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Thin Film Strain Transducer

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James L. Rand

March 1981

NASA
National Aeronautics and
Space Administration

Wallops Flight Center
Wallops Island, Virginia 23337
AC 804 824-3411

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Thin Film Strain Transducer

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Prepared Under Contract No. NAS6-3077

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TABLE OF CONTENTS

LIST OF ILLUSTRATIONS

LIST OF TABLES

I. INTRODUCTION

Scientific balloon platforms have been developed over the years to carry payloads to the upper limits of the atmosphere to observe and record a variety of phenomena. Both design and analysis techniques, as well as film quality, have steadily been improved so that balloon flights may be performed on a relatively routine basis. However, scientists continue to demand systems to carry heavier payloads to higher altitudes for longer durations. This requires the manufacturer to produce balloons which test the limits of the state-of-the-art of film production and design practice. This fact is quite apparent from the success rate when reviewed as a function of payload. The degraded reliability of heavy lift systems has led NASA to impose a moratorium on flights with payloads in excess of 3500 pounds. Although this moratorium was subsequently lifted, it points out the need for a more precise approach to balloon design and manufacturing. This can be accomplished by taking the results of various research efforts in the past and bringing them to bear on the problem.

The most widely used system today is the natural-shaped balloon, designed to have zero circumferential stress, and made of thin, balanced, polyethylene film. Rotationally symmetric shapes are generated by assuming the absence of any discontinuities such as seams and load tapes. The shape computed in this manner is that of the float configuration. Since the material is assumed to be inextensible, the manufactured shape is identical to the computed float configuration. In addition, the state of stress in the balloon wall is not considered a significant parameter in the design process although it is approximated in determining the required thickness of caps which are added to the basic design shape.

Recently, Rand [1] has published a technique for analyzing the state of stress in a fully deployed balloon by assuming the materials to be linearly elastic and taking into account a compatible state of strain between load tapes and film in the meridional direction. In this analysis, a load transfer mechanism is postulated which allows the film to lobe out between the load tapes and transfer loads back and forth between tape and film by virtue of a shear stress. Although the stress is computed quantitatively in this manner for any particular balloon gore pattern, certain qualitative results are worth noting. In particular:

- The state of stress in the balloon wall is biaxial instead of uniaxial as previously assumed. The ratio of circumferential to meridional stress may vary from zero (uniaxial meridional stress) at both ends of each gore to greater than one between the maximum radius and the apex. This would imply that circular diaphragm, racetrack and uniaxial testing are only directly applicable at a few widely separated points on a typical balloon.

- Forces carried by the load tapes are not constant but are transferred to film by means of a shear stress. This implies that the load tapes may go slack at some point along the gore. This feature has been observed by several investigators in actual flight operations.

- The distribution of forces between film and tape is dependent on the surface temperature of the balloon due to the influence of material properties. Since the tapes appear to have less sensitivity to temperature than the film, any significant change in temperature such as would occur at sunset or sunrise will cause a significant load redistribution.

Although the quantitative values of stress computed are not of sufficient magnitude in themselves to cause failure of the balloon film based on existing material data, they are sufficiently high to cause cracks to propagate and provide a state conducive to amplification in the presence of any flaws or manufacturing defects.

Flight testing has been used successfully in the past to obtain data on atmospheric properties, gas and skin temperatures, as well as radiant flux and pressure measurements. However, attempts to measure film or tape stress and strain have not been completely successful. The hostile environment is not only dynamic but the system must experience a variety of heat transfer mechanisms which alter not only the balloon temperature but the sensor temperature as well. The films are so thin that the presence of a sensor usually results in a localized stiffening of the film which alters the measurement. As a result, large gage lengths are used on very stiff materials such as polyester films with only moderate success. There is a very definite need to develop a strain gage suitable for flight which may be used to verify or negate the assumptions contained in analysis techniques and material models previously discussed. In addition, the need has been established for an analytic characterization of the thin polyethylene film used in the fabrication of scientific balloon platforms. This type of description of the mechanical response of the material is necessary if stresses are to be deduced from in-flight strain measurements.

In order to support the efforts of the National Aeronautics and Space Administration to determine the state of stress in high altitude balloons by flight testing, the Southwest Research Institute is conducting a research program for the purpose of understanding the thin film and developing a method to measure its response. The purpose of this report is to document the development of a unique sensor capable of monitoring the strain in the wall of a typical balloon when exposed to the hostile environment of ascent and float.

II. LITERATURE REVIEW

The need to measure either stress or strain in thin balloon films has been recognized for many years. A variety of very unique devices have been designed and developed; but, to date, successful flight measurements have not been reported.

One of the most unique devices appearing in the literature is the "film stress transducer" developed for the Air Force Cambridge Research Laboratory [2]. This is the only transducer reported which attempts to measure stress directly rather than strain. The device consists of a narrow plate which is attached to the film in such a way that it behaves as a rigid body. When displaced from its equilibrium position and released, the plate would oscillate at a frequency governed by the stress in the film normal to the axis of the plate. The device was calibrated at room temperature and demonstrated that the relationship between frequency and film stress was parabolic as expected. A number of these transducers were fabricated for hangar inflation tests [3]. Unfortunately, the transducer was nonlinear, sensitive to orientation, and subject to electrical "noise" which could mask the data. Two events were necessary to measure the film stress: the "rigid plate" had to be excited somehow and the subsequent natural frequency of oscillation then measured. Excitation was attempted electrically with a magnetic coil and the subsequent motion monitored with strain gages mounted on thin brass reeds. The electrical signal which was intended to cause the plate to oscillate, also caused an electrical signal to be generated on the gages intended to monitor the subsequent motion. This problem was eliminated by activating the striker shown in Figure 1 using a nylon line. It was found that the stress measured by this technique was dependent on the sharpness and force of the pull on the line. By carefully controlling the force on the line used to excite the striker, the film stress could be measured to within 5 percent at room temperature over a narrow range of stresses.

This device was used during hangar inflations of a small balloon fabricated of 2-mil polyethylene film. No data has been found for this device other than calibration data. Although the concept is unique, it was not developed sufficiently to provide useful in-flight stress measurements. The principal on which the device is based appears to have a number of anomolies which would require additional research to understand even the cause of the various measuring difficulties.

An investigation of the feasibility of using low modulus strain gages for stress analysis of balloon structures was commissioned by the National Center for Atmospheric Research (NCAR). The results of this investigation were reported by Hauser [4] and indicated that strain could be measured up to 50 percent at temperatures from +25°C to -80°C. Gages were made of silver or aluminum deposited chemically or by vacuum metalizing onto a plastic film substrate. This substrate could then be bonded to polyethylene film and the film strain measured. Unfortunately, Hauser

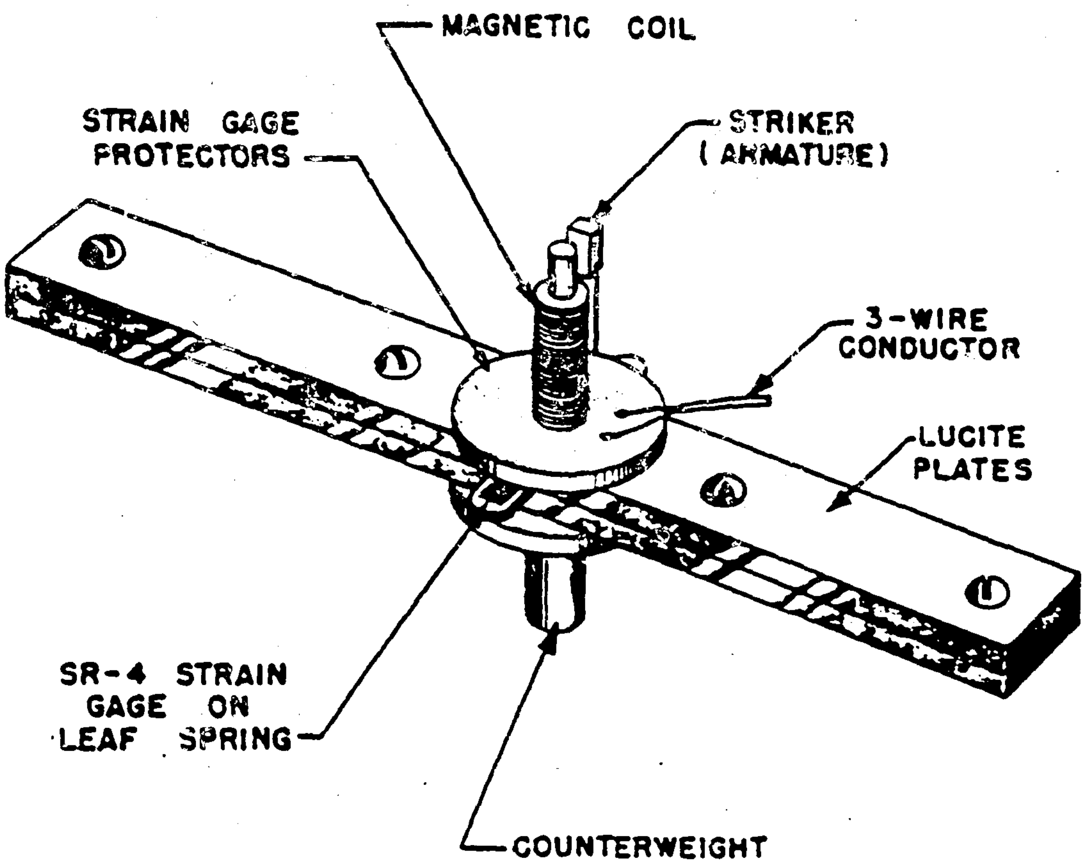

Figure 1. Film Stress Transducer

concluded his investigation by noting that the low modulus strain gages were not yet "optimized" in terms of fabrication, application and calibration.

In a subsequent presentation of this concept to the Western Regional Strain Gage Committee [5], it was pointed out that the substrate would increase the stiffness of the balloon film "by perhaps 75 percent" at room temperature. In addition, the gage factors reported for various gages were a function of not only temperature but the strain level itself. No attempt was made to separate resistance changes due to temperature from resistance changes due to strain. There is no record of any further development of this type of strain sensing device.

Another approach to the measurement of film strain was reported by Stefan [6] of the National Scientific Balloon Facility (NSBF). In this technique, a Mylar strip is masked in a sawtooth pattern and passes through a slot in the head which carries a light source and a photoelectric cell. The end of the tape and the head are anchored to the balloon film about 50 mm apart. As the film elongates, the tape is pulled through the cell and the sawtooth masking causes a variation in the photocell output. The power leads and signals run through fine wires to an amplifier fastened to the load tape and the amplified signal is then carried along the load tape to the telemetry package. This approach was attempted; however, the sensor was reported to be troublesome in field operations. "... the lights burn out, output is extremely voltage-sensitive, and power consumption is fairly high..." In addition to the difficulties reported with this optical device, the inability to measure compressive strain is a notable limitation.

In addition to the techniques already mentioned, a review of the literature suggested other possibilities such as the use of piezoelectric films and magnetic devices such as Hall-effect probes. Each of these approaches were explored and subsequently eliminated as viable solutions to the problem. In the case of piezoelectric films, a charge is generated as a function of either stress or strain depending on polarization. Unfortunately, this is a dynamic effect and no charge is produced for a slowly changing stress or strain. The Hall-effect probes available are somewhat temperature sensitive and would require the use of exceptionally strong magnets and thermal compensation.

The results of this literature review indicate that the need has been recognized and many attempts have been made to measure balloon film stress or strain in-flight for over twenty years. Each attempt has been thwarted by a variety of electrical, mechanical or optical problems. However, each attempt has added to the body of knowledge so that a gage may now be possible which avoids each of the problems experienced in the past.

III. GAGE DEVELOPMENT

A gage has been developed which is suitable for in-flight measurements of balloon film strain. In order to design this device in a manner which would avoid the problems reported in the preceding section, a set of design conditions had to be established which were both necessary and reasonable. Although the stress transducer developed for AFCRL is interesting, it was decided to attempt to measure strain or displacement rather than the indirect force measurement. With this in mind, the following design conditions were established:

1) Low Modulus - The sensor will be attached to thin polyethylene film. If a force is required to deform the sensor as the film strains, that force must be small relative to the force required to displace the film. For design purposes, it is assumed that the film is linearly elastic with a Young's modulus of 0.0689 GPa (10,000 psi) at room temperature and 0.689 GPa (100,000 psi) at low temperatures.

2) Temperature Sensitivity - The sensor will be required to function from relatively warm temperatures (+25°C) as might be experienced during an afternoon launch to the lowest temperatures (-80°C) anticipated during ascent through the tropopause.

3) Directional - Since strain is a tensor by definition, it has certain directional properties. The gage developed must be sensitive to strains or relative deformations in a given direction.

4) Film Reinforcement - The attachment of the gage to the film will create a local discontinuity in both film stress and strain. The influence of this discontinuity should be minimized.

5) In-Plane Forces - The state of stress in a typical balloon will vary in direction depending on the position along the gore and the point in the flight trajectory. The gage developed should be insensitive to all forces in directions other than the direction of the strain to be measured.

6) Strain Sensitivity - Although polyethylene films are capable of 400 percent strain to failure at room temperature, strains less than a few percent are anticipated during a typical flight. The sensor should be capable of measuring 10 percent tensile and 5 percent compressive strain during flight.

7) Linearity - The strain gage should respond in a linear manner with strain regardless of supply voltage or temperature. In addition, the sensor must exhibit no hysteresis or time dependence so that cyclic strain data may be obtained for at least one day.

8) Compatibility - The sensor should be both mechanically and electrically compatible with existing balloon systems. Electrical power and output signals should be readily adaptable for current telemetry packages.

The gage should have passive thermal characteristics which will prevent damage to the balloon film. The geometric design should be such as to prevent damage to the film during violent maneuvers such as launch and ascent.

Each of these design conditions must be satisfied if a successful strain measurement is to be expected. Current balloons are typically fabricated of polyethylene film having a 12.7 micron (0.5 mil) thickness. Any sensor that meets the requirements of such a thin balloon should be usable on thicker films, stiffer materials such as polyester film and fabrics such as nylon, rayon and other balloon or parachute materials.

The design of this sensor has evolved from the inability of each preceding design to meet a particular requirement. The original concept was based on satisfying the first two conditions of low modulus and temperature sensitivity. The film elongation was to cause the bending of a very flexible beam to which foil strain gages were bonded. The use of four active gages in a Wheatstone bridge arrangement would provide automatic temperature compensation and maximum electrical amplification. The beam could be imagined as a strip of thin shim stock rolled into a semicircular shape. The ends of the strip were attached to the balloon and separated by the gage length. The flexibility of the beam and, therefore, the modulus of the sensor could be reduced by either increasing the radius or reducing the thickness of the shim stock.

A number of transducers were fabricated on the basis of this bending principal, calibrated and tested both at room temperature and reduced temperatures. These tests were quite useful in establishing the proper beam stiffness. These transducers were fabricated from 127 micron (5 mil) stainless steel shim stock and attached to 12.7 micron (0.5 mil) polyethylene film. Tensile tests with and without the sensor attached indicated a 2 percent interference of the sensor with the film. However, two conditions were observed which demanded a redesign of the sensor prior to further development. The first condition was a long term drift in the output signal when the transducer was displaced for some time. The problem was ultimately traced to hysteresis in the stainless steel at relatively low stress levels. This would ultimately require the selection of a material with a high proportional limit rather than a high yield stress.

The second condition was the observation that large output signals were generated when relatively small forces were applied in the transverse direction. These signals were caused by twisting of the sensing element when side forces were applied. Although this does not represent a problem in the laboratory, it would render the sensor useless for flight testing where the direction of the load cannot be controlled. Therefore, the sensitivity of this design to in-plane forces in the transverse directions demanded a new transducer configuration.

The problem of transverse load sensitivity was addressed in several ways. The semicircular beam was rotated 90° onto its side which had the effect of changing the reaction to transverse loads from torsion to bending.

In this case, the bending would cause an inflection point at the center of the span where the strain is measured. In the event of asymmetric transverse loads, a symmetric gage could be used to electrically compensate for any inadvertent strain. Therefore, the final gage configuration is in the form of a thin narrow strip of metal, formed into a circular section and anchored to the balloon film so that the plane containing the circle is parallel to the surface of the film. The basic gage configuration has been dictated by the requirements for low modulus, temperature sensitivity and an ability to operate in a biaxial stress field. In the following section, the detail design procedure will be outlined so that the sensor may be sized and its response predicted.

IV. THIN FILM STRAIN TRANSDUCER

In order to assess the influence of the various geometric and material parameters on the overall gage response, it was necessary to develop the governing equations relating forces and displacements to stresses and strains in the sensor. Due to the nonlinear geometry, energy methods were employed to obtain these relationships. A complete derivation of these equations is contained in the appendix to this report and only the results of the analysis are presented here.

As the balloon film is strained, ε_f, the transducer ring will be displaced an amount q. Assuming the film strain is constant between the points of attachment to the ring, the relationship between these variables will be given by:

$$\varepsilon_f = \frac{q}{2R} \tag{1}$$

The force, P, required to displace the ring through this displacement is derived in the appendix and given by:

$$q = \frac{PR^3}{EI}\left(\frac{\pi}{4} - \frac{2}{\pi}\right) \tag{2}$$

Here, EI is the bending rigidity or stiffness of the strip forming the ring. For the purposes of sizing the transducer, the force must be small relative to the force required to produce the film strain in order to satisfy the first design condition.

The ring is anchored to the film by a tab with finite dimensions. This tab serves to collect the necessary force required to deform the ring. In order to utilize the transducer to measure strain in the laboratory where 25.4 mm (1 inch) wide film specimens are routinely used, the tab width was arbitrarily selected to be 25.4 mm. Assuming the stress is uniaxial, the film force may be estimated to be:

$$P_f = E_f \, w \, t_f \, \varepsilon_f \tag{3}$$

Equations (1) and (2) may be combined with equation (3) to obtain the ratio of transducer force to film force. When this is accomplished, an expression for the transducer properties may be obtained as a function of film properties and desired load ratio. This expression is given by:

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$$\frac{2EI}{R^2} = \left(\frac{\pi}{4} - \frac{2}{\pi}\right)\left(\frac{P}{P_f}\right) E_f \; w \; t_f \tag{4}$$

An additional constraint on sizing the transducer is associated with the desire to avoid hysteresis and maintain linearity over the entire range of measurement. Therefore, the maximum stress experienced by the ring must be limited to the proportional limit of the material and, in fact, should be reduced by some appropriate Factor of Safety. This factor should be greater than 1.5 but less than 5 to minimize weight. The maximum stress has been derived in the appendix and occurs at the point of application of the force to the ring. The magnitude of this stress is given by:

$$\sigma = \frac{E \, t \, q}{4R^2\left(\frac{\pi^2}{8} - 1\right)} \tag{5}$$

The transducer is to be designed for a maximum tensile strain of 10 percent which will allow the displacement in Equation (5) to be expressed in terms of the radius, R. The width, b, of the ring should be as small as possible and still accommodate standard precision strain gages. Therefore, a width of 10 mm is assumed for the ring since a standard gage is 6.5 mm wide. The area moment of inertia of the ring may then be expressed in terms of the thickness only:

$$I = \frac{bt^3}{12} \tag{6}$$

The actual material properties may be eliminated at this point by combining Equations (4), (5) and (6) so as to eliminate the modulus of elasticity. The transducer thickness may be expressed as a function of radius for assumed values of stress and P/P_f. In this analysis, the ratio of transducer load to film force will be assumed to vary from one to four percent when applied to a 12.7 micron polyethylene film at room temperature. The results of this analysis are presented in Figure 2 for stress levels of 50 MPa (7,250 psi) and 100 MPa (14,500 psi) since these levels are within the capabilities of most structural materials.

The results presented in this figure are very useful in estimating the size of the transducer. In order to relieve the choice of material, the design should be within the boundaries of the solid lines so that the stresses and modulus will be acceptably low. The radius of the ring will be related to the gage length of the transducer. If the anchor points are at the edge of the ring, then the gage length will be equal to the diameter.

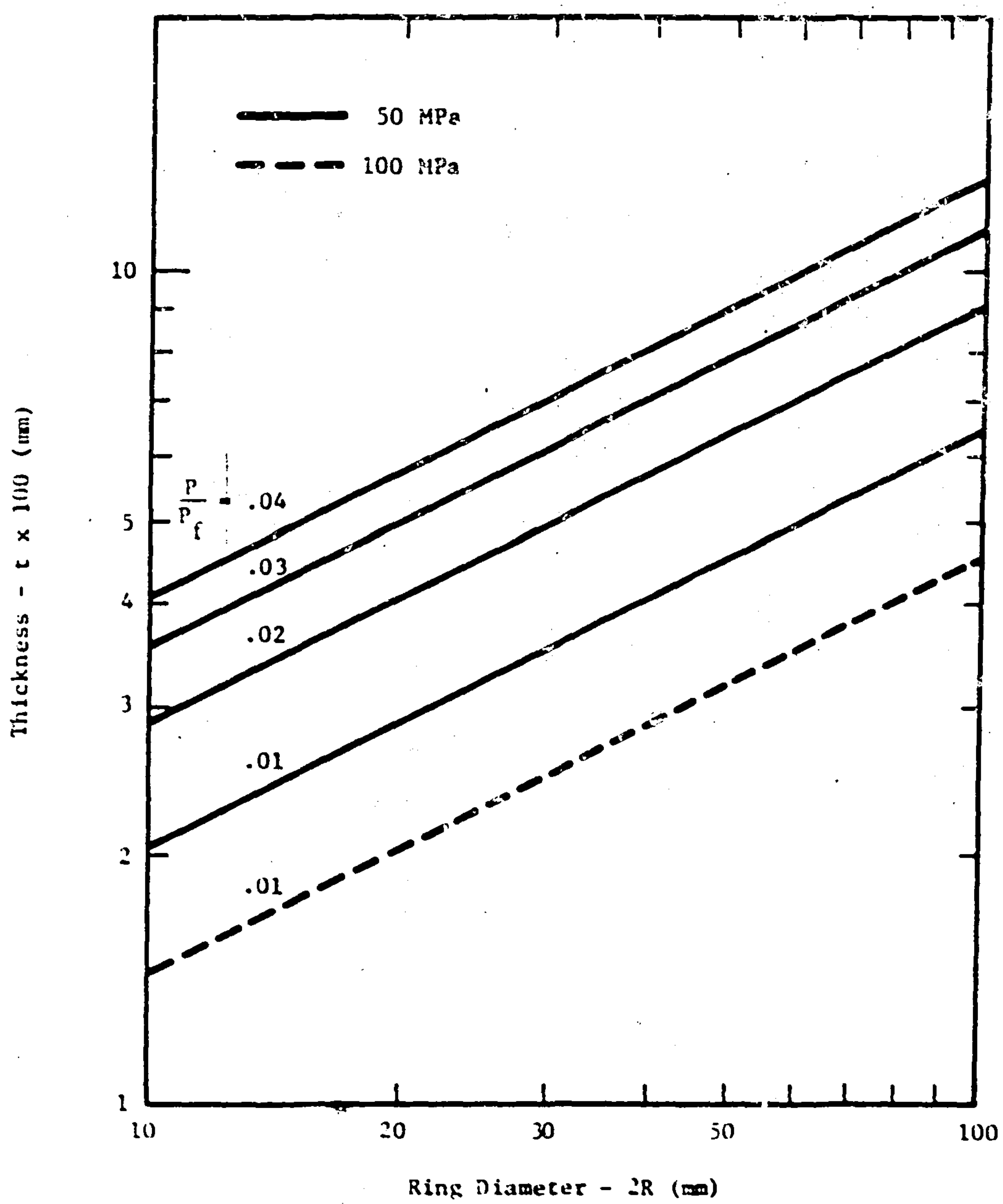


Figure 2. Transducer Design Parameters

In order to size the gage length, the primary concern is the minimization of the influence of the attachment to the balloon.

It has been previously noted that the tab was assumed to be 25.4 mm in width. A preliminary design of the tab was triangular in shape with the vertex away from the gage length. In order to assess the influence of the gage tab, the ANSYS program, a large finite element code which is routinely used to analyze the stress state in complex geometries, was exercised. The film was assumed to be linearly elastic and in a state of plane stress. The triangular tabs were modeled as a rigid inclusion separated by 50.8 mm (2 inches) which is an ASTM standard gage length. The film was assumed to have large lateral dimensions relative to the tab which is consistent with the situation to be encountered in flight. A unit stress was applied in the direction of the vertex and the stresses computed at all points around the tab. Anticipating large stress gradients near the tab, a fine mesh was used in this region and a course mesh at distant points in the film. The post processor of the ANSYS code is able to generate a graphical output of any of the many quantities computed. Shown in Figure 3 are lines of constant octahedral stress. This quantity is typically used since it corresponds to the Von Mises failure criteria and correlates well with the yielding of most structural materials. The rigid displacement of the tab was computed to be within 4 percent of the displacement that would have occurred in the absence of the tab. This accuracy could be improved to some extent by increasing the distance between the tabs.

The analysis of this tab configuration was quite revealing since it indicates a relatively large stress concentration in the film at the vertex of the tab. A 70 percent increase in the applied stress was computed which is sufficient to cause gross film deformation and possibly failure of the film at low temperatures. Therefore, other tab configurations were considered in an attempt to reduce the high stress concentration. The triangular configuration was reversed in direction so that the gage length occurred between the two vertices. The analysis was conducted as before and the pertinent results of this computation are given in Table 1. Unfortunately, another large stress concentration was found in the film at the vertex of the tab. In addition, the high stresses in the region of the gage length resulted in a rigid body displacement of the tab such that the apparent strain would be in excess of 34 percent greater than the strain in the absence of the tabs.

A third tab configuration was analyzed which utilized a 2:1 elliptic geometry. In this case, the maximum film stress concentration was reduced to less than 24 percent; however, the apparent strain was increased by 17 percent. Based on these three analyses, the effects of the various geometry changes were apparent and led to the final tab configuration. In order to minimize the influence of the tab on the apparent strain, the edge bounding the gage length should be straight. In addition, the gage length may be increased to further reduce this effect. Therefore, the distance between the tab edges was increased to 76 mm (3 inches) so that the apparent strain will be within 3 percent of the film strain in the absence of the gage. In order to minimize the stress concentrations, the outer edge of the

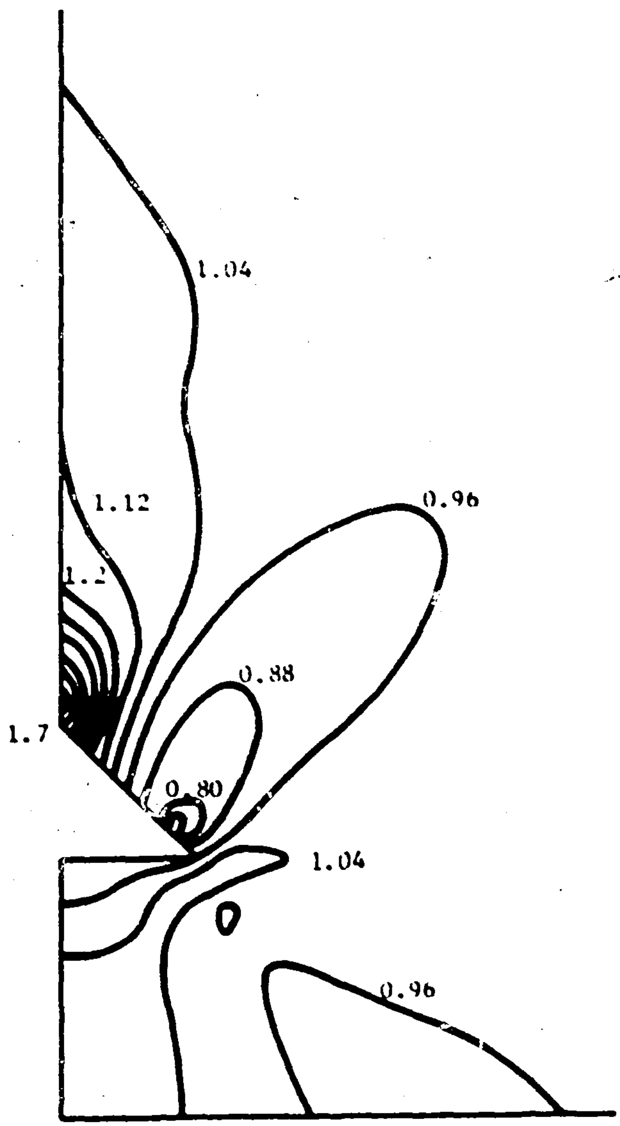
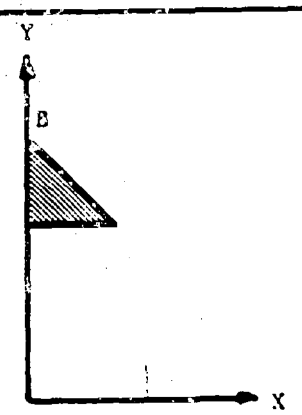
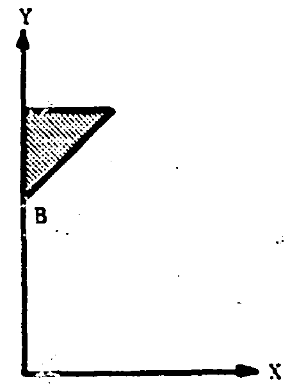
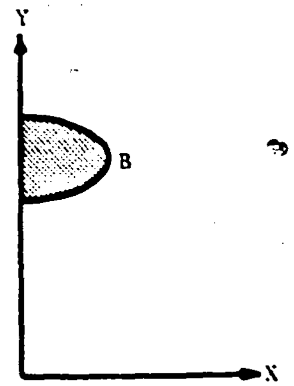


Figure 3. Film Octahedral Stress Distribution

Table 1. Stress Distribution Around Gage Tab

Tab Configuration	$\frac{v \text{ with Tab}}{v \text{ w/o Tab}}$	$\frac{\sigma_{max}}{\sigma \text{ w/o Tab}}$	$\frac{\sigma \text{ Between Tabs}}{\sigma \text{ w/o Tab}}$
	1.039	1.702 @ B	1.04 - 1.12
	1.345	1.714 @ B	1.04 - 1.7[illegible]4
	1.17	1.239 @ B	1.025 - 1.20

tab should have a generous curvature. Therefore, a semi-circular shape was selected for this boundary with generously rounded corners.

The final step in the design of this transducer is the establishment of the thickness of the strip from which the gage is to be formed. This decision is based somewhat on the material selected for the transducer. Recognizing the need for a material with low hysteresis and a high proportional limit, various commercially available spring materials were considered. After some consultation with various members of the staff with experience in the fabrication of instrumentation, a copper alloy was selected for its remarkably linear behavior, high endurance and strength limits, thermal compatibility with precision strain gages, availability in small thicknesses and ease of formability. The particular alloy selected contains 1.80 - 2.00 percent beryllium and is described by a variety of specifications (ASTM B-194-72, QQ-C-533, AMS 4503B, and ASM 4532A). The particular material used is produced by Brush Wellman, Inc. and is identified as Alloy 25. The various physical, mechanical and electrical properties of this alloy are given in Table 2. The beryllium copper strip was found to be available in coil form from suppliers in standard thicknesses of 0.0635 mm (2.5 mil), 0.0889 mm (3.5 mil) and 0.1143 mm (4.5 mil). In order to have a gage length of 76 mm, the ring radius should be 38 mm. The results presented in Figure 2 indicate that the thinnest material available would create unacceptably high stresses in the transducer and the thickest material considered would create an unacceptably high ratio of transducer to film force. Therefore, a strip thickness of 0.0889 mm was selected as the material from which to fabricate prototype transducers.

Having sized the transducer and selected the material, an estimate may be made of the expected performance of this particular design. The force required to produce a 10 percent deflection of the ring may be computed from equations (1), (2), and (6). The effective modulus in this case is increased from the uniaxial stress value given in Table 2 by a factor of $1/(1 - \nu^2)$ to account for the cylindrical bending of a plate rather than a beam. A value of 0.3 is assumed for Poisson's ratio in this calculation. The force required to displace the ring is computed to be 0.07727 N (7.88 grams) which is 1.73 percent of the force required to displace a 25.4 micron polyethylene film through the same strain at room temperature.

The maximum stress may be computed for the same conditions using equation (5). The results of this configuration indicate the maximum stress to be 70 MPa (10,150 psi). Considering the minimum values of the proportional limit for the material selected, Table 2, this would correspond to a factor of safety of 5.4 in the half-hard condition. Although this is an ample margin, it was observed that the ring maintained its circular shape much better when heat-treated. Therefore, it was considered necessary to form the transducer from the basic material in the half hard condition and then heat treat to the full hardness of the material.

Table 2. Physical and Mechanical Properties of Brush Wellman Alloy 25 - Half Hard Condition

Specific Gravity	8.26	
Density - g/cc (lb/cu in)	8.34	(0.301)
Thermal Expansion - 1/°C (1/°F)	16.9×10^{-6}	(9.4×10^{-6})
Thermal Conductivity - W/m/K (Btu/ft/hr/°F)	104.00	(60)
Modulus of Elasticity - GPa (psi)	128.00	(18.5×10^{6})
Magnetic Characteristics	Nonmagnetic	
Minimum Melting Temperature - °C (°F)	870.00	(1600)
Minimum Tensile Strength - MPa (ksi)	586.00	(85)
Heat Treated*	1276.00	(185)
Minimum Proportional Limit - MPa (ksi)	379.00	(55)
Heat Treated*	828.00	(120)
Minimum Yield Strength - MPa (ksi)	517.00	(75)
Heat Treated*	1103.00	(160)
Minimum Fatigue Strength at 10^8 Cycles - MPa (ksi)	269.00	(39)
Heat Treated*	221.00	(32)
Elongation - percent	5 - 95	
Heat Treated*	2 - 5	

*Heat treated for 7200 sec (2 hours) at 315°C (600°F)

V. FABRICATION

The thin film strain transducer described in the preceding section is fabricated in four pieces from the beryllium copper coil stock. Due to the thickness of the strip material, chemical milling of the sheet is considered to be the most economical and accurate method of forming the transducer components. A pattern is first drawn on a scale four times that described and then reduced photographically to produce a negative of the appropriate dimensions. The resulting negative, Figure 4, is then transferred to the beryllium copper sheet. The pattern is sized to preserve the basic design geometry as well as providing sufficient material to accomodate a bend radius of five times the sheet thickness to form the tab. The sheet is then immersed and the boundaries of the components etched away.

Two bends at each end of the strip form half of the gusset and tab. Since the material is in the half hard condition, this is accomplished without damage to the material. Two strips are then spot welded together. The semicircular tab is then spot welded to the tabs on the strips to form a double layer at this point. The welded ring is then placed on a brass mandrel having the same thermal characteristics as the beryllium copper and heat treated for 7200 sec (2 hours) at 315°C (600°F). This serves to bring the material to its full strength. The transducer is removed from the oven and electrochemically treated to remove the oxide coating that forms during the heat treating process.

Four precision strain gages are then bonded to the inner and outer surfaces at the center of the ring as shown in Figure 5. The gages selected for this application are standard 350 ohm general purpose gages having a constantan grid completely encapsulated in polyimide with large, integral, copper coated terminals. The gage resistance was selected due to its enhanced current-carrying ability as well as its compatibility with other low impedance devices currently used on scientific balloons. The gages are thermally compensated for beryllium copper and are calibrated for use in a temperature range from -75°C to +205°C. The particular lot of gages used have a gage factor at 20°C of 2.11 $\pm$ 0.5 percent. The gages are produced by Micromeasurements under the type designation CEA-09-125UW-350.

The strain gages are bonded to the transducer with M-Bond 600, a Micromeasurements product with a wide temperature range capability. This two-component, solvent-thinned epoxy-phenolic adhesive is reccommended for high-precision transducers and has an operating temperature range from -269°C to +260°C. The adhesive is cured for 7200 sec (2 hours) at 30°C above room temperature. This curing procedure produces an oxide coating on the transducer that must be removed prior to applying any coatings for moisture or thermal control. In addition to the gages, a terminal board is bonded to one tab to facilitate wiring of the gages into a Wheatstone bridge circuit.

The bridge circuit is formed by connecting the two gages on the outer surface of the ring into opposite arms of the circuit. The gages on the

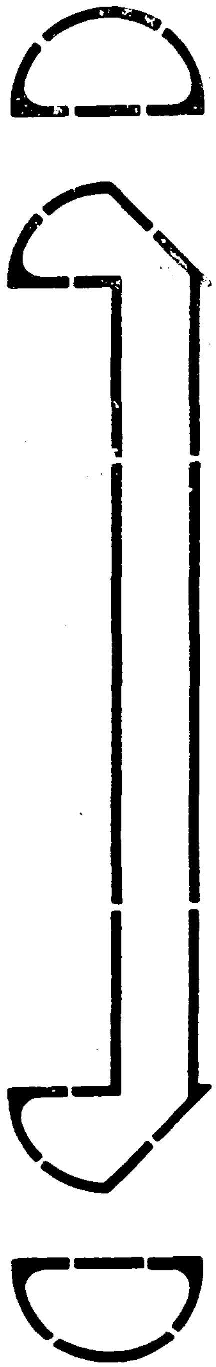


Figure 4. Chemical Milling Pattern

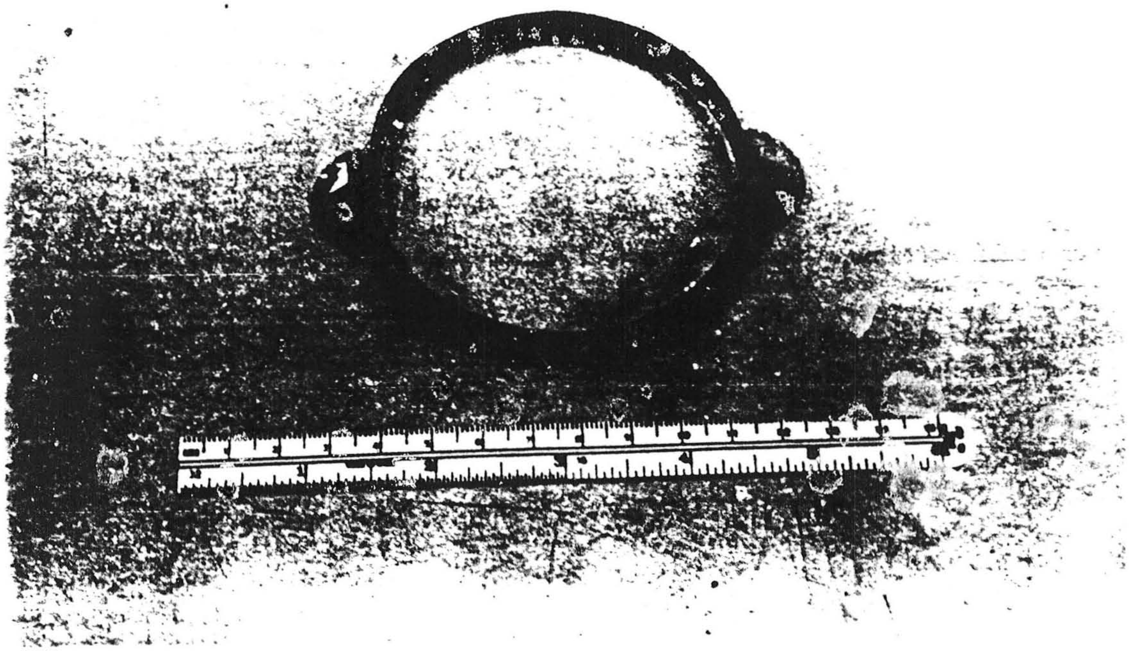


Figure 5. Thin Film Strain Transducer

inner surface are then used to complete the four active arm bridge. In this configuration, the output of the circuit is insensitive to any axial and thermal strains common to all gages. Theoretically, the output from a balanced bridge will be proportional to the bending strain in the beryllium copper strip and is given to a first approximation by:

$$\frac{\Delta V}{V} = 2.11\, \epsilon_b \qquad (7)$$

This signal will be directly proportional to the film strain between the tabs of the transducer. The output signal, ΔV, is proportional to the supply voltage, V, and is quite small requiring some amplification for practical application. The wiring is completed by using a white four conductor shielded cable and connector recommended by the National Scientific Balloon Facility.

A variety of coatings have been applied to the transducer for different reasons. Gagekote Number 1 supplied by Magnaflux Testing Systems was used to protect the strain gages and terminal connections from moisture. This produces a flat white finish to the transducer and is a primer for other coatings. In addition to Gagekote, a white paint (Sherwin Williams - A6W596) was recommended by NSBF due to its known radiometric properties. The ratio of solar absorptivity to infrared emissivity is 0.29, a minimum of all coatings considered. The finish does not appear significantly different from that of Gagekote.

VI. CALIBRATION

The calibration of the thin film strain transducer was accomplished with a variety of tests to evaluate the sensitivity to various conditions experienced in flight. Room temperature testing was used to verify the design features from which the transducer has evolved. The first test utilized a drum micrometer (Figure 6) to determine the relationship between the transducer displacement and the Wheatstone bridge output. An uncoated transducer was clamped to the micrometer as shown. The bridge was excited with a 5.000 volt DC supply and the transducer displaced in discrete steps from zero to 10 percent tensile strain and from zero to 5 percent compressive strain. The test was performed three times and the results recorded without the benefit of amplification. The results of this test are shown in Figure 7 as a function of the change in length per unit length of the transducer. The linearity of this response is remarkably good and indicates the transducer longitudinal sensitivity to be 7.216 mV/V/unit strain. This sensitivity is greater than standard precision strain gages ($\approx$ 2.0) which suggests that the signal may be easily conditioned, amplified and telemetered with standard strain gage measuring equipment.

In order to evaluate the transverse sensitivity of the transducer, the drum micrometer was again used to provide calibrated displacements. The transducer was rotated 90° and clamped to an extension of the micrometer head. Both positive and negative transverse displacements were applied and the output signal recorded. These data were normalized with respect to the ring diameter and are presented in Figure 8 as the transverse sensitivity. Since the original distance between the tabs is the transducer diameter and since the displacements were applied perpendicular to this diameter, the distance between the tabs actually increased regardless of the direction of the displacement. Using the longitudinal sensitivity established in Figure 7, the output signal due to the corresponding separation of the tabs is also shown in Figure 8. The difference between the curve and the measured points shown is a measure of the transverse sensitivity of the transducer. The maximum departure of the measured signal after amplification from that expected due to longitudinal strain is 2 mV which corresponds to 0.27 percent of the longitudinal signal.

As a final room temperature test, the dynamic characteristics of the transducer were determined. The gage was suspended by one tab and the bridge balanced. Once displaced, the transducer would oscillate at its natural frequency as shown in Figure 9(a). The internal damping of the material that forms the transducer is readily apparent and has been quantified in Table 3.

A second transducer was calibrated as before. In this case, the transducer was identical to the first with the exception of a thin layer of Gagekote to protect the transducer from moisture. This transducer was slightly more sensitive than the uncoated gage as indicated in Figure 7. However, a notable difference in dynamic response was observed as seen in Figure 9(b). The added mass caused by the coating causes not only a slight decrease in natural frequency but also a significant increase in damping

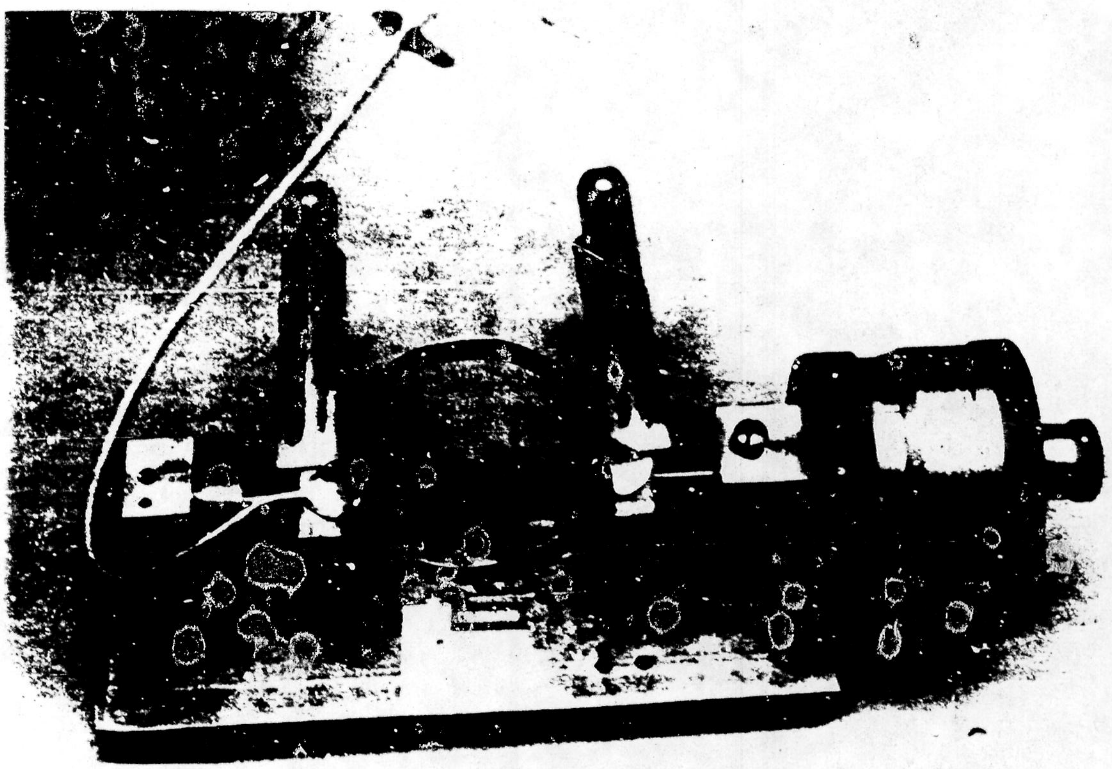


Figure 6. Calibration of Ring Transducer

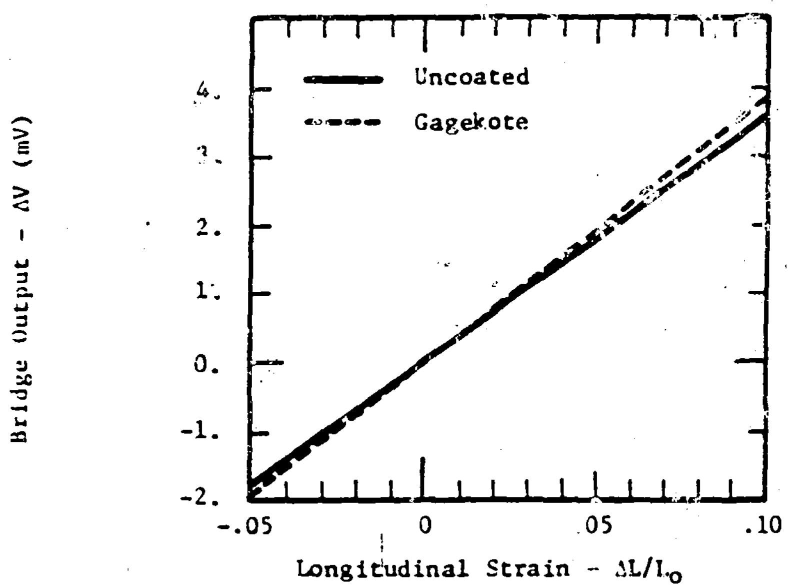


Figure 7. Room Temperature Longitudinal Sensitivity

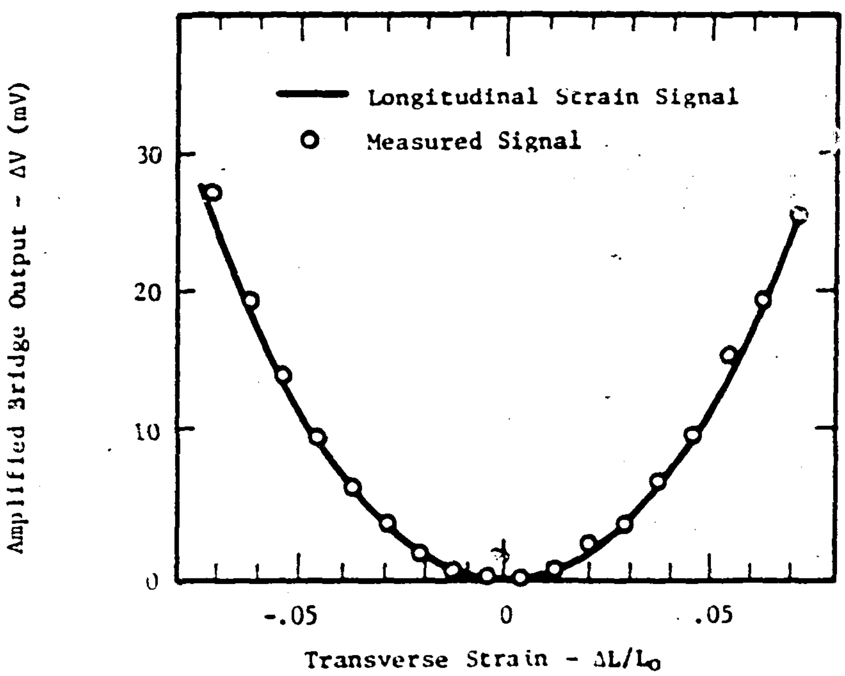
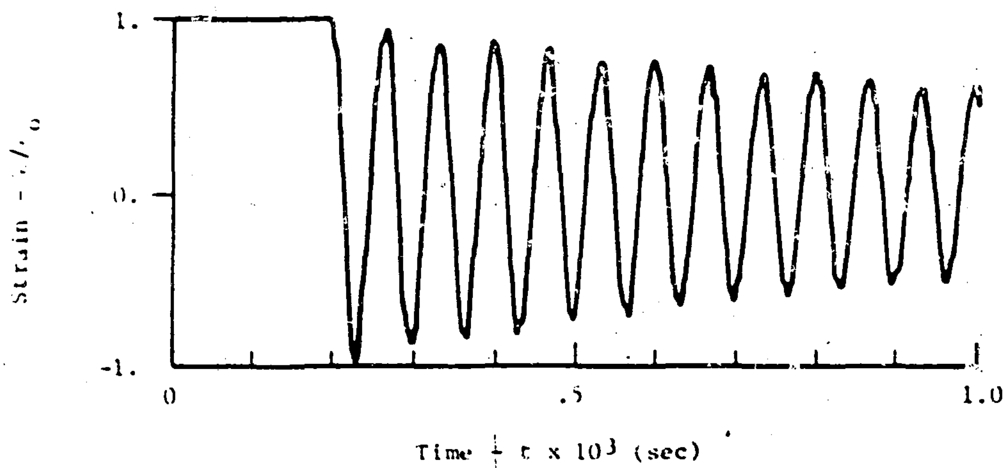
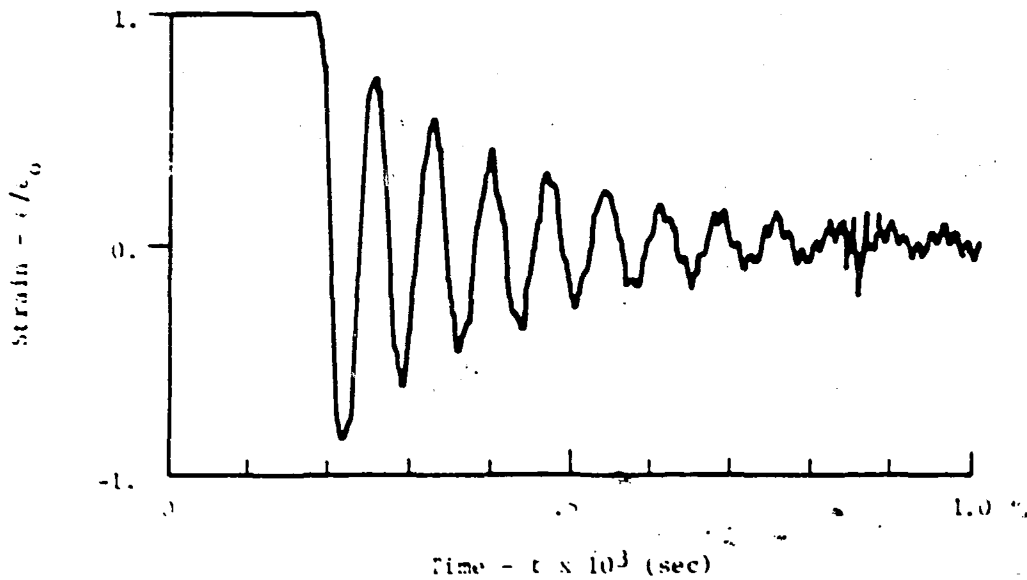


Figure 8. Room Temperature Transverse Sensitivity



(a) Uncoated Transducer



(b) Gagekoted Transducer

Figure 9. Dynamic Response of Transducer at Room Temperature

due to the viscous nature of the paint. These values have also been quantified in Table 3.

Table 3. Dynamic Properties at Room Temperature

Coating	Natural Frequency (Hz)	Circular Frequency (1/sec)	Damping (1/sec)
Uncoated	14.87	93.43	0.522
Gagekote No. 1	13.57	85.26	3.926

Extensive testing was conducted of the uncoated transducer at reduced temperatures. Both stiffness and longitudinal strain sensitivity were determined by clamping the transducer to rigid straps attached to the Instron testing machine film grips. The transducer and grips were enclosed in an insulated chamber (Figure 10) which was cooled by the regulated expansion of liquid nitrogen into the chamber. After bringing the chamber down to temperature for 300 sec (5 min), the bridge was balanced and the transducer then displaced 10 percent. The load and bridge output were recorded for later analysis. The results of this series of tests indicated that the force required to displace the transducer 10 percent increased from 0.076 N (7.8 grams) at room temperature to 0.114 N (11.6 grams) at -80°C. This slight increase in stiffness is indicative of the change in the effective modulus of the transducer material with temperature. The strain sensitivity of the transducer was relatively unaffected by the temperature changes indicating that a single calibration of the gage, such as Figure 7, is sufficient to reduce flight data to total longitudinal strains regardless of temperature. It should be cautioned that in order to separate thermal strain in film from that due to stress, an independent measure of film temperature will be required.

This test was repeated with a transducer that had been coated with Gagekote No. 1. This particular coating is rated for use at temperatures from -195°C to 455°C. However, at -80°C, the force required to displace the transducer 10 percent increased to 0.951 N (97 grams) which represents an increase of 8.36 times the force required for the gage at room temperature. In addition to this, the longitudinal strain sensitivity of the bridge was reduced to 79.2 percent of the room temperature value.

As a final test of the transducer sensitivity, the coated gage was painted with an additional layer of the white paint (A6W596) recommended by NSBF. After the temperature was reduced to -80°C, the bridge remained balanced when the transducer was deflected 10 percent. This rather strange

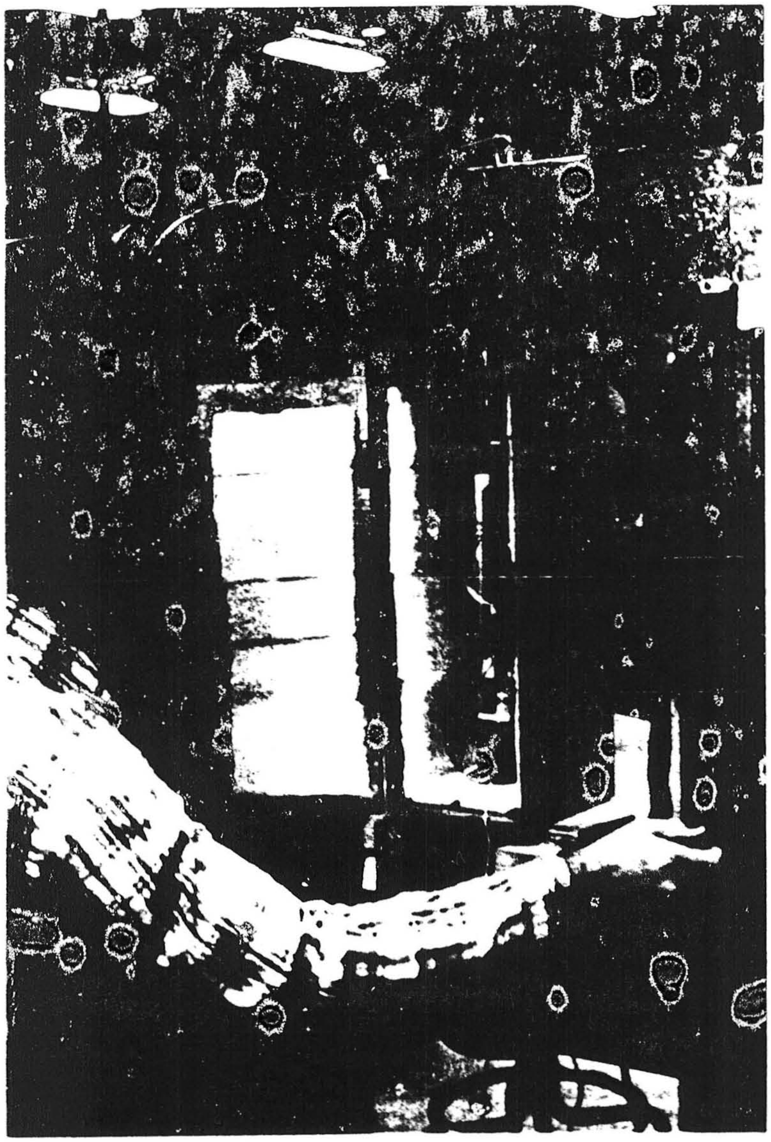


Figure 10. Low Temperature Test Configuration

result was checked by opening the chamber and physically measuring the displacement of the gage. After the transducer was returned to room temperature, its response was identical to the coated gage response shown in Figure 7.

VII. DISCUSSION OF RESULTS

The calibration program described in the preceding section has demonstrated that every design condition enumerated in Section III has been satisfied by the uncoated beryllium copper transducer. A force necessarily accompanies the displacement of the transducer ring; however, this force is small relative to the force required to displace the film through the same strain. A load path is established from the film, through the gage tab, partially to the ring and the balance to the film between the tabs. It has already been shown that the effect of the tab is to increase the apparent strain while the effect of the transducer stiffness is to reduce the force the film is required to transfer. These two effects are somewhat self compensating even though no attempt has been made to balance the effects.

The testing of this sensor has adequately demonstrated the ability to respond with fixed longitudinal strain sensitive at all temperatures of interest even though the stiffness of the material increases. The response is highly sensitive to the separation distance of the tabs and relatively insensitive to transverse forces. The transducer has been shown to respond in a constant linear fashion at all strains and temperatures of interest. It is electrically compatible with bridge amplifiers now being flown on scientific balloons.

The experiences reported on the use of various coatings is somewhat disappointing. The purpose of the coating is to protect the strain gages and terminal connections from the effects of moisture during the ascent of the balloon. Once on station, the radiometric properties of the coating serve to keep the temperature of the transducer sufficiently low to prevent burning through the thin balloon film. If strains are not to be measured at low altitude, the absence of water vapor at high altitudes will remove the requirement that the coating serve as a waterproofing agent. However, the need for passive thermal control of the device is essential to prevent damage to the balloon.

The increase in gage modulus (or force) at reduced temperatures using Gagekote is not a significant problem. The modulus of the gage was based on room temperature values of film modulus. At reduced temperatures, the modulus of the film will increase. The uncoated gage would become more accurate under these conditions while the coated gage will at least maintain its room temperature accuracy. Unfortunately, the longitudinal strain sensitivity of the coated transducer is affected by reduced temperatures. This could be calibrated but the effect would be a function of coating thickness and, therefore, different for each gage. In addition, the temperature of the film would have to be measured as well as the output of the strain transducer.

The use of the NSBF recommended paint during laboratory testing was quite revealing. The paint apparently becomes brittle at reduced temperatures and attempts to prevent the ring from displacing. The coating develops a high stress near the gage tab and cracks, causing the ring to deform in a

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manner other than that for which it was designed. This paint is considered totally inappropriate at low temperatures on flexible surfaces.

The need for a coating to control the temperature of the transducer has not yet been verified. A polished beryllium copper finish may be sufficient to maintain temperature control. Handbook values for solar absorptivity and infrared emissivity of bronze-like materials are not sufficiently accurate to compute the temperature. However, it is essential that the transducer thermal control be established prior to the attachment of this sensor to an operational balloon. In addition, there is a need to demonstrate that the dynamic launch of this low mass (3 grams) sensor will not damage the fragile films normally used in balloon construction.

VIII. CONCLUSIONS AND RECOMMENDATIONS

A thin film strain transducer suitable for use on high altitude balloons has been designed, fabricated and calibrated. The uncoated version of this device has demonstrated the required qualities of large strain capabilities over the most severe operating temperature range of a typical balloon with minimal reinforcement of the film. In addition, the device has been shown to be relatively insensitive to transverse forces while the longitudinal strain sensitivity is independent of both strain and temperature. In its present form, this device is suitable for laboratory use as shown in Figure 11.

The coated transducer was found to have similar qualities at room temperature; however, at -80°C, the transducer stiffness increased by almost an order of magnitude while the longitudinal sensitivity was reduced to 79 percent of the room temperature value.

It is concluded that the characteristics of the uncoated transducer are preferable for in-flight measurement of strain due to its lack of temperature sensitivity. The measurement of strain with this device would not be dependent on any other simultaneous measurement except perhaps the supply voltage. In order to separate the measured total strain into its thermal and mechanical components, the temperature should be either measured or computed from flight data.

It is recommended that the need for a coating for passive thermal control of the transducer be reexamined. It has been assumed that the solar absorptivity and infrared emissivity of the beryllium copper would be such as to cause heating of sufficient magnitude to cause failure of the adjacent polyethylene film. Unfortunately, handbook values of the radiometric properties have such a wide range of values that a theoretical prediction of temperature with acceptable accuracy is unlikely. It is recommended that the uncoated transducer be attached to a sample of polyethylene film and subjected to a radiation environment typical of a balloon flight. The sample could conceivably be located on the gondola and recovered with the payload. If a telemetry channel is available, the temperature of the transducer could be monitored during the flight. Otherwise, a passive post-flight inspection of the film would indicate the presence of significant thermal degradation.

If the need for passive thermal control is verified, the use of coatings other than those used to date should be explored. Several compounds are known to remain pliable at -80°C. Silastic 734 is an RTV rubber compound that could be thinned to a paint consistency and pigmented. Gagekotes formed from this base are available but have degraded electrical properties. It is recommended that the development of additional coatings be pursued only after the need is established with some certainty.

The ability of the transducer to survive the hostile dynamic environment of launch and ascent should be demonstrated. The use of a subscale balloon model with a wall thickness similar to that typically flown would be

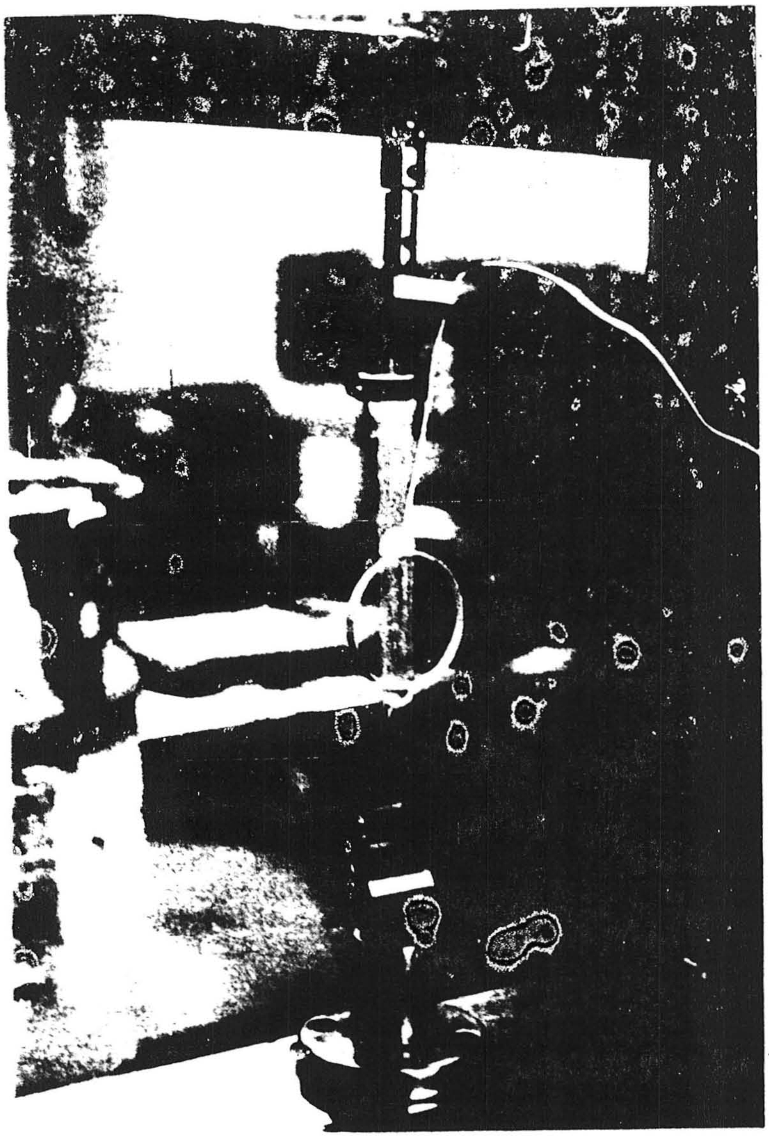

Figure 11. Instrumented Thin Film Sample

appropriate for this demonstration. The model should be tethered and filled with sufficient lifting gas to duplicate the launch accelerations normally encountered. After experiencing the launch accelerations, the balloon should be deflated and the film in the vicinity of the gage examined for abrasions or other damage.

On a full-scale balloon, the unfurling of multiple gores could cause damage to either the transducer or the balloon. This sensitivity could be easily evaluated by the use of an available balloon not certified for flight. As in the case of the subscale model, the prototype test balloon should simulate launch conditions but remain tethered to the ground. Again, the balloon should be deflated and the film and transducer examined for possible damage.

Upon completion of these demonstrations, the thin film strain transducer should be ready for launch on an engineering test flight. It is recommended that the strain be measured in both the circumferential and meridional directions on the shell of the balloon just below the longest cap. In addition, the strain in two directions at the midpoint of the shortest cap should be measured. These measurements would then be used to evaluate the various theories now being considered in the design and analysis of high altitude balloons.

APPENDIX

The design of a suitable ring transducer with exacting performance requirements demands the ability to accurately predict the displacement and stress distribution for any given applied load. Since the principal on which this gage is to operate is bending of the thin ring, it is necessary to evaluate the change in curvature at each point around the ring. Due to the nonlinear geometry, the usual Newtonian approach to bending of a beam is not recommended. Energy methods are ideally suited for this problem in general and the reciprocal theorems of Betti and Maxwell are particularly applicable. Sometimes known as the Principal of Virtual Work, this theorem states that the work done by a virtual force moving through the real displacements is equal to the work done by the real forces moving through the virtual displacements. This principal will be used extensively in the following development.

Consider the ring shown in Figure A-1 which is subjected to a tensile force, P. The ring is originally a circle of radius, R, but deforms into a noncircular shape as the load is applied. The displacement of one side relative to the other will be designated as q and is in the direction of the force, P. Due to the symmetry of the ring, only half of the ring need be analyzed if care is taken to establish the proper boundary conditions. A free body diagram of half of the system is given in Figure A-2 and defines the coordinate system to be used.

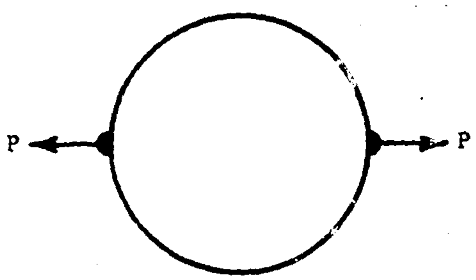

Figure A-1

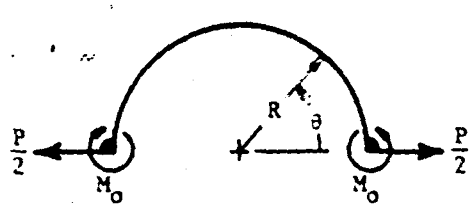

Figure A-2

It should be noted that a moment exists at each boundary but the value of this moment is unknown. Although this problem is statically indeterminant, the bending moment may be written in terms of the moment at the boundary as:

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$$M(\theta) = M_o + \frac{PR}{2} \sin \theta \qquad (A-1)$$

In order to evaluate the boundary moment, M_o, the Principal of Virtual Work may be easily applied. In this case, the virtual "force" will be assumed to be a unit moment at the boundary. The real "displacement" at the boundary is the angle of rotation at this point which is zero. The work done by the real forces moving through the virtual displacement may be expressed symbolically as:

$$\delta U = \int_0^L \frac{Mm}{EI} ds + \int_0^L \frac{Pp}{EA} ds \qquad (A-2)$$

This expression is quite general and includes the work associated with axial forces and bending moments. The lower case symbols in this equation refer to the axial force and bending moments for the unit load system. In this case:

$$p = 0 \qquad (A-3)$$

and

$$m = 1. \qquad (A-4)$$

Equation (A-2) may now be evaluated by substitution and integration as follows:

$$\delta U = 1 \times 0 = \int_0^{\pi} \frac{(M_o + \frac{PR}{2} \sin \theta)\ (1)}{EI} R\, d\theta \qquad (A-5)$$

Evaluating this equation yields an expression for the moment at the boundary in terms of the applied force and ring diameter given by:

$$M_o = - \frac{PR}{\pi} \qquad (A-6)$$

The bending moment, equation (A-1), may now be expressed in terms of the force and geometry as:

$$M(\theta) = \frac{PR}{2} \left(\sin \theta - \frac{2}{\pi}\right) \tag{A-7}$$

This expression is very useful in that it indicates the maximum bending moment occurs at the point where the load is applied ($\theta = 0$) and is given by:

$$M(0) = -0.3183 \; PR \tag{A-8}$$

While the maximum positive bending moment occurs at the center of the ring and is given by:

$$M(\pi/2) = 0.1817 \; PR \tag{A-9}$$

The energy method used to determine the boundary moment may also be used to determine the displacement, q, of the ring. In this case, the virtual force is assumed to be a unit force in the direction of the real displacement, q. The moment distribution due to this unit force system is given by:

$$m = R \sin \theta \tag{A-10}$$

Equation (A-2) may be simplified by assuming that the energy associated with the forces P and p are negligible when compared to that associated with the bending moment. In this case:

$$\delta U = 1 \times q = \int_0^{\pi} \frac{PR}{2EI} \left(\sin \theta - \frac{2}{\pi}\right) (R \sin \theta) (R \, d\theta) \tag{A-11}$$

Integration yields:

$$q = \frac{PR^3}{EI} \left(\frac{\pi}{4} - \frac{2}{\pi}\right) \tag{A-12}$$

This expression is quite useful in establishing the modulus of the ring and the stress as a function of the displacement.

The stress due to bending is given by the familiar expression:

$$\sigma = \frac{Mt}{2I} \tag{A-14}$$

Therefore, the maximum stress will occur at the point of maximum moment (θ = 0) and may be expressed in terms of the ring displacement by combining equations (A-9) and (A-13) with (A-14) to yield:

$$\sigma_{max} = \frac{E\ t\ q}{4\ R^2 \left(\frac{\pi^2}{8} - 1\right)} \tag{A-15}$$

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