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**Assessment of the Hydrogen
External Tank Pressure Decay
Anomaly on Space
Transportation
System (STS) 51-L**

Theresa M. Buckley

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51-L

Theresa M. Buckley
Lyndon B. Johnson Space Center
Houston, Texas

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ABSTRACT

Following the *Challenger* tragedy, an evaluation of the integrated main propulsion system flight data revealed a premature decay in the hydrogen external tank ullage pressure. A reconstruction of predicted ullage pressure versus time, to explore this anomaly, indicated an inconsistency between predicted and measured ullage pressure starting at approximately 65.5 seconds into the flight and reaching a maximum value between 72 and 72.9 seconds. This discrepancy could have been caused by a hydrogen gas leak or by a liquid hydrogen leak that occurred either in the pressurization system or in the external tank. The corresponding leak rates over the time interval from 65.5 to 72.9 seconds were estimated to range from 0.28 kg/s (0.62 lbm/s) \pm 41 percent to between 0.43 and 0.51 kg/s (0.94 and 1.12 lbm/s) \pm 1 percent for a gas leak and from 72.9 kg/s (160.5 lbm/s) \pm 41 percent to between 111.6 and 133.2 kg/s (245.8 and 293.3 lbm/s) \pm 1 percent for a liquid leak. No speculation is made to ascertain whether the leak is liquid or gas, as this cannot be determined from the analysis performed. Four structural failures in the hydrogen external tank were considered to explain the leak rates. A break in the 5-centimeter (2 inch) pressurization line, in the 13-centimeter (5 inch) vent line, or in the 43-centimeter (17 inch) feedline is not likely. A break in the 10-centimeter (4 inch) recirculation line with a larger structural failure occurring in the 72- to 73-second time period, the time of the visibly identified premature pressure decay, does seem plausible and the most likely of the four modes considered. These modes are not all-inclusive and do not preclude the possibility of a leak elsewhere in the tank.

INTRODUCTION

Subsequent to the Space Transportation System (STS) 51-L accident on January 28, 1986, the Propulsion and Power Division at the NASA Lyndon B. Johnson Space Center (JSC) formed an investigative team to assess and evaluate the main propulsion system (MPS) performance. This team concentrated on the integrated MPS consisting of

1. The Orbiter main propulsion system
2. The Space Shuttle main engines
3. The external tank

Flight data were scrutinized for anomalies that occurred during the *Challenger* flight.

During the investigation, a review of the MPS flight data showed that a premature decay in the hydrogen external tank ullage pressure started seconds before the catastrophe. Under normal conditions, once the hydrogen external tank pressurization system stabilizes after the initial 40 seconds of flight, the system operates such that the ullage pressure in the external tank is maintained within the bandwidth shown in figure 1.

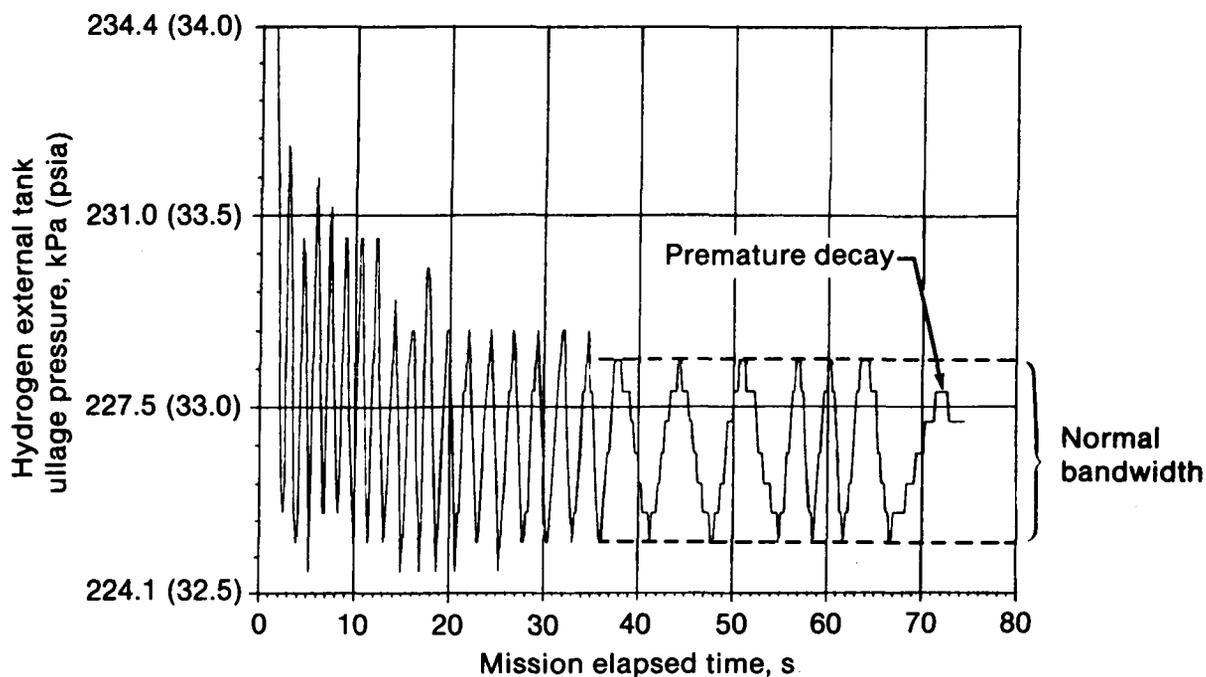


Figure 1.- Hydrogen external tank ullage pressure, STS 51-L.

The pressure decay indicated in figure 1 could be indicative of a hydrogen leak in the external tank. The accuracy of this postulation needed to be determined because the decay could be explained also if a pressurization system malfunction had occurred. For example, if a valve that permits pressurant gas to flow to the tank was never commanded open, an insufficient quantity of pressurization gas would be recirculated to the tank and a subsequent drop in ullage pressure would result.

In compliance with the NASA's publication policy, the original units of measure have been converted to the equivalent value in the Système International d'Unités (SI). As an aid to the reader, the SI units are written first and the original units are written parenthetically thereafter.

The author wishes to extend appreciation to Regina Rieves for her technical support and advice, and to Dr. Gene Ungar and Dr. Al Feiveson for the technical inputs during the review of this report. The author also wishes to extend thanks to Warren Brasher for his supervisory support of this project.

SUMMARY OF RESULTS

The STS 51-L hydrogen external tank ullage pressure was reconstructed using flight data (app. A) independent of measured pressure such that a comparison of predicted to measured ullage pressure would either verify or discount the presence of a leak. If no leak was present, then the predicted ullage pressure, which uses actual valve position flight data (i.e., it was assumed that flight data accurately indicated the position of each valve), should match the measured ullage pressure. The pressures would match regardless of whether or not an additional valve that would be required to open to prevent the decay was actually commanded open. Thus, if predicted and measured ullage pressures do match, one could then attribute the premature decay to a malfunction of the main propulsion system and delve further into the control system dynamics of the

pressurization system. If a leak was present, however, the predicted ullage pressure would deviate from that measured.

Ullage pressure was reconstructed using data from flights STS 61-A and STS 51-F as a baseline. A baseline was desired for two reasons. First, since the thermodynamics inside the ullage are complicated and undefined, data from flights STS 61-A and STS 51-F were needed to correlate the bulk ullage temperature, an important parameter required in the reconstruction. Second, consistent results between several flights would affirm the integrity of the analysis and confirm the validity of the results. Flights STS 61-A and STS 51-F were chosen because their throttling profiles (liquid hydrogen consumption rates) are similar to those of STS 51-L, and it was assumed that similar thermodynamics would be present.

Results of ullage pressure reconstruction for flights STS 51-L, STS 61-A, and STS 51-F, from 40 to 73 seconds mission elapsed time (MET), are shown in figures 2, 3, and 4, respectively. The timeframe prior to 40 seconds was not considered since the period of interest included only the final seconds of flight. As shown in figures 3 and 4, there is agreement to within ± 1.7 kPa (± 0.25 psia) between predicted (reconstructed) and actual (measured) ullage pressure, from the start of reconstruction at 40 seconds to the end of reconstruction at 73 seconds, for both flights STS 61-A and STS 51-F. These results are consistent with a no-leak assumption. On the other hand, as shown in figure 2, flight STS 51-L shows agreement to within ± 1.7 kPa (± 0.25 psia) until approximately 64 seconds. At this time, the predicted ullage pressure began to deviate significantly from actual ullage pressure, with the predicted ullage pressure of increasingly greater magnitude than the actual ullage pressure. The result verifies the postulation that a leak was the source of the premature ullage pressure decay.

The deviation of the ullage pressure from the prediction can be explained by one of two phenomena. One, the ullage volume is increasing faster than measured, implying a liquid leak in the tank. Two, the amount of pressurization gas actually entering and remaining in the tank is less than measured, implying a gaseous hydrogen leak either in the pressurization line leading into the tank or in the tank itself.

With an apparent leak identified and the leak initiation time estimated at 65.5 seconds, the next objective was to quantify the leak rates. Actual and predicted ullage pressure rise and decay rates, which are dependent upon hydrogen mass quantities in the tank, were chosen as the criteria from which leak rates could be quantified. If comparison of actual and predicted pressurization rates showed a deviation, this mismatch could be corrected by incorporating a leak into calculations of predicted pressure rise and decay rates. To calculate a hypothetical liquid leak, no gas leak is assumed. Likewise, to calculate a hypothetical gas leak, no liquid leak is assumed.

The time period in question, from 62 to 73 seconds, was broken into three separate intervals, and actual and predicted pressurization rates were calculated for each of these intervals and compared. Leak rates were computed at average times on each interval and are shown in figures 5 and 6. If a gas leak is assumed, calculations show an initial leak of 0.28 kg/s (0.62 lbm/s) \pm 41 percent at 65.5 seconds which increases to between 0.43 and 0.51 kg/s (0.94 and 1.12 lbm/s) \pm 1 percent in the 72- to 72.9-second time interval. If a liquid leak is assumed, calculations show an initial leak of 72.9 kg/s (160.5 lbm/s) \pm 41 percent at 65.5 seconds which increases to between 111.6 and 133.2 kg/s (245.8 and 293.3 lbm/s) \pm 1 percent in the 72- to 72.9-second time interval. In both cases, the leak remains relatively constant from 65.5 to 72.0 seconds and increases significantly at 72 to 73 seconds. This sharp increase in leak rates parallels the premature decay that was initially identified in the flight ullage pressure data of figure 1.

Finally, a matrix of several possible structural failure modes was constructed and the corresponding leak rates were determined. The failure modes contributing to a gas leak that were

analyzed include either a clean break (flow coefficient (C_d) = 0.98) or a jagged-edged hole (C_d = 0.68) in the gaseous hydrogen pressurization line and either a clean break (C_d = 0.98) or a jagged-edged hole (C_d = 0.68) in the gaseous hydrogen external tank vent. The failure modes contributing to a liquid leak that were analyzed include a break in the liquid hydrogen recirculation line and a break in the liquid hydrogen feedline, both at or near the external tank interface. Based on this analysis, if one were to consider the previously described failures only, a break in the liquid hydrogen recirculation line with a larger structural failure occurring in the 72- to 73-second time-frame, coinciding with the visibly identified premature ullage pressure decay, seems plausible. The analysis was performed in a speculative sense and is not to preclude the possibility that the leak could have resulted from a hole or a structural failure elsewhere in the tank.

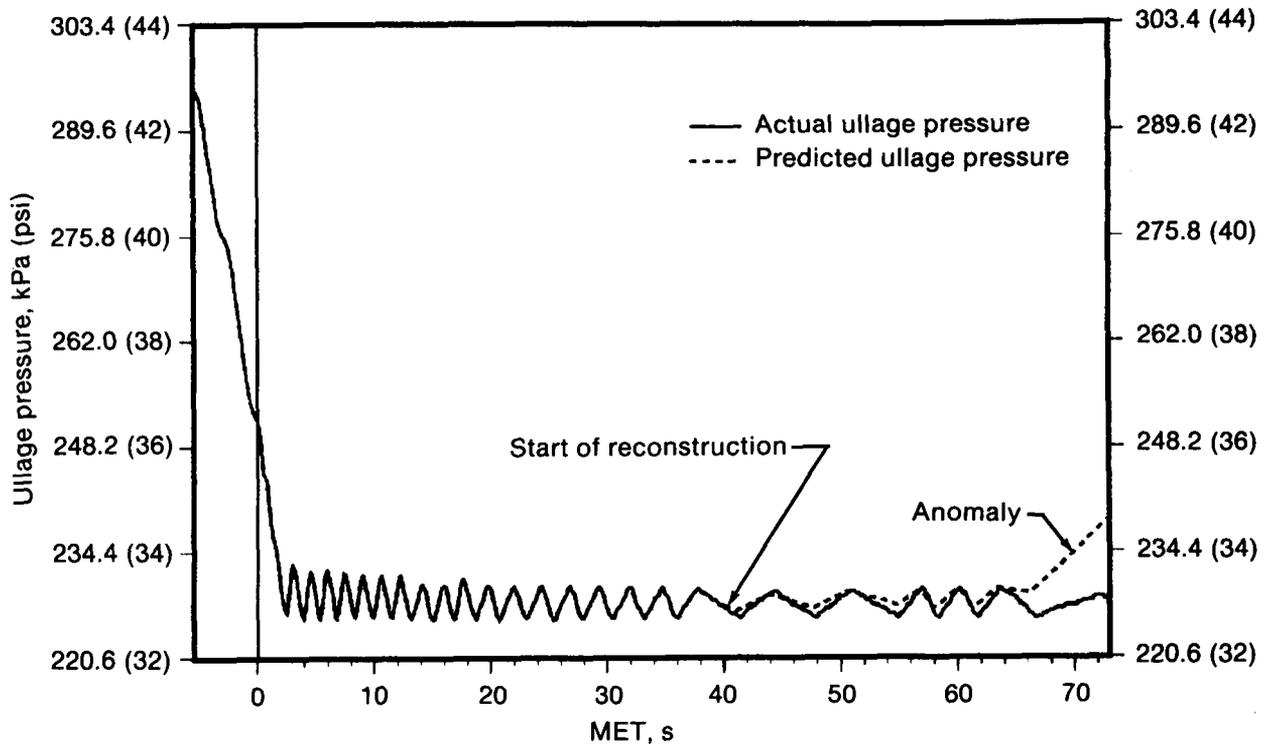


Figure 2.- STS 51-L ullage pressure reconstruction.

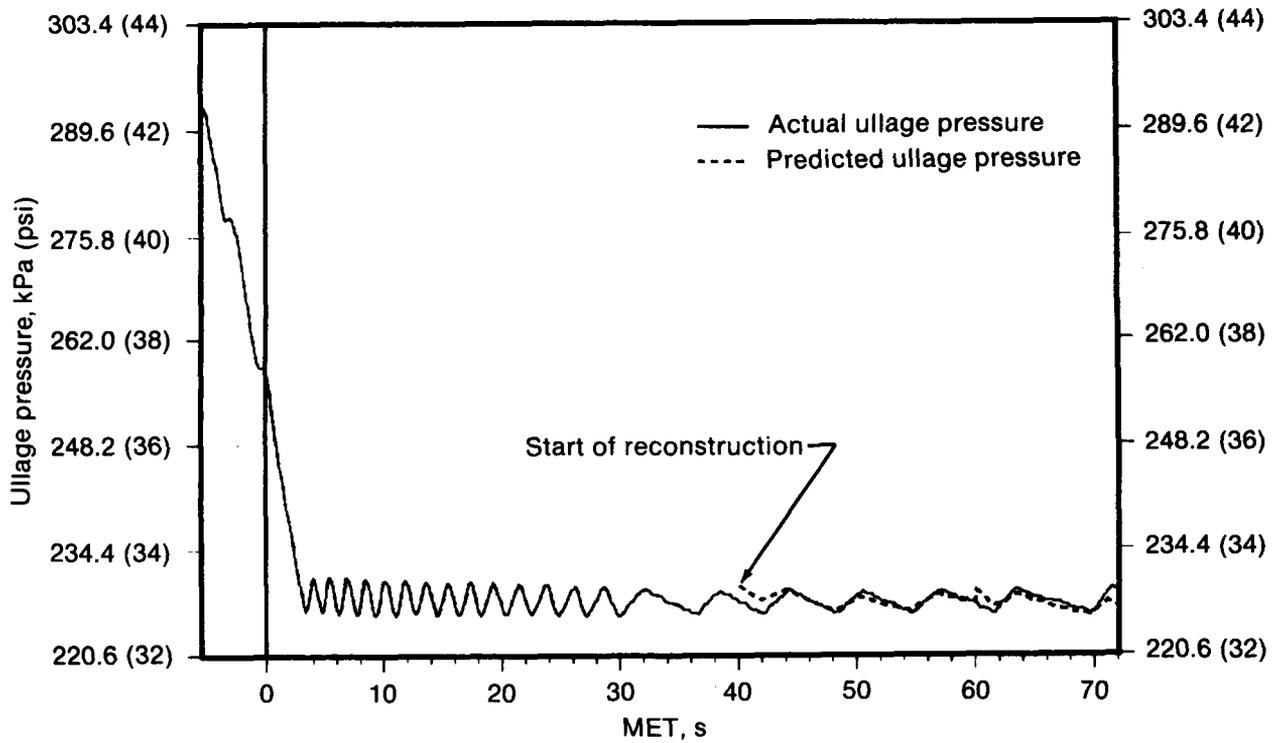


Figure 3.- STS 61-A ullage pressure reconstruction.

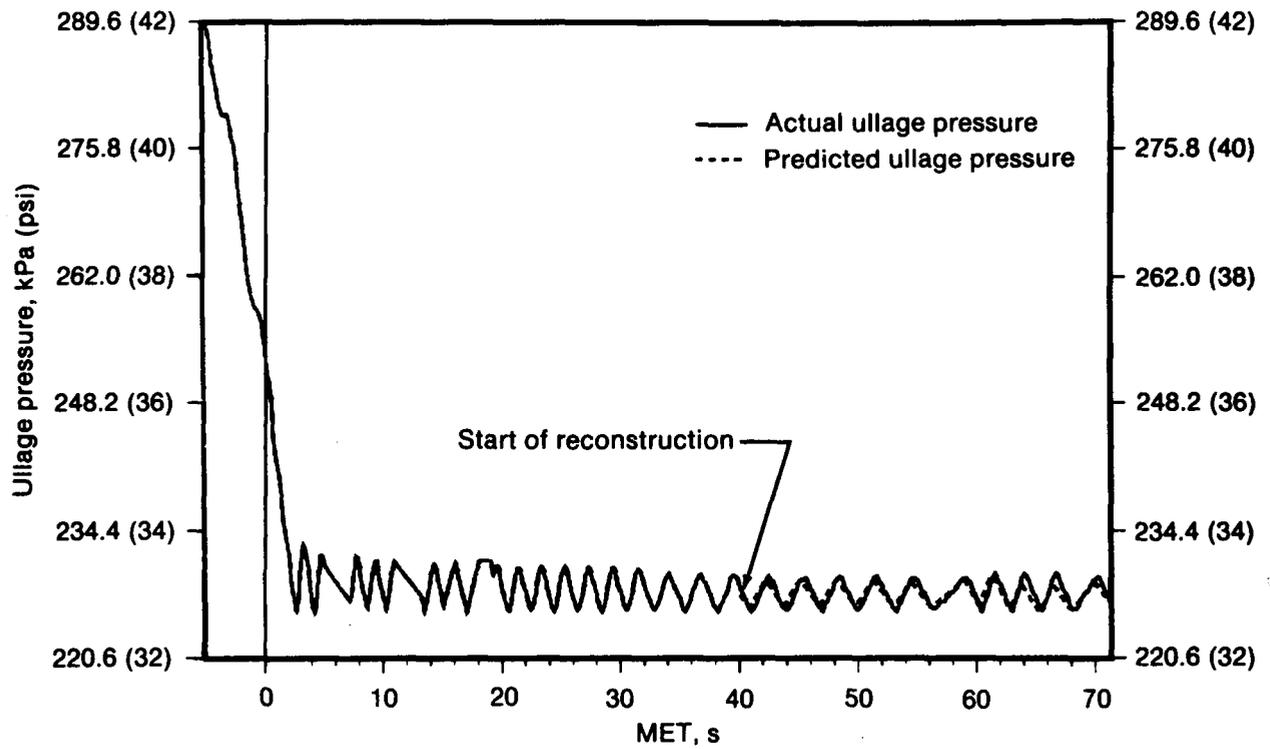


Figure 4.- STS 51-F ullage pressure reconstruction.

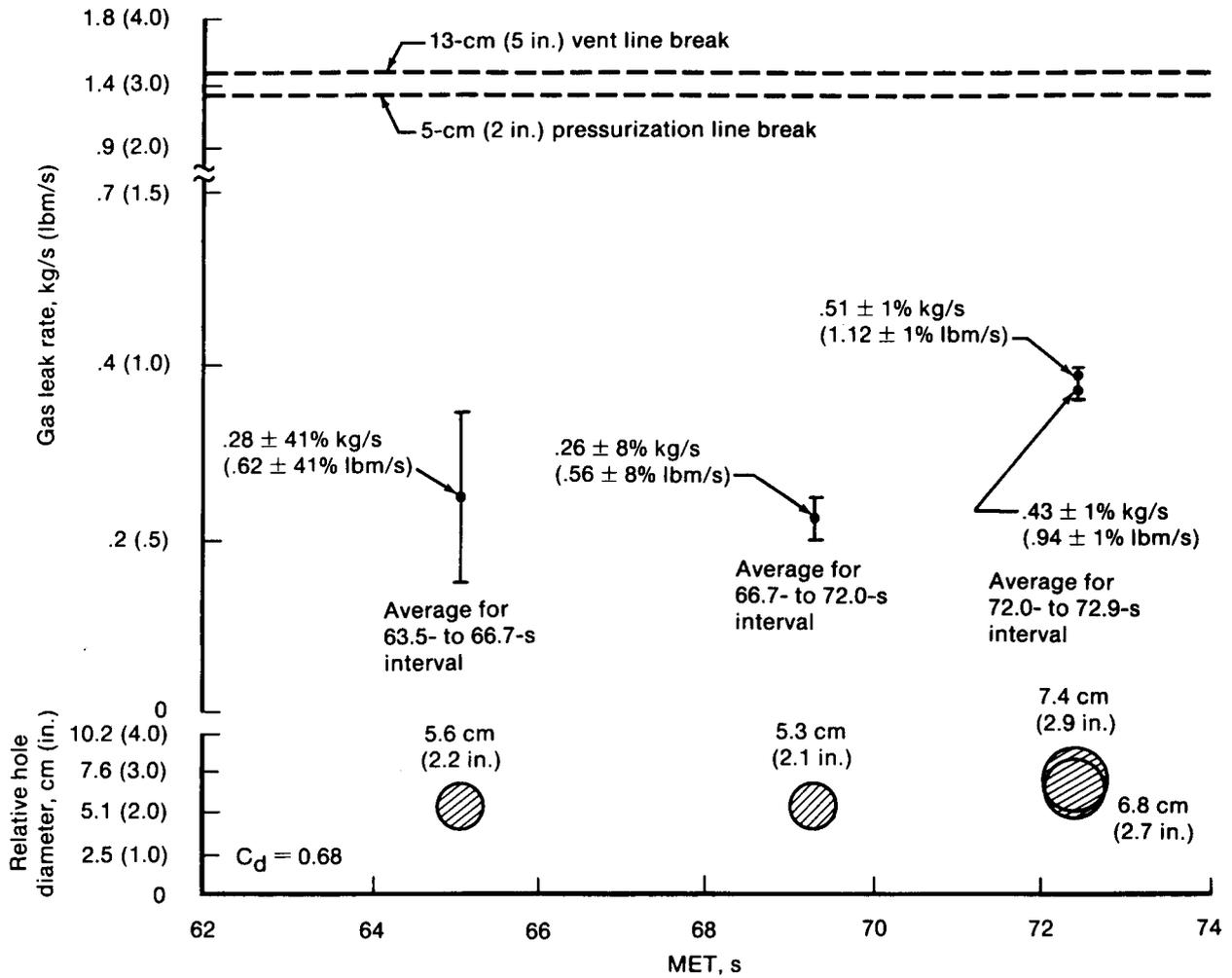


Figure 5.- Average gaseous hydrogen leak rate and relative hole size for each time interval.

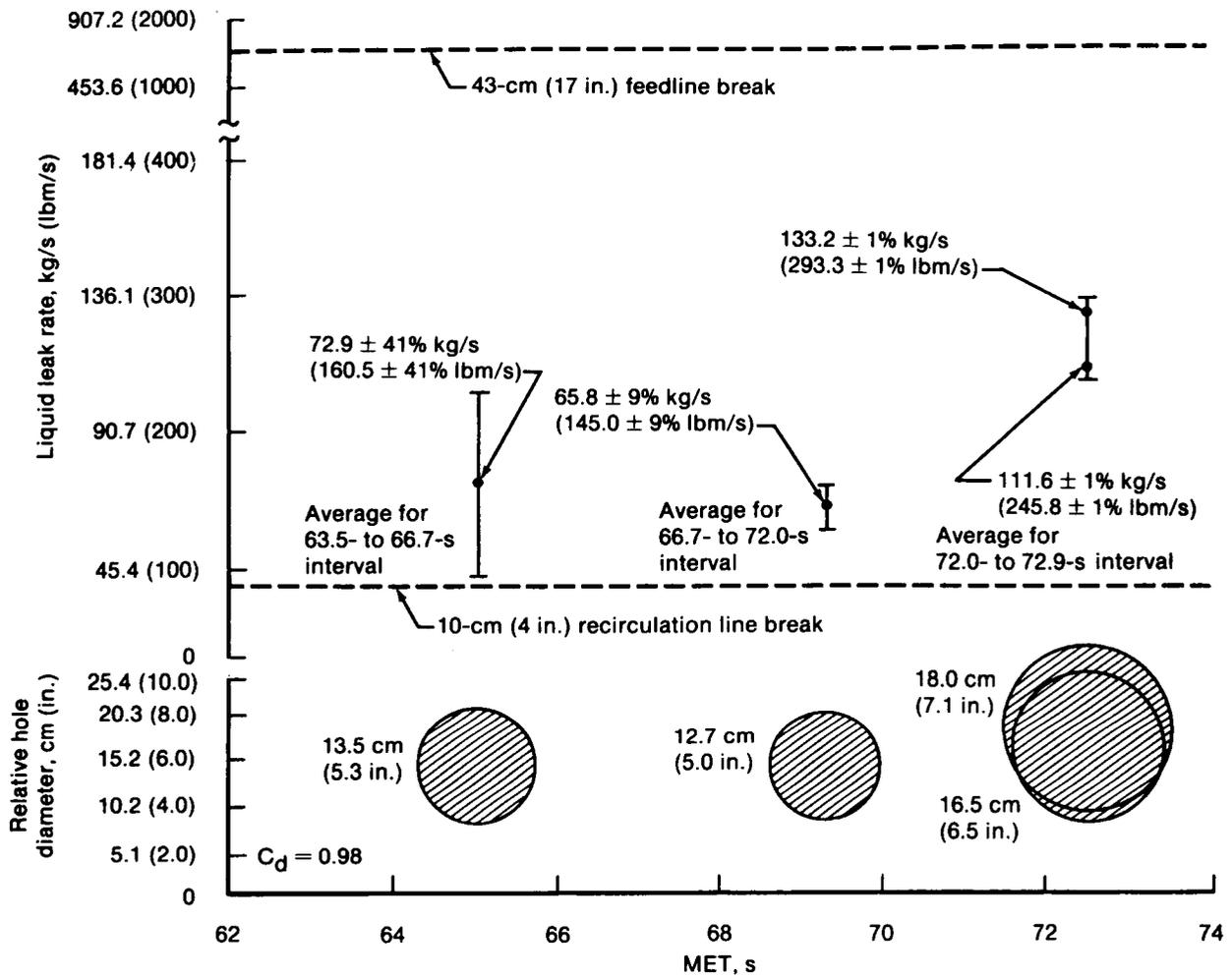


Figure 6.- Average liquid hydrogen leak rate and relative hole size for each time interval.

ULLAGE PRESSURE RECONSTRUCTION – DISCUSSION OF ANALYSIS

The external tank and its pressurization system are described in this section to lay a foundation for the detailed discussion of the analysis. With the system description complete, the development of the analytical pressurization model used is explained, and then the flight ullage pressure reconstruction results are presented.

PRESSURIZATION SYSTEM DESCRIPTION

A diagram of the *Challenger* main propulsion system is shown in figure 7. The system is composed of the Orbiter main propulsion system, the Space Shuttle main engines, and the external tank. Two separate cryogenic tanks (oxygen and hydrogen) comprise the external tank system. It is the hydrogen portion of the external tank and its pressurization system that are of interest.

A schematic of the hydrogen external tank pressurization system is shown in figure 8. As indicated, liquid hydrogen leaves the base of the tank through a 43-centimeter (17 inch) feedline (a) and feeds each of three main engines (b). The outlet pressurization gas from each of the three main engines passes through a flow control valve (c), where it combines with pressurization gas from the other two engines. This combined pressurization gas is channeled into the forward end of the ullage and serves to maintain the ullage pressure in a control band of approximately 226 kPa (32.8 psia) to 230 kPa (33.4 psia).

The purpose of the flow control valves is to control and limit the flow of pressurization gas into the forward end of the ullage. Each flow control valve is represented in the schematic by two orifices in parallel. The orifices are always in the choked-flow condition, and each set can simulate one of two flow control valve positions. When both orifices are opened, the equivalent flow control valve position is full open and permits maximum flow rate. When one orifice is opened and one is closed, the equivalent flow control valve position is partly open and limits the flow rate to a minimum.

The flow control valve control system is a closed-loop feedback system with ullage pressure being the feedback variable. Each of three ullage pressure transducers (d) acts independently to control the three engine outlet flow control valves. When ullage pressure rises above a predetermined transducer limit (different for each flow control valve), a signal is sent to close the corresponding valve and, consequently, to reduce the pressurization gas flow rate. Likewise, when the ullage pressure begins to fall below a predetermined transducer limit, a signal is sent to open the corresponding valve and, consequently, to increase the pressurization gas flow rate. The control system, therefore, maintains the ullage pressure in a prescribed band, and a plot of ullage pressure as a function of time assumes a saw-toothed shape.

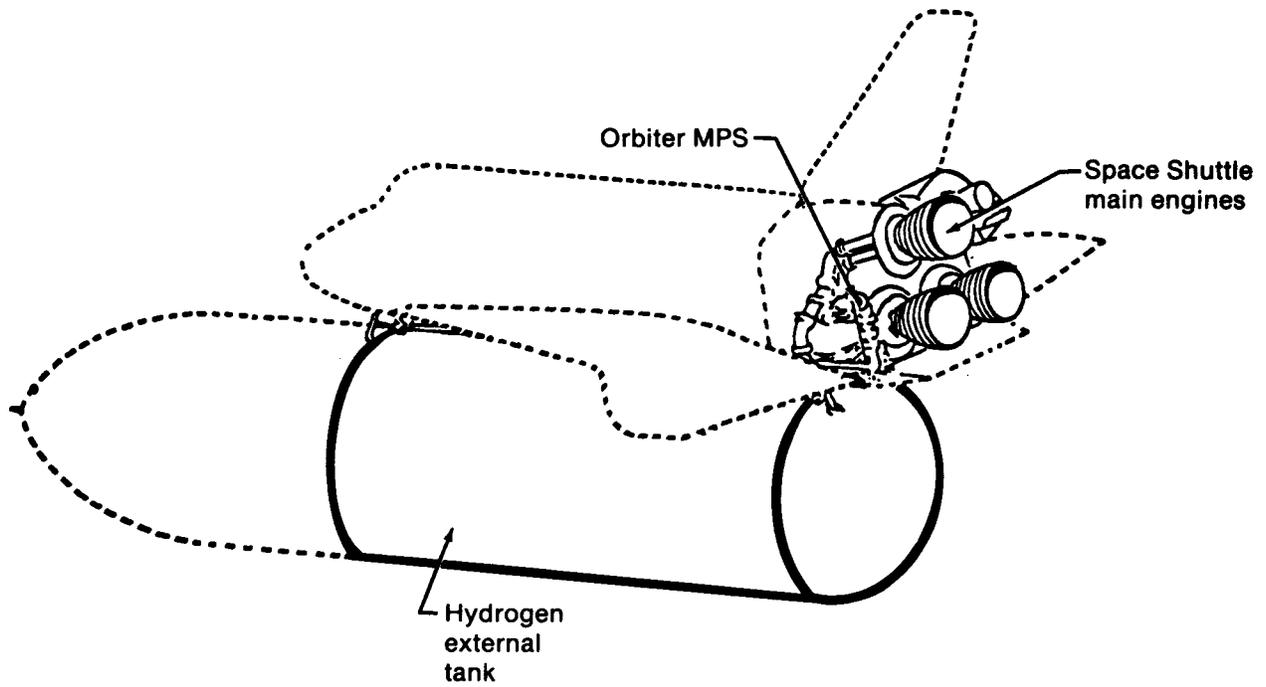


Figure 7.- Challenger integrated propulsion system.

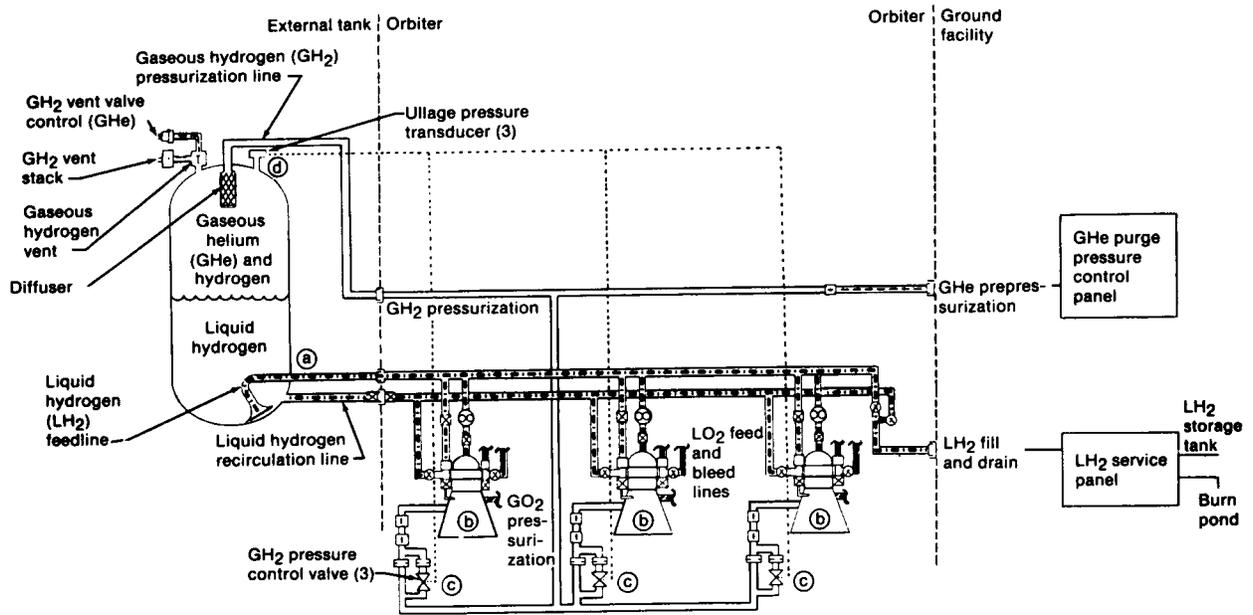


Figure 8.- Schematic of hydrogen external tank repressurization system.

PRESSURIZATION MODEL DEVELOPMENT

Overview

The first objective of this study was to determine whether a leak was, in fact, present. By reconstructing hydrogen external tank ullage pressure, a comparison could be made with actual measured ullage pressure to rule out a control dynamics malfunction. This reconstruction could be used not only to determine whether a leak was present but also to determine its initiation time by noting the time at which actual and predicted pressures begin to deviate. The first step in the analysis was to create a pressurization model that would enable reconstruction of predicted ullage pressure.

Ullage Pressure Prediction

The pressure inside the ullage volume is defined by the ideal gas law as

$$P(t) = \frac{N(t)\bar{R} T_{bulk}(t)}{V(t)} \quad (1)$$

where

$N(t)$ = total moles of gas in ullage, kmol (lb-mol)

$T_{bulk}(t)$ = mean bulk ullage temperature, K (°R)

$V(t)$ = total ullage volume, m³ (ft³)

\bar{R} = universal gas constant, $\frac{\text{kN-m}}{\text{kmol-K}} \left(\frac{\text{ft-lbf}}{\text{lb-mol-}^\circ\text{R}} \right)$

If instantaneous quantities of V , N , \bar{R} , and T_{bulk} are known, predicted ullage pressure can be reconstructed.

Throughout ascent, as liquid hydrogen leaves the tank, pressurization gas expelled from the main engines is channeled continuously into the forward end of the ullage in the hydrogen external tank. Thus, ullage volume V is a function of the original volume of liquid hydrogen in the tank and the amount of liquid hydrogen which leaves the tank. Likewise, the total moles of gas in the ullage N is a function of the original number of moles of gas plus the number of moles of pressurization gas channeled into the forward ullage. Liquid hydrogen boiloff will add to the total moles of gas; however, without knowing the temperature profile within the ullage, the amount of boiloff is difficult to predict. Therefore, boiloff was not considered directly but is assumed to be constant between flights and inherently compensated for in a bulk temperature correlation.

Finally, although an ullage temperature measurement exists, this temperature is not indicative of a mean bulk temperature. The ullage compartment is somewhat stratified, and the temperatures of the relatively warm inlet pressurization gas and the cold liquid hydrogen form the bounds on temperature. A measured temperature, therefore, is strongly a function of the transducer location and does not indicate the bulk temperature accurately. Consequently, a mean bulk ullage temperature T_{bulk} , as opposed to a measured temperature, was determined for use in this analysis,

and it was derived from a correlation using data from STS 51-L (before 60 seconds) and from previous missions. The specifics of the correlation are addressed later in the subsection titled "Mean Bulk Ullage Temperature Correlation."

The fluid flow diagram of the pressurization system shown in figure 9 indicates the critical parameters needed to predict ullage pressure using the ideal gas law of equation (1). Calculated parameters were computed as follows:

1. Gaseous hydrogen mass flow rate $\dot{m}(t)$, kg/s (lbm/s), assuming choked-flow conditions

$$\dot{m}(t) = \frac{10.75PC_d A}{\sqrt{T}} \quad (\text{SI units}) \quad (2a)$$

$$\dot{m}(t) = \frac{0.14PC_d A}{\sqrt{T}} \quad (\text{English units}) \quad (2b)$$

where

<u>SI units</u>	<u>English units</u>
P = engine outlet pressure, kPa	P = engine outlet pressure, psia
T = engine outlet temperature, K	T = engine outlet temperature, °R
$C_d A$ = effective flow area, m ²	$C_d A$ = effective flow area, in ²
= 0.3662 cm ² (engine 1; valve open)	= 0.05676 in ² (engine 1; valve open)
= 0.0712 cm ² (engine 1; valve closed)	= 0.01103 in ² (engine 1; valve closed)
= 0.3659 cm ² (engine 2; valve open)	= 0.05671 in ² (engine 2; valve open)
= 0.0688 cm ² (engine 2; valve closed)	= 0.01066 in ² (engine 2; valve closed)
= 0.3636 cm ² (engine 3; valve open)	= 0.05636 in ² (engine 3; valve open)
= 0.0669 cm ² (engine 3; valve closed)	= 0.01037 in ² (engine 3; valve closed)

The derivation of equation (2) is given in appendix B. It was assumed that flight data indicated the position of each valve accurately.

2. Total moles of gas $N(t)$

$$N(t) = N_{He-ESC} + N_{H_2-ESC} + \int_{ESC}^t \frac{\dot{m}(t)dt}{2.016 \text{ kg/kmol}} \quad (\text{SI units}) \quad (3a)$$

$$N(t) = N_{He-ESC} + N_{H_2-ESC} + \int_{ESC}^t \frac{\dot{m}(t)dt}{2.016 \text{ lbm/lb-mol}} \quad (\text{English units}) \quad (3b)$$

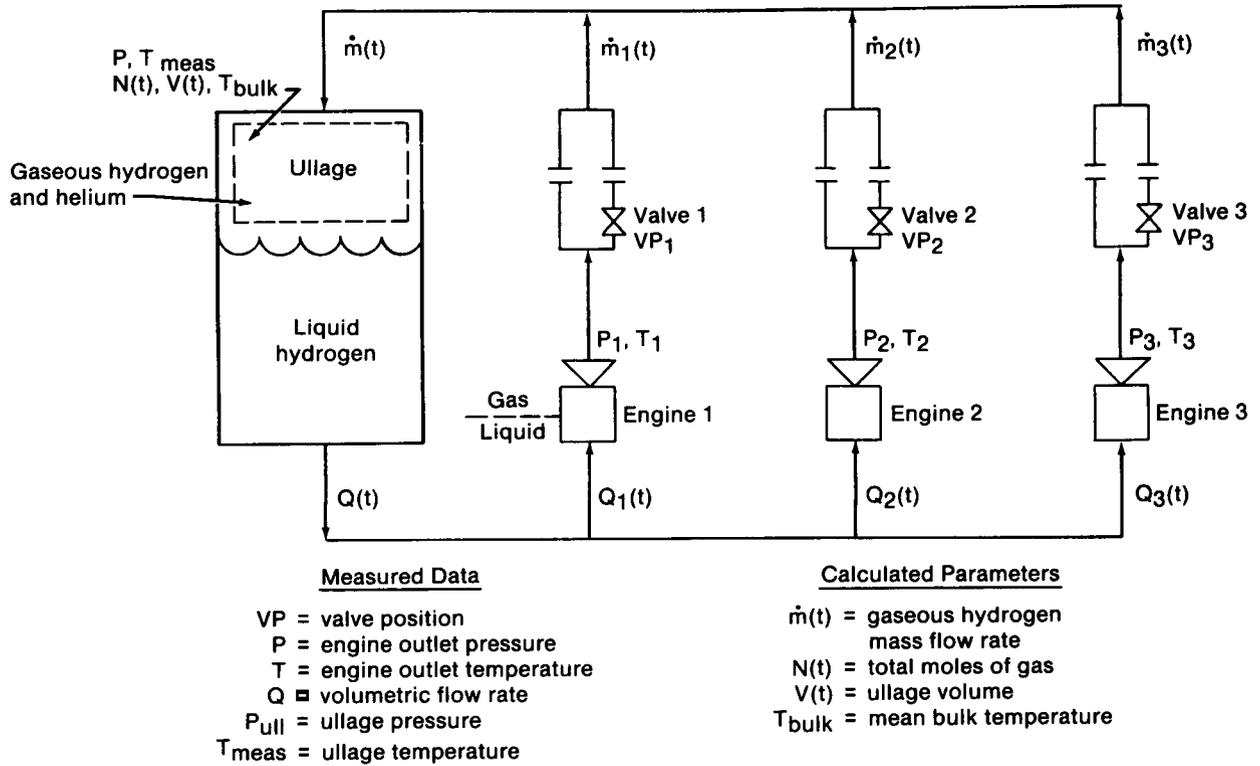


Figure 9.- Fluid flow schematic of hydrogen external tank repressurization system.

where

N_{He-ESC} = number moles gaseous He at engine start command, kmol (lb-mol)

N_{H_2-ESC} = number moles gaseous H₂ at engine start command, kmol (lb-mol)

$\dot{m}(t)$ = total gaseous hydrogen mass flow rate as a function of time, kg/s (lbm/s)

It is assumed that no net condensation and evaporation takes place between the ullage and the liquid hydrogen.

3. Ullage volume $V(t)$

$$V(t) = V_{ESC} + \int_{ESC}^t Q(t)dt \quad (4)$$

where

V_{ESC} = ullage volume at engine start command, m³ (ft³)

$Q(t)$ = total engine liquid hydrogen flow rate as a function of time, m³/s (ft³/s)

It is assumed that no net condensation and evaporation takes place between the ullage and the liquid hydrogen. Appendix C contains initial propellant load data used to determine the initial ullage volume prior to main engine ignition. Engine start command occurs 6 seconds before lift-off.

Initial Reconstruction

Using the technique just described, an initial reconstruction of predicted ullage pressure as a function of time was performed for STS flights 51-L, 61-A, and 51-F. Measured ullage temperature, as opposed to a derived mean bulk temperature, was used as the temperature parameter for an initial iteration. The purpose of this initial reconstruction was not to produce results but rather to provide trends that would later aid correlation of a mean bulk ullage temperature.

Results of the initial pressure reconstruction are shown in figures 10 to 12. As the figures indicate for all three flights, the predicted ullage pressure deviates by 3.4 kPa (0.5 psia) above actual ullage pressure at 30 seconds and increases to approximately 13.8 kPa (2.0 psia) above actual at 73 seconds. Since pressure is directly proportional to temperature, it follows that a difference in actual and predicted ullage pressure parallels the difference in required mean bulk ullage temperature and measured ullage temperature.

To investigate the relationship between the measured temperature and a required mean bulk ullage temperature further, a reconstruction of required mean bulk ullage temperature (required effective temperature) was performed. Required temperature was computed using actual ullage pressure and is equal to the temperature required to match predicted to actual ullage pressures. Results are shown in figures 13 to 15.

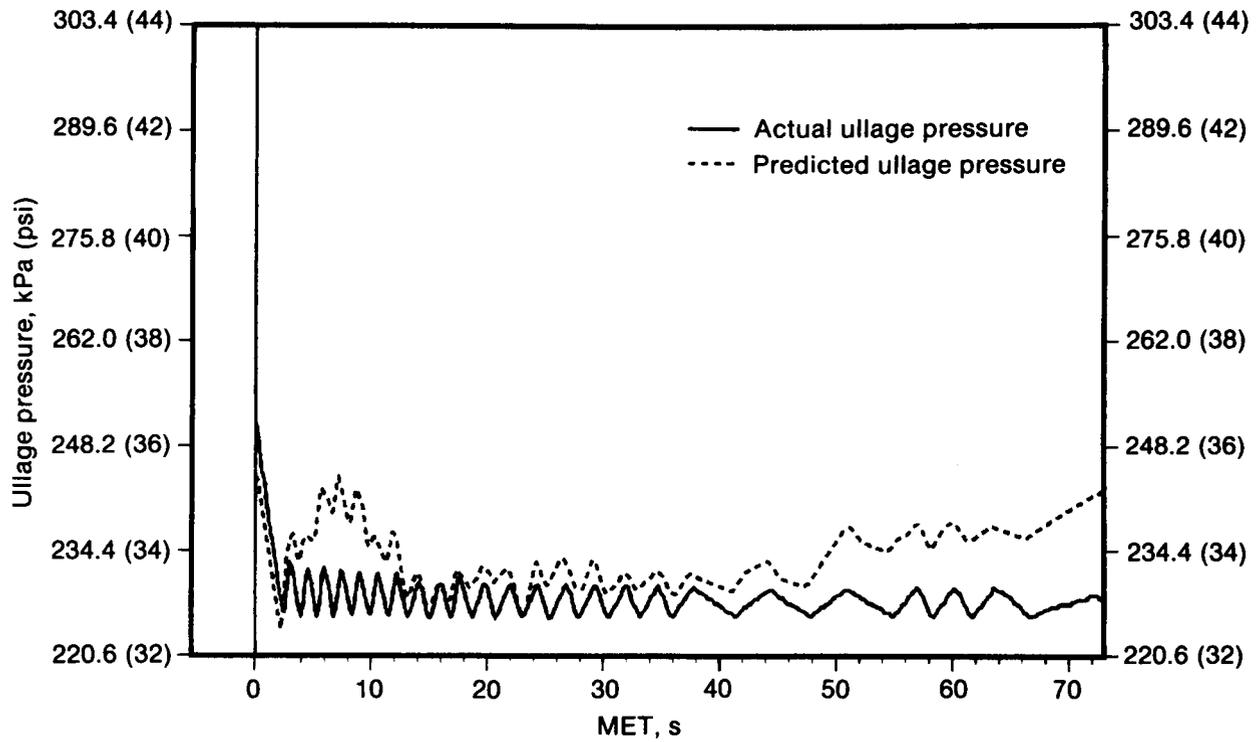


Figure 10.- STS 51-L initial ullage pressure reconstruction.

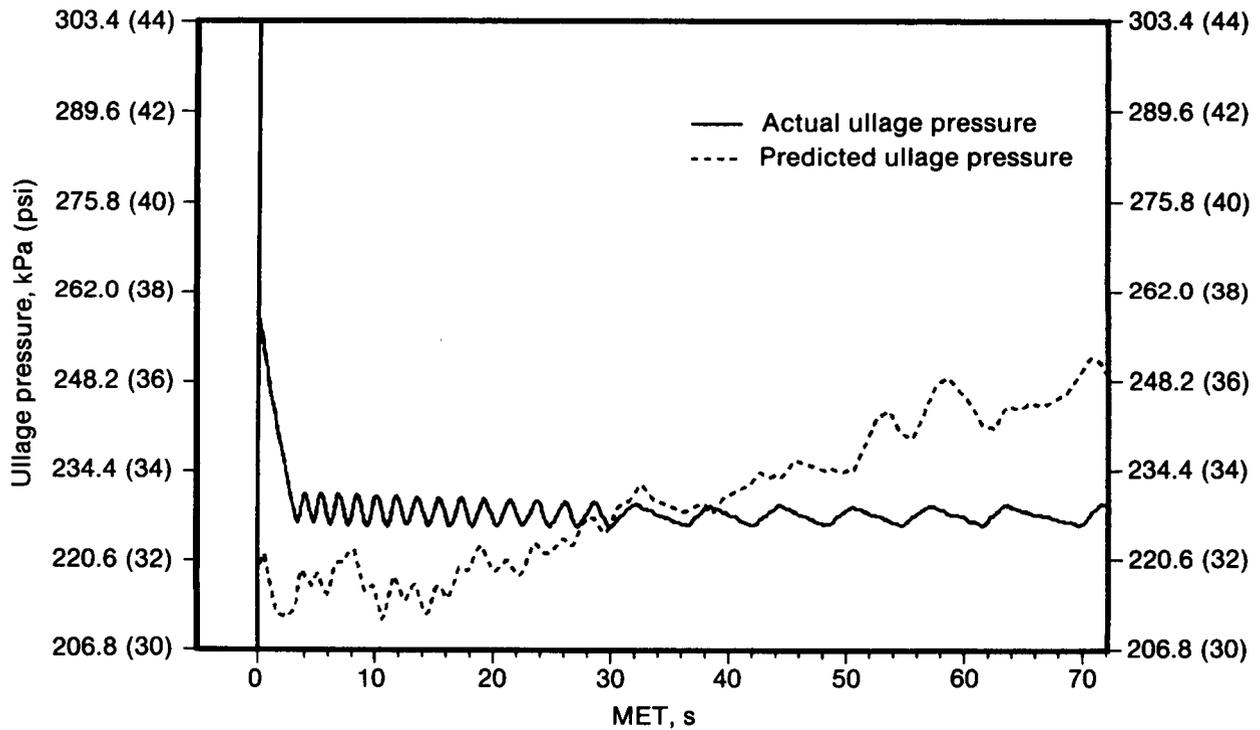


Figure 11.- STS 61-A initial ullage pressure reconstruction.

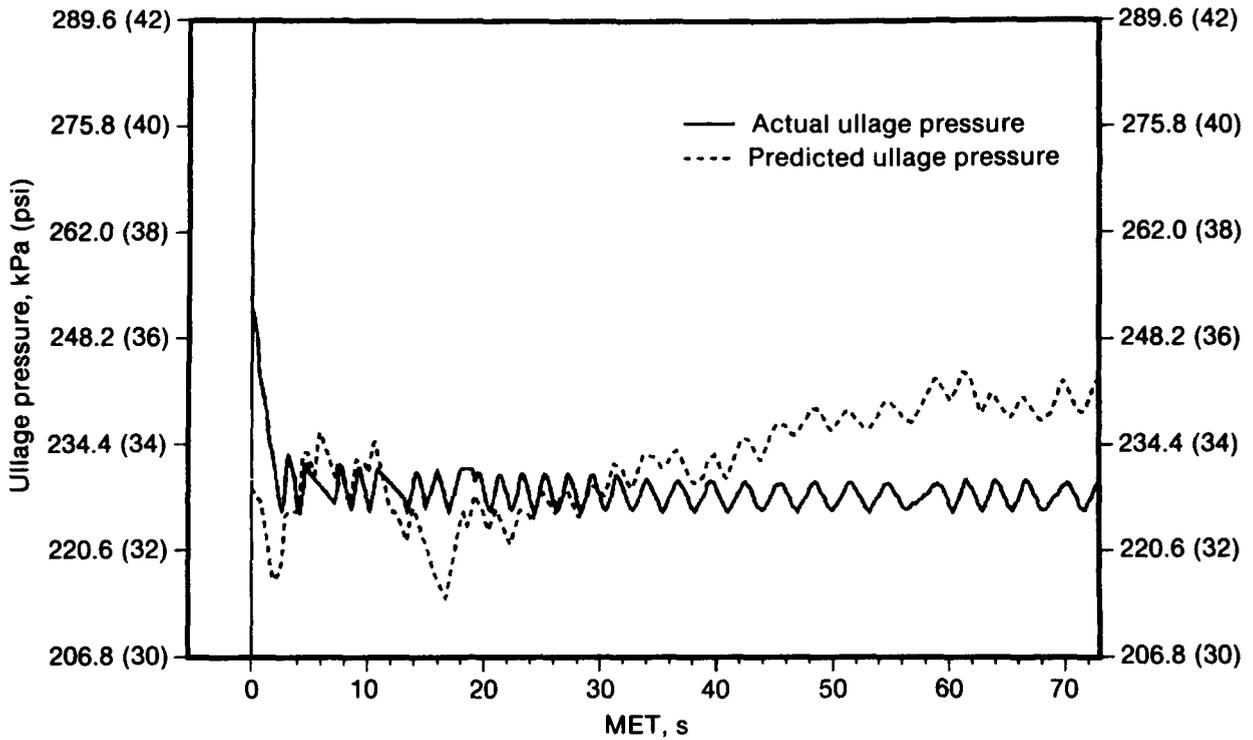


Figure 12.- STS 51-F initial ullage pressure reconstruction.

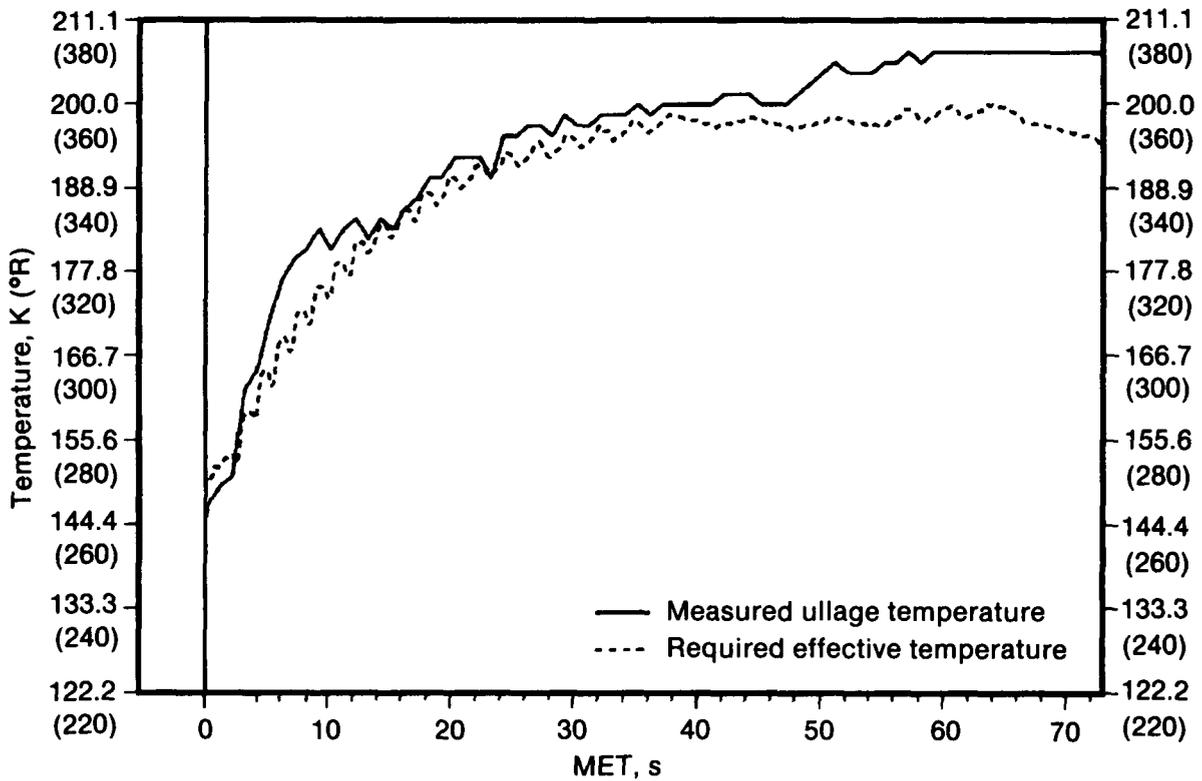


Figure 13.- STS 51-L comparison of measured and effective bulk temperature.

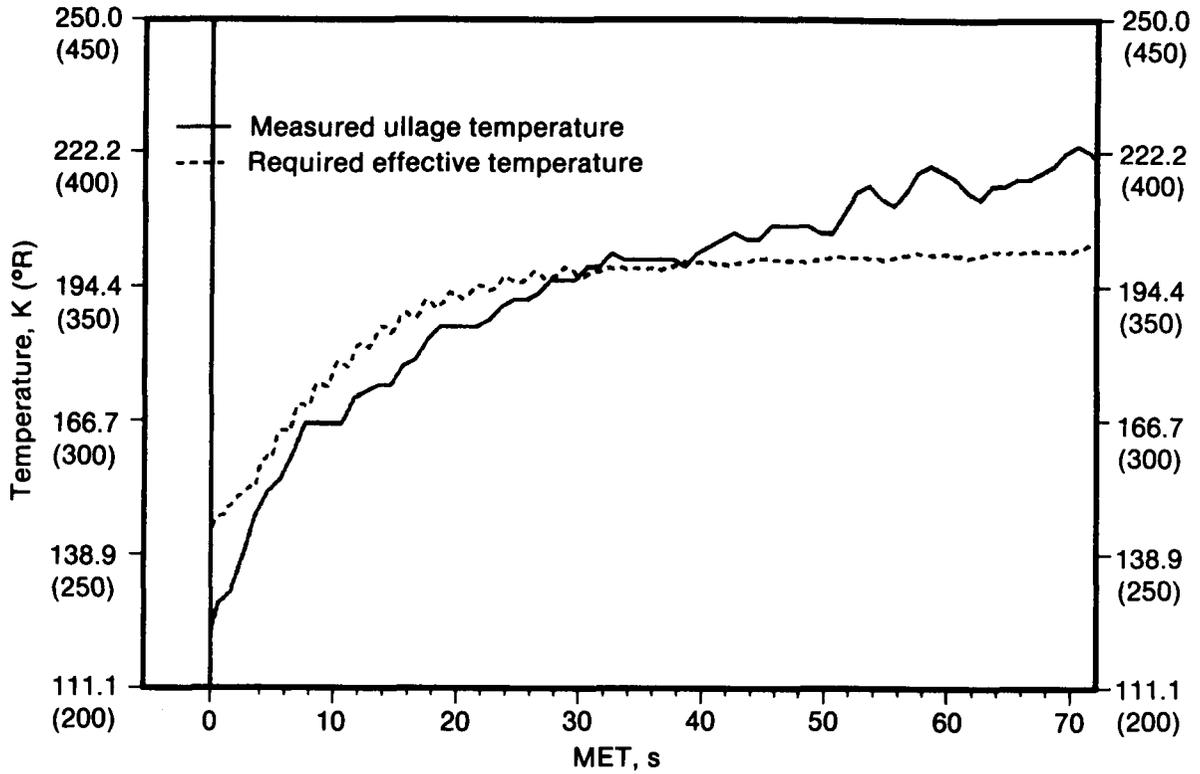


Figure 14.- STS 61-A comparison of measured and effective bulk temperature.

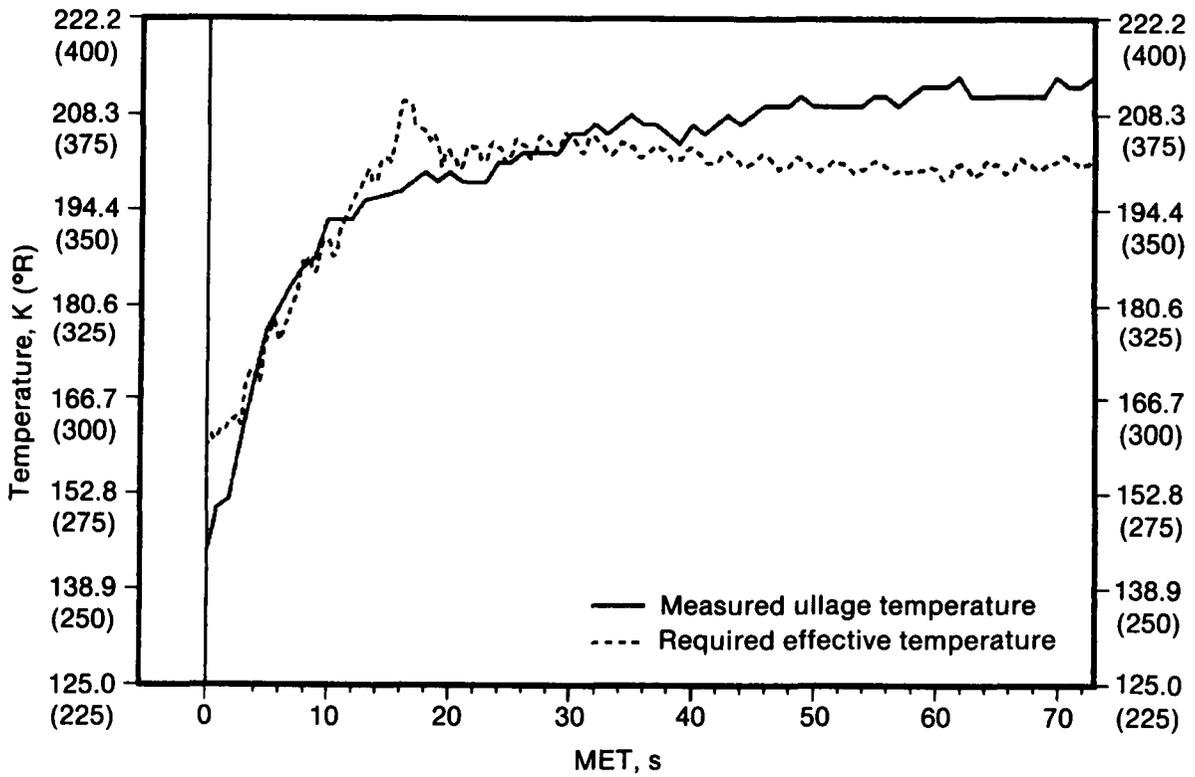


Figure 15.- STS 51-F comparison of measured and effective bulk ullage temperature.

Two important observations can be made. One, for all three flights, the required temperature, in a gross sense, follows a measured temperature. This result implies that, perhaps, a mean bulk temperature can be correlated with a measured temperature. Second, in all three flights, from approximately 30 to 70 seconds, the required effective temperature deviated from approximately 0 K (0° R) and decreased to 14 K (25° R) below measured temperature. Thus, time seems to affect the relationship between measured and bulk temperature as well. With trends of this initial reconstruction noted, the analysis proceeded with a focus on correlating a mean bulk temperature such that pressure reconstruction for STS flights 61-A and 51-F would coincide with the actual pressure history.

Computer programs used to perform engineering calculations on STS 51-L flight data are contained in appendix D.

Mean Bulk Ullage Temperature Correlation

The initial reconstruction phase of this analysis revealed that a mean bulk temperature required to make predicted pressure equal actual pressure appeared to be a function of both measured ullage temperature and time. With this first note, a simple correlation of bulk temperature as a function of measured temperature was derived for STS 51-F and reapplied to both STS 61-A and STS 51-L. Pressure was then reconstructed using $T_{bulk} = f(\text{measured temperature})$ as the temperature parameter in equation (1). This correlation, however, did not prove useful, and it was determined that measured temperature alone is an insufficient correlating parameter.

Since measured temperature alone was not adequate for correlation, the function of time needed to be assessed. It is reasonable to postulate that as the ullage volume expands, the location of the mean bulk temperature moves farther away from the location of measured temperature such that the mean bulk temperature becomes increasingly less than the measured temperature. Since ullage volume increases with time, it is not unreasonable to suggest that the function of volume on an effective bulk temperature might be manifested, in a rough sense, in the form of time. Furthermore, if the throttling profiles, which show the amount of liquid hydrogen leaving the tank as a function of time, of all three flights were the same, time alone would be a sufficient correlating parameter (for correlations between flights). The throttling profiles are different, however, and at any one time, the ullage volume for each of the three flights is not constant. Therefore, volume, not time, was chosen as the second correlating parameter.

With key correlating parameters of measured temperature and ullage volume chosen, a two-variable numerical fit for bulk ullage temperature was performed. The goal was to determine a single correlation using data from all three flights. The timeframe considered was from 40 to 60 seconds. The timeframe prior to 40 seconds was not considered since the period of interest was only during the final seconds of flight. Data for the period after 60 seconds were not used either since the MPS data for flight STS 51-L deviated from nominal during this timeframe.

The correlation was derived in a two-step fashion. First, a series of least squares fits of required bulk ullage temperature T_{bulk} (as determined in the initial reconstruction) as a function of measured temperature T was determined for constant ullage volume. The measured temperature used in these fits was calculated from a separate numerical fit performed to smooth out oscillatory flight data for this parameter. The following series of curve fits, one for each ullage volume considered, resulted. (They are shown in fig. 16.) The best fit describing the effect of measured temperature T on the bulk temperature T_{bulk} for constant ullage volume was linear.

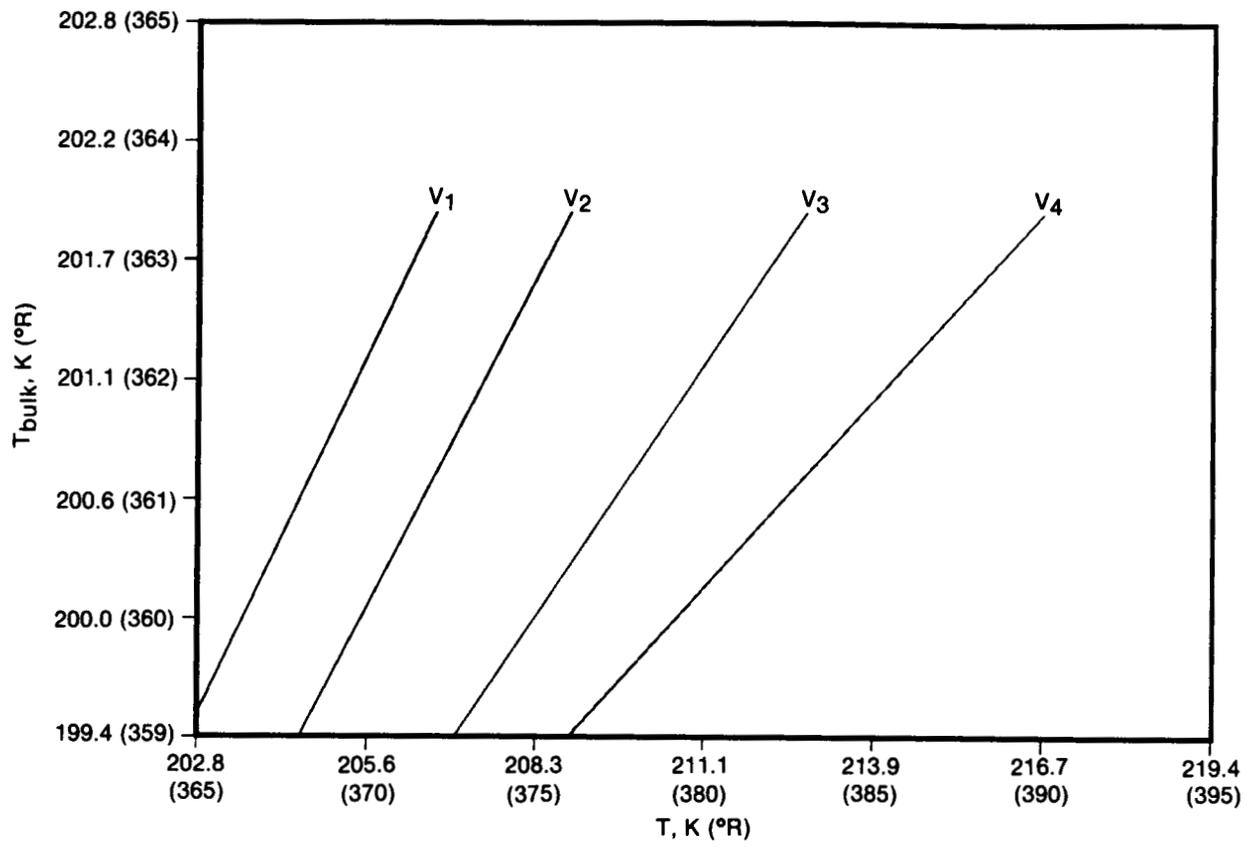


Figure 16.- Bulk ullage temperature correlation – two-variable fit (ullage volume constant).

$$\begin{aligned}
 V = V_1, T_{bulk} = f(T) &= \boxed{a_1} + \boxed{b_1} T \\
 V = V_2, T_{bulk} = f(T) &= \boxed{a_2} + \boxed{b_2} T \\
 V = V_3, T_{bulk} = f(T) &= \boxed{a_3} + \boxed{b_3} T \\
 V = V_4, T_{bulk} = f(T) &= \boxed{a_4} + \boxed{b_4} T
 \end{aligned}$$

A least squares fit was performed on the coefficients of this series of curves to determine the effect of volume. The result was a curve fit for the same order coefficients derived previously, and it is shown in figure 17.

$$A = f(V) = f(a_1, a_2, a_3, a_4)$$

$$B = f(V) = f(b_1, b_2, b_3, b_4)$$

Finally, the two-curve fits were combined resulting in a correlation of mean bulk temperature as a function of ullage volume and measured temperature.

$$T_{bulk} = A + BT, \quad T = K (^{\circ}R) \quad (5)$$

where

$$A = -138.331 + 1.558 \times V, \quad V = m^3$$

$$B = 2.993 - 0.203 \sqrt{V}, \quad V = m^3$$

$$A = -248.9958 + 7.955 \times 10^{-2} V, \quad V = ft^3$$

$$B = 2.993 - 3.4105 \times 10^{-2} \sqrt{V}, \quad V = ft^3$$

This correlation is based on the assumption that ullage volume and measured inlet temperature are the significant parameters in correlating a mean bulk temperature. Factors such as acceleration, vibration, and fluid properties, among others, are important heat-transfer parameters as well. However, these additional parameters were assumed to be constant between flights and/or to be less significant. As demonstrated in the final reconstructions, the accuracy of the derived correlation, which is based upon measured temperature and ullage volume alone, supports this assumption.

Final Reconstruction

With a correlation of mean bulk temperature determined, a final pressure reconstruction was performed for each flight. The correlation, derived for flight data from 40 to 60 seconds, was reapplied from 40 to 73 seconds. For the time period of 40 to 60 seconds, the term T in equation (5) was calculated from the fit of measured temperature as a function of time used in deriving the correlation. For the time period of 60 to 73 seconds, a least squares fit of measured temperature was not performed and actual measured temperature data were used for T in equation (5). If a curve fit of measured temperature had been performed for this latter period, the reconstructed pressure would most likely match the actual more closely for STS flights 61-A and 51-F. This procedure, however, would have no effect on flight STS 51-L results since the temperature was constant from 60 to 73 seconds.

Results of the reconstructions are shown in figures 18 to 20. Note that for STS flights 51-F and 61-A, reconstructed ullage pressure closely matches ± 1.7 kPa (± 0.25 psia), the actual throughout the 40- to 73-second period, verifying the analysis. Flight STS 51-L, on the other hand, only matches closely from 40 to approximately 64 seconds, at which time a discrepancy between the predicted and the actual pressure occurs.

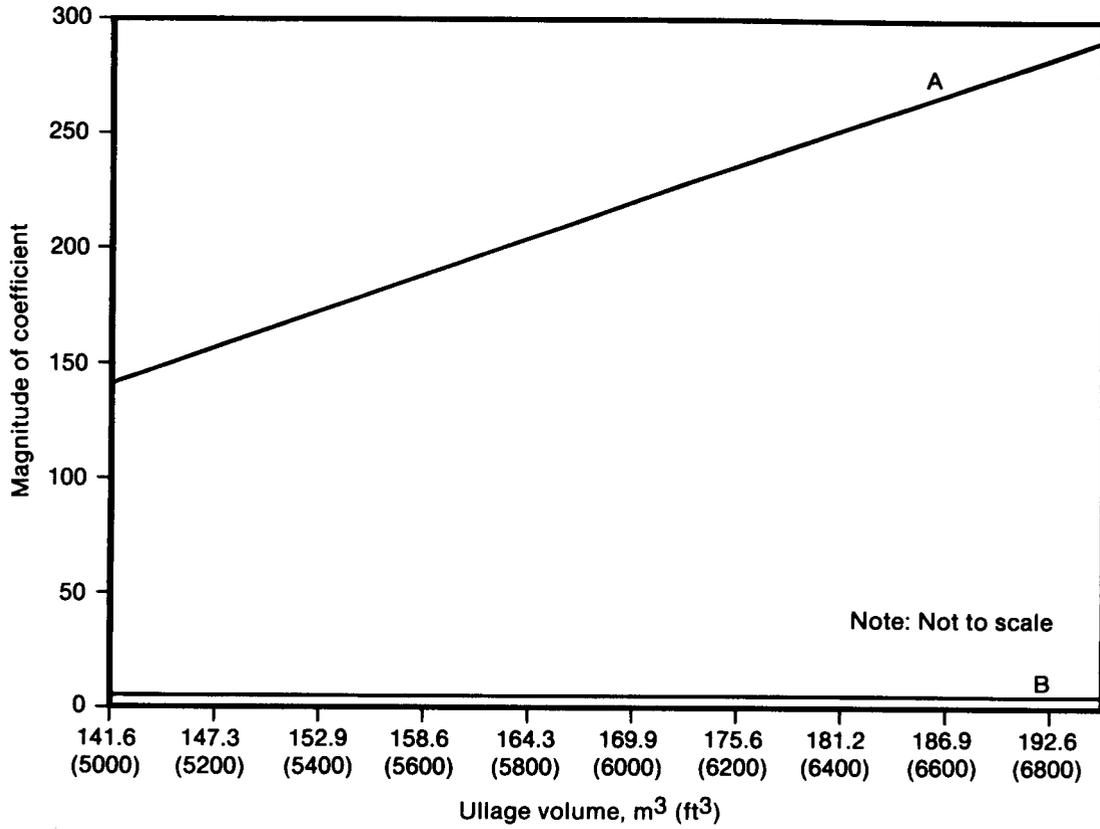


Figure 17.- Bulk ullage temperature correlation — two-variable fit (degree of coefficient constant).

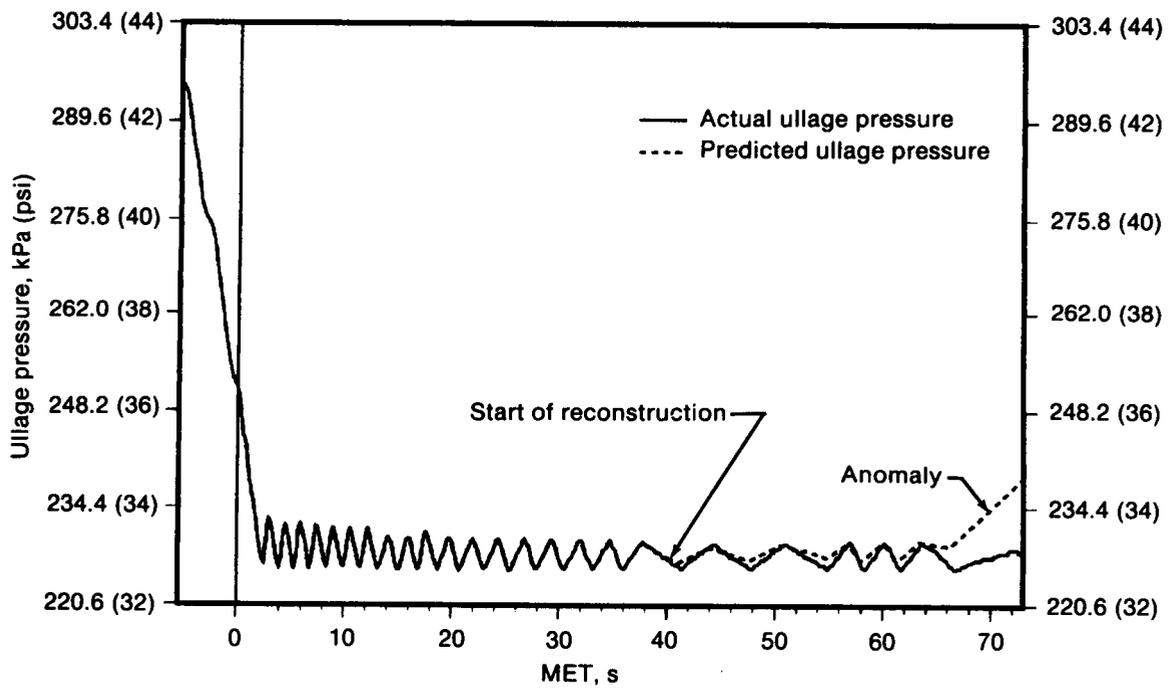


Figure 18.- STS 51-L ullage pressure reconstruction using mean bulk ullage temperature.

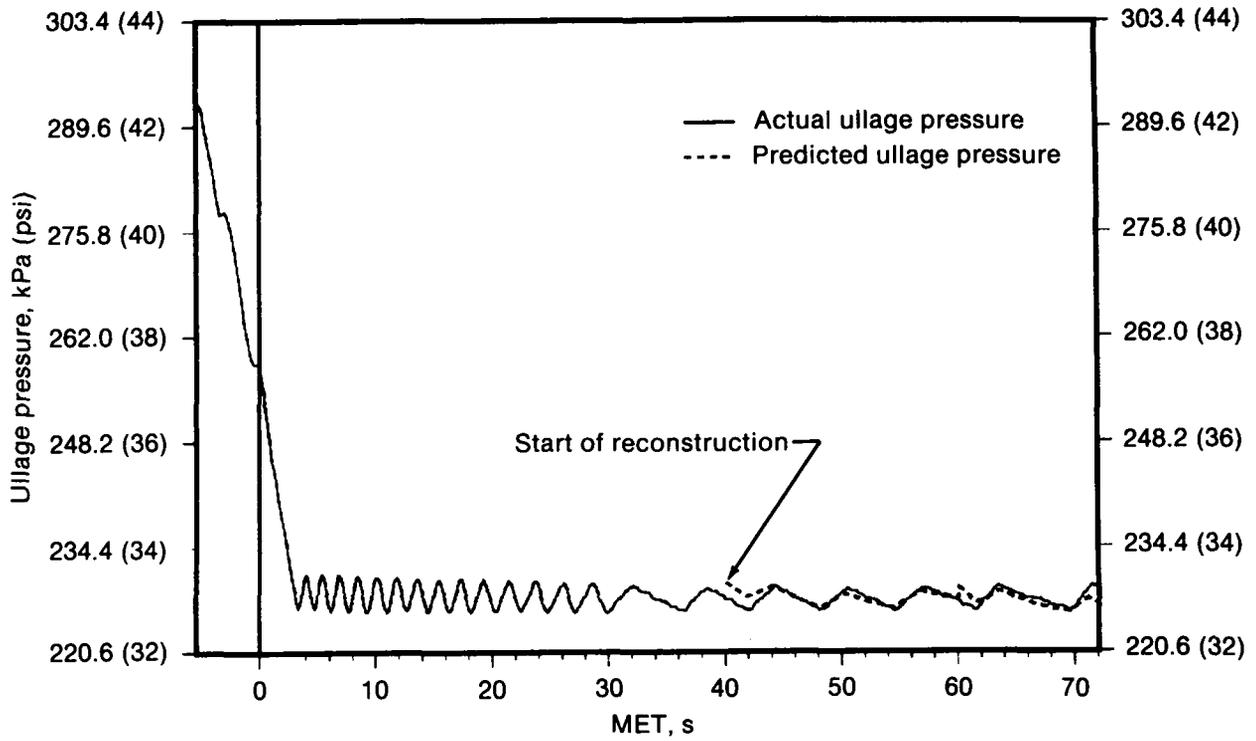


Figure 19.- STS 61-A ullage pressure reconstruction using mean bulk ullage temperature.

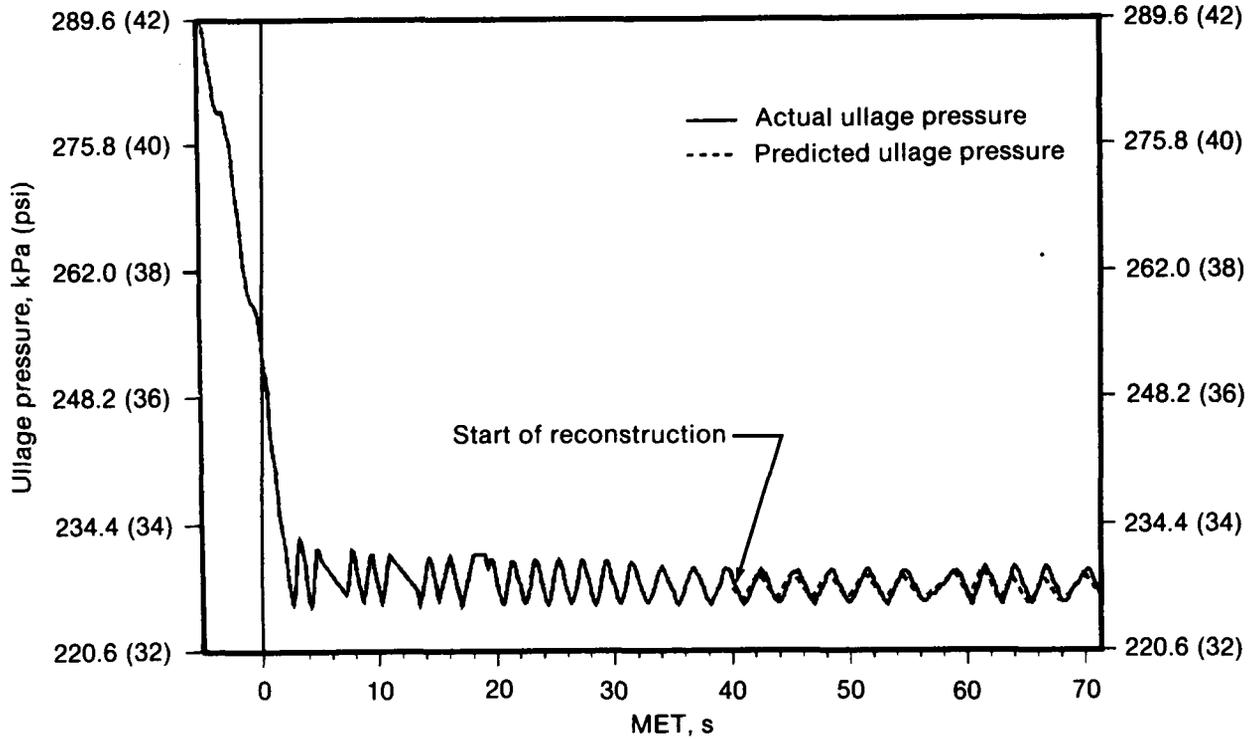


Figure 20.- STS 51-F ullage pressure reconstruction using mean bulk ullage temperature.

INVESTIGATION OF THE STS 51-L ULLAGE PRESSURE ANOMALY –
DISCUSSION OF ANALYSIS

INTERPRETATION OF PRESSURE DECAY

An enlargement of the observed anomaly in ullage pressure for flight STS 51-L is shown in figure 21. The match between predicted and actual ullage pressure from the time of correlation to approximately 64 seconds is consistent with a no-leak assumption. This result implies that the actual masses entering and leaving the tank are accounted for and are equal to those calculated from measured quantities. However, in the 64- to 73-second timeframe, there is a discrepancy between actual and reconstructed pressure. The reconstructed pressure becomes increasingly greater than the actual pressure with a maximum deviation of approximately 11.7 kPa (1.7 psia) occurring at 73 seconds.

The fact that the actual ullage pressure was lower than the predicted ullage pressure can be explained if either of two phenomena occurred. One argument is that the ullage volume is increased faster than is indicated by the volume calculated from measured quantities of liquid hydrogen leaving the tank. This explanation is consistent with a liquid hydrogen leak in the tank. Figure 22 depicts the amount of liquid hydrogen measured to have exited the external tank (based on engine flowmeter data) and the amount of liquid predicted to have exited in order to recreate the actual pressure. As observed, in the 64- to 73-second timeframe, more liquid would need to exit than measured in order to match the actual and predicted ullage pressures. The difference can be accounted for by postulating a liquid leak.

A second possibility is that there was no liquid leak but, rather, that the amount of gas pumped back into the ullage was less than measured to have passed through the flow control orifices. This theory is consistent with a gas leak either in the pressurization line or in the tank itself. Figure 23 indicates the measured amount of hydrogen gas recirculated to the ullage and the amount of hydrogen gas predicted to have entered the tank in order to recreate the actual pressure. As observed, the net increase in hydrogen gas in the tank predicted, based on the measured pressure, is less than the amount of gas that was supposed to have entered based on the flow measured to have passed through the flow control orifices along the path to the external tank. The difference can be accounted for by postulating a gas leak.

With the existence of a leak, either gas or liquid, confirmed, the initiation time has to be determined. Ullage pressure reconstruction begins to show a deviation between actual and predicted in the timeframe of 64 to 66 seconds. The leak initiation time can be established more accurately by determining when the pressure discrepancy exceeds the sensitivity of the analysis. Here, an assumed gas leak is used to plot the difference between expected and predicted masses (fig. 24) as a function of time. In the time period considered, this difference oscillates between -0.23 kg (-0.5 lbm) and $+0.45$ kg ($+1.0$ lbm). If this range is considered a limit on the sensitivity of the analysis, 65.5 seconds is the earliest time at which a leak can be confirmed.

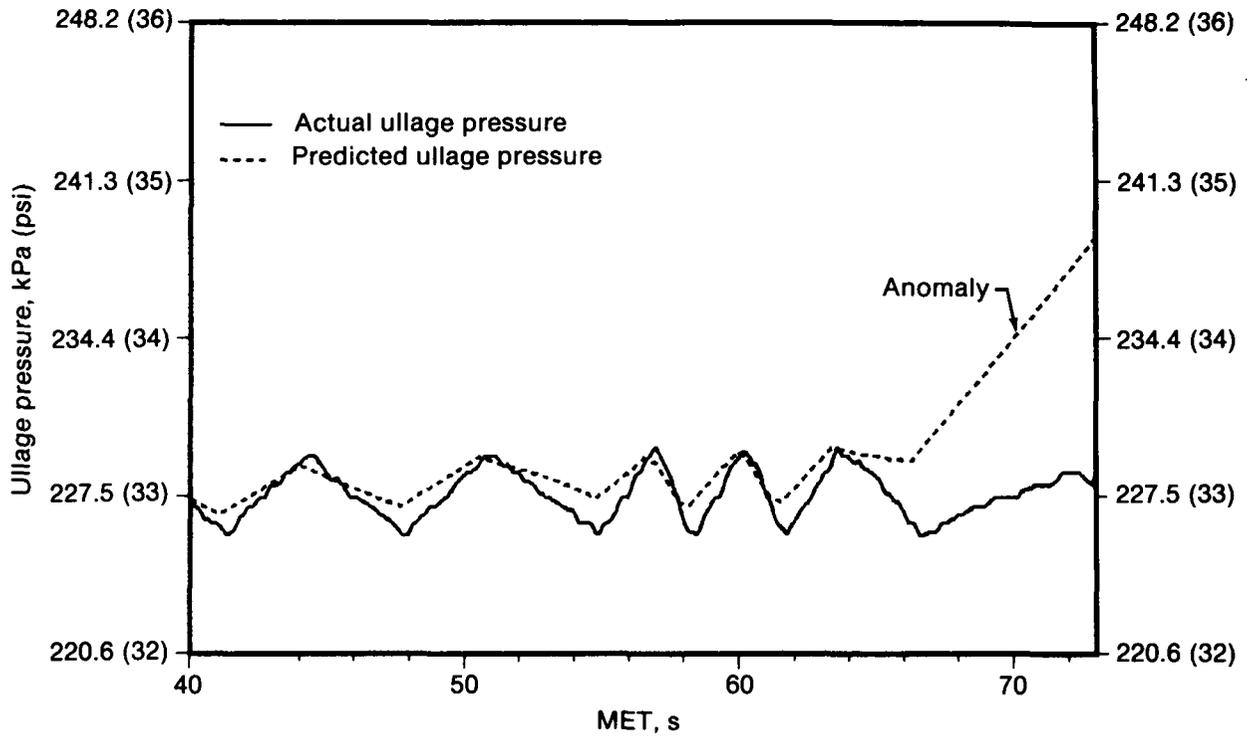


Figure 21.- STS 51-L ullage pressure anomaly.

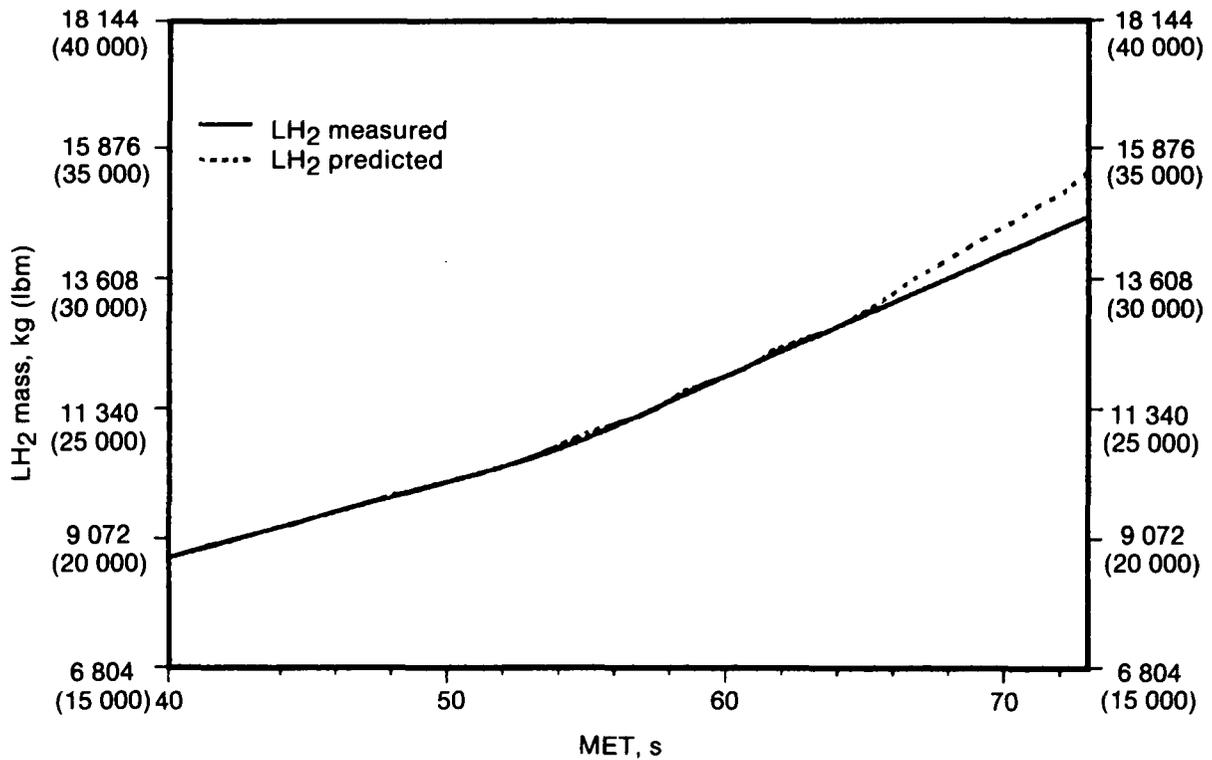


Figure 22.- Comparison of predicted and measured liquid hydrogen consumption.

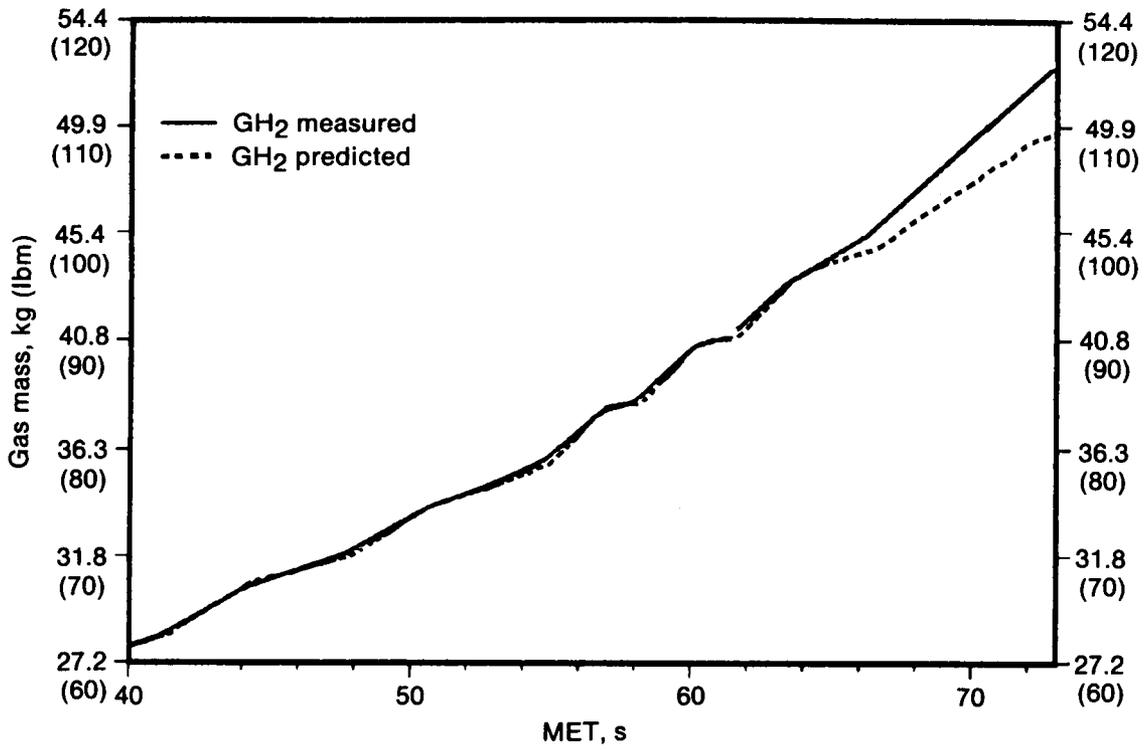


Figure 23.- Comparison of predicted and measured gaseous hydrogen repressurization gas into ullage.

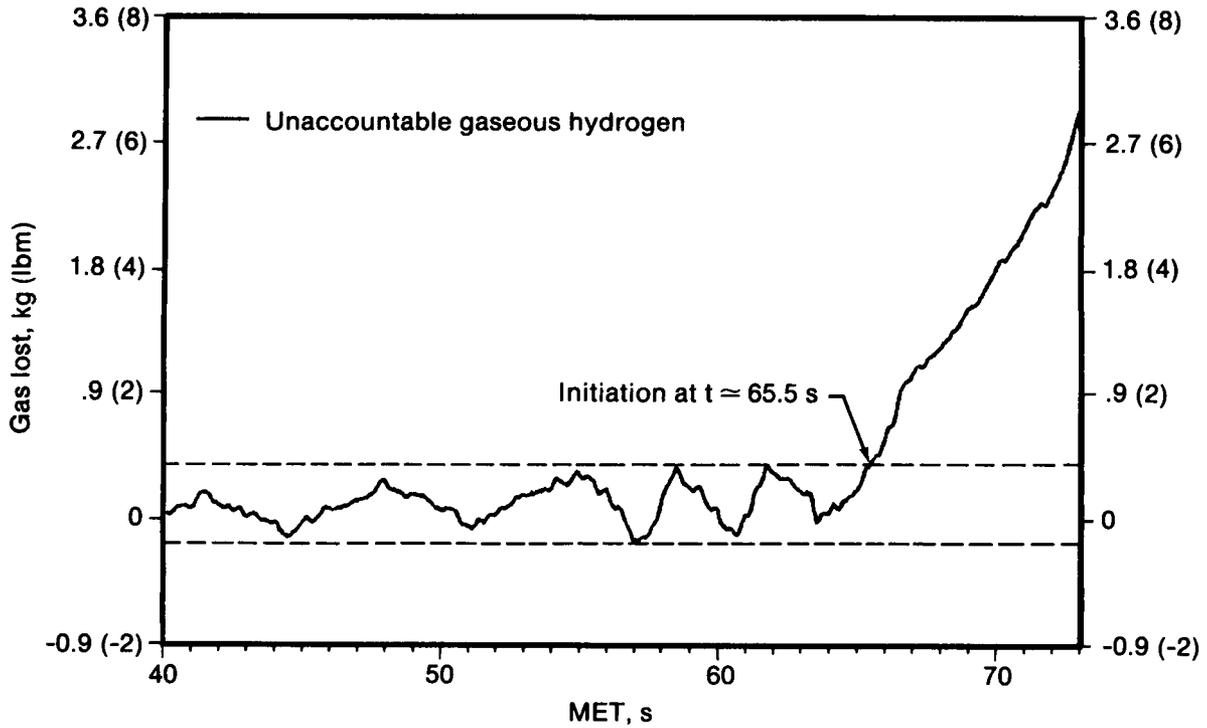


Figure 24.- Analysis sensitivity and leak initiation time.

MAGNITUDE OF LEAK

With the presence of a leak and the initiation time determined, the next objective was to estimate the magnitude of the leak. This analysis focuses on a comparison of predicted and actual pressure rise and decay rates. A plot of measured ullage pressure for one of the three pressure transducers during the last several seconds of flight is shown in figure 25. These empirical pressure rise and decay rates can, alternatively, be described analytically by taking the partial derivative of the ideal gas law with respect to time.

$$\frac{\partial}{\partial t} (P) = \frac{\partial}{\partial t} \left(\frac{N\bar{R}T}{V} \right) \quad (6)$$

Differentiating with respect to time,

$$\begin{aligned} \frac{\partial P}{\partial t} = N\bar{R}T \frac{\partial}{\partial t} \left(\frac{1}{V} \right) + \frac{N\bar{R}}{V} \left(\frac{\partial T}{\partial t} \right) \\ + \frac{\bar{R}T}{V} \left(\frac{\partial N}{\partial t} \right) + \frac{NT}{V} \left(\frac{\partial \bar{R}}{\partial t} \right) \end{aligned} \quad (7)$$

where

$$N\bar{R}T \frac{\partial}{\partial t} \left(\frac{1}{V} \right) = \frac{-P}{V} Q \quad (8)$$

$$\frac{N\bar{R}}{V} \left(\frac{\partial T}{\partial t} \right) = \frac{P}{T} \left(\frac{\partial T}{\partial t} \right) \quad (9)$$

$$\frac{\bar{R}T}{V} \left(\frac{\partial N}{\partial t} \right) = \frac{P}{N_{H_2} + N_{He}} \left(\frac{\dot{m}_{H_2}}{MW_{H_2}} \right) \quad (10)$$

$$\frac{NT}{V} \left(\frac{\partial \bar{R}}{\partial t} \right) = 0 \quad (11)$$

Substituting equations (8) to (11) into equation (7),

$$\frac{\partial P}{\partial t} = \frac{-PQ}{V} + \frac{P}{T} \left(\frac{\partial T}{\partial t} \right) + \frac{P}{N_{H_2} + N_{He}} \left(\frac{\dot{m}_{H_2}}{MW_{H_2}} \right) \quad (12)$$

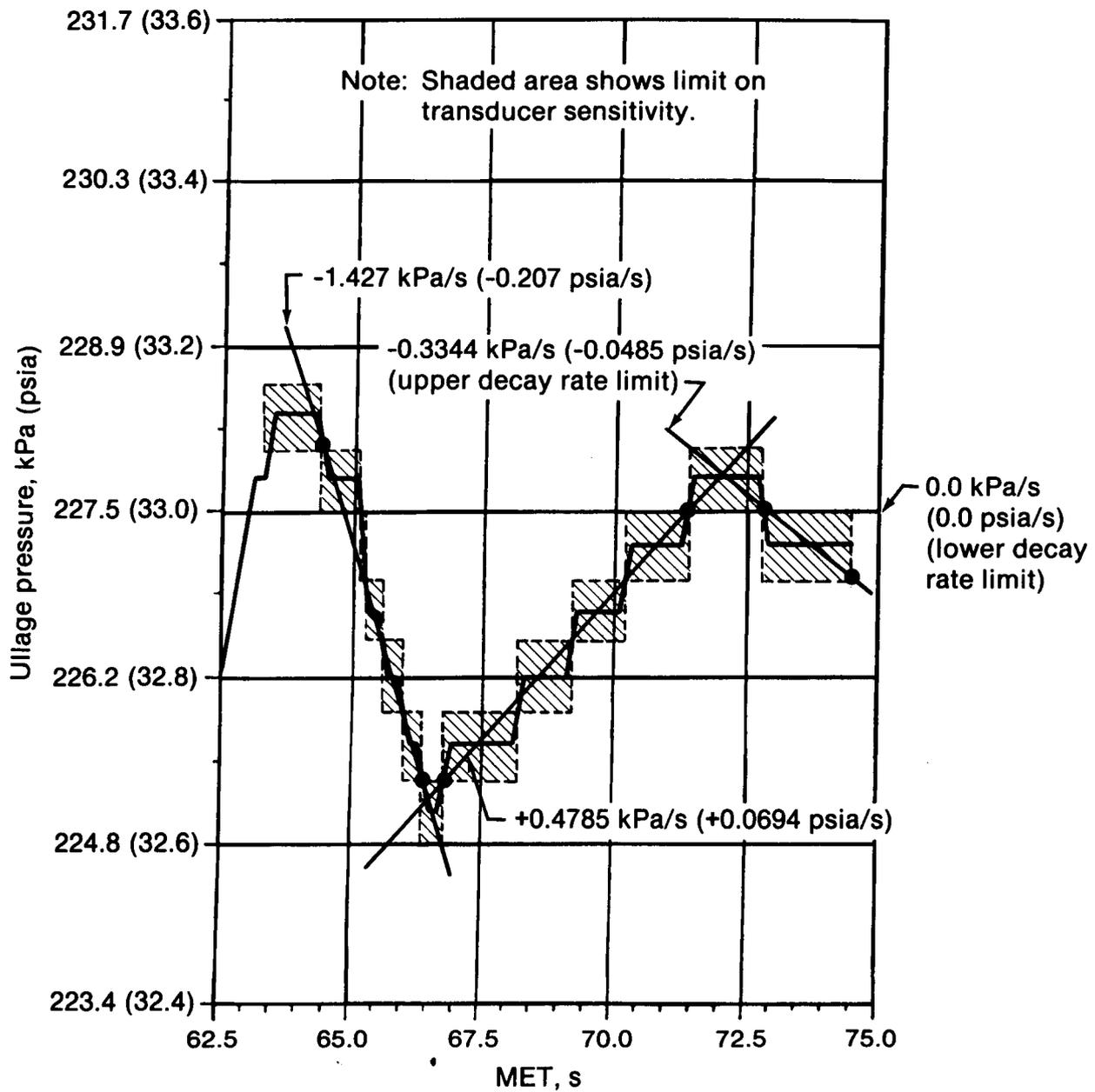


Figure 25.- Example of empirical ullage pressure rise and decay rates for transducer number 3.

Linearizing for each pressure rise and decay,

$$\frac{\Delta P}{\Delta t} = \left(\frac{-P_{av}}{V_{av}} \right) Q_{av} + \frac{P_{av}}{T_{av}} \left(\frac{\Delta T}{\Delta t} \right) + \frac{P_{av}}{(N_{H_{2av}} + N_{He})} \left(\frac{\dot{m}_{H_{2av}}}{MW_{H_2}} \right) \quad (13)$$

where

- P_{av} = average ullage pressure
- V_{av} = average ullage volume
- Q_{av} = average LH₂ volumetric flow rate
- ΔT = change in bulk ullage temperature
- $N_{H_{2av}}$ = total average moles gaseous hydrogen
- $\dot{m}_{H_{2av}}$ = average gaseous H₂ mass flow rate
- N_{He} = total moles gaseous helium
- MW_{H_2} = molecular weight of hydrogen

Using equation (13), it is possible to predict the ullage pressure rise and decay rates. For a no-leak scenario, equality between empirical pressure rise and decay rates and the rates predicted from equation (13) would be expected. Physically, this theory infers that the rate of pressurization observed should match the rate of pressurization predicted from ullage mass and volume dynamics. An inequality arises when the ullage volume and repressurization gas quantities inferred from independent flight measurements (such as liquid hydrogen fuel consumption or downstream repressurization gas flow rates) and used in equation (13) differ from the actual. This inequality can be corrected, however, if one incorporates either a gas leak (\dot{m}_{1k} denotes mass flow rate) or a liquid leak (Q_{1k} denotes volumetric flow rate) into the gas and volume terms of equation (13), respectively. Solving implicitly for leaks using this method, one can compute the leak rate as shown in the following equations.

Gas Leak (Assumes No Liquid Leak)

$$\dot{m}_{1k} = (A + B + C + D) \left(N_{He} + N_{H_{2av}} - \frac{\dot{m}_{1k} \Delta t}{2 \times 2.016} - \sum_{i=1} \frac{\dot{m}_{1k}}{2.016} \right) \quad (14)$$

where

$$A = - \left. \frac{\Delta P}{\Delta t} \right| \quad \text{actual}$$

$$B = - \frac{P_{av} Q_{av}}{V_{av}}$$

$$C = \frac{P_{av}}{T_{av}} \left(\frac{\Delta T}{\Delta t} \right)$$

$$D = \frac{P_{av} \dot{m}_{av}}{\left(N_{He} + N_{H_{2av}} - \frac{\dot{m}_{1k} \Delta t}{2 \times 2.016} - \sum_{i-1} \frac{\dot{m}_{1k}}{2.016} \right)}$$

(Note: "i - 1" refers to all time before the timeframe currently considered.)

Liquid Leak (Assumes No Gas Leak)

$$Q_{1k} = (A + B + C + D) \left(\frac{V_{av} + Q_{1k} \Delta t}{2} + \sum_{i-1} V_{1k} \right) \quad (15)$$

where

$$A = \left. \frac{\Delta P}{\Delta t} \right| \quad \text{actual}$$

$$B = \frac{P_{av}}{T_{av}} \left(\frac{\Delta T}{\Delta t} \right)$$

$$C = \frac{P_{av}}{N_{tot}} \left(\frac{\dot{m}_{av}}{MW_{H_2}} \right)$$

$$D = \frac{-P_{av} Q_{av}}{\left(V_{av} + \frac{Q_{1k} \Delta t}{2} + \sum_{i-1} V_{1k} \right)}$$

and

$$\dot{m}_{1k} = Q_{1k} \times \rho_{LH_2} \quad (16)$$

where ρ is fluid density.

Results

Because the transducer sensitivity was limited to ± 0.55 kPa (± 0.08 psia), an estimation of the empirical pressurization rise and decay rates from the data in figure 25 was made difficult. Given, however, that the sampling rate was high enough to indicate the time at which each pressure transducer setpoint was exceeded, it was assumed that the midpoints of each vertical step and the associated time defined real points of time and pressure. These points were used to calculate average pressure rise and decay rates from the start to the end of each of the three time intervals considered. In the last timeframe (72.0 to 72.9 seconds), few data points exist to estimate a pressure rise and decay rate. In this case, an upper and a lower bound on decay rate were determined and are shown in figure 25. Finally, a similar procedure was performed for each of three transducers, and the standard deviation σ was computed to estimate the error based on the three measurements. (The underlying assumption is that there is no error in calculating the rates for each transducer using the method just described.)

Gas and liquid leak rates were computed at an average time for each pressure rise and decay slope beginning with the slope from 63.5 to 66.7 seconds. Results are shown in figures 26 and 27 and in table 1. If a gas leak is assumed, calculations show an initial leak of 0.28 kg/s (0.62 lbm/s) \pm 41 percent at 65.5 seconds which increases to between 0.43 and 0.51 kg/s (0.94 and 1.12 lbm/s) \pm 1 percent in the 72- to 72.9-second time interval. If a liquid leak is assumed, calculations show an initial leak of 72.9 kg/s (160.5 lbm/s) \pm 41 percent at 65.5 seconds which increases to between 111.6 and 133.2 kg/s (245.8 and 293.3 lbm/s) \pm 1 percent in the 72- to 72.9-second time interval. In both cases, the leak remains relatively constant from 65.5 to 72.0 seconds and increases significantly at 72 to 73 seconds. This sharp increase in leak rates parallels the premature decay that was initially identified in the flight ullage pressure data of figure 1.

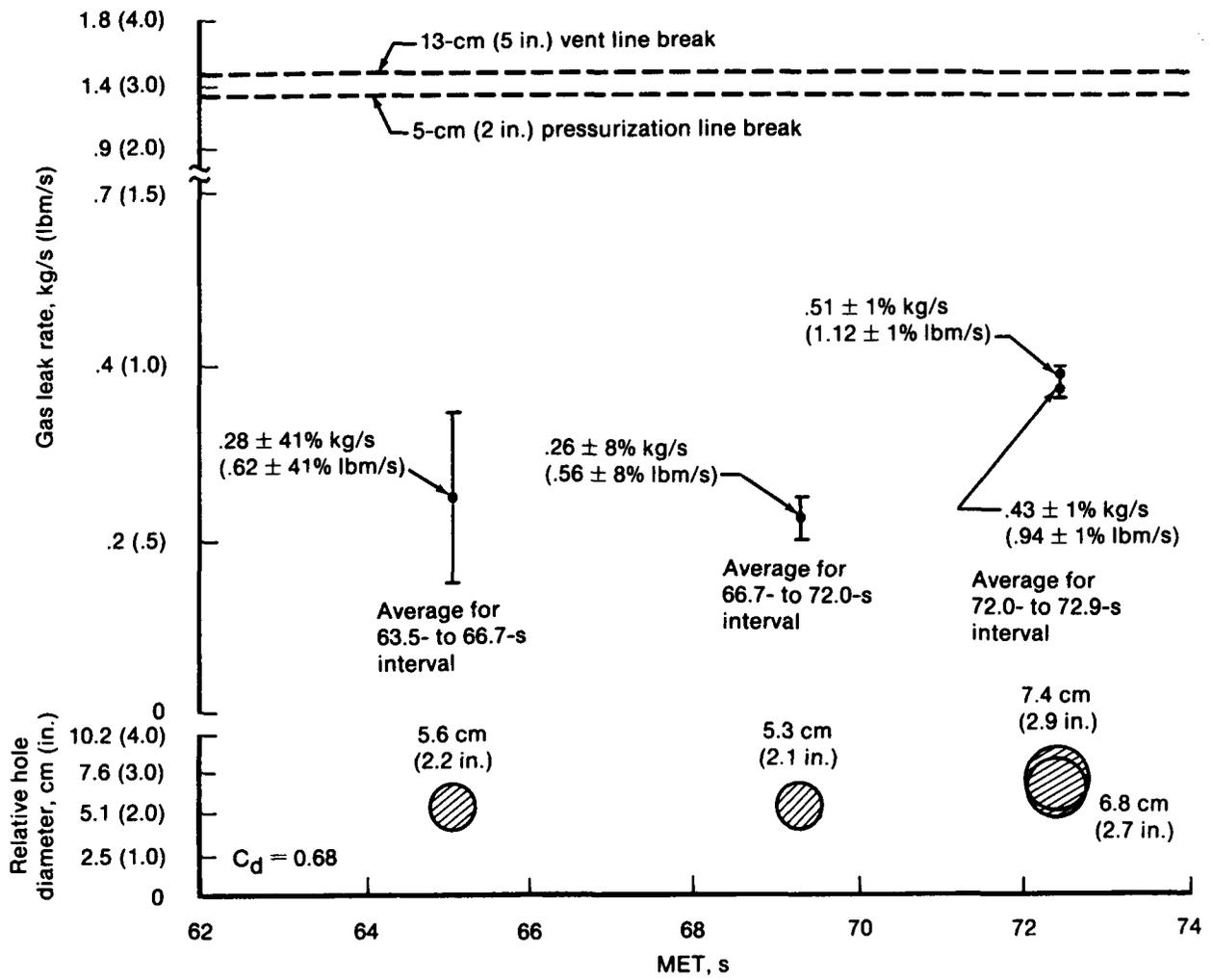


Figure 26.- Average gaseous hydrogen leak rate and relative hole size for each time interval.

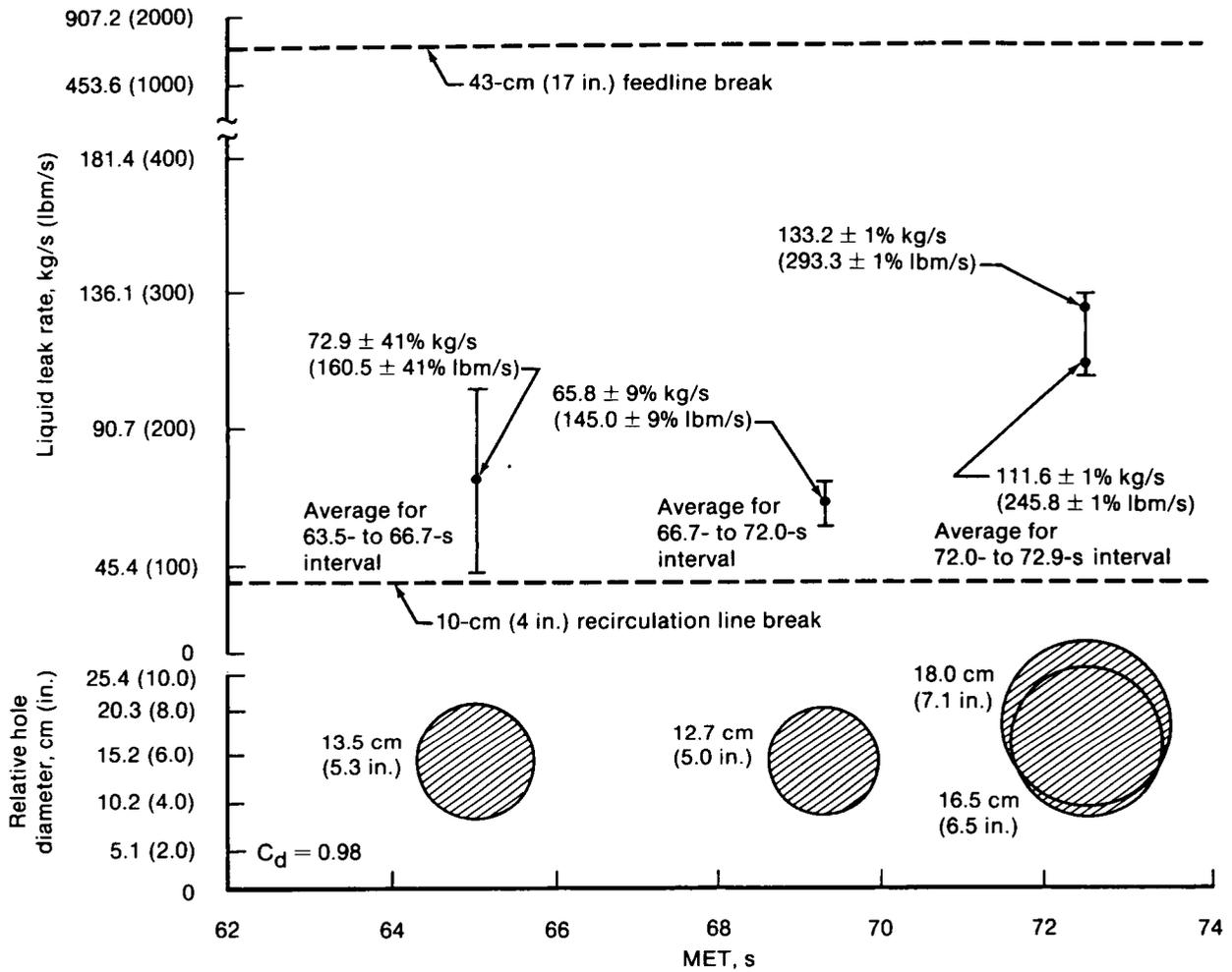


Figure 27.- Average liquid hydrogen leak rate and relative hole size for each time interval.

TABLE 1.- SUMMARY OF LEAK RATES

Parameter	Value for time interval, s, of -		
	63.5 to 66.7	66.7 to 72.7	72.0 to 72.9
			Lower limit Upper limit
Pressurization rate ^a measured, kPa/s (psia/s)	-0.937 (-0.192) ± 34%	0.356 (0.073) ± 11%	0.0 (0.0) -0.205 (-0.042) ± 1%
Gaseous hydrogen leak rate, kg/s (lbm/s)	0.28 (0.62) ± 41%	0.26 (0.56) ± 8%	0.43 (0.94) ± 1% 0.51 (1.12) ± 1%
Liquid hydrogen leak rate, kg/s (lbm/s)	72.9 (160.5) ± 41%	65.8 (145.0) ± 9%	111.6 (245.8) ± 1% 133.2 (293.3) ± 1%

^aError = 2.9σ.

EFFECTIVE FLOW AREA

If a gas leak is assumed, gas hole sizes can be computed using choked-flow equations. It was assumed that a leak occurred in the side of the tank at a location near the mean bulk ullage temperature and that flow at the hole was single phase. Thus, the effective leak area for a gas leak is

$$A = \frac{\dot{m}_{1k} \sqrt{T_{bulk}}}{10.75PC_d}, \left(\frac{\dot{m}_{1k} \sqrt{T_{bulk}}}{0.14PC_d} \right) \quad (17)$$

where

\dot{m}_{1k} = gas leak rate, kg/s (lbm/s)

T_{bulk} = mean bulk ullage temperature, K (°R)

P = ullage pressure, kPa (psia)

C_d = flow coefficient = 0.68 (jagged hole)

= 0.98 (clean pipe exit)

Hole sizes were computed for each leak rate assuming a jagged hole in the side of the tank ullage with a flow coefficient of 0.68. These hole sizes are shown in figure 26. An effective hole size for this case is approximately 5.6 centimeters (2.2 inches) \pm 23 percent in the 65.5- to 72.0-second time interval increasing to between 6.8 and 7.4 centimeters (2.7 and 2.9 inches) \pm 1 percent in the 72.0- to 72.9-second time interval. Hole sizes were also computed for a leak through a short stubbed tube ($C_d = 0.98$, clean pipe exit). Finally, to bound these calculations by considering the dependence of temperature on the location in the ullage, the term T_{bulk} in equation (17) was replaced with T_{meas} and T_{sat} . Hole sizes using T_{meas} were the same and those using a lower limit of T_{sat} resulted in a decrease in diameter of approximately 45 percent. Results of these calculations are summarized in table 2.

If a liquid leak is assumed, liquid hole sizes can be estimated using theory for two-phase critical flow through short tubes and orifices (refs. 1 to 3). The exit point was chosen to be in the vicinity of the liquid hydrogen feedline at the base of the tank since a structural failure at this location is plausible. The azimuthal orientation was determined to be insignificant. A schematic is shown in figure 28.

For a subcooled liquid undergoing two-phase critical flow through a short tube or an orifice, the critical flow rate and pressures are described by

$$\dot{m}_t = \left[\sqrt{\frac{(v_{ge} - v_{\ell o})}{g_c} \frac{N}{s_{ge} - s_{\ell e}} \frac{ds_{\ell e}}{dP}} \right]_t^{-1} A_t, N = \frac{x_{et}}{0.14} \quad (18)$$

and

$$P_t = P_o \left[1 - \frac{v_{\ell o}}{2C_d^2 P_o} \left(\frac{\dot{m}_t}{A_t} \right)^2 \cdot \frac{1}{g_c} \right] \quad (19)$$

where

\dot{m}_t = critical mass flow rate at throat, lbm/s (kg/s)

v_{ge} = specific volume of vapor at throat equilibrium conditions, ft³/lbm (m³/kg)

v_{le} = specific volume of subcooled liquid at stagnation conditions, ft³/lbm (m³/kg)

s_{ge} = entropy of vapor at throat equilibrium conditions, Btu/lbm-°R (kJ/kg-K)

s_{le} = entropy of liquid at throat equilibrium conditions, Btu/lbm-°R (kJ/kg-K)

ds_{le} = change of entropy of liquid from stagnation to throat equilibrium conditions, Btu/lbm-°R (kJ/kg-K)

dP = change of pressure from stagnation to throat critical pressure

A_t = throat area, ft² (m²)

N = empirical description of partial phase change occurring at the throat

x_{et} = quality at throat equilibrium

P_t = throat critical pressure, lbf/ft² (N/m²)

P_o = entrance stagnation pressure, lbf/ft² (N/m²)

$$g_c = 32.2 \frac{\text{lbm} \cdot \text{ft}}{\text{lbf} \cdot \text{s}^2} \text{ (English units only)}$$

Equations (18) and (19) can be solved implicitly for P_t and A_t (or \dot{m}_t) given the upstream stagnation conditions, P_o , T_o , and \dot{m}_t (or A_t). The upstream stagnation pressure was determined from the change in pressure head from point 1 to point 2 of figure 28. If one assumes a location in the tank as high as point 1, or $P_o = P_{ullage}$, the estimated hole sizes decrease by only approximately 5 percent. The resulting hole sizes are shown graphically in figure 27 and are summarized in table 2. These hole sizes were computed for a $C_d = 0.98$. Hole sizes for a $C_d = 0.68$ are within 6 percent of these estimates and are not shown. The exit quality and void fractions are estimated to be 0.03 and 0.7, respectively, within the accuracy of the model used. (See ref. 2.) However, given the inherent uncertainties that still exist in the modeling of two-phase critical flow, the author would like to qualify the hole sizes computed as best estimates and specific to the model used.

Finally, in a speculative sense, a matrix of four possible structural failures was constructed and the corresponding leak rates were determined using the same methods and assumptions described previously. Results are shown in table 3 and are presented graphically in figures 26 and 27 for comparisons to leak rates observed. The failures considered are not meant to be inclusive and should not preclude the possibility that the leak could have occurred by way of a hole somewhere else in the tank.

TABLE 2.- SUMMARY OF EFFECTIVE HOLE SIZES

Hole type	Hole diameter, cm (in.), for time interval, s, of -		
	63.5 to 66.7	66.7 to 72.0	72.0 to 72.9 (a)
Gas, $C_d = 0.68^b (T_{bulk})$	5.6 (2.2) \pm 23%	5.3 (2.1) \pm 5%	6.8 to 7.4 (2.7 to 2.9) \pm 1%
Gas, $C_d = 0.98^c (T_{bulk})$	4.6 (1.8) \pm 23%	4.3 (1.7) \pm 5%	5.6 to 6.1 (2.2 to 2.4) \pm 1%
Gas, $C_d = 0.68^b (T_{sat})$	3.0 (1.2) \pm 23%	3.0 (1.2) \pm 5%	3.8 to 4.3 (1.5 to 1.7) \pm 1%
Gas, $C_d = 0.98^c (T_{sat})$	2.5 (1.0) \pm 23%	2.5 (0.98) \pm 5%	3.3 to 3.5 (1.3 to 1.4) \pm 1%
Liquid, $C_d = 0.98^c$	13.5 (5.3) \pm 23%	12.7 (5.0) \pm 5%	16.5 to 18.0 (6.5 to 7.1) \pm 1%

^aBased on two different pressurization rates. (See fig. 25.)

^b $C_d = 0.68$ corresponds to a jagged hole.

^c $C_d = 0.98$ corresponds to a clean line break.

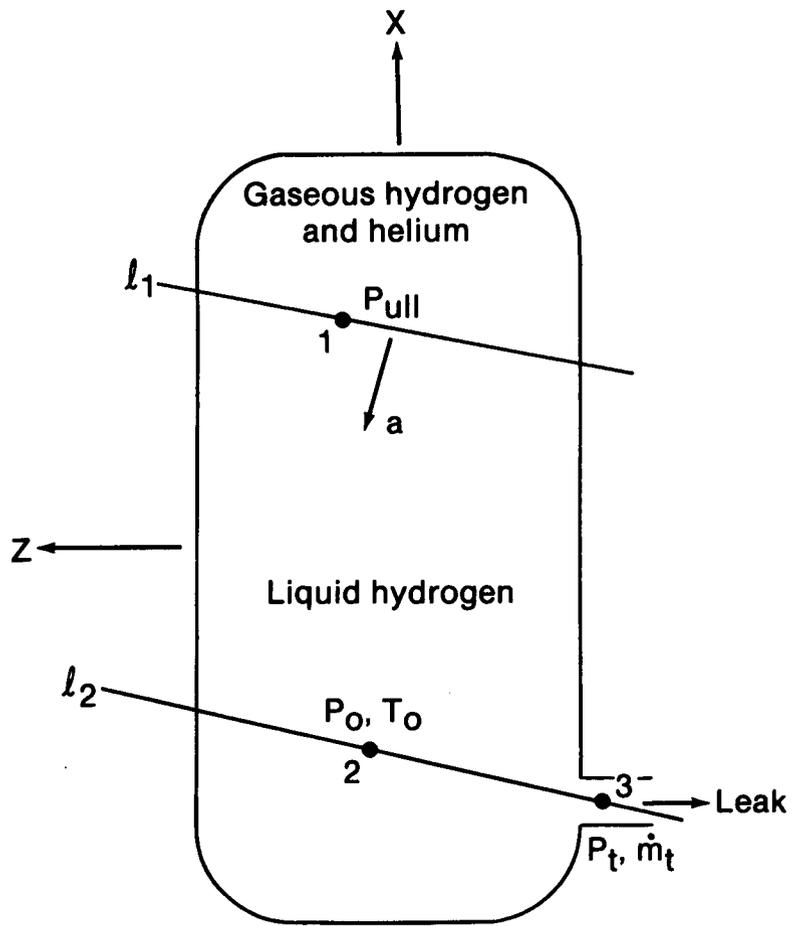


Figure 28.- First law of thermodynamics applied to the hydrogen external tank.

TABLE 3.- LEAK RATES ASSOCIATED WITH THE POSSIBLE STRUCTURAL FAILURES

Flight hardware	Hydrogen leak rates, kg/s (lbm/s)			
	Liquid		Gaseous	
	$C_d = 0.98$		$C_d = 0.68^a$	$C_d = 0.98^b$
LH ₂ recirculation line – 10-cm (4 in.) diameter	41.7	(91.8)	--	--
LH ₂ feedline – 43-cm (17 in.) diameter	752.8	(1658.2)	--	--
GH ₂ pressurization line – 5-cm (2 in.) diameter ^c	--	--	1.3 (2.9)	1.5 (3.2)
GH ₂ vent line – 13-cm (5 in.) diameter	--	--	1.5 (3.2)	2.1 (4.7)

^a $C_d = 0.68$ corresponds to a jagged hole.

^b $C_d = 0.98$ corresponds to a clean line break.

^c Assumes no pressurization gas enters for this break.

As observed, leak rates for the gaseous hydrogen vent line, the gaseous hydrogen pressurization line, and the liquid hydrogen feedline well exceed the leak rates observed across all time intervals. A break in the liquid hydrogen feedline, however, could account for the leak observed in the interval of 65.5 to 72 seconds, especially given the inherent margins for error in modeling two-phase flow. A larger structural failure would need to occur in the last time interval from 72.0 to 72.9 seconds to account for the large leak rate corresponding to the sharp decrease in pressure that was visibly identified as the premature decay in figure 1. Thus, of these four failures considered, a scenario of a break in the liquid hydrogen recirculation line at 65.5 seconds with a subsequent larger structural failure at 72 to 72.9 seconds seems plausible.

CONCLUSIONS

Hydrogen external tank ullage pressure reconstruction for flight STS 51-L shows that a deviation between predicted and actual pressure starting at 65.5 seconds is consistent with flight data. Furthermore, this pressurization loss can be interpreted as either a liquid hydrogen or a gaseous hydrogen leak. If a gas leak is assumed, calculations show an initial leak of 0.28 kg/s (0.62 lbm/s) \pm 41 percent at 65.5 seconds which increases to between 0.43 and 0.51 kg/s (0.94 and 1.12 lbm/s) \pm 1 percent in the 72- to 72.9-second time interval. If a liquid leak is assumed, calculations show an initial leak of 72.9 kg/s (160.5 lbm/s) \pm 41 percent at 65.5 seconds which increases to between 111.6 and 133.2 kg/s (245.8 and 293.3 lbm/s) \pm 1 percent in the 72- to 72.9-second time interval. In both cases, the leak remains relatively constant from 65.5 to 72.0 seconds and increases significantly at 72 to 73 seconds. This sharp increase in leak rates parallels the premature decay that was initially identified in flight ullage pressure data.

Finally, a comparison of effective hole sizes required to cause the calculated leak rates indicates a break in the gaseous hydrogen pressurization line, in the gaseous hydrogen vent line, or in the liquid hydrogen feedline is not likely. A break in the liquid hydrogen recirculation line with a larger structural failure occurring in the 72- to 72.9-second time interval, the time of the visibly identified premature pressure decay, does seem plausible and the most likely of the four modes considered. This comparison was performed in a speculative sense and is not meant to preclude the possibility that the leak could have occurred somewhere else in the tank.

REFERENCES

1. Henry, R. E.; and Fauske, H. K.: The Two-Phase Critical Flow of One-Component Mixtures in Nozzles, Orifices, and Short Tubes. ASME J. Heat Transfer, May 1971, pp. 179-187.
2. Henry, R. E.; and Fauske, H. K.: Two-Phase Critical Flow at Low Qualities, Part II: Analysis. Nuclear Science and Engineering, vol. 41, 1970, p. 79.
3. Henry, R. E.: The Two-Phase Critical Discharge of Initially Saturated or Subcooled Liquid. Nuclear Science and Engineering, vol. 41, 1970, pp. 336-342.

APPENDIX A
FLIGHT DATA RECONSTRUCTION

Plots created from the flight data used for flights STS 51-L, STS 61-A, and STS 51-F are shown. Tabular data were acquired initially from the NASA George C. Marshall Space Flight Center, Slidell Computer Complex. Interpolations to 0.01 second were performed on all data so that the files could be meshed at common times to be called upon during the pressure reconstruction. One exception is for engine outlet pressure. These plots are not actual flight data and were created from logic based on actual engine outlet pressure flight data plots and valve position data. Please see appendix D (Program ENGP51L) for the logic used. Table A-1 lists the Slidell files accessed. Figure A-1 shows the corresponding location of each measurement. Figures A-2 to A-43 are plots of the data used as a function of mission elapsed time (MET).

TABLE A-1.- SLIDELL DATA FILES ACCESSED

<u>No.</u>	<u>File code</u>	<u>Description</u>
1	E41R1021D	LH ₂ consumption (engine 1)
2	E41R2021D	LH ₂ consumption (engine 2)
3	E41R3021D	LH ₂ consumption (engine 3)
4	V41P1160A	Engine 1 outlet pressure
5	V41P1360A	Engine 3 outlet pressure
6	V41T1161A	Engine 1 outlet temperature
7	V41T1261A	Engine 2 outlet temperature
8	V41T1361A	Engine 3 outlet temperature
9	V41X1661E	Valve position (engine 1)
10	V41X1662E	Valve position (engine 2)
11	V41X1663E	Valve position (engine 3)
12	T41P1700C	Ullage pressure (1)
13	T41P1701C	Ullage pressure (2)
14	T41P1702C	Ullage pressure (3)
15	T41T1705A	Ullage temperature

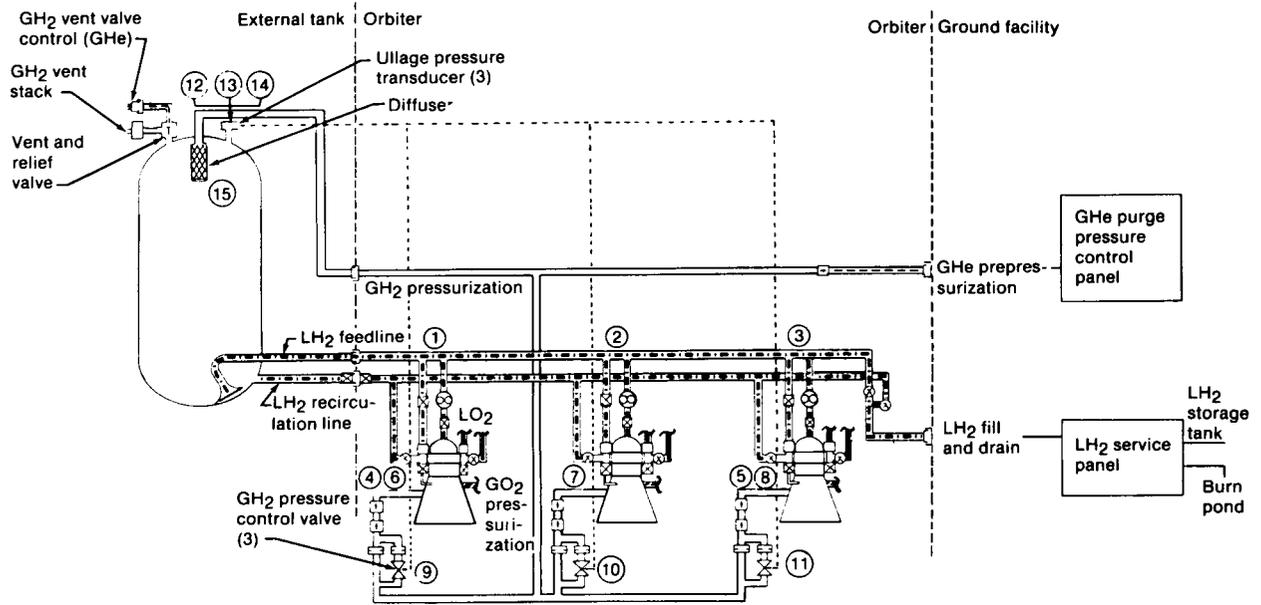


Figure A-1.- Transducer measurement location.

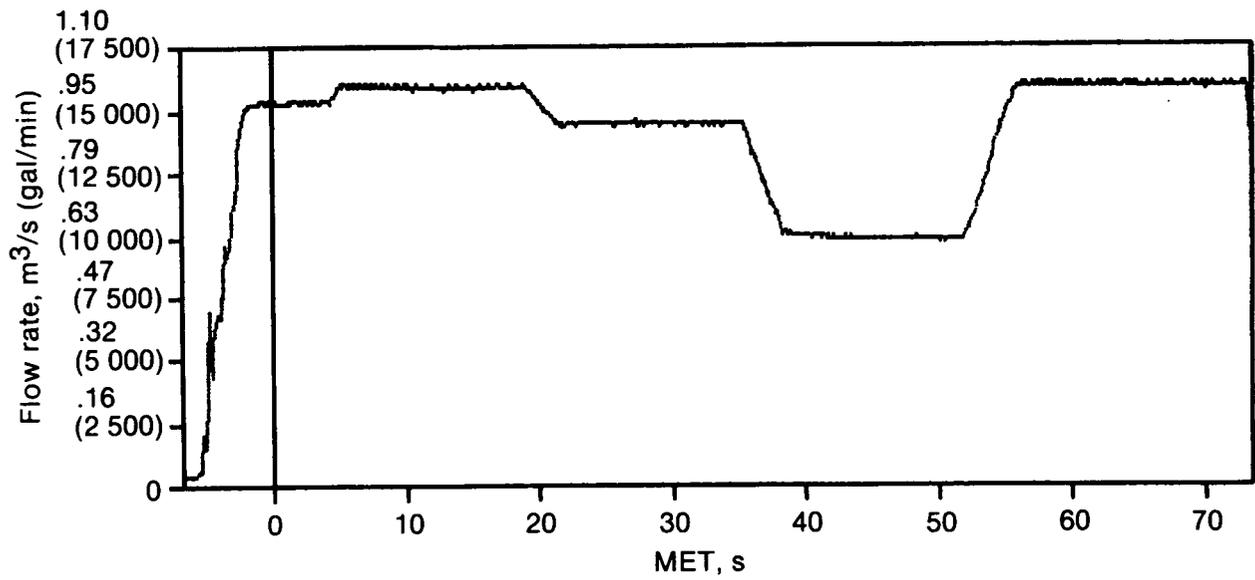


Figure A-2.- STS 51-L engine 1 fuel consumption (E41R1021D).

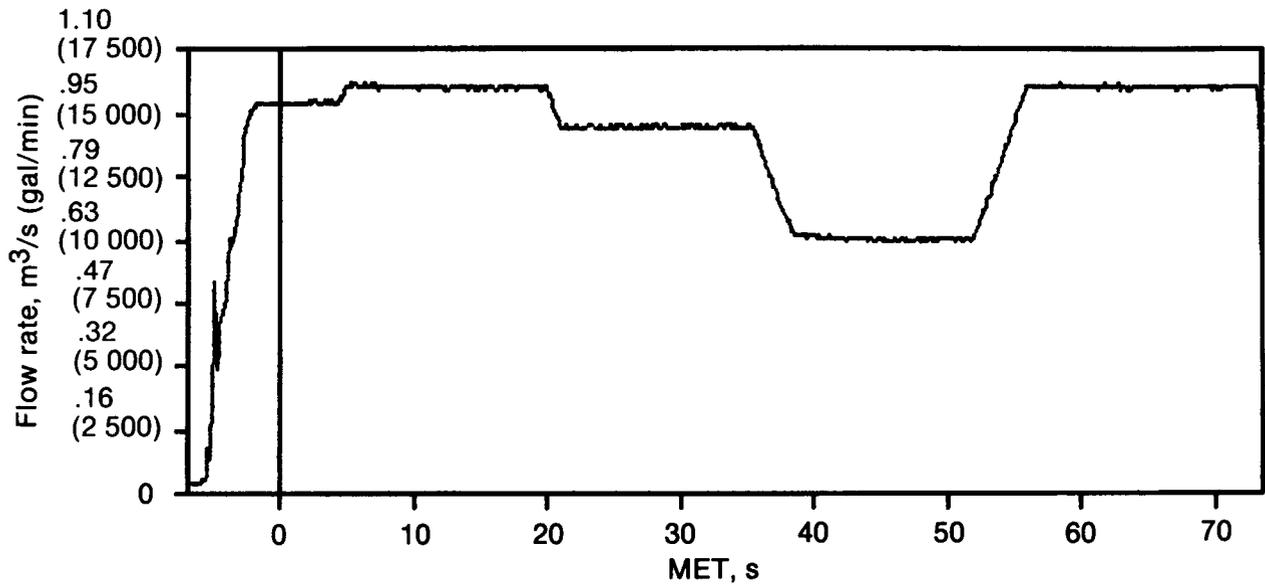


Figure A-3.- STS 51-L engine 2 fuel consumption (E41R2021D).

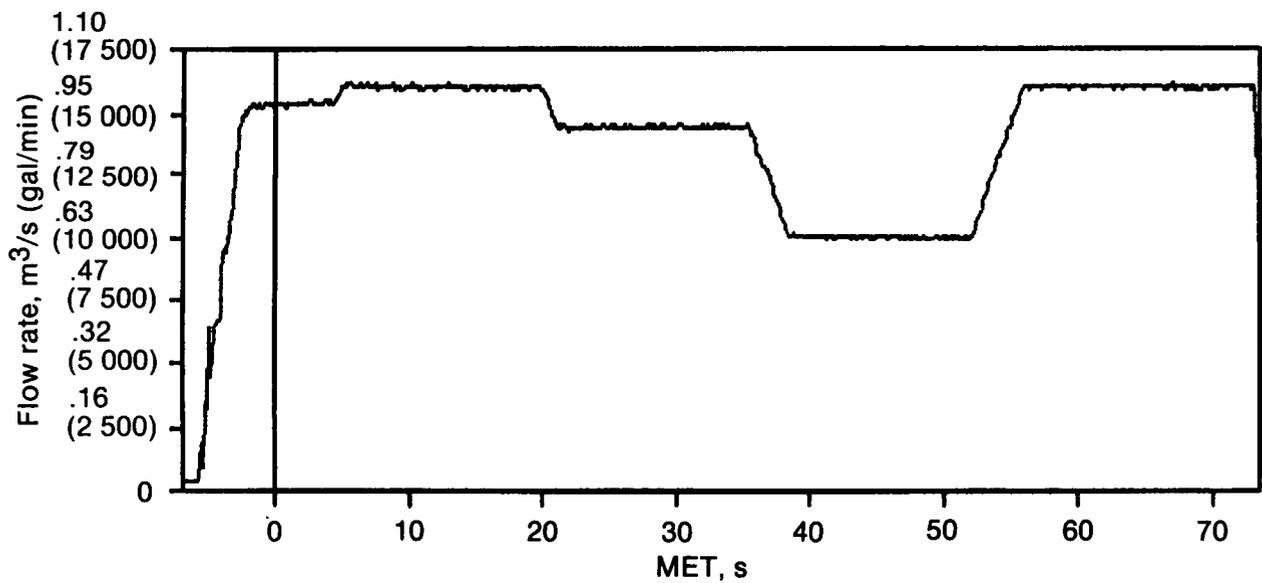


Figure A-4.- STS 51-L engine 3 fuel consumption (E41R3021D).

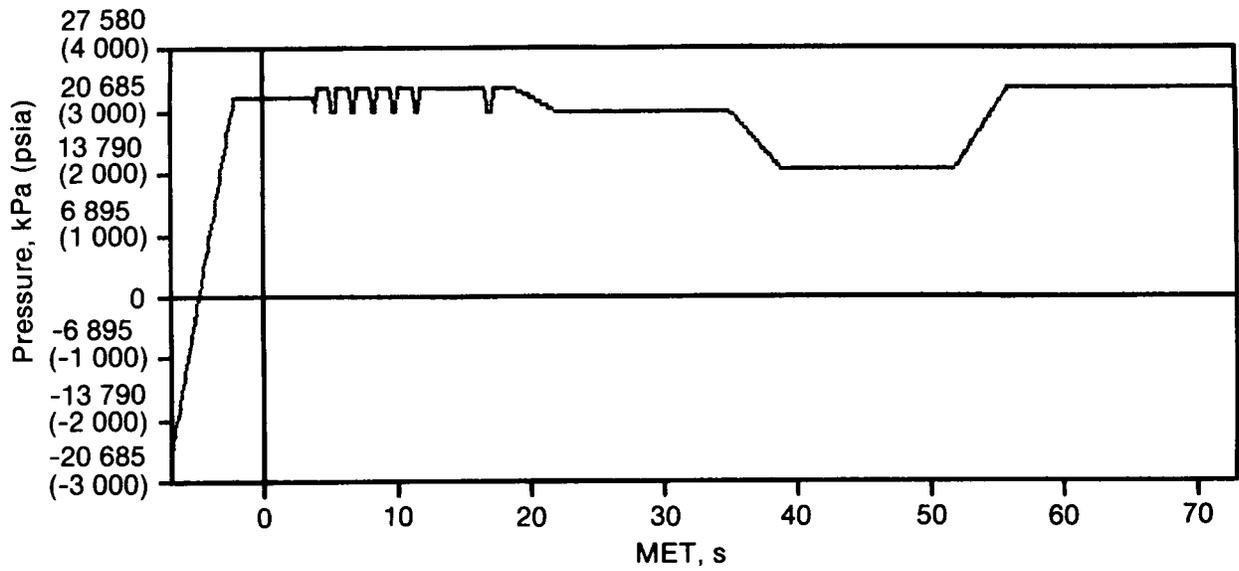


Figure A-5.- STS 51-L engine 1 outlet pressure (V41P1160A).

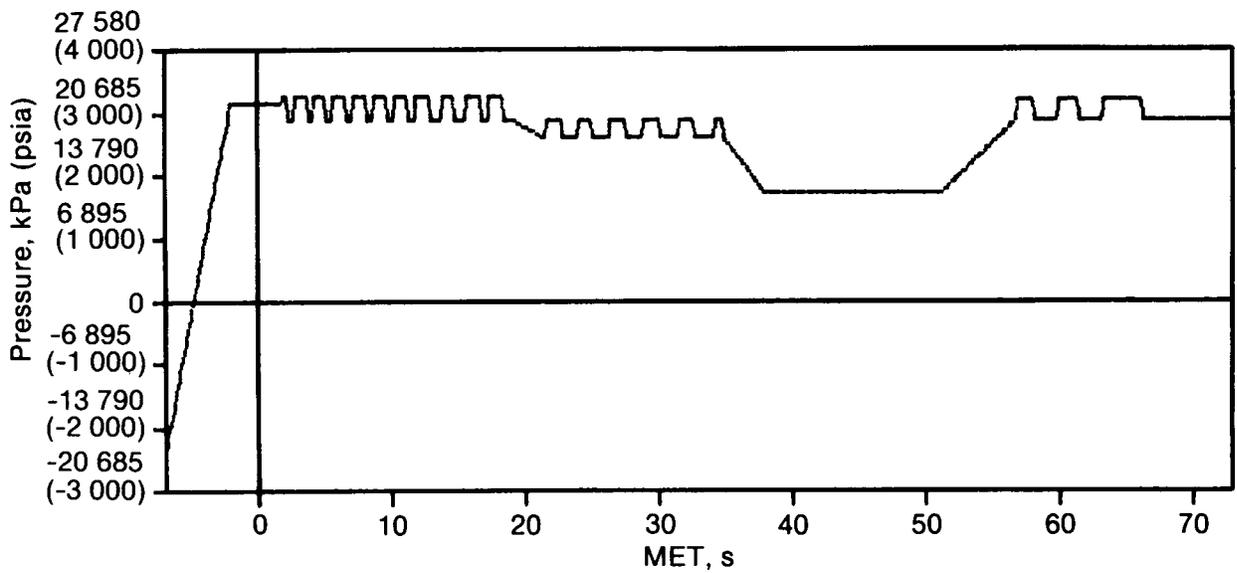


Figure A-6.- STS 51-L engine 2 outlet pressure (created).

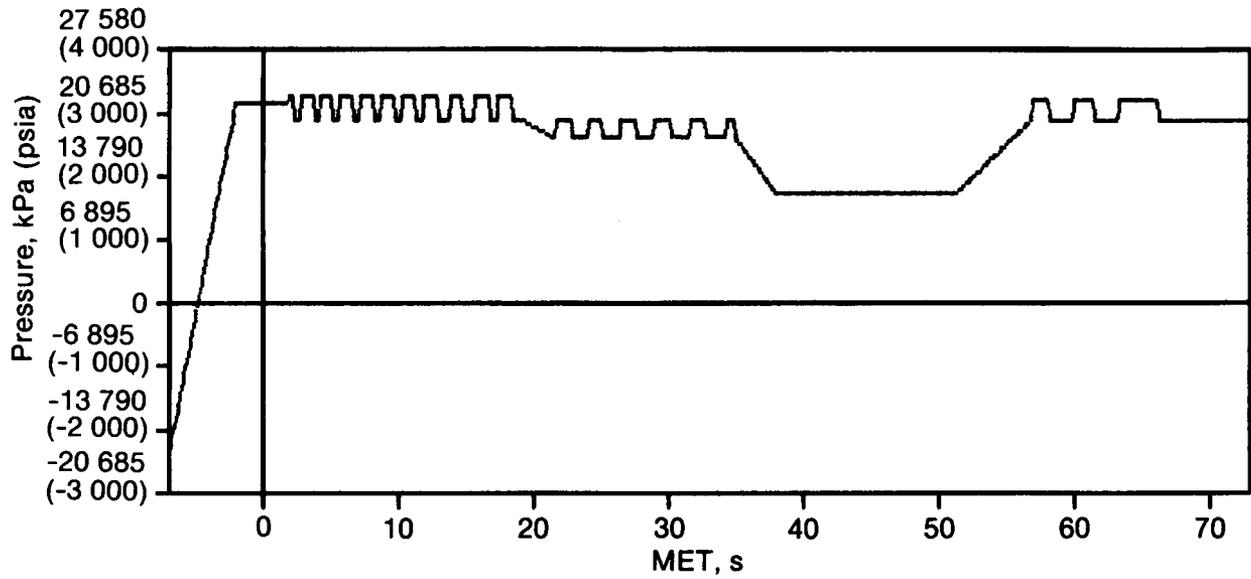


Figure A-7.- STS 51-L engine 3 outlet pressure (V41P1360A).

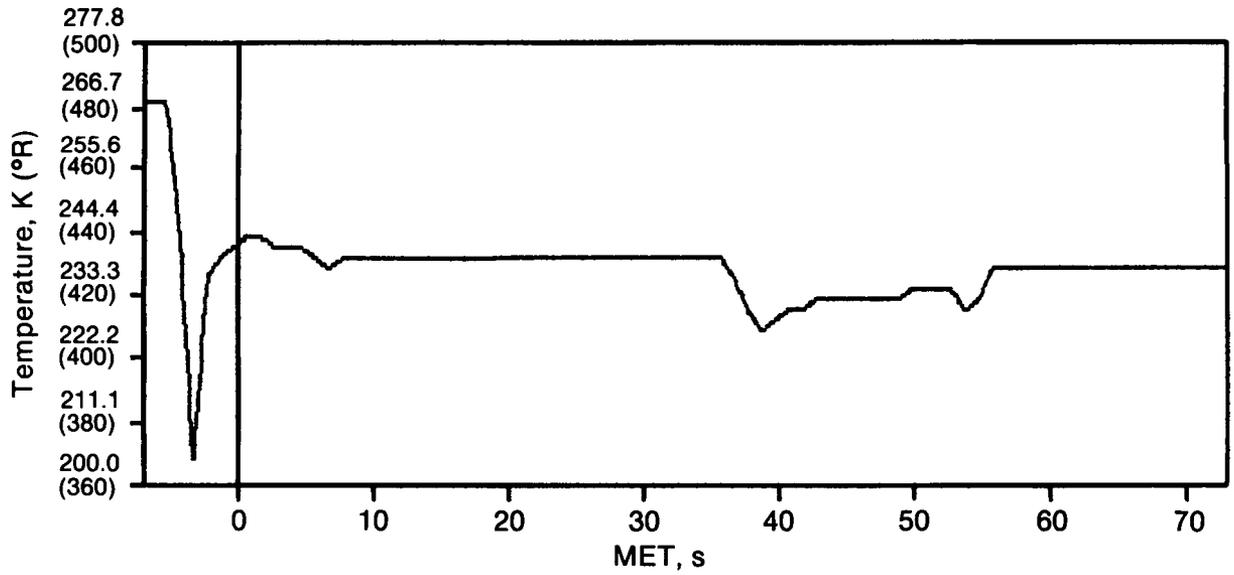


Figure A-8.- STS 51-L engine 1 outlet temperature (V41T1161A).

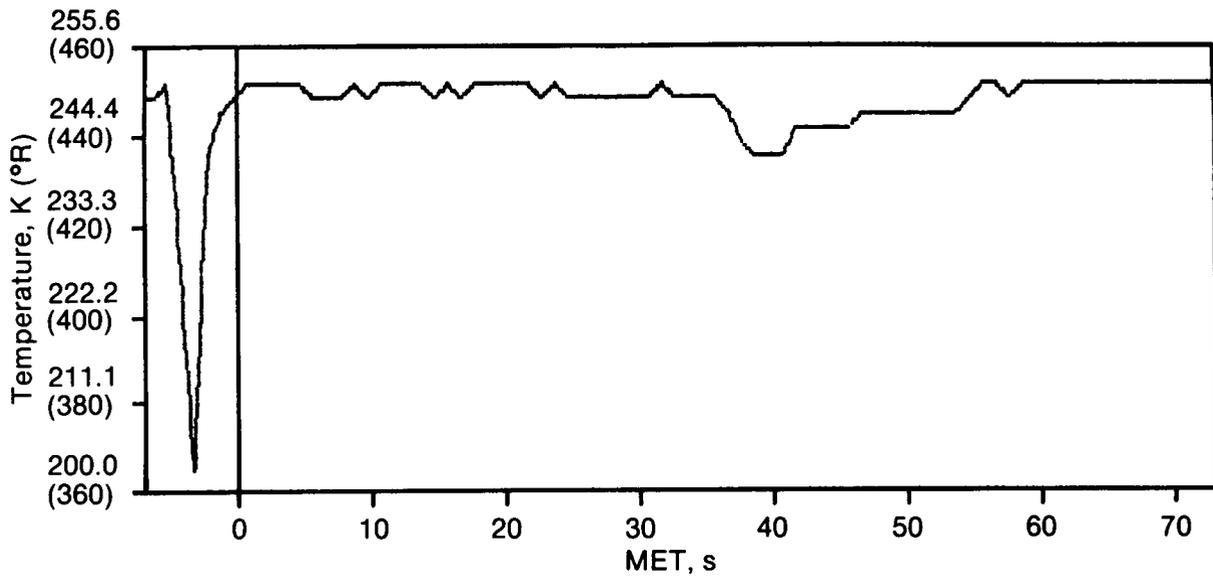


Figure A-9.- STS 51-L engine 2 outlet temperature (V41T1261A).

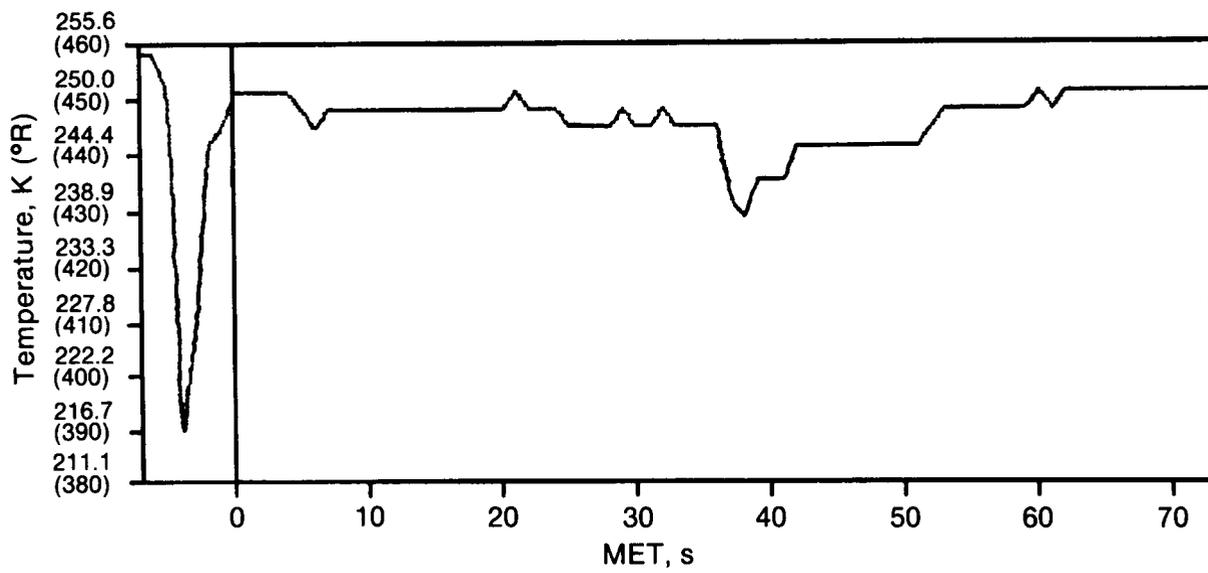


Figure A-10.- STS 51-L engine 3 outlet temperature (V41T1361A).

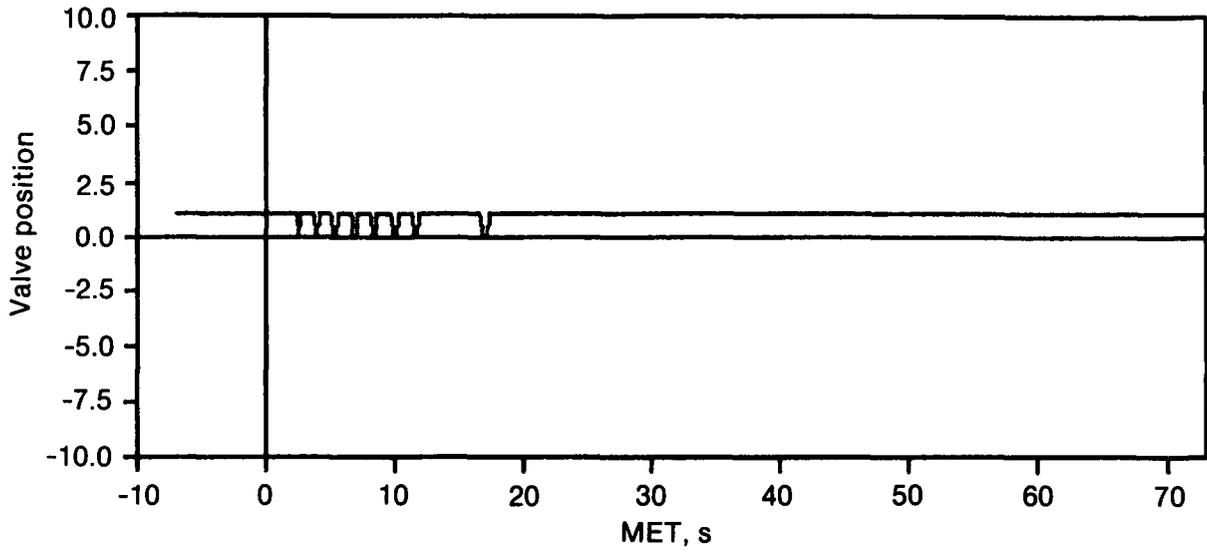


Figure A-11.- STS 51-L engine outlet valve 1 position (V41X1661E).

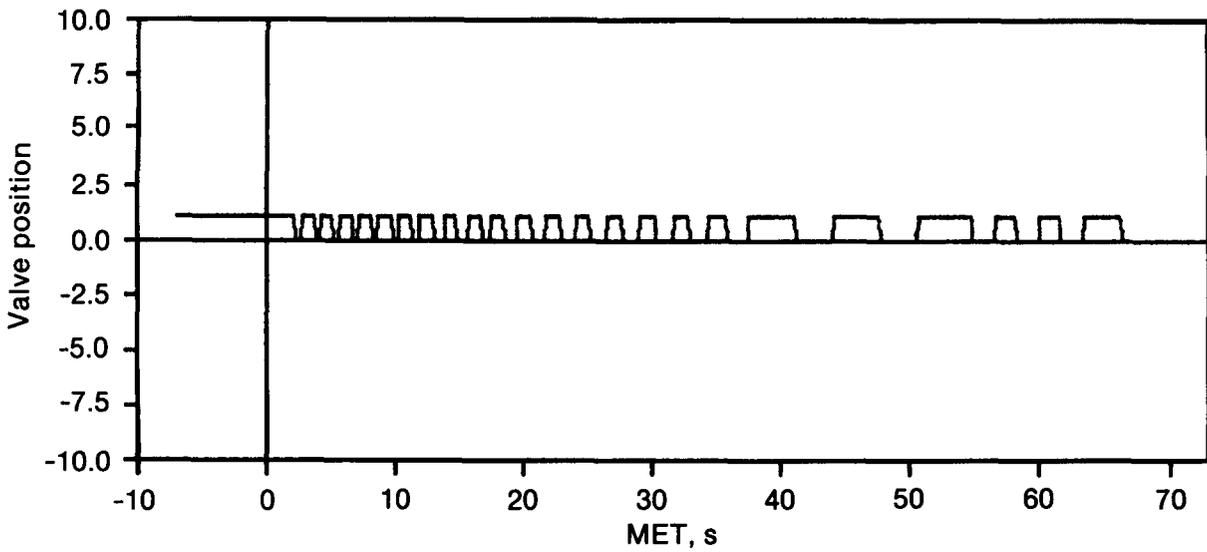


Figure A-12.- STS 51-L engine outlet valve 2 position (V41X1662E).

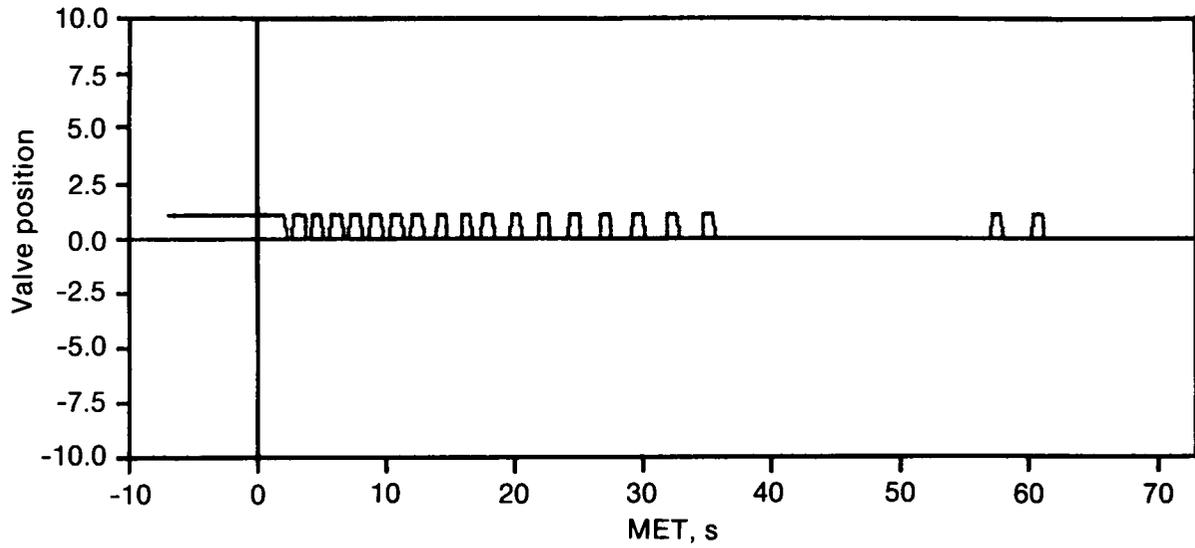


Figure A-13.- STS 51-L engine outlet valve 3 position (V41X1663E).

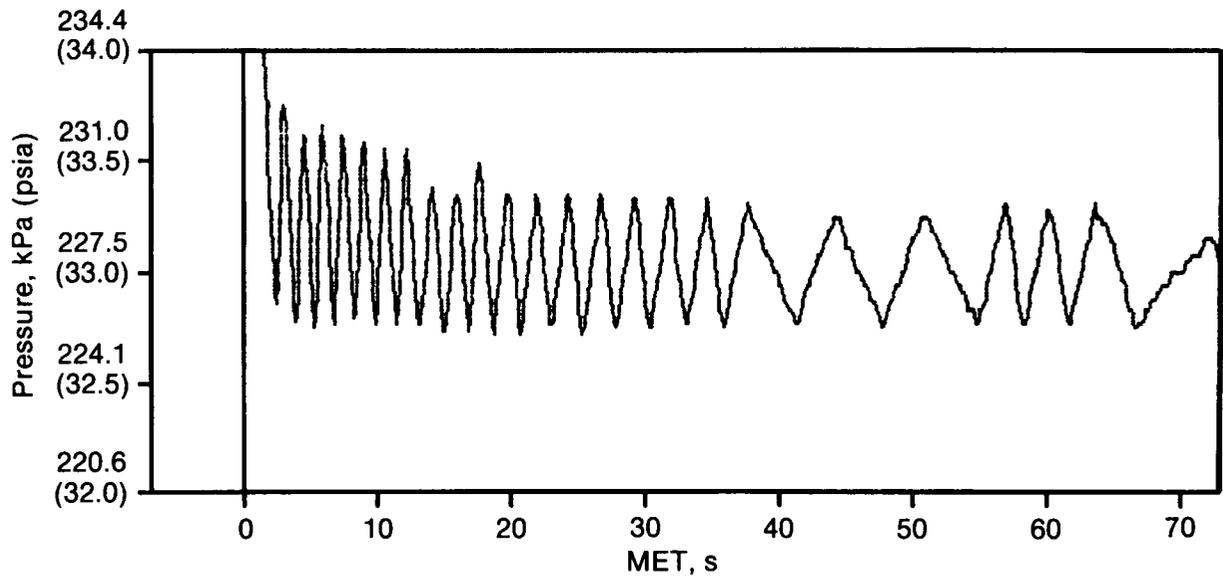


Figure A-14.- STS 51-L average ullage pressure (T41P1700C, T41P1701C, T41P1702C).

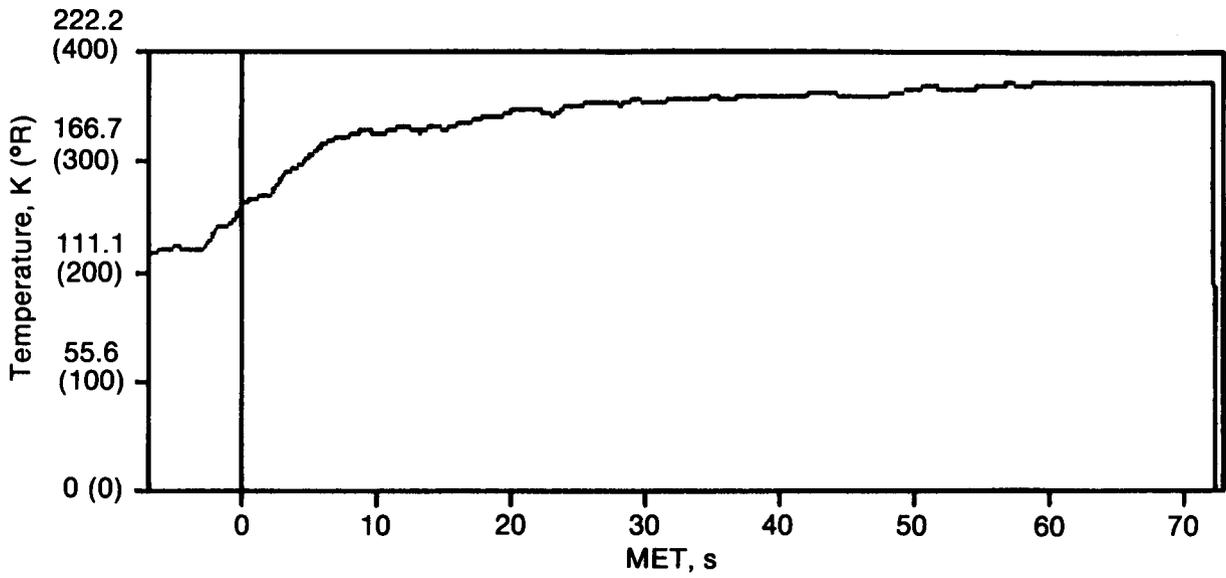


Figure A-15.- STS 51-L measured ullage temperature (T41T1705A).

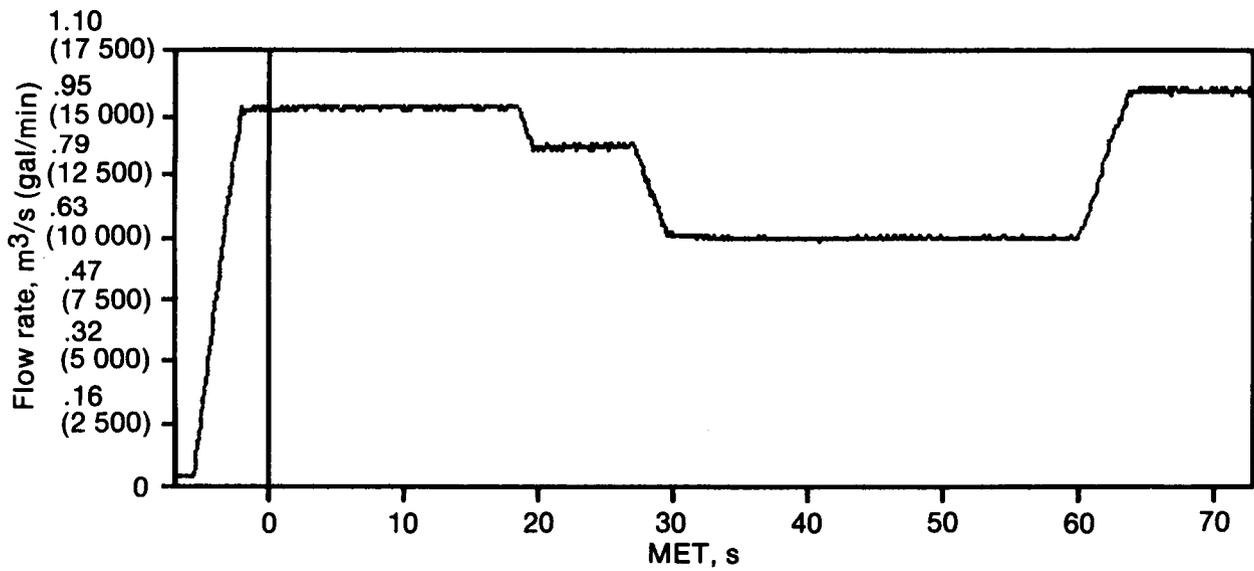


Figure A-16.- STS 61-A engine 1 fuel consumption (E41R1021D).

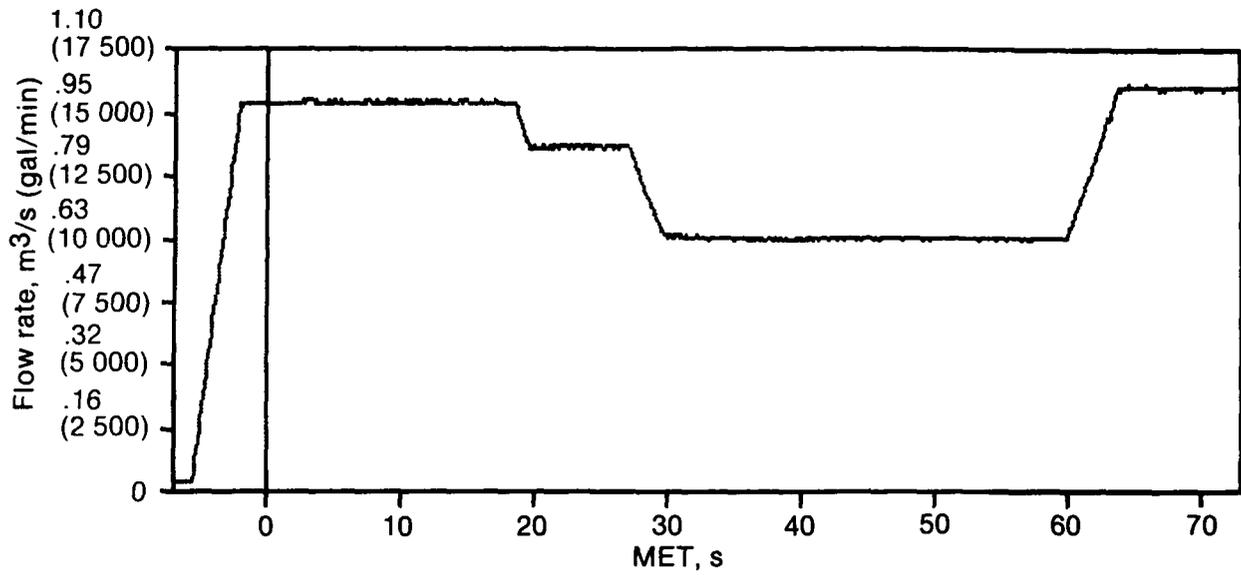


Figure A-17.- STS 61-A engine 2 fuel consumption (E41R2021D).

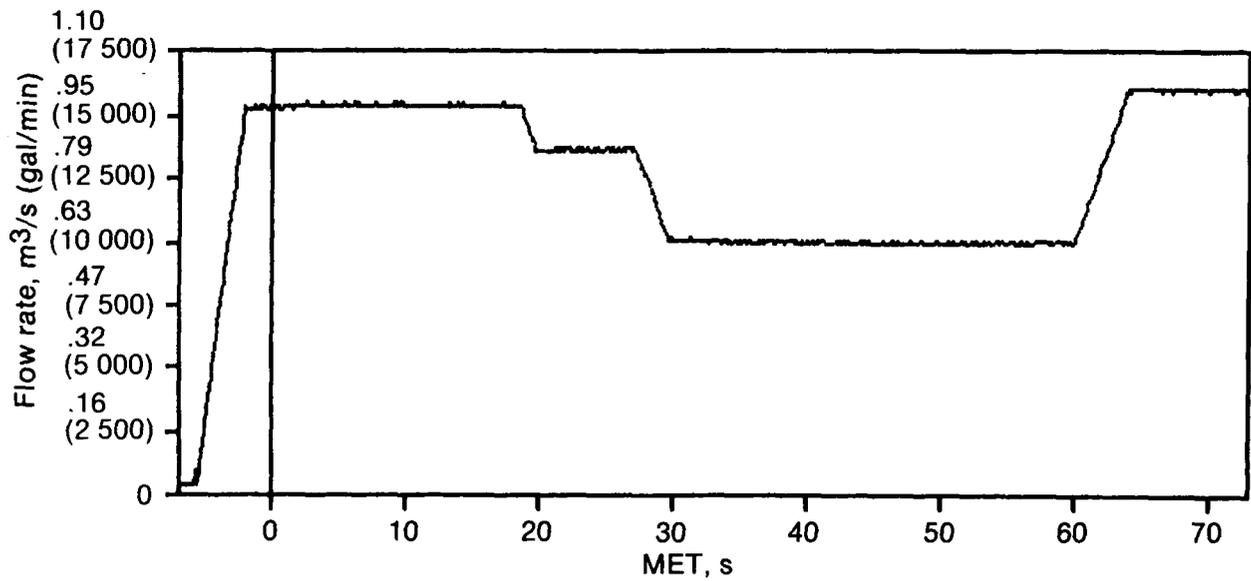


Figure A-18.- STS 61-A engine 3 fuel consumption (E41R3021D).

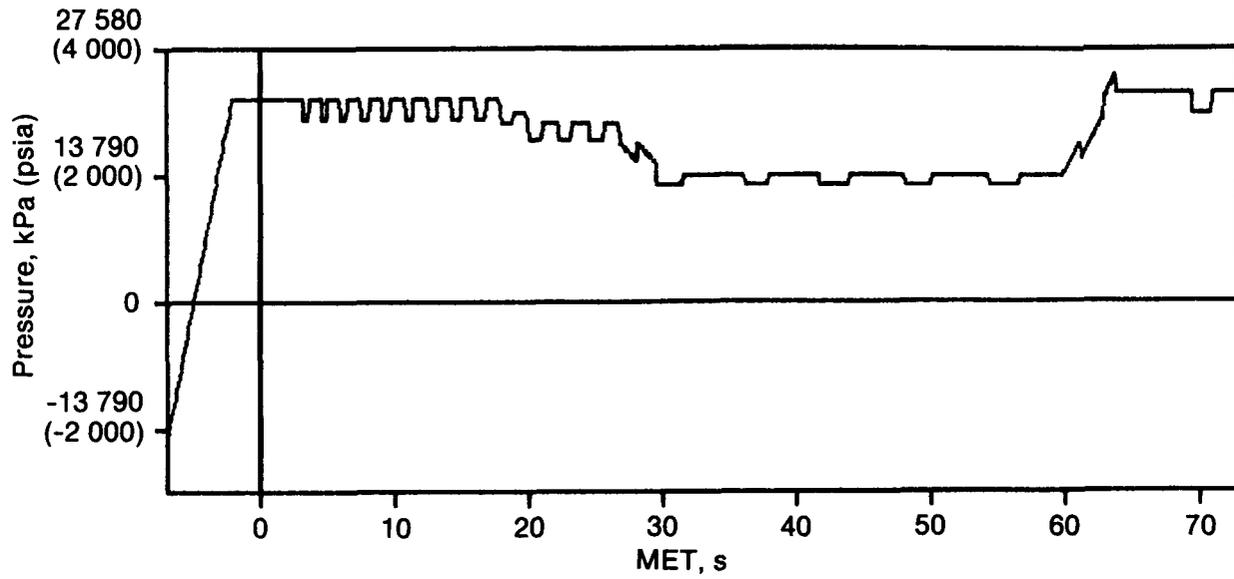


Figure A-19.- STS 61-A engine 1 outlet pressure (V41P1160A).

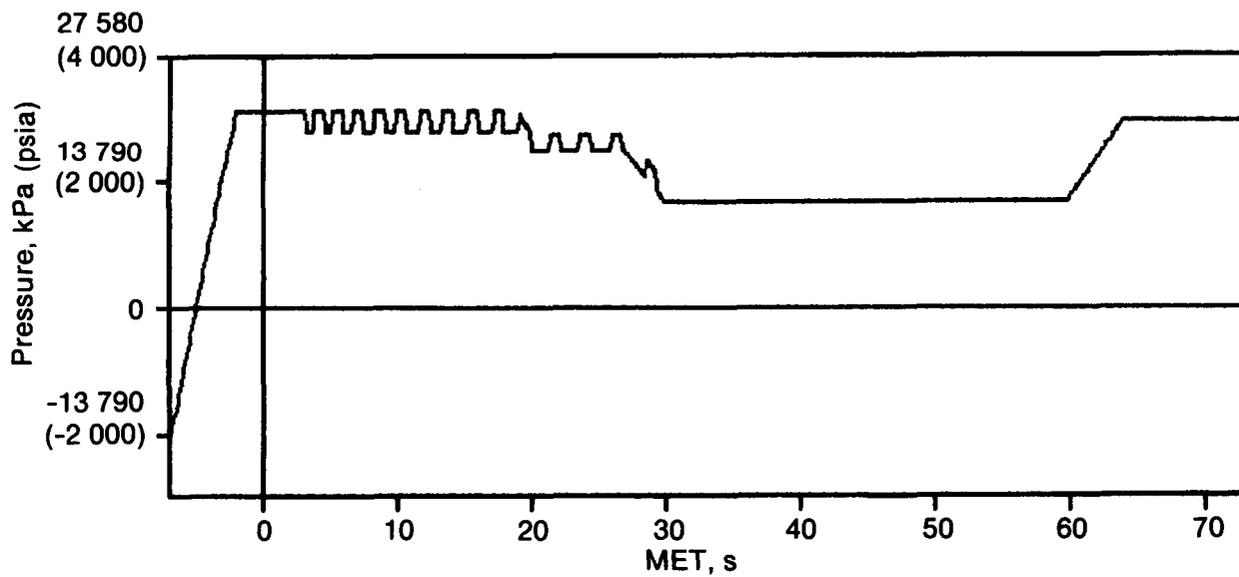


Figure A-20.- STS 61-A engine 2 outlet pressure (created).

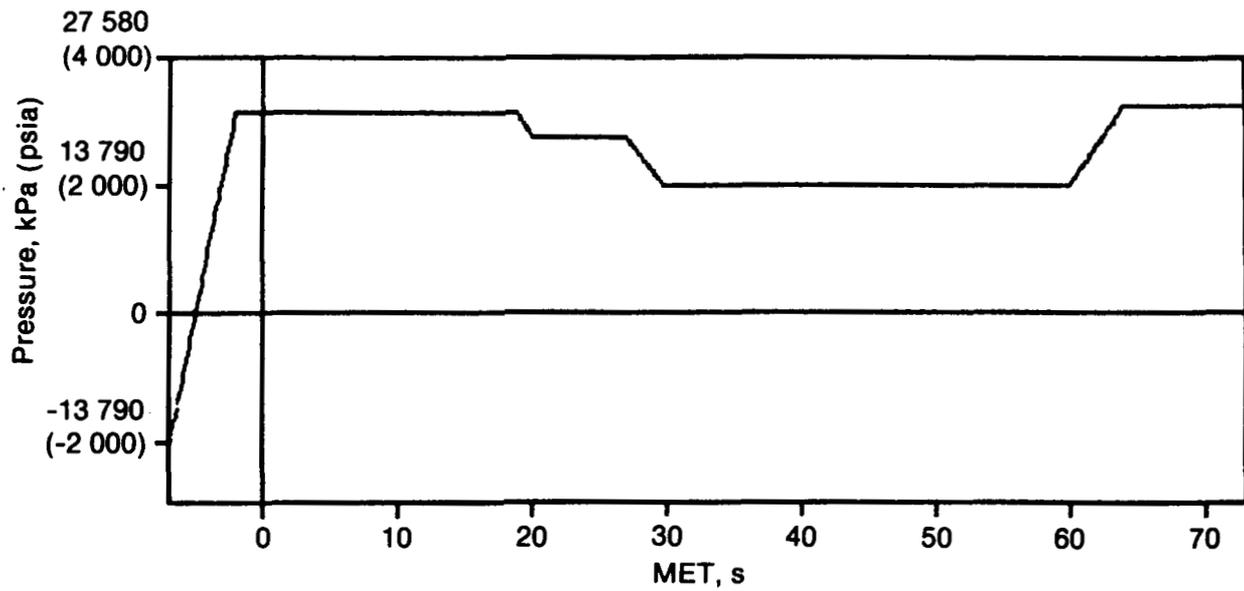


Figure A-21.- STS 61-A engine 3 outlet pressure (V41P1360A).

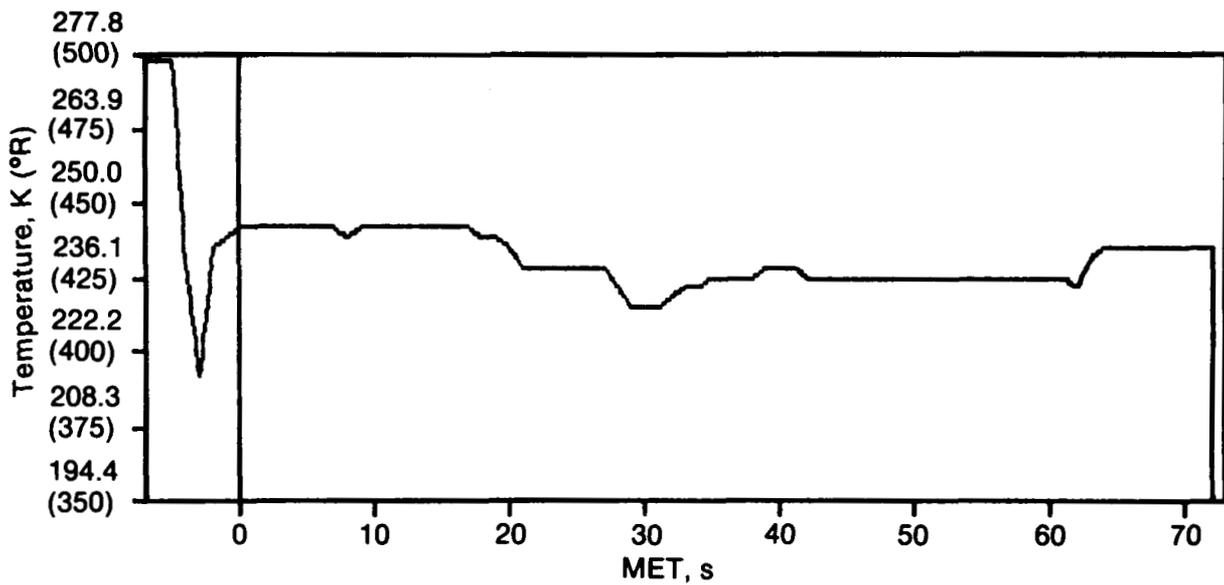


Figure A-22.- STS 61-A engine 1 outlet temperature (V41T1161A).

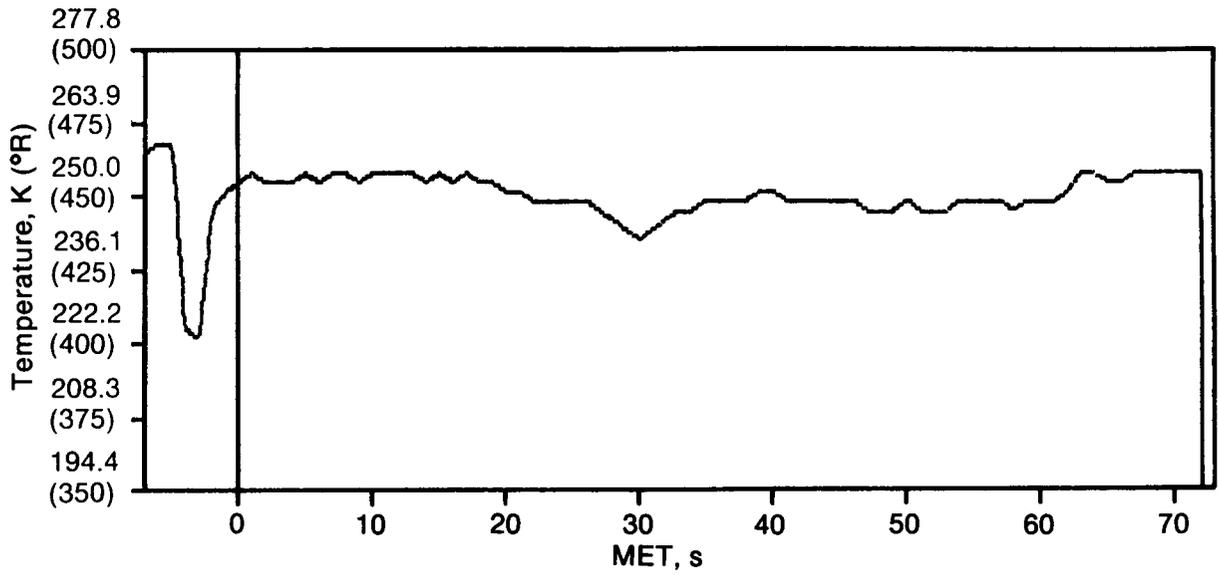


Figure A-23.- STS 61-A engine 2 outlet temperature (V41T1261A).

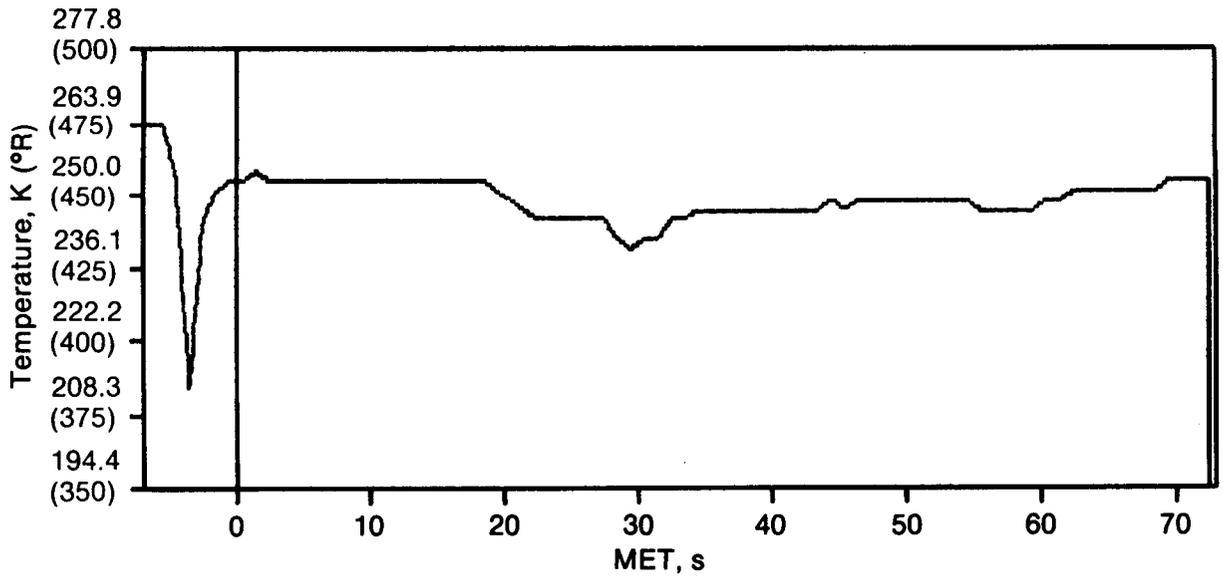


Figure A-24.- STS 61-A engine 3 outlet temperature (V41T1361A).

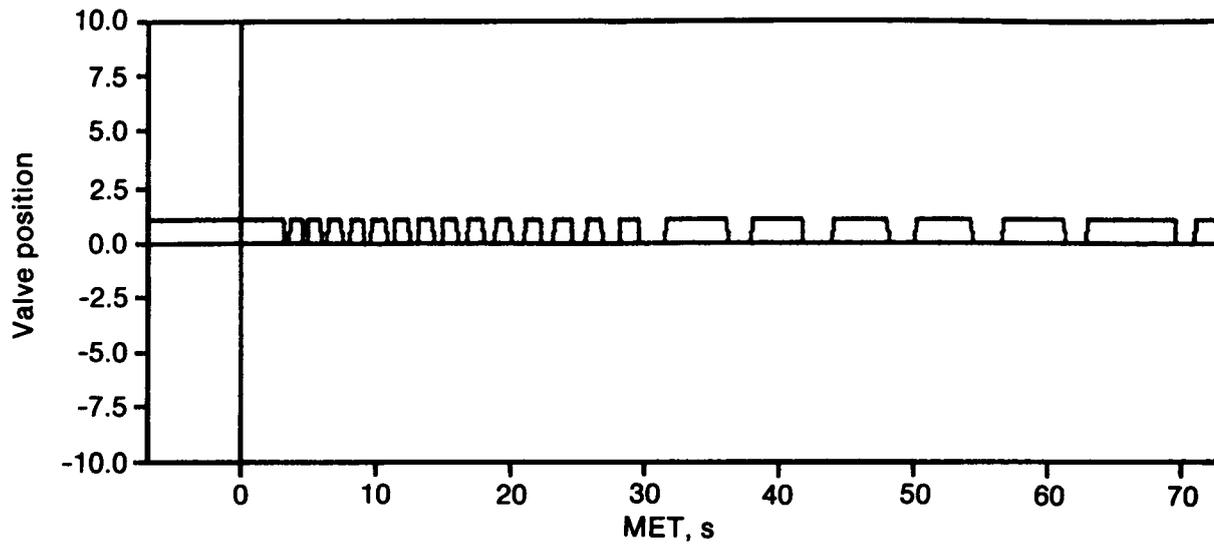


Figure A-25.- STS 61-A engine outlet valve 1 position (V41X1661E).

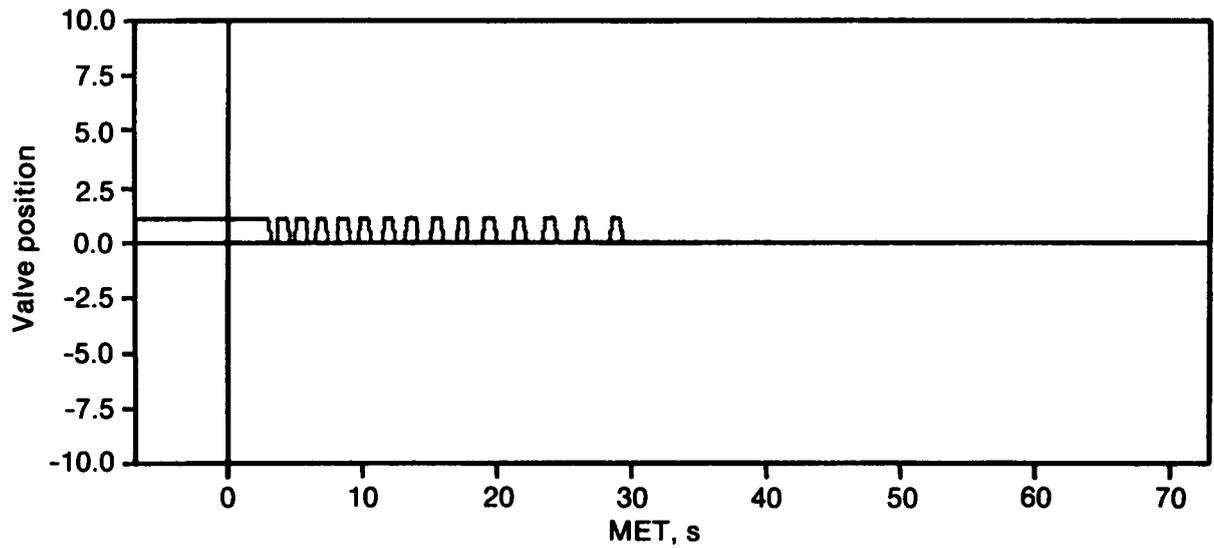


Figure A-26.- STS 61-A engine outlet valve 2 position (V41X1662E).

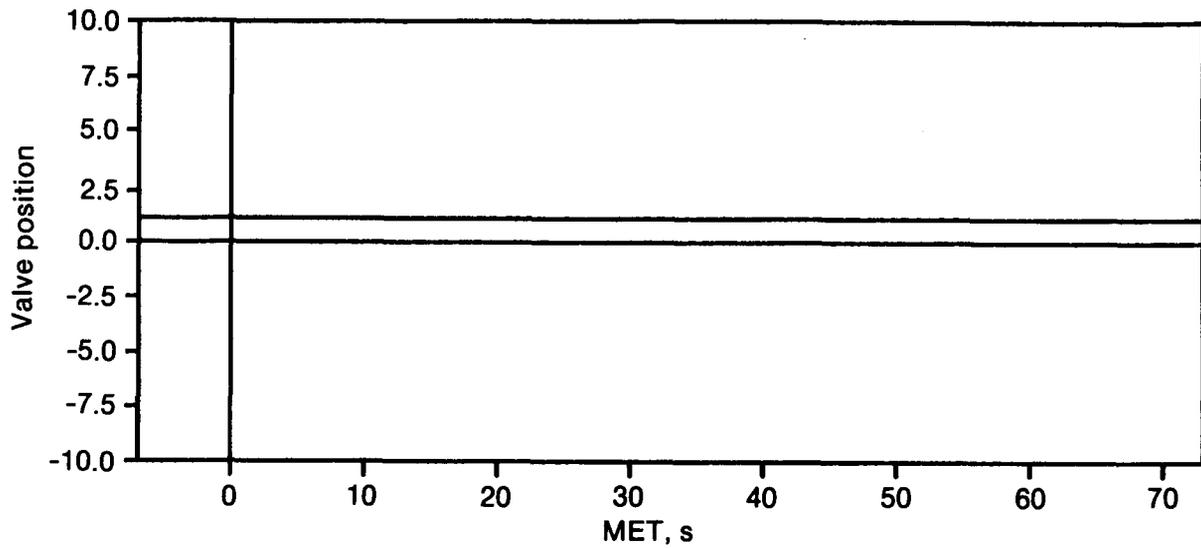


Figure A-27.- STS 61-A engine outlet valve 3 position (V41X1663E).

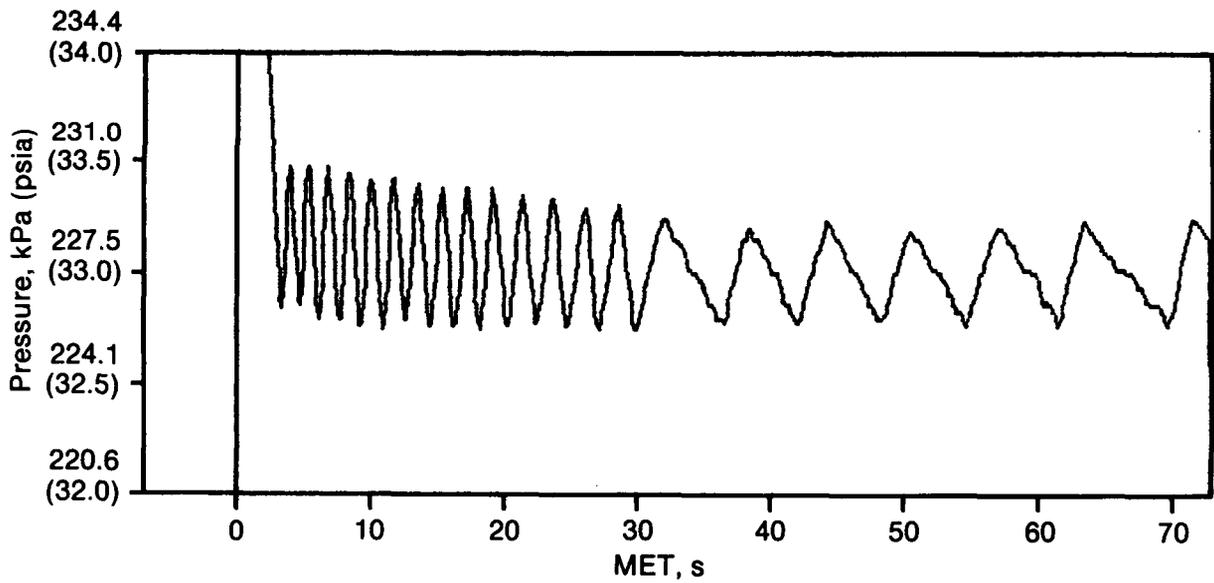


Figure A-28.- STS 61-A average ullage pressure (T41P1700C, T41P1701C, T41P1702C).

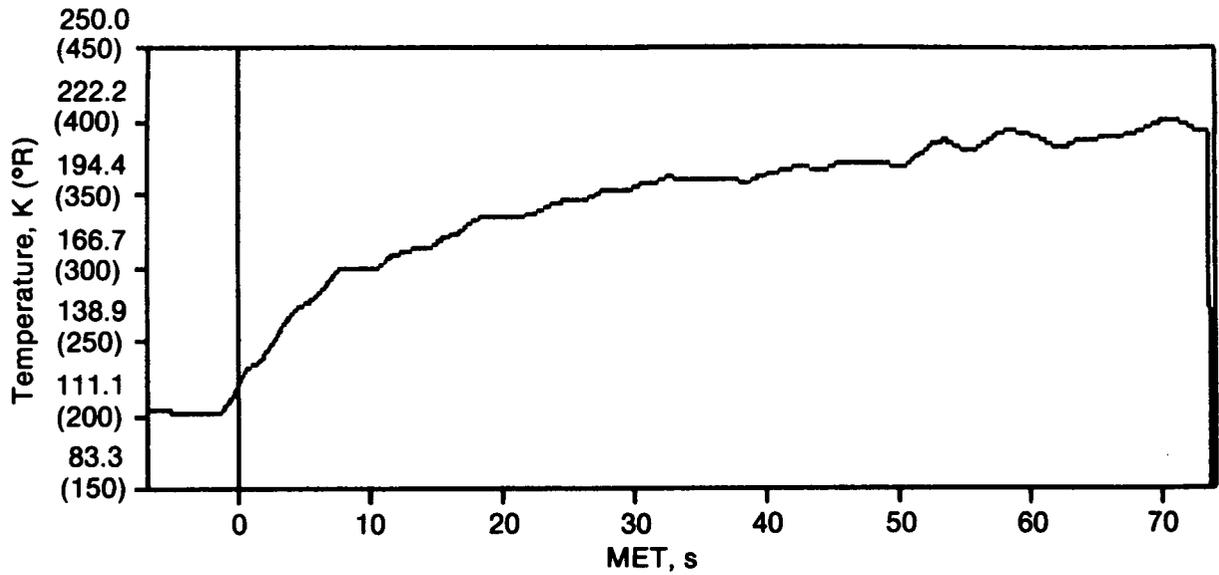


Figure A-29.- STS 61-A measured ullage temperature (T41T1705A).

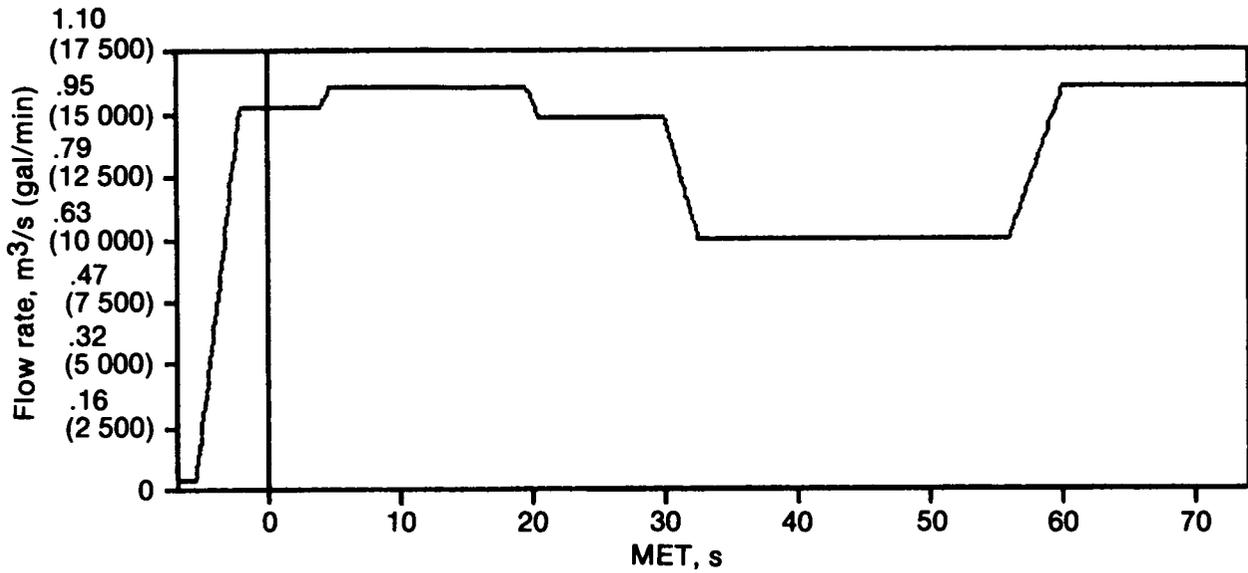


Figure A-30.- STS 51-F engine 1 fuel consumption (E41R1021D).

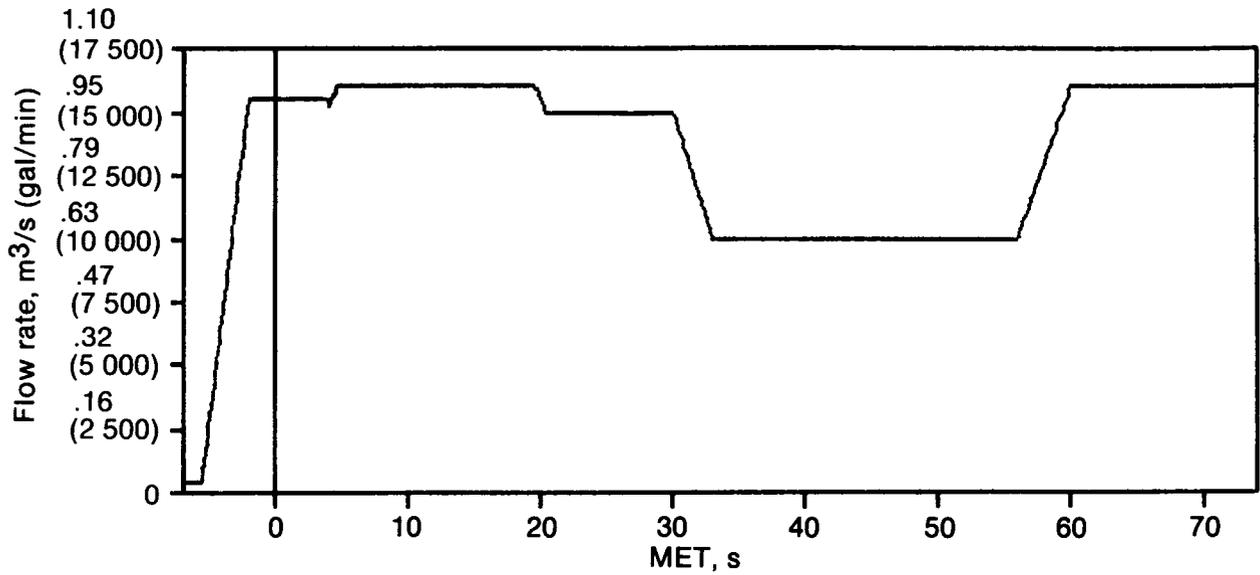


Figure A-31.- STS 51-F engine 2 fuel consumption (E41R2021D).

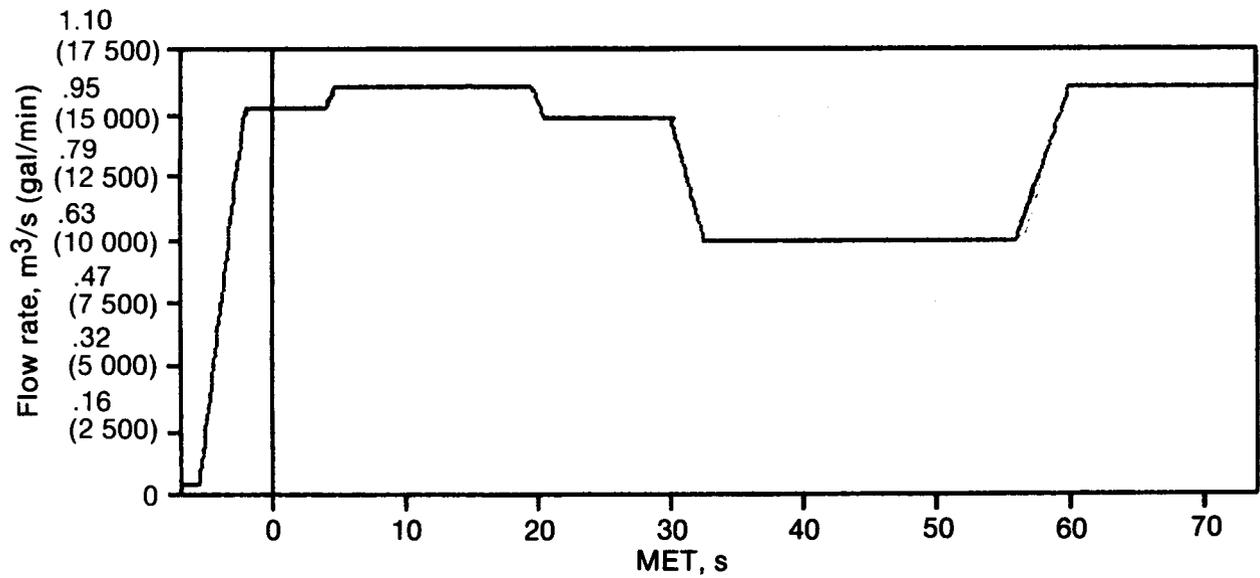


Figure A-32.- STS 51-F engine 3 fuel consumption (E41R3021D).

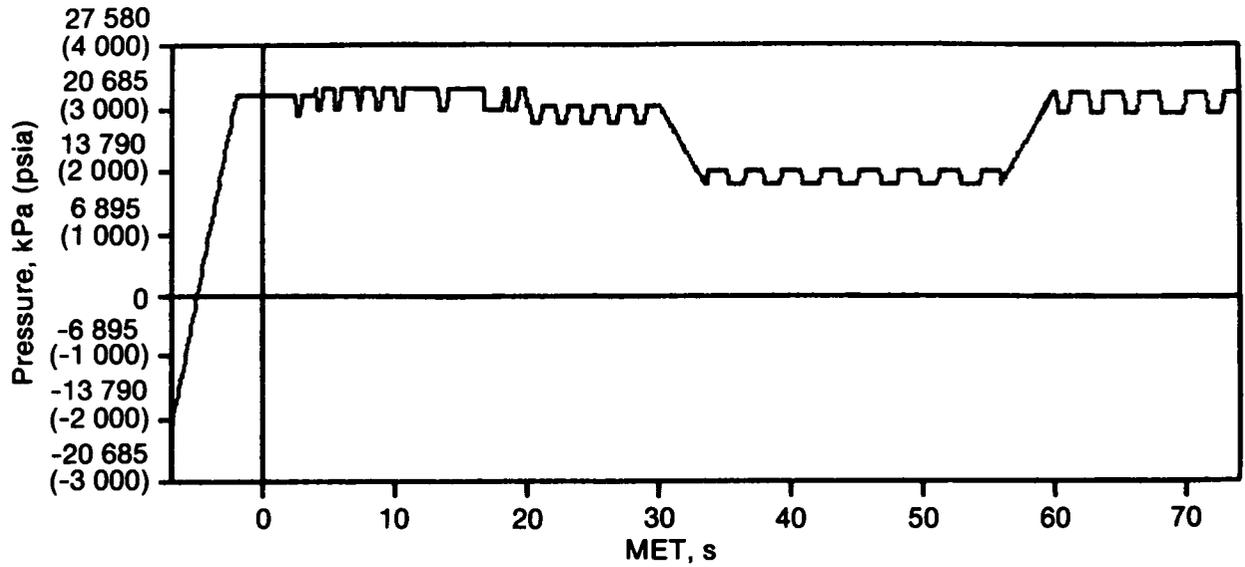


Figure A-33.- STS 51-F engine 1 outlet pressure (V41P1160A).

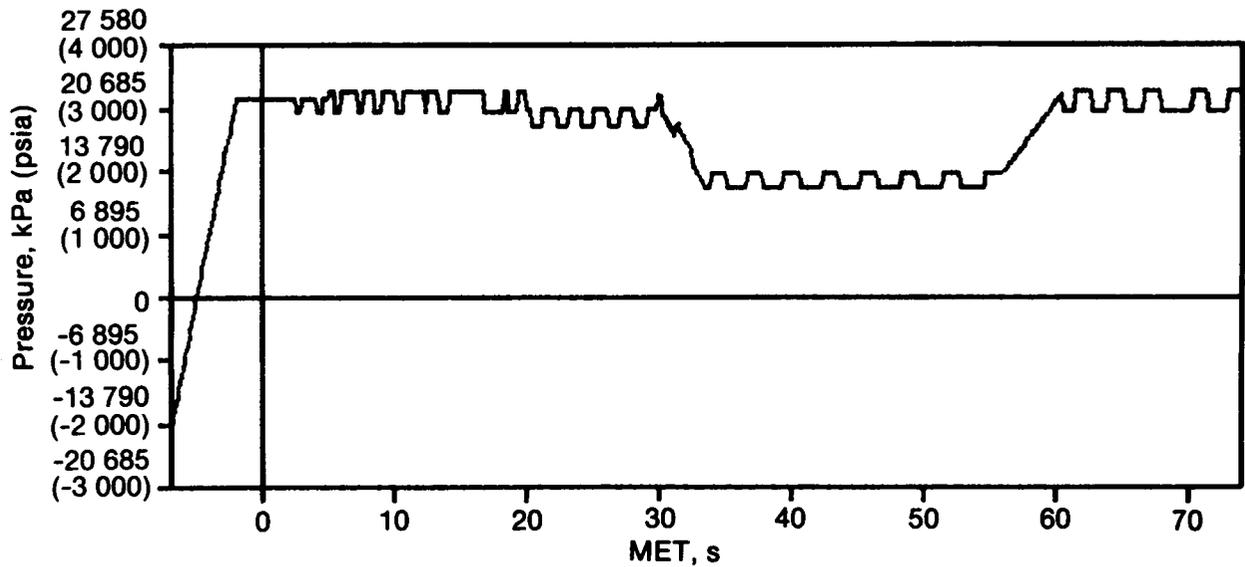


Figure A-34.- STS 51-F engine 2 outlet pressure (created).

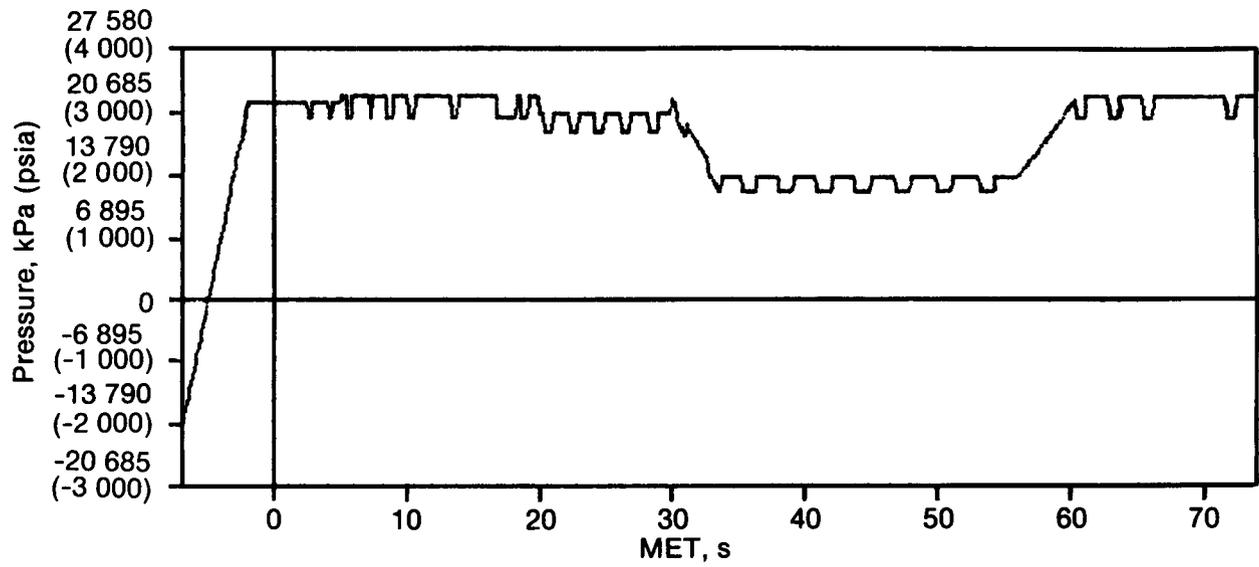


Figure A-35.- STS 51-F engine 3 outlet pressure (V41P1360A).

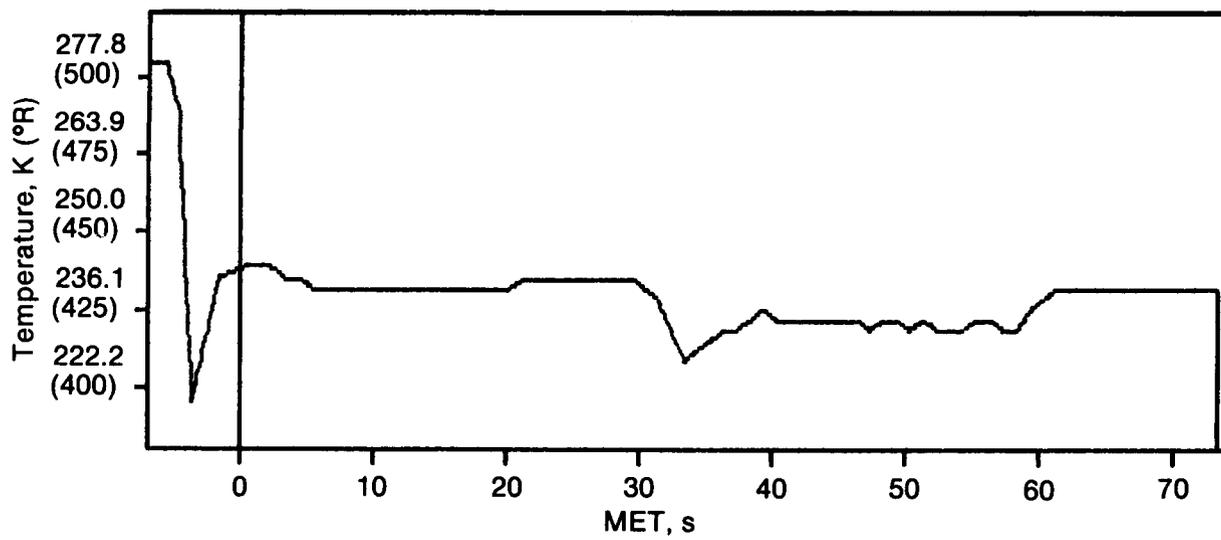


Figure A-36.- STS 51-F engine 1 outlet temperature (V41T1161A).

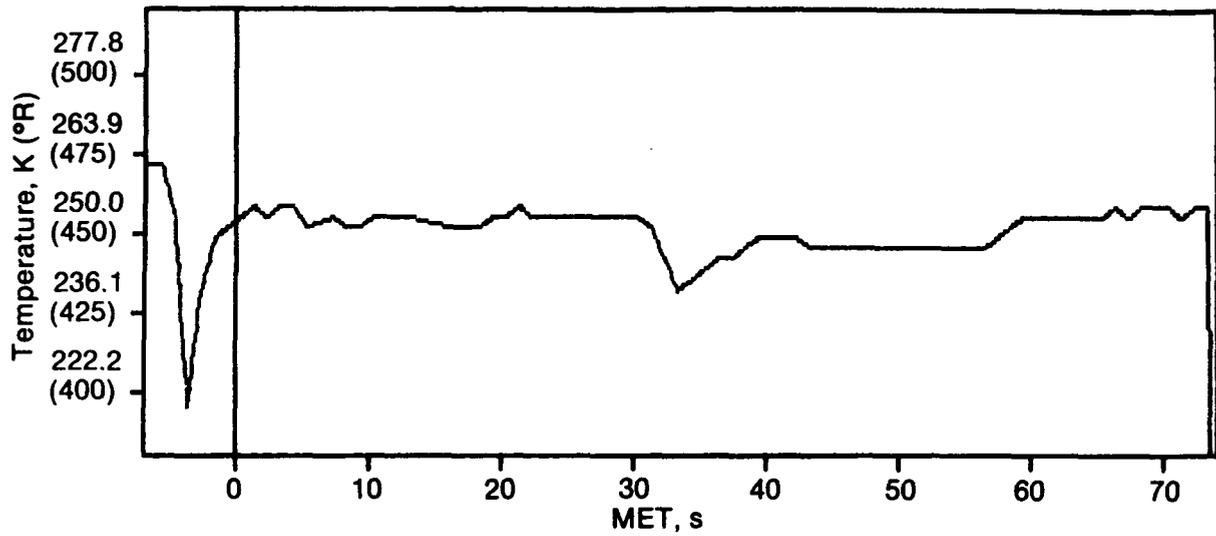


Figure A-37.- STS 51-F engine 2 outlet temperature (V41T1261A).

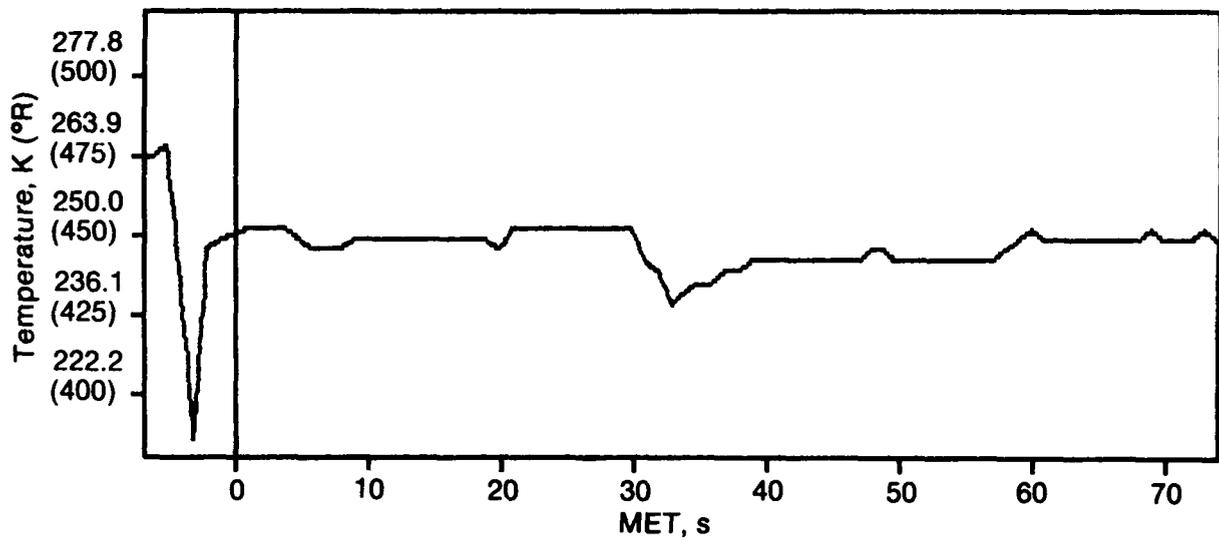


Figure A-38.- STS 51-F engine 3 outlet temperature (V41T1361A).

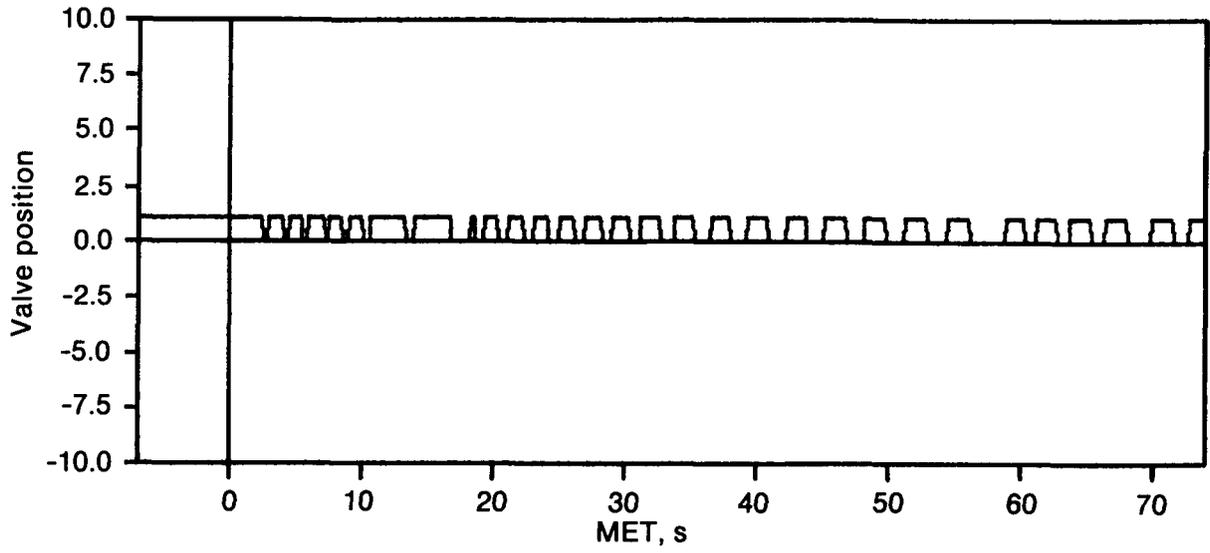


Figure A-39.- STS 51-F engine outlet valve 1 position (V41X1661E).

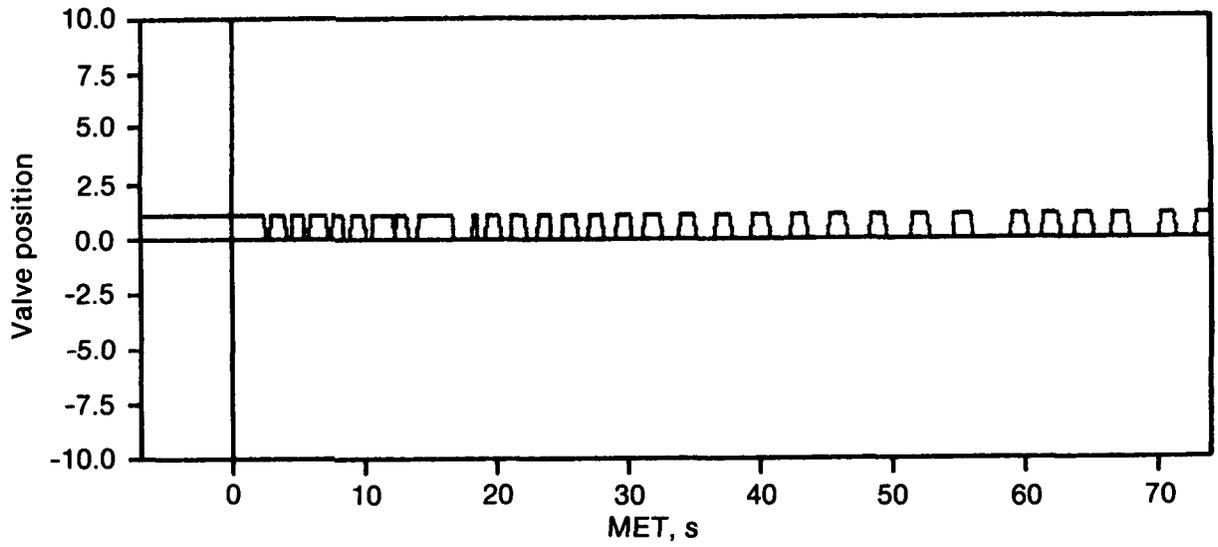


Figure A-40.- STS 51-F engine outlet valve 2 position (V41X1662E).

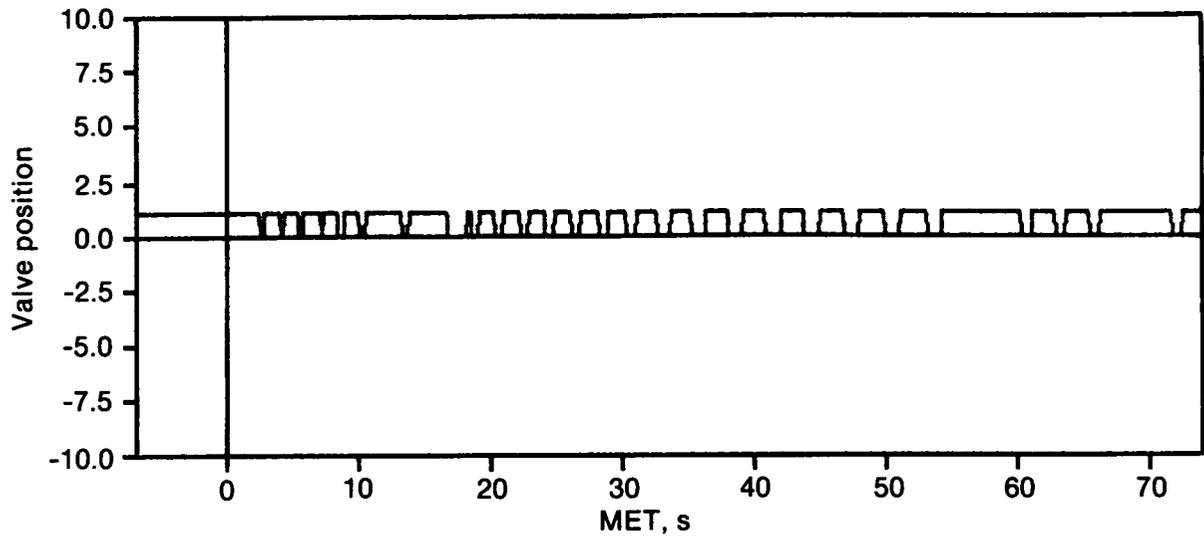


Figure A-41.- STS 51-F engine outlet valve 3 position (V41X1663E).

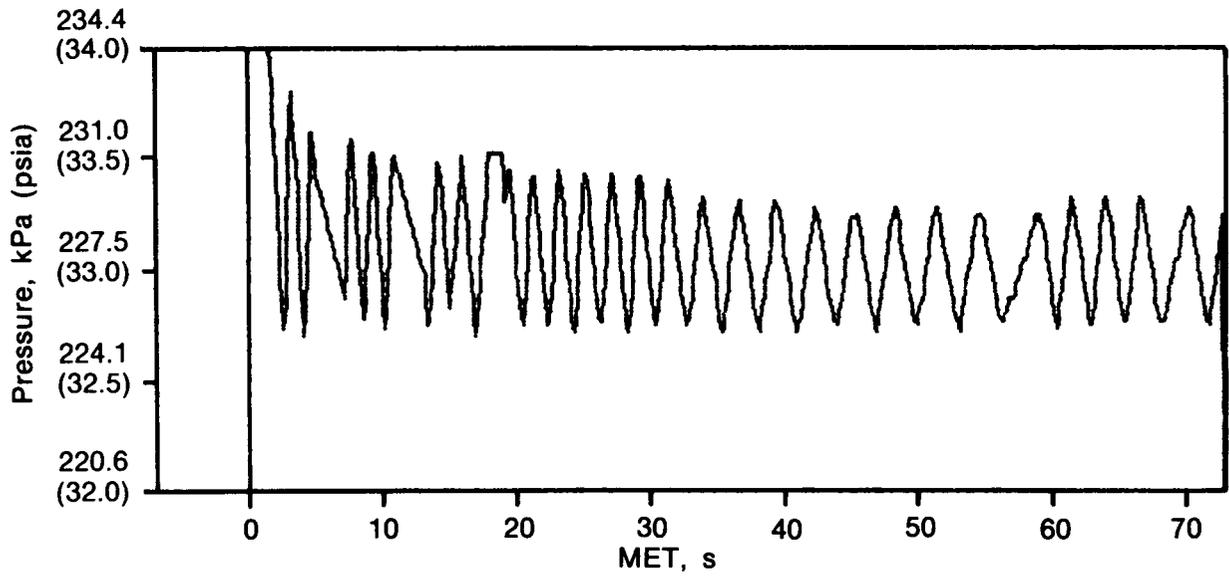


Figure A-42.- STS 51-F average ullage pressure (T41P1700C, T41P1701C, T41P1702C).

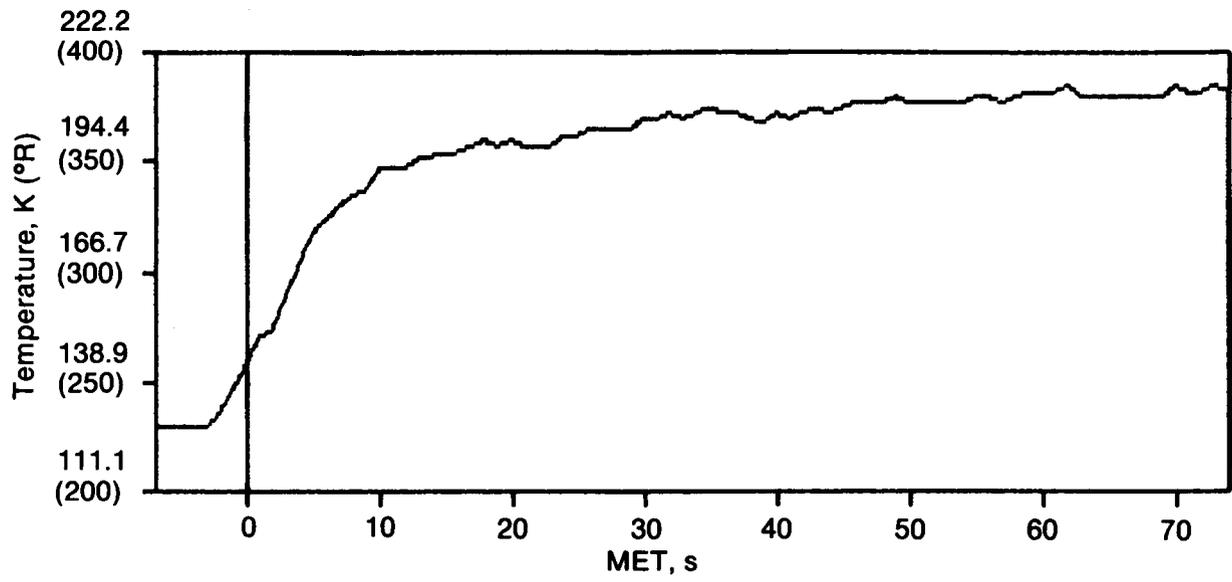


Figure A-43.- STS 51-F measured ullage temperature (T41T1705A).

APPENDIX B
DERIVATION OF CHOKED-FLOW MASS-FLOW-RATE EQUATIONS

The total quantity of gaseous hydrogen channeled to the hydrogen external tank ullage was determined by integrating the mass flow rate of gas expelled from each of the three main engines. No flowmeter data exist for this mass flow rate, however. Instead, the mass flow rate was computed using choked-flow equations taken from engine outlet pressure, engine outlet temperature, and downstream valve position flight data. The effective flow area, $C_d A$, was experimentally determined for each valve in both the open and the closed positions. The derivation of equation (2), gaseous hydrogen mass flow rate, is given herein.

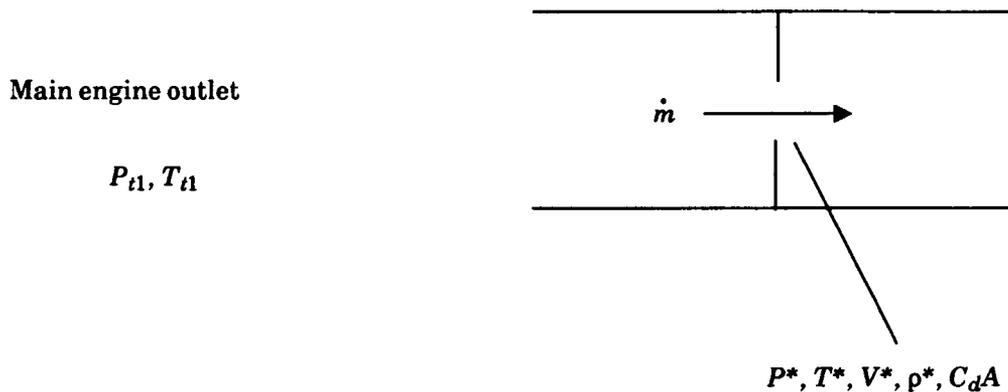


Figure B-1.- Choked engine outlet orifice.

1. Upstream stagnation pressure and temperature

$$P_{t1}, T_{t1}$$

These quantities are determined from engine outlet pressure P_{t1} and engine outlet temperature T_{t1} flight data.

2. Throat temperature T^*

$$\frac{T^*}{T_{t1}} = \frac{2}{k + 1} = \frac{2}{2.404} = 0.83$$

$$T^* = T_{t1} \times 0.83 \text{ (K or } ^\circ\text{R)}$$

3. Critical throat velocity V^*

$$\begin{aligned}
 V^* &= C = \sqrt{kRT^*} \\
 &= \sqrt{1.404 \times 4.124 \frac{\text{kJ}}{\text{kg-K}} \times T^* \times 1000.0 \frac{\text{J}}{\text{kJ}}} \\
 &= \left(\sqrt{1.404 \times 766.4 \frac{\text{ft-lbm}}{\text{lbm-}^\circ\text{R}} \times T^* \times 32.2 \frac{\text{lbm-ft}}{\text{lbf-s}}} \right) \\
 &= 186.14 \sqrt{T^*} \text{ ft/s} \\
 &= 76.23 \sqrt{T^*} \text{ m/s} \\
 &= 169.58 \sqrt{T_{t1}} \text{ ft/s} \\
 &= 69.45 \sqrt{T_{t1}} \text{ m/s}
 \end{aligned}$$

4. Throat pressure P^*

$$\begin{aligned}
 P^* &= \left(\frac{2}{k+1} \right)^{\frac{k}{k-1}} P_{t1} = \left(\frac{2}{2.404} \right)^{\frac{1.404}{0.404}} \\
 &= 0.53 P_{t1} \text{ kPa} \\
 &= 0.53 P_{t1} \text{ psia}
 \end{aligned}$$

5. Throat density ρ^*

$$\begin{aligned}
 \rho^* &= \frac{P^*}{RT^*} = \frac{0.53 P_{t1}}{4.124 \frac{\text{kN-m}}{\text{kg-K}}} \times 0.83 T_{t1} \\
 &= \left(\frac{0.53 P_{t1} \times 144.0 \frac{\text{in}^2}{\text{ft}^2}}{766.4 \frac{\text{ft-lbf}}{\text{lbm-}^\circ\text{R}} \times 0.83 T_{t1}} \right) \\
 &= \left(\frac{0.16 P_{t1} \text{ kg}}{T_{t1} \text{ m}^3} \right) \\
 &= \left(\frac{0.12 P_{t1} \text{ lbm}}{T_{t1} \text{ ft}^3} \right)
 \end{aligned}$$

6. Mass flow rate \dot{m}

$$\dot{m} = \rho^* V^* A C_d$$

$$= \frac{0.16 P_{t1}}{T_{t1}} \times 69.45 \sqrt{T_{t1}} \times A C_d \left[\begin{array}{l} P_t = \text{kPa} \\ A = \text{m}^2 \end{array} \right]$$

$$= \frac{0.12 P_{t1}}{T_{t1}} \times 169.58 \sqrt{T_{t1}} \times A C_d \left[\begin{array}{l} P_t = \text{lb/ft}^2 \\ A = \text{in}^2 \end{array} \right]$$

$$= \frac{10.75 P_{t1} A C_d}{\sqrt{T_{t1}}} \left[\begin{array}{l} P_t = \text{kPa} \\ A = \text{m}^2 \end{array} \right]$$

$$= \frac{0.14 P_{t1} A C_d}{\sqrt{T_{t1}}} \left[\begin{array}{l} P_t = \text{lb/in}^2 \\ A = \text{in}^2 \end{array} \right]$$

APPENDIX C
INITIAL LOAD DATA

The following data were used as reference material to determine the initial ullage volume prior to main engine start.

TABLE C-1.- HYDROGEN EXTERNAL TANK LOAD DATA

Parameter	STS 51-L	STS 61-A	STS 51-F
External tank total volume ^a (pressurized), m ³ (ft ³)	1508.1 (53 153.3)	1508.1 (53 153.3)	1508.1 (53 153.3)
Liquid hydrogen load at engine start command ^b (includes lines and Space Shuttle main engines), kg (lbm)	105 154 (231 617)	105 342 (232 030)	105 176 (231 666)
Orbiter line and Space Shuttle main engine loads, ^a kg (lbm)	139.4 (307.0)	139.4 (307.0)	139.4 (307.0)
Propellant bulk density, ^a kg/m ³ (lbm/ft ³)	70.7 (4.42)	70.7 (4.42)	70.7 (4.42)
Ullage volume at engine start command, m ³ (ft ³)	23.29 (821.0)	21.30 (750.8)	22.97 (809.7)
Moles gas (H ₂ and He) at engine start command, kmol (lb-mol)	6.67 (14.7)	6.58 (14.5)	6.22 (13.7)

^aSource – Rockwell International Baseline Propellant Inventories (predictions).

^bSource – Marshall Space Flight Center Flight Evaluation Report (reconstructions).

APPENDIX D
COMPUTER PROGRAMS CREATED

The following Fortran programs were used to perform engineering calculations on flight data for flight STS 51-L. Similar programs were created for flights STS 51-A and STS 51-F as well but are not included with this report. Before the programs are presented, they are listed in table D-1. The programs used to perform interpolations on tabular flight data and programs used to perform minor calculations and correlations also have not been included in this report.

TABLE D-1.- COMPUTER PROGRAMS CREATED FOR STS 51-L FLIGHT DATA

<u>Appendix</u>	<u>Program</u>	<u>Purpose</u>
D-1	ENGP51L	To create data files of engine outlet pressure
D-2	M51L	To calculate gaseous hydrogen mass flow rate from each main engine, then integrate to determine total mass expelled as a function of time
D-3	LHV51L	To integrate liquid hydrogen flow rate from the external tank to determine total volumetric fuel consumption
D-4	51L	To compute mean bulk ullage temperature and predict ullage pressure for comparison to measured ullage pressure
D-5	LIQLK51L	To compute hypothetical liquid leaks corresponding to pressurization loss observed
D-6	GASLK51L	To compute hypothetical gas leaks corresponding to pressurization loss observed

Figure D-1 illustrates the manner in which flight data were accessed by each of these programs.

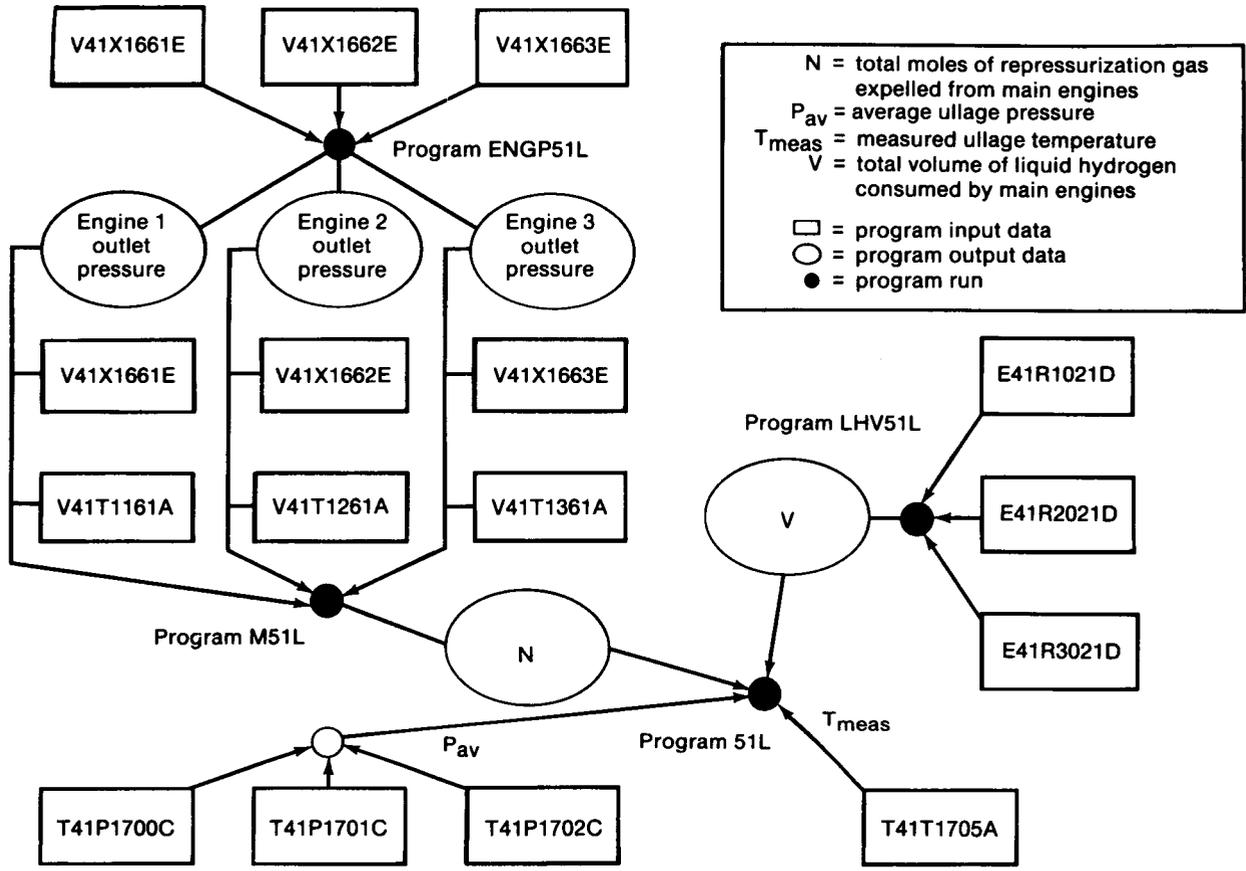


Figure D-1.- Flight data reduction.

APPENDIX D-1
ENGINE OUTLET PRESSURE

PROGRAM ENGP51L

C
C*****
C*****

C
C This program creates engine outlet pressure profiles for flight 51L.
C Although tabular data for engine outlet pressure exists, this data
C was acquired at 1 Hz only. Since the fluid system itself responds at a
C frequency greater than 1 Hz, interpolations to 0.01 seconds as performed
C on other flight data would be erroneous. Instead, plots of engine
C outlet pressure were used to create logic so that engine outlet pressure
C could be computed as a function of time at 0.01 second intervals. This
C logic is dependent on downstream flow control valve position and was
C created as follows:

- C
C 1) First, the plots were divided into like sections and equations
C of lines were written to match plots of flight data assuming no
C change in flow control valve position.
C
C 2) Second, for each section, pressure was decreased or increased
C in a step-wise fashion for each change in valve position.
C The reasoning is as follows. During a prescribed time
C interval, pressure fluctuates between an upper and lower limit.
C The upper limit is reached when the downstream flow control
C valve is in the closed position, thereby increasing the back
C pressure experienced by the engine. Likewise, the lower limit
C is reached when the downstream flow control valve is in the open
C position, thereby decreasing the back pressure experienced by
C the engine. It was assumed that changes between the upper and
C lower pressure limits were simultaneous with changes in flow
C control valve changes. Since changes in flow control valve
C position can be detected at a frequency >> 1 Hz, this method
C provides a way to more precisely predict changes in outlet
C pressure that would otherwise be missed by the 1 Hz pressure
C measurement itself.

C
C*****
C*****

DEFINITION OF VARIABLES
~~~~~

C           P\* = engine outlet pressure (psia)  
C           TIME = mission elapsed time (seconds)  
C           VP = flow control valve position (logical 1 = closed,  
C                   logical 0 = open)

C  
C\*\*\*\*\*  
C\*\*\*\*\*

READ FLOW CONTROL VALVE POSITION DATA  
~~~~~

C 1 READ (3,*,ERR=998) TIME, VP1, VP2, VP3
C

C*****
C*****

C
C
C
C
C

MATCH ENGINE 1 OUTLET PRESSURE LOGIC
WITH MISSION ELAPSED TIME
~~~~~

IF (TIME.LT.-2.0) GO TO 10  
IF ((TIME.LT.4.0).AND.(TIME.GE.-2.0)) GO TO 20  
IF ((TIME.LT.19.0).AND.(TIME.GE.4.0)) GO TO 30  
IF ((TIME.LT.22.0).AND.(TIME.GE.19.0)) GO TO 40  
IF ((TIME.LT.35.0).AND.(TIME.GE.22.0)) GO TO 50  
IF ((TIME.LT.39.0).AND.(TIME.GE.35.0)) GO TO 60  
IF ((TIME.LT.52.0).AND.(TIME.GE.39.0)) GO TO 70  
IF ((TIME.LT.56.0).AND.(TIME.GE.52.0)) GO TO 80  
IF (TIME.GE.56.0) GO TO 90

C  
C  
C  
C  
C  
C  
C

COMPUTE ENGINE 1 OUTLET PRESSURE  
~~~~~

10 P1 = 1192.86*TIME + 5725.7
GO TO 999
20 P1 = 3200.0
GO TO 999
30 IF (VP1.EQ.1.0) P1 = 3340.0
IF (VP1.EQ.0.0) P1 = 3000.0
GO TO 999
40 P1 = -113.3 * TIME + 5492.7
GO TO 999
50 P1 = 3000.0
GO TO 999
60 P1 = -237.5 * TIME + 11312.5
GO TO 999
70 P1 = 2060.0
GO TO 999
80 P1 = 322.5 * TIME - 14710.0
GO TO 999
90 P1 = 3350.0
GO TO 999

C
C
C
C
C
C

CREATE DATA FILE CONTAINING ENGINE 1 OUTLET PRESSURE VS. MET
~~~~~

999 WRITE (4,102) TIME,VP1,VP2,VP3,P1  
102 FORMAT (1X,4(F7.2,1X),F10.2)  
GO TO 1

C  
C  
C  
C  
C  
C  
C

rewind tape 4 containing engine 1 outlet pressure and mission  
elapsed time and prepare for computations of engines 2 and 3 outlet  
pressure

998 REWIND 4

C  
C\*\*\*\*\*

```

C*****
C
C          READ FLOW CONTROL VALVE POSITION DATA
C          ~~~~~
C
C      2 READ (4,*,ERR=997) TIME,VP1,VP2,VP3,P1
C
C*****
C*****
C          MATCH ENGINES 2 AND 3 OUTLET PRESSURE LOGIC
C          WITH MISSION ELAPSED TIMES
C          ~~~~~
C
C      IF (TIME.LT.-2.0) GO TO 100
C      IF ((TIME.LT.2.0).AND.(TIME.GE.-2.0)) GO TO 110
C      IF ((TIME.LT.19.0).AND.(TIME.GE.2.0)) GO TO 120
C      IF ((TIME.LT.21.5).AND.(TIME.GE.19.0)) GO TO 130
C      IF ((TIME.LT.35.0).AND.(TIME.GE.21.5)) GO TO 140
C      IF ((TIME.LT.38.0).AND.(TIME.GE.35.0)) GO TO 150
C      IF ((TIME.LT.51.5).AND.(TIME.GE.38.0)) GO TO 160
C      IF ((TIME.LT.57.0).AND.(TIME.GE.51.5)) GO TO 170
C      IF (TIME.GE.57.0) GO TO 180
C
C*****
C*****
C          COMPUTE ENGINES 2 AND 3 OUTLET PRESSURE
C          ~~~~~
C
C~~~~~ no engine outlet data exists for engine 2 due to a transducer failure;
C      since engine 3 outlet temperature is closest to engine 2 outlet
C      temperature, it was assumed that engine 3 outlet pressure logic would be
C      closest to engine 2 outlet pressure logic; therefore, engine 3 outlet
C      pressure logic was applied to engine 2; computations of engine 2 pressure
C      profile call upon engine 2 valve positions, and therefore, make this
C      pressure profile unique to engine 2
C
C      100 P2 = 1107.14*TIME + 5314.29
C          P3 = P2
C          GO TO 996
C      110 P2 = 3150.0
C          P3 = P2
C          GO TO 996
C      120 IF (VP2.EQ.0.0) P2 = 2900.0
C          IF (VP3.EQ.0.0) P3 = 2900.0
C          IF (VP2.EQ.1.0) P2 = 3250.0
C          IF (VP3.EQ.1.0) P3 = 3250.0
C          GO TO 996
C      130 P2 = -120.0* TIME + 5180.0
C          P3 = P2
C          GO TO 996
C      140 IF (VP2.EQ.1.0) P2 = 2900.0
C          IF (VP2.EQ.0.0) P2 = 2600.0
C          IF (VP3.EQ.1.0) P3 = 2900.0
C          IF (VP3.EQ.0.0) P3 = 2600.0
C          GO TO 996
C      150 P2 = -283.3 * TIME + 12516.7
C          P3 = P2
C          GO TO 996

```

```

160 P2 = 1750.0
    P3 = 1750.0
    GO TO 996
170 P2 = 209.1*TIME - 9018.2
    P3 = P2
    GO TO 996
180 IF (VP2.EQ.1.0) P2 = 3200.0
    IF (VP2.EQ.0.0) P2 = 2900.0
    IF (VP3.EQ.1.0) P3 = 3200.0
    IF (VP3.EQ.0.0) P3 = 2900.0
    GO TO 996
C
C*****
C*****
C
C          CREATE DATA FILE CONTAINING ENGINES 1, 2 AND 3
C          OUTLET PRESSURE VS. MISSION ELAPSED TIME
C          ~~~~~
C
C 996 WRITE (7,101) TIME,VP1,VP2,VP3,P1,P2,P3
C 101 FORMAT (1X,4(F7.2,1X),3(F10.2,1X))
C
C*****
C*****
C
C          GO TO 2
C 997 STOP
C          END

```



TMASS = 0.0

C  
C\*\*\*\*\*  
C\*\*\*\*\*

READ ENGINE OUTLET CHARACTERISTIC DATA  
~~~~~

5 READ (16,*,ERR=999) TIME,VP1,VP2,VP3,P1,P2,P3,T1,T2,T3

C
C*****
C*****

ASSIGN EFFECTIVE FLOW AREA FOR EACH SET OF ORIFICES
BASED ON CURRENT VALVE POSITION
~~~~~

IF (VP1.EQ.1.0) J = 2  
IF (VP1.EQ.0.0) J = 1  
IF (VP2.EQ.1.0) K = 2  
IF (VP2.EQ.0.0) K = 1  
IF (VP3.EQ.1.0) L = 2  
IF (VP3.EQ.0.0) L = 1

C  
C\*\*\*\*\*  
C\*\*\*\*\*

COMPUTE GASEOUS HYDROGEN MASS FLOW RATE AND  
INTEGRATE WITH RESPECT TO TIME  
~~~~~

C~~~~~ before time = -4.8, it was assumed that no gas was expelled from
C the engines (-4.8 is the average time between a change in engine
C outlet pressure from 0 to > 0 psia; a pressure > 0 indicates that
C gas is being detected at the pressure transducers); the next step
C ensures that no mass flow rate calculations are performed before this
C time

IF (TIME.LE.-4.8) P1 = 0.0
IF (TIME.LE.-4.8) P2 = 0.0
IF (TIME.LE.-4.8) P3 = 0.0

C
C~~~~~ gaseous mass flow rate through each flow control valve is computed
C using choked flow equations; the resulting equation for mass flow rate
C is given below and is a function of engine outlet pressure, engine
C outlet temperature and effective flow area only

RMDOT1 = (0.14*P1*CDA(1,J))/(T1**(0.5))
RMDOT2 = (0.14*P2*CDA(2,K))/(T2**(0.5))
RMDOT3 = (0.14*P3*CDA(3,L))/(T3**(0.5))
TMDOT(I) = RMDOT1 + RMDOT2 + RMDOT3

C
C~~~~~ total mass flow rate is integrated with respect to mission elapsed
C time to determine total mass of gaseous hydrogen expelled

TMASS = TMASS + 0.01*TMDOT(I)

C
C*****
C*****
C

```

C          CREATE DATA FILE CONTAINING TOTAL GASEOUS HYDROGEN MASS
C          EXPELLED FROM MAIN ENGINES AS A FUNCTION OF MISSION
C          ELAPSED TIME - TO BE ACCESSED BY ULLAGE PRESSURE
C          RECONSTRUCTION PROGRAM
C          ~~~~~
C          WRITE (17,101) TIME,RMDOT1,RMDOT2,RMDOT3,TMDOT(I),TMASS
C          101 FORMAT (1X,6(F10.2,1X))
C          *****
C          *****
C          GO TO 5
C          999 STOP
C          END

```

APPENDIX D-3
LIQUID HYDROGEN CONSUMED

PROGRAM LHV51L
DIMENSION TQ(100000)

This program computes total volume of liquid hydrogen that leaves the hydrogen external tank and is fed to each of the three main engines. The data files accessed contain the parameter:

- 1) liquid hydrogen fuel consumption (volumetric flow rate) for each main engine

These data files were created independently from interpolations on flight data and include data points for mission elapsed times of -7 to 73 seconds at every 1/100 th second.

DEFINITION OF VARIABLES
~~~~~

Q# = liquid hydrogen fuel consumption rate per engine (gall/min),  
where # = 1,2,3  
QFIT = liquid hydrogen fuel consumption rate per engine as computed  
from straight line fit of throttling profile for -5.5 < t  
< -2.0 (gall/min)  
TIME = mission elapsed time (seconds)  
TQ = total liquid hydrogen fuel consumption rate (ft3/min)  
VLH = total liquid hydrogen volume consumed by all three engines (ft3)

SET START CONDITIONS  
~~~~~

VLH = 0.0
TQ(1) = 0.0

READ ENGINE FUEL CONSUMPTION DATA
~~~~~

DO 10 I = 2,100000  
5 READ (29,\*,ERR=99) TIME,Q1,Q2,Q3  
IF (TIME.GT.73.0) GO TO 99

~~~~~ throttling profiles (fuel consumption vs. time) show sporadic  
fluctuations in flow rate prior to t = -2.0 seconds; these
fluctuations were attributed to flowmeter start-up transients,
therefore, tabular data was not used prior to t = -2 seconds;

instead, a straight line fit of fuel consumption as a function of time was computed from throttling profiles (-5.5 < t < -2.0) in order to wash out transients; this fit is given below

```

C
  IF ((TIME.GE.-5.5).AND.(TIME.LE.-2.0)) QFIT =
$ 3 * (4285.71 * TIME + 23971.43)
  IF ((TIME.GE.-5.5).AND.(TIME.LE.-2.0)) FLAG = 1
  IF (TIME.GT.-2.0) FLAG = 0
  IF (FLAG.EQ.1) Q1 = QFIT
  IF (FLAG.EQ.1) Q2 = QFIT
  IF (FLAG.EQ.1) Q3 = QFIT
  TQ(I) = (Q1 + Q2 + Q3) * 0.002228
C
C~~~~~ flight data indicates an increase in flow rate above a steady
C      recirculation value (approx. 410 gall/min) for each engine to occur at
C      an average time of t = -5.5 seconds; it was assumed that at this time
C      recirculation of liquid hydrogen ceased and a net flow out of the
C      external tank began; thus, prior to t = -5.5 seconds, fuel consumption
C      is set to zero
C
  IF (TIME.LT.-5.5) TQ(I) = 0.0
C
C*****
C*****
C
C      INTEGRATE FUEL CONSUMPTION WITH RESPECT TO TIME
C      ~~~~~
C
C~~~~~ integration performed using trapezoidal rule
C
  IPREV = I - 1
  VLH = 0.5*(TQ(I) + TQ(IPREV))*0.01 + VLH
C
C*****
C*****
C
C      CREATE DATA FILE CONTAINING TOTAL VOLUME OF LIQUID
C      HYDROGEN CONSUMED FROM EXTERNAL TANK AS A FUNCTION
C      OF MISSION ELAPSED TIME - TO BE ACCESSED BY ULLAGE
C      PRESSURE RECONSTRUCTION PROGRAM
C      ~~~~~
C
  WRITE (30,101) TIME,Q1,Q2,Q3,VLH
  101 FORMAT (1X,F7.2,1X,4(F12.3))
C
C*****
C*****
C
  10 CONTINUE
  99 STOP
  END

```

APPENDIX D-4
ULLAGE PRESSURE RECONSTRUCTION

PROGRAM 51L

C
C*****
C*****

C
C This program reconstructs hydrogen external tank ullage pressure
C for flight STS-51L. The data files accessed contain the critical
C parameters of:

- C
C 1) volume of liquid hydrogen consumed by main engines
C (computed in program LHV51L)
C 2) mass of gaseous hydrogen measured through flow
C control orifices (computed in program M51L)
C 3) measured ullage temperature
C 4) measured ullage pressure

C
C These data files were created independently from interpolations
C and computations on flight data and include data points for
C mission elapsed times of -7 to 73 seconds at every 1/100 th sec.

C*****
C*****
C

C REAL LIDEAL, L
C DIMENSION TGLOSS(100000), TLLOSS(100000), SEC(100000)
C TIME1 = -7.00

C
C*****
C*****

SET ESC ULLAGE CONDITIONS

C TGLOSS = total gas mass lost (lbm)
C TLLOSS = total liquid volume lost (ft3)
C NESC = total moles of gaseous hydrogen and helium
C at engine start command (moles)
C VESC = ullage volume at engine start command (ft3)

C TGLOSS(1) = 0.0
C TLLOSS(1) = 0.0
C NESC = 14.7
C VESC = 821.0

C
C*****
C*****

READ ULLAGE CHARACTERISTIC DATA

C TIME# = mission elapsed time (sec), where # =1,2,3,4
C V = measured volume of liquid hydrogen consumed from external
C tank (ft3)
C GMASS = mass of hydrogen gas computed to have passed through flow
C control orifices and expected to re-enter the ullage (lbm)
C TMEAS = measured ullage temperature (R)

```

C           P = measured average ullage pressure (psia)
C
C ~~~~~
C
C DO 10 NN = 2,1000
C READ (30,*,ERR=99) TIME1,*,*,*,V
C READ (17,*,ERR=99) TIME2,*,*,*,*,GMASS
C READ (43,*,ERR=99) TIME3,TMEAS
C READ (39,*,ERR=99) TIME4,*,*,*,P
C
C IF (TIME1.LT.-5.5) GO TO 10
C IF (TMEAS.EQ.0.0) TMEAS = 372.0
C IF (TIME4.GT.72.9) P = 33.12
C IF (TIME1.GT.73.0) GO TO 99
C IF (TIME1.NE.TIME2) WRITE (6,100)
C IF (TIME2.NE.TIME3) WRITE (6,100)
C IF (TIME3.NE.TIME4) WRITE (6,100)
100 FORMAT (1X,'ERROR')
C
C *****
C *****
C
C PREDICT ULLAGE PRESSURE
C
C ~~~~~
C
C RBAR = universal gas constant (ft-lbf/lbmole-R)
C TVOL = total ullage volume predicted from original volume at
C ESC and engine fuel consumption data (ft3)
C TMOLE = total moles of gas predicted in ullage based on moles
C at ESC plus quantity of gas measured to have passed
C through flow control orifices (mole)
C TBULK = mean bulk ullage temperature - computed from correlation
C and is a function of ullage volume and measured
C temperature (R)
C PEXP = predicted ullage pressure (psia)
C
C ~~~~~
C
C RBAR = 1545.0
C TVOL = VESC + V
C TMOLE = NESC + GMASS/2.016
C IF (TIME1.LT.40.0) GO TO 56
C
C ~~~~~ a curve fit was determined for measured temperature as a function
C of time (from 40 - 60 sec) in order to smooth measured ullage
C temperature data for use in derivation of a mean bulk temperature
C correlation; thus, final computation of a mean bulk temperature using
C the correlation requires a fitted measured temperature as opposed to
C an actual measured temperature; this fit is given below
C
C IF ((TIME1.GT.40.0).AND.(TIME1.LT.60.0))
C $ TMEAS = 363.401 -.634*TIME1 + .013289*(TIME1**2.0)
C
C ~~~~~ for time greater than 40 seconds, a mean bulk ullage temperature is
C computed as given by the correlation below
C
C AA = -248.996 + 7.94547E-2*TVOL
C BB = 2.99292 - 3.4105E-2*(TVOL**0.5)

```



```

C ~~~~~
C
C      TEFF = effective bulk ullage temperature required to make
C      measured and predicted ullage pressures match (this
C      parameter was used in deriving a correlation for
C      mean bulk ullage temperature by setting TEFF = TBULK =
C      f(TMEAS,TVOL))
C ~~~~~
C
C      TEFF = (P*144.0*TVOL)/(TMOLE*RBAR)
C
C *****
C *****
C
C      CREATE PLOT FILES
C
C ~~~~~ sends data every 0.05 seconds to plot files
C
C      A = NN
C      B = A/5
C      ICHECK = ANINT(B*100.0)
C      I = (NN/5) * 100.0
C      IF (ICHECK.NE.I) GO TO 10
C      WRITE (84,105) SEC(NN), TEMP
C      WRITE (86,105) SEC(NN), TEFF
C      WRITE (87,105) SEC(NN), P
C      WRITE (88,105) SEC(NN), PEXP
C      WRITE (93,105) SEC(NN), LIDEAL
C      WRITE (90,105) SEC(NN), L
C      WRITE (91,105) SEC(NN), GIDEAL
C      WRITE (92,105) SEC(NN), GMASS
C      WRITE (82,105) SEC(NN), TGLOSS(NN)
C      WRITE (83,105) SEC(NN), TLLOSS(NN)
C 105 FORMAT (1X, F7.2,1X,F10.2)
C
C *****
C *****
C
C 10 CONTINUE
C 99 STOP
C      END

```

APPENDIX D-5
LIQUID HYDROGEN LEAK RATE

PROGRAM LIQLK51L

C
C*****
C*****
C
C This program computes hypothetical liquid leaks (assuming
C no gas leak) that are required to make empirical ullage pressure rise
C and decay rates match predicted values. Time intervals correspond to
C either a single decay or rise slope or part of a single decay or rise
C slope on the saw-toothed ullage pressure plots. The data file accessed
C contains the critical parameters of:
C
C 1) time interval considered
C 2) empirical ullage pressure rise and decay rates
C 3) predicted ullage pressure assuming no leak
C 4) mean bulk ullage temperature
C 5) rate of change of mean bulk ullage temperature
C 6) average total mass of hydrogen fed to the external tank
C ullage
C 7) mass flow rate of gaseous hydrogen from engines to ullage
C 8) volumetric flow rate of liquid hydrogen to main engines
C 9) average total volume of liquid hydrogen consumed by
C main engines

C*****
C*****

DEFINITION OF VARIABLES
~~~~~

C  
C           ANSW = computed average liquid leak rate during time interval  
C                    considered (ft3/sec)  
C           DELTA = magnitude of time interval (seconds)  
C           P = average ullage pressure during time interval  
C                    considered (psia)  
C           PSLOPE = empirical ullage pressure rise and decay rate (as  
C                    determined from the slope of a straight line fit of  
C                    ullage pressure over the time interval considered)  
C                    (psia/sec)  
C           Q = volumetric flow rate of liquid hydrogen to the  
C                    main engines (ft3/sec)  
C           QLK = assumed liquid hydrogen leak rate (lbm/sec)  
C           RMASS = average total mass of hydrogen fed to the external  
C                    tank ullage  
C           RMDOT = gaseous hydrogen mass flow rate to ullage (lbm/sec)  
C           T = average mean bulk ullage temperature during time  
C                    interval considered  
C           TSLOPE = rate of change of mean bulk ullage temperature  
C                    (as determined from the slope of a straight line  
C                    fit of computed ullage temperature over the time interval  
C                    considered) (R/sec)  
C           V = average total volume of liquid hydrogen consumed by  
C                    the main engines (ft3)  
C           VPOT = total volume of estimated liquid leakage prior to  
C                    time interval considered (ft3)

```

C
C*****
C*****
C
C
C          READ INPUT DATA
C          ~~~~~
C
C          READ (49,*,ERR=50)DELT,PSLOPE,P,T,TSLOPE,RMASS,RMDOT,Q,V,QLK,VPOT
C
C*****
C*****
C
C          CALCULATE LEAK
C          ~~~~~
C~~~~~ the equation used to compute the liquid leak is not explicit for the
C leak itself; therefore, iterations were performed by assuming a leak
C (QLK), computing a leak (ANSW) which is dependent on QLK and then
C comparing the leak assumed (QLK) to the leak computed (ANSW); the
C procedure was repeated until QLK = ANSW
C
C          A = PSLOPE * 144.0
C          B = (P*TSLOPE*144.0)/T
C          E = 14.7 + RMASS/2.016
C          C = (P*RMDOT*144.0)/(E*2.016)
C          U = V + QLK*DELT/2.0 + VPOT + 821.0
C          D = (P*Q*144.0)/U
C          ANSW = (( -A + B + C - D)*U)/(P*144.0)
C          WRITE (6,99) A,B,C,D,E,ANSW
C          99 FORMAT (1X,5(F9.3,1X),E12.4)
C          50 STOP
C          END
C
C*****
C*****

```



```

C
C*****
C*****
C
C
C          READ INPUT DATA
C          ~~~~~
C
C          READ (49,*,ERR=50)DELT,PSLOPE,P,T,TSLOPE,RMASS,RMDOT,Q,V,RLK,RPOT
C
C*****
C*****
C
C          CALCULATE LEAK
C          ~~~~~
C~~~~~ the equation used to compute the gas leak is not explicit for the leak
C      itself; therefore, iterations were performed by assuming a leak (RLK),
C      computing a leak (ANSW) which is dependent on RLK and then comparing
C      the leak assumed (RLK) to the leak computed (ANSW); the procedure was
C      repeated until RLK = ANSW
C
C      A = PSLOPE * 144.0
C      B = (P * Q * 144.0)/(V + 821.0)
C      C = (P*TSLOPE*144.0)/T
C      E = ((RMASS - RPOT - (RLK*DELT)/2.0)/2.016 + 14.7)*2.016
C      D = (P*RMDOT*144.0)/E
C      ANSW = ((-A - B + C + D) * E)/(P * 144.0)
C      WRITE (6,99) A,B,C,D,E,ANSW
C      99 FORMAT (1X,5(F9.3,1X),E12.4)
C      50 STOP
C      END
C
C*****
C*****

```

1. Report No. NASA TM-100462		2. Government Accession No.		3. Recipient's Catalog No.	
4. Title and Subtitle Assessment of the Hydrogen External Tank Pressure Decay Anomaly on Space Transportation System (STS) 51-L				5. Report Date September 1988	
				6. Performing Organization Code 992-15-00-00-72	
7. Author(s) Theresa M. Buckley				8. Performing Organization Report No. S-574	
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16. Abstract  <p>Following the <i>Challenger</i> tragedy, an evaluation of the integrated main propulsion system flight data revealed a premature decay in the hydrogen external tank ullage pressure. A reconstruction of predicted ullage pressure versus time indicated an inconsistency between predicted and measured ullage pressure starting at approximately 65.5 seconds into the flight and reaching a maximum value between 72 and 72.9 seconds. This discrepancy could have been caused by a hydrogen gas leak or by a liquid hydrogen leak that occurred either in the pressurization system or in the external tank. The corresponding leak rates over the time interval from 65.5 to 72.9 seconds were estimated to range from 0.28 kg/s (0.62 lbm/s) <math>\pm</math> 41 percent to between 0.43 and 0.51 kg/s (0.94 and 1.12 lbm/s) <math>\pm</math> 1 percent for a gas leak and from 72.9 kg/s (160.5 lbm/s) <math>\pm</math> 41 percent to between 111.6 and 133.2 kg/s (245.8 and 293.3 lbm/s) <math>\pm</math> 1 percent for a liquid leak. No speculation is made to ascertain whether the leak is liquid or gas, as this cannot be determined from the analysis performed. Four structural failures in the hydrogen external tank were considered to explain the leak rates. A break in the 5-centimeter (2 inch) pressurization line, in the 13-centimeter (5 inch) vent line, or in the 43-centimeter (17 inch) feedline is not likely. A break in the 10-centimeter (4 inch) recirculation line with a larger structural failure occurring in the 72- to 73-second time period, the time of the visibly identified premature pressure decay, does seem plausible and the most likely of the four modes considered. These modes are not all-inclusive and do not preclude the possibility of a leak elsewhere in the tank.</p>					
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