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NUCLEAR ROCKET OPERATIONS

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REPORT NO. RN-S-0433

TO

AEC-NASA SPACE NUCLEAR PROPULSION OFFICE

EVALUATION OF DATA FROM ETS-1 FACILITY AND
NES-DUCT CHECKOUT TESTS

NERVA PROGRAM

CONTRACT SNP-1

NOVEMBER 1967

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CLASSIFICATION CATEGORY

UNCLASSIFIED

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AEROJET - GENERAL CORPORATION
A SUBSIDIARY OF THE GENERAL TIRE & RUBBER COMPANY

REPORT NO. RN-S-0433

EVALUATION OF DATA FROM ETS-1 FACILITY AND
NES-DUCT CHECKOUT TESTS

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ABSTRACT

This report covers the data analyses of the ETS-1 facility checkout tests (FEP-I through -IV) and the nuclear-exhaust-system (NES) Phase-I and Phase-II NEPN-1 tests. Specifically, the analyses were performed to further evaluate the facility and NES system performances and to upgrade the computer models for the engine cooldown predictions.

Data from the NES tests were analyzed to evaluate the duct and steam-generator performances, the simulated malfunction shutdowns, and various methods of preventing air backflow into the duct that could occur during certain modes of malfunction shutdowns. Flow resistances and dynamic response in the liquid-hydrogen cooldown system were checked out. Flow resistances in the various facility purge and cooldown lines were determined to upgrade the computer models for the engine cooldown predictions.

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CONTENTS

	<u>Page</u>
I. Introduction	1
II. Summary	2
III. Technical Discussion	6
A. NES-Duct Test-Data Analysis	6
1. Operating Characteristics	6
2. Simulated-Malfunction Shutdowns	18
3. Analysis of Flow Transients and Air Backflow	28
B. High-Pressure Liquid-Hydrogen Cooldown System	53
1. Flow-Resistance Data	53
2. Digital-Computer-Model Upgrading	65
3. Upgrading of Analog Mode by Using Dynamic-Response Data	69
C. Facility-Test-Data Analysis	82
1. Liquid-Nitrogen Cooldown Line	82
2. Gaseous-Helium Cooldown Line	95
3. Gaseous-Hydrogen Cooldown Line	123
4. Pump Inlet Line	142
References	155

APPENDIXES

A	Derivation of Correlation of Pressure Ratio with Valve Position	1
B	. Analog Computer Model and Equations	1

TABLES

<u>No.</u>	<u>Title</u>	<u>Page</u>
1	Basic Pressure-Drop and Heat-Transfer Equations	68
2	LH ₂ -Cooldown-Line Resistance Data	70
3	LN ₂ -Cooldown-Line Resistance Data	96
4	GHe-Cooldown-Line Resistance Data	124
5	GH ₂ -Cooldown-Line Resistance Data	143
6	Common Cooldown Line (LF-7 to Nozzle Torus)	144
7	XE-Systems-Analysis Design-Data-Book and FEP-III-2 Test Flow-Resistance and Pressure-Loss Data Comparisons	153
8	Revised XE-1 System-Analysis Design-Data-Book Flow Resistance and Pressure Losses	154

FIGURES

<u>No.</u>	<u>Title</u>	<u>Page</u>
1	Simplified Drawing of ETS-1 NES Duct	7
2	Steam Temperature and Pressure at Inlet of Secondary Ejector	9
3	Engine-Test-Compartment Pressure vs Steam Pressure at Secondary Ejector	11
4	Engine-Test-Compartment Pressure vs Nozzle Chamber Pressure, Start and Pull In with Secondary Ejector Operating	13
5	Expanded P_v vs P_c Pressures for P_c from 100 to 250 psia	14
6	Nozzle-Chamber Pressure vs Engine-Test-Compartment Pressure	16
7	Nozzle-Exit and Engine-Test-Compartment Pressures vs Nozzle Chamber Pressure	17
8	ETC and Primary-Nozzle Exit Pressures vs Nozzle Chamber Pressure During Start and Pull-In Periods	19
9	Engine-Test-Compartment Pressure vs Nozzle Chamber Pressure During Shutdown	20
10	Engine-Test-Compartment Pressure vs Chamber Pressure Immediately after Steam-Generator Shutdown (NES EPN-1 Tests)	22
11	Pressure Rise in Engine Test Compartment Immediately after Steam-Generator Shutdown	23
12	Engine-Test-Compartment Pressurization Rate vs Nozzle Chamber Pressure Immediately after Steam-Generator Shutdown (NES EPN-1 Tests)	25
13	Engine-Test-Compartment Pressure Rise Immediately after Primary-Nozzle Flow Shutdown (NES EPN-1 Tests)	26
14	Engine-Test-Compartment and Nozzle-Exit Pressures vs Nozzle Chamber Pressure Immediately after Primary-Nozzle Flow Shutdown (NES EPN-1 Test 4)	28
15	Engine-Test-Compartment and Nozzle-Exit Pressures vs Time Immediately after Primary-Nozzle Flow Shutdown (NES EPN-1 Test 4)	29

FIGURES (cont.)

<u>No.</u>	<u>Title</u>	<u>Page</u>
16	Engine-Test-Compartment Overpressurization and Rate of Pressure Rise as Functions of Rate of Nozzle-Chamber-Pressure Decrease (Primary-Nozzle Flow Shutdown NES EPN-1 Tests)	30
17	Schematic of ETS-1 and Analytical Model	33
18	Schematic Representation of Secondary-Fluid Temperature	35
19	Pressure and Flow Rate vs Time, NEPN-1 Run 1	38
20	Pressure and Flow Rate vs Time, NEPN-1 Run 2	39
21	Pressure and Flow Rate vs Time, NEPN-1 Run 3	40
22	Pressure and Flow Rate vs Time, NEPN-1 Run 4; Malfunction PCV-449	41
23	Pressure and Flow Rate vs Time, NEPN-1, Run 5; Steam-Generator Shutdown	42
24	Cell Pressure vs Secondary-Fluid Pressure	43
25	Pressure and Flow Rate vs Time for a 1500-cu-ft Accumulator	45
26	Pressure and Flow Rate vs Time for a 2000-cu-ft Accumulator	46
27	Pressure and Flow Rate vs Time (Basis: NEPN-1 Run No. 2, He Flow = 24 lbm/sec)	48
28	Pressure and Flow Rate vs Time (Basis: NEPN-1 Run No. 2, He Flow = 30 lbm/sec)	49
29	Pressure, Flow Rate, Pressure-Decay Rate vs Time (Basis: NEPN-1 Run No. 3)	51
30	Schematic of LH ₂ Engine-Emergency-Cooldown System	54
31	Comparison of Flow Rates, FEP-III LH ₂ Flow Test	57
32	Flow Characteristics, V5002 Tank to PCV-472; FEP-III LH ₂ Flow Test	58
33	PCV-472 Flow Characteristics, FEP-III LH ₂ Flow Test	61
34	Pressure Drop vs Flow Rate, Check-Valve 458; FEP-III LH ₂ Flow Test	62
35	Flow Characteristics, Check-Valve 458; FEP-III LH ₂ Flow Test	64

FIGURES (cont.)

<u>No.</u>	<u>Title</u>	<u>Page</u>
36	Flow Characteristics, Check-Valve 458 to FE-X Inlet; FEP-III LH ₂ Flow Test	66
37	Isometric of XE-Engine LH ₂ Cooldown Line	73
38	Isometric of XE Cooldown Simulator	74
39	Properties of Para-Hydrogen	76
40	Frequency Response, PT-484/θ ₄₇₂	77
41	Frequency Response, PT-774/θ ₄₇₂	78
42	Frequency Response, PT-772/θ ₄₇₂	79
43	Frequency Response, FT-12/θ ₄₇₂	80
44	Simplified Schematic, Liquid-Nitrogen Storage and Cooldown Line	83
45	K ₂₃ vs PCV-517 Stem Position	84
46	K _{Pcv-517} vs PCV-517 Stem Position	86
47	Comparison of FT-52 with FE-X	88
48	LN ₂ -System Line Temperatures vs Flow	88
49	LN ₂ -System Line Pressures vs Flow, Not Corrected for Elevation Head	89
50	Differential Pressures	90
51	K/A ² vs LN ₂ Flow, Without Elevation Correction	92
52	K/A ² vs LN ₂ Flow, with Elevation Correction	94
53	Simplified Flow and Instrumentation	99
54	Schematic, ETS-1 Gaseous-Helium Line Correlation of Flow Rates, FEP-IIB GHe Flow Test	101
55	Correlation of FT-415 Flow with PCV-750 Inlet Pressure, FEP-IIB GHe Flow Test	102
56	Correlation of FE-X Flow with PCV-750 Inlet Pressure, FEP-IIB GHe Flow Test	103
57	Gaseous-Helium-System Pressure-Drop Data, FEP-IIB GHe Flow Test (PCV-471 Outlet to FE-X Inlet, Based on Downstream Density)	106
58	Gaseous-Helium-System Pressure-Drop Data, FEP-IIB GHe Flow Test (Supply Bottles to FT-415 Inlet, Based on Downstream Density)	108

FIGURES (cont.)

<u>No.</u>	<u>Title</u>	<u>Page</u>
59	Pressure Ratio Across GHe-System Pressure-Control Valve, PCV-471, vs Valve Position; FEP-IIB GHe Flow Test	110
60	Gaseous-Helium-System Pressure-Control-Valve PCV-471 Flow Characteristics, FEP-IIB GHe Flow Test (Based on Downstream Density)	112
61	Gaseous-Helium-System Resistance Based on Upstream Density; FEP-IIB GHe Flow Test	114
62	Gaseous-Helium-System Pressure-Control-Valve PCV-471 Flow Characteristics; FEP-IIB GHe Flow Test (Based on Upstream Density)	115
63	Gaseous-Helium-System Pressure-Drop Data, FEP-IIB GHe Flow Test (from PT-473 to PT-103, Based on Upstream Density)	116
64	Gaseous-Helium-System Pressure-Drop Data, FEP-IIB GHe Flow Test (from PT-103 to PT-770, Based on Upstream Density)	118
65	Gaseous-Helium-Bottle Blowdown Characteristics Based on V-3206 Bottle Conditions)	119
66	Test vs Predicted Pressures, Gaseous-Helium Cooldown Line	125
67	Simplified Schematic, ETS-1 Gaseous-Hydrogen Cooldown System	126
68	FE-X and PCV-750 Inlet Pressures vs Flow Rate; FEP-IIB GH ₂ Test Data	129
69	Pressure Loss and Resistance from GH ₂ Bottles to PT-003; FEP-IIB GH ₂ Test Data	132
70	Pressure Loss and Resistance from PT-003 to PT-587; FEP-IIB GH ₂ Test Data	133
71	Pressure Ratio vs Valve Position for GH ₂ -Cooldown-System Control-Valve PCV-251; FEP-IIB GH ₂ Test Data	135
72	Flow Coefficient vs Valve Position for GH ₂ -Cooldown-System Control-Valve PCV-251; FEP-IIB Test Data	136
73	Pressure Loss and Resistance from PT-475 to PT-103; FEP-IIB GH ₂ Test Data	138

FIGURES (cont.)

<u>No.</u>	<u>Title</u>	<u>Page</u>
74	Pressure Loss and Resistance from PT-103 to PT-770; FEP-IIB GH ₂ Test Data	140
75	Pressure Loss and Resistance from PT-770 and PT-774; FEP-IIB GH ₂ Test Data	141
76	Pump Inlet Section	146
77	As-Measured Pressure Loss vs LH ₂ Flow Rate for Run- Tank Supply Line; FEP-III-2 Test Data	147
78	V5001 LH ₂ -Run-Tank Supply-Line Flow Resistance vs Propellant Flow Rate	148
79	Pressure Loss vs LH ₂ Flow Rate, Based on Total Pressure; FEP-III-2 Test Data	150
80	V5001 LH ₂ -Run-Tank Supply-Line Flow Resistance vs Flow Rate Based on Total Pressure	151
81	System Pressure Loss; LH ₂ -Run-Tank Supply Line to Pump	152

I. INTRODUCTION

This report covers the data analyses of the ETS-1 facility checkout tests (FEP-I through -IV) and the nuclear-exhaust-system (NES) Phase I and Phase II NEPN-1 tests. The test series on the facility systems were performed between September 1966 and March 1967, the NES Phase I tests were performed from 19 January 1967 to 7 February 1967, and the NES Phase II tests were initiated in June 1967 with the NEPN-1 tests. The primary purpose of these tests was to evaluate the capabilities of the facility systems and the NES in supporting X-engine testing.

Results of the facility checkout tests were reported in detail in the NTO Test Summary Report and FDET memoranda reports (References 1 through 9). Data were therefore analyzed primarily to obtain the flow resistances in the facility purge and cooldown lines from the test data. These flow resistances were then used to upgrade the computer models for the engine-system analytical programs.

The SPEAR Committee evaluated the NES Phase I and NEPN-1 test results (References 10 and 11). Data from these tests were further analyzed to evaluate the start, pull-in, and shutdown characteristics of the NES duct; the steam-generator performance at quasi-steady-state operating conditions; the simulated-malfunction shutdowns; and various methods of preventing air backflow into the duct that could occur during certain modes of malfunction shutdowns.

II. SUMMARY

A. NES-DUCT TEST-DATA ANALYSIS

Data analysis was concentrated on determining the operating characteristics of the system, comparing with subscale data, and performing studies to further ensure system safety. The NES is designed to achieve safe burning of the engine exhaust gas in the atmosphere, to prevent air backflow during normal or malfunction conditions of engine operation, and to simulated altitude conditions by aerodynamic pumping action of the engine exhaust gas. This system consists mainly of the shielded and vacuum-tight engine test compartment (ETC), the water-cooled exhaust duct with a secondary ejector, and steam generators to provide steam for the secondary ejector.

Prior to engine startup, the secondary ejector will be placed in operation. Initially, the ETC pressure will be reduced to about 10 psia with one steam generator brought up to full steam; with the normal two-module operation, the ETC pressure will be further reduced to about 7.5 psia. During start, as the nozzle chamber pressure is increased, the ETC pressure decreases smoothly. The nozzle starts to flow full at a chamber pressure of about 150 psia (conservative estimate). At a chamber pressure of about 160 psia, the pull-in point is reached, where the ETC attains a minimum operating pressure of about 0.3 psia. Beyond this point, engine operation is stable, with fully expanded flow at the nozzle exit.

Shutdown follows the duct-start operating line (ETC pressure vs nozzle chamber pressure) but is reversed in direction from the start and pull-in, provided the nozzle-chamber-pressure ramp-down rate is not excessively high.

Other operating characteristics of the NES are:

1. The steam generators, if maintained at a specified operating condition, will reproduce steam of consistent quality and quantity.
2. Start (or shutdown) operating line (ETC pressure vs nozzle chamber pressure) agrees well with the scale-model test prediction.
3. In the runs that have no flow through the turbine exhaust nozzles, pressure instability (corresponds to chamber pressure of 130 to 150 psia) occurs in the ETC as the pull-in point is approached. With flow through the turbine exhaust nozzles, however, there is no unstable pressure region as predicted from the scale-model tests.
4. During a rapid startup ramp (150 psi/sec in NEPN-1, Run 4), the ETC pressure lags the operating-line equilibrium pressure, whereas, during a rapid shutdown ramp, the ETC pressure again deviates from, but remains lower than, the equilibrium pressure.
5. Equilibrium pressure will be reached in the ETC if the chamber pressure is maintained at a hold condition after a rapid startup or shutdown ramp.

The test results showed that air backflow occurs when the steam-generator shutdown coincides with a low or zero primary flow (P_c less than 30 psia) or when the primary flow is cut off at a high rate (252 psi/sec) with the secondary ejector not operating.

Analytical models have been developed to predict the ETC over-pressurization due to a malfunction shutdown and the purge requirement to prevent air backflow into the duct. A mass-balance analytical model to calculate air backflow into the duct agrees well with the experimental data. Calculations that take into account a 35-mph head-on wind at the duct opening

and condensation of steam on the duct and the walls show that an on-line steam accumulator of 2000 cu ft will prevent air backflow. An alternative approach using helium gas to prevent air backflow shows that a 30-lbm/sec helium flow will be adequate; air backflow can be prevented if the demanded helium flow rate is established within 0.3 sec after a malfunction occurs in the steam-generator system.

B. FACILITY GAS AND LIQUID SYSTEMS

The test data were analyzed for five of the facility gas and liquid systems. Flow resistances were computed for each system and compared with values based on standard loss coefficients and on material obtained from the literature of vendors. The following summarizes the findings:

<u>System</u>	<u>Test Data</u>	<u>Overall Flow Resistance (K/A^2), in.⁻⁴</u>	
		<u>Theoretical</u>	<u>Corrected Theoretical</u>
Liquid nitrogen	0.26	0.29	0.25
Gaseous helium	0.16	0.18	0.18
Gaseous hydrogen	0.12	0.12	0.12
Liquid hydrogen	0.11	0.05	0.10
Pump inlet line	0.0039	0.002	0.0034

The major discrepancies were found to be in items such as valves and filters, which were based on vendor literature. Data for these items were frequently found to be in error, and the corrected numbers shown are based on measured values. The remaining errors, 0 to 12%, are felt to be as good as can be expected in analyzing system losses. Measurement errors in defining each of the systems and variations from theoretical could account for errors of this magnitude.

The actual loss-coefficient data were used to prepare models with the analog and digital computers. The following have been modeled.

Analog - High-pressure liquid-hydrogen system

Digital - High-pressure liquid-hydrogen system

Gaseous-helium cooldown line

Gaseous-hydrogen cooldown line

Liquid-nitrogen cooldown line

Dynamic response of the high-pressure liquid-hydrogen system on the analog computer has been used as a basis for defining and analyzing the emergency cooldown system. The helium data were used on the digital computer to establish the feasibility of a helium emergency cooldown.

III. TECHNICAL DISCUSSION

A. NES-DUCT TEST-DATA ANALYSIS

The NES duct is designed to achieve safe burning of the engine exhaust gas in the atmosphere, to prevent air backflow during normal or malfunction conditions of engine operation, and to simulate altitude conditions by aerodynamic pumping action of the engine exhaust gas.

NES design-demonstration tests were started in January 1967 to demonstrate the ability of the NES to perform its intended function at design and off-design conditions of engine nozzle chamber pressure (P_c) prior to XE-1 engine testing.

Data from these tests were analyzed to evaluate the start, pull-in, and shutdown characteristics of the NES duct, steam-generator performance at quasi-steady-state operating conditions, the simulated-malfunction shutdowns, and various methods of preventing the air backflow. Analytical models have been developed to predict ETC overpressurization due to a malfunction shutdown and the purge requirement to prevent air backflow into the duct and ETC.

1. Operating Characteristics

Figure 1 shows a simplified drawing of the NES duct, including the ETC and secondary ejector.

Prior to engine startup, the secondary steam ejector will be put in operation. Normally, steam from two steam generators (two-module operation) at 125 psia is injected into the secondary duct through the secondary ejector, consisting of a sonic-nozzle arrangement encircling the inlet of the secondary duct section. Initially, the ETC pressure will be reduced to about 10 psia with one steam generator brought up to full steam;

Figure 1 - Simplified Drawing of ETS-1 NES Duct

with the normal two-module operation, the ETC pressure will be further reduced to about 7.5 psia. During start, the ETC pressure decreases smoothly as the nozzle chamber pressure is increased. The nozzle starts to flow full at a chamber pressure of about 150 psia (a conservative estimate). At a chamber pressure of about 160 psia, the pull-in point is reached, where the ETC attains a minimum operating pressure of about 0.3 psia. Beyond this point, engine operation is stable, with fully expanded flow at the nozzle exit.

a. Performance of Steam Generators

Two steam generators will be used to supply steam to the secondary ejector to prevent air backflow into the duct during engine startup, shutdown, and malfunction. By its pumping action, the secondary ejector also maintains a partial vacuum in the ETC prior to the start of nozzle flow and assists in creating low pressures in the ETC during the startup and shutdown transients.

Steam is produced by continuous combustion of the propane and oxygen in the steam generators. Water is injected into the products of combustion to reduce the exhaust temperature and provide additional steam.

Test data were analyzed to check the reproducibility of the steam quality from test to test. Figure 2 shows plots of the steam pressures and temperatures at the inlet of the secondary ejector. The steam data have some random scatter but generally appear to be fairly consistent. It is therefore concluded that the steam generators, if maintained at a specified operating condition, will reproduce steam of consistent quality and quantity.

In some runs with no flow through the primary nozzle, the secondary ejector pulled the ETC pressure down to about 7 psia, whereas, in other runs, the ETC pressure was only reduced to about 8 psia. Effectiveness of the secondary ejector in reducing the ETC pressure depends on the momentum

Figure 2 - Steam Temperature and Pressure at Inlet of Secondary Ejector

of the steam from the choked nozzles expanding into the duct, which is proportional to $\dot{w}^2/\rho$, where $\dot{w}$ is the mass flow rate and ρ is the density of the steam.

From the perfect-gas law, and for the choked flow in the secondary-ejector nozzles, the following equations apply:

$$\rho_t = P_t / RT_t$$

and

$$\dot{w} = \frac{P_s A_t \gamma \left[\frac{2}{\gamma+1} \right]^{\frac{\gamma+1}{2(\gamma-1)}}}{(g \gamma RT_s)^{\frac{1}{2}}},$$

where P is pressure, T is temperature, A_t is the throat area of the secondary ejector, γ is the specific-heat ratio, R is the gas constant, and g is gravitational acceleration. Subscripts s and t refer respectively to the inlet and throat of the secondary ejector.

For isentropic flow through the secondary-ejector nozzles and with constant ρ and R assumed, $\dot{w}^2/\rho$ from the above two equations can be shown to be proportional to the steam pressure, P_s . Figure 3 shows plots of the ETC pressures and steam pressures taken from all tests. The correlation between the ETC and steam pressures, however, is poor, with wide data scatter. Further observation shows that the ETC pressures are consistently lower in the NEP-III and -IV tests, which used the small, temporary ETC, than in the NES EPN-1 tests, which used the larger, permanent ETC.

Additional studies will be conducted on this correlation as additional tests are conducted to determine the cause of the data spread.

NO PRIMARY NOZZLE FLOW

N₂ PURGE = 2 LB/SEC MAX

△ NEP-III AND -IV TESTS USING SMALL
TEMPORARY ENGINE COMPARTMENT

○ NES EPN-1 TESTS

Figure 3 - Engine-Test-Compartment Pressure vs Steam Pressure
at Secondary Ejector

b. Start and Pull-in

With two steam generators for normal duct operation, the secondary ejector can reduce pressure to about 7.5 psia in the ETC prior to the start of engine nozzle flow. Start and pull-in characteristics (ETC pressure vs chamber pressure) of the duct with the secondary ejector operating are shown in Figure 4.

During start, flow through the nozzle is separating in the divergent section. The point of flow separation moves from downstream of the throat to the nozzle exit as the chamber pressure is increased. The nozzle starts to flow full at a chamber pressure of about 150 psia as the pull-in point is approached. At the pull-in point, the ETC attains a minimum operating pressure of about 0.3 psia, and the expanding supersonic flow from the nozzle exit is completely attached to the duct wall, with the result that the shock system moves into the duct. Across the shocks in the duct, the flow goes from supersonic to subsonic flow by recovering the pressure to match the back pressure, which is at the atmospheric pressure of 12.8 psia. Thus, the combination of the supersonic nozzle flow expanding into the duct, the resulting shocks in the duct, and the pressure recovery enables the nozzle exit and the ETC to maintain low pressures for altitude simulation.

During the start, and prior to reaching the pull-in point, the ETC pressure decreases smoothly as the nozzle chamber pressure is increased, although the flow is separating in the nozzle. In the runs that have no flow through the turbine exhaust nozzles, pressure instability (corresponding to P_c of 130 to 150 psia) occurs in the ETC as the pull-in point is approached (Figure 5). This instability is caused by the expanding supersonic flow from the nozzle alternately attaching to and detaching from the duct wall. This pressure instability, however, will not greatly affect engine operation because, by this time, flow through the nozzle is already choked. Consequently the pressure oscillations downstream of the throat will

SYMBOLS	TEST NO.	TEST CONDITIONS
○	NEP-II-1	0.5 TO 1.5 LB/SEC GN ₂ PURGE
△	NEP-III-1	1.8 LB/SEC GN ₂ PURGE
◻	NEP-IV-1	1.5 LB/SEC GN ₂ PURGE; 0 TO 6.5 LB/SEC TURBINE EXHAUST FLOW
◇	NEPN-1	1.5 LB/SEC GN ₂ PURGE

Figure 4 - Engine-Test-Compartment Pressure vs Nozzle Chamber Pressure, Start and Pull In with Secondary Ejector Operating

A = UNSTABLE PRESSURE REGION

B = PULL-IN PRESSURE RANGE WHERE
 P_V REACHES MINIMUM OPERATING PRESSURE

SYMBOL	TEST NO.	PURGE (LB/SEC)	TURBINE FLOW (LB/SEC)	P_C RAMP RATE (PSI/SEC)
○	NEP-II-1	0.5 TO 1.5	NONE	10.2
△	NEP-III-1	1.8	NONE	18.0
◻	NEP-IV-1	1.5	1.8 TO 3.7	94.6

Figure 5 - Expanded P_V vs P_C Pressures for P_C from 100 to 250 psia

not be transmitted back to, and will not affect, the flow through the engine system. There will be heat-transfer fluctuation in the nozzle, but these should not adversely affect engine operation.

With flow through the turbine exhaust nozzles (NEP-IV-1 test), there was no apparent unstable pressure region as the pull-in point was approached. This effect is in agreement with the scale-model tests (Reference 12) in which introduction of turbine exhaust flow eliminated pressure instability (buzzing) during start.

The NEP-IV-1 run made with turbine flow and with a high chamber-pressure ramp rate (94.6 psi/sec) indicated a higher ETC pressure during startup than was indicated in the other tests that were run with no turbine flow and at slow chamber-pressure ramp rates.

Data indicated that the higher ETC pressure during this startup was caused by the ETC pressure lagging the equilibrium pressure because of the fast chamber-pressure ramp rate. This effect is shown in Figure 6, in which the ETC pressure is plotted against the chamber pressure for the NEPN-1 runs; these runs were ramped up at widely different rates and were held at steady-state chamber pressures before shutdown. NEPN-1 Run 4, with a ramp rate of 150 psi/sec, indicated that the ETC pressure lagged by approximately 1.2 psi; the equilibrium pressure was reached after about 5.5 sec at the hold condition. In NEPN-1 Run 5, with a slow ramp rate, the ETC pressure lagged the equilibrium pressure by a very small value.

The high ETC pressures during startup in NEP-IV-1 pose no problem to engine operation. In this run the nozzle started to flow full at about 150 psia. At chamber pressures higher than 158 psia, the ETC pressure remained below the nozzle exit pressure. In Figure 7 the nozzle exit pressure and the ETC pressure are plotted against the chamber pressure to illustrate this effect.

Figure 6 - Nozzle-Chamber Pressure vs Engine-Test-Compartment Pressure

Figure 7 - Nozzle-Exit and Engine-Test-Compartment Pressures vs Nozzle Chamber Pressure

ETC and nozzle exit pressures are plotted against chamber pressure in Figure 8 for the start and pull-in periods of all tests. The ETC pressures agree well with the scale-model test data shown in the figure. During start, while the flow is separating in the nozzle, the nozzle exit and ETC pressures remain about the same; beyond the pull-in region (155- to 165-psia chamber pressure), the ETC pressure is pulled much lower than the nozzle exit pressure.

c. Shutdown

Shutdown is expected to follow the duct operating line, but in the reverse direction from that for start and pull-in, provided the chamber-pressure ramp-down rate is not excessively high. This effect is shown in Figure 9, in which ETC pressure is plotted against chamber pressure during the NEP-III-1 test. A rapid chamber-pressure ramp-down rate will, however, cause the ETC pressure to deviate from the normal duct operating line, as described in detail in Section III.A.2.

2. Simulated-Malfunction Shutdowns

The experimental results of the 1/8-scale model and the full-scale model of the ETS-1 duct show that the sudden loss of primary flow and the total loss of steam resulting from a malfunction of the steam-generator (secondary fluid) system can cause overpressurization of the cell and air backflow into the duct. Although overpressurization does not seem to be a severe problem because of its low magnitude, air backflow may present a serious problem, as it may result in formation of an explosive mixture in combination with the primary fluid (H_2)

To determine the effect of steam-generator and primary-flow shutdown on ETC repressurization and oxygen content, a series of simulated-malfunction shutdowns was performed during the NES EPN-1 tests. Results of

TEST CONDITION: TWO S.G. OPERATING
SYMBOLS USED:

○ ETC PRESSURE

△ PRI NOZZLE EXIT PRESSURE

DATA FROM:

NEP-II-1 AND 2

NEP-III-1

NEP-IV-1

NEPN-1 TESTS 4 AND 5

Figure 8 - ETC and Primary-Nozzle Exit Pressures vs Nozzle Chamber Pressure During Start and Pull-In Periods

NEP-III-1 SHUTDOWN DATA
WITH SECONDARY EJECTOR
OPERATING, GN_2 PURGE =
1.5 LB/SEC, AND P_C RAMP = -16 PSI/SEC

Figure 9 - Engine-Test-Compartment Pressure vs Nozzle Chamber Pressure During Shutdown

the tests showed that air backflow can occur when loss of the secondary ejector due to steam-generator malfunction coincides with a low- or no-flow condition (P_c at less than 30 psia) through the primary nozzle and the primary-nozzle flow is shut down at a high rate with the steam generators not operating.

a. Steam-Generator Shutdown

Figure 10 shows ETC pressure vs chamber pressure immediately after steam-generator shutdown. Super-imposed on the figure are the predicted duct operating lines during shutdown (or start) when there is nozzle flow only and when there is nozzle flow and one or two steam generators are operating. Rise in the ETC pressure immediately after steam-generator shutdown appears as a straight vertical line in this figure between the operating points where there is nozzle flow with two steam generators operating and where there is nozzle flow only. With no flow through the nozzle, the ETC pressure rises from about 8 psia to atmospheric pressure (NES EPN-1 Test 2). The figure also shows that steam-generator shutdown at a chamber pressure higher than 200 psia will not cause the ETC pressure to rise because, in this chamber-pressure range, the pumping action of the primary-nozzle flow far exceeds that of the secondary ejector, so that the latter no longer contributes to the maintaining of reduced pressures in the ETC.

This figure, however, does not reveal the rate of the ETC pressure rise, which gives an indication of the rate at which gases backflow into the ETC. In Figure 11, the pressure rise in the ETC is shown as a function of time immediately after steam-generator shutdown. With no flow through the primary nozzle, the steam-generator shutdown was initially accompanied by a rapid rise in ETC pressure, which exhibited an overdamped oscillation and a small overpressurization, both with and without a large quantity of purge-nitrogen gas flow (NES EPN-1 Tests 2 and 3). This condition means that, with a large quantity of the purge gas accumulating in the ETC, a small or negligible quantity of gases is allowed to backflow into the ETC to pressurize

Figure 10 - Engine-Test-Compartment Pressure vs Chamber Pressure
Immediately after Steam-Generator Shutdown (NES EPN-1 Tests)

SYMBOL	TEST NO.	PRIMARY FLOW (LB/SEC)	PRIMARY CHAMBER PRESSURE (PSIA)	PURGE FLOW (LB/SEC)
△	NES EPN-1 NO. 2	0	8	1.8 (GN ₂)
○	NES EPN-1 NO. 3	0	9	30 (GN ₂)
□	NES EPN-1 NO. 4	187 (GN ₂)	123	0.5 (GN ₂)
▽	NES EPN-1 NO. 5	88 (GN ₂)	58	0.5 (GN ₂)

Figure 11 - Pressure Rise in Engine Test Compartment Immediately after Steam-Generator Shutdown

the compartment, whereas with a small quantity of the purge gas accumulating, a large quantity of gases is backflowing into the ETC.

With the primary nozzle flowing, as in NES EPN-1, Tests 4 and 5, steam-generator shutdown is accompanied by a slower rate of rise in ETC pressure. Further observation shows that the ETC pressure will rise at a decreasing rate with increasing primary-nozzle flow rate (or higher chamber pressure) and will cease to rise at chamber pressures higher than 200 psia.

Although only a few data points were available from the test, the rate of ETC pressure rise correlated reasonably well with chamber pressure, as shown in Figure 12. The repressurization rate is highest when steam-generator shutdown coincides with zero flow through the primary nozzle; this condition is potentially dangerous because of the possibility of air backflowing into the duct and ETC.

b. Primary-Nozzle-Flow Shutdown

Some test data were available for analysis of duct performance during the nozzle-flow shutdown when the secondary ejector was not operating. A rapid rate of nozzle-flow shutdown induced the ETC pressure to deviate from the equilibrium pressure on the duct operating line; the nozzle-flow shutdown rate and the initial ETC pressure level affected the overpressurization in the ETC.

Figure 13 shows the ETC pressure rise immediately after primary-nozzle-flow shutdown with the secondary ejector not operating. With chamber pressure decreasing at a rate of 10 psi/sec, the ETC pressure rise followed the normal duct operating line, whereas, at much higher rates, the ETC pressures deviated from the duct operating line. Shutdown at a high rate from initially low ETC pressure (NES EPN-1 Test 4 shutdown at a 252-psi/sec rate from a 6.4-psi initial pressure in the ETC) was further observed to be accompanied by a large overpressurization in the ETC.

PRIMARY NOZZLE FLOW IS GN_2

Figure 12 - Engine-Test-Compartment Pressurization Rate vs Nozzle Chamber Pressure Immediately after Steam-Generator Shutdown (NES EPN-1 Tests)

Figure 13 - Engine-Test-Compartment Pressure Rise Immediately after Primary-Nozzle Flow Shutdown (NES EPN-1 Tests)

To analyze the mechanism that causes a large deviation from the normal duct operating line at a fast primary-nozzle shutdown rate (NES EPN-1 Test 4), the ETC and nozzle exit pressures were plotted against chamber pressure (Figure 14). Both the ETC and nozzle-exit pressures can be observed to rise gradually until a chamber pressure of about 30 psia is reached, and to rise rapidly thereafter. This condition is probably caused by a breakdown in the pumping action of the primary-nozzle flow as the flow-separation point moves rapidly into the throat section, and by a lag in the surging of gas into the ETC because of the rapid transient.

The ETC and nozzle exit pressures from the NES EPN-1 Test 4 are replotted in Figure 15 as functions of time. These pressures rose slowly during the first 0.4 sec and then began to rise rapidly, resulting in overpressurization and peaking out at 1.2 sec.

The ETC overpressurization and rate of pressure rise were correlated with the chamber-pressure decay rate as shown in Figure 16. In this correlation, the effect of the initial level of ETC pressure is not explicitly shown because the available data are very limited. The analytical model used to predict overpressurization, however, does take into account of the effect of initial ETC pressure, as well as the chamber-pressure decay rate.

Although the correlation is based on a limited amount of data, the trends of overpressurization and repressurization rate shown in the figure should be correct. The figure also shows that a fast nozzle-flow shutdown without the secondary ejector operating is potentially dangerous because of overpressurization in the ETC and the possibility of air back-flowing into the duct and ETC.

3. Analysis of Flow Transients and Air Backflow

This section summarizes attempts to develop a simple analytical model to evaluate the dynamics of the duct and steam-generator shutdown.

Figure 14 - Engine-Test-Compartment and Nozzle-Exit Pressures vs Nozzle Chamber Pressure Immediately after Primary-Nozzle Flow Shutdown (NES EPN-1 Test 4)

Figure 15 - Engine-Test-Compartment and Nozzle-Exit Pressures vs Time Immediately after Primary-Nozzle Flow Shutdown (NES EPN-1 Test 4)

$(P_{V \text{ MAX}} - P_A)$ = DIFFERENCE BETWEEN MAXIMUM ETC PRE SSURE
AND ATMOSPHERIC PRESSURE

$\Delta P_V / \Delta \theta$ = RATE OF ETC PRESSURE RISE

$\Delta P_C / \Delta \theta$ = RATE OF NOZZLE CHAMBER PRESSURE DECREASE

GN₂ PURGE RATE = 25 LB/SEC

Figure 16 - Engine-Test-Compartment Overpressurization and Rate of Pressure Rise as Functions of Rate of Nozzle-Chamber-Pressure Decrease (Primary-Nozzle Flow Shutdown NES EPN-1 Tests)

This model was needed in studies performed to evaluate facility modifications under consideration to prevent the backflow of air in the event the steam-generator should malfunction during startup.

Understanding the dynamic behavior of the system, such as reverse flow and the frequency and amplitude of pressure surges, would require an extensive analysis of the three-dimensional flow field produced by the complex configuration of the cell and the duct system and resulting from the presence of shock waves. However, a purely empirical approach to the complex flow problem was made on the basis of mass balance alone to try to understand the gross behavior of the system and to identify the conditions under which air backflow occurs. This approach provided a basis for modifying the design and operating conditions to prevent air backflow. The approach is recognized as an oversimplification of the real flow phenomena, and the use of experimental data is deemed necessary to verify the soundness of any conclusions reached with this model.

a. Analytical Approach

Mass-balance calculations were made by using NEPN-1 data with the following simplifying assumptions:

- (1) The walls of the duct and the test cell are adiabatic.
- (2) Mixing of the fluids is complete.
- (3) Fluids in the duct obey the perfect-gas law.

The equation of mass balance may be presented as follows:

$$\sum_{i=1}^n V_i \frac{d\rho_i}{dt} = \sum \text{Flow Rate}$$

$$= W_P + W_C + W_S - W_{SD} \dots\dots (1)$$

For a perfect gas, Equation 1 reduces to:

$$\sum_{i=1}^n \frac{V_i M_i}{R T_i} \frac{dP_i}{dt} = W_P + W_C + W_S - W_{SD}, \quad (2)$$

where

- V_i = volume of i_{th} compartment,
- P_i = pressure of fluid in i_{th} compartment,
- M_i = molecular weight of fluid in i_{th} compartment
- T_i = temperature of fluid in i_{th} compartment,
- W_P = flow rate of purge gas,
- W_C = flow rate of primary fluid,
- W_S = flow rate of secondary fluid,
- W_{SD} = flow rate of fluid in secondary duct,
- t = time.

Because of the lack of sufficient experimental data, the system was divided into the following two compartments (see Figure 17), and the pressure and temperature changes in each compartment were assumed to take place instantaneously.

- V_1 = volume of cell + volume of primary duct;
- V_2 = volume of secondary duct.

Equation 2 becomes:

$$\frac{V_1 M_1}{R T_1} \frac{dP_1}{dt} + \frac{V_2 M_2}{R T_2} \frac{dP_2}{dt} = W_P + W_C + W_S - W_{SD} \quad (3)$$

Figure 17 - Schematic of ETS-1 and Analytical Model

Because the mass change that occurs in V_2 (pressure change of about 2 psia in 492 cu ft) is much smaller than that which occurs in V_1 (pressure change of 5.3 psia in 6783 cu ft), the second term in the above equation may be neglected. In the absence of primary gas flow, Equation 3 becomes:

$$\frac{V_1}{R} \frac{M_1}{T_1} \frac{dP_1}{dt} = W_P + W_S - W_{SD} \quad (4)$$

From the experimental values of T_1 and $\frac{dP_1}{dt}$, the rate of change of mass in the cell was calculated and compared with the combined flow rates of steam and purge gas, $W_P + W_S$. When the rate of change of mass in the cell was larger than $W_P + W_S$, it was concluded that air must enter the cell to satisfy the equation of conservation of mass. Thus, from a simple mass balance, one can qualitatively determine whether or not air will enter the duct under a given set of operating conditions.

To make a quantitative estimate of steam (and thus air) needed to repressurize the cell, the mixed-mean properties of the fluid mixture were calculated by assuming perfect mixing. The temperature of the steam in the secondary duct was estimated by assuming that the fluid originally at T_{SC} expands through the steam nozzles and passes through a normal shock with a temperature-recovery factor* (r) of 0.865 (Reference 13). It was further assumed that the fluid in the secondary duct at P_{SD} (14.8 psia) expands adiabatically to P_1 (7.5 psia). Thus, the temperature of the steam (T_S) before mixing with the cell fluid may be represented by (see Figure 18):

$$T_S = T_{SC} \frac{1 + r \left(\frac{\gamma-1}{2} \right) M^2}{1 + \frac{\gamma-1}{2} M^2} \left(\frac{P_1}{P_{SD}} \right)^{\frac{\gamma-1}{\gamma}} \quad (5)$$

Figure 18 - Schematic Representation of Secondary-Fluid Temperature

For $\gamma = 1.255$ and $M = 2.4$ (expansion of steam from 110 to 7.5 psia through steam nozzles), the above equation reduces to:

$$T_S = 0.8255 T_{SC} \quad (6)$$

The mixed-mean properties (indicated with subscript 1) were calculated as follows:

a. Molecular weight, M_1

$$M_1 = \frac{W_1 + W_S}{\frac{W_1}{M_1} + \frac{W_S}{M_S}} \quad (7)$$

b. Specific heat, C_{P_1}

$$C_{P_1} = \frac{W_1 C_{P_1} + W_S C_{P_S}}{W_1 + W_S} \quad (8)$$

c. Temperature, T_1

$$T_1 = \frac{W_1 C_{P_1} T_1 + W_S C_{P_S} T_S}{C_{P_1} (W_1 + W_S)} \quad (9)$$

where:

W_1 = mass of fluid in V_1 at any time, t .

b. Comparison with Experimental Data

To verify the soundness of the simple model presented in the previous section, the analytical results were compared with NEPN-1 test data. The analytical results conform to the experimental observations. Specifically, the analysis predicted air intrusion for all runs where air backflow was detected by the oxygen analyzer. For example, Figure 19 shows that no air will enter the cell during shutdown of the first generator, whereas air intrusion will occur during shutdown of the second generator. Similarly, Figures 20 through 23 show that air backflow will occur during Runs 2, 3, and 4, whereas none will occur during engine shutdown in Run 5. Analytical results also show that, during steam-generator shutdown in Run 4 and during primary-fluid-flow shutdown in Run 5, no air will enter the cell because the mass required for cell repressurization is much smaller than the combined flow rates of the purge gas and the primary fluid. The results of the oxygen analyzer agree well with these calculations.

c. Conceptual Design of a Steam Accumulator

Examination of Equation 4 shows that the controlling factor in air backflow lies in the repressurization rate, $\frac{dP_1}{dt}$. Therefore, if $\frac{dP_1}{dt}$ can be reduced enough so that the first term in Equation 4 is always lower than the combined flow rates of steam and purge gas, $W_P + W_S$, air backflow will be prevented. This condition can be achieved by reducing the steam decay rate by means of a larger steam reservoir (accumulator). However, to size the accumulator, the interrelationship between the steam decay rate, $\frac{dP_S}{dt}$, and the cell repressurization rate, $\frac{dP_1}{dt}$, must be known. The experimental results of Run 2 of NEPN-1 were used to establish the following relationship (see Figure 24):

$$\frac{dP_1}{dt} = -0.116 \frac{dP_S}{dt} . \quad (10)$$

Figure 19 - Pressure and Flow Rate vs Time, NEPN-1 Run 1

Figure 20 - Pressure and Flow Rate vs Time, NEPN-1 Run 2

Figure 21 - Pressure and Flow Rate vs Time, NEPN-1 Run 3

Figure 22 - Pressure and Flow Rate vs Time, NEPN-1 Run 4;
Malfunction PCV-449

Figure 23 - Pressure and Flow Rate vs Time, NEPN-1 Run 5;
Steam-Generator Shutdown

Figure 24 - Cell Pressure vs Secondary-Fluid Pressure

The results of tests of the 1/8-scale model with purge-gas flow equivalent to 30 lbm/sec are also shown in Figure 24 (slope = -0.055). To be conservative in sizing an accumulator, a slope of -0.116 was used. The rates of steam depressurization and cell repressurization were further assumed to occur under quasi-steady-state conditions. Thus, for an accumulator of a given size, the repressurization rate of the cell can be calculated. The depressurization rate of the steam accumulator was calculated with isentropic expansion assumed through a sonic nozzle and with the following initial conditions and properties used for the steam:

Pressure, P_S	110 psia
Temperature, T_{SC}	1375°R
Molecular weight, M_S	20.8 lb/lb-mol
Specific heat, C_{p_S}	0.469 Btu/lbm°R
Specific-heat ratio,	1.255

From $\frac{dP_S}{dt}$, the repressurization rate, $\frac{dP_1}{dt}$, of the cell and thus the mass change in the cell were calculated. The amount of steam required for repressurization was calculated by using mixed-mean properties. The calculated results for two different sizes of accumulator (volumes of 1500 and 2000 cu ft) are summarized in Figures 25 and 26. Thus, an on-line accumulator of 2000 cu ft may be used to prevent air intrusion.

The analytical results shown as curve 2 in Figures 25 and 26 are an underestimate of the steam requirements because the analysis not only assumes the duct fluids are discharged into quiescent air, but also that the NES-duct surfaces are adiabatic. Therefore, the steam requirements necessary to compensate for condensation of steam on the surfaces of the duct and the cell were estimated. On the basis of available data on the condensation of steam in the presence of large concentrations of noncondensable gases, it was estimated that about 5 lbm/sec of steam will condense on the duct and

- 1 - - - - THEORETICAL STEAM FLOW RATE
- 2 - . - . REQUIRED STEAM FLOW RATE
- 3 - - - - REQUIRED STEAM FLOW RATE INCLUDING
THE EFFECTS OF WIND AND CONDENSATION
- 4 - . . - CELL PRESSURE

Figure 25 - Pressure and Flow Rate vs Time for a 1500-cu-ft Accumulator

- 1 - - - - THEORETICAL STEAM FLOW RATE
- 2 - - - - REQUIRED STEAM FLOW RATE
- 3 ——— REQUIRED STEAM FLOW RATE INCLUDING
THE EFFECTS OF WIND AND CONDENSATION
- 4 — · — CELL PRESSURE

Figure 26 - Pressure and Flow Rate vs Time for a 2000-cu-ft Accumulator

the cell walls. Similarly, estimates were made of the steam requirements necessary to overcome the adverse effects of wind on the duct openings (due to the chimney effect through the duct drain pipe and the dynamic head of the wind--see Figure 17). For a 35-mph head-on wind, the most realistic steam flow rate was found to be 36.2 lbm/sec. However, if a suitable steam trap were to be installed in the drain pipe, it is estimated that the steam requirements would be reduced to about 22 lbm/sec. Therefore, a total of 27 lbm/sec (5 + 22) of steam was added to the cell repressurization rate to obtain the required steam flow rate. The combined effects of wind and the nonadiabatic walls are shown in curve 3 of Figures 25 and 26. Comparison of the analytical results presented in Figures 25 and 26 shows that a 2000-cu-ft accumulator appears to provide an adequate margin of safety in the event a malfunction occurs in the steam-generator system.

d. Flow of Helium as Purge Gas

Calculations based on mass balances made on the assumption that the cell is to be repressurized from 7.5 psia to 12.8 psia at ambient temperature show that about six moles of gas are needed. Therefore, on a weight basis, the gas with the lowest molecular weight would be the most desirable. Calculations were made with helium used as the purge gas. As there are no data on the effect of helium purge gas on the cell repressurization rate, the experimental data of NEPN-1, with 30 lbm/sec of nitrogen as the purge gas, were assumed to be valid ($\frac{dP_1}{dt} = -0.136 \frac{dP_S}{dt}$) for these calculations.

Calculations assuming repressurization with helium purge gas alone (no steam available from blowdown of present steam line) show that helium flow rates as high as 53 lbm/sec may be required. Calculations of the helium flow rates required in conjunction with the existing steam lines to prevent air intrusion were made. Figures 27 and 28 summarize the analytical

Figure 27 - Pressure and Flow Rate vs Time (Basis: NEPN-1 Run No. 2, He Flow = 24 lbm/sec)

Figure 28 - Pressure and Flow Rate vs Time (Basis: NEPN-1 Run No. 2, He Flow = 30 lbm/sec)

results and show that 24 lbm/sec of helium is marginal in preventing air intrusion, whereas 30 lbm/sec provides an adequate margin of safety.

If a malfunction occurs in the steam-generator system, air intrusion in the ETS-1 duct can be prevented by using a helium-purge-gas flow. To conserve helium, however, it is desirable to initiate the purge gas flow only if a malfunction is detected in the secondary fluid system. An analysis was therefore made to determine the requirement for design of a control system that could detect a malfunction and provide the demanded helium flow.

From the measured temperatures and pressures of the steam during steam-generator shutdown (NEPN-1 test series), the characteristics of the steam line (e.g., flow rates and pressure-decay rates) during a simulated malfunction of the steam-generator system were established. Following the procedure outlined in Section III.A.3.a, the rate of change of mass in the ETC was established. Any excess flow of steam above 27 lbm/sec (the rate required to overcome the adverse effects of wind and condensation) was assumed to be available for repressurization of the cell. When the available steam was less than the required mass of fluid, as dictated by the observed change in cell pressure, it was assumed that helium would be used to make up for this deficiency. Thus, the demanded helium flow rate (i.e., the least amount of helium required to prevent air intrusion) was calculated and is summarized in Figure 29. Figure 29 shows that, if a control system can be designed that detects a malfunction (e.g., $-\frac{dp_s}{dt} > 50$ psi/sec) within 0.1 sec and operates a valve to establish the demanded helium flow within 0.3 sec of a malfunction, air intrusion can be prevented.

e. Summary of Air Backflow Analysis

Major conclusions from the analysis of air backflow problems are:

Figure 29 - Pressure, Flow Rate, Pressure-Decay Rate vs Time
(Basis: NEPN-1 Run No. 3)

(1) The mass-balance analytical model to calculate air intrusion into the NES duct agrees well with the experimental data. The model predicts air backflow into the duct for all cases for which the presence of oxygen (air) was detected by the oxygen analyzer.

(2) Calculations that take into account a 35-mph head-on wind on the duct opening and condensation of steam on the duct and cell walls show that an on-line accumulator of 2000 cu ft will prevent air backflow. Similar calculations for an accumulator of 1500 cu ft show that this accumulator will be marginal in preventing air intrusion.

(3) An alternative approach with helium used as the purge gas to prevent air backflow shows that a 30-lbm/sec helium flow will be adequate.

(4) By using the experimental data of NEPN-1, Run 3, the pressure-decay rates and the demanded helium flow rates to prevent air backflow were calculated. The analytical results show that air intrusion can be prevented if the demanded helium flow rate is established within 0.3 sec after a malfunction occurs in the steam-generator system.

B. HIGH-PRESSURE LIQUID-HYDROGEN COOLDOWN SYSTEM

Flow tests of the high-pressure liquid-hydrogen cooldown line for ETS-1 were conducted during Facility Experimental Plan III (FEP-III). The results of these tests were analyzed to upgrade the digital and analog models that are used to establish a basis for the emergency-flow-shutdown system of XE-1. To upgrade these models, the flow-resistance data of the line were obtained, and the dynamic response data generated by perturbing PCV-472 were analyzed.

1. Flow-Resistance Data

a. Method of Analysis

A sketch of the liquid-hydrogen cooldown line and its instrumentation is shown in Figure 30. During the FEP tests, an engine-simulation system was used in place of an actual engine. The system included a venturi flowmeter (FE-X) and a flow-control valve (PCV-750) to simulate the engine impedance.

For the data analysis, the cooldown line was broken into segments in accordance with the instrumentation locations. The resulting segments were:

- (1) PT 097 to PT 101
- (2) PT 101 to PT 484
- (3) PT 484 to PT 103
- (4) PT 103 to PT 770

Because the pressure losses in the line sections are small compared with the losses across the valves, PT-101, PT-484, and PT-103 pressure measurements were assumed to be located at control valve PCV-472 and at the check-valve 458 inlet and exit, respectively. The cooldown line can then be assumed to be broken into four segments consisting of two basic line sections and two valves, i.e.:

Figure 30 - Schematic of LH₂ Engine-Emergency-Cooldown System

- (1) Line section from V-5002 tank to PC-471 valve inlet (PT-097 to PT-101),
- (2) PCV-472 valve (PT-101 to PT-484),
- (3) Check-valve 458 (PT-484 to PT-103),
- (4) Line section from check-valve 458 to FE-X inlet (PT-103 to PT-770).

This assumption results in the computed valve resistances being slightly high and those for the line sections being slightly low; total system resistance would be correct, however.

b. Flow Measurements

Flow measurements were obtained from two devices. A flow tube, FE-12, was located upstream of control-valve PCV-472; and a venturi, FE-X, was located in the engine-simulation section.

Of the two differential-pressure measurements, PT-780 and PT-601, that were available with FE-12, only the PT-601 measurements were used to calculate flow rate; the PT-780 measurement appeared to be improperly scaled and hence inaccurate.

For the FE-X venturi, the differential pressure PT-771 was a wide-range instrument; so only the narrow-range differential pressure PT-772 was used, except when the latter was off scale in Run 6. The pre- and posttest calibrations disclosed small calibration shifts in both of these instruments. They also appeared to be sensitive to the line pressure by indicating nonzero readings under no-flow conditions.

The data just before and after each run were therefore checked to obtain zero-flow ΔP corrections for the differential pressures used to calculate the flow rates.

Figure 31 provides a comparison of the FE-12 and FE-X flow rates and shows that the agreement is fairly good, the FE-12 value being within 10% of the FE-X value at all points. The flow from FE-12 does tend to be consistently higher than that for FE-X. During the previous gas-flow tests (FEP-I and -II), FE-X has provided the more accurate flow rates. For this reason, it was decided to use the flow rates computed from the FE-X venturi for this analysis.

c. Discussion of Results

(1) Line Segment from V-5002 Tank to PCV-472

At the flow rates at which most of the tests were run, the pressure losses that were obtained by subtracting the PT-101 pressure measurements from those for PT-097 were small (about 2 psi for a flow of 25 lb/sec). As both of these instruments have a range of 0 to 2000 psia, the pressure losses represent only about 0.1% of the full-scale value. A reasonable accuracy for these instruments is about 0.5% of the full-scale reading; therefore, most of the data for computing the resistances of this line segment were of poor quality.

Run 6 was a high-flow blowdown test with pressure losses of up to 18 psi. Primary consideration was therefore given to the data from this run, whereas secondary consideration was given to the data from Run 5B, a low-flow test.

The variation of pressure loss with flow rate for this line segment is shown in Figure 32. Both PT-097 and PT-101 were total-pressure measurements. Comparison of the measurements under zero-flow conditions disclosed that they differed by approximately 2 psi (PT-101 was higher); so the pressure losses were adjusted by this amount. Because of the low density of hydrogen, the effect of a difference in elevation was negligibly small.

Figure 31 - Comparison of Flow Rates, FEP-III LH₂ Flow Test

Figure 32 - Flow Characteristics, V5002 Tank to PCV-472;
FEP-III LH₂ Flow Test

Resistance data, which are computed from the test data, are also shown in Figure 32. The resistance parameter, K , is defined by the relationship:

$$K = \frac{\Delta P \rho}{\dot{w}^2} ,$$

where ΔP is the pressure loss in psi, ρ is the fluid density in lb/cu ft, and $\dot{w}$ is the mass flow rate in lb/sec. This parameter is related to the Darcy resistance parameter, K_D (dimensionless unit), by the relationship:

$$K_D = K (0.525 d^2)^2 ,$$

where d is an effective diameter in inches.

Some scatter is seen in both the ΔP and resistance data, particularly at the low flows. The pattern of scatter in the ΔP data indicates that the pressure-data roundoff of the small pressure difference is probably contributing to the data scatter. General agreement exists between the high-flow and low-flow resistance data; therefore, an average of the high-flow data, $0.025 \text{ sec}^2/\text{in.}^2\text{-ft}^3$, was taken as the best estimate of the resistance of the line segment from the tank to the inlet of PCV-471.

(2) PCV-472 Flow-Control Valve

Data for evaluating the flow characteristics of the control valve were obtained from the valve calibration test at the beginning of the facility checkout tests, the flow and pressurization tests, and Run 6 of the FEP-III LH₂ blowdown tests. The valve was full open during the blowdown runs, so only the high-flow test, Run 6, was used to establish the full-open valve characteristics. The other tests provided data to obtain the valve characteristics at intermediate positions.

The difference between PT-101 and PT-484 was assumed to be the pressure loss across PCV-472 because the line section between these two measurements had a negligible pressure loss. It was reported in Reference 7 that PT-101 was tapped to the total-pressure leg of FE-12, whereas PT-484 measured static pressure. The difference between total and static pressures in the line was fairly small (2.25 psi with a flow of 56 lb/sec and less than 0.5 psi with a flow of 25 lb/sec) and was insignificant except when the valve was opened to allow a high flow rate that resulted in relatively low ΔP . The data were adjusted for this difference in total and static pressures whenever high flow rates were involved.

The variation of valve flow coefficient, K_w (where $K_w = \dot{w} / \sqrt{\rho \Delta P}$), with valve position computed from the test data is shown in Figure 33. The K_w value with the valve full open is 3.72.

Prior to FEP-III, the liquid-hydrogen line was checked out by flow testing with liquid nitrogen. The results of this test were reported in Reference 6, which includes the valve flow coefficients that were computed from the test data. The difference between the full-open valve coefficients based on LH_2 and LN_2 flow tests is about 5.5%, but the reason for this difference is still unknown. Flow coefficients based on the LH_2 test data are recommended for use with LH_2 flow through the valve.

(3) Check-Valve 458

Check-valve 458 exhibited abnormal characteristics during FEP-III. The variation of pressure drop across the valve (assumed to be the difference between pressure measurements PT-484 and PT-103) with flow rate for the six blowdown tests is shown in Figure 34. The data tend to fall into two patterns; Runs 4A, 4B, 5A, and 5B indicate high pressure losses, whereas Runs 6 and 4B' indicate lower pressure losses.

$$KW = \frac{W}{\sqrt{\Delta P \rho}}$$

$$W = W_{FE-X}$$

$$\Delta P = PT-101 - PT-484$$

PT-101 WAS CORRECTED TO STATIC PRESSURE

Figure 33 - PCV-472 Flow Characteristics, FEP-III LH₂ Flow Test

Figure 34 - Pressure Drop vs Flow Rate, Check-Valve 458;
FEP-III LH₂ Flow Test

The tests are listed above in the order in which they were run. During the first four tests, the valve displayed high resistance characteristics, apparently from insufficient valve opening. In Run 6, the flow resistances in the valve appeared to decrease. The next low-flow run (4B') displays the same low flow-resistance characteristics as the high-flow run (Run 6).

It is presently assumed that the valve was displaying normal characteristics during Runs 6 and 4B'. The data from these two runs will be used to define the valve characteristics until additional test data provide better results.

The flow-coefficient data for the valve, computed from the test results of Run 6 and Run 4B', are shown in Figure 35. The K_w value for the full-open valve (where K_w is defined as $K_w = \dot{w} / \sqrt{\rho \Delta P}$) is 2.95. Below a flow rate of about 42 lb/sec, K_w begins to decrease as the valve begins to close and the flow rate decreases.

(4) Check-Valve 458 to FE-X Inlet

It was assumed that the pressure loss in the line between check-valve 458 and PT-103 was negligible and that the difference between the measurements at PT-103 and PT-770 therefore represents the pressure loss in the line segment from the exit of check-valve-458 to the inlet of the FE-X venturi meter. As in the case of the first segment of line from the tank to PT-101, the quality of the data becomes poor at the low flow rates. Therefore, only Runs 5A, 5B, and 6 were considered in the data analysis, and Run 6 was used as the primary source of data.

Figure 35 - Flow Characteristics, Check-Valve 458, FEP-III LH₂ Flow Test

The variation of pressure loss with flow rate is shown in Figure 36. Resistance data (where $K = \frac{\Delta P}{\dot{w}^2}$) computed from the test data are also shown in this figure. The resistance, based on an average of Run 6 data, is $0.029 \text{ sec}^2/\text{in.}^2\text{-ft}^3$.

(5) FE-X Inlet to PCV-750

The analog-computer model terminates at PCV-750 instead of at the inlet to FE-X. The resistance of the section from the FE-X inlet (PT-770) to PCV-750 (PT-774) was therefore computed from the data of Run 6. The average of the resistance values computed was $0.0104 \text{ sec}^2/\text{in.}^2\text{-ft}^3$.

d. Summary of Resistance Data

Analysis of the resistances in the LH_2 cooldown line showed the following results.

(1) $K = 0.025 \text{ sec}^2/\text{in.}^2\text{-ft}^3$ for the resistance from tank V-5002 to PCV-472, where $K = \Delta P / \dot{w}^2$.

(2) $K_w = 3.72$ for the PCV-472 full-open flow coefficient, where $K_w = \dot{w} / \sqrt{\Delta P \rho}$.

(3) $K_w = 2.95$ for the check-valve 458 full-open flow coefficient.

(4) $K = 0.029 \text{ sec}^2/\text{in.}^2\text{-ft}^3$ for the resistance from the check valve to FE-X.

2. Digital-Computer-Model Upgrading

The models for the ETS-1 facility have been developed in conjunctions with the X-engine model so that they can be used in cooldown and emergency studies. Effort in upgrading the digital model has been concentrated

Figure 36 - Flow Characteristics, Check-Valve 458 to FE-X Inlet;
FEP-III LH₂ Flow Test

on preparing the physical description for the system and checking the program output by using the steady-state data analyzed as described in the preceding section.

a. Description of Model

The digital program can simulate each facility system with a model that is made up of a maximum of seven parts, each of which can have 20 nodes. Each node is defined in terms of a physical description of the line or valves that exist at that point. The pressure drops and heat transfer are computed in each node with the equations summarized in Table 1, page 68.

b. Flow-Resistance Input Data

Flow-resistance data in the computer model were previously obtained from the handbook and the vendor-quoted C_v . These predicted flow-resistance data were compared with the actual flow-resistance data from the tests and were upgraded on the basis of this comparison.

The predicted resistances do not agree well with the actual resistances (test data), as shown in the following table:

<u>Section</u>	<u>Location</u>	<u>K/A^2</u>		<u>Difference, %</u>
		<u>Predicted</u>	<u>Actual</u>	
1	PT-97 to PT-101	0.0099	0.0112	-12
2	PT-101 to PT-484	0.0237	0.0326	-27
3	PT-484 to PT-103	0.0066	0.0514*	-91
4	PT-103 to PT-770	<u>0.0140</u>	<u>0.0130</u>	+8
		0.0542	0.1082	

*Bad Data

TABLE 1

BASIC PRESSURE-DROP AND HEAT-TRANSFER EQUATIONSEquation 1: PRESSURE-DROP CALCULATION

$$\Delta P = \frac{\dot{w}^2}{A_F^2 \rho g} \left[\frac{T_{(m+1, n)} - T_{(m, n)}}{T_{(m, n)}} + \frac{4 \phi \Delta Z}{\beta} \right] = P_{(m, n)} - P_{(m+1, n)}.$$

Equation 2: FLUID TEMPERATURE CHANGE

$$\Delta T = T_{(m+1, n)} - T_{(m, n)} = \frac{1}{M} \left[\theta_{(m + \frac{1}{2}, n)} - T_{(m + \frac{1}{2}, n)} \right];$$

$$T_{(m+1, n)} = \frac{2 \theta_{(m + \frac{1}{2}, n)} + (2M - 1) T_{(m, n)}}{2M + 1};$$

$$M = \frac{\dot{w} C_P}{h_f \Delta Z F_p}.$$

Equation 3: METAL TEMPERATURE

$$\theta_{(m + \frac{1}{2}, n)} = \theta_{(m + \frac{1}{2}, n-1)} \left[1 - \frac{2}{N(2M+1)} \right] + T_{(m, n-1)}$$

$$\left[\frac{1}{N} - \frac{2M-1}{N(2M+1)} \right] + \frac{S}{2} \left[\theta_{(m + \frac{3}{2}, n-1)} - 2 \theta_{(m + \frac{1}{2}, n-1)} + \theta_{(m - \frac{1}{2}, n-1)} \right] + U;$$

$$N = \frac{Q_i C_r \rho_r}{H \Delta T \dot{w} C_p}, \quad S = \frac{2 K \Delta T}{(\Delta Z)^2 \rho_r C_r}, \quad U = \frac{H \Delta T}{\rho_r C_r};$$

$$Q_i = \frac{H \Delta Z (1-f) A_F}{f}.$$

In Section 3, check-valve CHV-458 malfunctioned during the test. This valve had a 70-psi pressure drop across it instead of the normal 7 psi. The predicted resistance is therefore close to the expected resistance for normal operation. Pressure control valve PCV-472 is in Section 2; the 27% difference between the predicted and actual resistances were due to this valve.

Upgraded flow-resistance input data are shown in Table 2. Parts 1, 2, and 3 consist of 3, 3, and 1 nodes respectively in the computer model. These nodes were set up to describe the physical system in terms of the locations of such items as valves and elbows.

3. Upgrading of Analog Model by Using
Dynamic-Response Data

a. Dynamic-Response Data

Dynamic testing of the high-pressure LH_2 line and simulator was conducted during FEP-III-IA on 17 January 1967, as summarized in Reference 7.

Dewar V-5002 was maintained at approximately 500 psia while step and sine-wave demands were made on PCV-472 position (open-loop control) and later on flow rate (closed-loop control). Only the open-loop data were used because closing the loop provides no additional information on the physical system being controlled.

The pressure differential PT-601 across the upstream flowmeter FE-12 (upstream of PCV-472) was filtered in an attempt to remove a 24-cps oscillation noted prior to FEP-III. The FE-12 flow rate was controlled at the desired level during closed-loop operation. The same filter was used to produce the analog equivalent of FE-12, because this channel was one of only four output or response quantities that were zero-suppressed during the

TABLE 2
LH₂-COOLDOWN-LINE RESISTANCE DATA

Flow Elements	Resistance of One Element	Entry, Part I Tank 5002 to PCV-472, 5.761 in. dia		Part I to GH ₂ Line, 5.761 in. dia		Part II to LF-7, 5.761 in. dia		Part III, see Common (Table 6)	
		No. of Elements	K	No.	K	No.	K	No.	K
15° elbow	K=0.06								
30°	0.07								
45°	0.10					2	0.20		
60°	0.13								
90°	0.18	6	1.08	2	0.36	1	0.10		
135°	0.28								
Tee, flow through run 1/2 in.	K=0.05								
1 in.	0.07	3	0.21	2	0.14				
1-1/2 in.	0.10								
2 in.	0.10			1	0.10				
4 in.	0.15			1	0.15				
6 in.	0.20			1	0.20	1	0.20		
Tee, flow through branch 6 in.	0.85	1	0.85	2	1.70	1	0.85		
Tee, instrumentation 1 in.	0.07	2	0.14	3	0.21	1	0.07		
Tank exit	0.78	1	0.78						
Straight line (1-in. length)	f=0.009	982	1.5341	248	0.3874	144	0.2250		
RSV-107	C _V =580	1	2.927						
FE-12	K=0.224	1	0.224						
PCV-472	C _V =260	1	14.5672						
CHV-458	C _V =580			1	2.927				
CHV-2302	C _V =580					1	2.927		
Total K =			22.3123		6.1744		4.652		
K/A ² =			0.03284 in. ⁻⁴		0.0091 in. ⁻⁴		0.0068 in. ⁻⁴		

test. Only these channels, in which the dc component is canceled by an equal and opposite signal, furnish response data of sufficient resolution.

Several anomalies were noted that make exact analytic duplication of test conditions difficult:

(1) The dynamic-response test was made at a low flow rate (less than 10 lb/sec); so the accuracy was doubtful. The FE-12 (upstream of PCV-472) and FE-X (downstream) flow rates differed by 1 to 2 lb/sec. Determination of flow rates from observed valve positions is not possible because the low-flow-rate (less than 10 lb/sec) calibrations of these valves are not sufficiently well defined. Because pressures in the Dewar and between the two flow-control valves are known, the best approach that can be followed is an arbitrary combination of valve impedances to match the known pressure distribution at an arbitrary flow rate.

(2) During the dynamic-response test, data from either flowmeter indicate that C_v for PCV-750 increases with flow rate at constant position. In conjunction with low flow rate and relatively high temperatures, an uncertainty as to fluid state is indicated.

(3) At different times, CHV-458 apparently assumed several different partially open positions. A resistance yielding a pressure drop of 12 psi at 8.5 lb/sec was apparently valid throughout the areas of interest in the dynamic testing.

(4) The PCV-472 actuator-response data are apparently invalid because they show increases in measured amplitudes over demanded amplitudes at high frequencies. The measured position data are assumed to be correct. Test and predicted frequency responses are computed with respect to this quantity.

Dry-run response data measured on 12 April 1967 were well correlated by the following transfer function:

$$\frac{\theta_{472}}{\theta_{472d}} = \frac{1}{7.526 \times 10^{-7} s^3 + 1.385 \times 10^{-4} s^2 + 1.916 \times 10^{-2} s + 1}$$

The feedback gain was 5.5 and the opening and closing velocity limits were 550 and 600 %/sec, respectively. The time lag between command and beginning of motion was 6 millisecc from any partially open position and was 11 millisecc from a closed position.

b. Derivation of Analog Model

The model described in Appendix B and discussed below is a lumped parameter description of the FEP-III LH₂ cooldown line with the following qualifications:

- (1) Gaseous-hydrogen pressure in the V-5002 ullage space is assumed to be constant.
- (2) Relatively few lumps are used to minimize equipment consistent with accuracy.
- (3) Pressure change during a given response is small, so that a constant compressibility factor may be used.
- (4) Relatively simple determinations of temperature in a given lump are made as shown in Appendix B.

Line volumes and liquid inertia were computed from data in Figures 37 and 38, and resistances (other than CHV-458) were obtained from the ETS-1 facility test data.

Figure 37 - Isometric of XE-Engine LH₂ Cooldown Line

Figure 38 - Isometric of XE Cooldown Simulator

Liquid-hydrogen properties as functions of pressure and temperature are shown in Figure 39. The compressibility data were computed as the square of sonic velocities given in Reference 14 and are for an isentropic compression. Density is from Reference 15.

c. Frequency-Response Comparison

Figures 40 through 43 show test and predicted frequency responses of PT-484, PT-774, PT-772, and FT-12 with respect to measured PCV-472 position in the frequency range of 0.25 to 16 cps.

The predicted response is based on PCV-472 and PCV-750 openings that yield 350 psia between the valves at a flow rate of 8.4 lb/sec. As previously discussed, this pressure level is well defined by the test data, but the flow rate is not accurately known and is arbitrary.

Test and predicted frequency responses for PT-484, PT-774, and PT-772 are in relatively good agreement at frequencies below 7 cps. At higher frequencies, the predicted responses, which are much more detailed, show a pronounced resonance at 8.5 cps and a sharp attenuation at the upstream (of PCV-472) resonant frequency of 7.2 cps.

The predicted FT-12 frequency response shows the complementary attenuation-resonance behavior but poor agreement of amplitude with data. This condition may be due to the previously discussed qualifications about test conditions, or to improper mechanization of the FT-12 filter, or to a defect in the test data. The overall behavior is apparently that of two weakly coupled (by PCV-472) systems.

Figure 39 - Properties of Para-Hydrogen

Figure 40 - Frequency Response, PT-484/ θ_{472}

Figure 41 - Frequency Response, PT-774/ θ_{472}

Figure 42 - Frequency Response, PT-772/ θ_{472}

Figure 43 - Frequency Response, FT-12/ θ_{472}

d. Step-Response Comparison

The generalized response behavior for both test and predicted results is that the flow rates initially overshoot their final values and then drop back exponentially while the pressures move exponentially toward their final values from the prestep level. The flow rates have a damped oscillation at the previously discussed frequencies superimposed on the above quasi-static trend, whereas the pressures show a very minor oscillation.

In addition, the test data show a pronounced gradient because of rapid cooling of the line during this period.

In view of these characteristics, the exponential-response time constant can only be measured for the two pressures. The test-data values are 0.50 sec for both PT-484 and PT-774.

The predicted values were found to depend strongly on the mean flow rate and the temperature distribution along the line. As neither flow rate nor fluid conditions are well defined, as previously discussed, the step-response time constants were computed for several values of these parameters.

<u>Temperature Distribution</u>	<u>Mean Flow Rate, lb/sec</u>	<u>Time Constant, sec</u>			
		<u>PT-484</u>		<u>PT-774</u>	
		<u>Flow Increase</u>	<u>Flow Decrease</u>	<u>Flow Increase</u>	<u>Flow Decrease</u>
See Appendix B	8.4	0.30	0.36	0.32	0.38
See Appendix B, except last lump +5°	8.4	0.38	0.42	0.40	0.43
See Appendix B	5.84	0.40	0.50	0.44	0.50

Relatively good agreement is seen to exist if the higher temperature or lower flow-rate condition, or both, prevail.

C. FACILITY-TEST-DATA ANALYSIS

ETS-1 facility tests (FEP-I through FEP-IV) were performed from September 1966 to March 1967. Results of these tests are reported in detail in the NTO Test Summary Report (Reference 1) and the FDET memoranda (References 2 through 9). Test data were therefore analyzed primarily to estimate the flow resistances in the facility purge and cooldown lines. These resistances were then used to upgrade the computer models for engine cooldown predictions.

1. Liquid-Nitrogen Cooldown Line

a. Summary Flow Resistances

Flow resistances for the ETS-1 liquid-nitrogen cooldown line were calculated from FEP-IB and FEP-IC data. For this evaluation, the line was divided into three sections because of the location of the instrumentation (see Figure 44):

(1) PT-181 (LN₂ tank) to PT-194 (inlet of FE-52)

(2) PT-194 to PT-580 (inlet of RSV-545)

(3) PT-580 to PT-770 (inlet of FE-X)

The values of $K_D/A^2 = (2g/144) (\Delta P/\dot{w}^2)$ obtained from the raw data are:

$$(1) K_{12}/A^2 = 0.054$$

(2) K_{23} (see Figure 45 - variable, depending on valve position)

$$(3) K_{34}/A^2 = 0.050$$

Figure 44 - Simplified Schematic, Liquid-Nitrogen Storage and Cooldown Line

Figure 45 - K_{23} vs PCV-517 Stem Position

After the difference in elevation between the ends of a section has been taken into account, K_D/A^2 becomes:

$$(1) \quad K_{12}/A^2 = 0.066$$

(2) K_{23} (see Figure 46 - variable, depending on valve position)

$$(3) \quad K_{34}/A^2 = 0.066$$

The value of K/A^2 for Section 3 is based on data from FEP-IB and -IC. CHV-1885, which is in this section, was modified after these tests; but the modification was unsatisfactory, and the valve will be changed again. The Section 3 loss coefficient may have to be changed, depending on the final setting for CHV-1885.

b. Analysis of Test Data

The flow resistances were computed from the facility-checkout test data at steady LN_2 flow rates. Only FEP-IB and FEP-IC data were used throughout this evaluation. The data from FEP-IA were not used because PCV-517 froze partway open. After FEP-IC, check valve CHV-1885 malfunctioned, making the data invalid for Section 3.

Figure 44 is a simplified schematic of the LN_2 cooldown line and the engine-simulation section. Gross changes in elevation within a section are labeled. After check valve CHV-1885, the scheduled-20, 4-in. line changes to a schedule-80, 6-in. line. This change is important to know when providing input values to the engine-simulation programs. The engine-simulation section consists mainly of PCV-750, which duplicates the engine impedance, and FE-X, a venturi flowmeter.

Figure 46 - $K_{PCV-517}$ vs PCV-517 Stem Position

Figure 47 compares the flow rates calculated on-line from FE-52, a Potter meter, with those calculated for the venturi, FE-X. The two values agree to within ± 3 lb/sec throughout the range from 0 to 85 lb/sec, with no apparent bias. Near saturated-vapor-pressure conditions at the FE-X inlet, the two measurements are still in fair agreement. Since FE-X has been found to be accurate in past tests, the agreement between FE-X and FE-52 indicates that FE-52 can be used with confidence to determine LN_2 flow rates. The values from FE-52 were therefore used in all the following computations involving flow.

Figures 48 and 49 show the temperature and pressure changes with steady flow conditions in the line. Since only the data points at which PCV-750 correctly simulated the engine impedance were used (see FDET Memo No. 6 of Reference 2), the temperature, pressure, and flow relations in Figures 48 and 49 represent the best estimates of the steady LN_2 flows to the engine system. Figure 48 gives the smoothed temperature data that can be used to check the simulation-program output. A few of the temperature points that were abnormally low (more than 2°R) or actually lower than upstream values were deleted from Figure 48 as bad data.

Differential pressures for each section are shown in Figure 50. The differential pressure, ΔP_{23} , between Sections 2 and 3, drops with increased flow because the pressure control valve, PCV-517, is in this section; when this valve allows more flow by opening farther, it has less resistance. Some of the differential pressures in Sections 1 and 3 have negative values at low flows. These negative values are mainly due to elevation differences (liquid heads) that have not been accounted for in the data. Elevation corrections do not account for all of the negative values in the low flow region, but the remaining negative values can be explained by an additional elevation correction of ± 1.7 psid to ΔP_{12} for the LN_2 tank ullage and by the actual scatter of ± 3 psid, which is highly significant at low flow rates.

Figure 47 - Comparison of FT-52 with FE-X

Figure 48 - LN₂-System Line Temperatures vs Flow

Figure 49 - LN₂-System Line Pressures vs Flow, Not Corrected for Elevation Head

Figure 50 - Differential Pressures

Flow resistances (K/A^2) were calculated from the equation:

$$K/A^2 = (2g/144) (\rho \Delta P / \dot{w}^2) = 0.4472 \rho \Delta P / \dot{w}^2,$$

where

ΔP = measured pressure drop across section,

ρ = LN_2 density at inlet of section,

$\dot{w}$ = FE-52 = flow rate through section,

A = cross-sectional area of inlet = 12.58 in.²

The flow resistances were initially calculated from the raw data without the elevation corrections. Because the scatter in the ΔP values was as large in magnitude as the values themselves at low flow rates, the computed data for K/A^2 are unreliable below a 30-lb/sec flow. Figure 51 gives the flow resistances in Sections 1 and 3 computed with no elevation correction in ΔP : 0.054 in.⁻⁴ for K_{12}/A^2 and 0.050 in.⁻⁴ for K_{34}/A^2 . K_{23} vs PCV-517 stem position is given in Figure 45. An inlet diameter of 4 in. was used for the flow area.

Pressure drops due to impedance only are:

$$\Delta P_{12} \text{ due to resistance} = \Delta P_{12} + 2.1 \text{ psid},$$

$$\Delta P_{23} \text{ due to resistance} = \Delta P_{23} - 18.9 \text{ psid},$$

$$\Delta P_{34} \text{ due to resistance} = \Delta P_{34} + 1.8 \text{ psid},$$

where the extra term is a correction for the elevation difference between the ends of the section.

Figure 51 - K/A^2 vs LN₂ Flow, Without Elevation Correction

The flow resistances after correcting for elevation are given in Figures 46 and 52. Figure 52 gives a value of 0.066 in.^{-4} for both Sections 1 and 3. Figure 46 gives $K_{\text{PCV-517}}$ obtained by using a ΔP corrected for the elevation rise and the presence of FE-52 in Section 2:

$$\Delta P = \Delta P_{\text{MEASURED}} - 18.9 - 1.407 \times 10^{-3} \dot{w}^2$$

The correction term ($1.407 \times 10^{-3} \dot{w}^2$) for the presence of FE-52 was obtained by assuming that the ΔP of FE-52 was proportional to $\dot{w}^2$, and from the NTO estimate of $\Delta P_{\text{FE-52}} = 9 \text{ psid}$ at $\dot{w} = 80 \text{ lb/sec}$. Also shown in Figure 46 are the FEP-III-2 data for LN_2 flow under the plug of PCV-517 (Reference 9).

The flow resistances that are not corrected for elevation should be used in the simulation programs. These values will give the actual pressure drops. The corrected resistances should be used to compare the actual resistances with the theoretical values.

c. Flow-Resistance Input Data

Flow-resistance data in the computer model were previously obtained from the handbook and the vendor-quoted C_v . These predicted flow-resistance data were compared with, and upgraded on the basis of actual flow-resistance data from the tests.

Figure 52 - K/A^2 vs LN₂ Flow, with Elevation Correction

The predicted and actual flow resistances are compared in the following table:

		K/A^2 , in. ⁻⁴		% Diff.
		<u>Predicted</u>	<u>Measured</u>	
(1)	PT-181 to PT-194	0.072	0.066	+ 8%
(2)	PT-194 to PT-580	0.162	0.130	+ 24%
(3)	PT-580 to PT-770	<u>0.053</u>	<u>0.066</u>	<u>- 20%</u>
Total		0.286	0.262	Avg + 9%

The overall predicted loss coefficient is 9% higher than the actual value, and most of the errors appear to be in the valves. To bring the predicted values into better agreement with the measured values, the C_v of PCV-517 can be changed from 110 to 145. This change gives a predicted K/A^2 for Section 2 of 0.130. If the C_v of CHV-1885 is changed from 260 to 160, the predicted value of K/A^2 for Section 3 becomes 0.0595. It is concluded that the predicted loss coefficients agree satisfactorily with the observed values when vendor information is corrected.

Flow-resistance input data were upgraded on the basis of test data, as shown in Table 3. In this table, parts 1, 2, and 3 consist of 3, 3, and 1 nodes, respectively, in the computer model. These nodes were set up to describe the physical system in terms of such items as valves and elbows.

2. Gaseous-Helium Cooldown Line

This section summarizes the analysis of data from the gaseous-helium test in ETS-1 (FEP-IIB). Equations have been developed for use in simulating this system with an analog-computer program. Flow impedance data have been determined to upgrade the input to the digital-computer program. Blowdown characteristics of the helium bottles were also evaluated.

TABLE 3
LN₂-COOLDOWN-LINE RESISTANCE DATA

Flow Elements	Resistance of One Element	Entry Part I				Part I, PCV-517 Outlet to CHV-1885 Outlet					
		4.026-in. dia		2.967-in. dia		4.026-in. dia		5.761-in. dia		3.826-in. dia	
		No. of Elements	K	No.	K	No.	K	No.	K	No.	K
15° elbow	K=0.06										
30°	0.07										
45°	0.10					2	0.20				
60°	0.13										
90°	0.18	8	1.44			5	0.90	1	0.18		
135°	0.28										
Tee, flow through run 1/2 in.	K=0.05										
1 in.	0.07	1	0.07			1	0.07			1	0.07
1-1/2 in.	0.10										
2 in.	0.10	2	0.20								
4 in.	0.15	1	0.15								
6 in.	0.20										
Tee, flow through branch 6 in.	0.85										
Tee, instrumentation 1 in.	0.07	1	0.07								
Tank exit	0.78	1	0.78								
Straight line (1-in. length)	f=0.009	3077	6.8785	45	0.1365	840	1.8778	38	0.0594	43	0.1012
RSV-326	C _V =390	1	1.544								
PCV-517		1	16.00								
Flow through branch 4 in.	K=0.30	1	0.30								
FE-52				1	0.40						
RSV-545	C _V =625							1	3.0967		
CHV-1885	C _V =260									1	5.2880
Total K =			27.4325		0.5365		3.0478		3.3361		5.4592
K/A ² =			0.1693 in. ⁻⁴		0.0477 in. ⁻⁴		0.01881 in. ⁻⁴		0.0049 in. ⁻⁴		0.0413 in. ⁻⁴
			0.2170 in. ⁻⁴				0.0650 in. ⁻⁴				

TABLE 3 (cont.)

Flow Elements	Resistance of One Element	Part II, CHV-1885 to LF-7, 5.761-in. dia		Part III see Common (Table 6)	
		No. of Elements	K	No.	K
15° elbow	K=0.06				
30°	0.07				
45°	0.10				
60°	0.13				
90°	0.18	3	0.54		
135°	0.28				
Tee, flow through run 1/2 in.	K=0.05				
1 in.	0.07				
1-1/2 in.	0.10				
2 in.	0.10				
4 in.	0.15				
6 in.	0.20				
Tee, flow through branch 6 in.	0.85	1	0.85		
Tee, instrumentation 1 in.	0.07	2	0.14		
Tank exit	0.78				
Straight line (1-in. length)	f=0.009	212	0.3312		
Total K =			1.8612		
K/A ² =			0.0027 in. ⁻⁴		

a. Description of Test

The gaseous-helium line was flow-tested during FEP-IIB on 20 and 26 October 1966. This test involved a blowdown of the supply bottles from an initial pressure of about 2500 psia to about 500 psia. Initially, the flow was from all three GHe supply bottles. At bottle pressures of about 1600 psia, two of the bottles were shut off, and flow was supplied from only one bottle during the remainder of the test. The flow rate was controlled during the test with the pressure-control valve, PCV-471. Data were obtained with the flow varied from about 9 to 41 lb/sec. The control-valve position was varied from 10 to 97% open. The engine was simulated with a bypass that included a venturi flowmeter and a pressure-control valve, PCV-750, which was set 89% open to simulate the impedance of the engine system with gaseous-helium flow.

b. Analysis of Data

(1) Method of Analysis

A simplified schematic of the gaseous-helium cool-down line is shown in Figure 53. The line was divided into three sections, based on the location of instrumentation, for the calculations of line flow resistances and control-valve loss coefficient. The first section consisted of the bottle discharges, the helium header, and the portion of the main helium line to the inlet of the control valve, PCV-471; the second section consisted of the control valve; and the third section included the line from the control-valve exit to the venturi flowmeter, FE-X.

The data considered were taken from steady-state or quasi-steady-state portions of the test. Any flow accelerations were negligible, and the flow resistances were thus determined by the relationship $K = \rho \Delta P / \dot{w}^2$, where ρ is the density in lb/cu ft, ΔP is the pressure drop in lb/in.², and $\dot{w}$ is the mass flow rate in lb/sec. The flow-control valve was

Figure 53 - Simplified Flow and Instrumentation

represented by a correlation of a simplified loss coefficient vs valve position: $\dot{w} = K_W \sqrt{\rho \Delta P}$, where K_W is a function of valve position.

The valve choked at the lower gate positions, but the above relationship still holds because the valve is operating upstream of a choked-flow point.

(2) Evaluation of Flow Measurements

The initial effort involved correlating the flow-rate data. There were two flow-measuring devices in the line: the FT-415 flow tube and the FE-X venturi meter. Flow rates across FT-415 were computed on-line during the flow tests and were also computed manually after the tests on the basis of recorded test data. The posttest-calculated FT-415 and FE-X flows agreed fairly well (see Figure 54), whereas the on-line-computed FT-415 flow (after being corrected for temperature and compressibility) indicated poor agreement with the other two flows. The cause of this problem was not investigated in this study.

The flow across the engine-simulation valve, PCV-750, was sonic for all test data considered; therefore, the flow through the valve varied only with the valve inlet pressure, PT-774, inasmuch as temperature variation was insignificant. Figure 55 shows the plot of FT-415 flow vs PCV-750 inlet pressure, and Figure 56 shows the plot of FE-X flow vs PCV-750 inlet pressure. The plot with FE-X flow provides a straight-line curve fit passing through the origin, as expected. The plot with FT-415, although showing general agreement, also shows appreciable scatter. Because the PT-774 correlated better with FE-X than with FE-415, the FE-X flow was concluded to be more accurate and was used for the subsequent data analysis.

Figure 54 - Schematic, ETS-1 Gaseous-Helium Line Correlation of Flow Rates, FEP-IIB GHe Flow Test

Figure 55 - Correlation of FT-415 Flow with PCV-750 Inlet Pressure, FEP-IIB GHe Flow Test

Figure 56 - Correlation of FE-X Flow with PCV-750 Inlet Pressure,
FEP-IIB GHe Flow Test

(3) Evaluation of Pressure-Drop Data

A simplified schematic of the gaseous-helium system is shown in Figure 53. To evaluate the pressure-drop data, the cooldown line was broken into three sections as dictated by the pressure-measurement locations. The three sections were:

1. Supply bottles to FT-415 flow-tube inlet
2. FT-415 inlet to PCV-471 valve outlet
3. PCV-471 valve outlet to FE-X inlet

(a) Flow Resistance in Section 3

Flow through Sections 1 and 3 was at low Mach numbers throughout the test (approximately 0.11 in Section 3 and less than 0.10 in Section 1). With the flow through PCV-750 choked, the pressure drop through Section 3 should be approximately a function of the flow rate only, as shown by the following relation:

$$\Delta P \text{ Section 3} = \frac{K_3 \dot{w}^2}{\rho_3},$$

where the subscript 3 denotes conditions at the PCV-750 inlet. But $\rho_3 = \frac{144P_3}{Z RT_3}$ for the compressible gas, where Z is the compressibility factor.

Across PCV-750, $\dot{w} = \frac{K_v P_3}{\sqrt{T_3}}$, from the choked-flow equation.

Therefore, upon combining these two equations, $\Delta P = K'_3 \dot{w} \sqrt{T_3}$, where K'_3 is a constant that lumps together the constants from both equations.

Fluid temperature varied only about 30°R during the entire run, and it varied as the supply pressure decayed but did not vary with the flow rate. Because the fluid temperature has an insignificant effect

on the pressure-drop correlation, the pressure drop is expected to be proportional to the flow. This effect is shown in Figure 57, in which the pressure drop is plotted against the flow rate. At the lower flow rates, the data do deviate from the linear variation by small values (about 5 psi). The percentages of the deviations, however, are quite large (10% or greater); therefore, on the assumption that the small deviations were due to instrumentation errors (5 psi would be only 0.4% of the full scale range of the pressure transducers), the pressure differences were adjusted to agree with the linear variation.

The resistance of Section 3 (from PCV-471 outlet to FE-X inlet) was computed from the relationship

$$K = \frac{\Delta P \rho}{\dot{w}^2},$$

where $\Delta P = PT-473 - PT-770$ in psi and ρ is from the downstream conditions, PT-770 and TT-760, in lb/cu ft.

The computed K values for the test data points are shown in Figure 57. The data based on the uncorrected or as-measured pressure differences are widely scattered and would tend to indicate a decreasing value of K with increasing flow rate. When the pressure differences were adjusted to a linear variation, the scatter in the computed K values became much less, and a K that was constant with flow rate was indicated. The average of the latter data resulted in a K value of $0.053 \text{ sec}^2/\text{in.}^2\text{-ft}^3$ for that portion of the gaseous-helium line from PCV-471 to the FE-X inlet.

(b) Flow Resistance in Section 1

The resistance of Section 1 was computed from the test data by using the relationship

$$K = \frac{\Delta P \rho}{\dot{w}^2},$$

Figure 57 - Gaseous-Helium-System Pressure-Drop Data, FEP-IIB GHe Flow Test (PCV-471 Outlet to FE-X Inlet, Based on Downstream Density)

where ρ is based on downstream conditions, PT-586 and TT-528, and ΔP is the average tank pressure minus PT-586.

The computed K values are shown on Figure 58. The data also tended to indicate a decrease in resistance with increasing flow rate. The technique used on Section 3 could not be used to smooth the Section 1 data, as the flow is proportional to the pressure only for a fixed setting of PCV-471, which is in the downstream portion of this section. There were insufficient flow variations at any fixed position of the valve to determine a trend.

A pressure measurement, PT-5, is made at an intermediate location in Section 1. The transducer is located in a vent line just downstream of RSV-53 at a point estimated to be about 30 ft of the line distance from the helium header. This measured total pressure can be considered as the static pressure of the main line at the location where the vent line is installed, because the difference between static and total pressures would be minor for the low velocities through the line. Resistance was computed for the section of line from this location to the FT-415 inlet (Figure 58). The resistance is seen to be constant with flow rate, with an average value of $0.075 \text{ sec}^2/\text{in.}^2\text{-ft}^3$. The variation in K values in Section 1 then results from the test data measured from the supply bottles to the RSV-53 discharge.

The indicated variation in K values is probably caused by instrumentation errors. At the lower flow rates, the pressure difference becomes small, particularly at the high-pressure conditions. If the "X" points (marked in chronological order) in Figure 58 are correlated with their measured pressure differences, then, at the low flow rates of about 12 to 15 lb/sec, point 3 is seen to have the lowest ΔP (43 psi), point 4 the next lowest ΔP (53 psi), and so on, with points 6, 7, 8, and 9 having relatively higher ΔP values (100 to 110 psi). Because the pressure differences are obtained by taking the difference between gage measurements made with

Figure 58 - Gaseous-Helium-System Pressure-Drop Data, FEP-IIB GHe Flow Test (Supply Bottles to FT-415 Inlet, Based on Downstream Density)

instruments having a full range of 2500 psig, a nominal accuracy of 2% of full range would be 50 psi for each instrument or 70 psi for the pressure difference. Thus, the lower pressure differences fall within the 2% accuracy range of the data.

During the last portion of the test, the helium was supplied by only one bottle instead of three bottles. For the same flow rate, the portion of the system from the header to the bottle should have a greater pressure drop because the flow through this portion is three times as great as when all three bottles are supplying gas. The data at the higher flow rates (though only for two points) indicate a higher computed resistance, as expected.

Resistance in the line was therefore calculated by discounting the lower flow rates and assuming the resistance of the section from the bottles to the FT-415 inlet to be constant with flow rate. With all three helium bottles on line, the average resistance value (based on downstream density) was $0.12 \text{ sec}^2/\text{in.}^2\text{-ft}^3$, whereas, with only one bottle on line, the value was $0.17 \text{ sec}^2/\text{in.}^2\text{-ft}^3$.

(c) Loss Coefficient of PCV-471

The ratio of the measured pressures downstream and upstream of PCV-471 vs valve position is shown in Figure 59. The critical pressure ratio for helium is 0.487, and, as seen in Figure 59, choked flow was occurring below valve positions of 35°. Choked flow could also occur above the 35° position if there is a recovery of stagnation pressure downstream of the valve throat. Little pressure recovery is believed likely, and the downstream total pressure is most likely close to the throat static pressure.

Figure 59 - Pressure Ratio Across GHe-System Pressure-Control Valve, PCV-471, vs Valve Position; FEP-IIB GHe Flow Test

The loss coefficient for PCV-471 was computed by using the following relation:

$$K_w = \frac{\dot{w}}{\sqrt{\Delta P} \rho},$$

where ρ is based on the downstream conditions, PT-473 and TT-45, and $\Delta P = PT-586 - PT-473$.

This relationship, which assumes unchoked flow in PCV-471, can also be used for choked flow in this case because (1) the flow upstream and downstream of PCV-471 is always at a low mach number, (2) heat transfer to or from the fluid is insignificant, and (3) the flow is always choked at a downstream location across a fixed-resistance component.

Under these conditions, the pressure ratio across the valve, PCV-471, varies only with the valve position with both choked and unchoked flows, independent of the upstream conditions (see Appendix A). This effect was also verified by the test results shown in Figure 59. Thus, the transition between choked and unchoked flow will always occur at the same valve position, and a direct relationship that varies with valve position only exists between the upstream and downstream pressures under all conditions. With this direct pressure relationship under either choked or unchoked conditions, the equation $K_w = \dot{w} / \sqrt{\Delta P} \rho$ can be used to determine the loss coefficient, K_w , as a function of the valve position from the test data. A plot of loss coefficient vs valve position is shown in Figure 60.

(d) Loss Coefficient and Resistance Data Based on Upstream Densities

The format set up for the analog-computer model computes the flow conditions on the basis of downstream densities, whereas the digital program bases the computations on the upstream densities.

Figure 60 - Gaseous-Helium-System Pressure-Control-Valve PCV-471 Flow Characteristics, FEP-IIB GHe Flow Test (Based on Downstream Density)

The resistance data for Sections 1 and 3 and the loss-coefficient data for PCV-471 were therefore generated on the basis of upstream as well as downstream densities.

Figure 61 presents the resistance data for line Sections 1 and 3 based on the upstream densities, while Figure 62 shows the PCV-471 K_w data with density based on the valve inlet conditions (PT-586 and TT-528).

A pressure transducer, PT-103, that would see the static pressure from the helium line was located in the liquid-hydrogen cooldown line. This instrumentation is located just upstream of the interface between the gaseous cooldown line and the liquid-hydrogen line. The line Section 3 from PT-473, just downstream of control-valve PCV-471, to the inlet of FEX can therefore be further divided into two subsections: (1) from PT-473 to PT-103 and (2) from PT-103 to PT-770.

The data for pressure drop and computed resistance (based on upstream density) for the section from PT-473 to PT-103 are shown in Figure 63. The quality of the data is good; however, it is offset by a constant increment equal to about 8.5 psi. The magnitude of the data being evaluated previously caused this offset to go unrecognized in that analysis.

This offset is apparently attributable to the pressure loss induced by the force required to hold the two check valves in the line in an open position. These valves are CHV-685 and CHV-1721. Further investigation would be necessary to determine if this loss is representative of normal operation of the check valves or if one or both may have been operating in an abnormal manner. Similar results were obtained in an analysis of data from the gaseous-hydrogen line in the section from PT-475 to PT-103 (see Section III.C.3). Check valve CHV-1721 is common to both of these systems

Figure 61 - Gaseous-Helium-System Resistance Based on Upstream Density;
FEP-IIB GHe Flow Test

Figure 62 - Gaseous-Helium-System Pressure-Control Valve PCV-471 Flow Characteristics; FEP-IIB GHe Flow Test (Based on Upstream Density)

Figure 63 - Gaseous-Helium-System Pressure-Drop Data, FEP-IIB
GHe Flow Test (from PT-473 to PT-103, Based on
Upstream Density)

and may be the primary source of the constant ΔP loss. The constant ΔP offset results in a resistance that decreases with increasing flow rate. At a flow rate of 40 lb/sec, the resistance would be $0.0386 \text{ sec}^2/\text{in.}^2\text{-ft}^3$.

If the pressure-difference data are adjusted for the offset, the resulting pressure loss would be the loss of the system independent of the cause of the offset, whether it be due to normal valve opening force, abnormal valve operation, or an instrumentation error. The resistance of the section based on the adjusted ΔP data would be constant at about $0.0345 \text{ sec}^2/\text{in.}^2\text{-ft}^3$.

Figure 64 shows the pressure-difference and resistance data for the section from the LH_2 interface (PT-103) to the inlet to FE-X. The data have considerable scatter, but the variation with flow rate appears normal. A resistance value of $0.02 \text{ sec}^2/\text{in.}^2\text{-ft}^3$ was selected as the nominal value for this section.

(4) Helium-Bottle Blowdown Characteristics

During the gaseous-helium flow test, all three bottles were used from 2400 to about 1600 psia; then one bottle, V-3206, was used for the remainder of the run to about 500 psia. The test data were evaluated in an effort to establish a criterion for describing the blowdown characteristics in the computer models. The variation of bottle pressure with density is shown in Figure 65. The data shown are for bottle V-3206 only. The analysis was based on these data throughout the run to maintain consistency whether the flow was from one bottle or three bottles. In a computer simulation, the mass of gas in the bottles can be determined by subtracting an integration of the flow rate from the initial mass of the gas. It is then necessary to be able to relate the bottle pressure to the density.

Figure 64 - Gaseous-Helium-System Pressure-Drop Data, FEP-IIB
GHe Flow Test (from PT-103 to PT-770, Based on
Upstream Density)

Figure 65 - Gaseous-Helium-Bottle Blowdown Characteristics
 (Based on V-3206 Bottle Conditions)

A constant-volume blowdown process is comparable to a constant-mass increasing-volume process. The variation of pressure with density during the blowdown can thus be described by the following polytropic relationship:

$$\frac{P_2}{P_1} = \left(\frac{\rho_2}{\rho_1} \right)^n.$$

The exponent n was evaluated from the test data by using the initial bottle conditions (P_1 and ρ_1) as reference. Figure 65 shows that the exponent was fairly constant throughout the blowdown (except for the early part of the run), ranging between 1.3 and 1.4. A constant value of 1.3 results in pressures that are slightly higher than those for the test data in some ranges, whereas a value of 1.4 results in pressures that are too low in some areas. An exponent of 1.35 would apparently agree well enough with the test data; so a constant polytropic-process exponent of 1.35 is recommended for use in describing helium system blowdowns.

The control valve was constantly adjusted throughout the gaseous-helium flow test to provide both high- and low-flow conditions. In addition the flow was completely terminated for a short time during the test. If used for an emergency engine cooldown, the blowdown would not be subjected to such a range of control conditions, and a polytropic process with a constant exponent of 1.35 should provide even better agreement with the actual bottle conditions during the blowdown than is shown in Figure 65.

(5) Summary of Analysis

Analysis of data from the FEP-IIB gaseous-helium flow test produced the following results:

(1) Flow rates obtained from FT-415 were generally fairly good, with some data scatter.

(2) The resistance, based on downstream density, of the system from the supply bottles to the inlet of FT-415 was concluded to be $0.12 \text{ sec}^2/\text{in.}^2\text{-ft}^3$ with flow supplied by all three bottles and $0.17 \text{ sec}^2/\text{in.}^2\text{-ft}^3$ with flow supplied from only one bottle. When based on the upstream density, the corresponding values were 0.135 and 0.225 $\text{sec}^2/\text{in.}^2\text{-ft}^3$.

(3) The resistance of the system from the outlet of PCV-471 to the inlet of FE-X was concluded to be $0.053 \text{ sec}^2/\text{in.}^2\text{-ft}^3$ based on the downstream density and $0.0622 \text{ sec}^2/\text{in.}^2\text{-ft}^3$ based on the upstream density. When this section was further divided into two subsections from PT-473 to PT-103 and from PT-103 to PT-770, a constant pressure offset of about 8.5 psi that was probably associated with the operation of the check valves was discovered in the line section from PT-473 to PT-103. Resistance of the section from PT-473 to PT-103, based on the adjusted ΔP data (after subtracting 8.5 psi) and on the upstream density, was $0.0345 \text{ sec}^2/\text{in.}^2\text{-ft}^3$; and resistance of the section from PT-103 to PT-770, based on the upstream density, was $0.02 \text{ sec}^2/\text{in.}^2\text{-ft}^3$.

(4) The pressure ratio across the pressure-control valve, PCV-471, was established to vary only with valve position with either a choked or unchoked flow condition across the valve. The unchoked-flow relationship can then be used to compute flow conditions across the valve for all conditions.

(5) The loss coefficient, K_w , for the full-open position of the control valve, PCV-471 (actually includes the system from FT-415 inlet to PCV-471 outlet), was estimated to be 3.62 based on the upstream density. The flow coefficient based on the downstream density was estimated to be 3.79.

(6) Blowdown of the gaseous-helium system can be described by a polytropic process with a constant exponent, n , of 1.35.

c. Digital-Computer-Model Upgrading

(1) Comparison of Predicted and Actual Flow Resistances

Flow-resistance data in the computer model were previously obtained from the handbook and the vendor-quoted C_v . These predicted flow-resistance data were compared with, and upgraded on the basis of, actual flow-resistance data from the tests.

The predicted and actual-test-data flow resistances (between pressure transducers) for the gaseous-helium line agreed fairly well, as shown in the following table:

			$K/A^2, \text{ in.}^{-4}$		% Difference
			<u>Predicted</u>	<u>Actual</u>	
1.	Tank	to PT-586	0.1140	0.1006	+14
2.	PT-586	to PT-473	0.0376	0.0340	+11
3.	PT-473	to PT-103	0.0183	0.0173	+6
4.	PT-103	to PT-770	<u>0.0097</u>	<u>0.0089</u>	+9
Total			0.1796	0.1608	+12

The resistance values were all calculated from vendor-quoted C_v values by using the equation $K = \frac{(29.9)^2 d^4}{C_v^2}$. In Section 3, both remote shutoff valves,

RSV-454 and RSV-460, were open. A K value of 2.66 was calculated for the two resistances (5.85 and 4.90) in parallel. The resistances given for the first section are for flow from one tank only.

(2) Comparison of Predicted Computer Run and Test Data

A steady-state run was made on the digital computer program to simulate the pressure drop in the GHe cooldown line. Heat transfer was not checked because the fluid temperature rise in the line was negligible.

Flow-resistance inputs for the computer program appearing in Table 4 are the predicted values that were upgraded to match closely with the overall resistance data in the line from the tests. In the table parts 1, 2, and 3 consist of 3, 3, and 1 nodes, respectively. These nodes are set up to describe the physical system in terms of such items as valves and elbows.

Figure 66 compares the actual pressures along the cooldown line with the computer-predicted pressures. Although the comparison is limited, the good agreement shows that the computer program predicts the pressure drops accurately.

3. Gaseous-Hydrogen Cooldown Line

This section describes the analysis of the gaseous-hydrogen system data. The gaseous-hydrogen cooldown line for ETS-1 was flow-tested during FEP-II-B. These tests and the evaluation of the results are discussed in Reference 5. The test results were further evaluated to determine loss-coefficient data of the cooldown line for use in upgrading the digital- and analog-computer facility-simulation programs.

a. Analysis of Data

(1) Method of Analysis

A sketch of the gaseous-hydrogen cooldown line is shown in Figure 67 with the test instrumentation. An engine-simulation system was used in place of an actual engine during the FEP tests. This system included a flow-control valve, PCV-750, to simulate the engine impedance and a venturi flowmeter, FE-X.

In evaluating the flow characteristics, the cooldown line was broken into segments defined by the locations of the pressure measurements. The resulting segments were:

TABLE 4

GHe-COOLDOWN-LINE RESISTANCE DATA

Flow Elements	Resistance of One Element	Entry Part I, Tank 3204 to PCV-471				Part I to GH ₂ Interface, 5.761 in. dia		Part II to LN ₂ Interface, 5.761-in. dia		Part III, See Common (Table 6)	
		3.438-in. dia		5.189-in. dia							
		No. of Elements	K	No.	K	No.	K	No.	K	No.	K
15° elbow	K=0.06			4	0.24						
30°	0.07										
45°	0.10			4	0.40			2	0.20		
60°	0.13										
90°	0.18	6	1.08	13	2.34	2	0.36	2	0.36		
135°	0.28	1	0.28	1	0.28						
Tee, flow through run 1/2 in.	K=0.05			2	0.10						
1 in.	0.07	4	0.28			3	0.21				
1-1/2 in.	0.10										
2 in.	0.10										
4 in.	0.15										
6 in.	0.20	3	0.60			1	0.20	1	0.20		
Tee, flow through branch 6 in.	0.85	1	0.85	1	0.85			1	0.85		
Tee, instrumentation 1 in.	0.07			3	0.21	1	0.07	1	0.07		
Tank exit	0.78	1	0.78								
Straight line (1-in. length)	f=0.009	306	0.8010	5458	9.4662	233	0.3634	181	0.2828		
RSV-16	C _V =260	1	1.85								
RSV-53	C _V =580			1	1.92						
GLV-196	C _V =580			1	1.92						
GF-432	K=1.00			1	1.00						
FE-415	K=0.20			1	0.20						
PCV-471	C _V =210			1	14.60						
RSV-78	C _V =580					1	2.92				
CHV-685	C _V =580					1	2.92				
RSV-454 and RSV-460								1	2.6656		
CHV-1721	C _V =580							1	2.92		
CHV-2302	C _V =580							1	2.92		
Total K =			6.5210		33.5262		7.0440		10.4684		
$\frac{K}{A^2} (\text{in.}^{-4}) =$			0.07567 in. ⁻⁴		0.07497 in. ⁻⁴		0.0104 in. ⁻⁴		0.01541 in. ⁻⁴		
			0.1506 in. ⁻⁴

Figure 66 - Test vs Predicted Pressures, Gaseous-Helium Cooldown Line

Figure 67 - Simplified Schematic, ETS-1 Gaseous-Hydrogen Cooldown System

- (1) Bottle discharge pressures to PT-003
- (2) PT-003 to FE-015 inlet (PT-587)
- (3) FE-015 inlet to PCV-251 discharge (PT-475)
- (4) PCV-251 discharge to LH₂ interface (PT-103)
- (5) LH₂ interface to FE-X inlet (PT-770)
- (6) FE-X inlet to PCV-750 inlet (PT-774)

The pressure drops from the FE-015 inlet to the PCV-251 inlet and from the PCV-251 outlet to PT-475 are small; therefore, segment 3 is basically just the control valve PCV-251.

The resistances of the line segments were computed from the test data by using the relationship

$$K/A^2 = \frac{2g}{144} \times \frac{\Delta P \rho}{\dot{w}^2},$$

where

K/A^2 = Darcy resistance parameter, where A is cross-sectional area in in.²,

ΔP = pressure difference across the segment in psi,

ρ = density at inlet of segment in lb/cu ft,

$\dot{w}$ = flow rate across the segment in lb/sec.

The loss coefficient of the control valve, PCV-251, in Segment 3 was computed from the test data with the equation

$$K_W = \frac{\dot{w}}{\sqrt{\Delta P \rho}},$$

(2) Flow Measurements

Flow was measured with an orifice meter, FE-015, in the GH_2 line as well as with the FE-X venturi meter in the engine-simulation system. Previous experience had verified the accuracy of flow data obtained from FE-X. FE-415 flow data were not reliable, because the temperature measurement at the inlet was not operating properly during the test. Therefore, the flow-rate data used in the analysis were computed from the FE-X meter data.

The FE-X flow rate was computed from the relationship

$$\dot{w} = 0.62619 \frac{PT-770}{\sqrt{TT-760}} \times \frac{1}{\sqrt{Z}},$$

where PT-770 is the inlet pressure in psia, TT-760 is the inlet temperature in °R, and Z is a compressibility correction factor. This relationship assumes that the flow across the FE-X venturi is choked. A review of the data disclosed that the ratio of the pressure at the throat of the venturi to that at the inlet was nearly constant, varying from 0.56 to 0.58. It was concluded that the variation resulted from errors in the data and that a constant pressure ratio existed during the test. Choked flow conditions, therefore, existed across the venturi under all test conditions evaluated.

The flow is also choked across the engine-simulator valve, PCV-750. Because the flow across a choked component is a linear function of the upstream pressure (with insignificant temperature variations), these two choked components provide a means of evaluating the system flow rates. Figure 68 shows the variation of flow rate computed across FE-X vs the measured pressures at the inlets to FE-X and PCV-750. The data indicate that the flow rate obtained from FE-X is generally fairly accurate. There is considerable scatter but no evidence of a general bias or error.

Figure 68 - FE-X and PCV-750 Inlet Pressures vs Flow Rate;
FEP-IIB GH_2 Test Data

(3) Test Data

The test consisted of a blowdown from two bottles, V-3207 and V-3208, followed by a flow test with flow from bottle V-3207 only. Flow rates from about 5 to 24 lb/sec were established with the flow rate controlled by the PCV-251 control valve.

During evaluation of the test data, considerable scatter was found in the pressure-measurement differences throughout the cooldown line. This scatter reduces the confidence of the analysis; however, by using extensive data and giving primary consideration to the data at the higher flow rates, it is believed that good flow-coefficient data were established for the GH_2 line. A potential source of data scatter is the use of the difference between pressure measurements to determine the pressure losses. The pressure-measurement instruments have full-scale ranges of 1500 to 3000 psi. If the 3 σ uncertainty for these measurements is assumed to be 1/2% of the full-scale values, the uncertainty of the pressure difference would be from 11 to 21 psi. As most of the ΔP data have values from 0 up to about 70 psi, a large amount of scatter can result that would still be compatible with the accuracy of the instrumentation.

(4) Discussion of Results

(a) Segment 1 - Bottle Discharge Pressures to PT-003

Bottle pressures are measured in the bottle discharge lines upstream of the shutoff valve. The temperatures are measured in the bottles. In the case of flow from two bottles, the average pressure and temperature values were used. An examination of the data obtained when the control valve was closed and no flow existed through the cooldown line disclosed that the average of the discharge pressures from V-3207 and V-3208 was 8.7 psi greater than the pressure recorded for PT-003. The discharge

pressure of V-3207 was 13 psi greater than the PT-003 pressure. The pressure-difference data for Segment 1 were thus adjusted by these amounts for flow from two bottles and one bottle, respectively.

The variation of pressure difference with flow rate across Segment 1 is shown in Figure 69. The data obtained with flow from two bottles show considerable scatter (up to 5 psi) and appear to approach a constant value of about 8 psi at flow rates of 10 lb/sec and less. This trend and the scatter were concluded to be due to instrumentation inaccuracies.

Calculated flow resistances, K/A^2 , from the test data in the segment are also shown in Figure 69. For flow from two bottles, the average value of K/A^2 is about 0.015. For flow from only one bottle, the average value is about 0.018. The trend of constant ΔP at low flow rates is seen to create an increasing K/A^2 with decreasing flow. However, these effects were assumed to result from instrumentation inaccuracies, and the low flow data were ignored. The K/A^2 data indicate that the resistance of the segment increased by a factor of 1.2 when the GH_2 was supplied from only one bottle instead of two.

(b) Segment 2 - PT-003 to FE-015 Inlet (PT-587)

The second segment consists of the line segment from pressure measurement PT-003 to the inlet of the FE-015 flow orifice. Evaluation of the pressure measurements during a no-flow condition indicated that PT-003 was reading about 1.5 psi low compared with PT-587 at the FE-015 inlet. The pressure difference was therefore corrected by this amount.

The variation of pressure difference with flow rate is shown in Figure 70. Both a considerable scatter and an indication of a hysteresis effect appear at flow rates between 12 and 20 lb/sec. The resistance data, K/A^2 , computed from the test data are also shown in Figure 70.

Figure 69 - Pressure Loss and Resistance from GH_2 Bottles to PT-003;
FEP-IIB GH_2 Test Data

Figure 70 - Pressure Loss and Resistance from PT-003 to PT-587;
FEP-IIB GH_2 Test Data

An interesting contrast appears in the resistance data when compared with the ΔP data. At the high flow rates, there is much less scatter than at the low flow rates. The hysteresis loop indicated by the ΔP data is absent from the resistance data, indicating that this trend is probably not abnormal but is only a result of the decrease in density as the test progressed. Based on the high-flow data, the nominal resistance value for this segment of the cooldown line was selected as 0.033.

(c) Segment 3 - FE-015 Inlet to PCV-251 Discharge
(PCV-251 Control Valve)

Segment 3 consists primarily of control valve PCV-251, although the pressure measurements are taken from the flow-orifice inlet and a point downstream of the valve discharge. Test data were recorded for valve positions ranging from 20° to full open. The variation of pressure ratio across the valve with valve position is shown in Figure 71. As discussed in Section III.C.2.b, the pressure ratio across a valve located upstream of a point of choked flow (both FE-X and PCV-750 are choked) varies only with the valve position. Some of the data points were found to be offset from the main data; however, examination of the data disclosed that these data points were recorded during periods when the valve was being rapidly closed. The increase in pressure ratio apparently resulted from transient effects.

A flow coefficient defined by the relationship

$$K_w = \frac{\dot{w}}{\sqrt{\Delta P \rho}}$$

was computed from the test data. The variation of K_w with position is shown in Figure 72. K_w for the full-open valve position is approximately 3.06. The equivalent resistance value (K/A^2) would be 0.0478. The effect of the fast-closing-valve transient also shows up in the K_w data.

Figure 71 - Pressure Ratio vs Valve Position for GH₂-Cooldown-System
Control-Valve PCV-251; FEP-IIB GH₂ Test Data

Figure 72 - Flow Coefficient vs Valve Position for GH₂-Cooldown-System Control-Valve PCV-251; FEP-IIB Test Data

If the valve is assumed to be choked at pressure ratios of less than 0.528, Figure 71 shows that the flow across PCV-251 is then choked at valve positions of less than 65°. It was shown in Section III.C.2.b, however, that the above relationship for K_w can be used to define the valve characteristics under either choked or unchoked conditions, provided the flow is choked across a downstream fixed resistance.

(d) Segment 4 - PCV-251 Discharge to LH₂ Interface

Figure 73 shows the variation of pressure difference across Segment 4 with flow rate. This portion of the cooldown line was not exposed to a pressurized no-flow condition; therefore, no correction was established for the pressure difference for Segment 3 and locations downstream of this segment. Considerable scatter was again present in the data. The data are offset by about 13 psi. This offset is apparently the ΔP required to hold the two check valves, CHV-681 and CHV-1721, in the open position. It is not presently known whether this value is excessive or not. The resistance decreases as the flow rate increases. At high-flow conditions it approaches a K/A^2 of about 0.012. If the pressure-difference data are adjusted for the ΔP , offset of 13 psi, the remaining pressure difference should be the pressure loss across the system, independent of the pressure increments required to hold the check valves open, whether in normal or abnormal operation, and independent of any errors in the data. The resistance based on these adjusted data (see Figure 73) is constant at a K/A^2 value of approximately 0.008.

(e) Segment 5 - LH₂ Interface to FE-X Inlet

Segment 5 includes the cooldown line from the LH₂ interface (PT-103) through CHV-2302, the LF-7 filter, to the FE-X inlet of the engine simulator. The variation of the pressure difference across the segment with flow rate and the resistance data computed from the test results

Figure 73 - Pressure Loss and Resistance from PT-475 to PT-103;
FEP-IIB GH_2 Test Data

are shown in Figure 74. Aside from considerable scatter in both the pressure-difference and the resistance data, the data appear normal. In selecting a nominal constant resistance value, the high-flow data were given primary consideration. The value selected for the nominal resistance of the segment was 0.01.

(f) Segment 6 - FE-X Inlet to PCV-750 Inlet

Segment 6 consists of the engine-simulator portion from the inlet of FE-X to the inlet of the simulator valve, PCV-750. This segment is not relevant to the engine test program but is considered of interest in establishing criteria for predicting system resistance data. The pressure-difference and resistance data are presented in Figure 75. A nominal resistance value for K/A^2 of 0.035 was selected for the segment. However, as mentioned above, the flow was choked across FE-X throughout the test. The pressure-difference and resistance data are thus unrealistic because the flow is also choked across PCV-750 and the measured downstream pressure is not related to the upstream pressure but to the position of PCV-750.

b. Flow-Resistance Input Data

Flow-resistance data in the computer model were previously obtained from the handbook and the vendor-quoted C_v . These predicted flow-resistance data were compared with, and upgraded on the basis of, the actual flow-resistance data from the tests.

Predicted and actual (test data) flow resistances agreed well in the CH_2 cooldown line, as shown in the following table:

Figure 74 - Pressure Loss and Resistance from PT-103 to PT-770
FEP-IIB GH_2 Test Data

Figure 75 - Pressure Loss and Resistance from PT-770 and PT-774;
FEP-IIB GH_2 Test Data

				K/A^2		<u>% Difference</u>
				<u>Predicted</u>	<u>Actual</u>	
(1)	PT-442	to	PT-003	0.0225	0.018	+25
(2)	PT-003	to	PT-587	0.0287	0.033	-13
(3)	PT-587	to	PT-475	0.0466	0.0478	-3
(4)	PT-475	to	PT-103	0.0132	0.012	+10
(5)	PT-103	to	PT-770	<u>0.0097*</u>	<u>0.010</u>	-3
Total				0.1207	0.1208	.08%

*Same as Section 4 of gaseous-helium line.

In Section 4, both remote shutoff valves, RSV-454 and RSV-460, were open for the tests. A K of 2.67 was calculated for the two resistances (5.84 and 4.90) in parallel.

Upgraded flow-resistance input data are shown in Table 5. Parts 1, 2, and 3 consist of 3, 3, and 1 nodes, respectively, in the computer model. These nodes are set up to describe the physical system in terms of such items as valves and elbows.

Table 6 summarizes the flow-resistance input data to be used in the line section from the intersection of the LH_2 and LN_2 lines upstream of the filter, LF-7, to the nozzle torus inlet, which is common to all cooldown lines.

4. Pump Inlet Line

With the use of FEP III-2 test data and other available information, flow-resistance coefficients and pressure-loss values were determined for the propellant feed line from the LH_2 V-5001 run tank to the pump inlet. These values were then compared with those being used for the predictions of the LH_2 run-tank supply line (Reference 16). Finally, the test data

TABLE 5
GH₂-COOLDOWN-LINE RESISTANCE DATA

Flow Elements	Resistance of One Element	Entry Part I, Tank 3208 to PCV-251 5.189-in. dia		Part I to GHe Interface, 5.761-in. dia		Part II to LN ₂ Interface, 5.761-in. dia		Part III, See Common (Table 6)	
		No of Elements	K	No.	K	No.	K	No.	K
15° elbow	K=0.06	4	0.24						
30°	0.07								
45°	0.10	2	0.20			2	0.20		
60°	0.13								
90°	0.18	7	1.26	1	0.18	2	0.36		
135°	0.28								
Tee, flow through run 1/2 in.	K=0.05	2	0.10						
1 in.	0.07	4	0.28						
1-1/2 in.	0.10	2	0.20						
2 in.	0.10	1	0.10						
4 in.	0.15			1	0.15				
6 in.	0.20	11	2.20			1	0.20		
Tee, flow through branch 6 in.	0.85	3	2.55	1	0.85	1	0.85		
Tee, instrumentation 1 in.	0.07	5	0.35	1	0.07	1	0.07		
Tank exit	0.78	1	0.78						
Straight line (1-in. length)	f=0.009	5861	10.1655	132	0.2062	181	0.2828		
RSV-246	C _V =580	1	1.92						
RSV-520	C _V =580	1	1.92						
RSV-201	C _V =580	1	1.92						
CHV-117	C _V =580	1	1.92						
FE-15	K=0.70	1	0.70						
GF-14	K=1.00	1	1.00						
PCV-251	C _V =195	1	17.0454						
CHV-681	C _V =580			1	2.92				
RSV-454 and RSV-460						1	2.6656		
CHV-1721	C _V =580					1	2.92		
CHV-2302	C _V =580					1	2.92		
Total K =			44.8509		4.3762		10.4684		
K/A ² =			0.1003 in. ⁻⁴		0.0064 in. ⁻⁴		0.0154 in. ⁻⁴		

TABLE 6
COMMON COOLDOWN LINE
(LF-7 to Nozzle Torus)

Flow Elements	Resistance of One Element	Part III, LF-7 to CKV				Part III Exit, CKV to Nozzle Torus, 4.624-in. dia	
		5.761-in. dia		4.624-in. dia		4.624-in. dia	
		No. of Elements	K	No.	K	No.	K
15° elbow	K=0.06						
30°	0.07						
45°	0.10						
60°	0.13			1	0.13		
90°	0.18	1	0.18	2	0.36	1	0.18
135°	0.28						
Tee, flow through run 1/2 in.	K=0.05						
1 in.	0.07	1	0.07				
1-1/2 in.	0.10						
2 in.	0.10						
4 in.	0.15						
6 in.	0.20	2	0.40				
Tee, flow through branch 6 in.	0.85						
Tee, instrumentation 1 in.	0.07			1	0.07		
Tank exit	0.78						
Straight line (1-in. length)	f=0.009	214	0.3343	146	0.2842	190.9	0.3715
Flow through run 5 in.	K=0.18			1	0.18		
Bellows	K=0.035			6	0.21	1	0.035
Flow through branch 60-in.	K=0.35			1	0.35		
LF-7	K=1.00	1	1.00				
CSV				1	2.92		
CKV				1	2.92		
Total K =			1.9843		7.4242		0.5865
K/A ² =			0.0029 in. ⁻⁴		0.0263 in. ⁻⁴		0.0021 in. ⁻⁴
			0.0292 in. ⁻⁴				

were used to upgrade the prediction model. To achieve the above objective, the test data had to be adjusted to account for differences between static and total pressures and elevation-head effects.

a. Test Data and Flow Resistances

Figure 76 shows the pump inlet section, including the instrument measurement locations. The curves for ETS-1 facility LH_2 -run-tank supply-line pressure losses and flow-resistance coefficients vs propellant flow rate (Figures 77 and 78) were constructed from the FEP III-2 static-pressure test data.

Figure 77 shows little scatter in the pressure measurements. The small scatter increases confidence in the data, although there is no way to confirm that a data bias does not exist. The plots of flow resistances in Figure 78 show that, regardless of flow rate, the K/A^2 term remains relatively constant, which further confirms confidence in the data.

b. Comparison of Flow Resistances in Design Data Book with Test Data

The FEP-III test data incorporate the pressure loss of two 8-in. Potter flowmeters, but current plans for testing XE-1 are based on using one flowmeter. In addition, system flow-resistance and pressure-loss values outlined in the Design Data Book were based on total rather than static pressure and on the use of one flowmeter in the system. Therefore, to properly compare the flow resistance and pressure loss obtained from test data and as outlined in the Design Data Book, some system consistency had to be established.

An additional flowmeter (and corresponding flowmeter resistance value) was added to the system outlined in the Design Data Book.

EST. ITEM WEIGHTS * '																	
ITEM	1	2	3	4	5	6	7	8	9	10	11	12	13	14	15	16	17
LB	190	61	10	20	40	50	17	39	25	53	30	326	18	75	48	105	560

* ALL STAINLESS STEEL EXCEPT ITEM 14, 90% ALUM

● SENSING POINT

Figure 76 - Pump Inlet Section

- NOTE: 1. PRESSURE LOSSES ADJUSTED TO ELIMINATE THE EFFECTS OF ELEVATION DIFFERENCE
2. TWO POTTER FLOW METERS IN SYSTEM.

Figure 77 - As-Measured Pressure Loss vs LH₂ Flow Rate for Run-Tank Supply Line; FEP-III-2 Test Data

Figure 78 - V5001 LH₂-Run-Tank Supply-Line Flow Resistance vs Propellant Flow Rate

The test data were then converted to total pressure, and the pressure head due to the LH_2 propellant tank and line elevation was accounted for. Figures 79 and 80 represent the pressure-loss and flow-resistance curves based on total pressure.

Figure 81 presents the final results of this analysis. Pressure values have been plotted against each of the FEP pressure stations (PT-100, PT-485). Curve 1 represents the flow-resistance values of the Design Data Book that are shown in Table 7. Curves 3 and 4 represent the FEP test data for 80 and 20% propellant tank levels. A discrepancy is evident between the predicted and measured values. Each section in the LH_2 run-tank line was therefore analyzed.

In reviewing the curves shown in Figure 81, the largest discrepancy seems to be within the Potter-flowmeter area. Investigation of this area indicated that the ΔP of 0.664 psi used to calculate the Potter-flowmeter flow-resistance coefficient for the Design Data Book was low. Discussions with the vendor, as well as with AGC test personnel, indicated that the ΔP should be between 1 and 2 psi. However, no verification could be obtained that would provide a ΔP value. Therefore, a Potter-flowmeter ΔP was obtained by backtracking into the FEP test data. The pressure loss obtained was 1.51 psi.

Additional line components were also analyzed, and adjustments and corrections were made to various flow-resistance values. Table 7 shows the modified total line losses and flow-resistance values. Curve 2, shown as a dotted line in Figure 81, represents the revised pressure distribution plotted against the various line-pressure station points. Disagreement between the test-data pressure and that determined by the flow-resistance values shown in Table 7 is still seen to exist between the V-5001 run tank and a point downstream of the Hadley valve (PT-100). Investigation of this area revealed no reason for the discrepancy, and it is assumed that

- NOTES: 1. ELEVATION EFFECT NOT INCLUDED
2. ΔP IS BASED ON TOTAL PRESSURE
3. TWO POTTER FLOWMETERS IN SYSTEM

Figure 79 - Pressure Loss vs LH₂ Flow Rate, Based on Total Pressure;
FEP-III-2 Test Data

Figure 80 - V5001 LH₂-Run-Tank Supply-Line Flow Resistance vs Flow Rate Based on Total Pressure

LH_2 TEMP = 38°R
 FLOW RATE = 80 LB/SEC
 TANK PRESSURE = 35 PSIA
 TWO FLOWMETERS, AS RUN IN FEP III-2

Figure 81 - System Pressure Loss; LH_2 -Run-Tank Supply Line to Pump

TABLE 7
XE-1 SYSTEM-ANALYSIS DESIGN-DATA-BOOK AND
FEP-III-2 TEST FLOW-RESISTANCE AND PRESSURE-
LOSS COMPARISONS

Title	Total-Line Loss Coefficient LH ₂ -Run-Tank Supply Line ($\dot{w}=80$ lb/sec; $\rho=4.33$ lb/cu ft)					
	Design Data Book, RN-S-0289A		Design Data Book, Modified		FEP Test Data	
	K/A^2	ΔP	K/A^2	ΔP	K/A^2	ΔP
Entrance	0.0000132		0.0000132			
Six 90° elbows	0.0000647		0.0000647			
Line length	0.0000193		0.0000187			
14- by 8-in. reduction	0.0000333		0.0000333			
Bellows			0.0000064			
Straight line to Hadley valve	0.0000182		0.0000182			
Hadley valve	0.0001200		0.0001200			
Straight line below Hadley Valve to P _{T100}	<u>0.0000046</u>		<u>0.0000046</u>			
Total	0.0002733	0.904	0.0002791	0.922	0.0006596	2.18
Straight line P _{T100} to flowmeter	0.0000085		0.0000085			
50 F-1 425 flowmeter	0.0002034		0.0004658			
50 F-1 014 flowmeter	0.0002034		0.0004658			
Strainer	0.0000005		0.0000005			
Straight line below strainer	0.0000323		0.0000323			
Expansion section to P _{T485}	<u>0.0000405</u>		<u>0.0000405</u>			
Total	0.0007619	2.52	0.0012925	4.29	0.0016800	5.55
P _{T485} to end of expansion	0.0000234		0.0000234			
PSV	0.0003440		0.0002250			
Straight line to bellows	0.0000191		0.0000191			
Bellows	0.0003160		0.0000037			
Straight line to pump	<u>0.0000284</u>		<u>0.0000284</u>			
Total $\left[\left(\frac{K}{A^2} \right) \right]$	0.0014928	4.94	0.0015921	5.28	---	---

either some additional unaccountable restriction exists in the line or that the Hadley-valve flow coefficient is low.

Table 8 reflects the latest changes to be made in upgrading the XE System Analysis Design Data Book.

TABLE 8
REVISED XE-1 SYSTEM-ANALYSIS DESIGN-
DATA-BOOK FLOW RESISTANCES AND PRESSURE LOSSES

Total-Line Loss Coefficient LH ₂ -Run-Tank Supply Line ($\dot{w}=80$ lb/sec; $\rho=4.33$ lb/cu ft)			
<u>Title</u>	<u>Design Data Book, Revised</u>		<u>ΔP</u>
	<u>K/A^2</u>		
1 Entrance			
2 Six to 90° elbows			
3 Line length			
4 14- by 8-in. reduction			
5 Bellows			
6 Straight line to Hadley valve			
7 Hadley valve			
8 Straight line below Hadley valve to P _{T100}			
Total	0.0006596		2.18
9 Straight line P _{T100} to flowmeter	0.0000085		
10 50 F-1 425 flowmeter	0.0004653		
11 Straight line below flowmeter	0.0000131		
12 Strainer	0.0000005		
13 Straight line below strainer	0.0000323		
14 Expansion section to P _{T485}	0.0000405		
Total	0.0012198		4.03
15 P _{T485} to end of expansion	0.0000234		
16 PSV	0.0002250		
17 Straight line to bellows	0.0000191		
18 Bellows	0.0000037		
19 Straight line to pump	0.0000284		
Total $\int (\frac{K}{A^2})$	0.0015194		5.02

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APPENDIX A

DERIVATION OF CORRELATION OF PRESSURE RATIO WITH VALVE POSITION

APPENDIX A

DERIVATION OF CORRELATION OF PRESSURE RATIO WITH VALVE POSITION

In the GHe cooldown line, the loss coefficient, $K_W = \dot{w} / \sqrt{\rho \Delta P}$, which assumes unchoked flow through PCV-471, can also be used for choked flow because (1) the flow upstream and downstream of PCV-471 is always at a low Mach number, (2) heat transfer to or from the fluids is insignificant, and (3) the flow is always choked at a downstream location across a fixed-resistance component.

Under these conditions, the pressure ratio across the valve, PCV-471, can be shown to vary only with the valve position for both choked and unchoked flows, independent of the upstream condition.

If the flow across PCV-471 is not choked, it is described by the equation:

$$\dot{w} = K_W \sqrt{\rho_1 \Delta P}.$$

However,

$$\rho_1 = \frac{144 P_1}{Z R T_1} \text{ and } \Delta P = P_1 \left(1 - \frac{P_2}{P_1}\right);$$

thus, the equation can also be written as:

$$\dot{w} = \frac{K_1 P_1}{\sqrt{T_1}} \sqrt{1 - \frac{P_2}{P_1}}, \text{ where } K_1 = K_W \sqrt{\frac{144}{Z R}}.$$

It is shown in Figure 1 that the pressure loss across the section of the system located between PCV-471 and the downstream point of choking, PCV-750, is a function of flow rate only; i.e.:

APPENDIX A

Figure 1 - Gaseous-Helium-System Pressure-Drop Data, PCV-471
 Outlet to FE-X Inlet, Based on Downstream Density;
 FEP-IIB GHe Flow Tests

Appendix A

$$\dot{w} = \frac{K_2}{\sqrt{T_3}} \left[P_2 - P_3 \right] \quad (\text{temperature does not change significantly}).$$

The flow across PCV-750 is described by the choked relationship:

$$\dot{w} = K'_3 P_3 / \sqrt{T_3}.$$

With all valves at a fixed position, a quasi-steady-state condition exists in which the flow rate can be assumed to be constant throughout the system; thus,

$$\dot{w} = \frac{K_1 P_1}{\sqrt{T_1}} \sqrt{1 - \frac{P_2}{P_1}} = \frac{K_2}{\sqrt{T_3}} (P_2 - P_3) = K'_3 \frac{P_3}{\sqrt{T_3}}.$$

From the latter two terms, P_3 can be related to P_2 :

$$P_3 = \frac{K_2 P_2}{K'_3 + K_2}.$$

Then, replacing P_3 in the middle term with this relation and equating the first two terms gives:

$$\frac{K_1 P_1}{\sqrt{T_1}} \sqrt{1 - \frac{P_2}{P_1}} = \frac{K_2}{\sqrt{T_3}} \left(P_2 - \frac{K_2 P_2}{K'_3 + K_2} \right),$$

and

$$\frac{K_1 P_1}{\sqrt{T_1}} \sqrt{1 - \frac{P_2}{P_1}} = \frac{K_2 P_2}{\sqrt{T_3}} \left(\frac{K'_3}{K'_3 + K_2} \right),$$

Appendix A

or

$$\frac{P_2}{P_1} = \frac{K_1}{T_1} \left(1 - \frac{P_2}{P_1} \cdot \frac{T_3}{K_2} \left(\frac{K'_3 + K_2}{K'_3} \right) \right);$$

$$\frac{P_2}{P_1} = K_1 \left(\frac{K'_3 + K_2}{K_2 K'_3} \right) \frac{T_3}{T_1} \left(1 - \frac{P_2}{P_1} \right).$$

Because temperature variations during the test were small, the ratio $\frac{\sqrt{T_3}}{\sqrt{T_1}}$ can be considered constant. The parameters K_2 and K'_3 are constant resistance terms. The parameter K_1 is the flow-coefficient term for PCV-471 valve and varies only with valve position. Thus, the pressure ratio across PCV-471 is seen to vary only with the valve position.

If the flow across PCV-471 is choked, it is described by the relationship:

$$\dot{w} = \frac{K_1 P_1}{\sqrt{T_1}} \left(1 - \gamma_c \right),$$

where γ_c is the critical pressure ratio for choked flow with helium. This ratio is a constant; therefore, the equation can be written:

$$\dot{w} = \frac{K'_1 P_1}{\sqrt{T_1}}.$$

The flow across the section downstream of PCV-471 and across PCV-750 is described by the same relationships as above. Then

Appendix A

$$\dot{w} = \text{constant} = \frac{K'_1 P_1}{T_1} = \frac{K_2}{T_3} (P_2 - P_3) = K'_3 \frac{P_3}{T_3}.$$

This equation can be simplified to give

$$\frac{P_2}{P_1} = K'_1 \frac{(K'_3 + K_2)}{K_2 K'_3} \frac{T_3}{T_1}.$$

Thus, as in the case of unchoked flow, the pressure ratio across PCV-471 also is seen to vary only with valve position in the case of sonic flow.

The correlation of the pressures upstream and downstream of PCV-471 is related to the downstream resistance creating the downstream choked flow. In the GHe flow test, this resistance was created by the valve, PCV-750, set to simulate the engine resistance. In the case of an engine test, the engine itself would create this resistance. Therefore, if the valve were not correctly simulating the engine, or if the engine resistance were changed, the data shown in Figure 2 for PCV-471 valve pressure ratio vs position would be different.

APPENDIX A

Figure 2 - Pressure Ratio Across GHe-System Pressure-Control Valve, PCV-471, vs Valve Position; FEP-IIB GHe Flow Test

APPENDIX B

ANALOG-COMPUTER MODEL AND EQUATIONS

APPENDIX B

ANALOG-COMPUTER MODEL AND EQUATIONS

This appendix describes the analog-computer model and equations that were used to predict transient response in the LH_2 cooldown line.

I. DERIVATION OF EQUATIONS

A. VALVE FLOW EQUATION

$$C_V \equiv \frac{\text{gpm of water}}{\sqrt{\Delta P}} = \frac{Q}{\sqrt{\Delta P \left(\frac{62.4}{\rho}\right)}}$$

$$\begin{aligned} w &= Q \rho \frac{0.1337}{60} \\ &= \frac{0.1337}{60} \rho C_V \sqrt{\Delta P \left(\frac{62.4}{\rho}\right)} \\ &= 0.0176 C_V \sqrt{\rho \Delta P}. \end{aligned}$$

B. LINE PRESSURE DROP

$$\begin{aligned} \Delta P &= K \left(\frac{v^2}{2g} \frac{\rho}{144} \right) = f \frac{L}{D} \left(\frac{v^2}{2g} \frac{\rho}{144} \right) \\ &= K \left(144^2 \frac{w^2}{A^2 \rho^2} \frac{\rho}{2g(144)} \right) \\ &= 2.24 \frac{K}{A^2} \frac{w^2}{\rho} \end{aligned}$$

Appendix B

C. INERTIAL PRESSURE DROP IN LINE

$$F = \frac{W dv}{g dt};$$

$$v = 144 \frac{w}{A \rho};$$

$$w = \frac{AL \rho}{1728};$$

$$(P_1 - P_2)A = \frac{AL \rho}{1728g} \frac{144}{A \rho} \frac{dw}{dt} = \frac{L}{12g} \frac{dw}{dt};$$

$$P_1 - P_2 = \frac{L}{12gA} \frac{dw}{dt}.$$

D. LINE FLOW EQUATION

$$\Delta P = \text{friction drop} + \text{inertial drop}$$

$$= 2.24 \frac{K}{A^2} \frac{w^2}{\rho} + \frac{L}{12gA} \frac{dw}{dt};$$

$$\frac{dw}{dt} = \frac{12g}{L/A} \left(\Delta P - 2.24 \frac{K}{A^2} \frac{w^2}{\rho} \right).$$

Appendix B

E. LIQUID-COMPRESSIBILITY EQUATION

$$dP = \frac{dP}{d\rho} d\rho;$$

$$\rho = \frac{W}{V};$$

$$\text{for fixed volume, } d\rho = \frac{dW}{V};$$

$$dP = \frac{dP}{d\rho} \frac{dW}{V};$$

$$\frac{dP}{dt} = \frac{dP}{d\rho} \frac{1}{V} \frac{dW}{dt} = \frac{dP}{d\rho} \frac{1}{V} \Delta w.$$

F. NOTATION

A = cross section, in.²

C_V = valve admittance, gpm/ $\sqrt{\text{psi}}$

D = diameter, in.

f = friction factor

F = force, lb

g = gravitational constant, 32.2 ft/sec²

K = resistance factor

L = length, in.

P = pressure, psi

Q = volumetric flow rate, gpm

t = time, sec

v = velocity, ft/sec

V = volume, cu ft

w = flow rate, lb/sec

W = weight, lb

ρ = density, lb/cu ft

Appendix B

II. ANALOG MODEL SUMMARY

KEY:

$$\frac{L/A, \text{ in.}^{-1}}{K/A^2, \text{ in.}^{-4}}$$

VOLUME, cu ft

$$\frac{dw_{107}}{dt} = 9.45 (P_{5002} - P_{472}) - 0.064 w_{107}^2$$

$$\frac{dP_{472}}{dt} = 213 (w_{107} - w_{472})$$

$$w_{472} = 0.103 \theta_{472} \sqrt{P_{472} - P_{458}}$$

$$\frac{dP_{458}}{dt} = \frac{dp/d\rho}{3.89} (w_{472} - w_{458})$$

Appendix B

$$\frac{dw_{458}}{dt} = 22.05 (P_{458} - P_{2302}) - \frac{16.1}{\rho} w_{458}^2$$

$$\frac{dP_{2302}}{dt} = \frac{dP/d\rho}{5.77} (w_{458} - w_{2302})$$

$$\frac{dw_{2302}}{dt} = 7.92 (P_{2302} - P_{750}) - \frac{0.257}{\rho} w_{2302}^2$$

$$\frac{dP_{750}}{dt} = \frac{dp/d\rho}{22.94} (w_{2302} - w_{750})$$

$$w_{750} = 0.0176 C_V \sqrt{\rho (P_{750} - 13)}$$

$$P_{772} = \frac{0.108}{\rho} w_{2302}^2$$

III. TEMPERATURE DISTRIBUTION

Dimensions given are equivalent (in terms of volume) inches of 5.761-in.-ID pipe.

Appendix B

P_{472}	$T = TT-28$
P_{458}	$T = TT-495$
P_{2302}	$T = TT-495 + 0.75 (TT\ 474 - TT\ 495)$
P_{750}	$T = 0.25 (TT\ 474 + TT\ 761 + 2\ TT\ 763)$
w_{107}	$T = TT-28$
w_{472}	$T = TT-28$
w_{458}	$T = TT-495$
w_{2302}	$T = TT-761$
w_{750}	$T = TT-763$

<u>Quantity</u>	<u>Value During</u>	
	<u>Manual Steps</u>	<u>Frequency Response</u>
TT 28	39.6	39.6
TT 495	41.0	41.5
TT 474	48.0	46.0
TT 761	50.0	46.5
TT 763	58.0	51.0
$w_{107}\ T, \rho$	39.6, 4.57	39.6, 4.57
$P_{472}\ T, dP/d\rho$	39.6, 3380	39.6, 3380
$w_{472}\ T, \rho$	39.6, 4.57	39.6, 4.57
$P_{458}\ T, dP/d\rho$	41, 3050	41.5, 3010
$w_{458}\ T, \rho$	41, 4.46	41.5, 4.44
$P_{2302}\ T, dP/d\rho$	45.5, 2720	45, 2760
$w_{2302}\ T, \rho$	50, 4.06	46.5, 4.23
$P_{750}\ T, dP/d\rho$	53.5, 2040	48.6, 2460
$w_{750}\ T, \rho$	58, 3.56	51, 4.00

Appendix B

NOMENCLATURE

f	= void fraction
H	= internal heat generation - Btu/sec-in. ³
ΔT	= time increment - sec
K	= thermal conductivity - Btu/in.-sec-°R
C_r	= Specific heat of metal - Btu/in.-°R
ρ_r	= density of metal - lbm/in. ³
$T_o(m + 1, n)$	= fluid temperature in node $m + 1$ at time point n - °R
W	= fluid flow rate - lbm/sec
A_F	= flow area - in. ²
ρ	= density - lbm/in. ³
g	= gravitational constant - in.-lbm/lbf-sec ²
ΔZ	= node length - in.
$\emptyset$	= hydraulic diameter - in.
$\emptyset$	= friction factor
C_p	= specific heat of fluid - Btu/in.-°R
h_f	= convective-heat-transfer film coefficient - Btu/sec-°R-in. ²
ΔZ	= node length - in.
F_p	= wetted perimeter - in.
θ	= metal temperature

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