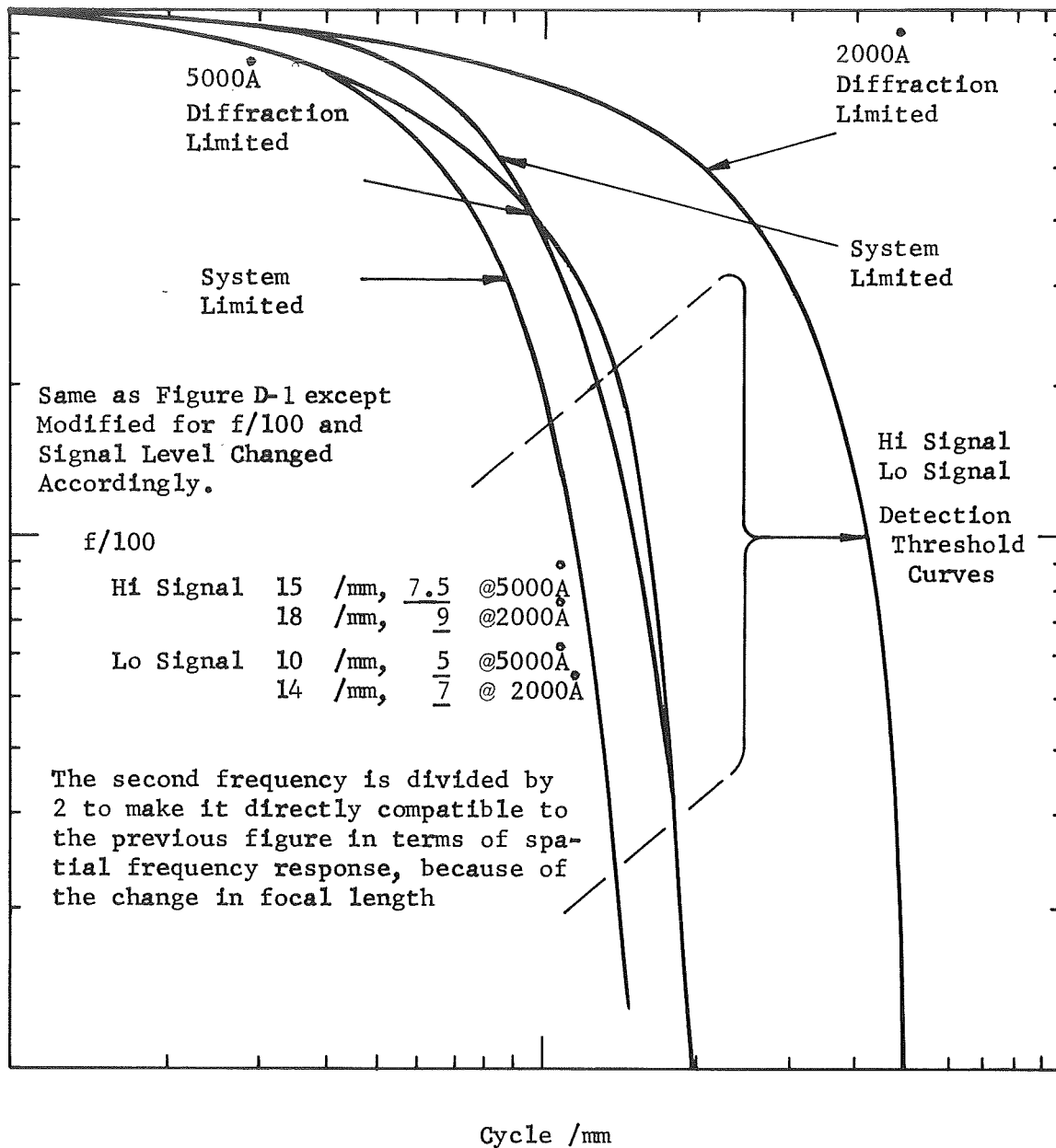


Figure C-2. f/100 Frequency Response

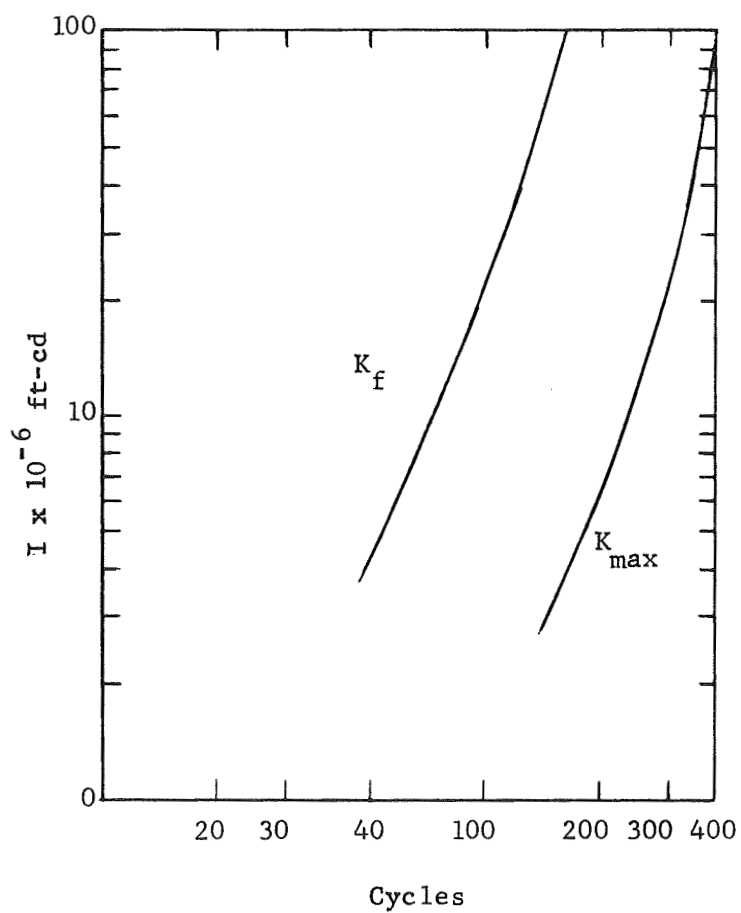


Figure C-3. Low Contrast Behavior K_f at 10 Percent Modulation

APPENDIX D - CURRENT PROBLEMS IN INFRARED ASTRONOMY

INTERSTELLAR LINE ABSORPTION AND EMISSION BY IONS AND MOLECULES

Quite recently, ammonia, water vapor, and formaldehyde have been detected as interstellar absorption lines at microwave frequencies using radio astronomy techniques.* This suggests the possibility of formation of a variety of molecules in interstellar space. The greatest number of molecular lines occurs in the infrared region (due to vibration-rotation transitions) so that a search for interstellar molecules would probably be most fruitful in the infrared. (No compilation of possibly detectable infrared molecular transitions between 1 and 1000 microns has been found, and such a list would be desirable for a future study.)

A variety of ions may exist in H II regions and it has been suggested that an important cooling mechanism for H II regions may be infrared line emission.** Gould[†] gives the following compilation of lines which are most likely to be observed:

<u>ION</u>	<u>WAVELENGTH (Microns)</u>
C ⁺	156.3
N ⁺	203.6
N ⁺²	121.7
N ⁺²	57.31
O ⁺²	88.18
	51.71
O ⁺³	25.87
	12.79
Ne ⁺	15.38
Ne ⁺²	36.10
Ne ⁺⁴	24.15
	14.33
Mg ⁺³	4.492
Si ⁺	34.8
S ⁺²	33.65
	18.681
S ⁺³	10.53

*Editorial Staff: Sky and Telescope. Vol. 37, March 1969, p. 150.

Editorial Staff: Sky and Telescope. Vol. 37, May 1969, p. 271.

**Burbidge, G.R., Gould, R.J., and Pottasch, S.R.: Astrophysics Journal. Vol. 138, 1963, p. 945.

[†]Gould, R.J.: Astrophysics Journal. Vol. 138, 1963, p. 1308.

In addition, molecular hydrogen has a line at 28μ . This would be of great cosmological interest since the only lines of H_2 are in the ultraviolet and infrared regions,* so it is not known with any certainty how much molecular hydrogen there is in the universe.

PLANETARY STUDIES

It has been established that Jupiter and Saturn internally generate more energy than they receive from the sun. This was found from very broadband bolometer measurements.** Considerably more data is needed to understand the mechanism for this. The next step would be narrower band measurements throughout the infrared region which are difficult below the atmosphere because of signal-to-noise problems. Very little can be done on the outer planets Uranus, Neptune, and Pluto, below the atmosphere because they are so cold and lost in the noise of the earth's thermal radiation. Observations of Venus and Mars are barely possible from balloon altitudes† but are still hampered by atmospheric emission, absorption, and contamination from balloon outgassing.††

A continued program of space probes of these planets may render satellite observations superfluous.

THERMAL EMISSION BY GAS CLOUDS (PROTOSTARS) AND HEATED INTERSTELLAR GRAINS

A blackbody at $100^\circ K$ has its peak emission at 30μ . Some protostars have been detected at near-infrared wavelengths, but these would necessarily be relatively warm clouds or large cooler ones. Ability to observe without absorption between 5μ and 500μ would probably add considerably to information on the early evolutionary stages of stars by allowing us to see stars in their beginning stages.

Interstellar dust which is not near a particular star would have temperatures on the order of $7-20^\circ K$ and would appear as a diffuse source with peak emission in the far infrared region.† Observation of such a source is impossible below the atmosphere where the only method of studying interstellar dust is by observing its extinction and reddening in the visible and near infrared region.

* Dieter, N.H., and Goss, W.M.: Review of Modern Physics. Vol. 38, p. 260.

** Aumann, H.H., Gillespie, C.M., and Low, F.J.: Astrophysics Journal Letters. Vol. 157, 1969, p. L69.

† Strong, J.: Applied Optics. Vol. 6, 1967, p. 179.

†† Zander, R., and Bottema, M.: Journal of Geophysical Research. Vol. 72, 1967, p. 5749.

‡ Harwit, M., Houck, J.R., and Fuhrmann, K.: Applied Optics. Vol. 8, 1969, p. 473.

INFRARED STARS

There are stars which have evolved enough to no longer be considered condensing gas clouds but which radiate most of their energy in the infrared region. This happens because they are either still relatively cool stars, or hotter stars with dust clouds around them that radiate strongly in the infrared region. Many of these have been found in the past few years, but there is considerable room for further survey work and detailed spectral studies of individual stars.*

COSMIC BLACKBODY BACKGROUND

The isotopic background radiation of Penzias and Wilson** which has the appearance of a blackbody at 3°K would have its peak emission in the far infrared and short microwave region 2mm. Though the most popular explanation is that it is left over from the primordial fireball, the data is not sufficient to preclude other explanations. For example, Wolfe and Burbidge† maintain that observations at 350 μ are necessary to rule on the possibility that the radiation arises from many small discrete sources. It is possible there are other explanations as well, and data in the far infrared region would provide a much better understanding of the radiation.

RADIATION FROM OUR GALAXY AND OTHER NORMAL AND PROTOGALAXIES

Observations of the center of our galaxy in the far infrared region from a balloon reveal an infrared "excess" possibly attributable to heated dust clouds.†† Further observations are necessary in all of the infrared spectrum. The signal to noise in this work was only 2 or 3 to 1, and this would be greatly improved by satellite observations. Protogalaxies may be barely visible in ground-based observations as an integrated background,‡ but chances of detecting this radiation would be greatly increased above the atmosphere. Information on protogalaxies would add much to our understanding of the evolution of the universe.

* Johnson, H.L: Science. Vol. 157, 1967, p. 635.

** Penzias, A.A., and Wilson, R.W.: Astrophysics Journal. Vol. 142, 1965 p. 419.

† Wolfe, A.M., and Burbidge, G.R.: Astrophysics Journal. Vol. 156, 1969, p. 345.

†† Hoffmann, W.F., and Frederick, C.L.: Astrophysics Journal Letters. Vol. 155, 1969, p. L9.

‡ Partridge, R.B., and Peebles, P.J.E.: Astrophysics Journal. Vol. 148, 1967, p. 377.

QUASARS, PULSARS, AND SEYFERT GALAXIES

There is a growing collection of objects for which there is no or only very rough theory. Some are visible only in the visible or radio, some in both. Observations throughout all of the infrared would provide more information on which to base theory. In particular, in the case of quasi-stellar objects and Seyfert galaxies, it appears that the majority of the radiation is emitted in the middle and far infrared regions*, both of which are inaccessible from the ground. Colgate, among others, has suggested one preliminary theory but states, "The decisive observation requires further infrared monitoring of quasi-stellar objects.**"

*Low, F.J., and Kleinmann, D.E.: Astronomical Journal. Vol. 73, 1968, p. 868.

**Colgate, S.A.: Physics Today. Vol. 22, 1969, p. 27.

APPENDIX E. BRIEF DESCRIPTION OF SCATTERPLATE INTERFEROMETER

A pinhole source of monochromatic light is imaged by a very special projection lens on the test mirror surface. When a large number of fringes is anticipated, a laser light source is used. Between the projection lens and the test mirror is the Burch scatterplate with its center of symmetry on the optical axis. The scatterplate is a surface containing scattering means (indentations or irregularities) arranged in extremely precise pairs about a center of symmetry.

The beam passes through the scatterplate located a distance "S" away from the center of curvature of the mirror. At the scatterplate, a portion of the beam is scattered into a host of divergent bundles, one for each scatterer, and a portion passes straight through. These light paths are depicted in Figure E-1 for one ray emanating from the source.

The mirror will return each bundle to a focus at a point diametrically opposite to the first scatterpoint relative to the center of curvature, point "C". For the sample bundle shown, this point is below and to the right of "C". The light of interest will then continue on in a divergent bundle and pass through the scatterplate undisturbed.

The portion of the original ray that passed straight through the scatterplate is returned by the mirror to the plate at a point diametrically opposite to the original point of entry. If the scatterplate is true, there will be a scattering element at this spot which will produce a second divergent bundle.

Thus, two divergent bundles or cones of light, will be produced from each original ray, with the centers of divergence separated by two times the distance "S" in the figure. The interference pattern produced by two spherical wavefronts with different radii (or displaced centers of curvature) consists of a series of concentric circles. The fringe patterns for all scatterpoint pairs superimpose upon each other.

An example of the resulting interference pattern is shown in Figure E-2. The pattern in Figure E-2 was obtained by placing the center of symmetry of the scatterplate on the axis of the paraboloid, but displaced upward from the center of curvature by about 0.015 meter.

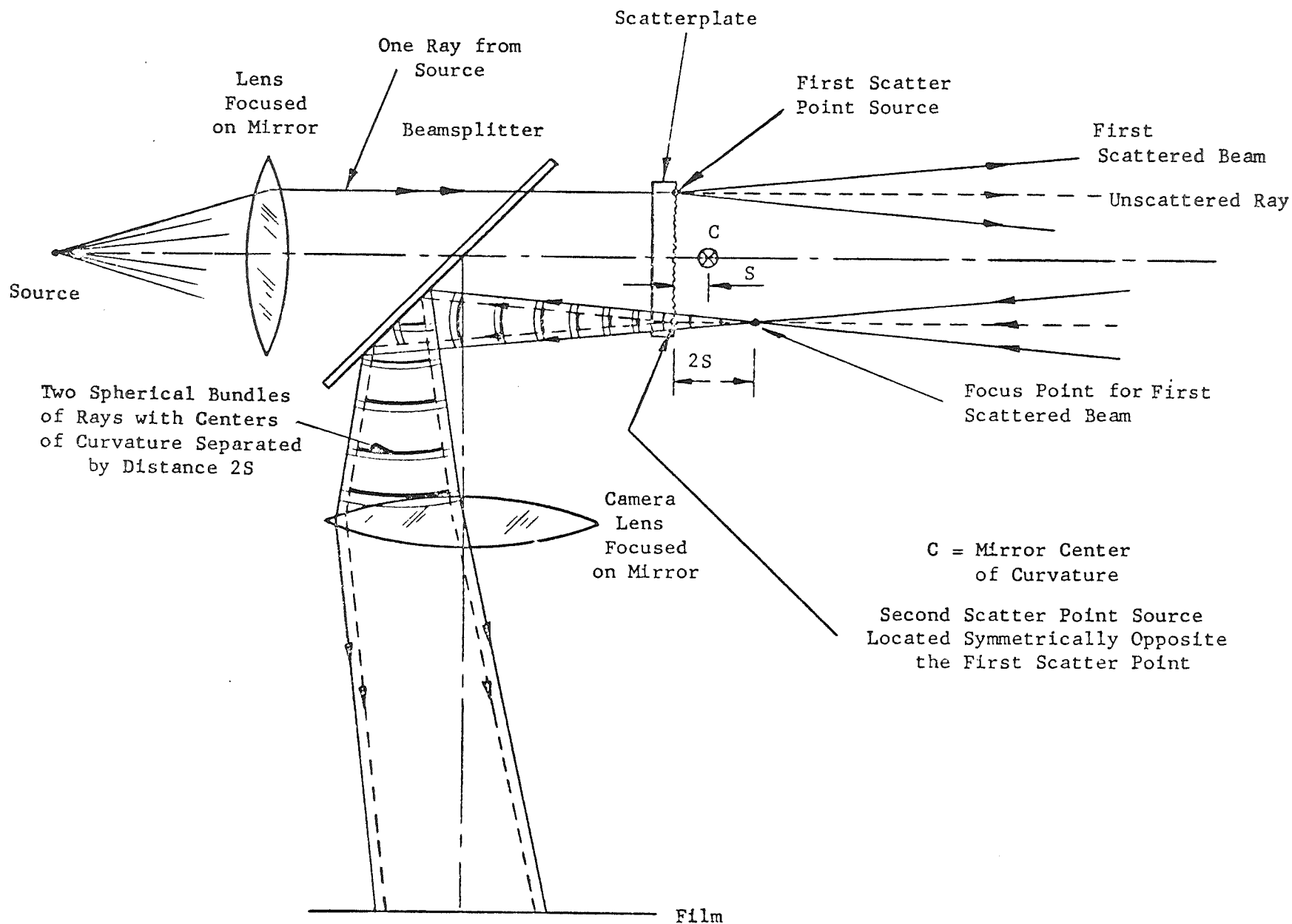


Figure E-1. Scatterplate Interferometer (With Scatterplate Displaced Axially from Center of Curvature)

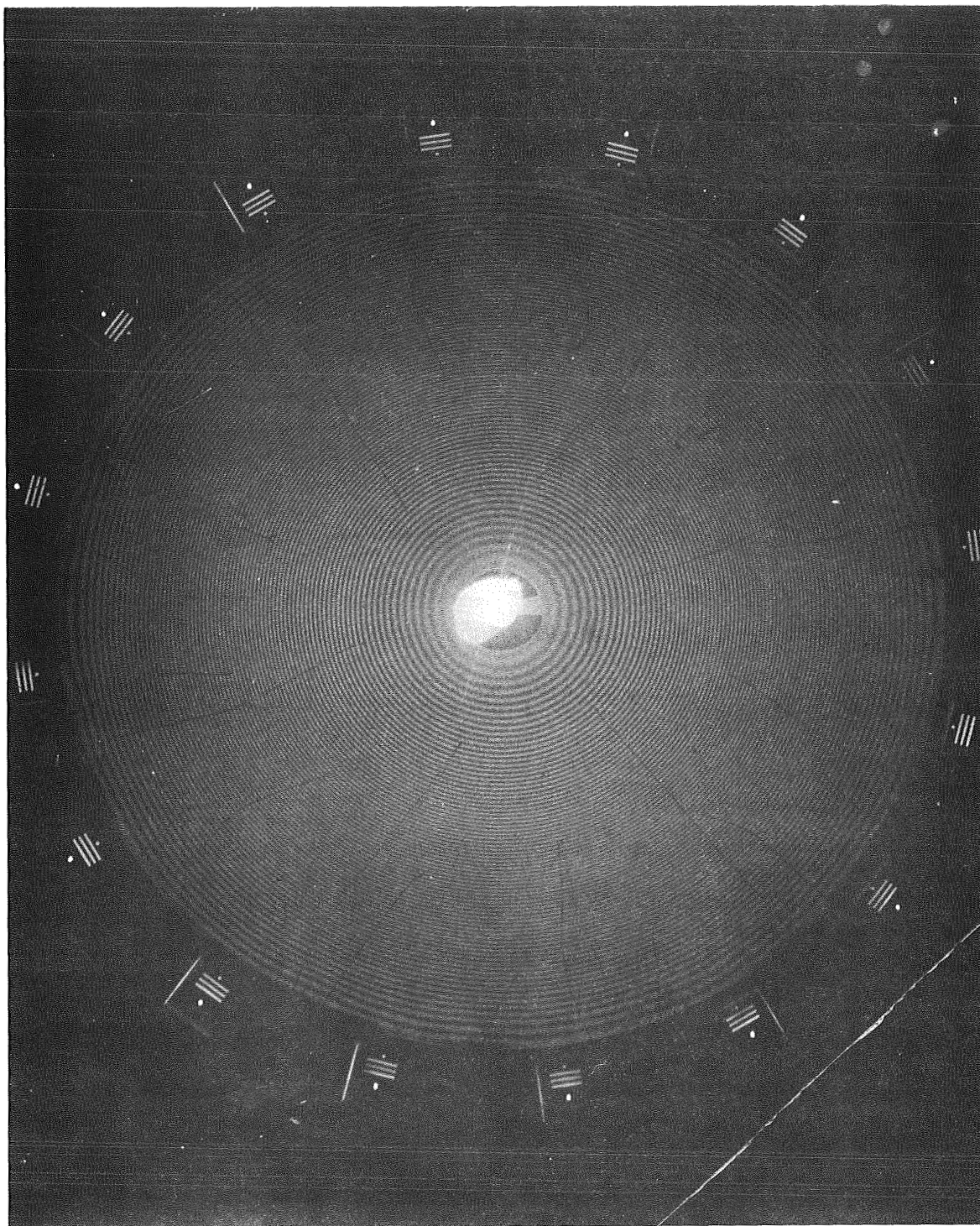


Figure E-2. Interferogram of a Scatterplate

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Key Words _____

APPENDIX F - THE DETECTION OF COMA IN THE IMAGE OF A STAR USING A SLIT AND PHOTOCELL

The apparent profile of a star image scanned by a slit has the same shape as the profile of a line image* having the same orientation as the slit and scanned by the same slit. If the aberrations affecting the image are small, a relatively strong peak will be observed flanked by a pair of subsidiary peaks (side lobes) the order of one tenth the magnitude of the central peak. The dip between the main lobe and a side lobe does not go to zero (Figure 1).

The presence of coma may be detected by the asymmetry of the side lobes. If the slit is oriented to scan in the direction of the comatic flare, one lobe will be higher than it would be in the absence of coma, and the other lower (Figure 2). The difference in magnitude between the two side lobes can be taken as the "signal" which measures the amount of coma. No other elementary aberration will produce this asymmetry and the presence of other aberrations will not affect its magnitude (at least for small aberrations).

As the slit is rotated, the magnitude of the "signal" varies as the cosine of the angle of rotation (at least for small coma) from the direction of the comatic flare. Therefore two orthogonal scans are sufficient to determine both the direction and the amount of coma.

What remains to be shown is the manner in which the signal varies with the amount of coma, the sensitivity of the signal, and the effect of the width of the scanning slit on the signal.

These properties can be readily determined by using Steel's method⁽¹⁾ for obtaining the transfer function in the presence of small amounts of aberration, multiplying by the transfer function of the slit, and then evaluating the inverse transform.

* The image of an infinitesimally fine line.

First consider the general case. We will use reduced values for both the spatial coordinates and the spatial frequency coordinates in the image plane:

$$r = \frac{\rho}{N\lambda} \quad \varrho = \lambda \sqrt{r} \quad (1)$$

$$k = N\lambda \nu,$$

where N is the f/no., λ is the effective wavelength of the light, ρ is the radial distance from the origin* in the image plane, and ν is the spatial frequency in the image plane. The direction of scan with respect to the direction of the comatic flare will be represented by the angle θ .

The transfer function is expressed by

$$T(\theta, k) = |T| e^{i\phi} = |T| \cos \phi + i |T| \sin \phi, \quad (2)$$

where both the modulus $|T|$ and the phase ϕ of the transfer function are functions of the polar coordinates θ and k .

The line spread function is obtained by the inverse Fourier transform

$$S(\theta, r) = \int_{-\infty}^{\infty} T e^{-i2\pi kr} dk \quad (3)$$

The fraction of the total energy of the star image which passes through the slit is given by

$$E(\theta, r; w) = w \int_{-\infty}^{\infty} T e^{-i2\pi kr} \left[\frac{\sin \pi wk}{\pi wk} \right] dk \quad (4)$$

where w is the width of the slit (reduced to the same units as r) and the expression in square brackets is the transfer function of the slit. The quantity E goes to unity as $w \rightarrow \infty$.

Because we are interested in the asymmetry of the image, it is advantageous to separate S into its even and odd parts,

$$S = S_e + S_o, \quad (5)$$

and taking into account the symmetry of the transfer function, we obtain

$$S_e = \int_{-\infty}^{\infty} |T| \cos \phi \cos(2\pi kr) dk \quad (6)$$

and

$$S_o = \int_{-\infty}^{\infty} |T| \sin \phi \sin(2\pi kr) dk.$$

* in this case, the position of the maximum of the central peak.

The corresponding energy fractions are

$$E_e = w \int_{-\infty}^{\infty} |T| \cos \phi \cos(2\pi kr) \left[\frac{\sin \pi wk}{\pi wk} \right] dk$$

and

$$E_o = w \int_{-\infty}^{\infty} |T| \sin \phi \sin(2\pi kr) \left[\frac{\sin \pi wk}{\pi wk} \right] dk. \quad (7)$$

Our asymmetry "signal" at a distance r from the centre of the image is given by

$$s = E_o(\theta, r, w) - E_o(\theta + \pi, r, w) = 2E_o, \quad (8)$$

and our average level at r by

$$\bar{E} = E_e. \quad (9)$$

Assuming that the noise is entirely associated with the light reaching the photo-cell, and is "white", we have for the noise

$$n = K \sqrt{\bar{E}} \quad (10)$$

and the signal-to-noise ratio is given by

$$s/n = \frac{1}{K} (2E_o / \sqrt{E_e}), \quad (11)$$

which is dependent on w , among other things.

Using Steel's approach, we have for the modulus and the phase of the transfer function (for small coma),

$$|T| = T_o - C^2 (L_1 \cos^2 \theta + L_2 \sin^2 \theta)$$

and

$$\phi = [C \cos \theta] \phi_c \quad (12)$$

where T_o is the aberration-free transfer function, C is the amount of coma in wavelength units, L_1 and L_2 are loss functions (functions of K), and ϕ_c is the phase function (also a function of K). We have tabulated values of T_o , L_1 , L_2 , and ϕ_c (and corresponding functions for the elementary aberrations) for annular apertures with aberration ratios running from 0 to 1 in steps of 0.1. For the present problem we will assume an obscuration ratio of 0.3. Figure 3 shows the pertinent functions for this ratio.

It is apparent that ϕ is very small for small coma, say less than 0.1λ , and therefore we can let

$$\begin{aligned} \sin \phi &= \phi = [C \cos \theta] \phi_c \\ \cos \phi &= 1 \end{aligned} \quad (13)$$

Then equations (7) become

$$\begin{aligned} E_e &= a - (b \cos^2 \theta + c \sin^2 \theta) C^2 \\ &= a \left[1 - \left(\frac{b}{a} \cos^2 \theta + \frac{c}{a} \sin^2 \theta \right) C^2 \right] \\ E_o &= dC \cos \theta, \end{aligned} \quad (14)$$

where

$$\begin{aligned} a &= 2w \int_0^1 T_o \cos(2\pi kr) \left[\frac{\sin \pi wk}{\pi wk} \right] dk \\ b &= 2w \int_0^1 L_1 \cos(2\pi kr) \left[\frac{\sin \pi wk}{\pi wk} \right] dk \\ c &= 2w \int_0^1 L_2 \cos(2\pi kr) \left[\frac{\sin \pi wk}{\pi wk} \right] dk \\ d &= 2w \int_0^1 T_o \phi_c \sin(2\pi kr) \left[\frac{\sin \pi wk}{\pi wk} \right] dk. \end{aligned} \quad (15)$$

The terms involving the product of L_1 and L_2 with ϕ_c have C^3 as a coefficient, and for small C can be neglected. In fact, for $C < 0.1$, b and c can also be neglected with small error.

Thus we can find, as a function of slit width,

- (1) The fractional energy of the aberration-free central peak by setting $r = 0$ and evaluating a ;
- (2) The fractional energy of the aberration-free side lobe by setting $r = 1.5$ and evaluating a ;
- (3) The coma detection factor (which when multiplied by $C \cos \theta$ gives the asymmetry "signal") by setting $r = 1.5$ and evaluation $2d$; and
- (4) The signal-to-noise factor by setting $r = 1.5$, evaluation a and $2d$, and taking the ratio of $2d$ to the square root of a .

The results of these calculations are shown in Figure 4 and 5 and in Table I.

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It can be seen that the optimum slit width from signal-to-noise considerations is about 1.2 or about equal to the radius of the Airy disc. With this slit width, close to 70% of the total light is the image passes through the slit when it is centered on the central peak, and close to 8% of the total light when it is centered on a side lobe. An asymmetry "signal" of 3.5% of the total light (5% of the peak value) corresponds to about $1/10\lambda$ of coma, and is linearly proportional to it.

"Signal" = difference in magnitude of the two side lobes

Roland V. Shack

R. V. Shack

RVS/mjf

Reference

1. Etude des Effets Combinés des Aberrations et d'une Obturation Centrale DE la Pupille sur le Contraste des Images Optiques, W.H. Steel, Revue d'Optique 32, pp. 162-173 (1953).

TABLE I

SLIT WIDTH (1)	SLIT WIDTH EFFECTS					actual slit width in 5μ light ↓ in 1μ light			
	0	.2	.4	.6	.8	1.0	1.2	1.4	1.6
CENTRAL PEAK (2)	0	.151	.293	.420	.527	.612	.675	.715	.746
SIDE LOBE (2)	0	.018	.033	.046	.057	.067	.076	.088	.102
COMA DETECTION FACTOR (3)	0	.087	.168	.236	.288	.326	.349	.358	.363
SIGNAL-TO-NOISE FACTOR (4)	0	.329	.460	.549	.600	.629	.633	.605	.568

- (1) Multiply by $N\lambda$ to get actual slit width.
- (2) Fraction passing through slit of total energy of aberration-free star image. There is no significant change for coma less than $1/10\lambda$.
- (3) Multiply by $C \cos\theta$ (C = coma in wavelength units, θ = orientation angle) to obtain coma detection "signal", i.e., difference between side lobes expressed as fraction of total energy of star image.
- (4) Multiply by $1/K$ to get signal-to noise ratio, where K is the ratio of the observed noise to the square root of the observed signal for the total image ("infinitely" wide slit) or any fraction thereof (e.g., the central peak signal with the operating slit width).

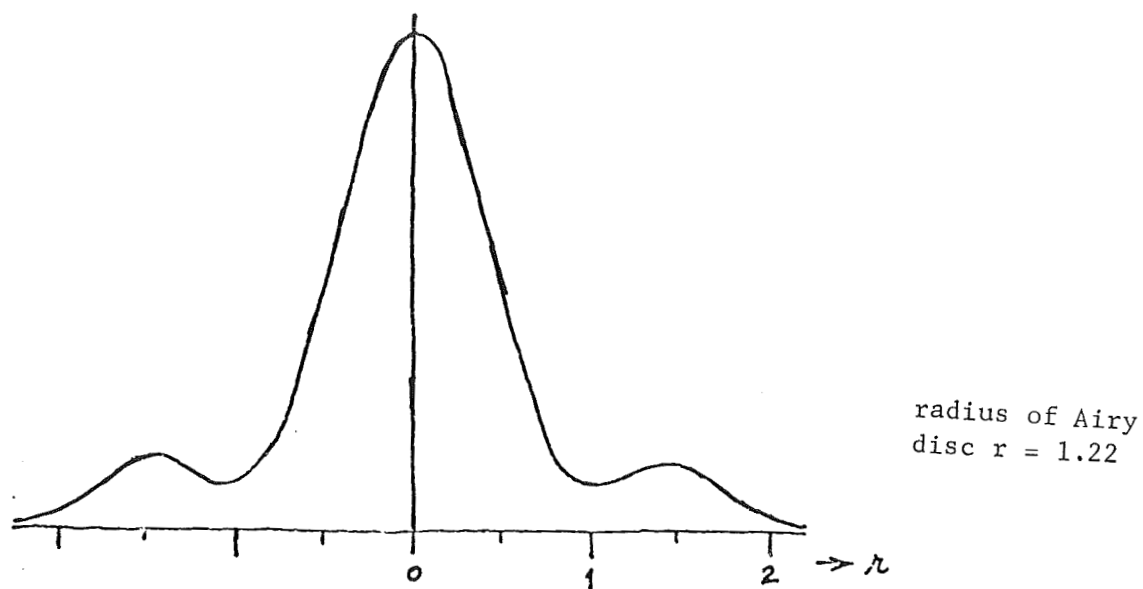


Figure 1. Line Image Profile (Line Spread Function)
(No Aberration, 33% Obscuration)

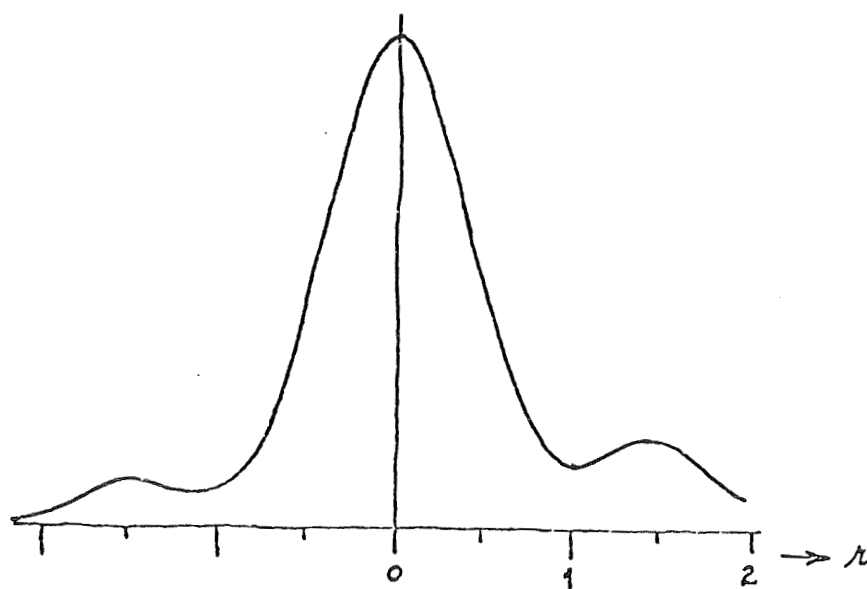
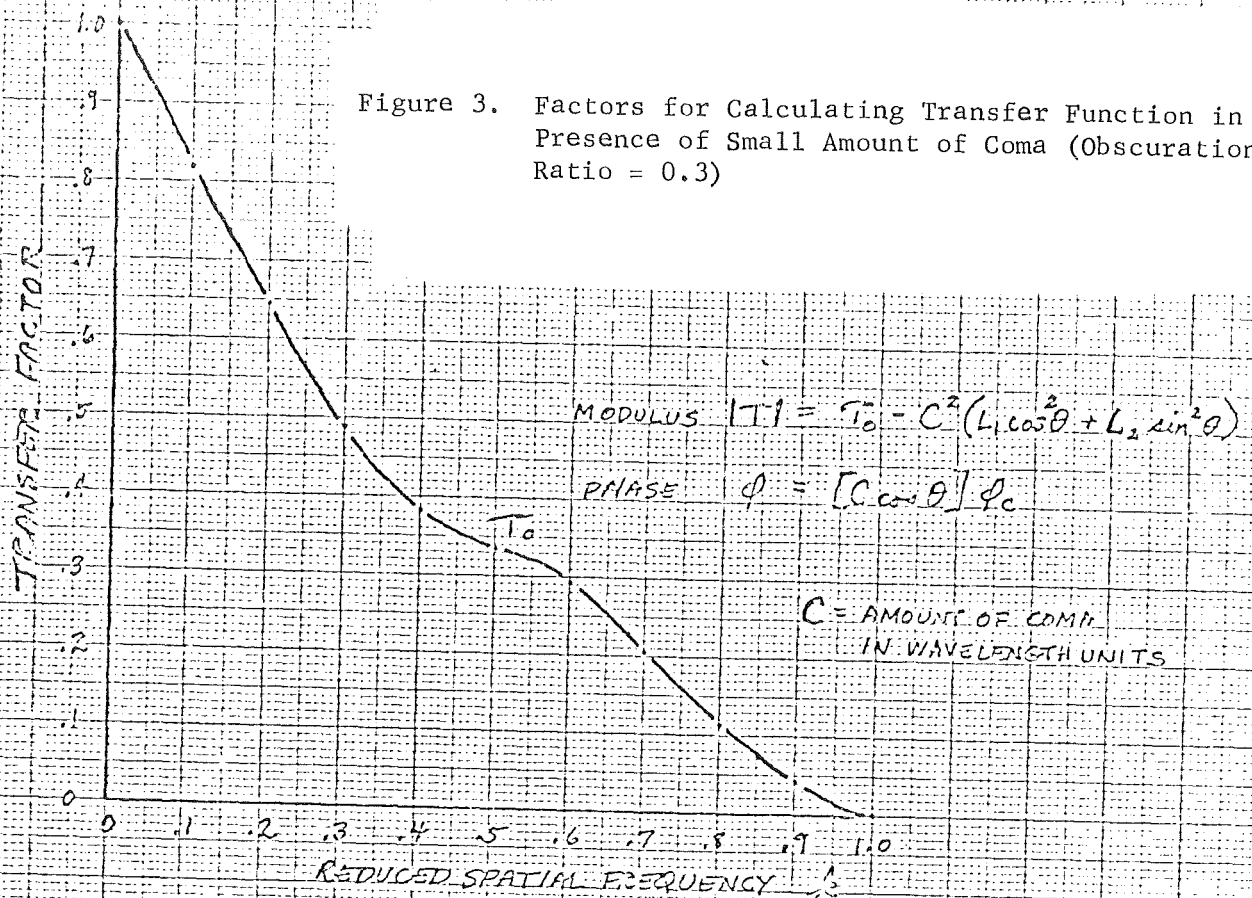


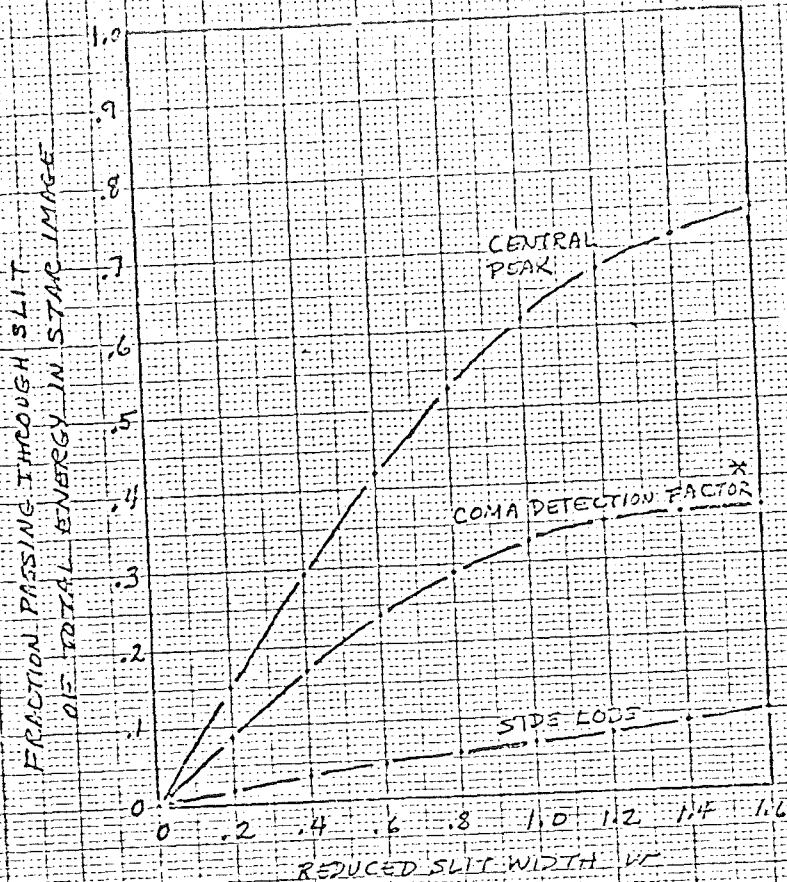
Figure 2. Line Image Profile in Presence of Small
Amount of Coma (33% Obscuration)

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Figure 3. Factors for Calculating Transfer Function in Presence of Small Amount of Coma (Obscuration Ratio = 0.3)



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