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SNAP 29—POWER SUPPLY SYSTEM

Sixth Quarterly Progress Report, January 1, 1968—March 31, 1968

April 19, 1968

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Martin Marietta Corporation
Nuclear Division
Baltimore, Maryland

AEC Research and Development Report

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SYSTEMS FOR NUCLEAR AUXILIARY POWER (SNAP) (M-3679(56th Ed.))

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SNAP 29

Power Supply System

Sixth Quarterly Progress Report

SPECIAL AGENCY
DETERMINATION
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FOREWORD

This is the Sixth Quarterly Progress Report prepared under Contract AT(29-2)-2062. It covers the progress of the Martin Marietta Nuclear Division on the SNAP 29 program and is prepared for the United States Atomic Energy Commission.

The report covers the period from January 1, 1968 through March 31, 1968.

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SUMMARY

A. RADIOISOTOPE THERMOELECTRIC GENERATOR SYSTEM

Determination of selected physical properties of ATJ and Carb-I-Tex graphite has been completed.

Dynamic tests on the first ATJ four-module fuel block were terminated by block failure at 29.1 g rms.

Effective thermal conductivity was measured across the three capsule components in vacuum, argon and helium.

Candidate emittance coatings were selected for further evaluation.

Fuel capsule baseline design was established.

A four-module generator configuration was selected for the demonstration system.

The problem of large temperature gradients in heater blocks was resolved.

Module S/N 001 completed life test and module S/N 004 test was terminated. Testing continues on the other three piston-heat sink modules.

The primary and backup heat pipe designs were selected.

Heat pipe dynamic environmental tests were completed.

An analysis of the effects of a nonoperating heat pipe on system performance was completed.

A design study on RTG separation springs was made to decrease system weight.

B. SYSTEM EVALUATION AND INTEGRATION

Revision A-4 to the SNAP 29 Program Plan (MND2062-500, Revision A) was coordinated and delivered to the AEC during this reporting period.

Fourteen test series were completed during this period, and three tests are in progress.

C. RELIABILITY

A preliminary prediction of the electric power output at the end of life was completed for the four-module configuration.

A degradation rate was estimated for thermoelectric power.

The current status of the electric heater failure analysis program is presented.

A reliability assessment of the thermal control subsystem three-shutter assembly has been prepared based on the random vibration test.

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D. AEROSPACE GROUND EQUIPMENT

Due to program redirection, effort on AGE design has been deferred until later in the program.

E. SAFETY EVALUATION AND INTEGRATION

The first draft of the Preliminary Safety Analysis Report was completed.

Trajectory and heating data for various re-entry modes were determined.

The test report for the hypersonic aeroheating wind tunnel test for two- and four-module fuel blocks was completed.

The water jet impact tests were completed.

F. MOUND LABORATORY EFFORT IN SUPPORT OF SNAP 29

Six of the original H-capsules were examined metallographically.

A new series of compatibility tests at 1100° C has been initiated.

Several experiments to determine particle size of material effusing from fuel forms have been carried out.

Gadolinium polonide vapor pressure has been measured at 1600° C.

A sealed can method for loading polonium into matrices was proved feasible.

An automatic technique for the fabrication of matrices was tested.

A 9 x 9 x 1-inch ATJ graphite block was clad with nickel, using nickel carbonyl.

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I. INTRODUCTION

The purpose of the SNAP 29 program is to develop and demonstrate a radio-isotope thermoelectric generator (RTG) system that meets the following objectives:

- (1) The Radioisotope Thermoelectric Technology by fabricating and electrically operating, for 144 days, two groundbased RTG demonstration type units which utilize a modular concept design that is capable of intact heat source re-entry.
- (2) The Aerospace Ground Equipment Technology by fabricating and operating AGE demonstration units.

The SNAP 29 radioisotope thermoelectric generator (RTG) system is made up of:

- (1) A heat source subsystem
- (2) Thermoelectric module subsystem
- (3) Thermal control subsystem
- (4) Heat rejection subsystem
- (5) Structures subsystem.

Integrated aerospace ground equipment (AGE) is provided for transportation, storage, installation of the isotopic heat source into the prototype RTG on a simulated launch pad and system checkout.

The SNAP 29 development program consists of five major tasks:

- (1) Radioisotope Thermoelectric Generator System--Control Point 1000
- (2) Aerospace Ground Equipment System--Control Point 2000
- (3) Program Management and Control--Control Point 3100
- (4) Systems Evaluation and Integration--Control Point 3200
- (5) Safety Evaluation and Integration--Control Point 3300.

Chapter II presents the status of development for the radioisotope thermoelectric generator system (Control Point 1000). The RTG system consists of six subsystems:

- (1) CP 1100--RTG Heat Source (Isotopic)
- (2) CP 1200--Heat Source (Electrical)
- (3) CP 1300--Thermoelectric Modules
- (4) CP 1500--Thermal Control
- (5) CP 1800--Heat Pipe Heat Rejection
- (6) CP 1900--Structures.

Chapter III relates to systems evaluation and integration (Control Point 3200).

Reliability is presented in Chapter IV.

Chapter V concerns the development of the aerospace ground equipment (CP 2000).

Safety evaluation and integration (CP 3300) are treated under Chapter VI.

Chapter VII represents the Mound Laboratory effort in support of SNAP 29.

Figure I-1 represents the present schedule as proposed in the current program plan (MND-2062-500, Rev A-4).

FIG. I-1. SNAP 29 MILESTONE SCHEDULE

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II. RADIOISOTOPE THERMOELECTRIC GENERATOR SYSTEM-- CONTROL POINT 1000

The SNAP 29 RTG system goal is to demonstrate the radioisotope thermoelectric technology by fabricating and electrically operating, for 144 days, two ground-based RTG demonstration units which utilize a modular concept design. The heat source shall be capable of intact re-entry.

The system design life of 144 days includes a 90-day duration orbital mission. In order to more effectively accomplish the design, a control point system has been established which breaks the RTG into its major subsystems. The control point breakdown is defined below.

- CP 1100--Isotopic Heat Source Subsystem
- CP 1200--Electrical Heat Source Subsystem
- CP 1300--Thermoelectric Module Subsystem
- CP 1500--Thermal Control Subsystem
- CP 1800--Heat Pipe Heat Rejection Subsystem
- CP 1900--Structures, Instrumentation and Shielding Subsystem

The first phase of the program consists of design and development testing on a subsystem level in order to generate sufficient data to establish required design modifications prior to initiation of the second phase (design verification testing). On the basis of design verification tests, further design refinements will be effected in order to optimize the system design toward the stated goal. These changes will appear in the demonstration system configuration, which will demonstrate the capability of the RTG on a system level and affirm the applicability of the system to flight hardware.

A. SUMMARY OF SIGNIFICANT ACCOMPLISHMENTS

1. CP 1100--Isotopic Heat Source Subsystem

- (1) Determination of selected physical properties of ATJ and Carb-I-Tex graphite has been completed.
- (2) Work was continued on cladding of the fuel block for oxidation protection.
- (3) The graphite unit cell flexural tests were completed.
- (4) Dynamic tests on the first ATJ four-module fuel block were terminated by block failure at 29.1 g rms.
- (5) Effective thermal conductivity was measured across the three capsule components in vacuum, argon and helium.
- (6) Candidate emittance coatings were selected for further evaluation.
- (7) Fuel capsule baseline design was established.
- (8) Elevated temperature tests were performed on capsule liner material.
- (9) A four-module generator configuration was selected for the demonstration system.

2. CP 1200--Electrical Heat Source Subsystem

- (1) An analysis was accomplished on heater failures to date.
- (2) The problem of large temperature gradients in heater blocks was resolved.
- (3) Design of the design verification electrical heat source was started.

3. CP 1300--Thermoelectric Module Subsystem

- (1) Module S/N 001 completed life test and module S/N 004 test was terminated. Testing continues on the other three piston-heat sink modules.
- (2) Testing was terminated on the diaphragm module due to leaks and low performance. Diagnostic disassembly was performed.
- (3) Thermoelectric couple analysis was performed on the latest batches of 3M elements, showing a decrease in efficiency.

4. CP 1500--Thermal Control Subsystem

- (1) All design verification system drawings have been completed and released, with the exception of the system assembly and installation drawing.

5. CP 1800--Heat Pipe Heat Rejection System

- (1) The primary and backup heat pipe designs were selected.
- (2) Cleaning procedures for nickel heat pipes were evaluated.
- (3) Heat pipe dynamic environmental tests were completed.
- (4) Life and performance tests of heat pipe configurations continued.
- (5) An analysis of the effects of a nonoperating heat pipe on system performance was completed.

6. CP 1900--RTG Housing Structural Design

- (1) The design of the component development housing is in progress.
- (2) A design study on RTG separation springs was made to decrease system weight.
- (3) A launch pad abort separation study was accomplished.

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B. RTG HEAT SOURCE (ISOTOPIC) SUBSYSTEM--CONTROL POINT 1100

The objective of this task is to design, develop material, fabricate, inspect, and test an isotopic heat source capable of meeting the following requirements:

- (1) To provide sufficient thermal energy to the thermoelectric modules subsystem for the generation of the required power
- (2) To be capable of intact re-entry of the fuel capsules
- (3) To provide full containment as required to meet safety criteria.

The development program for the heat source will consist of the following sequential phases:

CP 1140--Materials and Component Development

CP 1130--Design Verification

CP 1120--Demonstration Unit

1. Summary of Accomplishments

a. Fuel block

(1) Material properties determination

Determination of selected physical properties of ATJ and Carb-I-Tex (Grade 500, cross-needled graphite) has been completed during this period. The properties determined on each of the two candidate graphites included emissivity by reflectance and calorimetric techniques, thermal expansion, thermal conductivity, specific heat and density. Data have been obtained on specimens prepared parallel and perpendicular to the grain direction and at elevated temperatures as high as 2500° F.

Thermal conductivity. The thermal conductivity of both graphites parallel and perpendicular to the grain was determined from room temperature to 1800° F after steady state conditions were obtained throughout the test configuration. Data were computed from the balance of heat input, the heat flux through the graphite test samples and the heat losses. Results for each of the measurements performed are presented in Figs. II-1 and II-2.

Two significant conclusions are readily obtained after a comparison is made between the data of the two graphites. First, the thermal conductivity of ATJ graphite is approximately one order of magnitude greater than that of Carb-I-Tex graphite over the same temperature range. Second, the thermal conductivity of both graphites parallel to the grain is slightly larger than values perpendicular to the grain.

Linear thermal expansion. The thermal expansion of ATJ and Carb-I-Tex graphites has been measured from room temperature to 2000° F. Expansion studies were made with a Leitz HTV dilatometer which permanently recorded on photographic film the expansion traces as a function of temperature. The results of the experimental measurements as a function of grain direction and temperature are presented in Figs. II-3 and II-4.

The linear thermal expansion perpendicular to the grain of both graphites is greater than those values parallel with the grain. It should also be noted that, in the case of Carb-I-Tex 500 graphite, there is a marked change in the slope of the thermal expansion curve above 1800° F, which is probably related to the presence of the cross-needled fibers.

FIG. II-1. THERMAL CONDUCTIVITY OF CARB-I-TEX-500 GRAPHITE

FIG. II-2. THERMAL CONDUCTIVITY OF ATJ GRAPHITE

FIG. II-3. LINEAR THERMAL EXPANSION OF CARB-I-TEX-500 GRAPHITE

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FIG. II-4. LINEAR THERMAL EXPANSION OF ATJ GRAPHITE

Specific heat. The heat content of Carb-I-Tex and ATJ graphites has been measured up to 2400° F. From the heat content functions, the equations defining the specific heat (cal/gm-°C) were derived. An adiabatic ice calorimeter was used to measure the heat content of the graphites when a specimen at temperature was dropped into the calorimeter and ice in equilibrium with water was melted. Volume changes of ice melting to water was measured by weighings of the mercury required to compensate for the volume changes. The results of the heat content measurements are presented in Figs. II-5 and II-6.

Heat content, and therefore specific heat of graphite, is the same regardless of configuration, direction, type, etc. The only item which could alter the specific heat of essentially pure graphite is the presence of a foreign compound or material, such as the cross-needled material. As can be seen from the two heat content functions of the tested graphites, values at each temperature are almost the same with a slight experimental difference noted in the Carb-I-Tex function between 200° and 300° C. However, this resulted in only a slight difference between specific heat values of the two graphites calculated from their derived specific heat equations. The specific heat equation for Carb-I-Tex graphite, applicable between room temperature and

2400° F is $C_p \text{ (cal/gm-°F)} = 3.13 \times 10^{-2} T^{0.276}$ and, for ATJ graphite over the same temperature range, is $C_p = 1.69 \times 10^{-2} T^{0.384}$. From these two equations, the specific heat at a given temperature can be calculated for each material.

Density. The average density of the tested ATJ graphite samples is 1.79 gm/cm³ and an average value of 1.35 gm/cm³ was measured for Carb-I-Tex graphite. The experimental measurement deviation was found to be ±1%.

Spectral emittance. Two independent methods were utilized in the measurement of the emittance of ATJ and Carb-I-Tex graphite. The first used approximately one-inch disks involving room temperature measurements on the Gier Dunkle apparatus. From Planck's law, the emissivity at 500°, 1000°, 1600° and 2500° F was calculated from the spectral reflectance curves. The second technique utilized total hemispherical cups on which the emissivity was directly measured at 1600° and 2000° F under vacuum.

The emissivity of carbon usually varies between 0.7 and 0.9, depending upon the surface characteristics of the tested samples. Assuming that both the ATJ and Carb-I-Tex disks and cups received the same surface preparation and treatment, the emissivity values obtained from both techniques for Carb-I-Tex are higher than those of ATJ graphite. Total hemispherical cup values of Carb-I-Tex graphite were 0.92 and 0.85 at 1600° and 2000° F, respectively. ATJ graphite values were 0.86 and 0.84 at 1600° and 2000° F, respectively.

A summary of all of the specific experimental data for each of the temperatures tested is presented in Table II-1.

During the next reporting period, a comparison of these physical property data with data from other sources will be completed and a standardized set of physical property data will be established for all future analysis work with graphite materials.

The status of the mechanical properties testing program was discussed in the Fifth Quarterly Progress Report. During this period, the graphite flexure test program was completed and a comparison of mechanical property data for ATJ graphite with other sources was initiated. During the next period, a test report on the flexure test program will be completed and the comparative study will be completed. Subsequently, as in the case of the physical properties, a standard set of mechanical property data will be established for all future program use.

FIG. II-5. HEAT CONTENT OF CARB-I-TEX GRAPHITE

FIG. II-6. HEAT CONTENT OF ATJ GRAPHITE

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TABLE II-1

Physical Property Data of ATJ and Carb-I-Tex Graphites

Emissivity (Gier Dunkle)

	<u>500° F</u>	<u>1000° F</u>	<u>1600° F</u>	<u>2500° F</u>
ATJ	0.62, 0.67	0.68, 0.71	0.71, 0.74	0.75, 0.77
Carb-I-Tex	0.80, 0.82	0.84, 0.86	0.86, 0.88	0.88, 0.89

Emissivity (total hemispherical)

	<u>1600° F</u>	<u>2000° F</u>
ATJ	0.89, 0.83, 0.86	0.84, 0.84
Carb-I-Tex	0.92, 0.92	0.85, 0.86

Thermal Expansion ($\alpha \times 10^{-6}$) (mm/mm-° F)

	<u>500° F</u>	<u>1000° F</u>	<u>1600° F</u>	<u>2000° F</u>
ATJ (parallel)	1.3, 1.2	1.6, 1.6	1.7, 1.8	1.7, 1.8
(perpendicular)	1.8, 1.7	2.1, 2.0	2.2, 2.2	2.2, 2.3
Carb-I-Tex (parallel)	0.71, 1.1	0.92, 1.1	1.1, 1.2	1.1, 1.1
(perpendicular)	1.7, 1.6	1.8, 1.9	2.0, 2.0	2.0, 2.1

Thermal conductivity (Btu/ft-hr-° F)

	<u>800° F</u>	<u>1600° F</u>	<u>1800° F</u>
ATJ (parallel)	54, 48	30, 32	27, 28
(perpendicular)	37, 42.5	24, 26	23, 24
Carb-I-Tex (parallel)	6.2, 6.0	7.3, 7.6	7.8, 8.0
(perpendicular)	3.2, 3.4	4.5, 3.4	4.0, 4.2

Heat content (cal/gm)

	<u>1000° F</u>	<u>1600° F</u>	<u>2400° F</u>
ATJ	154, 157	280, 274	451, 457
Carb-I-Tex	163, 166, 167	273, 282, 285	434, 439, 431

Density (gm/cm³)

ATJ	1.81, 1.77, 1.80 (room temp)
Carb-I-Tex	1.35, 1.33, 1.36 (room temp)

The mechanical properties of ATJ graphite, i.e., elastic modulus (tensile and compressive), ultimate tensile strength, flexure strength, shear strength and compressive strength, are a function of temperature. The data presented here were compiled in an effort to determine average mechanical properties and an expected range for each of these properties at any temperature.

Literature on ATJ graphite provides an extensive listing of its mechanical properties. Unfortunately, consistency of results is not widespread. Typical data have been collected and the range of data given in each of the referenced reports is presented. These data are recorded at room temperature, 800°C (1472°F), 1200°C (2192°F), 1600°C (2912°F), 2000°C (3632°F), 2400°C (4352°F) and 2800°C (5072°F). The data from all of the literature were averaged and are compiled in Table II-2. The average data of each property, along with the range in data, as a function of temperature, have been plotted on Figs. II-7 through II-12. The properties plotted are elastic modulus in tension, elastic modulus in compression, ultimate tensile strength, flexure strength, shear strength and compressive strength, respectively. Average properties as found in the literature are compiled on Table II-3 through II-9 for increasing temperatures. Table II-10 gives the referenced literature used in compiling Tables II-3 through II-9. Data are presented in both the with-grain direction and the across-grain direction. Based upon flexure specimens tested at Martin Marietta at 1500°F, the flexure stresses are calculated. A complete set of flexure test data will be incorporated upon completion of the flexure test report.

A large amount of scatter was observed in most of the data averaged from the literature. Scatter of +15% of the average property was not uncommon. In some cases, scatter exceeded +50% of the average property at a given temperature. A small amount of scatter, e.g., 5%, can be attributed to experimental error. The large amount of scatter, corrected for this error, indicates that consistent mechanical properties of ATJ graphite are not to be expected.

(2) Fuel block oxidation protection system

During the past quarter, the Martin Marietta effort in developing an oxidation-resistant fuel block cladding was centered on a nickel coating which was applied by air spraying a lacquer suspension of nickel powder followed by vacuum sintering at 2375°F. The nickel cladding was then sealed by one of two approaches. The first was the direct application of a layer of one of the Coast Metal alloys which are nickel-base brazing alloys containing Si and sometimes Cr. The second approach was the brazing of a nickel foil to the sintered nickel layer using a Coast Metal alloy.

A summary of the specimens processed during the past quarter is given in Table II-11.

The following conclusions have been drawn from the results summarized in Table II-11:

- (1) The use of Coast Metal 53 and Coast Metal 60, which contain a significant amount of Cr, causes embrittlement of the nickel cladding, probably by diffusion of Cr into it. All further work will be done using Coast Metal 52, which contains no Cr.
- (2) The approach of sealing the sprayed nickel layer with a brazed nickel foil presents a difficult problem in sealing the edges and corners of the block. This approach has therefore been abandoned.

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TABLE II-2
Average Mechanical Properties of ATJ Graphite

		Room Temperature		800° C		1200° C		1600° C		2000° C		2400° C		2800° C	
		WG	AG	WG	AG	WG	AG	WG	AG	WG	AG	WG	AG	WG	AG
Elastic modulus (tension) (10 ⁶ psi)	Average	1.60	1.15	1.69	1.21	1.82	1.26	1.95	1.35	1.95	1.38	1.76	1.38	1.47	0.99
	High	1.82	1.20	1.82	1.22	2.01	1.31	2.22	1.46	2.28	1.57	2.23	1.56	2.13	1.47
	Low	1.45	1.09	1.56	1.19	1.62	1.21	1.68	1.23	1.67	1.20	1.30	1.20	0.82	0.50
	Range	0.37	0.11	0.26	0.03	0.39	0.10	0.54	0.23	0.55	0.37	0.93	0.36	1.31	0.97
Elastic modulus (compression) (10 ⁶ psi)	Average	1.46	1.04	1.66	1.21	2.01	1.31	2.22	1.46	2.27	1.58	2.24	1.55	2.13	1.47
	High	2.00	1.22	1.89	1.25	2.10	1.33	2.31	1.51	2.33	1.61	2.34	1.58	2.30	1.49
	Low	1.24	0.70	1.21	1.14	1.95	1.20	2.17	1.38	2.24	1.54	2.18	1.49	2.00	1.45
	Range	0.76	0.52	0.68	0.11	0.15	0.13	0.14	0.13	0.09	0.07	0.16	0.09	0.30	0.04
Ultimate tensile strength (psi)	Average	3,785	3,205	4,330	3,170	4,515	3,235	5,010	3,730	5,710	4,065	6,930	5,115	4,745	4,380
	High	4,780	4,010	5,000	3,500	5,550	4,300	5,820	4,600	6,600	4,550	8,130	6,100	7,030	5,515
	Low	2,380	2,626	3,630	3,040	3,760	2,745	3,740	3,640	4,310	3,900	5,400	4,180	2,935	2,765
	Range	2,400	1,384	1,370	460	1,790	1,555	2,080	960	2,290	650	2,730	1,920	4,095	2,760
Flexure strength (psi)	Average	4,060	3,640	4,780	3,780	4,780	3,950	5,050	4,150	6,050	4,800	8,300	7,200	8,200	8,150
	High	5,700	4,180	5,560	--	5,570	4,450	5,150	4,500	6,300	5,200	9,800	7,750	8,900	8,500
	Low	2,170	3,210	3,880	--	4,020	3,650	4,050	3,550	5,500	3,900	6,600	6,750	7,400	7,300
	Range	3,530	970	1,680	--	1,550	800	1,100	950	800	1,300	3,200	1,000	1,500	1,200
Shear strength (psi)	Average	1,530	1,835	2,400	2,180	2,895	2,770	3,675	2,795	5,050	4,060	7,235	5,755	8,980	6,910
	High	1,550	1,850	--	--	4,095	3,105	4,250	2,900	--	4,080	7,620	5,925	9,400	7,100
	Low	1,505	1,820	--	--	1,375	2,430	2,785	2,690	--	4,035	6,850	5,580	8,560	6,725
	Range	45	30	--	--	2,720	675	1,465	210	--	45	770	345	840	375
Compressive strength (psi)	Average	9,005	8,685	10,845	8,275	10,720	8,355	11,050	10,035	10,510	10,160	16,515	16,125	32,735	27,390
	High	12,100	12,190	12,540	10,550	11,200	11,100	11,205	12,250	12,900	14,300	18,845	17,500	43,500	30,000
	Low	6,700	5,670	9,500	6,000	10,200	6,465	10,750	8,045	8,750	6,315	14,500	14,750	21,500	25,975
	Range	5,400	6,520	3,040	4,500	1,000	4,635	455	4,205	4,150	7,985	4,345	2,750	22,000	4,025

FIG. II-7. ELASTIC MODULUS IN TENSION OF ATJ GRAPHITE

FIG. II-8. ELASTIC MODULUS IN COMPRESSION OF ATJ GRAPHITE

FIG. II-9. ULTIMATE TENSILE STRENGTH OF ATJ GRAPHITE

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FIG. II-10. FLEXURE STRENGTH OF ATJ GRAPHITE

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FIG. II-11. SHEAR STRENGTH OF ATJ GRAPHITE

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FIG. II-12. COMPRESSIVE STRENGTH OF ATJ GRAPHITE

TABLE II-3

Mechanical Properties of ATJ Graphite at Room Temperature

	Reference	[1]		[2]		[3]		[4]		[6]		[8]		[9]	
		WG	AG	WG	AG	WG	AG	WG	AG	WG	AG	WG	AG	WG	AG
Elastic modulus (tension) (10 ⁶ psi)	Average	--	--	--	--	1.45	1.15	1.65	1.17	--	--	1.70	1.09	1.59	1.17
	High	--	--	--	--	--	--	--	--	--	--	--	--	1.82	1.20
	Low	--	--	--	--	--	--	--	--	--	--	--	--	1.52	1.11
	Range	--	--	--	--	--	--	--	--	--	--	--	--	0.30	0.09
Elastic modulus (compression) (10 ⁶ psi)	Average	1.45 ⁽¹⁾	1.15 ⁽¹⁾	1.29	0.81	--	--	1.72	1.22	--	--	--	--	1.39	0.98
	High	--	--	1.33	0.91	--	--	--	--	--	--	--	--	2.00	1.16
	Low	--	--	1.24	0.70	--	--	--	--	--	--	--	--	1.22	0.92
	Range	0.166 ⁽¹⁾	0.95 ⁽¹⁾	0.09	0.21	--	--	--	--	--	--	--	--	0.78	0.24
Ultimate tensile strength (psi)	Average	1790 ⁽⁴⁾ 3355	1420 ⁽⁴⁾ 2935	3670	2,860	4300	3,150	3,850	3800	3845	3045	3500	2940	3,985	3,710
	High	--	--	4720	3,190	4450	3,250	--	--	4370	3140	3700	--	4,780	4,010
	Low	--	--	2380	2,626	4200	3,080	--	--	3320	2950	3300	--	3,290	3,515
	Range	225 ⁽⁴⁾ 379	226 ⁽⁴⁾ 117	2340	564	250	170	--	--	1050	190	400	--	1,490	495
Flexure strength (psi)	Average	1505	1820	--	--	--	--	--	--	--	--	1550	1850	--	--
	High	--	--	--	--	--	--	--	--	--	--	--	--	--	--
	Low	--	--	--	--	--	--	--	--	--	--	--	--	--	--
	Range	389	250	--	--	--	--	--	--	--	--	--	--	--	--
Shear strength (psi)	Average	4010	3580	4150	3,675	4000	3,700	--	--	4080	3595	--	--	--	--
	High	--	--	5700	4,180	4650	3,900	--	--	4155	--	--	--	--	--
	Low	--	--	2170	3,210	3700	3,500	--	--	4000	--	--	--	--	--
	Range	773	484	3530	970	950	400	--	--	155	--	--	--	--	--
Compressive strength (psi)	Average	8270 ⁽²⁾ 6700 ⁽³⁾	8540 ⁽²⁾ 5670 ⁽³⁾	9340	9,560	8800	10,300	11,400	9150	--	--	7000	5750	11,530	11,825
	High	--	--	9440	10,260	--	--	--	--	--	--	--	--	12,100	12,190
	Low	--	--	9270	8,800	--	--	--	--	--	--	--	--	10,880	11,600
	Range	1028 ⁽²⁾ 306 ⁽³⁾	1144 ⁽²⁾ 724 ⁽³⁾	170	1,460	--	--	--	--	--	--	--	--	1,220	590

Notes:

- (1) Not specified compression or tension; assumed compression
 (2) 1 x 1 x 1-inch sample
 (3) 1/2 ϕ x 1/2 sample
 (4) Briquet sample

TABLE II-4
Mechanical Properties of ATJ Graphite at 800° C (1472° F)

Reference		[1]		[2]		[3]		[5]		[6]		[7]		[8]		[10]	
		WG	AG	WG	AG	WG	AG	WG	AG	WG	AG	WG	AG	WG	AG	WG	AG
Elastic modulus (tension) (10 ⁶ psi)	Average					1.56	1.19					1.06 ⁽³⁾		1.82	1.22		
	High											1.15 ⁽³⁾					
	Low											1.01 ⁽³⁾					
	Range											0.14 ⁽³⁾					
Elastic modulus (compression) (10 ⁶ psi)	Average	1.79 ⁽¹⁾	1.21 ⁽¹⁾					1.53									
	High	1.80 ⁽¹⁾	1.25 ⁽¹⁾					1.89									
	Low	1.77 ⁽¹⁾	1.14 ⁽¹⁾					1.21									
	Range	0.03 ⁽¹⁾	0.11 ⁽¹⁾					0.68									
Ultimate tensile strength (psi)	Average					4800 ⁽²⁾	3,300 ⁽²⁾			4200	3165			3,990	3040		
	High					5000 ⁽²⁾	3,500 ⁽²⁾			4770	3275			4,350			
	Low					4600 ⁽²⁾	3,180 ⁽²⁾			3630	3050			3,630			
	Range					400 ⁽²⁾	320 ⁽²⁾			1140	125			720			
Flexure strength (psi)	Average			4105 ⁽²⁾						4825	3780	5370 ⁽³⁾				4815 ⁽²⁾	
	High			4310 ⁽²⁾						5300		5510 ⁽³⁾				5560 ⁽²⁾	
	Low			3900 ⁽²⁾						4345		4990 ⁽³⁾				3880 ⁽²⁾	
	Range			410 ⁽²⁾						955		520 ⁽³⁾				1680 ⁽²⁾	
Shear strength (psi)	Average													2,400	2180		
	High Low Range																
Compressive strength (psi)	Average					9500	10,550	12,538						10,500	6000		
	High Low Range							13,400 11,151 2,249									

Notes:

- (1) Not specified compression or tension; assumed compression at 600° C (1112° F)
 (2) At 816° C (1500° F)
 (3) At 732° C (1350° F)

TABLE II-5

Mechanical Properties of ATJ Graphite at 1200° C (2192° F)

Reference		[1]		[2]		[3]		[6]		[8]	
		WG	AG	WG	AG	WG	AG	WG	AG	WG	AG
Elastic modulus (tension) (10^6 psi)	Average High Low Range					1.62	1.21			2.01	1.31
Elastic modulus (compression) (10^6 psi)	Average High Low Range	2.01 ⁽¹⁾ 2.10 ⁽¹⁾ 1.95 ⁽¹⁾ 0.15 ⁽¹⁾	1.31 ⁽¹⁾ 1.33 ⁽¹⁾ 1.28 ⁽¹⁾ 0.05 ⁽¹⁾								
Ultimate tensile strength (psi)	Average High Low Range	3,760 ⁽²⁾	2745 ⁽²⁾	4522 ⁽²⁾ 4685 ⁽²⁾ 4358 ⁽²⁾ 327 ⁽²⁾	3308 ⁽²⁾	5,400 ⁽³⁾ 5,550 ⁽³⁾ 3,850 ⁽³⁾ 1,700 ⁽³⁾	3,650 ⁽³⁾ 4,300 ⁽³⁾ 4,100 ⁽³⁾ 200 ⁽³⁾	4575 5175 3970 1205	3315 3455 3175 280	4,325 4,680 3,970 710	3160
Flexure strength (psi)	Average High Low Range			4800 ⁽²⁾ 4795 ⁽³⁾ 5570 ⁽³⁾ 4020 ⁽³⁾ 1550 ⁽³⁾		4,750 ⁽³⁾ 5,050 ⁽³⁾ 4,650 ⁽³⁾ 400 ⁽³⁾	3,950 ⁽³⁾ 4,450 ⁽³⁾ 3,650 ⁽³⁾ 800 ⁽³⁾				
Shear strength (psi)	Average High Low Range	2,735 ⁽²⁾ 4,095 ⁽²⁾ 1,375 ⁽²⁾ 2,720 ⁽²⁾	3105 ⁽²⁾							3,050	2430
Compressive strength (psi)	Average High Low Range	10,755 ⁽²⁾ 10,815 ⁽²⁾ 10,695 ⁽²⁾ 110 ⁽²⁾	6465 ⁽²⁾			10,200	11,100			11,200	7500

Notes:

(1) Not specified compression or tension; assume compression

(2) At 1000° C (1832° F)

(3) At 1371° C (2500° F)

TABLE II-6

Mechanical Properties of ATJ Graphite at 1600° C (2912° F)

Reference		[1]		[2]		[3]		[8]	
		WG	AG	WG	AG	WG	AG	WG	AG
Elastic modulus (tension) (10 ⁶ psi)	Average High Low Range					1.68	1.23	2.22	1.46
Elastic modulus (compression) (10 ⁶ psi)	Average High Low Range	2.22 ⁽¹⁾ 2.31 ⁽¹⁾ 2.17 ⁽¹⁾ 0.14 ⁽¹⁾	1.46 ⁽¹⁾ 1.51 ⁽¹⁾ 1.38 ⁽¹⁾ 0.13 ⁽¹⁾						
Ultimate tensile strength (psi)	Average High Low Range	3,740 ⁽²⁾	3770 ⁽²⁾	5820 4910 ⁽²⁾ 5240 ⁽²⁾ 4500 ⁽²⁾ 740 ⁽²⁾	3789 ⁽³⁾ 3730 ⁽²⁾ 3800 ⁽²⁾ 3640 ⁽²⁾ 160 ⁽²⁾	5,800 ⁽⁴⁾ 5,750 ⁽⁴⁾ 4,900 ⁽⁴⁾ 850 ⁽⁴⁾	3,900 ⁽⁴⁾ 4,600 ⁽⁴⁾ 3,900 ⁽⁴⁾ 700 ⁽⁴⁾	4,775 5,000 4,550 450	3470
Flexure strength (psi)	Average High Low Range					5,050 ⁽⁴⁾ 5,150 ⁽⁴⁾ 4,050 ⁽⁴⁾ 1,100 ⁽⁴⁾	4,150 ⁽⁴⁾ 4,500 ⁽⁴⁾ 3,550 ⁽⁴⁾ 950 ⁽⁴⁾		
Shear strength (psi)	Average High Low Range	3,515 ⁽²⁾ 4,250 ⁽²⁾ 2,785 ⁽²⁾ 1,465 ⁽²⁾	2690 ⁽²⁾					3,830	2900
Compressive strength (psi)	Average High Low Range	11,205 ⁽²⁾	9005 ⁽²⁾ 9965 ⁽²⁾ 8045 ⁽²⁾ 1920 ⁽²⁾			11,200	12,250	10,750	8850

Notes:

(1) Not specified compression or tension; assumed compression

(2) At 1500° C (2732° F)

(3) At 1800° C (3272° F)

(4) At 1649 (3000° F)

TABLE II-7

Mechanical Properties of ATJ Graphite at 2000° C (3632° F)

Reference		[1]		[2]		[3]		[8]	
		WG	AG	WG	AG	WG	AG	WG	AG
Elastic modulus (tension) (10^6 psi)	Average High Low Range					1.67	1.20	2.28	1.57
Elastic modulus (compression) (10^6 psi)	Average High Low Range	2.27 ⁽¹⁾ 2.33 ⁽¹⁾ 2.24 ⁽¹⁾ 0.09 ⁽¹⁾	1.58 ⁽¹⁾ 1.61 ⁽¹⁾ 1.54 ⁽¹⁾ 0.007 ⁽¹⁾						
Ultimate tensile strength (psi)	Average High Low Range	5630 6190 5065 1125	4,070	5382 5641 ⁽²⁾ 6586 6465 ⁽²⁾ 4697 4310 ⁽²⁾ 1889 2155 ⁽²⁾	4158	6,350 ⁽³⁾ 6,600 ⁽³⁾ 5,300 ⁽³⁾ 1,300 ⁽³⁾	3,950 ⁽³⁾ 4,550 ⁽³⁾ 3,900 ⁽³⁾ 650 ⁽³⁾	5545 5750 5340 410	4080
Flexure strength (psi)	Average High Low Range			6300		5,800 ⁽³⁾ 5,850 ⁽³⁾ 5,500 ⁽³⁾ 350 ⁽³⁾	4,800 ⁽³⁾ 5,200 ⁽³⁾ 3,900 ⁽³⁾ 1,300 ⁽³⁾		
Shear strength (psi)	Average High Low Range	5050	4,035					5050	4080
Compressive strength (psi)	Average High Low Range	9875 9880 9870 10	8,180 10,045 6,315 3,730			12,900	14,300	8750	8000

Notes:

(1) Not specified compression or tension; assumed compression

(2) At 2200° C (3992° F)

(3) At 1927° C (3500° F)

TABLE II-8
Mechanical Properties of ATJ Graphite at 2400° C (4352° F)

	Reference	[1]		[2]		[3]		[8]	
		WG	AG	WG	AG	WG	AG	WG	AG
Elastic modulus (tension) (10 ⁶ psi)	Average High Low Range					1.30	1.20	2.23	1.56
Elastic modulus (compression) (10 ⁶ psi)	Average High Low Range	2.24 ⁽¹⁾ 2.34 ⁽¹⁾ 2.18 ⁽¹⁾ 0.16 ⁽¹⁾	1.55 ⁽¹⁾ 1.58 ⁽¹⁾ 1.49 ⁽¹⁾ 0.09 ⁽¹⁾						
Ultimate tensile strength (psi)	Average	6,575 ⁽²⁾	5100 ⁽²⁾	7185	5012	7,200 ⁽³⁾	5,900 ⁽³⁾	5,975	5,020
	High	7,990 ⁽²⁾	5920 ⁽²⁾	7715 ⁽²⁾ 7550	4540 ⁽²⁾	7,850 ⁽³⁾	6,100 ⁽³⁾	6,550	
	Low	5,090 ⁽²⁾	4180 ⁽²⁾	8130 ⁽²⁾ 6815		6,450 ⁽³⁾	5,700 ⁽³⁾	5,400	
	Range	2,900 ⁽²⁾	1740 ⁽²⁾	6900 ⁽²⁾ 735 1230 ⁽²⁾		1,400 ⁽³⁾	400 ⁽³⁾	1,150	
Flexure strength (psi)	Average			8400 ⁽²⁾		8,200 ⁽³⁾	7,200 ⁽³⁾		
	High					9,800 ⁽³⁾	7,750 ⁽³⁾		
	Low					6,600 ⁽³⁾	6,750 ⁽³⁾		
	Range					3,200 ⁽³⁾	1,000 ⁽³⁾		
Shear strength (psi)	Average High Low Range	7,620 ⁽²⁾	5925 ⁽²⁾					6,850	5,580
Compressive strength (psi)	Average High Low Range	18,845				16,200	17,500	14,500	14,750

Notes:

- (1) Not specified compression or tension; assumed compression
- (2) At 2500° C (4532° F)
- (3) At 2482° C (4500° F)

TABLE II-9

Mechanical Properties of ATJ Graphite at 2800° C (5072° F)

Reference		[1]		[2]		[3]		[8]	
		WG	AG	WG	AG	WG	AG	WG	AG
Elastic modulus (tension) (10^6 psi)	Average High Low Range					0.82	0.50	2.13	1.47
Elastic modulus (compression) (10^6 psi)	Average High Low Range	2.13 ⁽¹⁾ 2.30 ⁽¹⁾ 2.00 ⁽¹⁾ 0.30 ⁽¹⁾	1.47 ⁽¹⁾ 1.49 ⁽¹⁾ 1.45 ⁽¹⁾ 0.04 ⁽¹⁾						
Ultimate tensile strength (psi)	Average High Low Range	2,935 ⁽²⁾	4,490 ⁽²⁾ 5,515 ⁽²⁾ 2,765 ⁽²⁾ 2,750 ⁽²⁾	3010 7030 ⁽³⁾		5,600 ⁽⁴⁾ 5,900 ⁽⁴⁾ 5,200 ⁽⁴⁾ 700 ⁽⁴⁾	3,950 ⁽⁴⁾ 4,300 ⁽⁴⁾ 3,600 ⁽⁴⁾ 700 ⁽⁴⁾	5,150	4,700
Flexure strength (psi)	Average High Low Range					8,200 ⁽⁴⁾ 8,900 ⁽⁴⁾ 7,400 ⁽⁴⁾ 1,500 ⁽⁴⁾	8,150 ⁽⁴⁾ 8,500 ⁽⁴⁾ 7,300 ⁽⁴⁾ 1,200 ⁽⁴⁾		
Shear strength (psi)	Average High Low Range	8,650 ⁽²⁾	6,725 ⁽²⁾					9,400	7,100
Compressive strength (psi)	Average High Low Range	33,200 ⁽²⁾	25,975 ⁽²⁾			21,500	26,200	43,500	30,000

Notes:

- (1) Not specified compression or tension; assumed compression
 (2) At 2700° C (4892° F)
 (3) At 2600° C (4712° F)
 (4) At 2760° C (5000° F)

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TABLE II-10
Mechanical Properties References

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TABLE II-11
Summary of Specimens Processed this Quarter

Sample No. and Date	Materials Joined	Method of Joining (including temperature)	Remarks
27 12-67	Ni powder to graphite;* CM-52 over diffused powder layer.	Diffusion--Ni powder to graphite** ~0.013 inch thick at 2375° F; CM-52 ~0.005 inch thick brazed to diffused powder surface at 1950° F.	Failed after first cycle room temperature to 1600° F and held for 2 hr: (1) ATJ oxidized (wt loss ~300 gm out of ~1475 gm) (2) Possible slight crack in bottom
28 1-68	Ni powder to graphite;* CM-52 over diffused powder layer; CM-52 and Ni foil; seam-welding of foil.	Diffusion--Ni powder to graphite** ~0.010 inch thick at 2375° F; CM-52 ~0.005 inch thick brazed to diffused powder surface at 1925° F; CM-52 ~0.003 inch thick +0.002 inch Ni foil brazed to surface at 1925° F; seam-welding of foil with complete "canning" of block.	Failed during fabrication due to leak at a Ni foil seam weld.

TABLE II-11 (continued)

<u>Sample No. and Date</u>	<u>Materials Joined</u>	<u>Method of Joining (including temperature)</u>	<u>Remarks</u>
29 1-68	Ni powder to graphite;* CM-52 over diffused powder layer.	Diffusion--Ni powder to graphite** 0.010 inch at 2375° F; then CM-52 0.004 inch thick brazed to diffused powder surface at 1925° F.	Failed during fabrication due to separation of the Ni layer from graphite.
30 1-68	Ni powder to graphite;* CM-52 over diffused powder layer.	Diffusion--Ni powder to graphite** 0.010 inch thick at 2375° F; CM-52 0.002 inch thick brazed to diffused powder surface at 1925° F.	Successfully withstood thermal cycles to 1600° F + 14 days at 1600° F in air with weight loss of 5%.
30a 2-68 30b	Same as 30.	Same as 30.	Failed during sintering due to reaction of Ni coating with ZrO ₂ supports.
30c 30d	Same as 30.	Same as 30 but supported during sintering by graphite rod inserted into internal hole in block. Hole subsequently closed with graphite plug.	Failed during fabrication due to excessive diffusion of graphite through nickel.
31 1-68	Ni powder to graphite;* CM-52 and Ni foil and edge sections over diffused powder layer.	Diffusion--Ni powder to graphite**0.003 inch thick at 2375° F; 0.002 inch Ni foil brazed with 0.003 inch CM-52 at 1925° F; 0.002 inch Ni foil all edges, overlapping side foils brazed with CM-52 at 1925° F.	Successfully withstood 2 thermal cycles to 1600° F + 14 days at 1600° F in air with weight loss of 5%.
32 1-68	Ni powder to graphite; *CM-52 and Ni foil over diffused powder layer; plus coat of CM-52 sprayed to edges.	Diffusion--Ni powder to graphite** 0.003 inch thick at 2375° F; then 0.002 inch Ni foil brazed with 0.001 to 0.003 inch CM-52 at 1925° F; 0.002 inch CM-52 sprayed to edges and brazed at 1925° F.	Failed during fabrication due to formation of leak in foil.

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TABLE II-11 (continued)

<u>Sample No. and Date</u>	<u>Materials Joined</u>	<u>Method of Joining (including temperature)</u>	<u>Remarks</u>
33 2-68	Ni powder to graphite*	Diffusion--Ni powder to graphite** 0.030 inch thick at 2375° F.	Failed--bad cracks in coating on flat surfaces and edges. See EWO 66.
34 2-68	Ta-Mo powder to graphite;* CM-52 and Ni foil over powder layer.	0.0025 - 0.003 inch Ta powder plasma sprayed; 0.001 - 0.002 inch Mo powder plasma sprayed; 0.001 - 0.003 inch CM-52 Ni and 0.005 inch Ni foil brazed to both sides of block**.	Ta-Mo coating alone withstood 6 thermal cycles to 1600° F in vacuum without unbonding. During application of foil, leak formed in foil which was repaired. On thermal cycling in air, cladding separated apparently due to oxidation caused by another leak in Ni foil.
35 2-68	Ni powder to graphite;* CM-52 and Ni foil overhanging edges over diffused powder layer.	Diffusion--Ni powder to graphite**0.003 inch thick at 2375° F; 0.005 inch Ni foil brazed with 0.001 - 0.003 inch CM-52 at 1925° F; seam weld; add SS vacuum tube.	Failed during fabrication due to a leak in Ni foil.
37a	Same as 30.	Same as 30 except Ni sintering time reduced from 1 to 1/2 hr and Coast Metal brazing time reduced from 30 to 5 min.	Flexure tested at elevated temperature in as-fabricated condition.
37b	Same as 30.	Same as 37a.	Failed on first thermal cycle to 1600° F by separation of cladding from block followed by gross failure of cladding.
37c	Same as 30.	Same as 37a followed by grit blasting of one side with No. 100 chilled iron grit and sand blasting of other side with Al ₂ O ₃ .	Failed on first thermal cycle to 1600° F. Separation and failure of cladding occurred on both sides of block.
37d	Same as 30.	Same as 37a.	Completed 2 thermal cycles to 1600° F with weight loss of less than 1%. Currently on life test in air at 1600° F.

*Grain direction of graphic parallel with longitudinal direction

**Note: Prior to preparation of specimen, graphite block was outgassed.

- (3) The sinter-braze cycle used for blocks 37a through 37d develops a cladding sufficiently impervious and oxidation-resistant. Failures of this cladding are mainly caused by separation of the cladding from the block, which appears to be the result of differential thermal expansion stresses.

Results from the series No. 37c and 37d specimens point up the continuing problem of establishing a repeatable performance from blocks fabricated under the same process.

The results from specimen No. 34 indicate that the Ta-Mo substrate approach offers a potential solution to the relative thermal expansion problem in the sense that it may be a good intermediary between the graphite and nickel. Additional specimens are being fabricated for evaluation testing.

In order to evaluate the effect of nickel cladding on the fuel block structural behavior, some preliminary evaluation tests have been performed. Flexure tests performed on as-fabricated clad graphite blocks at room temperature resulted in failures at approximately half the equivalent strength of an unclad graphite block. Analysis indicated that since the nickel clad is applied at high temperature (~1900° F), a substantial tensile prestress exists in the nickel when the composite block cools down to room temperature. This residual stress results in a premature failure of the specimen under flexure testing. In order to verify this, a clad block was flexure tested at 1500° F to reduce residual stresses. This is more realistic, since block strength is required at ~1500° F during launch vibration, and structural performance is not critical at room temperature. The specimen failed at a stress level equivalent to that of bare graphite, verifying the significance of residual stresses. It is apparent that further development effort on block cladding must consider structural effects as well as oxidation protection capability.

(3) Graphite bonding and joints

Since fuel block dimensional requirements exceed the limits of ATJ graphite fabrication for a one-piece block, the fuel block must be manufactured from two pieces and joined. C-9 and C-34 cements, procured from Union Carbide, represent two potential adhesives. The basic joint design was chosen where specific details such as faying surface angle, adhesive thickness and groove radius, etc., were variable.

To gain insight to establish an optimum joint, a test program was initiated to provide a basis of final parameter selection. Reference is made to Martin Marietta NTD-1140-05 (including Addendums 1 and 2), entitled "ATJ Joint Optimization Test," dated September 14, 1967. All test specimens are beams loaded in flexure at two points. Phase 1 of the test program utilizes three solid beams without joints in order to establish a target load limit and a basis for calculating joint efficiencies. Phase 2 provides for investigation of 24 joint specimens. Adhesive thickness is varied between 0 and 0.030 inch and the faying surface angle is variable at 22.5° and 45°.

All tests were conducted at specimen temperatures of 1500° F.

Using a bar chart representation, Figs. II-13, II-14 and II-15 illustrate the average joint efficiencies for each combination of variables. From Fig. II-13, the thicker adhesive provides a greater joint efficiency by about 7 or 8%. It may be noted here that Union Carbide has indicated adhesive thicknesses of ~0.060 inch, at least for C-34, to obtain an efficient bond.

From Fig. II-14 the variation in faying surface angle does not seem important when observing the data of all samples. However, when considering only a thick adhesive layer, the 45° angle is predominant for both C-9 and C-34.

FIG. II-13. VARIATION OF ADHESIVE THICKNESS

FIG. II-14. VARIATION OF FAYING SURFACE ANGLE

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FIG. II-15. VARIATION OF ADHESIVE

Figure II-15 indicates that C-9 is superior to C-34.

In summation, test results indicate that greater joint efficiency is achieved by:

- (1) Thick adhesive layer
- (2) 45° faying surface angle
- (3) C-9 adhesive.

It has been recently pointed out that, by wetting the joint mating surfaces with binder when applying C-34, an increase in joint strength should be seen. As a result of this information, Phase 3 of the test program designates 19 additional specimens. Twelve of these specimens employ C-9 cement at thicknesses of 0.010 inch and 0.030 inch. The remaining seven specimens receive C-34 cement at a nominal thickness of 0.030 inch. All specimens have a faying surface angle and groove radius of 30° and 0.125 inch, respectively. These specimens are now being manufactured and the flexure test results should be available for the next reporting period. The remaining test phase will include six specimens to be tested at 2500° F.

(4) Graphite unit cell flexural tests

To further study the bending capability of the fuel block, the graphite unit cell flexure test was initiated to determine the flexural strength of both ATJ and Carb-I-TEX unit cells at elevated temperature and to study the effect of capsules on block flexural strength.

Twenty specimens were tested at 1500° F, some with capsules and some without. The sample size is relatively small, but the results did indicate that the capsules were neither an asset nor a deficit to block capability. Within reasonable bounds, the failure loads were quite close for each graphite material.

The results are tabulated in Table II-12.

All ATJ specimens failed by complete separation. Separation occurred between and approximately parallel to the two load lines of the loading head across the specimen. Those containing capsules failed near the center, exposing the end of a capsule. The ΔT across the specimens without capsules was of the order of 60° F and 335° F for test temperatures of 1500° F and 2400° F, respectively. The ΔT increased to an average of 110° F for the two specimens containing capsules and tested at 1500° F. The maximum average failing load (1295 lb) was obtained at the highest test temperature. The average failing load for the specimens without capsules was 1047 pounds and that of specimens with capsules was 1153 pounds.

All Carb-I-TEX specimens failed in shear and did not separate except for No. 3, which separated completely. The delaminations occurred over a distance of up to 6 inches at the center of the specimen. The ΔT across the specimen thickness (plain unit cells) was approximately 225° F and 740° F for test temperatures of 1500° F and 2400° F, respectively. The ΔT increased only slightly (average 230° F) for the two specimens with capsules tested at 1500° F. Here again the maximum average failing load (2185 lb) was obtained at 2400° F. The average failing load for plain unit cell specimens tested at 1500° F was 1770 pounds as compared with 1877 pounds for those containing capsules.

Oxidation of the graphite specimens during test was very slight and was considered negligible for purposes of this test.

TABLE II-12

Summary of Test Results
Graphite Unit Cell Flexure Specimens
Two-Point Loading

Specimen No.	Type	Dimensions (in.)	Temperature (°F)		Failure Load (lb)	Deflection (in.)	Average Failure Loads (P)	Load Limits (lb)
			Top Surface	Bottom Surface				
1	ATJ	0.998 x 2.95 x 12	1455	1505	1250	0.114		
2	ATJ	0.997 x 2.99 x 12	1450	1500	1050	0.089	1150	1050 to 1250
3	Carb-I-Tex	1.006 x 2.56 x 12	1328	1515	2016	0.117		
4	Carb-I-Tex	1.005 x 2.56 x 12	1318	1508	2000	0.105	2008	2000 to 2016
5	ATJ	1.001 x 2.973 x 12	1440	1500	920	0.0906		
6	↑ ↓	1.001 x 2.998 x 12	1445	1515	910	0.0774		
7		1.002 x 3.000 x 12	1424	1508	1000	0.0822	(1) 943	910 to 1000
8		1.000 x 2.992 x 12	2135	2412	1320	0.0850		
9	ATJ	1.001 x 3.000 x 12	2017	2412	1270	0.0739	(2) 1048	910 to 1320
10	Carb-I-Tex	1.005 x 2.511 x 12	1272	1512	1740	0.0947		
11	↑ ↓	1.001 x 2.503 x 12	1225	1508	1020	0.0528		
12		1.002 x 2.512 x 12	1269	1510	2070	0.0955	(1) 1610	1020 to 2070
13		1.003 x 2.505 x 12	1604	2412	2230	0.1016		
14	Carb-I-Tex	1.003 x 2.506 x 12	1734	2412	2140	0.0888	(2) 1840	1020 to 2230
(3) 15	ATJ	0.992 x 2.989 x 12	1388	1512	1160	0.0665		
(3) 16	ATJ	0.996 x 2.967 x 12	1384	1506	1170	0.0719		
(3) 17	ATJ	0.991 x 2.968 x 12	1433	1518	1130	0.0654	1153	1130 to 1170
(3) 18	Carb-I-Tex	1.008 x 2.502 x 12	1270	1505	2080	0.0854		
(3) 19	Carb-I-Tex	1.004 x 2.533 x 12	1302	1515	1710	0.0723		
(3) 20	Carb-I-Tex	1.007 x 2.511 x 12	1275	1515	1840	0.0860	1877	1710 to 2080

- (1) Average of three failure loads at 1500° F
 (2) Average of five failure loads (1500 x 2400° F)
 (3) With capsules--all others plain unit cells

A final report of the unit cell flexure test program will be issued during the next reporting period.

(5) Preliminary fuel block room temperature vibration test

Vibration tests for the preliminary evaluation of an unclad, four-module ATJ graphite fuel block (specimen No. 1) at room temperature were terminated because of structural failure (crack near mid-span) of the block. The block had successfully passed vibration tests of:

- (1) 20% design level sinusoidal vibration, 25-minute resonant survey at a pre-load of 450 pounds
- (2) 20% design level sinusoidal vibration, 25-minute resonant survey at a pre-load of 650 pounds
- (3) 10.26 g_{rms} level random vibration, 5 minutes at a preload of 650 pounds
- (4) 20 g_{rms} level random vibration, 3 minutes at a preload of 650 pounds.

It had survived 37.4 seconds (accumulated) of a scheduled 3 minutes at 29.1 g_{rms} design level random vibration at a preload of 650 pounds when failure occurred. Data for this last run were analyzed to determine, if possible, the degree of structural performance of the fuel block and a possible cause of failure with the following results:

- (1) Failure of the fuel block occurred in the tension half of the stress cycle, probably from the bottom of the block, with the crack passing directly through strain gage No. 11 (near mid-span).
- (2) Stress level recorded by strain gage No. 11 at the time of failure was approximately 1800 psi. A maximum stress of 2424 psi, at the time of failure, occurred at strain gage No. 13 (at $\bar{C}$ of the block, approximately 3/4 inch from the point of failure).
- (3) These stress levels compare with ultimate flexure stress levels of approximately 4000 psi for ATJ graphite whose minimum density is 1.73 gm/cm^3 , as reported in the literature (Ref.: "Properties of ATJ Graphite," Westinghouse Astronuclear Laboratory Report No. WANL-TME-956, November 4, 1964).
- (4) Modal distribution of stresses was: 1st mode, 83%; 2nd mode, 5%; 3rd mode, 10%; and 4th mode, 2%.
- (5) Peak accelerations and stresses were approximately five times rms accelerations and stresses, indicating a non-gaussian statistical distribution.
- (6) Strain gages indicated near symmetrical loading in the fuel block (in the capsule-axis direction).
- (7) Typically, strain gages on the bottom of the block recorded higher stresses than corresponding gages on the top of the fuel block, indicating eccentric loading of the block in the support fixture.
- (8) Frequency change of the first structural mode was noted from (low level) sine (43 cps) and (design level) random (37 cps) tests due to nonlinearity of the frequency with amplitude of vibration.

- (9) Structural damping in the first mode of the fuel block/fixture combination was determined to be 4% (i.e., $\frac{C}{C_c} = 0.04$).

In order to verify the literature values of ultimate flexural strength, four 1.00 x 1.00 x 1.00-inch density specimens and two 12.00 x 2.00 x 1.00-inch flexure specimens were cut from the failed block and tested. Densities of 1.765 gm/cm³, 1.736 gm/cm³, 1.771 gm/cm³ and 1.732 gm/cm³ were obtained for the four test samples; failure stresses of 3600 psi and 4300 psi were determined from the flexure test samples. These test data confirm the previously reported literature values of ultimate stress but leave no explanation for the apparent discrepancy in stress levels given in Items (1) through (3) above. In Table II-13 rms and peak stress levels from the final 29.1 g_{rms} test are shown (see Fig. II-16 for location of strain gages) at the time of fuel block failure.

TABLE II-13
Fuel Block Dynamic Test Strain Gage Data

<u>Strain Gage No.</u>	<u>Stress (psi)*</u>	
	<u>rms</u>	<u>Peak</u>
1	163	761
2	221	1126
3	337	1702
4	378	1917
5	337	1829
6	281	1313
7	273	1283
8	346	1638
9	165	802
10	208	888
11	305	1818
12	350	1713
13	454	2424
14	228	1277
15	234	1245
16	348	1720

*Based on a flexural modulus of elasticity at room temperature of 1.87×10^6 psi. (This value of E gives a calculated first mode frequency of 38 cps as compared with a measured frequency of 37 cps).

An analysis was begun to evaluate the effects on stresses of increased fuel block thickness. Although not completed, the analysis indicates only a minor reduction in stresses for a major increase in thickness--due to the overriding effects of increase in spectral density input with increase in block first mode frequency (which accompanies increased thickness). Additional analysis along these lines is being performed.

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FIG. II-16. SNAP 29 FUEL BLOCK NO. 1 VIBRATION TEST (BLOCK TOP VIEW)

Figure II-17 shows the fuel block specimen after failure.

During the next reporting period, a final test report will be issued.

b. Fuel capsule

(1) Effective thermal conductivity test

Measurement of the temperature profile across the inner liner, liner and coated capsule clad has been completed in vacuum, argon and helium. Previous analytical calculations based on known heat transfer mechanisms and the thermal conductivities of the materials of capsule construction and environmental atmospheres predicted that, for a SNAP 29 fuel capsule, the temperature drop in helium would be on the order of 50° to 60° F. The results of the experimental program confirmed these calculations, thereby supporting the analytical techniques for calculating the temperature profile of the SNAP 29 fuel capsule configuration.

The test configuration consisted of a glass bell jar system capable of operating at elevated temperatures in vacuum and in inert atmospheres. The chromel-alumel thermocouples positioned inside the bell jar and attached to the simulated fuel capsule were passed through a vacuum-tight base plate and attached to either a multi-point recorder or a 24-point switch attached to a potentiometer. Initially, commercially available 1000-watt Watlow resistance heaters were used to simulate the power output of a SNAP 29 isotopic fuel capsule. It soon became evident that their materials and methods of construction were unsuitable for this application. The electrical leads positioned at the end of the heater and the relative low melting materials of construction prevented any continuous operation in ionizable helium and argon at elevated temperatures. Subsequently both tungsten-wound and platinum-30% rhodium-wound heaters, coated with aluminum oxide and constructed with their electrical leads extending from each end, were used to obtain the continuous operation at 2000° F. This heater was positioned down the center of the fuel capsule.

The diametric dimensions of the inner liner, liner and coated capsule clad conformed to those specified in Martin Marietta drawing No. 466A3331017. Although these dimensions have changed, the basic principle of a definite temperature drop was still verified and remains applicable even with the larger fuel capsule dimensions.

The experimental program was divided into two tasks, the first enabled the test problems to be identified and resolved, and the second obtained the experimental data which established the magnitude of the temperature drop under different test conditions. Three major problems were identified which had to be resolved prior to obtaining relatively accurate data: utilization of laboratory fabricated resistance heaters consisting of tungsten or platinum-30% rhodium wire wound around aluminum oxide rods with their leads extending from each end; a spot welding technique which attached the thermocouple beads to the inside and outside of the metallic tubing; and the inability of the five inside thermocouples to accurately read the true temperature of the inner liner inside wall, due to the influence of the heater operating at a few hundred degrees greater than the test temperatures. Through the addition of five thermocouples positioned to the outside wall of the inner liner metal directly opposite those five inside, a significant difference in the temperature was recorded due to the heater influence.

The simulated fuel capsule consisted of 12-inch lengths of the inner liner, liner and capsule cladding metals with blocks of Min-K insulation positioned at the ends of the tubing. The capsule cladding was coated with an emittance coating with a value of approximately 0.85 to provide the correct radiation characteristic. Five thermocouples were spotwelded to the inner wall of the inner liner tubing approximately two

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(a)

(b)

FIG. 17. PRELIMINARY FUEL BLOCK VIBRATION TEST SPECIMEN AFTER FAILURE

inches apart and five additional thermocouples were positioned on the outside of the capsule cladding tubing directly opposite those inside. It was these five pairs of thermocouples which were initially used to provide the temperature drop from the inside of the simulated fuel capsule to the outside of the fuel capsule cladding.

Two pieces of insulating tubing, the inner one being Al_2O_3 and the outer one Haynes-25, surrounded the test capsule configuration. A tantalum shield was also positioned against the glass bell jar to protect the glass from the intense heat.

The experimental data obtained from the above fuel capsule and thermocouple configuration only provided accurate data on the temperature in the insulation blocks, at the Al_2O_3 insulator and at the inside surface of the glass bell jar. The temperature drop through the capsule as measured by the five sets of thermocouples was 200° to 400°F greater than that originally calculated for all three test environments (vacuum, argon and helium). A summary of the data is presented in Table II-14.

TABLE II-14

Equilibrium Temperature Conditions for Various Wattage Inputs ($^\circ\text{F}$)

<u>Test Condition</u>	<u>Wattage</u>	<u>Ceramic Tube</u>	<u>Glass Wall</u>	<u>Min-K Top*</u>	<u>Min-K Bottom*</u>	<u>Min-K Top**</u>	<u>Min-K Bottom**</u>
Argon 1 atmosphere	215	522	139	420	625	323	308
Argon 1 atmosphere	603	763	190	599	902	619	402
Vacuum 200 microns	146	465	108	327	475	378	296
Vacuum 200 microns	224	528	114	357	532	375	305
Vacuum 1200 microns	634	799	186	667	934	909	628
Helium 1/3 atmosphere	1165	1070	282	871	1188	1111	829
Helium 2/3 atmosphere	1170	1036	280	858	1179	1073	756
Helium 1 atmosphere	1170	1033	283	861	1177	1067	765
Helium 1/3 atmosphere	578	837	223	697	934	871	666
Helium 2/3 atmosphere	616	819	221	685	934	829	600
Helium 1 atmosphere	640	795	219	679	935	808	560

* Insulator block positioned over top of the fuel capsule tubes

**Insulator block positioned at the bottom of the fuel capsule tubes

Note from these data that the partial pressures of helium did not result in significantly different temperatures at positions throughout the test system. As expected, vacuum provided the best insulating conditions. The data taken with the five sets of thermocouples during this series of test runs are not reported, because it was recognized that the central heater was influencing the inner wall thermocouple temperature readings. This conclusion was later verified after the test configuration was modified through the addition of five thermocouples.

These five additional thermocouples were located on the outer wall of the inner liner tubing directly opposite those positioned on the inside of the inner liner material. The thermocouple beads were spot welded to the tubing wall with their leads passing directly through the two outer tubings. Each set of thermocouples now consisted of three, with a total of five sets present, each two inches apart down the length of the tubing. As can be seen from the data presented in Table II-15, there was a significant temperature drop across the inner liner wall due to the influence of the resistance heater on the inside set of thermocouples. The resulting temperature drop across the fuel capsule system varied between 49° F to 82° F, depending upon temperatures at which the system reached equilibrium. Even when the power input was reduced, resulting in nonequilibrium conditions being attained, the temperature drop across the system remained approximately the same. The most significant change was noted in the temperature readings of the inner wall thermocouples, again verifying the influence of the central heater. These temperature values agree quite well with those calculated from known heat transfer mechanisms and thermal conductivity values.

TABLE II-15
Equilibrium Temperature Profile in Helium
Across the Fuel Capsule System

Test Condition	Wattage	Inside Wall ⁽¹⁾	ΔT	Outside Wall ⁽²⁾	ΔT	Outside Pt-20% Rh Wall Temperature (° F)
		Temperature (° F)		Temperature (° F)		
Vacuum	140	1422 ⁽³⁾	83	1339	666	673
200 microns						
Vacuum	360	1922 ⁽³⁾	126	1796	764	1032
200 microns						
Helium	588	1456 ⁽³⁾	102	1354	55	1299
1/2 atmosphere						
Helium	952	1899 ⁽³⁾	155	1744	82	1662
1/2 atmosphere						
Helium	519 ⁽⁴⁾	1573 ⁽⁵⁾	129	1444	54	1390
1/2 atmosphere						
Helium	480	1458 ⁽⁵⁾	92	1366	51	1315
1 atmosphere						
Helium	960	1856 ⁽⁵⁾	186	1670	63	1607
1 atmosphere						
Helium	855 ⁽⁴⁾	1670 ⁽⁵⁾	109	1561	49	1512
1 atmosphere						

(1) Inner liner

(2) Capsule cladding

(3) These temperature readings are the average of 15 thermocouple readings

(4) Power input reduced, transient temperature readings

(5) These temperature readings are the average of twelve thermocouple readings.

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The following conclusions and observations represent the summation of this experimental program.

- (1) The effective thermal conductivity of the SNAP 29 fuel capsule system can be approximated using established heat transfer relationships involving the utilization of known thermal conductivity values for metals and helium. This was shown from the temperature profile values across the fuel capsule.
- (2) Consideration of the influence of the control heater on the inner thermocouples was necessary before accurate data were obtained.
- (3) The electrical leads for the heater must be positioned at opposite ends to prevent arcing across the leads in ionized inert gases.

(2) Liner/inner liner creep evaluation test

After it was established that the amount of oxygen present in purified helium was still sufficient to severely oxidize the inner liner and liner metals, the test configuration was modified to prevent this from recurring. This was accomplished by welding individual sets of the compatibility coupons inside of a Haynes-25 sandwich at approximately one quarter of an atmosphere of helium. The samples, which have been withdrawn for chemical and mechanical property analyses after exposure to a temperature of 2030°F for a total of 70 days at 1060 psi, have shown that oxidation is no longer a problem. However, the two liner coupons on each side of the inner liner coupon have fused together due to migration of tungsten. This mechanism was identified through the observation that the liner coupons did not fuse together under the same conditions. These two additional liner coupons, one on each side of the three comprising the compatibility couple, were provided as a barrier against interdiffusion with the Haynes-25 container.

The results from the first twenty days exposure have been previously reported. The compatibility couples which have been recently withdrawn after 10, 40 and 70 days exposure at 2030°F at a pressure of 1060 psi are being analyzed for tungsten migration and hardness. The average tungsten concentration in the liner coupons was found to be 10.71%. Initial stress values for the liner and inner liner were 128,000 psi and 42,500 psi, respectively. The initial Vicker's hardness value for the liner was V-220 and, for the inner liner, it was V-81. A summary of the modified time, temperature and pressure cycle for these creep test coupons is presented in Table II-16. The remaining 44-day exposure is currently in progress.

TABLE II-16
Time-Temperature-Pressure Cycle for The Test Couples

<u>Temperature</u> (°F)	<u>Applied Pressure</u> (psi)	<u>Elapsed Test Time</u> (days)	<u>Sample No.</u>
1370	80	10	1
1370	155	20	2
2030	1060	30	3
2030	1060	60	4
2030	1060	90	5
2030	1060	134	6

(3) Capsule cladding environmental compatibility test

Metallographic examination of the compatibility couples has been partially completed on those subjected to helium at 300° F, 1230° F and 1500° F for 20, 10 and 14 days, respectively, and on those which have been withdrawn from a vacuum (10^{-6} torr) furnace at 1850° F for 30 days. Since the emissivity values for the lithoids and black hot paint did not maintain values greater than 0.8, they are no longer being considered as coatings for the capsule cladding. The interfaces which are being examined for long-term compatibility are the Pt-20% Rh surface which is in contact with the emittance coating in contact with the graphite, the Pt-20% Rh surface in contact with the Ta-10% W liner, and the Ta-10% W liner surface in contact with the Pt-20% Rh. As can be seen from the photomicrographs represented in Figs. II-18 through II-23, the Ta-10% W liner surface is compatible with the Pt-20% Rh cladding. Slight oxidation of the liner can be seen in the photos representing the exposure at 1850° F. Similar satisfactory compatibility of the Pt-20% Rh in contact with the liner was observed in Figs. II-24 through II-28. The beginning of a slight reaction zone was noted after 30 days at 1850° F. In Fig. II-29, the CaTiO_3 emittance coating had only penetrated the Pt-20% Rh 1-1/2 mils after 30 days at 1850° F. Subsequent examination of the compatibility coupons of CaTi_3 with Pt-20% Rh will establish whether the reaction zone is a continuous phenomenon or stops after the initial formation. There was found no incompatibility between Fe_2TiO_5 and the Pt-20% Rh substrate after a total of 44 days of exposure at 300° F, 1230° F and 1500° F (Fig. II-30). Metallographic, X-ray diffraction and emission spectrographic analyses are continuing as the remaining series of test couples are undergoing the final 90-day treatment in vacuum at 1850° F.

c. Heat source

(1) Emittance coating development

After the first phase of the environmental treatment, each of the three substrates, Haynes-25 (hot plate), Inconel 600 (fuel block) and Pt-20% Rh (capsule cladding), had two candidate emittance coatings which still maintained emissivity values in excess of 0.8. For the hot plate, the first phase was subjection to air for 54 days at 1200° F; for the fuel block, the first phase was treatment in air for 54 days at 1600° F; and for the capsule cladding, the initial phase was in helium and ATJ graphite at 1850° F for 54 days. However, after the first 30-day exposure to vacuum (10^{-6} torr) for each coating, the black hot paint on Inconel-600 volatilized from the surface, thereby eliminating its use for the fuel block application. A summary of the experimental results to date is presented in Table II-17. As can be seen from the data in Table II-17, there remain two suitable coatings for Haynes-25, black hot paint and Fe_2TiO_5 , one acceptable coating for Inconel-600, Fe_2TiO_5 and two suitable coatings for Pt-20% Rh, Fe_2TiO_5 and CaTiO_3 . The remaining exposure period to the vacuum conditions will be completed shortly. Chemical and instrumental analyses will then be performed on the selected coatings to establish their chemical behavior during the environmental treatment.

FIG. II-18. COMPATIBILITY STUDY SHOWING Ta SURFACE IN CONTACT WITH Pt AFTER 300°F FOR 20 DAYS

FIG. II-19. COMPATIBILITY STUDY SHOWING Ta SURFACE IN CONTACT WITH Pt AT 300°F FOR 20 DAYS and 1230°F FOR 10 DAYS

FIG. II-20. COMPATIBILITY STUDY SHOWING Ta SURFACE IN CONTACT WITH Pt AFTER 1500°F FOR 44 DAYS

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FIG. II-21. SAMPLE NO. 1, 1850°F FOR 30 DAYS IN VACUUM AT 1 MICRON--Ta-10%W
EDGE

FIG. II-22. COMPATIBILITY STUDY COUPLE NO. 4 SHOWING Ta IN CONTACT WITH Pt
AFTER 1850°F FOR 22 DAYS

FIG. II-23. SAMPLE NO. 9, 1850°F FOR 30 DAYS IN VACUUM AT 1 MICRON--Pt TO Ta-10%W
INTERFACE

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FIG. II-24. COMPATIBILITY STUDY SHOWING Pt IN CONTACT WITH Ta AFTER 300° FOR 20 DAYS

FIG. II-25. COMPATIBILITY STUDY Pt SURFACE IN CONTACT WITH Ta AFTER 20 DAYS AT 3000°F AND 10 DAYS AT 1230°F

FIG. II-26. COMPATIBILITY STUDY SHOWING Pt IN CONTACT WITH Ta AFTER 1500°F FOR 44 DAYS

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FIG. II-27. SAMPLE NO. 9, 1850°F FOR 30 DAYS IN VACUUM AT 1 MICRON--Ta-10%W
IN CONTACT WITH Pt

FIG. II-28. SAMPLE NO. 1, 1850°F FOR 30 DAYS IN VACUUM AT 1 MICRON--Pt EDGE

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FIG. II-29. SAMPLE NO. 9, 1850°F FOR 30 DAYS IN VACUUM AT 1 MICRON--Pt EDGE COATED

FIG. II-30. COMPATIBILITY STUDY SHOWING Pt COATED SURFACE AFTER 1500°F FOR 44 DAYS

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TABLE II-17
Emittance Values of Candidate Coatings for the Haynes-25,
Inconel-600 and Pt-20% Rh Substrates

<u>Environmental Condition</u>	<u>Candidate Coating</u>	<u>Substrate</u>	<u>Emissivity (Gier Dunkle)</u>	<u>Emissivity (total hemi-spherical cup)</u>
Initial, 1200° F	Black hot paint	Haynes-25	0.87	0.94
	Lithoid C10A	"	0.83	-
	Lithoid CZS	"	0.88	-
	Fe ₂ TiO ₅	"	0.84	-
	CaTiO ₃	"	0.88	0.88
Initial 1600° F	Black hot paint	Inconel-600	0.88	0.93
	Lithoid C10A	"	0.87	-
	Lithoid CZS	"	0.86	-
	Fe ₂ TiO ₅	"	0.82	-
	CaTiO ₃	"	0.83	-
Initial 1850° F	Black hot paint	Pt-20% Rh	0.88	-
	Lithoid C10A	"	0.85	-
	Lithoid CZS	"	0.80	-
	Fe ₂ TiO ₅	"	0.84	-
	CaTiO ₃	"	0.87	-
After 18 days in air at 1200° F	C10A	Haynes-25	0.75	-
	CZS	"	0.63	-
	Fe ₂ TiO ₅	"	Coating chipped	-
	CaTiO ₃	"	0.56	-
	Black hot paint	"	0.83	-
After 27 days in air at 1200° F	Fe ₂ TiO ₅	Haynes-25	-	0.88
	Black heat paint	"	-	0.94
After 18 days in air at 1600° F	C10A	Inconel-600	0.73	-
	CZS	"	0.73	-
	Fe ₂ TiO ₅	"	Coating chipped	-
	CaTiO ₃	"	0.59	-
	Black hot paint	"	0.90	-
After 27 days in air at 1600° F	Fe ₂ TiO ₅	Inconel-600	-	0.86
	Black hot paint	"	-	0.96
After 18 days in He at 1850° F with graphite	C10A	Pt-20% Rh	0.73	-
	CZS	"	0.83	-
	Fe ₂ TiO ₅	"	0.85	-
	CaTiO ₃	"	0.90	-
	Black hot paint	"	Volatilized	-
After 27 days in air at 1850° F	Fe ₂ TiO ₅	Pt-20% Rh	-	0.86

TABLE II-17 (continued)

<u>Environmental Condition</u>	<u>Candidate Coating</u>	<u>Substrate</u>	<u>Emissivity (Gier Dunkle)</u>	<u>Emissivity (total hemi-spherical cap)</u>
After 36 days in air at 1200° F	Black hot paint Fe_2TiO_5	Haynes-25 "	0.94 0.84	- -
After 36 days in air at 1600° F	Black hot paint Fe_2TiO_5	Inconel-600 "	0.89 Coating chipped	- -
After 36 days in He at 1850° F with graphite	Fe_2TiO_5 CaTiO_3	Pt-20% Rh "	0.82 0.91	- -
After 54 days in air at 1200° F	Black hot paint Fe_2TiO_5	Haynes-25 "	0.93 0.80	0.93 0.84
After 54 days in air at 1600° F	Black hot paint Fe_2TiO_5	Inconel-600 "	0.87 0.80	0.95 0.93
After 54 days in He at 1850° F with graphite	Fe_2TiO_5 CaTiO_3	Pt-20% Rh "	0.83 0.91	0.86 0.93
After 30 days in vacuum at 1200° F	Black hot paint Fe_2TiO_5	Haynes-25 "	0.91 0.83	- -
After 27 days in vacuum at 1600° F	Black hot paint Fe_2TiO_5	Inconel-600 "	Vaporized Coating chipped	- -

Current effort is underway to identify a backup emittance coating for the fuel block. Manganese dioxide was found to be unsatisfactory in vacuum as the MnO_2 was reduced to MnO which has a low emissivity value. Nickel oxide, formed by soaking nickel in air at 1600° F for varying periods of time, resulted in emittance values ranging between 0.70 to 0.74. Additional effort is being directed towards evaluating Cr_2O_3 in air and vacuum at 1600° F.

The following candidate emittance coatings have been examined and eliminated from use for a variety of reasons, including low melting, high sublimation and vaporization rates, oxidation, reduction, and primarily initial emittance values below 0.8. They include platinum black, the lithoids, manganese oxide, cadmium oxide, tungsten oxide, zirconium diboride, molybdenum disilicide, a mixture of $\text{ZrO}_2 + \text{Na}_2\text{SiO}_3$, silicon carbide, tantalum carbide, zirconium dioxide, hafnium-27% tantalum, zinc oxide and some of the more common rare earths.

(2) Post-test examination and evaluation of the simulated capsules used in the Phase I exploratory drop test

The evaluation of the simulated fuel capsules recently tested in the Phase I exploratory drop test was completed and is reported in Appendix B to this report.

(3) Fuel capsule baseline design

The fuel capsule baseline design dimensions have been established based upon a joint-capsule-per-module design. The establishment of these dimensions has led to the generation of a new fuel capsule drawing. This drawing has been released for comments. After the resolution of any comments received, the drawing will be officially released and used for the fabrication of all fuel capsules for the 1130 control point. A listing of the capsule diametral dimensions and clearances is presented in Table II-18.

TABLE II-18

Capsule Diametral Dimensions and Clearances (inches)

<u>(1) Fuel Matrix</u>	
ID	0.645 $\pm$ 0.001
OD	0.746 $\pm$ 0.001
OD of matrix/ID of inner liner diametral clearance	0.005/0.011
<u>(2) Inner Liner</u>	
ID	0.752 $^{+0.004}_{-0.000}$
Nominal wall thickness	0.020
OD	0.795 $^{+0.000}_{-0.003}$
OD of inner liner/ID of liner diametral clearance	0.015/0.021
<u>(3) Liner</u>	
ID	0.813 $^{+0.000}_{-0.003}$
Wall thickness	0.027 $\pm$ 10%
OD (maximum theoretical)	0.872
OD of liner/ID of clad diametral clearance	0.013/0.016 minimum
<u>(4) Cladding</u>	
ID	0.885 $^{+0.003}_{-0.000}$
Wall thickness	0.020
OD	0.928 $^{+0.000}_{-0.003}$
<u>(5) Emissivity Coating</u>	
Thickness (maximum)	0.005 radial
<u>(6) Capsule OD (maximum)</u>	
	0.938 $^{+0.000}_{-0.003}$

Supporting analysis for establishment of fuel capsule baseline design dimensions is included as Appendix C of this report, and the structural analysis in particular as Appendix A.

(4) Fuel capsule structural liner material capability

In support of the fuel capsule baseline design analysis effort, material property tests of the liner material were performed at elevated temperatures. The tests

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were conducted by the Fansteel Corporation at their Baltimore facility. The specimens, per Fig. II-31, were taken from tubing with a nominal wall thickness of 0.020 inch. All tests were conducted in a vacuum and all specimens were taken from the same melt.

A considerable amount of strength data for this alloy is found in the literature. However, the vast majority is based upon some condition of cold work. Although the fully annealed condition is inherent in testing at 2700° F and above, test data via the literature at these temperatures should be examined with caution. Apparently most testing has been accomplished in an argon atmosphere, and it may be assumed that the impurities present in the argon have provided some erroneous results. This is evident by examining some of the percent elongation results as reported. Elongation far below the levels expected seem to support this hypothesis. The vacuum environment associated with the test data in this report is from about 3×10^{-5} to 7×10^{-5} torr.

The data provided by this recent set of tests are based primarily upon a slightly higher strain rate than typical. The strain rate of these data is 0.05 in./in./min to yield and 0.5 in./in./min to failure. It may be noted that these higher rates provide a substantially higher yield and ultimate strength. Although most metals behave in this manner, the magnitude of the difference was not expected, since a strain rate of 0.5 in./in./min is not normally within the regime of rapid strain rates.

Appendix A contains the complete set of tensile test data, and Fig. II-32 represents a plot of ultimate tensile strength versus temperature for the tests corresponding to 0.05 in./in./min to yield and 0.5 in./in./min to failure.

(5) Capsule temperatures under a transportation fire environment

An evaluation of steady-state capsule temperatures in a hot air environment associated with a gasoline fire on day 30 (BOL) was performed in support of safety studies. The capsule is assumed to be suspended horizontally and freely in the fire environment, i.e., there are no capsule-to-surface conductive paths. The fire temperature regime was assumed to fall in the region of 1600° F to 3000° F. For this temperature regime, it was determined that convection heat transfer is on the order of 3% of the total heat transfer; therefore, it was neglected.

Capsule clad and inner liner temperature were determined for radiative heat transfer and a conservative hot air emissivity of 0.80. Figure II-33 shows the variation in capsule temperatures as a function of the environmental temperature.

In contrast, a bare capsule exposed to 100° F ambient air conditions on day 30 will have a clad surface temperature of only 1200° F. In this case, convective heat transfer in still air is appreciable--approximately 18% of the total heat transferred.

The details of this analysis are presented in SNAP 29 IDC 1140-305.

(6) Fuel block

A four-module generator configuration has been selected. The justification for this selection was based partially on the aerothermal analyses which showed there was no significant differences in re-entry heating of the fuel blocks between the two-, four-, six- or eight-module generators. Some of the aerothermal data are shown in Appendix D. In addition, the thermal and weight parametric studies showed no great differences in the selection of a four-, six- or eight-module generator. These data

Note:
All dimensions in inches.

FIG. II-31. TENSILE TEST SPECIMEN--FULLY ANNEALED Ta-10%W

FIG. II-32. ULTIMATE TENSILE STRENGTH AT ELEVATED TEMPERATURE--FULLY ANNEALED Ta-10%W

FIG. II-33. CAPSULE TEMPERATURES--TRANSPORTATION FIRE ENVIRONMENT

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are summarized in Appendix E. It should be noted that the fuel block was 1.19 inches thick for this study versus the thickness shown below. However, this change will not alter the conclusions reached. The four-module size was therefore selected since it provided the minimum practical size with which to demonstrate the RTG system.

A new heat source fuel block assembly drawing, 466A1141001, has been completed. This fuel block reflects the four-module design size and the capsule baseline dimensions. The fuel block built from this drawing will be used in Control Point 1140, "Four-Module Fuel Block Hot Vibration and Retention Test." The overall dimensions of the graphite fuel block (without cladding) are:

Length	=	31.668 in.
Width	=	19.847 in.
Thickness	=	1.285 in.

(7) Meteoroid penetration of fuel block clad

A meteoroid penetration evaluation was performed to determine the thickness of the fuel block clad required to assure containment of helium during the portion of the mission when capsule inner liner temperatures are critical. The calculated temperature for the inner liner of the recommended capsule design is above the currently accepted temperature limit and it will remain above this limit for a period of 30 to 60 days beyond BOL (day 30). Containment of helium within the fuel block would lower capsule temperatures below the critical point. Since under these circumstances, the fuel block clad would have to act as the helium gas retainer, the clad must be thick enough to reduce the meteoroid penetration probability to an acceptable level.

The clad thickness required was determined using the Aerospace Meteoroid Environment and Penetration Criterion report method. Only the shutter side of the clad is exposed to sporadic (omnidirectional) meteoroids and shower meteoroids (unidirectional). The vulnerable area of the clad reduces with mission time due to the shutters closing. An evaluation of the shutter effective thickness showed that they were an effective meteoroid bumper for meteoroids that strike the external honeycomb surface of the shutter panel. The study showed that the shutters were effectively closed to meteoroid penetration for shutter angles below about 15°. A cumulative probability approach was used to account for the varying vulnerable area conditions, i.e.,

$$P = \prod_{x=1}^n P_x$$

where:

P	=	cumulative probability of no penetration
P _x	=	average probability of no penetration by a meteoroid during a specified interval of time, Δt.
n	=	number of intervals.

The cumulative probability of no penetration by a meteoroid was determined for various thicknesses of nickel clad and for periods of 30 days, 60 days and 90 days. Figure II-34 shows these data for two shutter penetration criteria: the entire shutter constitutes a nonpenetrable meteoroid shield against direct hits; the Min-K insulative backing of the shutter provides no resistance to direct meteoroid hits on the Min-K.

FIG. II-34. FUEL BLOCK CLAD THICKNESS REQUIRED TO PREVENT METEOROID PENETRATION

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The increase in nickel clad weight with the increase in thickness of the clad shutter face side was determined assuming a basic clad thickness of 0.020 inch elsewhere. Figure II-35 shows the total fuel block weight and incremental weight as a function of clad thickness on the shutter side of the fuel block.

If clad integrity must be maintained for a period of 60 days with a probability of 0.9990, the clad weight increase required will be approximately 8 pounds. Increasing the probability to 0.9999 will add another 10 pounds to the clad weight. The shutter side clad thicknesses associated with these two probability criteria are 0.058 inch and 0.108 inch, respectively.

The actual clad thickness can be determined for the final capsule design when the inner liner temperature-time characteristics are available. At this time, the maximum no-penetration period can be established and safety studies will have established firm, minimum no-penetration probability criteria.

(8) Canned fuel block

A study was performed to determine the effect on capsule temperatures of placing a thin Haynes-25 or nickel cover around the entire SNAP 29 fuel block. This cover or "can" would substitute for the bonded oxidation clad which has been under extensive evaluation as the primary method for oxidation protection.

A preliminary analysis was used to establish critical capsule temperatures and then a complex computer code analysis was performed for a single design case to provide an accuracy check. The computer analysis indicated that the more idealized preliminary computations yielded temperatures about 15° to 30° F lower than could be expected for a "can" vacuum environment, depending upon the particular component.

The calculations were performed for a four-module, four-capsule per module fuel block at BOL (orbit) conditions. The distribution of heat between the thermoelectric module, the shutters and the structural housing is known from previous studies; therefore, the analysis was performed for that part of the thermal model shown in Fig. II-36 which is between the hot plate and the fuel block surface. The can is shown as being separated from the fuel block because fabrication tolerances, thermal differential expansion and/or internal gas pressure preclude a flush fit.

Three possible environments within the can were examined: vacuum, argon and helium. The argon or helium result in reduced capsule temperatures but require absolute nonleakage integrity of the can. Radiative and conductive heat transfer was assumed for the argon and helium study. RTG design characteristics were as follows:

(1) Hot plate temperature	1212° F
(2) Fuel block material	ATJ graphite
(3) $Q_{T/E}$	442 watts/capsule
(4) T between fuel block surface and bore	15° F (assumed)
(5) Maximum capsule liner diameters	
(6) Liner gap distance	5 mils
(7) Can environment	vacuum, argon, helium
(8) Expected can emissivity	0.80

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FIG. II-35. WEIGHT OF METEOROID PROTECTION MATERIAL

Hot plate
 $T_{HP} = 1212^{\circ} \text{ F}$

FIG. II-36. CANNED FUEL BLOCK THERMAL MODEL

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An indication of the effect of the can and can environment on temperature is illustrated by the magnitude of the fuel block-to-capsule temperature drop variations noted in Table II-19.

TABLE II-19
Effect of Oxidation Clad Gap Environment
on Fuel Block Thermal Gradient (10-mil gap)

<u>Gap Environment</u>	<u>Clad-to-Block Bore Thermal Gradient (°F)</u>
Vacuum (without can)	365
Vacuum (with can)	300
Argon (with can)	140
Helium (with can)	30

The temperature of the inner liner and fuel block has been plotted as a function of separation distance between the can and the fuel block surface in Fig. II-37. Radiation is the predominant mode of heat transfer when the separation distance exceeds approximately 100 mils. The inner liner temperature increases from about 1060°C to approximately 1120°C if the evacuated can is substituted for a bonded fuel block clad.

The effect of a lower can emissivity was also evaluated for the vacuum environment case. While the inner liner temperature increases fairly rapidly with decreasing emissivity, a can emissivity better than approximately 0.40 is expected to be satisfactory for an inner liner temperature of less than 1200°C. A plot of inner liner temperature as a function of can emissivity is given in Fig. II-38.

The evacuated can approach appears feasible from the thermal standpoint if temperatures of 1100° to 1200°C are acceptable. Argon or helium gas within the can is essential if the temperatures are limited to less than 1100°C.

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- 1 ——— Average inner liner temperature--vacuum
- 2 ——— Average inner liner temperature--argon
- 3 ——— Average inner liner temperature--vacuum without can
- 4 ——— Average inner liner temperature--helium
- 5 ——— Average fuel block temperature--vacuum
- 6 ——— Average fuel block temperature--argon filled
- 7 ——— Average block temperature--helium filled
- 8 ——— Average fuel block temperature--vacuum without can

Can emissivity = 0.80

FIG. II-37. TEMPERATURE VERSUS DISTANCE BETWEEN FUEL BLOCK AND CAN

FIG. II-38. TEMPERATURE VERSUS ϵ OF THE SURFACE ON THE CAN FOR THE VACUUM-FILLED CAN

C. HEAT SOURCE (ELECTRICAL) SUBSYSTEM--CONTROL POINT 1200

The objective of this task is the development and qualification of electrical heat sources for the thermoelectric module and generator which simulate the thermal characteristics of the isotopic heat source. The task is divided into the following sub-tasks.

CP 1240--Heat Source (Electrical) Subsystem, Component Development

CP 1230--Heat Source (Electrical) Subsystem, Design Verification

CP 1220--Heat Source (Electrical) Subsystem, Demonstration Unit

Effort was continued during this reporting period on the component development of the electric heat source for the T/E modules and of the generator electric heat source.

1. Summation of Accomplishments

For the analysis of all previously identified heater failures, see Chapter IV, Reliability. Based on this heater failure analysis, Watlow heaters have been selected. The only failure modes encountered with the Watlow heaters were the loss of leads at the juncture with the body of the heater in the area of the ceramacast and the embrittlement of the nickel lead wires. The first is no longer a problem, since the boron nitride caps and ceramacast potting have been eliminated. The caps were originally installed to reduce the arcing potential; but, based on tests, they are no longer considered necessary.

The embrittlement is a function of time and temperature, with temperature being the predominant consideration. Since the dynamic testing of the electrical heat source occurs at a relatively early period in the heater life, the embrittlement of the nickel wires should not be a problem. In addition, the design of the electrical heat source will be such that the static tests, which follow the dynamic tests, should not cause premature failures of the heaters. This philosophy is substantiated by the module tests under Control Point 1300.

The advisability of changing the lead material has been discussed with Watlow. If such a change can be made without further changes to the heater, it will be considered for future heater procurement.

Large temperature gradients were observed across the heater blocks of the T/E module test units and the module heater life and thermal cycle test fixtures. All of these heater blocks exhibited lower temperatures on the end of the block from which the heater leads were extended. The gradients across the heater blocks ranged from 100° F to 160° F. The corresponding gradient across the T/E hot junctions was about 100° F. The system efficiency is limited by the maximum T/E material temperature. Thus, the gradients should be reduced in order to permit the T/E's to be operated at a higher average temperature.

Based on the analysis of the individual heater operation in air and in vacuum, it was concluded that the heaters were not the cause of the large temperature gradients exhibited in T/E module life and heater block performance tests. When installed in the heater blocks, the individually tested heater elements produced the temperature profiles seen in the T/E module life performance tests. This further supported the belief that the problem was not in the heaters. It was found, however, that the temperature gradients could be reduced by adjusting the position of the heaters so that

the heat losses out the sides of the test fixture were equally distributed. This led to a final test which indicated that the heater lead end of the test fixture was providing a large heat sink. When the voids at the ends of the test fixture were packed with Min-K, the thermal gradients were reduced from 130° F to less than 50° F.

It was also found in these tests that a surface coating of black hot paint (a high emissive coating) allowed the block to operate approximately 25° F to 50° F cooler than with a stably oxidized heater block.

The Control Point 1230 electrical heat source design has been initiated. The philosophy guiding this design is to use one long (approximately 30-inch) Watlow cartridge-type heater in each fuel capsule hole and to firmly support this heater on the heater lead end by attachment to the protective can or similar means. A "canned" type of cladding will be used to protect the graphite fuel block from oxidation. This type of cladding has been successfully fabricated. The canning consists essentially of two pieces of 0.018-inch thick nickel 200 sheet which have been formed to the contour of the fuel block. The two sheets of nickel are seal welded into place on the graphite fuel block. Welding tests to prove the feasibility of making this seal weld have been performed satisfactorily.

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D. THERMOELECTRIC MODULES SUBSYSTEM--CONTROL POINT 1300

The objective of this task is to develop a thermoelectric module subsystem which will withstand the predicted dynamic and environmental conditions and produce the required power output over the mission life with high reliability.

The thermoelectric module subsystem is designed, fabricated, inspected and tested under this task. The basic philosophy in the module development program is to develop module components on an individual basis and then integrate the components into module assemblies. The assembled modules will then be subjected to performance and environmental testing for reliability data and design verification. Modules will then be fabricated and acceptance tested for use in demonstration RTG units. Commensurate with this philosophy, the work effort has been broken down into the following control points:

CP 1340--T/E Module Subsystem, Component Development

CP 1330--T/E Module Subsystem, Design Verification

CP 1320--T/E Module Subsystem, Demonstration Unit.

At the present time, five piston-heat sink modules (Fig. II-50, Third Quarterly Report) and one diaphragm module (Fig. II-38, Fourth Quarterly Report) are being manufactured for evaluation testing.

1. Summary of Accomplishments

a. T/E module performance

Data reduction and evaluation of the spring-piston module life tests are continuing. Figure II-39 shows the corrected power history of the life test modules and the degradation per thousand hours (D) based on a least squares fit of $P = P_0 \exp(-\alpha t)$. Detailed histories of the tests are shown in Figs. III-7 through III-11 in Chapter III and additional data are given in Table II-20.

The degradation rates are below 2% per 1000 hours except for modules S/N 002 and S/N 004. Module S/N 002 has recovered the power lost during the vibration testing. S/N 004 developed a gas leak during life testing and was removed from test for repair. The leak could not be repaired completely and the module was put back on test with a continuous argon gas supply. The effects of the leak and rework continued to degrade it at an abnormally high rate (~8% per 1000 hours). Therefore, the module was removed from the test loop, and will be disassembled for detail inspection during the next report period.

The piston heat sink dynamic module, S/N 002, was subjected to a thorough physical examination and leak check after the vibration tests. A leak was discovered in the hot plate. The defect would appear to be a material or internal stress problem rather than a direct result of the vibration tests based on a similar leak in module No. 4. Efforts to seal the leak by welding were unsuccessful. The module was put into the performance test loop with a constant supply of argon to maintain an inert atmosphere for the duration of the tests.

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FIG. II-39. SNAP 29 SPRING-PISTON MODULE POWER HISTORY

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FIG. II-39. (continued)

TABLE II-20
SNAP 29 Spring-Piston Module Data

<u>Module</u>	<u>S/N 001</u>		<u>S/N 002</u>		<u>S/N 003</u>		<u>S/N 004</u>		<u>S/N 005</u>	
Date	10/28	3/21	2/8	3/19	11/7	3/22	11/12	3/26	11/25	3/20
Accumulated operating time	0	3406	0**	944**	0	3273	0	1988	0	2788
Average hot junction temperature (°F)	1046	1055	1055	1062	1043	1054	1040	1052	1054	1050
Average cold junction temperature (°F)	344	355	332	341	343	333	339	343	339	334
Average bulk coolant temperature (°F)	225	255	240	253	254	253	253	258	243	253
Measured power (watts)	97.4	92.4	112	107.5	103.7	107.5	103.5	91.8	108.9	105.4
Corrected power at T _H = 1050°F, T _C = 350°F (watts)	97.4	92.8	105.7	102.8	103.6	102.8	103.2	89.9	105.6	101.8
Contact resistivity ($\mu\Omega\text{-cm}^2$)	650	1135	-225	110	30	110	70	1425	-120	210
Open-circuit voltage (volts)	7.23	7.10	7.53	7.47	7.45	7.83	7.69	7.65	7.81	7.83
Predicted open-circuit/ open-circuit	1.050	1.071	0.034	1.045	1.017	0.992	0.983	1.002	0.989	0.986
Load voltage (volts)	3.74	3.52	3.78	3.70	3.77	3.93	3.85	3.85	3.93	3.92
Cold end ΔT (°F)	119	100	92	88	89	80	86	85	91	81
R _I (milliohms)	133.7	136.6	126.5	129.6	133.9	142.4	143.0	159.5	140.1	145.5
Degradation (%)*		4.6	--	2.25		1.9	--	9.4 [†]	--	4.8
Degradation/1000 hr (%)*		1.4	--	2.4		0.58	--	7.8 [†]	--	1.7

* Degradation based on least square fit of corrected power.

**Since dynamic test

See IDC 1340-333A for vibration test data.

[†]Based on accumulated time starting on 2/5.

When S/N 004 developed a gas leak during life testing, the opportunity was taken to fill the module with helium and take a complete set of thermal data. The effect of replacing the argon with helium was a reduction in cold end ΔT of $\sim 15^\circ\text{F}$. This reduced the average ΔT from 92°F to 77°F . A report has been issued on the results. An approximation of the effect on the system is that the power output would increase 3.3%, but the system efficiency would decrease 4% due to the increased parasitic heat loss of about 80 watts per module. The exact effect, including that on system weight, would call for a more detailed study; but it appears that, if any benefits are to be gained, it would be only for a specialized application.

b. Diaphragm module design and development

Fabrication of the predevelopment diaphragm module was completed. This design uses a diaphragm in lieu of pistons and springs for pressure loading the thermoelectric elements. Difficulty was experienced in sealing the module case in the area of the fusion weld of the skirt to the heat sink.

The module was placed in the performance test loop with a constant supply of argon and subjected to initial performance testing. The hot junction temperatures in the center of the module were excessively low, indicating a possible loss of pressure in the diaphragm. The module was cooled down and removed from the fixture. Examination of the module disclosed a crack in the diaphragm pressure port. The crack was repaired and a tube welded onto the pressure port to allow for pressurizing the diaphragm after bringing the hot plate up to operating temperature. The module was reinstalled in the fixture and returned to the performance test loop. Hot junction temperature gradients were slightly improved but were still excessive. Measured power output was very low (approximately 70 watts) as shown in Fig. III-12 in Chapter III.

The module was removed from the test loop and disassembled. The diaphragm was severely buckled. (See the Fifth Quarterly Progress Report, Fig. II-43.) Further disassembly of the hot plate disclosed no effective bond of the braze joint between the inner sheet of the diaphragm and the beryllium plate. The inner sheet was also buckled, as shown by Fig. II-40. The heat sink/T/E conversion assembly portion of the module has been salvaged for use in a beryllium plate creep test.

The low output power of the diaphragm module can be attributed to three things. First, during the T/E conversion assembly fabrication, it was deemed advisable to replace certain couples. During this process, two couples were inadvertently reversed, which reduces the power output equivalent to the output of four couples. Second, the poor thermal distribution at the hot junctions resulted in a low average hot junction temperature and, thus, a low output. Third, the contact resistance of the module was higher than any of the five piston-type modules, and this factor alone reduced the power expectancy to approximately 90 watts.

Components for a simulated module hot plate have been fabricated for evaluation of the creep of the beryllium hot plate during operating conditions. The plate consists of a Haynes-25 frame, a beryllium plate with dummy NaK tubes, a Haynes-25 sheet for the outer cover and a diaphragm of two Haynes-25 sheets with a pressure port welded to the inner sheet. The frame, beryllium block, tubes, outer cover and the inner sheet of the diaphragm will be brazed together after electron beam welding the Haynes sheets to the frame. Brazing will be accomplished in a fixture which applies a uniform pressure across the braze joint. The outer sheet of the diaphragm will be electron beam welded to the inner sheet and to the frame. The hot plate will be assembled to the heat sink/TE conversion assembly. A Haynes-25

FIG. II-40. DIAPHRAGM MODULE HOT PLATE

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skirt will be installed around the two components and electron beam welded to the heat sink and to the hot plate. The module will be installed in the performance test loop, and creep test measurements of the hot plate will be taken. Thermal performance of the module will also be evaluated. The assembly is being held until the braze development problem is under control. Several Haynes-25-beryllium specimens will be evaluated to determine the best braze parameters.

The feasibility of electron beam welding the skirt to the heat sink was investigated. This method will reduce the heat input to the material during welding and possibly reduce the tendency to crack. A small Haynes-25 frame and skirt assembly was fabricated and subjected to a heat cycle of 1000° F prior to welding. The weld was accomplished without any difficulty. A dimensional check of the assembly showed no warpage due to welding. A helium leak check also showed no leaks either in the weld or in the material adjacent to the weld.

The diaphragm module hot plate assembly was analyzed to predict thermal performance characteristics. The results indicate that the hot plate surface temperatures will be ~1175° F, with edge temperatures ~25° F higher (Fig. II-41). The required heat to the module is ~1780 watts, and the parasitic losses will be ~136 watts at operating temperatures of 1050° F at the hot junction and 350° F at the cold junction. The heat rejected is ~1685 watts. These results apply to a nominal 0.01-inch helium-filled diaphragm gap.

The heat sink of the diaphragm module was analyzed to determine the temperature gradients. The results show that the heat sink temperature differential from Haynes clad to the heat pipe will be ~4.6° F (Fig. II-42).

c. Thermoelectric couples

The 2B series of property measurements on 3P elements have shown changes in Seebeck (α), resistivity (ρ) and thermal conductivity (K) from 3M published data and from earlier SNAP 29 measurements (7B series). Thermoelectric couple analysis shows that a system built with the 2B series of elements would be only ~95% as efficient as a system employing the 7B series of elements. These results are reported in detail in Appendix F.

A comparison of the $(\Delta T)^2$ and the $(EOC)^2$ methods of normalizing the SNAP 29 two-hour couple power check data was made. The results showed that there is a variation in the lot average couple power between either method but that the overall average couple power based on lots Nos. 29-003 through 29-017, except 29-004, are not significantly different. Also, the standard deviations of the couple power as determined by either method are not significantly different.

d. Extended mission SNAP 29

The performance of a SNAP 29 RTG after the nominal end of life (i.e., 134 days after fueling) was analyzed. Assuming no thermoelectric degradation after EOL and assuming a 350° F cold junction temperature at EOL, the analysis shows that 50 watts/module are obtainable for a period of 80 days after the RTG thermal control system shutters close (nominal EOL). Figures II-43 and II-44 summarize the results of this study.

FIG. II-41. HOT PLATE SURFACE TEMPERATURE DISTRIBUTION--DIAPHRAGM MODULE

FIG. II-42. HAYNES-25 CLAD-TO-HEAT PIPE TEMPERATURE GRADIENT VERSUS HEAT REJECTED--DIAPHRAGM MODULE

Fixed load impedance
design radiator rejects
1675 watts at 300° F
for one module
EOL at 134 days

Maximum fueling
Minimum fueling

FIG. II-43. EXTENDED LIFE SNAP 29

Fixed load impedance
design radiator rejects
1675 watts at 300° F
for one module
EOL at 134 days

— Maximum fueling
— Minimum fueling

FIG. II-44. EXTENDED LIFE SNAP 29

E. THERMAL CONTROL SUBSYSTEM--CONTROL POINT 1500

The objective of the effort within this control point is to develop a thermal control system which will maintain the thermoelectric module hot junction temperature within the prescribed limits during generator operation.

The thermal control system consists of a shutter assembly (six shutters); an actuator assembly (two sets, each consisting of an actuator, reservoirs, interconnecting tubing); and a linkage and bearing assembly which interconnects the actuators to the shutter. The system working fluid is the eutectic alloy of NaK.

Development of the complete system is achieved in the following sequence:

CP 1540--Thermal Control Subsystem, Component Development

CP 1530--Thermal Control Subsystem, Design Verification

CP 1520--Thermal Control Subsystem, Demonstration Unit

1. Summary of Accomplishments

a. System design

All CP 1530 system drawings have been completed and released, with the exception of the system assembly and installation drawing, which will be released early in the next quarter. The major differences in this design from the CP 1540 design consist of shorter shutters (four-module size), the use of torsion springs in place of extension springs to provide a shutter closing force, a separable link to permit fuel block separation without severing NaK lines and the elimination of the shutter latch at the 90° open position. In addition, the NaK reservoirs are integral with the module hot plate.

b. Design verification test

The design of the CP 1530 design verification test system was completed during this period. This system will be included in a combined test with the CP 1940 housing with dummy modules acting as a test fixture, and a CP 1230 electrical heat source providing the thermal inputs. This should provide the most realistic test possible. The rough draft of the combined test specification has been circulated for comment. The test will be conducted in three phases, as follows:

Phase I--Dynamic shock and vibration tests at operating temperature in an in-air environment

Phase II--Thermal vacuum performance tests

Phase III--Separation tests.

The final draft of the test specification will be completed and released during the next quarter.

A report was issued this quarter documenting the results of a thermal analysis which defined the design characteristics of the simulated thermoelectric elements to be used in the dummy module. The analysis determined that the T/E elements could be simulated by the use of 321 stainless steel, 0.5-inch OD, 12-mil wall, 0.58-inch long tubes. Each tube replaces its counterpart T/E element and is brazed on both ends. The Min-K insulation design is identical to that used in the actual module. Transport of the heat from the module heat sink will be accomplished by a water-cooled plate in place of the beryllium heat sink and heat pipe heat rejection system.

F. HEAT PIPE HEAT REJECTION SUBSYSTEM--CONTROL POINT 1800

The objective of this effort is to develop a static heat rejection subsystem capable of dissipating the waste heat from the thermoelectric module subsystem to a space environment over the life of the mission. Development has been divided into the following sequential efforts.

CP 1840--Heat Pipe Heat Rejection Subsystem - Component Development

CP 1830--Heat Pipe Heat Rejection Subsystem - Design Verification

CP 1820--Heat Pipe Heat Rejection Subsystem - Demonstration Unit

1. Summary of Accomplishments

a. Selection of primary and backup heat pipe designs

Early this quarter, a milestone was reached in the heat rejection subsystem development with the selection of the primary and backup heat pipe designs. The selected configurations are given below:

<u>Component</u>	<u>Primary</u>	<u>Backup</u>
(1) Wick: Evaporator	3 layers, 30 mesh screen	3 layers, 30 mesh screen
Condenser	20-mil annulus, 200 mesh screen	35-mil corrugated, 200 mesh screen
(2) Tube: OD, thickness	3/8 in., 10 mils	3/8 in., 10 mils
Length	15 ft	15 ft
(3) Materials: (all)	Nickel 200	Nickel 200
(4) Other	Nickel 200 internal helical spring	No internal spring
(5) Wick joint:	Butt	Butt
(6) Transport fluid:	Water	Water

Early in the development program, screening tests conducted on 7-foot long, 7/16-inch OD stainless steel heat pipes employing various wick geometries led to the selection of hybrid wick heat pipes as having the best potential to meet the SNAP 29 goals of transporting more than 200 watts through a 15-foot, 3/8-inch OD heat pipe. (For a discussion of these results, and the reasons for selection of the hybrid wick, see the Fifth Quarterly Progress Report and Table II-39 therein.)

Screening tests conducted on a variety of hybrid wick combinations installed in straight, 8-foot, 3/8-inch OD, stainless steel pipes, led to the selection of the 3-layer, 30 mesh evaporator wick based on heat transport capability and overall heat pipe temperature drop. (For results of all 8-foot, hybrid wick heat pipe screening tests, see the Fifth Quarterly Progress Report, Table II-39.) A comparison of the performance of three condenser wick geometries with the 3-layer, 30 mesh evaporator is presented in Fig. II-45. Despite the obvious superiority of the dual artery and the corrugated condensers shown in Fig. II-45, the requirement to bend the condenser section of the SNAP 29 heat pipe in order to wrap the radiator around the vehicle, delayed condenser selection until the results of the bent heat pipe tests were available.

FIG. II-45. HEAT TRANSPORT VERSUS TRANSPORT TEMPERATURE COMPARISON OF CONDENSER WICK GEOMETRIES

Tests on bent corrugated and annular condensers with the 3-layer, 30 mesh evaporator proved that the annular configuration suffered less performance degradation than the corrugated. Figure II-46 compares the performance of straight and bent (4-foot radius bends) annular and corrugated condensers when tested with the 3-layer, 30 mesh evaporator. The dual artery condenser was not tested because manufacturing evaluation and tests indicated that bending this configuration without excessive wick deformation would be difficult and costly. Further tests of the 3-layer, 30 mesh annular geometry in various bent configurations (including one mitre joint) confirmed that bending did not seriously degrade its static performance (see Table II-21).

TABLE II-21

Summary of Results of 8-Foot, Bent, Hybrid Wick Heat Pipe Tests

<u>Wick Geometry</u>	<u>Bend Radius (ft), Location</u>	<u>q_{max}, 300° F Level (watts)</u>	<u>Total Heat Pipe ΔT (°F)</u>	<u>Comments</u>
3-30/annular	4, condenser	255	14	Compare with 247 watts for straight pipe (see Fig. II-46). Also subject to acceleration tests.
3-30/corrugated	4, condenser	232	14	Compare with 272 watts for straight pipe (see Fig. II-46). Also subject to acceleration tests.
3-30/annular	2, condenser	252	10 and 11	Condenser screen slit and overlapped for bending.
3-30/annular	1, evaporator	262	15 to 18	Bent without mandrel.
3-30/annular	45° mitre joint, condenser	247	9 to 10	Good performance over 200° to 400° F (see February 1968 Monthly Progress Report).

Before final selection of the primary and backup wick designs, the 3-30/annular and 3-30/corrugated designs were subject to vibration, shock and acceleration testing. These tests and their results were discussed in detail in the SNAP 29 Monthly Progress Reports for January and February 1968 and are reviewed in this section. In general, both wick configurations performed well and exhibited excellent capability to recover from transient dryout conditions induced by the more severe of the environmental exposures to which they were subjected.

An ancillary step in the selection process was to determine the effects of condenser length (i. e., 15 feet versus 8 feet) on the performance and fabricability of each condenser type. Comparison of the primary and backup designs in both 8-foot and 15-foot long pipes is presented in Table II-22. As anticipated, based on hybrid wick theory (i. e., evaporator wick flow resistance is controlling), the performance degradation was about 10% or less between 8-foot and 15-foot pipes for either wick configuration.

FIG. II-46. MAXIMUM HEAT TRANSPORT VERSUS ELEVATION COMPARISON OF STRAIGHT AND BENT HEAT PIPES

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TABLE II-22
Comparison of 8-Foot and 15-Foot Stainless
Steel, Hybrid Wick, Straight Heat Pipes

<u>Wick Geometry</u>	<u>OD (in.)</u>	<u>Length (ft)</u>	<u>q_{max} 300° F Level (watts)</u>	<u>Total Heat Pipe ΔT (°F)</u>
3-30/annular	3/8	8	247	13
3-30/annular	3/8	15	220	13
3-30/corrugated	3/8	8	272	14 and 15
3-30/corrugated	3/9	15	257	13 and 14

The selection of primary and backup designs was based on the results of stainless steel-water heat pipe tests. However, difficulties in bonding the stainless steel evaporator section to the thermoelectric module beryllium heat sink resulted in a decision to change the heat pipe tube and wicking material from stainless steel to Nickel 200. A number of other reasons favored the use of nickel viz stainless steel in addition to improving the module-heat pipe bond. It was believed that use of nickel would facilitate the bonding of the heat pipe condenser section to the radiator aluminum armor piece, decrease the overall heat pipe temperature drop due to the higher thermal conductivity of nickel and, in theory, promote better wetting with the water transport fluid. In addition, life tests on stainless steel-water heat pipes showed a tendency for gas (hydrogen) buildup with operating time. It was judged that the use of nickel, properly cleaned, would minimize the gas buildup problem.

Accordingly, a number of 15-foot long Nickel 200 heat pipes employing the primary and backup wick design were fabricated and tested to ascertain:

- (1) The validity of the wick choices based on the stainless steel tests.
- (2) Proper cleaning procedures.
- (3) Whether gas buildup was a problem with nickel.

While some of these tests are still in progress, the initial series of nickel pipe tests (see Table II-23) proved that, with proper cleaning and preconditioning of the nickel material, performance as good as that measured with the stainless steel pipes was obtainable. Table II-23 compares stainless steel and Nickel 200 heat pipes with identical wick geometries. The primary and backup wick configurations are thus based on the use of Nickel 200 material as indicated in the initial part of this section. Additional test results from the nickel heat pipe program are discussed in the following paragraphs.

b. Heat pipe dynamic environmental tests

During this quarter, the primary and backup heat pipe designs were subject to vibration, shock and acceleration tests.

TABLE II-23

Comparison of Straight Stainless Steel and Nickel 200 Heat Pipes

<u>Wick Geometry</u>	<u>Material</u>	<u>OD (in.)</u>	<u>Length (ft)</u>	<u>$q_{\max}^*$ (watts)</u>	<u>Total ΔT (°F)</u>	<u>Comments</u>
3-30/annular	SS	3/8	15	220	13	Standard process
3-30/annular	Ni	3/8	15	235	8	Standard** + air bake
3-30/corrugated	SS	3/8	15	257	13 and 14	
3-30/corrugated	Ni	3/8	15	272	7 and 8	Standard** + air bake

*300° F level

**Standard + air bake--cleaned by standard process used for stainless steel pipes and baked in air at 750° F for 1-1/2 to 2 hours.

(1) Vibration and shock tests

The two heat pipe configurations which were subjected to a typical booster vibration and shock environment had identical 3-layer, 30 mesh evaporator sections, with one employing an annular condenser and one a corrugated condenser. The mechanical vibration tests were performed on the C-210 electrodynamic vibrator, and the shock tests on a Barry shocker (arresting medium--sand). The performance of each pipe while mounted on the test fixture was determined before each test for comparison with the performance during and after the test. The pipes were installed one above the other in the same test fixture (see Figs. II-47 and II-48) and tested simultaneously. The annular pipe was mounted above (or outboard of) the corrugated pipe and, for the same electrical power input, stabilized at about 20° F higher temperature than the bottom (or inner) corrugated pipe due to higher heat losses through the bottom of the fixture. The tests conducted and a summary of the results are given in Table II-24. Plots of thermocouple readings taken during testing are presented in Figs. II-49 to II-53.

In general, the results of both the shock and vibration tests were very encouraging. No significant effects were noted by exposure to the shock environment. Despite the fact that the temperatures in the first part of the evaporator did rise significantly during some of the vibration tests, recovery without changing the power input or attitude of the heat pipe indicates that the heat pipe was not forced into an unstable drying out condition. Further, the low operating temperatures of the heat pipes, coupled with a relatively high estimated heat input to each pipe (i. e., 275 watts less tare heat loss), probably resulted in a heat pipe operating condition closer to the $q_{\max}$ condition than will be experienced in practice.

(2) Acceleration test

Acceleration tests were conducted on the primary and backup wick configurations by installing them in two, 8-foot long, 3/8-inch OD heat pipes in which the condensers had been bent to a 4-foot radius. The heat pipe condensers were bent to match the radius of the centrifuge arm, thus assuring an acceleration vector transverse to the pipe when the centrifuge reached a steady rotation rate. These two bent configurations, i. e., 3-30/annular and 3-30/corrugated with 4-foot radius bends in the condenser section, had been static performance tested previously so that their performance characteristics before the acceleration tests were well known. Both heat pipes were tested simultaneously, by mounting them in the same fixture side-by-side with the

FIG. II-47. VIBRATION TEST ARRANGEMENT

FIG. II-48. SHOCK TEST ARRANGEMENT

TABLE II-24

Summary of Heat Pipe Environmental Tests

Run No.	Type Test	Environment	Heat Pipe Inputs and Condition	Results, Comments
1	Longitudinal axis, low level random 10.26 g (rms) vibration duration--5 minutes	400 to 1000 cps at 0.082 g ² /cps. Roll off below 400 at 3 db octave. Roll off above 1000 at 6 db octave.	275 w(e) each. Heater annular temperature--200° F. Corrugated temperature--180° F.	No noticeable effect on either heat pipe. (Top pipe--annular pipe.)
2	Longitudinal axis, design level random 29.1 g (rms) vibration duration--3 minutes	400 to 1000 cps at 0.65 g ² /cps. Roll off below 400 at 3 db octave. Roll off above 1000 at 6 db octave.	275 w(e) each. Heater annular temperature--200° F. Corrugated temperature--180° F.	<u>Annular:</u> Maximum evaporator rise to ≈13° F; started to recover 3 to 5 minutes after test with no power change. <u>Corrugated:</u> Maximum evaporator rise during test ≈69° F; started to recover 3 to 5 minutes after test with no power change.
3	Longitudinal axis, design level sinusoidal vibration duration--25 minutes	5 to 14 cps--0.10 (P-P) 14 to 400 cps--1.0 g 400 to 3000 cps--3.0 g. Constant octave sweep rate.	275 w(e) each. Heater annular temperature--198° F. Corrugated temperature--180° F.	<u>Annular:</u> Slight rise in evaporator temperature at low frequencies, but recovered during test at higher frequencies. <u>Corrugated:</u> Maximum temperature rise 60° F at lower frequencies, but recovered rapidly during test at higher frequencies (≈500 cps).
4	Longitudinal axis, repeat of design level random 29.1 g (rms) vibration duration--3 minutes	Same as test run No. 2 above.	275 w(e) each. Heater annular temperature--200° F. Corrugated temperature--180° F.	<u>Annular:</u> Slight evaporator temperature rise during test followed by a 15° to 20° F drop due to redistribution of H ₂ O; normal in 3 minutes. <u>Corrugated:</u> Maximum evaporator rise ≈36° F, but recovered rapidly during test.
5	Transverse axis, low level random 10.26 g (rms) vibration duration--5 minutes	Same as test run No. 1 above.	275 w(e) each. Heater annular temperature--200° F. Corrugated temperature--180° F.	<u>Annular:</u> Slight adjustment of pipe temperatures during test, but no dryout indicated. <u>Corrugated:</u> No effects noticeable.
6	Transverse axis design level random 29.1 g (rms) vibration duration--3 minutes	Same as test run No. 2 above.	275 w(e) each. Heater annular temperature--219° F. Corrugated temperature--200° F.	<u>Annular:</u> Maximum evaporator rise 20° F occurred two minutes after test but started to recover immediately thereafter. <u>Corrugated:</u> Maximum evaporator rise ≈40° F occurred one minute after test, but started to recover immediately thereafter.
7	Transverse axis design level sinusoidal vibration duration--25 minutes	Same as test run No. 3 above.	275 w(e) each. Heater annular temperature--216° F. Corrugated temperature--199° F.	<u>Annular:</u> Virtually no effect. <u>Corrugated:</u> Virtually no effect.
8	Transverse axis -Y design level shock	Half sine shape pulse of 40 g peak amplitude--8 millisecond duration, 3 times.	275 w(e) each. Heater annular temperature--195° F. Corrugated temperature--176° F.	<u>Annular:</u> Essentially no effect. <u>Corrugated:</u> Essentially no effect.
9	Transverse axis +Y design level shock	Same as test run No. 8 above.	275 w(e) each. Heater annular temperature--193° F. Corrugated temperature--174° F.	<u>Annular:</u> Essentially no effect. <u>Corrugated:</u> Essentially no effect.

FIG. II-49. HEAT PIPE DESIGN LEVEL, RANDOM VIBRATION TEST (29.1 g (RMS))--LONGITUDINAL AXIS--RUN NO. 1

FIG. II-50. HEAT PIPE DESIGN LEVEL, SINUSOIDAL VIBRATION TEST--LONGITUDINAL AXIS

FIG. II-51. HEAT PIPE DESIGN LEVEL, RANDOM VIBRATION TEST (29.1 g (RMS))--LONGITUDINAL AXIS--RUN NO. 2

FIG. II-52. HEAT PIPE DESIGN LEVEL, RANDOM VIBRATION TEST (29.1 g (RMS))-- TRANSVERSE AXIS

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FIG. II-53. HEAT PIPE DESIGN LEVEL, SINUSOIDAL VIBRATION TEST--TRANSVERSE AXIS

annular pipe on a slightly smaller radius. Both heat pipes were mounted with a small positive elevation, simulating the planned mounting on a vehicle. Prior to the test, the pipes were stabilized at a power input to the common heater block of 400 w(e), i. e., about 200 w (e) per pipe. Because of higher tare heat losses to the test fixture, the annular (inner pipe) stabilized at about 25° to 30° F lower than the corrugated pipe; that is, the pretest transport temperatures were about 260° F for the annular pipe, and 290° F for the corrugated pipe. The test consisted of a relatively rapid acceleration to 15 g, steady-state operation at 15 g for 8 minutes and a rapid deceleration to zero rpm in 20 seconds. Figures II-54 and II-55 present plots of three thermocouples per pipe monitored continuously before, during and after the test run. In comparing the first and third thermocouple readings on the annular pipe with the second and fourth thermocouple readings on the corrugated pipe, it is apparent that most of the water was forced into the upper end of the evaporator section, thus starving a majority of the evaporator section as well as preventing "heat piping" out into the condenser. The large decrease in the condenser temperature is due not only to convective cooling over the rotating pipes (uninsulated), but to a temporary lack of "heat piping" during the run. It can be seen on Figs. II-54 and II-55 that, as soon as the fixture decelerated, the pipes started to recover and were within a few degrees of their pretest temperatures within 10 minutes. In interpreting the results, it should be noted that, during the initial acceleration, the rotation of the centrifuge was such that the water in the pipe would be driven towards the evaporator. Once the centrifuge reached steady state, there was a slight component of the normal acceleration vector along the evaporator section due to the transition involved in matching a straight evaporator section to a condenser section bent to the accelerometer arm radius.

The results of this test were very encouraging in that despite the fact that a large section of the evaporator of each pipe started to dry out while the input was constant, both configurations recovered readily.

c. Heat pipe static performance and life tests

By the end of the last quarter, the first nickel heat pipe had been built and placed on test. The pipe was 15 feet long, 3/8 inch in OD, had a 35-mil wall tube, (note: all subsequent nickel pipes have had 10-mil wall tubes), a 3-30/annular wick configuration and was cleaned using the standard cleaning process previously used for all stainless steel pipes. The pipe did not perform well and, after extensive examination (visual, X-ray, etc.), it was concluded that the poor performance could only be attributed to poor wetting of the wick. The pipe was then reconditioned by baking out in air for two hours at 750° F. The subsequent results are presented in Table II-25. The improvement in performance as a result of air baking was again verified by the results of a series of tests on the primary and backup wick designs, using 15-foot long, 3/8-inch OD, 10-mil wall nickel pipes as shown in Table II-25.

The air bake produces an oxide coating on the exterior surface of the heat pipe which must be removed before bonding to either the module heat sink or radiator. Also, because the air bake introduces two additional steps in the fabrication and assembly process, a series of tests on nickel heat pipes is currently in progress to determine whether a cleaning process is available which will produce performance results equivalent to the air bake process. To date, several processes have been tested as shown on Table II-25. In the first test, the condenser section was cleaned with a mixture of acids which was shown previously to promote good wetting between water and nickel. However, as shown, this pipe built up gas rapidly as a result of deposits left in the screen which were not easily removable. In addition, the acid mixture attacked the screen rather severely and embrittled the nickel wire.

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FIG. II-54. TEMPERATURE-TIME HISTORY FOR HEAT PIPE 15 g ACCELERATION TEST--
3-30/ANNULAR HEAT PIPE

FIG. II-55. TEMPERATURE-TIME HISTORY FOR HEAT PIPE 15 g ACCELERATION TEST--
3-30/CORRUGATED HEAT PIPE

TABLE II-25

Summary of Nickel Heat Pipe Tests
(All pipes 3/8 in. OD, 15 feet long)

<u>Wick Geometry</u>	<u>Cleaning and Preconditioning</u>	<u>q_{max}, 300° F Level (watts)</u>	<u>Total Heat Pipe ΔT (°F)</u>	<u>Comments</u>
3-30/annular (35-mil wall)	Standard SS cleaning	--	--	Poor performance; did not wet properly.
3-30/annular (35-mil wall)	Standard SS cleaning plus air bake at 750° F --2 hours	260 to 270	10 to 12	
3-30/corrugated (10-mil wall)	Standard SS cleaning	202	8	
3-30/corrugated (10-mil wall)	Standard SS cleaning plus air bake at 750° F --1-1/2 hours	272	7.5	
3-30/annular (10-mil wall)	Standard SS cleaning	222	12	
3-30/annular (10-mil wall)	Standard SS cleaning plus air bake at 750° F --1-1/2 hours	235	8.5	
3-30/corrugated (10-mil wall)	Evaporator: concen- trated HNO ₃ Condenser: mixed acids			Generated large amount of H ₂ gas.
3-30/corrugated	Evaporator and con- denser: HNO ₃	312	10	Does not appear to be building gas but will be life tested to verify.

In the second test, the entire pipe was cleaned with concentrated nitric acid. The performance of this pipe appears as good as a comparable pipe preconditioned by air baking. Additional testing and life testing is required to assure that this result is valid and repeatable and that there is not a gas generation problem. The tests planned for the next period will be concentrated on resolving the cleaning and preconditioning procedures and determining the problem, if any, of gas generation in nickel pipes.

d. Cold soak test

A 15-foot nickel heat pipe was placed in a level position and allowed to cold soak at below freezing temperatures for approximately 12 hours. It was found that this heat pipe could be started using a low power input but would not start up using the high power input. The heat pipe was then placed in a vertical position and allowed to freeze. When returned to the level position and startup was attempted (while frozen), the heat pipes would not operate. Subsequent examination by X-ray showed the condenser screen to be permanently deformed and damaged by the expansion of the water column as it froze.

e. Mitre joint evaluation

A mitre joint heat pipe was made with a 40° mitre joint in the condenser section. This heat pipe has successfully passed the performance tests with a q (maximum) of approximately 250 watts and is presently on life test.

f. Analysis of effects of nonoperating heat pipes

An analysis has been completed which evaluates the thermal effects resulting from one or more heat pipes becoming inoperative due to drying out after being punctured by a meteoroid. A report documenting the results of this analysis will be issued in the next quarter. The thermal model of the beryllium heat sink coupled to the radiator panel for one module, is shown in Fig. II-56. The results of the study show that, even under normal operating conditions, the heat distribution to the 10 heat pipes is not uniform, due to the presence of the thicker and wider fins at the edges of each panel which tend to draw more heat into the two end heat pipes. These results are shown in Table II-26. The radiator panel temperature distribution for the normal case is shown at the bottom of Fig. II-57 ($\epsilon = 0.9$). Removing the radiator thermal control coating from the end fin areas produces a more uniform heat distribution, as shown in Table II-26 and a more uniform temperature distribution as shown in Fig. II-57. However, it increases the radiator temperature and thus cold junction temperature by an unacceptable 25° F. The nonuniform heat distribution is accentuated when one end heat pipe dries out as shown on Table II-26. In fact, the heat transport required of the adjacent pipe exceeds the heat transport capability of the present heat pipe design, even in the case where the end fin coating is removed. The values shown in parentheses represent the distribution when the diaphragm module generator capability is matched to the existing radiator heat rejection capability. Under matched conditions, the heat to be rejected decreases from the 1700 watts initially assumed to 1650 watts with the end pipe failed; thus, the heat transport requirements are only slightly reduced as shown.

Continued operation of the system is possible if any pipe except an end pipe "dries out." The "end pipe out" condition will be actually more severe than indicated by the model analyses because attachments to the vehicle are required both at the extreme edges of the stack of radiator panels and at the splice areas between individual panels, thus flowing even more heat into the end pipes.

FIG. II-56. BERYLLIUM HEAT SINK-RADIATOR PANEL THERMAL MODEL

TABLE II-26

Heat Pipe Transport Requirements for Various Failure Conditions
(Radiator emittance 0.9, except as noted. See Fig. II-56 for pipe location)

Heat Transported/Pipe (watts)

<u>Heat Pipe Number</u>	<u>All Operating</u>	<u>No. 1 Failed</u>	<u>No. 2 Failed</u>	<u>No. 3 Failed</u>	<u>No. 4 Failed</u>	<u>No. 5 Failed</u>	<u>No. 1 and 2 Failed</u>	<u>No. 1, 2 and 3 Failed</u>	<u>No. 5 and 6 Failed</u>	<u>All Operating*</u>	<u>No. 1 Failed*</u>
1	194 (189.09)	0 (0)	273.8	212.6	199.8	196.3	0	0	198.7	166.2	0
2	171.8 (167.45)	310.3 (301.17)	0	233.1	186.3	176.1	0	0	181.5	172.3	292
3	163 (158.88)	197.4 (191.59)	229.5	0	222.8	177.3	415.2	0	192.8	170.8	200.3
4	160.9 (156.83)	171.2 (166.16)	176.9	221.5	0	219.6	226.4	516.3	277	170.4	179.3
5	160.3 (156.24)	164 (159.17)	165.2	174.8	219.4	0	181.5	255.0	0	170.2	173.4
6	160.3 (156.24)	162.2 (157.43)	162.1	164.7	174.6	219.7	169.1	193.1	0	170.2	171.8
7	160.9 (156.83)	162.3 (157.53)	161.8	162.5	165.2	175.1	166.1	176.3	277	170.4	171.4
8	163 (158.88)	164.2 (159.37)	163.6	163.8	164.5	167.4	167.1	173.3	192.8	170.8	171.7
9	171.8 (167.45)	173.3 (168.2)	172.7	172.7	172.9	173.7	176.2	181.5	181.5	172.3	170.2
10	194 (189.09)	195 (189.26)	194.3	194.2	194.3	194.7	198.4	204.2	198.7	166.2	166.9

*Edges of radiator panel (splice area) uncoated; emittance is 0.2.

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FIG. II-57. RADIATOR TEMPERATURE PROFILE WITH AND WITHOUT COATING ON END FINS

Investigations of possible design changes to cope with an "end pipe out" condition have tentatively concluded that additional meteoroid protection over the end pipes is probably the best corrective action. Since the current meteoroid armor thickness is based on the assumption that only one heat pipe out of the total of 40 may be punctured, regardless of position, the required armor thickness for four allowable hits, one per module, is not expected to be significantly increased when the hits are restricted to any pipe except an end heat pipe.

G. RTG HOUSING STRUCTURAL SUBSYSTEM--CONTROL POINT 1900

The objective of the effort within this control point is to develop a minimum weight housing structure which maintains the parasitic heat losses within acceptable limits, supports the various subsystems in their proper orientation, provides for remote loading of the fuel block, provides for ejection of the fuel block and provides a means of attaching the RTG to the vehicle supports. As part of the reprogramming, instrumentation is now covered under the individual control points where it is used.

Development of the complete structure is achieved in the following sequence:

CP 1940--RTG Housing Structural Subsystem, Component Development

CP 1930--RTG Housing Structural Subsystem, Design Verification

CP 1920--RTG Housing Structural Subsystem, Demonstration Unit

This report includes work conducted under CP 1940 during this reporting period.

1. Summary of Accomplishments

a. Component design

The design of the CP 1940 component development housing is in progress. This design is based upon the four-module RTG size. Drawings are approximately 60% complete at this point. All drawings will be complete and released during the next quarter.

Two housing assemblies will be built. One is to be included in the fuel block vibration test and the other will be included in the combined 1940, 1530, 1230 tests. The latter test will be conducted in three phases as follows:

Phase I --Dynamic shock and vibration tests at operating temperature

Phase II --Thermal vacuum test

Phase III--Fuel block separation test.

A rough draft of the test specification has been written and circulated for comments. The final draft will be written and released during the next reporting period.

b. Design studies

Two design studies were completed during this period. The first consisted of an evaluation of all the springs used in the RTG in order to optimize the system weight. The results of this study indicated that approximately 4.8 pounds per RTG could be saved by changing to a torsion spring design from the original compression spring. The door opening springs were also evaluated and changed from a compression spring cartridge to a torsion spring design. The shutter ejection spring design was changed from a single compression spring to a nested compression spring design, which results in a more compact unit. This study also included the CP 1530 shutter return springs, the design of which was changed to two torsion springs from two extension springs. This study is included in Appendix G.

The second study completed during this period was a launch pad abort-fuel block ejection study. The purpose of this study was to determine the feasibility of modifying the fuel block ejection system to accommodate the additional requirement of

clearing a fireball resulting from a major launch pad catastrophe. Three fireball radii were included: 100 feet, 350 feet and 650 feet. The results of this study indicated that this was not a feasible concept, particularly with respect to the larger fuel block sizes and fireball radii. In addition, the initiation system required to perform this task appears to have several major problems. This system would have to sense impending failure, differentiate between a major and minor failure and initiate the separation system while the vehicle was still in the vertical position, before the blast effects caught up to the fuel block. This study is included in Appendix H.

III. SYSTEMS EVALUATION AND INTEGRATION-- CONTROL POINT 3200

Control of the overall system baseline design, coordination and integration of SNAP 29 RTG system and its associated AGE into the simulated launch complex, and demonstration of system performance and reliability objectives are conducted under this task. Three subtasks were involved in performing this effort during the reporting period.

CP 3210--Systems Interface Evaluation

CP 3230--Reliability (Reported in Chapter IV)

CP 3240--Systems Test Integration

A. SUMMARY OF SIGNIFICANT ACCOMPLISHMENTS

Revision A-4 to the SNAP 29 Program Plan (MND2062-500, Revision A) was coordinated and delivered to the AEC during this reporting period.

Fourteen test series were completed during this period and three tests are in progress.

The module vibration test report was completed.

Module S/N 001 (piston heat sink) completed module life testing of 3600 hours.

The heat pipe dynamic test report was released.

B. SYSTEM INTERFACE EVALUATION--CONTROL POINT 3210

The objectives of the system interface evaluation include the dissemination of design constraint criteria and disciplines to control RTG/AGE systems design, development, fabrication, test and reporting to properly satisfy program requirements.

Logistic activities relating to program reporting, material/parts-release/control and liaison support of integration efforts are provided.

1. Program Plan

During the current period, Revision A-4 to the SNAP 29 Program Plan (MND2062-500, Revision A) was coordinated and delivered to the AEC. This revision covered deletion of the hypersonic pressure test and subsonic rotating force tests and added the following:

- (1) Hypersonic forced oscillation test
- (2) Hypersonic roll damping test
- (3) Hypersonic free flight test
- (4) Hypersonic yaw damping test.

C. SYSTEM TEST INTEGRATION--CONTROL POINT 3240

This section presents a brief overall review of the test status and results. Specific test results influencing the design of components and the system are being reported under the section covering the subsystem or task concerned.

1. Heated Fuel Capsule Liner Burst Test--CP 1140

The objective of this test was to obtain preliminary design information as to the ability of the tantalum alloy liner to provide fuel containment under re-entry conditions. Design calculation had established that re-entry temperatures would approach 3000° F, and internal capsule helium pressure would approach 1300 psi. Two burst tests were conducted on a SNAP 29 simulated capsule with a tantalum-10% tungsten inner liner and a platinum outer liner.

The first capsule was brought up to temperature, pressure increased and burst occurred with a loud report at a temperature of 2948° F and a pressure of 720 psi. A small hole about 1/4 inch in diameter was blown in the walls of the tantalum and the platinum sleeves. The nature of the failure seemed to indicate a localized hot spot caused either by the capsule being off center in the induction coil or an arc-over from the induction coil to the capsule. Arc-overs may leave copper at the arc-over point; however, no copper was found in the capsule failure.

The test schedule for the second capsule was to increase the temperature of the capsule from 1800° to 3000° F in 75 seconds and at the same time increase the internal pressure to 1300 psi as shown in Fig. III-1. The second capsule burst close to the design point. The temperature was increased from approximately 1800° to 2696° F in 120 seconds. The pressure at the time of burst was 1280 psi. The failure occurred as a longitudinal crack in the tantalum sleeve. At the time of failure, a loud hiss was noted and the pressure dropped rapidly. The platinum sleeve had increased in diameter at the failure zone from 0.656 to about 0.672 inch.

Metallographic examination was performed on the first capsule. A picture of the burst hole is shown in Fig. III-2. A microscopic examination is shown in Fig. III-3. The latter photograph clearly shows diffusion between the tantalum and the platinum.

The results of the second test are shown in Fig. III-4. The dark spot in the center of the photograph is the point where the platinum thermocouple touched the tantalum sleeve.

2. Fuel Block Vibration Test--CP 1140

The fuel block vibration test was completed on February 16, 1968. The test specimen was subjected to sine resonant frequency tests at 450- and 650-pound preloads, respectively. Then, under the 650-pound preload, the fuel block was successfully subjected to random vibration accelerations of 10.26 and 20 g rms. The level was increased to 29.1 g rms and the unit failed after 34 seconds of vibration acceleration as shown in Fig. III-5.

3. Plasma Arc Re-entry Heating--CP 1140

Plasma arc tests have been conducted to evaluate the effects of constant heat fluxes on two fuel block materials, ATJ graphite and Carb-I-Tex 500 needled graphite, and on reduced size models of the SNAP 29 fuel block. Results are discussed under Control Point 1100 in Chapter II of this report.

4. Vacuum Testing of Watlow Heaters--CP 1240

Three Watlow heaters were subjected to a vacuum environment test that was to be continued for 90 days. One heater open-circuited in 28 days and in 36 days a residue from the heaters caused the vacuum chamber ion pump to become inoperative.

FIG. III-1. SNAP 29 BURST PRESSURE TEST CAPSULE BURST POINTS

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III-4

FIG. III-2. HOLE MADE DURING SNAP 29 BURST TEST

Etchant:

Ta = mixed acids

Pt = HCl D.C. Electro

Magn. 250X

FIG. III-3. SNAP 29 BURST TEST CAPSULE, SHOWING Ta TO Pt INTERFACE

Macro 7X

FIG. III-4. SNAP 29 BURST TEST FUEL CAPSULE

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III-5

FIG. III-5. FUEL BLOCK AFTER FAILING AT 29.1 G RMS FOLLOWING 34 SECONDS OF ACCELERATION

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III-6

5. Heater Performance Test--CP 1240

Uneven temperature distribution associated with module heater blocks dictated that tests be conducted to determine the nature of the problem. Individual tests were conducted on eight Watlow heater and three heater test modules to determine the temperature profiles.

A temperature profile was conducted on eight Watlow heaters with a nominal power input of 1000 watts to determine the temperature variance along the length of the heater. A schematic of the test setup is shown in Fig. III-6. The test consisted of applying 1000 watts of power to the heater, allowing the temperature to stabilize and taking temperature readings along the length of the heater. The heaters have two areas along the length, other than the ends, that are cooler than the rest of the heater. Examination of the heaters has shown that the heater is constructed in three sections. Where the sections join, there are less heater wires and less heat generated causing a reduced temperature in this area. When heaters are placed in heater blocks, the effect of the cooler spots is not noticeable because of the heat distribution characteristics of the beryllium heater block.

Heater block temperature profile tests were conducted on three module heater blocks to establish the high and low temperatures across the face of the heater block. The allowable hot junction temperature establishes the maximum temperature of the heater block. Lower temperatures across the face of the module cause lower power to be generated by the module. These tests indicated an insufficient amount of insulation on the heater lead end of the heater block. This situation has been corrected.

6. Module Life Performance Tests--CP 1340

Five SNAP 29 thermoelectric modules are undergoing life performance tests in the steady state test loop. The number of test hours on each module is as listed:

<u>S/N</u>	<u>Test Time (hr)</u>
001	3600
002	1198
003	3400
004	2035
005	2965

Module S/N-001 completed the life performance test on March 29, 1968. Module S/N 004 developed an argon leak and testing was terminated on March 29, 1968. Testing of the other modules will continue during the next quarter. Performance test histograms of each of the modules are shown in Figs. III-7 through III-11.

7. Diaphragm Predevelopment Module--CP 1340

The diaphragm module was placed on test during this reporting period. The functional testing was started and after three days the test was stopped to repair a leak in the pressurization line to the diaphragm. On disassembly of the module, inspection of the hot plate revealed that sections of the 0.012-inch Haynes-25 clad sheet had separated from the beryllium block. The module was repaired and placed back on test. The internal pressure of the module diaphragm was set at 1.5 psig. The resultant output of the module at stabilization was 68 watts. The diaphragm was pressurized to 42 psig and the module stabilized at this pressure for 12 days. An output of 73 watts was obtained at these conditions. A performance test histogram is shown in Fig. III-12. The module was removed from test for evaluation and rework.

*Note: Shaded areas represent dead spots (low temperature of $\sim 3/4$ inch at 1, 4 and 10 inches along the sheath; at the lead end, the dead spot is ~ 2 inches).

FIG. III-6. INDIVIDUAL WATLOW HEATER TEST SETUP

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FIG. III-7. PERFORMANCE TEST HISTOGRAM, SNAP 29 MODULE S/N-001

FIG. III-8. PERFORMANCE TEST HISTOGRAM, SNAP 29 MODULE S/N-002

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FIG. III-9. PERFORMANCE TEST HISTOGRAM, SNAP 29 MODULE S/N-003

FIG. III-10. PERFORMANCE TEST HISTOGRAM, SNAP 29 MODULE S/N-004

FIG. III-11. PERFORMANCE TEST HISTOGRAM, SNAP 29 MODULE S/N-005

FIG. III-12. SNAP 29 DYNAMIC DIAPHRAGM MODULE, S/N-006

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8. Input Power Accuracy Evaluation--CP 1340

Module efficiency depends on the accurate measurement of power input to the SNAP 29 modules on test. The module test consoles contain two wattmeters, a Hall effect wattmeter and a standard Weston wattmeter, which do not agree within 10% or more in some cases. An investigation was undertaken to find causes for the discrepancies. The current transformer in the console was found to contribute the main source of error. A lesser source of error was found to come from placing the current coil of the Hall effect wattmeter in series with the current coil of the Weston wattmeter. Input watts can be read to an accuracy of 1% by placing the wattmeter directly in series with the load. The Hall effect wattmeter was accurate at the maximum power setting because it was calibrated for use with the current transformer.

9. Heat Pipe Dynamic Test--CP 1840

Two straight heat pipe configurations (annulus and corrugated) were subjected to vibration and shock testing. Random vibration testing consisted of vibration at 10 g rms for five minutes and 29 g rms for three minutes. Sinusoidal vibration testing consisted of a 3-g maximum, 5 to 3000 hertz for 25 minutes. The test was passed successfully. Temporary dryout during vibration was experienced, however. Shock testing consisted of the application of three shocks in two axes. Each shock was to 40 g for 8 milliseconds and half-sine shape. The heat pipes were not affected by the shock test.

Two curved heat pipe configurations (annulus and corrugated) were subjected to acceleration testing at 15 g for eight minutes. The test was passed successfully. Temporary dryout did occur during acceleration, however. The test report is completed and issued as 466A1845601.

10. Heat Pipe Performance Tests--Cp 1840

a. Eight-foot heat pipes

Five eight-foot stainless steel heat pipes were under test during this quarter to determine the rate of gas buildup in the condenser sections. Two heat pipes, one with a miter joint near the transport section, the other with a 24-inch bend in the transport, have accrued a total test time of 38 days each. One pipe was cleaned with nitric acid to reduce gas buildup. This pipe is now being prepared for test. Two pipes mounted back to back are also being prepared for test.

b. Fifteen-foot heat pipes

Three 15-foot stainless steel heat pipes have undergone Q maximum tests. Five 15-foot nickel heat pipes were also tested. An investigation is underway to determine the differences between nickel and stainless steel pipes.

A high power startup test of a standard SNAP 29 heat pipe was completed. This heat pipe was baked out before the test started.

The Q maximum testing of S/N-29, experimental and S/N-39, standard heat pipes was initiated. Testing of S/N-39 standard was completed at the 250° F level and testing at a 225° F level is continuing after a new cleaning process was completed.

11. Fuel Block Sled Impact Test--CP 3330

Six graphite SNAP 29 fuel blocks with simulated capsules were impact tested at the Sandia water jet facility to simulate earth impact under re-entry conditions. The

purpose of the test was to obtain stress and strain data of the SNAP 29 fuel block under impact conditions. The data are to be used in an analytical program. The fuel blocks were instrumented internally and externally with strain gages and suspended in the path of a 8500-pound granite block and sled. The sequence of the tests and their impact orientations were as follows:

<u>Test No.</u>	<u>Date</u>	<u>Velocity (fps)</u>	<u>Orientation*(deg)</u>
1	12/21/67	90.5	30
2	1/3/68	90.8	30
3	1/5/68	90.0	60
4	1/10/68	91.0	60
5	2/2/68	135.6	60
6	2/6/68	134.5	60

*Angles selected for analytical purposes as being typical of corner impact.

The strain gage data were obtained on three magnetic tape recorders and the shattering of the graphite block was photographed with four high speed and two medium speed movie cameras. The originals of all film and magnetic tapes are being returned to Martin Marietta for data reduction.

12. Project PYRO Tests--CP 3340

SNAP 29 participated in four project PYRO tests during this period. These tests were:

- (1) 25,000-pound liquid oxygen/liquid hydrogen drop test
- (2) 1000-pound liquid oxygen/liquid hydrogen drop test
- (3) Titan I in-tank tests (liquid oxygen/RP-1) (two tests).

Details of these tests are given under CP 3340 in Chapter VI.

At the present the following tests are in progress:

	<u>Control Point</u>
Module life performance	1340
Heat pipe performance	1840
Project PYRO	3340

The following list includes the tests that were completed this quarter:

Heated fuel capsule liner burst	1140
Fuel block vibration	1140
Plasma arc re-entry heating	1140

	<u>Control Point</u>
Vacuum testing of Watlow heaters	1240
Heater performance	1240
Diaphragm predevelopment module	1340
Input power accuracy evaluation	1340
Heat pipe dynamic	1840
Heat pipe screening	1840
Fuel block sled impact	3330
Project PYRO (four tests)	3340

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IV. RELIABILITY--CONTROL POINT 3230

The SNAP 29 reliability program has been proceeding in accordance with the tasks and schedule presented in the SNAP 29 Program Plan. The plan establishes the management and technical reliability controls necessary to implement an effective and economically planned and integrated reliability program in support of design, procurement, fabrication and test. To accomplish this overall program most effectively, three major work areas have been established. These are:

CP 3231--Planning and System Analysis

CP 3232--Design Evaluation and Approval

CP 3233--Test Program Definition and Evaluation

The three work areas are further subdivided into tasks for planning and control purposes. The task breakdown follows that developed in the program plan and is used for presenting the information under Sections B through D.

A. SUMMARY OF SIGNIFICANT ACCOMPLISHMENTS

A preliminary prediction of the electric power output at end of life was completed for the four-module RTG configuration. The prediction is based on the beginning-of-life data presented in the Fifth Quarterly Report, MND2062-12-7, and the degradation rate established in this report.

A degradation rate was estimated for thermoelectric power at 3500 hours using data accumulated from life tests of five piston heat sink T/E modules. No statistical distributions have been included at end of life, since only two modules have reached the 3000-hour point. Extrapolation procedures were used to obtain the degradation rate at 3500 hours.

The current status of the electric heater failure analysis program is presented. Embrittlement of both heater nickel power leads and nickel ground straps has created cracks and wire failures under specific test conditions. The conditions under which the failures occur are not fully identified at the time of this report. Specific failures have also been experienced which are peculiar to given vendor designs.

A presentation of three methods for calculating numerical reliability values from data developed during random vibration tests are presented. All have application or potential application to the program.

An assessment of the thermal control subsystem three-shutter assembly has been prepared based on the results of the random vibration test. Calculated reliability values have been made, using two of the three proposed methods. The limited data precluded the development of values to which practical confidence levels could be applied.

B. RELIABILITY PLANNING AND SYSTEMS ANALYSIS--
CONTROL POINT 3231

1. Program Planning

The reliability program has been proceeding in accordance with the tasks and schedule established in the SNAP 29 Program Plan. Starting with this report, most analyses, predictions and trade-off studies will be confined to the RTG configuration comprising four T/E modules. The four T/E module RTG configuration has been selected for the demonstration unit.

2. System Analyses

The SNAP 29 Fifth Quarterly Progress Report presented a prediction of the power output of the four-module RTG configuration at beginning of life (BOL). No end-of-life (EOL) values were included, due to unavailability of a degradation rate.

A degradation rate of 1.24 to 1.59%/1000 hours projected to 3500 hours has been calculated from data currently available from life tests of five piston heat sink T/E modules (Paragraph C1). The 1.24%/1000 hours at a 3500-hour value is the average of two modules, S/N 003 and S/N 005. The 1.59%/1000 hours at a 3500-hour value is also limited to the average of modules S/N 001, S/N 003 and S/N 005, since S/N 002 and S/N 004 have too few hours accumulated to date.

Table IV-1 is a summary of the predicted power output values for the four-module RTG configuration at BOL and EOL of 144 days. The BOL values are obtained from the Fifth Quarterly Report, MND2062-12-7. The EOL mean power output values were calculated using the equation:

where:

$$P_{EOL} = P_{BOL} (1 - Dt)$$

P_{EOL} = power output at end of life (144 days)

P_{BOL} = power output at beginning of life

D = degradation rate in %/1000 hr

t = time (144 days)

TABLE IV-1
RTG Power Output Characteristics

<u>Distribution</u>	<u>BOL</u> <u>(watts)</u>	<u>EOL (watts) (144 days)</u>	
		<u>1.24%/1000 hr</u>	<u>1.59%/1000 hr</u>
USL (3 σ)	416.8	--	--
Mean $\bar{X}$	410.4	392.8	387.8
LSL (3 σ)	404.0	--	--

No upper or lower sigma limits are available for the 3500-hour time due to limitations in sample sizes and operating time. It is to be expected that there may be some increase in the standard deviation at EOL over that experienced at BOL. The degradation rate value of 1.24%/1000 hours at 3500 hours is considered to be representative of the SNAP 29 design based on the piston heat sink module test program.

C. DESIGN EVALUATION AND CONTROL--CONTROL POINT 3232

1. T/E Power Degradation Rate

The power output degradation rate for the SNAP 29 program has been developed from an analysis of the data accumulated from the performance and life tests of five piston heat sink T/E modules. The test program is currently in progress, with termination planned early in July 1968. The degradation rate presented herein has been extrapolated to 3500 hours.

Figure IV-1 is the graphical presentation of the power output/time profile of the individual T/E modules. A reliability distribution, based on the average of all five modules, is included at beginning of life.

S/N 001, 003, 004 and 005 are undergoing life and performance testing. S/N 002 has undergone a sinusoidal and random vibration test and is currently operating in the performance test program. S/N 004 developed a case leak after approximately 650 hours. The performance since repair shows a rapid drop off of power with time.

Table IV-2 is a summary of the degradation rates for the individual modules in percent of power loss per 1000 hours for a specific range of time. The values are calculated from the power output/time data presented in Fig. IV-1.

Tables IV-3 and IV-4 provide the degradation rate based on the average of all five modules and on the average of modules S/N 003 and S/N 005, respectively. The rate is expressed in percent of power loss per 1000 hours for a specific range of time. The values are developed from information presented in Fig. IV-1.

TABLE IV-2

Individual Module Power Output Degradation Rate

Module <u>S/N</u>	Degradation Rate in %/1000 Hours		
	<u>1000 hr</u>	<u>2000 hr</u>	<u>Latest Time (hr)</u>
001	1.13	1.72	1.71 at 3000
002	--	--	1.98 at 750
003	0.34	0.72	0.70 at 3000
004*	--	--	5.09 at 1600
005	0.85	1.46	1.70 at 2600

*Serial No. 004 developed a case leak after 600 to 700 hours of testing; degradation rate at 1600 hours based on new power level recorded after repair. Module has been removed from test.

FIG. IV-1. PISTON HEAT SINK MODULE POWER OUTPUT AND DISTRIBUTION

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TABLE IV-3

Power Output Degradation Rate
(mean power S/N 001 through S/N 005)

Time Interval (hr)	Power* Δ (watts)	Degradation Rates	
		(watts/1000 hr)	(%/1000 hr)
0 to 500	0.44	0.88	0.85
0 to 1000	1.29	1.29	1.25
0 to 1500	2.14	1.43	1.39
0 to 2000	3.14	1.57	1.52
0 to 2500	4.00	1.60	1.55
0 to 3000	4.84	1.61	1.56*
0 to 3500	5.74	1.64	1.59*

*Data extrapolated beyond recorded data.

TABLE IV-4

Power Output Degradation Rate
(mean power modules S/N 003 and S/N 005)

Time Interval (hr)	Power* Δ (watts)	Degradation Rates	
		(watts/1000 hr)	(%/1000 hr)
0 to 1000	0.65	0.65	0.62
0 to 2000	2.25	1.13	1.07
0 to 3000	3.85	1.28	1.22*
0 to 3500	4.60	1.31	1.24*

*Data extrapolated beyond recorded data points.

It is expected that a degradation rate of approximately 1.29%/1000 hours at 3500 hours is most truly representative of the inherent design capabilities of the SNAP 29 T/E module. The evidence indicates that the standard deviation associated with the degradation rate is increasing with increased time. However, no rate at 3500 hours can be postulated from the data, due to limited time and sample size.

2. Electric Heater Failure Analysis

The electric heater evaluation and selection program was terminated in December 1967. The notice of termination, the status of each of the specific tests and a listing of failures that had occurred at the time of termination were presented in the Fifth Quarterly Progress Report, MND2062-12-7. A failure analysis program was instituted to determine the causes and make recommendations for subsequent design application. The status of this analysis program is presented herein.

The electric heater evaluation and selection program was based on formal controlled tests involving 126 electric heaters. Six groups, comprising module- and generator-type heaters from three selected vendors, were subjected to identical tests. The three vendors were Sanders Nuclear Corporation, TRW Systems and Watlow Corporation. Within each group of 21 heaters, five each were allocated to life, thermal cycle and thermal vacuum tests and three each were allocated to vibration and shock tests. For the life, thermal cycle and thermal vacuum tests, five heaters were installed into a single Inconel 600 block containing identical type and vendor units. Similarly, for the vibration test, three heaters were installed in a single block and, for the shock test, a single heater was installed in a block.

Each type of heater was furnace brazed at both ends into heater blocks in a helium atmosphere at 1925 to 1950° F for 15 minutes. Boron nitride caps were installed over the lead end of each heater, using Ceramacast 505 (water mix) as the cement to secure the cap to the heater and to eliminate all cavities. The cement was processed by air drying for 36 hours, followed by an air bake at 200° F for four hours. The completed blocks were installed into the test fixtures containing instrumentation, insulation and cooling coils, using water as the heat rejection agent.

A preliminary thermal vacuum heater test was performed using three Watlow module heaters. This test was initiated to gain experience with operation of heaters in a thermal vacuum.

a. Sanders heater analysis

The final report covering the failure analysis of the Sanders Nuclear Corporation electric heaters has been completed. The evaluation is based on the results obtained during the SNAP 29 electric heater evaluation and selection test program. Early in November 1967, the Sanders life test of the module (SN 60038) and generator (SN 60039) heaters were started and terminated shortly thereafter due to an excessive number of failures. The other tests--thermal vacuum, thermal cycle, shock and vibration--were not conducted, since the heater test program was terminated December 6, 1967.

Tables IV-5 and IV-6 provide a summary of the failures that were detected during post-assembly check and during development tests, respectively. Of the 13 failures which occurred, seven pertain to generator- and six to module-type units. Eight of the 13 failures were detected subsequent to manufacturing, and five occurred during the evaluation test program. Six failed units were subjected to a failed parts analysis. These units were taken from the module shock and module life block assemblies, and selected as most representative of the failure modes expected, based upon conditions observed at the time that the failures occurred.

A failed parts analysis was performed on a selected group of module heaters. The analysis identified three modes of failure. They are:

- (1) A short to case and subsequent burnout of the resistance wire approximately three inches from the end of the heater (Fig. IV-2).
- (2) A burnout of the resistance wire where it makes a reverse bend at the bottom end of the heater opposite the leads (Fig. IV-3).
- (3) Improperly fabricated connection at the crimp between the Kanthal resistance wire and the external power lead (Figs. IV-4 and IV-5).

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FIG. IV-2. LONGITUDINAL SECTION THROUGH SHEATH, SHOWING
FAILURE--SANDERS S/N 1013

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FIG. IV-3. ELEMENT FAILURE AT BOTTOM
END OF HEATER--SANDERS
S/N 1019

FIG. IV-4. TYPICAL CRIMP CONNECTION
BETWEEN KANTHAL HEATER
WIRE AND NICKEL LEAD--
SANDERS S/N 1006

FIG. IV-5. METALLOGRAPH CROSS SECTION
THROUGH CRIMP CONNECTION--
SANDERS S/N 1011

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TABLE IV-5

Sanders Heater--Fabrication/Assembly Failures

<u>Heater Type</u>	<u>Serial Number</u>	<u>Test Assignment</u>	<u>Failure Mode</u>
Generator SN 60039	3006	Life	Short
	3002	Thermal vacuum	Intermittent
	3010	Thermal vacuum	Intermittent
	3011	Thermal vacuum	Intermittent
	3023	Thermal vacuum	Intermittent
Module SN 60038	1016	Life	Intermittent
	1024	Thermal cycle	Intermittent
	1011	Shock	Intermittent

TABLE IV-6

Sanders Heater--Evaluation Test Failures

<u>Heater Type</u>	<u>Serial Number</u>	<u>Test Assignment</u>	<u>Failure Mode</u>	<u>Operating Time (hr)</u>
Generator SN 60039	3005	Life	Open	1.1
	3007	Life	Short	1.7
Module SN 60038	1013	Life	Open	16 to 76*
	1014	Life	Open	2
	1019	Life	Open	16 to 76*

*Failed during weekend.

The number of failures and limited time accumulated before the termination of the tests precluded developing the operating life expectancy or the failure rate of the heaters during the operating life.

b. TRW heater analysis

A status report of the failure analysis of the TRW systems electric heaters is presented herein. It covers the results obtained during the SNAP 29 electric heater evaluation and selection test program. The life tests of the module heaters, C401445-1, and generator heaters, C401445-2, were initiated early in November 1967. Failures of the heaters during that month precluded maintaining the required operating block temperatures. This resulted in the termination of these tests. The thermal vacuum, thermal cycle, shock and vibration tests were not conducted, due to the termination of the heater evaluation program on December 6, 1967.

Table IV-7 provides a summary of the failures that were detected during the evaluation test program. Of the six failures observed, one pertains to the generator and five to the module. The five module heaters from the life test block assembly were selected for failed parts analysis. These five are judged to represent the modes of failure.

TABLE IV-7
TRW Heater Failures

<u>Heater Type</u>	<u>Serial Number</u>	<u>Test Assignment</u>	<u>Failure Mode</u>	<u>Operating Time (hr)</u>
Generator C401445-2	005	Life	Open	192
Module C401445-1	001	Life	Open	*
	002	Life	Open	20
	003	Life	Open	3
	004	Life	Open	648
	005	Life	Open	*

*Failure discovered subsequent to test termination at 840 hours.

The failed parts analysis identified the following modes of failure. The analysis related to determination of the failure mechanism has not been completed:

- (1) The major failure mode was related to embrittlement on cracking of the nickel leads external to the boron nitride caps (Fig. IV-6). An example of a cracked lead is shown in Fig. IV-7.
- (2) A burnout of the heater power leads at the interface of the boron nitride cap and heater unit (Fig. IV-8).

Although the failure mechanism could not be ascertained within the scope of the analysis, it showed that all failures were confined to the external and the interstitial nickel leads. Evidence indicates that the failures resulted from progressive stages of deterioration, which aggravated inherent defects induced during the fabrication of the heaters.

c. Watlow heater analysis

A status report of the failure analysis of the Watlow Corporation electric heaters is presented herein. The report covers the observations during the SNAP 29 electric heater evaluation and selection test program and the preliminary heater thermal vacuum test program.

The life and thermal cycle tests of the module heaters, 466C74N5-1, and generator heaters, 466C74N5-2, were initiated in November 1967. Although failures were encountered, the tests continued until December 6, 1967, when the program was terminated. The vibration, shock and thermal vacuum tests were not started.

Three module electric heaters with boron nitride caps installed (no blocks) were started in a preliminary thermal vacuum environment and tested for over 700 hours, with one failure. This test was terminated because chamber contamination precluded maintaining the required vacuum.

Table IV-8 provides a summary of the failures that were observed during the test programs. Of these failures, the 10 heaters in the life and thermal cycle block assemblies and the one failure in the thermal vacuum were selected for failed parts analysis.

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FIG. IV-6. HEATER NICKEL WIRE LEAD FAILURES, LIFE TEST
MODULE--TRW

FIG. IV-7. TYPICAL CROCK IN NICKEL LEAD WIRE--TRW S/N 005

FIG. IV-8. LONGITUDINAL SECTION THROUGH HEATER TRW S/N 005, SHOWING FAILURE

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TABLE IV-8
Watlow Heater Failures

<u>Heater Type</u>	<u>Serial Number</u>	<u>Test Assignment</u>	<u>Failure Mode</u>	<u>Operating Hours</u>
Generator 466C74N5-2	5	Life	Open	192
Module 466C74N5-1	17	Thermal cycle	Open	3rd cycle
	28		↑	4th cycle
	33		↓	*
	55	Life	Open	*
	78			*
	38		Open	**
	91		↑	**
	92		↓	**
	128		Open	**
	135			30
	25	Preliminary thermal vacuum	Open	657

*Failure discovered after test termination of 7 cycles.

**Failure discovered after test termination of 726 hours.

The failed parts analysis identified the following modes of failure. The analysis for determining the failure mechanism(s) has not been completed.

- (1) The major failure mode is related to embrittlement or cracking of the nickel leads external to the boron nitride caps (Fig. IV-9).
- (2) Burnout of the heater power leads at the interface of the boron nitride cap and the heater unit (Fig. IV-10).

D. TEST PROGRAM DEFINITION AND EVALUATION-- CONTROL POINT 3233

1. Random Vibration Assessment Techniques

The RTG and its subsystems will be exposed to various dynamic environments during the different phases existing throughout its operating life. The types of dynamic environment have been identified and categorized as follows:

- (1) Handling and transportation shock
- (2) Handling and transportation vibration
- (3) Launch acceleration
- (4) Launch shock
- (5) Launch acoustic noise

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FIG. IV-9. HEATER NICKEL WIRE LEAD FAILURES, LIFE TEST
MODULE--WATLOW

FIG. IV-10. RADIOGRAPHIC VIEW OF WATLOW HEATER S/N 25 LEAD END

- (6) Launch or orbital separation shock
- (7) Launch sinusoidal vibration
- (8) Launch random vibration.

Of these eight environments, the random vibration has the most significant impact upon space hardware, since it produces the greatest amount of energy contributing to fatigue failures. Methods to ascertain the fatigue capability of the SNAP 29 components and assemblies, based on random vibration environmental tests, can be approached by using three methods. All three methods yield numerical values to which statistical significance may be applied if sufficient data are available. Where the data are limited, they provide a convenient tool for evaluation and design comparisons. The three methods are identified as:

- (1) Safety margin/log-normal distribution

This method presents the most dependable statistical approach. It is best adapted to test programs where a quantity of articles are tested to failure, and a mean failure level and standard deviation can be calculated.

- (2) Exponential distribution

The exponential distribution approach is more suited to test programs where a quantity of units are tested during various phases of the program with no test-to-failure criteria stipulated. However, one penalty involved is the number of test hours that must be accumulated in order to obtain a usable failure rate. In addition, little confidence can be forecast unless a number of units are tested at least equal to or in excess of the total exposure to be seen by the operational unit.

- (3) Dynamic vibration fatigue factor

This method assigns a multiplying damage or fatigue factor to be applied to any g-rms stress level that deviates from an established baseline value.

The safety margin/log-normal distribution and exponential distribution methods will generally provide conservative assessment results. The dynamic vibration fatigue factor method tends to eliminate the conservative aspects but, due to unknown damaging factors, may provide a sense of false security. All of the methods require the knowledge or the estimation of the operational environmental profile and the assumption that g-rms-seconds is a logical measurement factor. If a damage factor has been developed or can be assumed, it may be applied to all three methods in handling of the accumulated data.

The basic assumptions are similar for each of the methods in relation to the accumulation of vibration stress-versus-time measurements; however, the handling of recorded data varies with each method. The established assumptions are:

- (1) The stress/number of cycles relationship (S/N curves) have been assumed as applicable to a complex assembly in much the same way as when applied to a basic structural member. For each applied stress level there is a limiting number of cycle applications that can be completed before the article under test will fail due to inherent fatigue limitations. The failure mode may be due to a number of causes depending on the composition or complexity of the assembly (i.e., soldered connections, insulation, basic structure, etc.).

- (2) It is assumed that the S/N curve will be either a straight line or a concave relationship with the two reference axes.
- (3) In the test-to-failure modes and the accumulation of data for reliability purposes, any failures which occur during the sinusoidal vibration runs or the random vibration acceptance test levels will not be counted. Failures which occur during these test phases indicate that the hardware as fabricated does not meet the basic design requirements.
- (4) It is assumed that the articles tested have a constant failure rate in relation to each of the articles that have been fabricated to the same design, fabrication and quality control procedures. This does not mean that the individual failure rates for each stress level are identical; rather, on an overall basis, the failure rates should be the same for specific test program applications.
- (5) The operational hardware random vibration exposure during ground testing and flight launch mode is considered as the same baseline for all three evaluation methods. In addition the stress/time values established for SNAP 29 are used in all examples.

a. Safety margin and log-normal distribution method

The S/N relationship indicates the normal characteristics of the strength of an item of hardware in relation to the applied stress and the length of time or number of discrete operations where the stress is applied. The design approach for most hardware is to maintain a significant safety margin between the normal operational stress characteristics of the material and the strength of that material to the type of stress encountered. For the random vibration environment, the stress is specified as the g-rms level applied and the interval is specified as the time span in seconds. The area under the curve (Fig. IV-11) is therefore g-rms-seconds.

In random vibration tests to failure, the length of time that a unit may survive the operational or qualification level environment could be excessive where a large safety margin exists. The normal approach is to gradually increase the applied stress in accelerated increments until failure occurs or until sufficient g-rms-seconds are accumulated to provide a demonstrated reliability assessment value. The S/N curve (Fig. IV-11) is shown to indicate the relationships used. The dashed curve indicates typical test procedures.

FIG. IV-11. S/N CURVE AND ACCELERATED TEST LEVEL RELATIONSHIP

The safety margin relationship is used to measure the number of standard deviations of the failure stress distribution curve that exist between the reliability boundary (RB) of the operational stress expected and the mean failure stress level. The reliability boundary is selected as the upper 3 sigma limit of the operation stress distribution curve. The log-normal distribution relationship will be used for the presentation of the failure data due to the cumulative effects of the vibration stress and due to the anticipated skewed distribution of failures near the ultimate strength of the material. The safety margin relationship is shown in Fig. IV-12 and represented mathematically as follows:

$$SM = \frac{\bar{X}_S - RB}{\sigma_S} = \text{No. of Standard Deviations}$$

The reliability value is then determined from the normal probability function curve

FIG. IV-12. SAFETY MARGIN STRESS/LOAD RELATIONSHIP

The accumulation of random vibration g-rms-second data during the test-to-failure tests is represented in Table IV-9. The test time and g-rms loading are specified in the environmental test requirements for each subsystem and for each component where applicable. Entries, line 1 (10.6 g-rms) and 2 (29.1 g-rms), are the nominal operational vibration levels for two possible boosters. Entry line 3 (43.65 g-rms) is the qualification level environment specified as 150% of the higher operational vibration level. The subsequent entries are for tests at increasing 10% increments until failure. The corresponding g-rms-seconds are accumulated until the unit fails.

The accumulated g-rms-seconds for each of the units that failed in the test are converted to $\log_{10}$ values and added together. The summation is divided by the number of test articles to obtain the mean failure level, $\bar{X}_S$.

The standard deviation (σ_S) is determined by the following equation values are calculated based on $\log_{10}$:

$$\sigma_S = \sqrt{\frac{\sum (X_i - \bar{X})^2}{n - 1}}$$

where:

- X_i = individual unit failure level
 $\bar{X}_S$ = mean failure level of all units
 n = number of units tested.

TABLE IV-9
 Calculated Random Vibration g-Seconds
 (unit tests to failure)

Test Time (sec)	Vibration Level (g-rms random)	Duration Level (g-rms-sec)	Accumulated Duration Levels (g-rms-sec)	% x 29.1
300	10.6	3,180.0	3,180.0	--
180	29.1	5,238.0	8,418.0	--
180	43.65	7,857.0	16,275.0	150
180	46.56	8,380.8	24,655.8	160
180	49.47	8,904.6	33,560.4	170
180	52.38	9,428.4	42,988.8	180
180	55.29	9,952.2	52,941.0	190
180	58.20	10,476.0	63,417.0	200
180	61.11	10,999.8	74,416.8	210
180	64.02	11,523.6	85,940.4	220
180	66.93	12,047.4	97,987.8	230

In determining the mean accumulated g-rms-second load for the operational hardware, the assumption is made that each will be subjected to the following random vibration sequences:

- (1) Unit acceptance or Production Monitoring Test (PMT)
- (2) Acceptance test as part of the flight system (assumed as 1/2 of unit acceptance g-rms level).
- (3) Launch or flight profile (assumed at the same g-rms level as the system acceptance level but only for a one-minute duration).

The g-rms-seconds are totaled from the three test sequences and assumed as the mean vibration stress load for the operation hardware.

In determining the upper 3 sigma (or reliability boundary) limit for the load, the possibility of the operational hardware seeing any of the following additional exposures must be considered:

- (1) At least one additional PMT vibration run on one axis
- (2) Possibly one additional system vibration run in one axis
- (3) Excessive vibration level during launch due to some nonstandard performance.

Based on these possible exposures, the assumption is made that an additional PMT or acceptance vibration test level of the unit in all three axes should provide a conservative 3 sigma limit above the normal vibration load for the reliability boundary condition.

The above operational load conditions are indicated in Table IV-10, based on the designated vibration specification requirements.

TABLE IV-10
Accumulated Estimated Vibration g-Second Loads
(operational hardware)

<u>Test</u>	<u>Test Level (g-rms)</u>	<u>Test Duration (sec)</u>	<u>No. of Axes</u>	<u>Test Total (g-rms-sec)</u>	<u>Accumulated (g-rms-sec)</u>	<u>Assumed Operating Loads</u>
Acceptance (PMT)	29.1	180	3	15,714		
Acceptance (system)	14.6	180	3	7,884	23,598	
Flight (launch)	14.6	60	3	2,628	26,226	← Mean load ($\bar{X}_L$)
Acceptance (PMT)*	29.1	180	3	15,714	41,940	← $3\sigma_{X_L}$

*Assumed as maximum possible additional exposure

$$\text{Log mean load } (X_L) = \text{Log } 26,226 = 4.41868$$

$$\text{Log } 3\sigma_{X_L} = \text{Log } 41,940 = 4.62263$$

b. Exponential failure rate distribution method

The method of assessing the reliability of articles subjected to the random vibration mode is to apply the exponential distribution law for reliability in much the same manner as it is applied to electrical or electrical-mechanical hardware under operating time or operating cycle evaluation. For the random vibration application, the term g-rms-seconds is used rather than time or cycles. The exponential reliability equation is thus:

$$R = e^{-\lambda t} \quad R = e^{-\lambda c} \quad R = e^{-\lambda g \text{ sec}}$$

(t = time) (c = cycles) (g sec = g-rms-sec for random vibration)

The g-rms-second relationship is based on the accumulation of the area under the stress/time curve where the stress in g-rms level is applied for a specified period of time. If the hardware item being tested is subjected to a number of these cycles, the g-rms-seconds are cumulative, using the summation of the individual test cycle results. The division of the accumulated g-seconds into the number of observed failures results in the failure rate (λ). The t, c or g-seconds term in the above equation is the number of stress units that the operational hardware (or unit under evaluation) will normally be exposed to during its checkout tests and operational periods.

The concept is based on the expectation that the S/N curve relationships for structures can be applied to complex fabricated assemblies such as components and systems. This relationship assumes that, at various stress levels, there exists a mean period of time (or number of operations) that the individual stress level can be applied before the article reaches its basic failure mode due to the inherent characteristics of the article itself. Workmanship and quality of the individual article may shift the failure level about some normalized mean value.

Table IV-11 and Fig. IV-13 indicate the present random vibration input patterns specified in the various test specifications as Case 1 and Case 2. The tests are patterned as indicated but are extended further until failure occurs or a maximum g-rms level is reached (the maximum has been set at approximately 90 g-rms in some specifications).

TABLE IV-11

Random Vibration Test Modes

Test No.	Case 1		Case 2	
	(g-sec/test)	(g-sec/accumulated)	(g-sec/test)	(g-sec/accumulated)
1	3,078.0	3,078.0	3,078	3,078.0
2	5,238.0	8,316.0	5,238.0	8,316.0
3	7,857.0	16,173.0	5,761.8	14,077.8
4	8,380.8	24,553.8	6,285.6	20,363.4
5	8,904.6	33,458.4	6,804.4	27,167.8
6	9,428.4	42,886.8	7,333.2	34,501.0
7	9,952.2	52,839.0	7,857.0	42,358.0
8	10,476.0	63,315.0	8,380.8	50,738.8
9	10,999.8	74,314.8	8,904.6	59,643.4
10	11,523.6	85,838.4	9,428.4	69,071.8
11			9,952.2	79,024.0
12			10,476.0	89,500.0
13			10,999.8	100,499.8
14			11,523.6	112,023.4

Case 1 - One test for 5 minutes at 10.25 g-rms, one test for 3 minutes at 29.1 g-rms; one test for 3 minutes at 43.65 g-rms (150% times 29.1 g-rms); seven tests for 3 minutes each at increasing 10% increments of 29.1 g-rms.

Case 2 - One test for 5 minutes at 10.26 g-rms, one test for 3 minutes at 29.1 g-rms; ten tests for 3 minutes each at increasing 10% increments of 29.1 g-rms.

Note: Each of the above test conditions result in approximately the same accumulation of g-rms-seconds.

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FIG. IV-13. RANDOM VIBRATION STRESS VERSUS TIME

In determining the probable exposure of the operational hardware to the random vibration environment on an accumulated g-rms-second basis, all test and operational phases must be considered. In the case of the RTG components, it must be assumed that the following environmental applications may be encountered:

- (1) Three-axis vibration tests as a component at acceptance levels
- (2) Three-axis vibration tests in an RTG system at acceptance levels
- (3) One- to three-axis vibration tests to acceptance levels as part of the spacecraft flight system
- (4) Flight vibration spectrum during launch.

The reliability estimate that will be obtained from the above test mode applications is conservative for the following reasons:

- (1) The applied test spectrum at increasing stress levels is not representative of flight conditions and may introduce new stress modes in the hardware as the levels increase.
- (2) As each test run is performed, there is a starting shock pulse on the hardware which may produce additional stresses that are not applied to the same extent on operational hardware. However, the question arises, "Is the stress more critical when applied during successive test cycling or when applied over a long period of time with weeks or months between cycles?"

c. Dynamic vibration fatigue factors

Martin Marietta Report No. ER 14487, dated January 1967, entitled, "Safety Margins Established by Vibration Test," discusses vibration stress levels and time durations and whether fatigue or failure correlation can be made to other stress levels and time increments.

The report questions that tests at one level of vibration can establish any margin for vibration tests at a higher level using a g-second fatigue approach. The contention is that the relative stress on the article under test is not a 1:1 progression, as the stress is increased while holding the time increments constant. It is indicated that, as the g-rms applied stress level is doubled, the fatigue stress may increase by a factor of 10. It is also contended that, if a conservative rule is applied so that the total g-seconds in qualification test be a limit for production monitoring test (PMT) and flight endurance, the chance of the package failing due to vibration in flight is simply the chance that the environment exceeds the qualification environment.

Using this damage or fatigue factor approach, the relation of total equivalent damage established during qualification or development tests to the total equivalent damage anticipated on the flight hardware indicates the number of equivalent flight margins which exist. Table IV-12 indicates the use of the fatigue or damage factor. The major problem in applying this method is to establish the damaging factor applicable to the specific item being tested. When this is not possible, the common practice is to assume some value on the order of five when doubling the stress.

TABLE IV-12
Fatigue Life Margin Approach
(random vibration)

Operational Flight Hardware

PMT Test

Vibration stress level equivalent damage factor	1.0	
Time equivalent damage factor	<u>x 1.3</u>	
		1.3

Flight Boost Phase

Vibration stress level equivalent damage factor	1.0	
Time equivalent damage factor	<u>x 1.0</u>	
		<u>1.0</u>

Total Damage for Equivalent Flight Hardware		2.3
---	--	-----

Qualification Test Hardware

PMT Test

Vibration stress level equivalent damage factor	1.0	
Time equivalent damage factor	<u>x 1.3</u>	
		1.3

Qualification Test Cycle

Vibration stress level equivalent damage factor	10.0	
Time equivalent damage factor	<u>x 3.0</u>	
		<u>30.0</u>

Total Damage for Qualification Test Hardware		31.3
--	--	------

Margin for Flight Hardware

31.3 - 2.3 = 29 equivalent flights

2. Dynamic Vibration Assessment of Thermal Control Subsystem Shutter Assembly

The reliability assessment of the thermal control subsystem, CP 1540, three-shutter assembly has been prepared using the exponential distribution and the fatigue factor method described in Section D1.

The exponential distribution method has demonstrated a reliability of the shutter assembly under random vibration environment of 0.864 at a 50% confidence level. This numerical value is based on an estimated normal flight article random vibration exposure. On the basis of an estimated 3-sigma value, the demonstrated reliability value becomes 0.796 at a 50% confidence level. Both these values are conservative, being limited by the total time accumulated on the single test specimen.

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The fatigue factor method, as applied to the single test article, presents a low confidence in the results and prevents a conversion to a demonstrated numerical reliability. The data indicate a factor of safety of slightly better than 1 to approximately 11 based on the upper 3-sigma level of the estimated flight article random vibration exposure.

The anticipated performance of the shutter assemblies for the four-module configuration should be as high or higher than that experienced by the six-module shutter assembly tested, based on stress/design analysis.

Table IV-13 summarizes the test exposure time and random vibration input levels as converted to g-rms-seconds. The total random vibration g-rms-seconds exposure of the assembly was 53.946 at random input levels ranging from 10.26 to 43.6 g-rms. The test was performed on a single assembly and not to failure.

TABLE IV-13

Titanium Shutter Random Vibration Test Results

<u>Vibration Input (axis)</u>	<u>Vibration Level (g-rms)</u>	<u>Exposure Time (sec)</u>	<u>Assessment Value (g-rms-sec)</u>
Vertical	10.26	300	3,078
Lateral	10.26	300	3,078
Longitudinal	10.26	300	3,078
Longitudinal	29.10	180	5,238
Lateral	22.80	180	4,104
Lateral	17.60	180	3,168
Vertical	29.10	180	5,238
Vertical	32.00	180	5,760
Vertical	34.90	180	6,282
Vertical	39.30	180	7,074
Vertical	43.60	180	<u>7,848</u>
Total			53,946

Table IV-14 summarizes the normal g-rms-second exposure expected during the flight acceptance and launch boost phase. The normal is 11,439 g-rms-seconds with a 3-sigma level of 17,799 g-rms-seconds. The 3-sigma level makes allowance for two retests. These values may be high, and thus conservative, since they are based on component and not system test level. System test level will not be available until the mission is defined.

TABLE IV-14

Titanium Shutter Assembly Flight Vibration Requirements

<u>Vibration Input (axis)</u>	<u>Vibration Level (g-rms)</u>	<u>Exposure Time (sec)</u>	<u>Assessment Value (g-rms-sec)</u>
Vertical	10.6	300	3,180
Lateral	10.6	300	3,180
Longitudinal	10.6	300	3,180
Flight (peak loading)	10.6	90	954
Flight (sustained load)	3.5	270	<u>945</u>
Total (mean)			11,439
Additional axis	10.6	300	3,180
Additional axis	10.6	300	<u>3,180</u>
Total (assume upper 3 σ level)			17,799

a. Exponential distribution method

This method requires the development of a failure rate (λ) which can be used in the exponential equation, $R = e^{-\lambda t}$. The chi-squared distribution presents a method of estimating the failure rate under both a zero failure condition and known failure conditions. Therefore:

$$\lambda = \frac{\chi^2(\alpha, 2r + 2)}{2T} = \frac{\chi^2(0.50, 2)}{2(53,946)} = \frac{1.386}{107,892} = 1.28 \times 10^{-5}$$

$$R = e^{-\lambda t}$$

$$R = e^{-(1.28 \times 10^{-5})(11,439)} = e^{-0.146} = 0.864, \text{ based on mean flight article level}$$

$$R = e^{-\lambda t}$$

$$R = e^{-(1.28 \times 10^{-5})(17,799)} = e^{-0.228} = 0.796, \text{ based on } 3\sigma \text{ flight article level}$$

where:

$\chi^2(\alpha)$ = chi-squared value from tables

α = confidence level (50%)

r = number of failures (zero)

T = total accumulated measurements (g-rms-sec)

t = flight article exposure (g-rms-sec)

The nature of this reliability assessment method provides an increasing demonstrated reliability assessment number as more vibration response data are accumulated and as long as no failures are recorded. The existence of failures requires an equivalent increase in accumulated test data.

b. Fatigue factor method

The fatigue factor method required the establishment of certain factorial relationships concerning both g-level inputs and time duration of tests based on flight operational levels and test levels. Table IV-15B presents a possible factorial relationship which indicates a logarithmic expansion of stress by a factor of 10 between the flight level and the design verification level. Table IV-15C presents the same type of data but using the stress factor and time factor on a strict straight line relationship.

The approximate ratio of five between the results in Table IV-15B and C (17.9/3.07) emphasizes the relative uncertainty associated with the interpretation of energy relationships in the mechanical dynamic environments. Intuitive reasoning would place the dynamic stress relationship well above the simple doubling of resultant energy with double the stress input; however, whether it approaches a logarithmic factorial relationship has not been verified. This relationship was utilized by Martin Marietta on the Gemini and Prime programs, but was not fully substantiated by analysis of test results.

The data results of Tables IV-15 are now related to a standard structural stress analysis diagram (Fig. IV-14)

FIG. IV-14. STANDARD STRESS ANALYSIS DIAGRAM

The term $\bar{X}_L$ is the estimated cumulative g-rms-second load that the normal flight article will see which is equivalent to 11,439 g-rms-seconds in Table IV-14 and a 11.8 factor in Table IV-15A. The upper 3-sigma limit (referred to as reliability boundary) is also estimated as shown at 17,799 g-rms-seconds in Table IV-14 and a factor in Table IV-15A.

TABLE IV-15

Fatigue Factor Relations Vibration Level and Time

Unity time factor = 90 seconds

Unity level factor = 10.6 g-rms

Level factor x 10 = 29.1 g-rms

Level factor x 1 = 10.6 g-rms

} log relationship

A. Flight Vibration Requirements (Ref. Table IV-14)

<u>Vibration Input</u>	<u>Level Factor</u>	<u>Time Factor</u>	<u>Composite Factor</u>
Vertical	1.0	3.3	3.3
Lateral	1.0	3.3	3.3
Longitudinal	1.0	3.3	3.3
Flight (peak load)	1.0	1.0	1.0
Flight (sustained load)	0.3	3.0	0.9
Subtotal (mean)			<u>11.8</u>
Additional axis	1.0	3.3	3.3
Additional axis	1.0	3.3	3.3
Total (3σ)			<u><u>18.4</u></u>

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TABLE IV-15 (continued)

B. Titanium Shutter Random Vibration Exposure (Ref. Table IV-13)

Level factor multiplier of 10

<u>Vibration Input</u>	<u>Level Factor</u>	<u>Time Factor</u>	<u>Composite Factor</u>
Vertical	0.97	3.3	3.20
Lateral	0.97	3.3	3.20
Longitudinal	0.97	3.3	3.20
Longitudinal	10.0	2.0	20.00
Lateral	4.6	2.0	9.20
Lateral	2.4	2.0	4.80
Vertical	10.0	2.0	20.00
Vertical	14.0	2.0	28.00
Vertical	21.0	2.0	42.00
Vertical	36.0	2.0	72.00
Vertical	62.0	2.0	<u>124.00</u>
			329.60

$$\text{Safety factor} = \frac{329.60}{18.40} = 17.9$$

C. Titanium Shutter Random Vibration Exposure (Ref. Table IV-13)

Level factor multiplier of 1

<u>Vibration Input</u>	<u>Level Factor</u>	<u>Time Factor</u>	<u>Composite Factor</u>
Vertical	0.97	3.3	3.20
Lateral	0.97	3.3	3.20
Longitudinal	0.97	3.3	3.20
Longitudinal	2.75	2.0	5.50
Lateral	2.15	2.0	4.30
Lateral	1.66	2.0	3.32
Vertical	2.75	2.0	5.50
Vertical	3.02	2.0	6.04
Vertical	3.29	2.0	6.58
Vertical	3.71	2.0	7.42
Vertical	4.11	2.0	<u>8.22</u>
			56.48

$$\text{Safety factor} = \frac{56.48}{18.40} = 3.07$$

The design level usually factored into most hardware is usually a minimum of 150% of the maximum value that the deliverable unit may see during usage. This can be estimated as 26,700 g-rms-seconds or a factor of 27.6 (1.50 x 18.4).

The additional safety margin interval (C) is therefore the factor that exists between the design level and the actual strength of the unit as determined by the point of failure. If a number of units are tested to failure, a strength distribution curve can be constructed with a mean strength ($\bar{X}_S$) indicated. If a single sample

is tested to failure, the interval of C can be designated for that unit, which may or may not be within the normal distribution of a number of tested samples.

In our particular test case, a single shutter assembly was tested at increasing stress levels; however, the test was not run until failure occurred. Therefore we can indicate a minimum C level for this sample but with no additional assurance that it still has any reserve strength contained in it. The margin of safety for the data presented in Table IV-15 can be indicated as follows:

For Table IV-15B

$$\begin{aligned}\text{Safety margin} &= \frac{A + B + C}{A + B} - 1 \\ &= \frac{329.6}{27.6} - 1 \\ &= 10.9\end{aligned}$$

For Table IV-15C

$$\begin{aligned}\text{Safety margin} &= \frac{A + B + C}{A + B} - 1 \\ &= \frac{56.48}{27.6} - 1 \\ &= 1.05\end{aligned}$$

The additional safety margin is therefore between 1 and 11. However, since all reliability numerical evaluations are based on the number of standard deviations ($X\sigma$) from the strength statistic, we cannot calculate a reliability number based on either data point.

V. AEROSPACE GROUND EQUIPMENT SYSTEM--CONTROL POINT 2000

The AGE system for the SNAP 29 consists of the equipment necessary to service, test, check out and transport the RTG system.

The objective of the AGE system is to develop equipment which will demonstrate the AGE capability of checking out and transporting the RTG and heat source and loading and unloading the heat source in the RTG.

Tasks to be conducted are:

CP 2200--Fuel Handling Machine

CP 2400--Transportation and Handling Equipment

CP 2600--Field Test Equipment

AGE effort was rescheduled and replanned as a result of the SNAP 29 program redirection. As a result of this rescheduling, no design effort will be expended in the AGE area until later in the program.

VI. SAFETY EVALUATION AND INTEGRATION--CONTROL POINT 3300

A. SUMMARY OF SIGNIFICANT ACCOMPLISHMENTS

There were eight major items of work accomplished during this period:

- (1) Completion of the Preliminary Safety Analysis Report (PSAR) first draft.
- (2) Determination of trajectory and heating data for various re-entry modes.
- (3) Formulation of aeroheating digital computer program.
- (4) Completion of aerodynamics write-up for PSAR.
- (5) Completion of test report for hypersonic aeroheating wind tunnel test for two- and four-module fuel blocks.
- (6) Preparations (specifications, procedures, model designs and model fabrication) for several wind tunnel test programs.
- (7) The water jet impact tests were successfully concluded.
- (8) The Phase II portion of the Launch Abort Environment Test Plan was completed.

A summary of each of these items follows; the detailed description of these and other accomplishments will be discussed under separate control point headings.

The Preliminary Safety Analysis Report (PSAR) was completed at the end of March. The report consists of three volumes: the Reference Design Document, the Safety Support Document and the Safety Analysis Document. This report is in draft form and is being jointly reviewed and modified by Sandia and Martin Marietta. The final version of the PSAR will be completed in June 1968. It has not been possible to summarize all of the results obtained for Volume III of the PSAR at this time. This volume of the PSAR and its results will be summarized in full in the next quarterly report.

Trajectories and aerodynamic heating were determined for all the modular fuel blocks in tumbling, broadside and oscillating about broadside $+45^\circ$ re-entry modes. The results are based on the wind tunnel force and moment and heating data described in the Fifth Quarterly Progress Report (MND2062-12-7, October through December 1967).

A digital computer program was written to integrate aeroheating data on the surface of a body as it oscillates in pitch and roll during orbital re-entry. Since a large number of static heat rates on the surface of a body are required to perform an aeroheating analysis, this program averages the heating at each position as a function of body motion.

A complete write-up of the aerodynamic analyses and tests conducted on the SNAP 29 program to date was prepared as input to Volume II of the Preliminary Safety Analysis Report.

The test report for the hypersonic aeroheating wind tunnel test for two- and four-module fuel blocks was completed and released. Ninety-four thermocouples were used to determine heating distributions over the forward-facing flat plate surfaces and hemispherical edges.

Preparations for several wind tunnel tests were made. Test procedures for the rotating force, scorch and aerodynamic damping tests were released. Models were designed. Force and damping models were fabricated and shipped to AEDC. Test specifications and procedures for a hypersonic free flight test were released. Models and launching brackets were designed, fabricated and shipped to AEDC. Test specifications and model drawings for a hypersonic forced oscillation test were released. Test specifications for a hypersonic low density test were released and a contract was signed with Cornell Aeronautical Laboratory to run the test.

The water jet impact tests conducted at Sandia Laboratory were completed. The purpose of these tests was to evaluate the failure modes of graphite plates. Simulated capsules were used in all of the six blocks tested.

SNAP 29 test articles, including heated and unheated ATJ graphite blocks, simulated capsules, and heated prototype capsules were included in four Project PYRO tests. While some of the fuel blocks were broken, due to the overpressure and blast effects, none of the capsules were ruptured.

B. SAFETY EVALUATION AND INTEGRATION--CONTROL POINT 3310

1. Downwind Diffusion

Work continued on the checkout and modifications to DELFIC, the Defense Atomic Support Agency's downwind diffusion computer program. A Richardson number subroutine was added to DELFIC to account for changes in turbulent diffusion coefficients due to topography and weather. US Weather Bureau probability estimates for exposure downwind after night release were obtained. Turbulence intensities were calculated from Pasquill's cloud size estimates.

2. Atmospheric Agglomeration, Transport and Removal

The STREAK computer code, which predicts fallout from a high-altitude release, has been received through the AEC. The code is being modified for use on the IBM 360/44. Results from the code will be compared with the two-stage fallout model. The population data tape received from Sandia was copied and a program was written to check this copy on the IBM 360/50. The data will be used in conjunction with ground deposition following a high-altitude release.

3. Internal Dosimetry

Development of the body model was completed and internal dosimetry calculations were performed. Typical results for a deposition of one microcurie of polonium-210 in the pulmonary region of the respiratory tract are shown in Table VI-1.

TABLE VI-1
Commensurated Doses in Rem Following One μ Ci Deposition

Stomach	0.44	Small intestine	0.12	Upper large intestine	1.3
Lower large intestine	2.	Pulmonary	41.	Lymph (respiratory)	90.
Gonads	0.63	Kidneys	1.9	Bone	0.71
Marrow	0.08	Blood cells	0.96	Heart	0.06
Lymph (remainder)	0.31	Muscle	0.01	Spleen	1.5
Liver	0.28	Residual tissues	0.03		

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Note that the magnitude of the dose to the respiratory lymph nodes is over twice that to the pulmonary region. This problem has been apparent ever since adoption of the latest ICRP task group model (Health Physics, Volume 12 No. 2). Recent conversation with Dr. Upton at Oak Ridge has revealed that this same problem was discussed at the recent meeting of ICRP Committee One (January 1968, London). The Committee has adopted a maximum permissible dose for these lymph nodes of 15 rem per year. The mass used in the calculation is averaged over the entire lymph system. Should this prepublished information be used, the respiratory lymph doses would be reduced to 1.93 rem. The whole-body commensurated dose following this deposition was computed to be 142 rem.

Work on the re-entry temperature error analysis was completed. An investigation of analytical approaches to the general problem of error analysis was performed. The objective of the study was to determine what applicability, if any, these techniques have to the current study. It was determined that approximate formulations for determining mean and variances of the unknown distribution, although shedding some light on the general characteristics of the probability function, are of little value for this study. In general, nonlinear functional relationships of more than one distributed variables result in probability functions for which analytic solutions have not been found.

Monte Carlo techniques are used in the error analysis program. This method is analogous to the performing of many experiments. In each experiment, a variable is found from its probability function and combined with the other variables determined in the same manner to form the answer. If the answers are then scored in sufficiently fine interval widths and the number of experiments is large, the recorded distribution approaches the exact solution. In particular, knowledge of the distribution function near its upper limits (which is the significant result for this study) is found. This information cannot be derived from approximate analytic techniques.

C. AERODYNAMIC ANALYSES AND TESTS--CONTROL POINT 3320

1. Trajectories and Heating for Modular Blocks During Various Re-entry Modes

Trajectories and aerodynamic heating for the modular blocks during spinning, edge-on re-entry were presented in the Fifth Quarterly Progress Report (MND2062-12-7, October through December 1967). This study was continued with an examination of three additional re-entry modes: tumbling, broadside and oscillating about broadside +45°. Re-entry trajectory time histories, aerodynamic stagnation heating rates and heating rate distributions were determined for the five SNAP 29 modular fuel block configurations. The characteristics of the one-inch-thick modular blocks are:

Module Configuration (no. of blocks)	Length (in.)	Width (in.)	Ref Area (ft ²)	Weight (lb)	Aspect Ratio, AR
8	35.63	32.68	8.09	100.44	1.09
6	32.68	27.20	6.18	76.75	1.20
4	32.68	18.79	4.27	52.40	1.74
2 (low AR)	18.79	17.39	2.27	28.05	1.08
2 (high AR)	32.68	10.35	2.36	28.05	3.16

Aerodynamic force and moment data and aerodynamic heating distribution data were obtained on one-inch-thick (full-scale), four-module and two-module (high aspect ratio) blocks in the AEDC Tunnel B during October and November 1967.

The experimentally determined drag coefficients versus angle of attack for zero roll angle ($\xi = 0^\circ$) are shown in Fig. VI-1 for the two-module (AR = 3.16), four-module and eight-module fuel blocks. The eight-module block data were originally obtained on a 0.696-inch-thick block and have been corrected to the currently configured one-inch-thick blocks. The six-module (AR = 1.20) and two-module (AR = 1.08) drag curves were estimated to be approximately the same as the drag curves for the eight-module and four-module blocks, respectively.

The data shown in Fig. VI-1 were used to determine average drag coefficients, $\bar{C}_D$, for each modular block configuration for the tumbling, broadside and oscillating about broadside $\pm 45^\circ$ re-entry modes. For a given re-entry mode, the variation in average drag coefficient between modular block configurations was small. Consequently, one average drag coefficient was determined for all modular fuel blocks in a given re-entry mode (see Table VI-2).

TABLE VI-2
Average Drag Coefficients for Various Re-entry Modes

Module Configuration	Reference Area (ft ²)	Weight (lb)	Re-entry Mode					
			Broadside		Oscillating Broadside $\pm 45^\circ$		Tumbling	
			$\bar{C}_D$	W/C _D A	$\bar{C}_D$	W/C _D A	$\bar{C}_D$	W/C _D A
8	8.09	100.44	1.58	7.85	1.311	9.46	0.798	15.55
6	6.18	76.75	1.58	7.85	1.311	9.46	0.798	15.55
4	4.27	52.40	1.59	7.72	1.331	9.22	0.823	14.95
2 (low AR)	2.27	28.05	1.59	7.78	1.331	9.27	0.823	15.00
2 (high AR)	2.36	28.05	1.57	7.58	1.346	8.84	0.855	13.95
Average			1.58	7.8	1.325	9.3	0.819	15.1

Re-entry trajectory and continuum stagnation point heating time histories were determined for all modular blocks in the broadside, oscillating about broadside $\pm 45^\circ$ and tumbling re-entry modes at an initial nominal design re-entry angle (γ) of -3.0 degrees. These data are shown in Figs. VI-2 (stagnation heating rate), VI-3 (total heat), VI-4 (velocity) and VI-5 (altitude). Comparable data for an orbital decay re-entry angle of -0.1 degree are shown in Figs. VI-6 (stagnation heating rate), VI-7 (total heat), VI-8 (velocity) and VI-9 (altitude).

The aerodynamic heating distribution, $\dot{q}/\dot{q}_s \sqrt{R}$, has been determined by using heat rate data obtained in AEDC Tunnel B at Mach number = 8 and Reynolds number = 0.64×10^6 per foot. Only the four-module (AR = 1.74) and the two-module (AR = 3.16) fuel block configurations were tested. The heating data for the other three configurations were obtained through techniques of data extrapolation and geometric correction. The resulting heating distributions for each of the five configurations are presented in the following tables for the motions under consideration as a function of distance in inches from a corner.

FIG. VI-1. SNAP 29 MODULAR FUEL BLOCK DRAG COEFFICIENTS

FIG VI-2. SNAP 29 MODULAR FUEL BLOCK BROADSIDE, OSCILLATING AND TUMBLING RE-ENTRY (ALL BLOCKS)--CONTINUUM STAGNATION POINT HEATING RATE ($\gamma = -3.0^\circ$)

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FIG. VI-3. SNAP 29 MODULAR FUEL BLOCK BROADSIDE, OSCILLATING AND TUMBLING RE-ENTRY (ALL BLOCKS)--CONTINUUM STAGNATION POINT TOTAL HEAT ($\gamma = -3.0^\circ$)

FIG. VI-4. SNAP 29 MODULAR FUEL BLOCK BROADSIDE, OSCILLATING AND TUMBLING RE-ENTRY (ALL BLOCKS)--VELOCITY VERSUS TIME ($\gamma = -3.0^\circ$)

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FIG. VI-5. SNAP 29 MODULAR FUEL BLOCK BROADSIDE, OSCILLATING AND TUMBLING RE-ENTRY (ALL BLOCKS)--ALTITUDE VERSUS TIME ($\gamma = -3^\circ$)

FIG VI-6 SNAP 29 MODULAR FUEL BLOCK BROADSIDE, OSCILLATING AND TUMBLING RE-ENTRY (ALL BLOCKS) -CONTINUUM STAGNATION POINT HEATING RATE ($\gamma = 0.1^\circ$)

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FIG. VI-7. SNAP 29 MODULAR FUEL BLOCK BROADSIDE, OSCILLATING AND TUMBLING RE-ENTRY (ALL BLOCKS)--CONTINUUM STAGNATION POINT TOTAL HEAT ($\gamma = -0.1^\circ$)

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FIG VI-8 SNAP 29 MODULAR FUEL BLOCK BROADSIDE, OSCILLATING AND TUMBLING RE-ENTRY (ALL BLOCKS) -- VELOCITY VERSUS TIME ($\gamma = 0^\circ$)

FIG VI-9 SNAP 29 MODULAR FUEL BLOCK BROADSIDE, OSCILLATING AND TUMBLING RE-ENTRY (ALL BLOCKS)--ALTITUDE VERSUS TIME ($\gamma = -0.1^\circ$)

Broadside:	Tables VI-3 through VI-7
Oscillating About Broadside $\pm 45^\circ$:	Tables VI-8 through VI-12
Tumbling:	Tables VI-13 through VI-17

The above distributions are valid for blocks containing an 0.5-inch radius corner. An estimate is also given in the following tables for blocks containing chamfered corners.

Broadside:	Tables VI-18 through VI-22
Oscillating About Broadside $\pm 45^\circ$:	Tables VI-23 through VI-27
Tumbling:	Tables VI-28 through VI-32

This estimate considers only geometrical effects and does not include the effects of flow irregularities which may exist.

2. Aeroheating Digital Computer Program

A digital computer program was written to integrate aeroheating data on the surface of a body as it oscillates in pitch and roll during orbital re-entry. The aeroheating analysis of an arbitrary body shape requires the integration of a large number of static heat ratios located on the surface of the body over various ranges of pitch and roll attitudes. The digital program receives as input surface static heat ratios for particular locations on the body as it is positioned at various pitch and roll angles relative to the free stream velocity vector. The input heat ratios are put into storage. The program then interpolates for data at attitudes not given as input, and integrates the data over specific range of pitch and roll angles that is designated by the user. The program is set up to receive data for 1 to 100 nodal stations, and provides an output of 1 to 200 stations which accounts for image nodes on the leeside of the body.

The program was checked out by hand calculations, and heating distributions were obtained for the following edge-on and near edge-on spinning nodes for the four-module fuel block:

Spinning, $\eta(\text{deg})$	Spinning and Oscillating, $\eta(\text{deg})$
90	90 ± 10
80	90 ± 20
70	90 ± 30
60	90 ± 45
45	

3. Aerodynamics Input to the Preliminary Safety Analysis Report, Volume II

A complete description of the aerodynamic analyses and tests conducted on the SNAP 29 program to date was prepared as inputs to the Preliminary Safety Analysis Report, Volume II. The purpose of the aerodynamic effort was to define the motion and heating of the re-entering SNAP 29 fuel block. Much of the early aerodynamic effort was expended on an eight-module fuel block. Starting in July 1967, emphasis was placed on examining the characteristics of modular fuel blocks; i.e., blocks

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sized for compatibility with two, four, six or eight thermoelectric modules. The results of the investigation of the modular configurations are described in detail.

Specifically, the following efforts are reported:

- (1) Re-entry trajectories and motions of the fuel block
- (2) Time histories of stagnation heating
- (3) Distribution of the local heating on the fuel block surface
- (4) Averaging of the heating as a function of the block motions.

The wind tunnel tests performed to supply the basic aerodynamic and aeroheating inputs required by the analyses are also described.

The six-degree-of-freedom re-entry motion hybrid computer program and the digital trajectory computer program are described in appendices to the Preliminary Safety Analysis Report (PSAR). Another appendix contains heating distributions for all the modular blocks with 0.5-inch radius corners and chamfered corners. These distributions are given for all re-entry modes investigated.

4. Wind Tunnel Aeroheating Tests

The test report for the hypersonic aeroheating test of the two-module and four-module fuel block configurations conducted in AEDC Tunnel B during November 1967 was completed and released as Martin Marietta Drawing 466A3322679. These tests were conducted at a Mach number of 8.0 for angles of attack of 0° to 90° for four angles of roll.

5. Wind Tunnel Rotating Tests

Test procedures for the rotating force, scorch and aerodynamic damping tests have been completed and released as Martin Marietta Drawing 466A3322580. Six 24% scale stainless steel models of the four-module fuel block were made as shown in Martin Marietta Drawings 466A3322022, -023 and -025 and shipped to AEDC for testing during April 1968. Twenty hours of tunnel occupancy time have been reserved in Tunnel B for these tests. The six scorch models and calibration spheres, to be made of RTV silicone rubber with a steel insert for strength and rigidity, have been designed and released as Drawings 466A3322031 and -032. The scorch models will be shipped to AEDC for testing in May 1968. Eight hours of tunnel occupancy time have been reserved in Tunnel C for these tests.

6. Wind Tunnel Free-Flight Tests

Test specifications and test procedures for a hypersonic free-flight test have been completed and released as Martin Marietta Drawings 466A3322082 and -582. Thirty 10% scale models of the four-module fuel block and four launching adapters have been completed and shipped to AEDC. The test is scheduled to be conducted in AEDC Tunnel B at a Mach number of 8.0 during April 1968. Models will be launched broadside to the airflow, edge-on to the airflow, rotating in pitch and rotating in roll. Model motions will be recorded on motion picture cameras at a rate of 4000 frames per second. Dynamic damping coefficients will be derived from the camera data.

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TABLE VI-3

Heating Distribution--Broadside, $\eta = 0^\circ$

SNAP 29 Eight-Module Fuel Block (0.5-inch radius corners)

$(\dot{q}/\dot{q}_s \sqrt{R})_{\text{full scale}} \sim AR = 1.09$

$\bar{X}$ = Distance (inches) measured from block edges

$\bar{X}$	0	0.3	0.75	1.25	1.75	2.5	3.5	4.5	5.5
0	0.30	0.36	0.44	0.43	0.40	0.37	0.33	0.28	0.26
0.3	0.36	1.01	0.99	0.96	0.94	0.91	0.88	0.85	0.82
0.75	0.44	0.99	0.97	0.95	0.93	0.90	0.86	0.83	0.81
1.275	0.43	0.96	0.95	0.92	0.90	0.87	0.84	0.81	0.79
1.825	0.40	0.93	0.92	0.89	0.87	0.85	0.82	0.79	0.77
2.5	0.37	0.91	0.90	0.87	0.85	0.82	0.79	0.76	0.74
3.225	0.34	0.88	0.87	0.85	0.82	0.80	0.77	0.74	0.72
3.875	0.31	0.86	0.85	0.83	0.80	0.78	0.75	0.75	0.71
4.6	0.28	0.84	0.83	0.81	0.78	0.76	0.74	0.71	0.69
5.325	0.26	0.83	0.82	0.80	0.77	0.74	0.72	0.70	0.68

TABLE VI-4

Heating Distribution--Broadside, $\eta = 0^\circ$

SNAP 29 Six-Module Fuel Block (0.5-inch radius corners)

$(\dot{q}/\dot{q}_s \sqrt{R})_{\text{full scale}}, AR = 1.20$

$\bar{X}$ = Distance (inches) measured from block edges

$\bar{X}$	0	0.3	0.75	1.25	1.75	2.5	3.5	4.5	5.5
0	0.30	0.36	0.43	0.42	0.39	0.36	0.32	0.27	0.25
0.3	0.36	1.02	1.01	0.98	0.96	0.93	0.90	0.88	0.86
0.75	0.43	1.01	1.00	0.97	0.95	0.92	0.89	0.87	0.85
1.275	0.42	0.97	0.96	0.93	0.91	0.88	0.85	0.83	0.81
1.825	0.39	0.94	0.93	0.90	0.88	0.85	0.82	0.80	0.78
2.5	0.36	0.91	0.90	0.87	0.85	0.82	0.79	0.77	0.75
3.225	0.33	0.88	0.87	0.84	0.82	0.79	0.76	0.74	0.72
3.875	0.30	0.86	0.85	0.82	0.79	0.77	0.74	0.72	0.70
4.6	0.27	0.84	0.83	0.80	0.78	0.76	0.73	0.71	0.69
5.325	0.25	0.83	0.82	0.79	0.77	0.75	0.72	0.70	0.68

TABLE VI-5

Heating Distribution--Broadside, $\eta = 0^\circ$

SNAP 29 Four-Module Fuel Block (0.5-inch radius corners)

$$(\dot{q}/\dot{q}_s \sqrt{R})_{\text{full scale}}, \text{ AR} = 1.74$$

 $\bar{X}$ = Distance (inches measured from block edges)

$\bar{X}$	0	0.3	0.75	1.25	1.75	2.5	3.5	4.5	5.5
0	0.30	0.36	0.39	0.37	0.34	0.31	0.28	0.25	0.24
0.3	0.36	1.14	1.13	1.10	1.08	1.05	1.02	1.00	0.98
0.75	0.39	1.11	1.09	1.06	1.04	1.01	0.98	0.96	0.95
1.275	0.37	1.05	1.03	1.00	0.97	0.94	0.91	0.89	0.88
1.825	0.34	1.00	0.98	0.95	0.92	0.89	0.86	0.84	0.83
2.5	0.31	0.96	0.94	0.91	0.88	0.85	0.82	0.80	0.79
3.225	0.28	0.93	0.91	0.88	0.85	0.82	0.79	0.77	0.76
3.875	0.26	0.90	0.88	0.85	0.82	0.79	0.76	0.74	0.73
4.6	0.24	0.88	0.86	0.83	0.80	0.77	0.74	0.72	0.71
5.325	0.22	0.86	0.84	0.81	0.78	0.75	0.72	0.71	0.69

TABLE VI-6

Heating Distribution--Broadside, $\eta = 0^\circ$

SNAP 29 Two-Module (low AR) Fuel Block (0.5-inch radius corners)

$$(\dot{q}/\dot{q}_s \sqrt{R})_{\text{full scale}} \sim \text{AR} = 1.08$$

 $\bar{X}$ = Distance (inches) measured from block edges

$\bar{X}$	0	0.3	0.75	1.25	1.75	2.5	3.5	4.5	5.5
0	0.40	0.56	0.57	0.50	0.42	0.37	0.32	0.29	0.27
0.3	0.56	0.32	1.29	1.23	1.18	1.14	1.09	1.06	1.03
0.75	0.57	1.29	1.25	1.20	1.15	1.11	1.06	1.03	1.00
1.275	0.50	1.23	1.20	1.15	1.10	1.06	1.01	0.98	0.95
1.825	0.42	1.18	1.15	1.10	1.06	1.02	0.98	0.95	0.92
2.5	0.37	1.14	1.11	1.06	1.02	0.98	0.94	0.91	0.89
3.225	0.31	1.10	1.07	1.03	0.99	0.95	0.91	0.88	0.86
3.875	0.33	1.09	1.06	1.01	0.97	0.93	0.89	0.87	0.84
4.6	0.29	1.05	1.03	0.98	0.94	0.91	0.87	0.84	0.82
5.325	0.27	1.03	1.00	0.96	0.92	0.89	0.85	0.82	0.80

TABLE VI-7

Heating Distribution--Broadside, $\eta = 0^\circ$

SNAP 29 Two-Module (high AR) Fuel Block (0.5-inch radius corners)

$$(\dot{q}/\dot{q}_s \sqrt{R})_{\text{full scale}} \sim \text{AR} = 3.16$$

 $\bar{X}$ = Distance (inches) measured from block edges

$\bar{X}$	0	0.3	0.75	1.25	1.75	2.5	3.5	4.5	5.5
0	0.30	0.36	0.31	0.25	0.21	0.18	→	→	→
0.3	0.36	1.44	1.29	1.13	1.07	1.04	→	→	→
0.75	0.31	1.35	1.30	1.23	1.18	1.16	→	→	→
1.275	0.25	1.18	1.13	1.06	1.02	1.00	→	→	→
1.825	0.21	1.07	1.04	0.98	0.94	0.92	→	→	→
2.5	0.18	1.03	0.99	0.93	0.89	0.87	→	→	→
3.225	0.16	1.00	0.97	0.91	0.87	0.85	→	→	→
3.875	0.15	0.99	0.95	0.89	0.85	0.83	→	→	→
4.6	↓	↓	↓	↓	0.84	0.82	→	→	→
5.325	↓	↓	↓	↓	↓	↓			

TABLE VI-8

Heating Distribution--Oscillating, $\eta = 0^\circ \pm 45^\circ$

SNAP 29 Eight-Module Fuel Block (0.5-inch radius corners)

$$(\dot{q}/\dot{q}_s \sqrt{R})_{\text{full scale}} \sim \text{AR} = 1.09$$

 $\bar{X}$ = Distance (inches) measured from block edges

$\bar{X}$	0	0.3	0.75	1.25	1.75	2.5	3.5	4.5	5.5
0	0.48	0.58	0.55	0.52	0.49	0.48	→	→	→
0.3	0.44	0.98	0.92	0.85	0.80	0.78	→	→	→
0.75	0.38	1.04	0.97	0.89	0.84	0.82	→	→	→
1.275	0.35	0.95	0.88	0.80	0.76	0.74	→	→	→
1.825	0.31	0.86	0.80	0.73	0.69	0.67	→	→	→
2.5	0.28	0.78	0.74	0.67	0.62	0.60	→	→	→
3.225	0.25	0.73	0.68	0.61	0.57	0.55	→	→	→
3.875	0.24	0.69	0.64	0.58	0.54	0.52	→	→	→
4.6	0.22	0.65	0.62	0.56	0.52	0.50	→	→	→
5.325	0.21	0.63	0.60	0.54	0.50	0.48	→	→	→

TABLE VI-9

Heating Distribution--Oscillating, $\eta = 0^\circ \pm 45^\circ$

SNAP 29 Six-Module Fuel Block (0.5-inch radius corners)

$(\dot{q}/\dot{q}_s \sqrt{R})_{\text{full scale}} \sim AR = 1.20$

$\bar{X}$ = Distance (inches) measured from block edges

$\bar{X}$	0	0.3	0.75	1.25	1.75	2.5	3.5	4.5	5.5
0	0.50	0.60	0.58	0.54	0.51	0.50			→
0.3	0.45	0.97	0.93	0.88	0.84	0.82			→
0.75	0.39	1.03	0.99	0.93	0.90	0.88			→
1.275	0.34	0.92	0.88	0.82	0.78	0.76			→
1.825	0.30	0.86	0.80	0.73	0.70	0.68			→
2.5	0.27	0.79	0.73	0.66	0.62	0.60			→
3.225	0.25	0.74	0.70	0.62	0.58	0.56			→
3.875	0.23	0.70	0.66	0.59	0.55	0.53			→
4.6	0.21	0.67	0.63	0.57	0.53	0.51			→
5.325	0.20	0.65	0.61	0.55	0.52	0.50			→

TABLE VI-10

Heating Distribution--Oscillating, $\eta = 0^\circ \pm 45^\circ$

SNAP 29 Four-Module Fuel Block (0.5-inch radius corners)

$(\dot{q}/\dot{q}_s \sqrt{R})_{\text{full scale}} \sim AR = 1.74$

$\bar{X}$ = Distance (inches) measured from block edges

$\bar{X}$	0	0.3	0.75	1.25	1.75	2.5	3.5	4.5	5.5
0	0.50	0.60	0.58	0.54	0.51	0.50			→
0.3	0.43	1.14	1.09	1.02	0.98	0.96			→
0.75	0.36	1.13	1.06	0.98	0.94	0.92			→
1.275	0.30	0.98	0.92	0.84	0.80	0.78			→
1.825	0.26	0.89	0.82	0.74	0.70	0.68			→
2.5	0.23	0.82	0.76	0.69	0.65	0.63			→
3.225	0.21	0.76	0.71	0.65	0.61	0.59			→
3.875	0.19	0.73	0.69	0.62	0.58	0.56			→
4.6	0.18	0.71	0.66	0.60	0.56	0.54			→
5.325	0.18	0.69	0.65	0.59	0.55	0.53			→

TABLE VI-11

Heating Distribution--Oscillating, $\eta = 0^\circ \pm 45^\circ$

SNAP 29 Two-Module (low AR) Fuel Block (0.5-inch radius corners)

 $(\dot{q}/\dot{q}_s \sqrt{R})_{\text{full scale}} \sim \text{AR} = 1.08$ $\bar{X}$ = Distance (inches) measured from block edges

$\bar{X}$	0	0.3	0.75	1.25	1.75	2.5	3.5	4.5	5.5
0	0.66	0.79	0.71	0.66					→
0.3	0.56	1.25	1.07	1.00					→
0.75	0.46	1.26	1.08	1.01					→
1.275	0.38	1.09	0.96	0.87					→
1.825	0.34	0.98	0.87	0.77					→
2.5	0.30	0.90	0.78	0.70					→
3.225	0.26	0.84	0.73	0.65					→
3.875	0.25	0.80	0.69	0.62					→
4.6	0.24	0.78	0.67	0.60					→
5.325	0.23	0.77	0.66	0.59					→

TABLE VI-12

Heating Distribution--Oscillating, $\eta = 0^\circ \pm 45^\circ$

SNAP 29 Two-Module (high AR) Fuel Block (0.5-inch radius corners)

 $(\dot{q}/\dot{q}_s \sqrt{R})_{\text{full scale}} \sim \text{AR} = 3.16$ $\bar{X}$ = Distance (inches) measured from block edges

$\bar{X}$	0	0.3	0.75	1.25	1.75	2.5	3.5	4.5	5.5
0	0.50	0.60	0.58	0.54	0.51	0.50			→
0.3	0.39	1.46	1.33	1.23	1.18	1.16			→
0.75	0.30	1.24	1.17	1.09	1.05	1.03			→
1.275	0.24	1.05	0.98	0.90	0.86	0.84			→
1.825	0.21	0.96	0.89	0.80	0.76	0.74			→
2.5	0.18	0.91	0.84	0.76	0.72	0.70			→
3.225	0.17	0.89	0.82	0.74	0.69	0.67			→
3.875	0.16	0.88	0.81	0.73	0.68	0.66			→
4.6	0.15	0.87	0.80	0.72	0.67	0.65			→
5.325	0.15	0.87	0.80	0.72	0.67	0.65			→

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TABLE VI-13

Heating Distribution--Tumbling

SNAP 29 Eight-Module Fuel Block (0.5-inch radius corners)

 $(\dot{q}/\dot{q}_s \sqrt{R})_{\text{full scale}} \sim AR = 1.09$ $\bar{X}$ = Distance (inches) measured from block edges

$\bar{X}$	0	0.3	0.75	1.25	1.75	2.5	3.5	4.5	5.5
0	0.38	0.76	0.84	0.83	0.82	0.81			→
0.3	0.34	0.62	0.68	0.66	0.64	0.62			→
0.75	0.28	0.47	0.50	0.47	0.44	0.43			→
1.275	0.22	0.35	0.39	0.38	0.36	0.34			→
1.825	0.17	0.30	0.33	0.32	0.30	0.29			→
2.5	0.13	0.27	0.30	0.28	0.26	0.25			→
3.225	0.11	0.25	0.27	0.25	0.24	0.23			→
3.875	0.09	0.23	0.25	0.23	0.22	0.21			→
4.6	0.08	0.22	0.24	0.22	0.21	0.21			→
5.325	0.08	0.21	0.23	0.21	0.20	0.20			→

TABLE VI-14

Heating Distribution--Tumbling

SNAP 29 Six-Module Fuel Block (0.5-inch radius corners)

 $(\dot{q}/\dot{q}_s \sqrt{R})_{\text{full scale}} \sim AR = 1.20$ $\bar{X}$ = Distance (inches) measured from block edges

$\bar{X}$	0	0.3	0.75	1.25	1.75	2.5	3.5	4.5	5.5
0	0.41	0.82	0.89	0.87	0.87	0.86			→
0.3	0.36	0.66	0.70	0.66	0.65	0.64			→
0.75	0.28	0.47	0.49	0.46	0.44	0.43			→
1.275	0.21	0.35	0.38	0.36	0.35	0.33			→
1.825	0.15	0.31	0.33	0.31	0.30	0.28			→
2.5	0.11	0.28	0.30	0.27	0.26	0.25			→
3.225	0.09	0.26	0.27	0.25	0.24	0.23			→
3.875	0.08	0.24	0.26	0.24	0.23	0.22			→
4.6	0.08	0.23	0.25	0.24	0.22	0.21			→
5.325	0.08	0.23	0.25	0.24	0.22	0.21			→

TABLE VI-15

Heating Distribution--Tumbling

SNAP 29 Four-Module Fuel Block (0.5-inch radius corners)

$$(\dot{q}/\dot{q}_s \sqrt{R})_{\text{full scale}} \sim AR = 1.74$$

$\bar{X}$ = Distance (inches) measured from block edges

$\bar{X}$	0	0.3	0.75	1.25	1.75	2.5	3.5	4.5	5.5
0	0.57	0.85	0.91	0.89	0.88	0.87			→
0.3	0.41	0.63	0.67	0.60	0.60	0.59			→
0.75	0.28	0.47	0.51	0.48	0.45	0.44			→
1.275	0.19	0.36	0.40	0.36	0.35	0.33			→
1.825	0.15	0.32	0.35	0.32	0.30	0.28			→
2.5	0.13	0.29	0.32	0.29	0.27	0.26			→
3.225	0.11	0.28	0.30	0.27	0.25	0.24			→
3.875	0.10	0.27	0.29	0.27	0.24	0.23			→
4.6	0.09	0.26	0.28	0.26	0.23	0.22			→
5.325	0.09	0.25	0.27	0.25	0.22	0.21			→

TABLE VI-16

Heating Distribution--Tumbling

SNAP 29 Two-Module (low AR) Fuel Block (0.5-inch radius corners)

$$(\dot{q}/\dot{q}_s \sqrt{R})_{\text{full scale}} \sim AR = 1.08$$

$\bar{X}$ = Distance (inches) measured from block edges

$\bar{X}$	0	0.3	0.75	1.25	1.75	2.5	3.5	4.5	5.5
0	0.53	1.14	1.14	1.12	1.12				→
0.3	0.42	0.76	0.71	0.66	0.65				→
0.75	0.28	0.51	0.49	0.44	0.44				→
1.275	0.18	0.42	0.38	0.35	0.34				→
1.825	0.14	0.37	0.33	0.30	0.30				→
2.5	0.13	0.34	0.31	0.28	0.28				→
3.225	0.11	0.33	0.29	0.27	0.27				→
3.875	0.11	0.32	0.28	0.26	0.26				→
4.6	0.11	0.31	0.27	0.25	0.25				→
5.325	0.11	0.30	0.26	0.24	0.24				→

TABLE VI-17

Heating Distribution--Tumbling

SNAP 29 Two-Module (high AR) Fuel Block (0.5-inch radius corners)

 $(\dot{q}/\dot{q}_s \sqrt{R})_{\text{full scale}} \sim \text{AR} = 3.16$ $(q/q_{sR=1 \text{ ft}})_{\text{ave-flight}}$ $\bar{X}$ = Distance (inches) measured from block edges

$\bar{X}$	0	0.3	0.75	1.25	1.75	2.5	3.5	4.5	5.5
0	0.63	0.97	0.99	0.96	0.93	0.91			→
0.3	0.47	0.79	0.77	0.73	0.72	0.67			→
0.75	0.31	0.54	0.56	0.51	0.48	0.46			→
1.275	0.22	0.40	0.43	0.40	0.37	0.35			→
1.825	0.19	0.36	0.39	0.36	0.33	0.30			→
2.5	0.16	0.34	0.37	0.33	0.31	0.27			→
3.225	0.15	0.33	0.35	0.32	0.29	0.26			→
3.875	0.14	0.32	0.35	0.31	0.28	0.26			→
4.6	0.14	0.32	0.35	0.31	0.28	0.25			→
5.325	0.14	0.32	0.35	0.31	0.28	0.25			→

TABLE VI-18

Heating Distribution--Broadside, $\eta = 0^\circ$

SNAP 29 Eight-Module Fuel Block (chamfered corners)

 $(\dot{q}/\dot{q}_s \sqrt{R})_{\text{full scale}} \sim \text{AR} = 1.09$ $\bar{X}$ = Distance (inches) measured from block edges

$\bar{X}$	0	0.3	0.75	1.25	1.75	2.5	3.5	4.5	5.5
0						0.36	0.42	0.37	0.33
0.3					0.36	0.99	0.94	0.90	0.87
0.75				0.44	0.99	0.95	0.91	0.87	0.84
1.275			0.44	0.96	0.94	0.90	0.86	0.83	0.81
1.825	0.30	1.01	0.98	0.93	0.89	0.85	0.82	0.79	0.77
2.5	0.44	0.98	0.95	0.89	0.86	0.82	0.79	0.76	0.74
3.225	0.42	0.94	0.92	0.87	0.83	0.80	0.77	0.74	0.72
3.875	0.39	0.92	0.90	0.85	0.81	0.78	0.75	0.73	0.71
4.6	0.36	0.89	0.86	0.83	0.79	0.76	0.74	0.71	0.69
5.325	0.33	0.86	0.84	0.81	0.77	0.74	0.72	0.70	0.68

TABLE VI-19

Heating Distribution--Broadside, $\eta = 0^\circ$

SNAP 29 Six-Module Fuel Block (chamfered corners)

 $(\dot{q}/\dot{q}_s \sqrt{R})_{\text{full scale}}, AR = 1.20$ $\bar{X}$ = Distance (inches) measured from block edges

$\bar{X}$	0	0.3	0.75	1.25	1.75	2.5	3.5	4.5	5.5
0						0.40	0.40	0.36	0.32
0.3					0.30	1.01	0.96	0.92	0.90
0.75				0.30	1.01	0.97	0.93	0.91	0.89
1.275			0.35	0.98	0.95	0.91	0.87	0.85	0.83
1.825	0.30	1.02	1.00	0.95	0.90	0.85	0.82	0.80	0.78
2.5	0.43	1.00	0.96	0.91	0.86	0.82	0.79	0.77	0.75
3.225	0.41	0.95	0.92	0.87	0.83	0.79	0.76	0.74	0.72
3.875	0.38	0.93	0.89	0.84	0.80	0.77	0.74	0.72	0.70
4.6	0.35	0.89	0.86	0.82	0.78	0.76	0.73	0.71	0.69
5.325	0.32	0.86	0.84	0.80	0.77	0.75	0.72	0.70	0.68

TABLE VI-20

Heating Distribution--Broadside, $\eta = 0^\circ$

SNAP 29 Four-Module Fuel Block (chamfered corners)

 $(\dot{q}/\dot{q}_s \sqrt{R})_{\text{full scale}}, AR = 1.74$ $\bar{X}$ = Distance (inches) measured from block edges

$\bar{X}$	0	0.3	0.75	1.25	1.75	2.5	3.5	4.5	5.5
0						0.40	0.36	0.31	0.28
0.3					0.30	1.13	1.08	1.04	1.02
0.75				0.30	1.11	1.06	1.02	0.99	0.96
1.275			0.35	1.10	1.02	0.97	0.93	0.91	0.89
1.825	0.30	1.14	1.11	1.02	0.95	0.89	0.86	0.84	0.83
2.5	0.40	1.09	1.03	0.96	0.90	0.85	0.82	0.80	0.79
3.225	0.36	1.02	0.97	0.91	0.86	0.82	0.79	0.77	0.76
3.875	0.33	0.98	0.93	0.89	0.83	0.79	0.76	0.74	0.73
4.6	0.30	0.94	0.90	0.85	0.81	0.77	0.74	0.72	0.71
5.325	0.27	0.90	0.87	0.83	0.79	0.75	0.72	0.71	0.69

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TABLE VI-21

Heating Distribution--Broadside, $\eta = 0^\circ$

SNAP 29 Two-Module (low AR) Fuel Block (chamfered corners)

 $(\dot{q}/\dot{q}_s \sqrt{R})_{\text{full scale}} \sim \text{AR} = 1.08$ $\bar{X}$ = Distance (inches) measured from block edges

$\bar{X}$	0	0.3	0.75	1.25	1.75	2.5	3.5	4.5	5.5
0						0.56	0.46	0.37	0.32
0.3					0.56	1.29	1.18	1.13	1.08
0.75				0.57	1.29	1.20	1.12	1.08	1.04
1.275			0.57	1.23	1.18	1.10	1.05	0.99	0.97
1.825	0.40	1.32	1.25	1.17	1.08	1.02	0.98	0.95	0.92
2.5	0.57	1.26	1.20	1.11	1.04	0.98	0.94	0.91	0.89
3.225	0.48	1.20	1.14	1.06	1.00	0.95	0.91	0.88	0.86
3.875	0.41	1.15	1.10	1.03	0.98	0.93	0.89	0.87	0.84
4.6	0.35	1.11	1.07	1.01	0.95	0.91	0.87	0.84	0.82
5.325	0.32	1.09	1.05	0.98	0.93	0.89	0.85	0.82	0.80

TABLE VI-22

Heating Distribution--Broadside, $\eta = 0^\circ$

SNAP 29 Two-Module (high AR) Fuel Block (chamfered corners)

 $(\dot{q}/\dot{q}_s \sqrt{R})_{\text{full scale}} \sim \text{AR} = 3.16$ $\bar{X}$ = Distance (inches) measured from block edges

$\bar{X}$	0	0.3	0.75	1.25	1.75	2.5	3.5	4.5	5.5
0						0.36	0.23	0.18	→
0.3					0.36	1.29	1.07	1.04	→
0.75				0.31	1.35	1.23	1.16	→	→
1.275			0.31	1.18	1.10	1.02	1.00	→	→
1.825	0.30	1.44	1.30	1.14	0.98	0.92	→	→	→
2.5	0.31	1.28	1.13	1.00	0.91	0.87	→	→	→
3.225	0.24	1.11	1.02	0.93	0.88	0.85	→	→	→
3.875	0.20	1.04	0.99	0.91	0.86	0.83	→	→	→
4.6	0.17	1.01	0.96	0.89	0.84	0.82	→	→	→
5.325	0.15	0.99	0.95	↓	↓	↓	→	→	→

TABLE VI-23

Heating Distribution--Oscillating, $\eta = 0^\circ \pm 45^\circ$

SNAP 29 Eight-Module Fuel Block (chamfered corners)

$(\dot{q}/\dot{q}_s \sqrt{R})_{\text{full scale}} \sim AR = 1.09$

$\bar{X}$ = Distance (inches) measured from block edges

$\bar{X}$	0	0.3	0.75	1.25	1.75	2.5	3.5	4.5	5.5
0						0.58	0.51	0.48	→
0.3					0.48	0.92	0.80	0.78	→
0.75				0.45	1.04	0.89	0.83	0.82	→
1.275			0.42	0.95	0.84	0.76	0.74		→
1.825	0.39	0.92	0.89	0.78	0.71	0.67			→
2.5	0.34	0.89	0.80	0.71	0.64	0.60			→
3.225	0.29	0.81	0.73	0.64	0.58	0.55			→
3.875	0.27	0.75	0.68	0.60	0.55	0.52			→
4.6	0.25	0.70	0.64	0.57	0.52	0.50			→
5.325	0.23	0.67	0.61	0.55	0.50	0.48			→

TABLE VI-24

Heating Distribution--Oscillating, $\eta = 0^\circ \pm 45^\circ$

SNAP 29 Six-Module Fuel Block (chamfered corners)

$(\dot{q}/\dot{q}_s \sqrt{R})_{\text{full scale}} \sim AR = 1.20$

$\bar{X}$ = Distance (inches) measured from block edges

$\bar{X}$	0	0.3	0.75	1.25	1.75	2.5	3.5	4.5	5.5
0						0.59	0.53	0.50	→
0.3					0.50	0.93	0.84	0.82	→
0.75				0.40	1.03	0.93	0.89	0.88	→
1.275			0.30	0.92	0.85	0.78	0.76		→
1.825	0.40	0.91	0.89	0.81	0.75	0.68			→
2.5	0.35	0.89	0.81	0.71	0.64	0.60			→
3.225	0.29	0.81	0.73	0.64	0.59	0.56			→
3.875	0.26	0.76	0.70	0.61	0.56	0.53			→
4.6	0.24	0.71	0.66	0.58	0.53	0.51			→
5.325	0.22	0.68	0.62	0.56	0.52	0.50			→

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TABLE VI-25

Heating Distribution--Oscillating, $\eta = 0^\circ \pm 45^\circ$

SNAP 29 Four-Module Fuel Block (chamfered corners)

$$(\dot{q}/\dot{q}_s \sqrt{R})_{\text{full scale}} \sim AR = 1.74$$

 $\bar{X}$ = Distance (inches) measured from block edges

$\bar{X}$	0	0.3	0.75	1.25	1.75	2.5	3.5	4.5	5.5
0						0.59	0.53	0.50	→
0.3					0.50	1.09	0.98	0.96	→
0.75				0.40	1.13	0.98	0.93	0.92	→
1.275			0.30	0.98	0.88	0.80	0.78		→
1.825	0.38	1.02	0.95	0.83	0.73	0.68			→
2.5	0.30	0.94	0.84	0.73	0.66	0.63			→
3.225	0.24	0.85	0.76	0.67	0.61	0.59			→
3.875	0.22	0.79	0.72	0.64	0.58	0.56			→
4.6	0.20	0.75	0.68	0.61	0.56	0.54			→
5.325	0.19	0.71	0.66	0.59	0.55	0.53			→

TABLE VI-26

Heating Distribution--Oscillating, $\eta = 0^\circ \pm 45^\circ$

SNAP 29 Two-Module (low AR) Fuel Block (chamfered corners)

$$(\dot{q}/\dot{q}_s \sqrt{R})_{\text{full scale}} \sim AR = 1.08$$

 $\bar{X}$ = Distance (inches) measured from block edges

$\bar{X}$	0	0.3	0.75	1.25	1.75	2.5	3.5	4.5	5.5
0						0.79	0.66		→
0.3					0.66	1.07	1.00		→
0.75				0.60	1.27	1.01			→
1.275			0.55	1.09	0.92	0.87			→
1.825	0.50	1.12	0.98	0.86	0.80	0.77			→
2.5	0.40	1.04	0.87	0.75	0.72	0.70			→
3.225	0.32	0.93	0.78	0.68	0.66	0.65			→
3.875	0.29	0.86	0.74	0.64	0.62				→
4.6	0.26	0.82	0.70	0.61	0.60				→
5.325	0.24	0.79	0.67	0.60	0.59				→

TABLE VI-27

Heating Distribution--Oscillating, $\eta = 0^\circ \pm 45^\circ$

SNAP 29 Two-Module (high AR) Fuel Block (hamfered corners)

 $(\dot{q}/\dot{q}_s \sqrt{R})_{\text{full scale}} \sim \text{AR} = 3.16$ $\bar{X}$ = Distance (inches) measured from block edges

$\bar{X}$	0	0.3	0.75	1.25	1.75	2.5	3.5	4.5	5.5
0						0.59	0.53	0.50	➔
0.3					0.50	1.33	1.18	1.16	➔
0.75				0.40	1.24	1.09	1.04	1.03	➔
1.275			0.30	1.05	0.94	0.86	0.84		➔
1.825	0.36	1.21	1.07	0.90	0.79	0.74			➔
2.5	0.24	1.03	0.91	0.79	0.73	0.70			➔
3.225	0.20	0.94	0.85	0.75	0.69	0.67			➔
3.875	0.18	0.90	0.83	0.73	0.68	0.66			➔
4.6	0.16	0.88	0.81	0.72	0.67	0.65			➔
5.325	0.15	0.87	0.80	0.72	0.67	0.65			➔

TABLE VI-28

Heating Distribution--Tumbling

SNAP 29 Eight-Module Fuel Block (chamfered corners)

 $(\dot{q}/\dot{q}_s \sqrt{R})_{\text{full scale}} \sim \text{AR} = 1.09$ $\bar{X}$ = Distance (inches) measured from block edges

$\bar{X}$	0	0.3	0.75	1.25	1.75	2.5	3.5	4.5	5.5
0						0.79	0.83	0.81	➔
0.3					0.73	0.68	0.64	0.62	➔
0.75				0.65	0.51	0.47	0.44	0.43	➔
1.275			0.60	0.48	0.39	0.36	0.34		➔
1.825		0.48	0.44	0.37	0.32	0.30	0.29		➔
2.5	0.22	0.36	0.34	0.31	0.27	0.25			➔
3.225	0.16	0.29	0.30	0.27	0.25	0.23			➔
3.875	0.13	0.26	0.27	0.24	0.23	0.21			➔
4.6	0.10	0.24	0.25	0.23	0.22	0.21			➔
5.325	0.09	0.22	0.23	0.22	0.21	0.20			➔

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TABLE VI-29

Heating Distribution--Tumbling

SNAP 29 Six-Module Fuel Block (chamfered corners)

 $(\dot{q}/\dot{q}_s \sqrt{R})_{\text{full scale}} \sim AR = 1.20$ $\bar{X}$ = Distance (inches) measured from block edges

$\bar{X}$	0	0.3	0.75	1.25	1.75	2.5	3.5	4.5	5.5
0						0.80	0.87	0.86	▶
0.3					0.76	0.70	0.65	0.64	▶
0.75				0.67	0.51	0.46	0.44	0.43	▶
1.275			0.61	0.46	0.38	0.35	0.33		▶
1.825		0.51	0.44	0.36	0.31	0.29	0.28		▶
2.5	0.21	0.36	0.34	0.30	0.27	0.25			▶
3.225	0.15	0.30	0.30	0.26	0.25	0.23			▶
3.875	0.11	0.27	0.28	0.25	0.23	0.22			▶
4.6	0.09	0.25	0.26	0.24	0.22	0.21			▶
5.325	0.09	0.24	0.26	0.24	0.22	0.21			▶

TABLE VI-30

Heating Distribution--Tumbling

SNAP 29 Four-Module Fuel Block (chamfered corners)

 $(\dot{q}/\dot{q}_s \sqrt{R})_{\text{full scale}} \sim AR = 1.74$ $\bar{X}$ = Distance (inches) measured from block edges

$\bar{X}$	0	0.3	0.75	1.25	1.75	2.5	3.5	4.5	5.5
0						0.87	0.89	0.87	▶
0.3					0.74	0.67	0.60	0.59	▶
0.75				0.69	0.50	0.48	0.44		▶
1.275			0.63	0.45	0.38	0.35	0.33		▶
1.825		0.51	0.44	0.37	0.31	0.29	0.28		▶
2.5	0.22	0.37	0.36	0.32	0.28	0.26			▶
3.225	0.15	0.31	0.32	0.28	0.26	0.24			▶
3.875	0.12	0.29	0.31	0.28	0.25	0.23			▶
4.6	0.11	0.27	0.29	0.27	0.24	0.22			▶
5.325	0.10	0.26	0.28	0.26	0.23	0.21			▶

TABLE VI-31

Heating Distribution--Tumbling

SNAP 29 Two-Module (low AR) Fuel Block (chamfered corners)

$$(\dot{q}/\dot{q}_s \sqrt{R})_{\text{full scale}} \sim \text{AR} = 1.08$$

 $\bar{X}$ = Distance (inches) measured from block edges

$\bar{X}$	0	0.3	0.75	1.25	1.75	2.5	3.5	4.5	5.5
0						1.14	1.12		→
0.3					0.89	0.71	0.65		→
0.75				0.78	0.51	0.44			→
1.275			0.71	0.45	0.37	0.34			→
1.825		0.62	0.44	0.34	0.31	0.30			→
2.5	0.23	0.41	0.34	0.30	0.29	0.28			→
3.225	0.14	0.36	0.31	0.28	0.28	0.27			→
3.875	0.13	0.34	0.29	0.27	0.27	0.26			→
4.6	0.12	0.32	0.28	0.26	0.26	0.25			→
5.325	0.11	0.31	0.27	0.25	0.25	0.24			→

TABLE VI-32

Heating Distribution--Tumbling

SNAP 29 Two-Module (high AR) Fuel Block (chamfered corners)

$$(\dot{q}/\dot{q}_s \sqrt{R})_{\text{full scale}} \sim \text{AR} = 3.16$$

 $\bar{X}$ = Distance (inches) measured from block edges

$\bar{X}$	0	0.3	0.75	1.25	1.75	2.5	3.5	4.5	5.5
0						0.98	0.95	0.91	→
0.3					0.83	0.77	0.72	0.67	→
0.75				0.74	0.56	0.51	0.47		→
1.275			0.68	0.56	0.42	0.37	0.35		→
1.825		0.60	0.51	0.41	0.35	0.32	0.30		→
2.5	0.25	0.42	0.40	0.35	0.32	0.27			→
3.225	0.18	0.35	0.37	0.33	0.29	0.26			→
3.875	0.16	0.34	0.36	0.32	0.28	0.26			→
4.6	0.15	0.33	0.35	0.31	0.28	0.25			→
5.325	0.15	0.32	0.35	0.31	0.28	0.25			→

7. Wind Tunnel Forced Oscillation Tests

Test specification for the forced oscillation test has been completed and released as Martin Marietta Drawing 466A3322083. One 55% scale stainless steel model of the four-module fuel block will be tested on an existing AEDC forced oscillation balance in Tunnel B to obtain aerodynamic damping coefficients for angles of attack at or near edge-on to the air flow for three roll angles. Model design has been completed and released as Martin Marietta Drawing 466A3322028. The test is scheduled to be conducted in Tunnel B at Mach 8.0 during one eight-hour shift in June.

8. Wind Tunnel Low-Density Force and Moment Test

A force and moment test has been planned to obtain three-component force and moment coefficients of a SNAP 29 modular fuel block configuration at Reynolds numbers appropriate to the transitional flow regime. The data from this test will be used with continuum flow test data previously obtained at higher Reynolds numbers and with theoretical values for the free molecule flow regime to predict the change in coefficients during re-entry. Test specifications have been completed and released as Martin Marietta Drawing 466A3322081. A contract to run the test in the Cornell 48-inch shock tunnel has been signed with Cornell Aeronautical Laboratory. Model design has been started, and testing is scheduled for June 1968.

9. Wind Tunnel Force and Moment Tests

The test report for the hypersonic force and moment test of the two-module and four-module fuel block configurations conducted in AEDC Tunnel B during November 1967 was completed and released as Martin Marietta Drawing 466A3322678. Comparison of the results for the two-module and four-module configurations with those obtained for an eight-module configuration are presented in this report to indicate the effect of aspect ratio on the aerodynamic coefficients. These results were presented in the Fifth Quarterly Progress Report (MND2062-12-7, October through December 1967). The tests were conducted at a Mach number of 8.0.

D. IMPACT TESTING AND ANALYSES--CONTROL POINT 3330

1. Water Jet Impact Test

The impact tests on the Sandia water jet catapult were completed and the test series was terminated. The impact orientations and velocities examined during the testing are given in Table VI-33. The orientations of 30° and 60° designate the angle between the target face and the plane of the two diagonals of the test specimen. All impacts occurred initially on the corner of the block whose adjacent edges formed equal angles with the target face. The blocks were impacted at ambient temperature.

TABLE VI-33

Impact Test Parameters

<u>Test No.</u>	<u>Orientation (deg)</u>	<u>Impact Velocity (ft/sec)</u>
1	30	90
2	30	90
3	60	90
4	60	90
5	60	135
6	60	135

In comparing the stress and strain concentrations for the first four impact tests, it was found that the steeper angle of 60° produced higher localized loading in the immediate area of impact. Thus, this angle was selected for examination at the higher velocity, Tests 5 and 6.

At this writing, data reduction is not complete. However, preliminary checks show that the recorded instrumentation data are valid. The results will provide meaningful empirical data in analyzing stress-strain relationships through the fuel block for varying impact parameters. They will also be applicable to studies of energy dissipation related to block breakup phenomena.

2. Input to Volume II of PSAR

A draft of the impact test progress to date has been prepared for input to Volume II of the PSAR. The final edited report was prepared in March.

3. Review of 1966 Sandia Helicopter Drop Tests

The films of the July and August 1966 helicopter drops sponsored by Sandia Corporation were reviewed. The purpose was to study and compare the motions of a high aspect ratio fuel block, such as the four-module design, with those of a low aspect ratio configuration, such as the eight-module design. A total of 18 drops were performed. Fourteen were with blocks weighing 40 pounds and measuring 0.69 x 27 x 32 inches (low aspect ratio). The remaining four tests used models weighing 44.5 pounds with dimensions of 0.50 x 18 x 48 inches (high aspect ratio).

Although official test data have not been released to date, some general results are known. All impact velocities ranged between 75 and 90 ft/sec and appeared to be independent of the release altitude and orientations. No common modes of stability were observed. The 27 x 32-inch specimens experienced various motions, although the majority tumbled about a slowly rotating diagonal. One block, however, was released broadside and impacted broadside, failing to flip even once during its flight. Rotational rates for these blocks ranged from 4.5 to 6.0 rps.

The 18 x 48-inch models also showed varied motion. Three attained a high angular rate of 16 to 19 rps around the long axis, which in turn oscillated in a conical sweep about the center of gravity. The fourth block saw a different motion, however--a slower rotation of about 5.0 to 5.5 rps around its short axis. In general, it can be said the high aspect ratio blocks, such as the four-module design, will have a higher rotational rate than the lower ratioed blocks, and have a preferred axis of rotation.

4. Matrix Evaluation Test Report

The test report for the matrix evaluation tests performed with the drop hammer was released as Report 466A3331042. Post-test X-ray and metallographic examination of selected fuel matrices showed only hairline cracks in specimens which were tested for radial loads. For all 20 tests, no significant difference could be found between the 1000° C sintered matrices and those sintered at 1500° C. Thus, there appears to be no advantage of one sintering temperature over the other for impact considerations.

5. Phase I Drop Tests

Evaluation of the Phase I drop tests conducted last November continued. Although data reduction is not complete, and although two tests alone do not provide sufficient examination to allow definite conclusions, some broad observations have been made.

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a. Unit cell versus fuel block

In December 1966, unit cell specimens about one-twelfth the size of an eight-module block were impacted at the rocket sled facility. Capsules held within the graphite cells easily survived velocities up to twice that attained in the drops. The extent of deformation was limited to a slight bend in one of the capsules. This suggested that unit cells might be appropriate for evaluations of full-sized block impact. The drop tests have disqualified this possibility, however. The extensive capsule deformation shows that unit cell tests are not representative.

b. Fuel matrix

Prior to the drop tests, the structural soundness of the SNAP 29 fuel matrix form was unknown. The matrix shock evaluation tests produced only insignificant cracks. The drops, however, have shown a matrix to pulverize when the capsule is badly deformed. Subsequent redistribution and collection of the granules may present a localized overheating problem. This condition is now being analyzed.

The drops were made with unheated specimens. Perhaps heated matrices might not crumble as they did at ambient temperatures. This is possible, but unlikely. As a case in point, the matrix shock tests mentioned above caused longitudinal cracks in matrices heated to 1000° F. Similar cracking was found in many of the unheated drop test matrices. Thus, elevated temperature is not expected to make a matrix significantly more ductile or elastic.

c. Impact media

One of the drop specimens hit the concrete target pad, the second impacted the adjacent dry lake bed. The soil impact did not cause less damage than the concrete as expected. In fact, capsule deformations were more extensive. A possible explanation for greater damage from soil impact is the fuel block orientation. An angle of about 72° between target and plane of the test specimen has been found for the concrete pad event. A steeper angle of about 90° occurred in the soil impact. This may have caused more energy to be dissipated in the impact corner of the block.

Regardless of the reason for greater damage, the result discounts earlier notions that the more rigid, nonyielding targets present greater impact threats.

d. Corner impacts

Both of the drop test blocks impacted on a corner. This allowed two preliminary conclusions. First, corner impacts tend to limit the number of capsules damaged. In both cases, the badly deformed capsules were located in the corner of the block that impacted first. Only four to six capsules were damaged in each drop. This is significant in that it may be possible to perform future controlled impact tests on full-sized blocks with fewer SNAP 29 capsules than planned.

The second important result is that steep angles seem to be more severe for corner impacts. The drop test blocks impacted at about 72° and 90°. The damage sustained to bronze capsules in the water jet tests for 30° and 60° is less extensive. Although the lack of rotational effects in the water jet tests must not be overlooked, preliminary indications are that the steeper the impact angle, the greater the capsule damage.

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A major goal of the Phase I drop tests was to determine seriousness of impact. This question is still unanswered. On one hand, it seems that there is no problem. That the capsules suffered appreciable deformation is insignificant, since no leaks occurred as a direct result; and containment integrity might be more favorable for heated capsules, since elevated temperatures are believed to render greater ductility.

On the other hand, though the capsules did not rupture they did deform to a greater extent and in different modes than had been anticipated. It may be true that elevated temperature should make a capsule more elastic, but this might lead to critical stretching and thinning of its walls. Internal capsule pressure might then pose a problem.

It is generally agreed that further tests should be performed to determine if hot, pressurized capsules of a four-module block react more favorably than did the cold unpressurized ones of an eight-module block. If they do, this will lend greater confidence to the fuel block and capsule designs.

6. Dynamic Data Development

Test Procedure No. DEV-TP-3332-01 (Dynamic Data Development) was released. Specimens to be used in the first of three test phases were manufactured. Strain gages were attached to the ATJ graphite specimens by a procedure adapted from one used at Sandia Corporation. The instrumented specimens were loaded at the rate of 2000 lb/min. The loads were applied under tension, compression and shear (at room temperature) to obtain stress-strain curves. All data, except exact ultimate shear stress, were reduced during this period. Further testing, which involves capsule materials and fuel block material at 1000° F, is yet to be conducted.

7. Impact Analyses

Computer code checkout for the energy-based equations of three-dimensional response to impact was completed. The solution was found to be divergent for all attempted parameter variations. Apparently, the method of solution used for this set of equations fails for the three-dimensional case, even though it has reportedly been used successfully for one-dimensional and two-dimensional cases. The effort involved in taking a different approach to this problem is considered to be beyond the level of effort scheduled for impact analysis. Thus, the three-dimensional impact computer program development was stopped.

E. THERMAL AND STRUCTURAL ANALYSES AND TESTS-- CONTROL POINT 3340

1. Launch Abort Tests

An ATJ graphite block, clad with nickel foil, containing simulated capsules and heated internally with 1000-watt cartridge heaters (Watlow), was included in a 25,000-pound Project PYRO drop test. The propellants involved were liquid hydrogen and liquid oxygen. The test block surface was penetrated in three places by flying fragments but did not break up, and no damage was sustained by the capsules.

An ATJ graphite block containing simulated capsules and cartridge heaters was installed in a PYRO 1000-pound LH₂/LO₂ drop test. Both the thermal and explosive yields were low and no damage was inflicted on the block or capsules.

In one Titan in-tank test involving 113,500 pounds of $\text{LO}_2/\text{RP-1}$, SNAP 29 had one heated block and one heated, bare, prototype capsule. The block was destroyed in the test environment with no penetration of simulated capsules; the bare capsule, which was exposed to 4000° F temperatures, was struck by fragments but containment was not breached.

In a second Titan test, the SNAP 29 program had one heated block, with simulated capsules, one unheated block and one bare, heated, prototype capsule. The heated block was demolished by a heavy fragment, capsules were somewhat distorted but not penetrated, the unheated block was neatly "sliced" by the single pulse initial wave. The bare capsule, blown to a point 355 feet from the test position, was not penetrated. Data from the foregoing tests have been correlated with Sandia Corporation representatives and presented in a final report.

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VII. MOUND LABORATORY EFFORT IN SUPPORT OF SNAP 29

As part of an extensive radioisotope fuels development effort being conducted at Mound Laboratory, a Po-210 program has been defined and implemented in support of the total SNAP 29 effort. This program comprises the radioisotope fuel and fueled heat source fabrication for the SNAP 29 RTG. Interface responsibilities among Martin Marietta, Mound and the AEC are identified in Specification MN-2062-4, Rev. A, "RTG Nuclear Fuel and Fuel Encapsulation Interface Plan, SNAP 29 Power Supply System."

The program being conducted at the Mound Laboratory is summarized in outline form as follows:

FUEL COMPATIBILITY

Potential container materials for polonium fuel forms are being evaluated at elevated temperatures in support of the SNAP 29 program.

C-Capsule Program

In a series of 1000° C tests, six capsule material-fuel combinations have been examined: tantalum-polonium metal; tantalum-gadolinium polonide; tantalum-10% tungsten-polonium metal; tantalum-10% tungsten-gadolinium polonide; Haynes-25-polonium metal; and Haynes-25-gadolinium polonide.

H-Capsule Program

Compatibility of tantalum and tantalum-10% tungsten with gadolinium polonide has been established at 1000° C by the C-series tests. Because of the possibility that the fuel operating temperature for the SNAP 29 system may exceed 1000° C, the H-series of tests at 1200° C was initiated. The H-series will be identical to the C-series except for the difference in temperature.

E-Capsule Program

This series is a test of the compatibility of tantalum with gadolinium polonide in a helium-purged vacuum furnace environment at the nominal temperature of 1500° C.

FUEL PROPERTIES (Po AND GdPo AND Ta MATRIX)

Information Constraining Safety Conclusions

- (1) Gaseous environment reactivity
- (2) Aqueous environment reactivity
- (3) Mechanical stability of Ta matrix--GdPo system using GdTe simulation
- (4) Thermal conductivity of Ta matrix
- (5) Capsule burial studies

Information Constraining Final Confirmation of Fuel Form

- (1) Fuel form vapor pressure
- (2) Phase equilibrium of $\text{GdPo}_{1-x}\text{Pb}_x$ $\left| \begin{array}{l} x = 1 \\ x = 0 \end{array} \right.$ system

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- (3) Gas blanket experiment
- (4) Fuel matrix studies
- (5) Die-pressed matrix strength studies
- (6) Hydrostatically pressed matrices

Polonium Fuel Properties Status Summary

MATERIAL WELD AND BRAZE STUDIES FOR CAPSULES AND HEAT SOURCE
(COMBINED MARTIN MARIETTA/MOUND PROGRAM)

Capsule Weld Development

- (1) Tantalum inner liner weld qualification
- (2) Tantalum-10% tungsten liner weld qualification
- (3) Capsule clad weld qualification

Capsule-Fuel Block Assembly Process

FUEL BLOCK CLAD DEVELOPMENT

Cladding Process Development

COLD ENCAPSULATION LINE

Fixture Development and Line Installation

Forty-Capsule Weld Qualification

Heat Source Assembly Qualification

Fabrication of Impact Reliability Test Specimens

HOT ENCAPSULATION FACILITY

Fixture Development and Line Installation

- (1) Fuel Loading
- (2) Capsule cooling

Qualification of Hot Capsule Fabrication Techniques

Qualification of Heat Source Fabrication Techniques

Production of One Demonstration Heat Source

PREPARATION OF FINAL PRODUCT SPECIFICATION FUELING
AND ASSEMBLY OF HEAT SOURCES AS AUTHORIZED

A. SUMMARY OF SIGNIFICANT ACCOMPLISHMENTS

Six of the original H-capsules (1200°C compatibility tests) were examined metallographically. Less than 0.001 inch of corrosion was found in all except the weld-affected areas of the tantalum and tantalum-10% tungsten capsules which were under test for 22 days. Weld-affected zones had corrosion up to 0.010 inch in depth. Two tantalum-10% tungsten capsules which were under test for 39 days were corroded to a depth of 0.005 inch.

A new series of compatibility tests (J-series) at 1100° C has been initiated. Except for the temperature and an outer oxidation clad, these tests will be identical to those for the H-capsules.

Several experiments to determine the particle size of material effusing from oxidizing polonium fuel forms have been carried out. These experiments used gadolinium telluride as a fuel simulant. Particles in the range of one to two microns were generally found.

The vapor pressure of polonium dioxide is being measured by the transpiration technique. Gas flow rates necessary to assure equilibrium between the solid polonium dioxide and the gas stream are being determined.

Gadolinium polonide vapor pressure measurements were made at 1600°C, using a Knudsen apparatus. Time-of-flight mass spectrometer measurements on gadolinium polonide indicate significant polonium loss at temperatures above approximately 1100°C. Additional data are needed to establish the vapor pressure of the compound.

A sealed can method for loading polonium into gadolinium-tantalum matrices was studied. This method appears to be feasible for fueling SNAP 29 heat sources.

An automatic filling and pressing technique for the fabrication of gadolinium hydride-tantalum matrices was tested for product consistency. The process will be satisfactory for production of SNAP 29 matrices.

One 9 x 9 x 1-inch ATJ graphite block was successfully coated with nickel at the nickel carbonyl facility at Mound Laboratory.

B. FUEL COMPATIBILITY

1. H-Capsule Program

The H-series (1200°C) compatibility tests were initiated to establish fuel-container material compatibility at or above the anticipated SNAP 29 operating temperature. The outer members of these original tantalum and tantalum-10% tungsten test capsules were oxidized due to failure of a cooling line in the furnace. Intergranular corrosion was found in several tantalum-10% tungsten capsules which were under test 39 days. In an attempt to further substantiate the corrosion pattern, several additional capsules were examined.

a. Experimental data

Two tantalum and two tantalum-10% tungsten capsules were metallographically examined after being in contact with gadolinium polonide at 1200° C for 22 days.

Two additional tantalum-10% tungsten capsules which contained gadolinium polonide for 39 days at 1200° C were also examined. Table VII-1 summarizes the metallographic and bend test results of all the individual capsules.*

Less than 0.001 inch of corrosion was generally found in the two tantalum and tantalum-10% tungsten capsules after 22 days at 1200° C. The corrosion that was observed was primarily intergranular and the average penetration was about 0.0005 inch. Figures VII-1 through VII-3 show the typical appearance of the slightly corroded regions of these capsules. (It is difficult to obtain well defined microstructures of the unalloyed tantalum specimens in the glove box facility. This must be taken into account when examining Figs. VII-1 and VII-2.

Figures VII-4b and VII-5 show the only area of excessive intergranular attack found in the "22-day" specimens. This area was located in the weld-affected zone of the tantalum-10% tungsten capsule H-17. Figure VII-5 corresponds to the highly magnified region in the top left-center area of Fig. VII-4b. Termination of the intergranular corrosion is visible in the highly magnified photograph. There also appears to be a precipitate in the uncorroded grain boundaries. It was not possible to identify this precipitate; however, there is a possibility of nitride formation since the capsule welding atmosphere was contaminated with nitrogen.

Examination of the two tantalum-10% tungsten specimens, H-1 and H-2, supported previously reported compatibility results.** Figure VII-6 is representative of the intergranular penetration which occurred to a depth of approximately 0.005 inch in these tantalum-10% tungsten capsules after 39 days at 1200° C. However, the tantalum-10% tungsten is readily embrittled and has demonstrated a possible susceptibility to intergranular corrosion in the weld-affected zone during this time period.

Repair of the vacuum furnace used in the H-tests was completed during this report period. Temperature standardization of the new furnace was also performed. The primary deviation in the performance of the new furnace is the operating vacuum. A vacuum of 5×10^{-5} torr is being obtained with the new furnace, meeting the manufacturer's specifications, whereas the old assembly operated at 5×10^{-6} torr. No experimental difficulties are expected because of this pressure deviation.

The desired number of new H-series capsules have now been prepared. These capsules are now being subjected to the 1200° C thermal exposure. Table VII-2 summarizes the loading data for the capsules, and the exposure time through the end of January. (The first three capsules, H-24, 25 and 26, were included in the thermal test; but, due to low power densities, three replacement capsules, H-36, 37 and 38, were fueled.)

Thermal testing of specimens H-28 through H-31 (tantalum-10% tungsten) and H-32 through H-35 (tantalum) was discontinued on February 12 as planned, after 41 days at 1200° C. These capsules are currently being prepared for metallographic examination.

*A description of the bend test procedure is contained in the SNAP 29 Power Supply System Fifth Quarterly Progress Report, MND2062-12-7.

**Ibid

TABLE VII-1

Summary of the Metallographic Analysis of the H-series Capsules

Specimen Number	Container Material	Power Density (watts/cm ³)	Neutron Flux (n/ci/sec)	Time at 1200° C (days)	Bend Test Results	Metallographic Results
H-1	Ta-10%W	30.0	71	39	Not tested	6 mils maximum intergranular penetration at base and 5 mils at top. Negligible corrosion on cylindrical walls.
H-2	Ta-10%W	28.3	106	39	Brittle	4 mils maximum intergranular penetration at base and 6 mils at top. Negligible corrosion on cylindrical walls.
H-3	Ta-10%W	Control		39	Not tested	Possible second phase particles in grains and grain boundaries.
H-8	Ta	31.3	51	22	Ductile <20-mil bend radius	Less than 1 mil corrosion in all areas of capsule.
H-10	Ta	29.1	90	22	Ductile <20-mil bend radius	Less than 1 mil corrosion in all areas of capsule.
H-11	Ta	Control		22	Ductile <20-mil bend radius	No corrosion observed
H-17	Ta-10%W	25.0	60	22	Brittle	Less than 1 mil corrosion, with the exception of a localized area in the bottom weld-affected zone. Intergranular penetration about 10 mils in this particular area. Possibility of second phase particles in grain boundaries.
H-18	Ta-10%W	25.1	84	22	Brittle	Less than 1 mil corrosion in all areas of capsule. Possibility of second phase in grain boundaries.
H-19	Ta-10%W	Control		22	Brittle	Second phase not readily visible in microstructure.

a. Top of capsule showing possible initiation of intergranular attack.

b. Base of capsule

FIG. VII-1. TANTALUM CAPSULE H-8 AFTER 22 DAYS AT 1200° C

FIG. VII-2. CYLINDRICAL WALL OF TANTALUM CAPSULE H-10 AFTER 22 DAYS AT 1200° C. THIS REGION IS JUST ABOVE THE BASE OF THE CAPSULE UPON WHICH THE FUEL RESTED

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a. Base of capsule

b. Detail of possible initiation of intergranular corrosion

FIG. VII-3. BASE OF Ta-10%W CAPSULE H-18

a. Base of capsule showing negligible corrosion

b. Base of capsule at weld affected zone showing apparent intergranular corrosion

FIG. VII-4. BASE OF Ta-10%W CAPSULE H-17

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FIG. VII-5. HIGH MAGNIFICATION OF WELD AFFECTED ZONE IN THE Ta-10%W CAPSULE H-17. TERMINATION OF THE INTERGRANULAR CORROSION AND POSSIBLE SECOND PHASE FORMATION IN THE UNCORRODED GRAIN BOUNDARIES ARE SHOWN

FIG. VII-6. BASE OF Ta-10%W CAPSULE H-2 AFTER 39 DAYS AT 1200° C

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TABLE VII-2

Summary of the Renewed H-Series Compatibility Tests

Specimen Number	Container Material	As Loaded Power Density (watts/cm ³)	Neutron Emission (n/ci/sec) ²	Time at 1200° C on 1/31/68 (days)
H-24	Ta	19.6	64	29
H-25	Ta	27.2	110	29
H-26	Ta	17.4	215	29
H-27	Ta	Control	--	29
H-28	Ta-10%W	34.0	78	29
H-29	Ta-10%W	32.2	93	29
H-30	Ta-10%W	31.2	521	29
H-31	Ta-10%W	Control	--	29
H-32	Ta	29.6	64	29
H-33	Ta	26.8	61	29
H-34	Ta	27.8	64	29
H-35	Ta	Control	--	29
H-36	Ta	21.0	47	17
H-37	Ta	18.3	59	17
H-38	Ta	28.3	54	17
H-39	Ta	Control	--	17
H-40	Ta-10%W	27.9	54	17
H-41	Ta-10%W	25.4	390	17
H-42	Ta-10%W	23.2	225	17
H-43	Ta-10%W	Control	--	17

¹Based on calorimetric measurement of polonium content and a capsule volume of 0.26 cm³.

²A neutron emission of greater than 100 n/ci/sec is indicative of a high oxygen content in the fuel.

b. Conclusions

The metallographic measurements obtained to date from the original H-capsules suggest that a simple diffusion-controlled reaction does not occur between the tantalum-10% tungsten and gadolinium polonide. Negligible corrosion occurred within the first 22 days at 1200° C followed by five or more mils during the next 17 days. These data are indicative of a type of corrosion mechanism that is preceded by a period of passivity.

2. J-Capsule Program

Due to the apparently negative compatibility results obtained from the original H-capsules, a new series of tests at 1100° C was initiated.

The program plan for the 1100° C tests is basically the same as for the H-series, 1200° C tests. Six tantalum and six tantalum-10% tungsten capsules are being loaded with gadolinium polonide at a power density of 30 watts/cm³ of capsule volume. Two control capsules of each material are also being prepared.

The dimensions of the inner test capsule and the tantalum-10% tungsten outer strength body are identical to those of the H-series capsules. However, because of limited furnace facilities in the alpha glove box lines, the 1100° C tests are not being conducted in vacuum. An argon atmosphere tube furnace is being used for the thermal exposure. To minimize contamination, the refractory metal capsules were weld-sealed inside Hastelloy-X capsules having a minimum wall thickness of 0.1 inch. Three-mil-thick tungsten foil was placed between the Hastelloy-X and the tantalum-10% tungsten strength capsule to minimize metallic interdiffusion.

Preparation of the inner test capsules was completed on February 29, and they are currently being subjected to the thermal exposure.

No results are available, since the thermal test is still underway.

C. FUEL PROPERTIES

1. Particle Size Determinations

Under certain conditions, the exposure to air of reduced polonium fuel forms, such as that specified for use in the SNAP 29 program, could result in oxidation and subsequent release of radioactive material. To evaluate the potential biological hazard involved, it is necessary to obtain data regarding the character of the evolved particulates. For this reason, methods are being investigated for determining the particle size distribution and constitution of airborne material collected in laboratory experiments. Initial results of this work were reported in the SNAP 29 Power Supply System Fifth Quarterly Progress Report.

a. Experimental data

Several additional experiments have been made to develop a suitable particle sampling and evaluation scheme. Figure VII-7 is a schematic representation of the laboratory apparatus used in these experiments. Mock fuel specimens, composed of approximately 6 wt % gadolinium telluride in an annular configuration of compacted tantalum, were used in these tests. The specimens were heated in an air stream at 830° to 930° C and the evolved airborne material was collected and examined.

In experiment No. 4, a filter train composed of several membrane filters was utilized as a particle collector in an attempt to separate the airborne material into various size ranges. Filter disks of 8-, 1.2-, 0.1- and 0.05-micron porosity were arranged in the order of decreasing pore size, and the gas flow from a sample exposed to air at 820° C was passed through the train. The filters were microscopically examined and a sample of each filter was analyzed by emission spectroscopy.

Microscopic examination indicated that particles had deposited only on the first (and also the coarsest--8 microns) filter disk, even though the basic particle size was about the same as in previous tests (one micron or less). Emission spectrographic analysis of sections of each filter verified that only the 8-micron disk had trapped evolved material and that it contained tellurium. This result can be explained on the basis of random entrapment of particles within the filter with reduction in effective porosity, agglomeration by electrostatic attraction and/or surface binding of the filter.

In the remainder of the experiments a device called a "thermal precipitator" was used. A cross-section sketch of this apparatus is shown in Fig. VII-8. The thermal

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FIG. VII-7. PARTICLE SIZE APPARATUS

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precipitator works on the principle that the deposition of particles from a gas stream onto a cooled surface can be effected when the stream passes between heated and cooled surfaces. The main advantages of particle collectors of this type are that they are suitable for small samples with a wide range of particle sizes, they remove solids without altering their physical characteristics, and they deposit samples directly on a substrate suitable for microscopic examination.

The collection efficiency of the thermal precipitator at a low sampling flow rate was tested in an experiment in which a 0.45-micron membrane filter was used as a backup collector. The disks from both collectors were weighed to determine the quantity of deposit on each and a material balance was calculated. The thermal precipitator collection disk and the membrane filter were examined with an optical microscope. According to the weight measurements, virtually all the airborne particulates were collected by the thermal precipitator; no weighable deposit was collected on the filter disk.

The results of the experiments using both the filters and the thermal precipitator are summarized in Table VII-3. Experiments Nos. 2 through 8 were carried out during this report period.

In the thermal precipitator experiments the carrier gas flow rate was varied from 25 to 270 cm³/min; the corresponding air velocities at the sample point were 11 to 119 cm/min. The deposits collected in the thermal precipitator were weighed and examined to determine particle size and chemical composition.

In each experiment, the gross particle size of the airborne material was 1 to 2 microns as determined by optical microscopy. The particles appeared to be very uniform in size and spherical. The quantity of deposit which collected upon the sample disk varied from 2.8 mg to 39.6 mg and increased as the carrier gas flow rate increased.

Emission spectrographic analysis of the deposits showed them to be composed of a tellurium species; other elements initially present in the mock fuel specimen were not detected. The deposits as collected were X-ray amorphous. Two samples from experiments Nos. 6 and 7 yielded identical X-ray patterns characteristic of tellurium dioxide after annealing in air at 410° C for six hours. The tellurium dioxide was present in two allotropic modifications, the major species being the paratellurite and the minor species being the tellurite. No other constituents were detected.

In preparation for similar tests with gadolinium polonide, fuel specimen deposits from GdTe tests are being examined in the "hot" metallographic facility to determine if that equipment is suitable for particle size evaluations.

b. Conclusions

The thermal precipitator appears to be an effective device for collecting airborne particles from burnup of mock fuel forms. Varying the carrier gas flow rate from 25 to 270 cm³/min did not apparently alter the particle size or chemical composition of material collected in the thermal precipitator. The particle size of material collected in tests made to date was 1 to 2 microns as determined by optical microscopy. The carrier gas flow rate did affect the quantities of deposit collected, with the higher flow rates producing larger quantities of deposit. Emission spectrographic analysis of the collected material showed it to be composed of a tellurium species. X-ray diffraction patterns of annealed samples were indicative of tellurium dioxide.

TABLE VII-3

Summary of Polonium Fuels Particle Size Experiments

Experiment No.	Carrier Gas (air) Flow Rate (cm ³ /min)	Exposure		Particle Collector Type	Deposit Chemical Analysis	Deposit Particle Size (microns)	Deposit Weight (mg)
		Temperature (°C)	Time (hr)				
1	500	860	2	1.2-micron filter	Te (major) Ta (minor)	0.1 to 0.2 (determined by electron microscope)	--
3*	25	910	1	Thermal precipitator	--	1 to 1.5	--
4	25	820	3/4	8.0-, 1.2-, 0.1-, 0.05-micron filters	Te (major) (deposit on 8.0-micron filter only)	1 to 2	--
5	25	930	1	Thermal precipitator	--	1 to 2	2.8
6	270	930	1	Thermal precipitator	TeO ₂ -- paratellurite major phase, tellurite minor phase (by X-ray diffraction analysis)	1 to 2	39.6
7	88	885	1	Thermal precipitator		1 to 2	14.1
8	165	920	1	Thermal precipitator	--	1 to 2	33.0

*Experiment No. 2 was aborted due to very low flow rate.

2. Polonium Oxide Vapor Pressure Determinations

The vapor pressure of polonium dioxide at elevated temperatures is being studied to develop some of the basic data required to evaluate the probable consequences of atmospheric exposure of reduced polonium fuel forms. The method used in these studies is a transpiration (gas saturation) technique. This method was chosen because the simplicity of the equipment and ease of interpretation of the data make it readily adaptable to experiments involving radioactive species.

a. Experimental data

The transpiration method for measuring vapor pressure involves saturating a measured volume of carrier gas with vaporized polonium dioxide at a constant temperature. The quantity of polonium condensed in a collection tube is measured by calorimetry. Since the experiment is conducted in an oxygen atmosphere, the assumption is made that all polonium exists as polonium dioxide. From the quantity of condensed polonium dioxide and the volume of gas passed over the sample, the partial pressure (p_1) of the PoO_2 is calculated using Dalton's law and assuming the ideal gas law

$$p_1 = \frac{G_1}{M_1} \left(\frac{RT}{V} \right)$$

where R is the gas constant in appropriate units, T is the absolute temperature at which the measurement is made and V is the total volume* of gas (carrier and vapor) containing a mass G_1 of vapor of molecular weight M_1 .

Several runs at one temperature are usually required to ensure that conditions are adequate to effect saturation of the carrier gas. Saturation is assumed when successively lower flow rates of carrier gas do not result in higher vapor pressure values.

Figure VII-9 is a sketch of the apparatus which was designed for these studies. The carrier gas is exhausted through a soap bubble flowmeter (not shown) after being depleted of the vaporized PoO_2 . From the measured flow rate and the time of the experiment, the total volume of gas used in an experiment is computed. In these studies, oxygen is being used as a carrier gas because of the tendency of PoO_2 to undergo thermal decomposition:

It is reported that polonium dioxide is stable when heated in oxygen at 1 atmosphere, up to a temperature of 900° C.**

*If the vapor pressure of the material being determined is low compared to atmospheric pressure, the volume of the vaporized species can be neglected in the calculations.

**Moyer, "Polonium," Technical Information Service Extension USAEC, 1956.

FIG. VII-8. THERMAL PRECIPITATOR

FIG. VII-9. TRANSPIRATION VAPOR PRESSURE APPARATUS

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Three runs have been made at approximately 730° C with different flow rates of carrier gas. Additional tests are necessary, and are being planned, to determine the lowest carrier gas flow rate which is required to ensure saturation by the vapor species. Vapor pressure studies at other temperatures are also being planned.

b. Conclusions

The data obtained in the runs made thus far are not sufficient to compute a meaningful vapor pressure for polonium dioxide, since the necessary flow rate has not been established. Reproducibility of the procedure must also be established before significant vapor pressure values can be calculated.

3. Gadolinium Polonide Vapor Pressure Measurements

Two methods are being employed at Mound to measure the vapor pressure of gadolinium polonide. In addition to a Knudsen cell (heat and weigh technique) apparatus, a time-of-flight mass spectrometer is also being used to identify the vapor species. The vapor pressure of the compound is, of course, an important measurement in the SNAP 29 safety analysis. The composition of the vapor may be important from a compatibility standpoint.

a. Experimental data

The equipment used in measuring the vapor pressure by the Knudsen technique consists of a large stainless steel vacuum chamber inside of which an induction heating coil is mounted. The Knudsen cell containing the sample is suspended in the coil and heated under vacuum. The cell temperature can be optically measured through a quartz window in the vacuum chamber. Weight loss due to sample vaporization can be determined by weighing the cell and its contents before and after each heating cycle.

During several preliminary experiments with this apparatus, a great deal of difficulty was encountered in both heating and weighing the cell. As a result, the data show a large amount of scatter. All measurements to date have been made at a nominal temperature of 1600° C. The vapor pressure at this temperature is estimated to be $6 \times 10^{-5} \pm 4 \times 10^{-5}$ atmosphere.

Time-of-flight mass spectrometer measurements were made on a gadolinium polonide sample of about 170 mg. The sample (in a tungsten crucible) was placed in the mass spectrometer where it maintained a self-heated temperature of 900° C overnight. Polonium⁺ was observed in the sample vapor initially but disappeared after the overnight period.

No vapor species were observed upon heating to 1000° C for two hours; however, when the temperature was increased to approximately 1100° C, an intense Po⁺ peak was noticed. Upon further temperature increase to 1440° C, the Gd⁺ species was detected. The Po⁺ species slowly decreased in intensity at this temperature until it was no longer discernible. After cooling and reheating the sample, the Gd⁺ species was again briefly observed but then disappeared. The sample was heated to 1950° C with no further vapor species being observed.

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b. Conclusions

The results of the time-of-flight mass spectrometer measurements indicate that polonium vapor dissociates from gadolinium polonide at approximately 1100° C. Since this indication is based on only one experiment, additional runs will be made to confirm the results. Since the Knudsen measurements were carried out at approximately 1600° C, all the polonium may have been vaporized from the sample. In this event, only the vapor pressure of gadolinium would be measured.

4. Matrix Fuel Loading

A program is under way to determine the parameters to be controlled for the production of tantalum-gadolinium polonide fuel matrices for the SNAP 29 heat source. The materials are being combined at various temperatures in order to find the lowest necessary operating temperature. The reaction is carried out in sealed tube containers under helium atmosphere. Currently, excess gadolinium for a one-to-one reaction is being used; in the future excess polonium will be tested.

a. Experimental data

The reaction tube or bomb used in this work was similar to a stainless steel calorimetry can with a soft copper O-ring seal. Material balances within the reaction can have been obtained, indicating that the sealed cans are not leaking polonium.

The reaction between the tantalum-gadolinium matrix and polonium is achieved under a helium atmosphere inside the can. Polonium plated gauzes are placed in the bottom of the can, the matrices are then mounted on top of the gauzes and the cans are sealed by screwing the can down tightly on the O-ring seal. A muffle furnace located in a helium dry box is used to heat the cans to the reaction temperatures.

Excess gadolinium over the amount required for a one-to-one reaction with polonium was added to the matrices used in the five experiments described. Table VII-4 shows the results of reactions carried out at 950° and 825° C using polonium-plated platinum gauzes. Results of a previous experiment using polonium distilled from gauzes are also shown. In both the 825° and 950° experiments, 68% of the gadolinium placed in the matrix was used up in the reaction with polonium.

TABLE VII-4
Sealed Can Matrix Fuel Loading

	<u>Plated</u> <u>Gauzes</u>		<u>Distilled</u> <u>Polonium</u>
Temperature (°C)	950	825	950
Initial polonium (ci)	1221	1019	2503
Reacted polonium (ci)	1054	1010	2390
Fuel neutron flux (n/ci/sec)	276	329	190
Available Gd, * (ci-eq wt)	1531	1486	2700
Available Gd, ** (ci-eq H ₂)	1180	1240	2540
% Po reacted	86	99	95
% Gd reacted* (wt)	68	68	88
% Gd reacted** (H ₂)	90	82	94

*The number of curies of polonium required to react in a one-to-one ratio with the total amount of gadolinium which was used in the matrix.

**The number of curies of polonium required to react in a one-to-one ratio with the amount of gadolinium which was dehydrided.

There is undoubtedly some oxidation of gadolinium hydride during the matrix fabrication process. Due to this fact, the amount of hydrogen given off from the GdH_x compound is determined for each batch of gadolinium hydride made. Taking into account only the amount of gadolinium which was in the GdH_x compound, the reaction efficiencies rise to 90% and 82% completion, respectively, for the 950° and 825° C reactions.

Table VII-5 shows results of loadings at 600° and 700° C. An attempt was made to maintain a 20% excess of gadolinium over the amount required for a 1:1 reaction. In the 600° C experiment, there was a 25% excess; and in the 700° C experiment, the excess was actually 13%. The difficulty in maintaining the 20% value comes about because, at the present time, the polonium production calorimeter is not operable and only estimated values for the amount of polonium are received. The initial and final values for the polonium quantities reported in Tables VII-4 and VII-5 were measured calorimetrically after the reaction. The initial value is the sum of the matrix polonium and the polonium left in the reaction can.

TABLE VII-5
Polonium Matrix Loading Data

Temperature (°C)	600	700
Initial polonium (ci)	1218	1277
Reacted polonium (ci)	1187	1240
Fuel neutron flux (n/ci/sec)	212	240
Available Gd, (ci-eq wt)*	1681	1450
Available Gd, (ci-eq H_2)**	741	1079
Percent Po reacted	97	97
Percent Gd reacted (wt)	70	85
Percent Gd reacted (H_2)	164	114

*The number of curies of polonium required to react in a one-to-one ratio with the weight of gadolinium used in the matrix.

**The number of curies of polonium required to react in a one-to-one ratio with the amount of gadolinium based on H_2 loss.

b. Conclusions

The most important conclusion to be drawn from these studies is that high reaction efficiencies have been attained at all temperatures tested to date. The reaction efficiency at 600° C was high enough that the reaction temperature will be lowered further. The reported value of reaction temperature may well be several hundred degrees lower than the actual temperature in the reaction can due to the self heating by the polonium. There are also some indications that the lower temperature reactions are resulting in a lower neutron count in the product; however, more data are needed to firmly establish this trend.

As can be seen from Table VII-5, the amount of available gadolinium as calculated from the hydrogen loss does not appear to be valid. This could be due

to retention of the hydrogen by the outgassing chamber or the tantalum matrix or to loss of hydrogen through the vacuum system. Further testing will be done in an attempt to solve this problem.

5. Matrix Production

The variations in matrix height and density can be closely controlled in hand-pressed matrices by weighing the material which goes into each matrix and controlling the pressing pressure. For SNAP 29 fuel fabrication, however, a more rapid pressing procedure using a gas-operated press is planned. In this procedure, a fast, accurate method of measuring the quantity of powder is also required. The purpose of the experiments described below was to determine if the pressing procedure would produce useful matrices.

a. Experimental data

An Accofil technique employing a vacuum-operated fill tube was used to measure a given amount of powder for each matrix. This powder was then released into the die cavity of the gas-operated press. Table VII-6 presents typical results of a press run. The first column in the table indicates the weights as measured by the Accofil tube. The gas pressure at which the press was operated in this run was kept at a constant 200 pounds per square inch. As can be seen from Table VII-3, the length of the matrices varied from 0.262 to 0.272 inch and the density ranged from 57.9% to 59.2% of theoretical density. The absolute value of the average density can be changed by increasing or decreasing the operating pressure.

TABLE VII-6
Matrix Preparation

<u>Weight of Tantalum (gm)</u>	<u>Length (in.)</u>	<u>Percent Theoretical Density*</u>
4.312	0.262	57.9
4.369	0.262	58.7
4.408	0.263	59.0
4.512	0.269	59.0
4.517	0.270	58.9
4.379	0.266	57.9
4.510	0.268	59.2
4.565	0.272	59.1
4.482	0.268	58.9
4.502	0.270	58.7
4.435	0.269	58.0
4.517	0.270	58.9

*On the basis of Ta density, 16.6 gm/cm³

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b. Conclusions

The results of the experiment show that the process will be satisfactory for the SNAP 29 program from the standpoint of matrix fabrication. The press will be further tested to determine if the equipment is reliable for production of large quantities of matrices.

6. Polonium Fuel Properties Status Summary

Mound Laboratory has established a research program to measure basic physical and chemical properties of polonium fuels. Table VII-7 presents a summary of the data compiled to the present for gadolinium polonide in a tantalum matrix. It is planned to update this information for the SNAP 29 fuel form in each SNAP 29 Quarterly Progress Report (see Table VII-8).

TABLE VII-7

Polonium Fuels Property List

1. Neutron spectrum and emission rate
2. Half-life
3. Alpha spectrum and emission rate
4. Gamma spectrum and emission rate
5. X-ray spectrum and emission rate
6. Impurities in fuel form
7. Chemical reactions and rates in air as a function of pressure
8. Chemical reactions and rates in fresh water
9. Chemical reactions and rates in seawater or equivalent
10. Chemical reactions and rates in soil (model to be specified by Martin Marietta)
11. Chemical reactions and rates with capsule materials
12. Chemical reactions and rates with graphite
13. Solubility constants of reaction products
14. Density of predominant reaction products
15. Vapor pressure of GdPo and PoO
16. Melting point
17. Boiling point
18. Heat of fusion
19. Heat of vaporization
20. Specific heat
21. Thermal conductivity of matrix
22. Heat of reaction of GdPo with oxygen
23. Reactions and reaction rates with propellants and propellant reaction products to be specified by Martin Marietta
24. Fuel state as a function of environmental conditions to be determined by Martin Marietta
25. Density of GdPo
26. Stoichiometry of GdPo
27. Specific activity of Po
28. Matrix density (final product)
29. Volume ratio of GdPo to matrix material

TABLE VII-7 (continued)
Polonium Fuels Properties

Property No	Available Data	Date for New Data	Comments
1	1 0 to 2 5 Mev 2 1 x 10 ³ neutrons/w-sec	5/69	Available in Po metal data sheet New data for encapsulated fuel
2	138 375		Available in Po metal data sheet
3	5 298 Mev - 1 155 x 10 ¹² alpha/w-sec) 4 5 Mev - 1 410 x 10 ⁷ alpha/w-sec)		Available in Po metal data sheet
4	0 804 Mev - 1 41 x 10 ⁷ photons/w-sec)		Available in Po metal data sheet
5	0 077 Mev - 1 7 x 10 ⁶ photons/w-sec) 0 01 to 0 015 Mev - 3 4 x 10 ⁸ photons/w-sec)		Available in Po metal data sheet
6	<div> <div> Te - ~1% Se - ~0 1% O₂ - 2 to 4% Pb - time dependent </div> <div> } Po metal </div> </div> <div> <div> N₂ - ppm Re - ppm </div> <div> } Gd metal </div> </div> <div> <div> C - 100 ppm O₂ - 1000 ppm H₂ - 150 ppm N₂ - 100 ppm B - 5 ppm Na - 50 ppm Ni - 50 ppm Ca - 20 ppm Cr - 30 ppm Sn - 20 ppm Zr - 20 ppm Ti - 30 ppm Si - 100 ppm W - 100 ppm Fe - 100 ppm Mn - 20 ppm Al - 20 ppm Mo - 20 ppm Nb - 50 ppm Other - 45 ppm </div> <div> } Ta matrix impurities </div> </div>		Available in Po metal, GdPo and GdPo in tantalum matrix data sheets (Pb impurity increases at an approximate rate of 0 5% of Po content per day) All data are maximum impurities of components--To find maximum impurities in GdPo-Ta matrix fuel, add impurities in ratio
7	Rate of oxidation of Po in a GdPo-Ta matrix = 50 ci/hr	7/69	Data is maximum rate during first hour of air exposure test No 2
8		4/69	
9		4/69	
10			Mound has no plans to determine this property
11		8/68	
12			Schedule not determined
13		4/69	
14		9/69	
15	For GdTe → Gd (g) + Te (g) $\text{Log } P_{\text{total}} = -2 \frac{4372 \pm 0 \ 1253 \times 10^{-4}}{T^{\circ}\text{K}} + 7 \ 37 \pm 0 \ 76$	2/68	Estimate for GdPo on the basis of that for GdTe Data available in GdPo data sheets
16	GdPo melting point = 1675° C Ta melting point = 2996° C		Data available in GdPo in Ta matrix data sheets Data for Gd-Po-Pb system available in GdPo data sheets
17		2/68	To be calculated from GdPo vapor pressure measurements
18	For Ta $\Delta H_{(f)}$ = 34 6 to 41 5 cal/g at = 3000° C	2/68	Data available in GdPo in Ta matrix data sheets GdPo data to be calculated from GdPo vapor pressure measurements
19	For Gd (Te) ₃ → Gd (g) + Te (g) $\Delta H_{(v)} = 111 \ 5 \pm 5 \ 7 \text{ cal/mole}$ For Ta $\Delta H_{(v)} = 0 \ 995 \text{ to } 1 \ 021 \text{ kcal/g}$		Estimate available in GdPo in Ta matrix data sheets To be calculated from GdPo vapor pressure measurements
20	Sp H GdPo at 25° C = 0 034 cal/g-°C Sp H GdPo at 1000° C = 0 038 cal/g-°C Sp H GdPo at 2000° C = 0 043 cal/g-°C Sp H Ta matrix at 25° C = 0 034 cal/g-°C	4/69	
21	For 50 l Ta GdPo wt ratio at 293° K $Q = 8 \ 2 \times 10^{-2} \text{ cal (sec-cm } ^{\circ}\text{C)}^{-1}$ For 10 l Ta GdPo wt ratio at 293° K $Q = 7 \ 2 \times 10^{-2} \text{ cal}$	4/69	Estimate available in GdPo in Ta matrix data sheets
22		2/68	Calculated from polonium oxide vapor pressure measurements
23			Mound has no plans to determine this property

TABLE VII-7 (continued)

Property No	Available Data	Date for New Data	Comments
24		4/69	Gd-Po-Pb phase diagram available in GdPo data sheet. Thermal profile test will provide more data. Adequate environmental conditions have not been defined for the thermal profile test.
25	For GdPo density = 9.78 g/cm^3		Data available in GdPo data sheet
26	1.1		Data available in GdPo data sheet
27	4500 ci/gm		Data available in the Po metal data sheets
28	For 50:1 Ta-GdPo wt ratio 10.4 g/cm^3 at 63% theoretical density For 10:1 Ta-GdPo wt ratio 10.3 g/cm^3 at 64% theoretical density For 10:1 Ta-GdPo volume ratio 9.91 g/cm^3 for 62% theoretical density		Data available in the GdPo in Ta matrix data sheets
29	Minimum Ta volume/GdPo volume = 10/1		A smaller ratio than 10/1 may require further testing of fueling technique

D. FUEL BLOCK CLAD DEVELOPMENT

1. Cladding Process Development

Because of nickel adherence problems encountered in the Martin Marietta fuel block clad development program, Mound was asked to investigate nickel deposition from nickel carbonyl as the cladding method. A preliminary investigation has been initiated by cladding and testing one 9 x 9 x 1-inch ATJ graphite block.

a. Experimental data

On contact with surfaces heated to a temperature of about 100° C or higher, nickel carbonyl ($\text{Ni}(\text{CO})_4$) decomposes into nickel, which plates out on the surface, and carbon monoxide, which is exhausted through a suitable system. Such a process has been used to produce nickel plates of very high purity on metal substrates. Plating of graphite surfaces had not been attempted at Mound prior to this effort.

The block used in the first test was 9 x 9 x 1 inches and had sharp, unrounded corners. The Mound facility does not permit a one-step coating to be applied to the whole block. Radiographs were made of the block before and after each of the two coating steps. The first step was carried out with the block at 150° C (300° F) and required four hours. The depth of the nickel on the first coat varied from 0.040 to 0.080 inch. Because of the difference in thermal expansion between ATJ graphite and nickel (nickel = 13.3×10^{-6} , graphite = 3.2×10^{-6}) the block was bowed concave toward the nickel.

The parameters for the second coating step were essentially the same as for the first except that a more efficient carbonyl distribution system was employed. The depth of the nickel was 0.035 to 0.040 inch on this side of the block.

The following thermal tests were run on the fully clad block: two 1-hour and one 16-hour air exposure at 870° C (1600°F). All the tests were initiated by placing the block, at ambient temperature, inside the furnace at 870° C. At the end of the heating period, the hot block was placed on a cold surface and allowed to cool to ambient temperature. During the first 1-hour test, several bubbles developed in the coating overlap area on the side of the block. Later examination showed that these bubbles resulted from separation of the two nickel layers and did not involve separation of the nickel from the graphite. When the block was withdrawn from the furnace after the 16-hour exposure, a dark area, indicating rapid cooling, was noticed on one side.

Upon cooling, a crack in the nickel extending into the graphite was discovered in this area; however, the nickel was not separated from the graphite. Figure VII-10 shows this area of the block after thermal testing. It was estimated from radiographs that the crack in the block extended about 3/4 inch into the graphite and was in an area where the nickel was very thin.

It is believed that the crack resulted from high stresses in the graphite possibly caused by the overly thick nickel coatings. An attempt will be made to coat the next specimens to a depth of about 0.020 inch to reduce the amount of stress on the graphite.

An attempt was made to clad another 9 x 9 x 1-inch block before moving the carbonyl plating facility to another building. A small section (approximately 1 x 1/2 inch) of the surface of this block remained uncoated by nickel. This was probably due to insufficient cleaning of the block prior to cladding.

Preparations are being made to nickel coat several 3 x 3 x 1.3-inch blocks to be thermally tested by Martin Marietta.

b. Conclusions

The important results of this first test are the facts that the carbonyl process can be used to nickel coat ATJ graphite and that the coating does not separate from the graphite as a result of vigorous thermal cycling. It is anticipated that the coating process can be refined to achieve a thinner, more uniform clad, thereby reducing the stresses set up in the graphite.

FIG. VII-10. NICKEL-CLAD BLOCK AFTER THERMAL CYCLING

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APPENDIX A

SNAP 29 FUEL CAPSULE DESIGN STUDY

This report provides a structural evaluation of the fuel capsule outer liner. As a result of this analysis, the fuel capsule and fuel block thickness baseline designs were established. In addition, the required cladding is evaluated structurally assuming the gas gap between the outer liner and cladding is initially one atmosphere. The environment of re-entry heating is the principal factor examined to establish this design. The evaluation is based upon the present four-module fuel block design with a maximum thermal wattage of 4400 (4160 watts nominal).

The fuel capsule outer liner is manufactured from fully annealed (recrystallized) Ta-10%W extruded tubing and is considered the strength member of the capsule system to withstand the internal pressure excursion as a function of thermal environment. The outer liner is concentric with a nominal 0.020-inch inner liner of tantalum and an outer cladding of 0.020-inch wall manufactured from an alloy of platinum-rhodium.

These latter two shells do provide some degree of structural support in conjunction with the structural liner. However, at re-entry temperature environments of from 2400° to 3000° F, their contribution is very minor and is therefore neglected.

In this study, four, five and six capsules per modules are considered.

The pressure loading on the Ta-10%W cylindrical liner results from helium generation within the fuel during operation and is a function of isotope decay. From the time of encapsulation of the fuel through the period of generator operation, the helium temperature is ~1800° F maximum. Based upon this magnitude, the required outer liner thickness is relatively thin to meet this loading. Creep-strain will result over this history span; however, this effect is not expected to be critical. A creep analysis will be provided as an addendum to this report at a later date. The critical or design loading is derived from the extreme temperature excursion (and, thus, a pressure increase) during the peak re-entry heating. A nominal peak re-entry temperature of ~2500° F is anticipated. Via an error analysis based upon the various confidence levels of analysis inputs to achieve a peak temperature, temperatures up to 2935° F are possible.

The error analysis, Ref. A-1, associated with the expected peak re-entry temperature of the fuel block is primarily based upon the confidence levels of aerodynamic analysis. In brief, all individual analysis confidence margins are combined via probability techniques to establish a peak re-entry temperature corresponding to three standard deviations from the mean. The maximum temperature is at present approximately 2935° F. A margin of 3σ implies that the temperature will exceed 2935° F only twice in every sample of 1000. Upon completion of plasma arc and wind tunnel testing, the confidence level of establishing a re-entry environment will increase and thus the maximum temperature expected will decrease accordingly. Later in this report, the increased confidence in temperature predictability will be reflected.

The planned maximum mission life or time from encapsulation of the isotope through generator operation is 134 days. At this time the generator is boosted back into the earth's atmosphere and re-entry commences. However, should the deboost system fail to operate and the system is in a potential 250-nautical mile orbit, the generator could conceivably maintain orbit for three years or approximately 1000 days prior to orbital decay. Re-entry at day 1000, its associated total

helium generation and a potential re-entry temperature of 3000° F would result in a significantly high capsule loading. This assumption will be taken as the present critical design condition and will be applied as a basis throughout this evaluation.

A. MATERIAL DATA

The material of interest, tantalum-10% tungsten, is certainly not as well defined as aluminums or steels when one discusses physical or mechanical properties at elevated temperatures. The particular state of the material, that is, fully annealed, 90% hard, stress relieved, cold rolled condition, etc., complicates the search even further. A majority of the available data providing tensile ultimate and yield strength at elevated temperature is thought of to be in error for two reasons:

- (1) Much of the testing has been performed in an argon atmosphere and the resulting elongations are much lower than would be expected.
- (2) Data obtained prior to about 1963 may be in error since the alloy has been more recently processed with a lower percentage of impurities. This should imply better welding characteristics but also indicates lower strengths.

Figure A-1 presents available ultimate and yield strengths versus temperature to date. The wide spread of data is certainly indicative of a low confidence level when attempting to choose a reasonable capability at any finite temperature state.

Secondly, the data per Fig. A-1 are obtained from the typical low strain rates associated with standard tensile tests. The order of magnitude is usually from 0.001 in./in.-min to 0.01 in./in.-min. During re-entry, the temperature excursion is relatively rapid and at this time it is estimated to be in excess of 0.5 in./in.-min. A more rapid strain rate implies higher yield and ultimate capability. It should be noted that this difference in strain rate is significant as will be shown via test, and this additional factor shall not be dismissed from improving confidence in the chosen allowable stress.

1. Estimation of Strain Rate During Fuel Block Re-entry

During re-entry, a typical temperature excursion as a function of time is as shown, where:

$$\begin{aligned} T_1 &= 1985^\circ \text{ F} \\ T_2 &= 3000^\circ \text{ F} \\ t_1 &= 120 \text{ seconds} \\ t_2 &= 160 \text{ seconds} \end{aligned}$$

FIG. A-1. ULTIMATE AND YIELD STRENGTHS OF FULLY ANNEALED Ta-10%W AT ELEVATED TEMPERATURE

Consider, as an example, a helium pressure of ~1300 psi at 3000° F and a liner radius and wall thickness of 0.3822 and 0.0234 inch, respectively,

$$\sigma_1 = 1300 \left(\frac{1985 + 460}{3000 + 460} \right) \left(\frac{0.3822}{0.0234} \right) = 15,000 \text{ psi}$$

$$\sigma_2 = 1300 \left(\frac{0.3822}{0.0234} \right) = 21,200 \text{ psi}$$

Thus, the maximum rate of stress becomes,

$$\dot{\sigma} = \frac{21,200 - 15,000}{160 - 120} (60) = 9300 \text{ psi/min}$$

To estimate the strain rate, $\dot{\epsilon}$, assume a stress-strain curve of the rigid linear strain hardening variety where the difference between the yield and ultimate strengths is about 6000 psi. At 3000° F, the total elongation is very conservatively 40%. Test evidence has indicated elongations greater than 50% at 3000° F. Therefore, the tangent modulus is,

$$E_T = \frac{\Delta\sigma}{\Delta\epsilon} = \frac{6000}{0.40} = 15,000 \text{ psi}$$

With $\dot{\sigma} = 9300 \text{ psi/min}$

$$\dot{\epsilon} = \frac{9300}{15,000} = 0.62 \text{ in./in. -min}$$

This strain rate represents the general magnitude expected for loads exceeding yield.

Below yield, the strain rate is quite small. Between 1900° and 3000° F, the average elastic modulus is $12 \times 10^6 \text{ psi}$.

$$\therefore \dot{\epsilon} = \frac{9300}{12 \times 10^6} = 0.78 \times 10^{-3} \text{ in./in. -min}$$

To obtain higher confidence in the structural capability of Ta-10%W at elevated temperature, a number of tensile coupons have been manufactured and tested from tube stock presently in-house. The tensile specimens have been tested at 2700° and 3000° F in a vacuum by Fansteel at their Baltimore facility. Table A-1 provides the yield strength, ultimate strength and elongations witnessed in these tests. It may be noted that the consistency or repeatability of the data is excellent and that the higher or expected strain rates are included. Ideally, a strain rate of 0.001 in./in. -min to yield and >0.5 in./in. -min from yield to failure should be employed. However, the capability of the testing equipment dictates a limiting strain rate multiple of ten. Thus 0.05 in./in. -min to yield and 0.5 in./in. -min to failure was chosen to be more representative of the actual condition.

B. BURST TEST DATA

Burst testing of capsule specimens at room temperature and considering only the tests associated with the chosen capsule and closure design, all failures resulted in blowing out the end closures. No failures resulted because of an opening in the

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TABLE A-1

Tensile Test Data of Fully Annealed Ta-10%W at Elevated Temperature

(Provided by Fansteel, March 1968)

<u>Test Temperature (°F)</u>	<u>Strain Rate to Yield (in. /in. -min)</u>	<u>Strain Rate-- Yield to Ultimate (in. /in. -min)</u>	<u>Yield Stress (psi)</u>	<u>Ultimate Tensile Strength (psi)</u>	<u>Elongation (%)</u>	<u>Temperature Soak Time (min)</u>	<u>Remarks</u>
2700	0.01	0.01	14,300	16,400	69.0	30	Doubtful results
2700	0.5	0.5	21,500	24,100	55.9	30	
2700	0.01	0.1	14,300 17,900	20,600	56.5	30	
2700	0.05	0.5	17,000 22,700	24,600	58.7	30	
2700	0.05	0.5	17,400 22,500	24,600	59.5	10	
2700	0.05	0.5	16,500 22,100	24,000	53.2	10	
3000	0.01	0.1	10,900 15,100	15,300	68.0	30	
3000	0.05	0.5	12,600 16,500	17,800	57.8	30	
3000	0.05	0.5	12,400 16,400	18,000	65.2	30	
3000	0.05	0.5	12,400 17,500	18,300	64.0	10	
3000	0.05	0.5	12,200 17,600	18,600	40.4	10	

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cylindrical side wall. The remote EB and TIG welds did not provide welds of great effectiveness. Of the six tests conducted, the minimum and average burst pressures were 6000 and 6680 psi, respectively. The nominal mean radius and wall thickness of the specimens were 0.24 and 0.020 inch. Thus, the minimum and average circumferential stresses at failure were,

$$\sigma_{\theta} = p \frac{R}{t} = 6000 \left(\frac{0.24}{0.020} \right) = 72,000 \text{ psi (minimum)}$$

$$\sigma_{\theta} = 6680 \left(\frac{0.24}{0.020} \right) = 80,100 \text{ psi (nominal)}$$

Two tensile tests were conducted on the material used and the average yield and ultimate strengths at room temperature were found to be 83,000 and 96,000 psi, respectively, with little spread. Thus, the failures are associated with stresses below yielding and so the equations applied are adequate. From Ref. A-2, the estimated burst pressure of the cylindrical wall is 11,230 psi for these specimens at room temperature.

To obtain an estimated failure pressure of the end closures at 3000° F, a simple ratio of tensile strengths is applied. From Fig. A-1, the typical ultimate tensile strength of 20,000 psi is taken at 3000° F.

$$\therefore \text{Failure at } 3000^{\circ} \text{ F} \cong 72,000 \left(\frac{20,000}{96,000} \right) = 15,000 \text{ psi}$$

This value may be conservative since the temperature of 3000° F implies much higher elongations than at room temperature. Thus, a failure stress >15,000 psi may be expected.

The alternative way to estimate the capability is rapidly done by comparing the minimum and nominal stresses of 72,000 and 80,100 psi directly with a typical yield stress of ~83,000 psi. It is immediately concluded that failure occurred at or slightly below yielding. Again, this may be better for high temperatures with increased elongation.

C. DESIGN CRITERIA

The stress criteria for the SNAP 29 program (Ref. A-3) states that the effective membrane stress intensity, via the Mises criterion, is limited to the lesser of 2/3 ultimate tensile stress or 90% yield stress of the material at temperature. The criteria also provides a basis for the limiting discontinuity stress at the cylinder end closure interface. However, the determination of the stress distribution at this interface is extremely difficult as a result of geometric complexities and the inefficiency of the weldment cannot be determined analytically. There are also indications of stress risers in the weldment area.

1. Effective Stress for Cylindrical Vessels per Mises Criterion

For a three-dimensional stress state, the Mises criterion states that the effective stress, σ_e , is given by,

$$\sigma_e = \frac{1}{\sqrt{2}} \sqrt{(\sigma_1 - \sigma_2)^2 + (\sigma_2 - \sigma_3)^2 + (\sigma_3 - \sigma_1)^2}$$

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where σ_i are the principal stresses.

For two dimensions where for thin shells the radial stress is negligible,

$$\sigma_e = \sqrt{\sigma_1^2 - \sigma_1 \sigma_2 + \sigma_2^2}$$

where

$$\left. \begin{aligned} \sigma_1 = \sigma_\theta &= \frac{pR}{t} \\ \sigma_2 = \sigma_x &= \frac{pR}{2t} \end{aligned} \right\} \begin{array}{l} \text{per thin wall} \\ \text{cylinders with} \\ \text{internal pressure, } p \end{array}$$

Substituting,

$$\sigma_e = \frac{\sqrt{3}}{2} \frac{pR}{t}$$

The mean radius in terms of the outer liner inside diameter and wall thickness is,

$$\sigma_e = \frac{\sqrt{3}}{4} p \left(\frac{ID_{OL} + t_{OL}}{t_{OL}} \right)$$

Because of this problem, the design criteria proposed shall state that the maximum principal stress in the cylindrical wall shall not exceed 2/3 of the minimum UTS or 90% of the minimum yield strength at temperature. Since 90% of yield corresponds to the failure load associated with the burst test data, it is imperative that the end cap weldment capability be higher. This to some extent may have been accomplished by a recent end closure design change which provided larger corner radii and decreases stress concentrations. Further design changes and more knowledge of the weldment characteristics at elevated temperature may establish this goal. For the very least goal, failure should not occur (with confidence) below the yield stress level. The adequacy of the end closure may be improved through development to the extent that the Mises criterion could potentially be applied as the sidewall design basis.

D. STRUCTURAL EVALUATION

The P-V-T relationship for establishing the helium pressure history is a function of isotope mass, decay rate, etc., and is given by the following equation.

$$p = \frac{12 M_i (1 - e^{-\lambda t}) RT^1}{VM} \quad (A-1)$$

where:

$$M_i = \frac{(PW)}{454C}$$

PW = thermal power per capsule (watts)

C = specific power (watts/gm)

V = void volume (in. ³)
 λ = $\frac{\ln 2}{(HL)}$
 HL = half-life of isotope (days)
 t = time from encapsulation (days)
 R = Universal gas constant = $1544 \frac{\text{ft-lb}}{\text{mole-}^\circ\text{R}}$
 T^1 = $T + 460$ ($^\circ\text{R}$)
 T = temperature ($^\circ\text{F}$)
 M = molecular weight of isotope (lb/mole)
 p = pressure (psi)

For this study,

$(PW) = 1100 \text{ watts}(t)$ (for 4 capsules/module)
 $C = 144 \text{ watts/gm}$
 $HL = 138 \text{ days}$
 $t = 1000 \text{ days}$
 $M = 210 \text{ lb/mole}$
 $T = 3000^\circ \text{ and } 2600^\circ \text{ F re-entry temperatures}$

Substituting into Eq. (A-1),

$$p = \frac{5102.658}{V} \text{ (for } T = 3000^\circ \text{ F)} \quad (\text{A-2a})$$

$$p = \frac{4512.756}{V} \text{ (for } T = 2600^\circ \text{ F)} \quad (\text{A-2b})$$

The available void volume, V , is simply a geometric function and may be readily expressed as a function of outer liner ID. The void volume is,

$$V = \frac{\pi}{4} (ID_{OL})^2 L - V_M - V_{IL} \quad (\text{A-3})$$

Subscript notation:

OL = outer liner (Ta-10%W)
 IL = inner liner (Ta)
 M = fuel matrix

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This equation may be converted to a function of ID_{OL} by considering the nominal gap, g_1 , between inner and outer liners and the nominal thickness of the inner liner, g_2 .

Therefore,

$$V = \frac{\pi L}{4} \left[ID_{OL}^2 - \frac{4V_M}{\pi L} - (ID_{OL} - 2g_1)^2 + (ID_{OL} - 2g_1 - 2g_2)^2 \right] \quad (A-4)$$

where:

ID_{OL} = inside diameter of outer liner (in.)

L = fuel capsule length (in.)

V_M = matrix volume (in.³)

For this study, assuming the present design of four capsules per module,

g_1 = 0.005 inch

g_2 = 0.020 inch

L = 12.83 inches

V_M = 0.873 in.³ (0.698 in.³ for 5 capsules/module and 0.582 for 6 capsules/module)

The final equation of interest is that of stress limitation. The allowable stress is given as,

$$\sigma = \frac{p}{2} \left(\frac{ID_{OL} + t_{OL}}{t_{OL}} \right) \quad (A-5)$$

where t_{OL} = outer liner wall thickness (minimum). At the present time the indication of maximum wall thickness for remote TIG weldment by Mound Laboratory is ~0.027 inch (nominal) = ~0.0243 inch (minimum). This study will not be limited to this upper bound.

The combined solution of Eqs. (A-1), (A-4) and (A-5) is one of implicit functions which most often are solved by trial and error or more accurately by graphical means. Their solution is now obtainable via a program utilizing a digital computer.

It should be noted that the gap, g_1 , between inner and outer liners is included in the void volume capability since at elevated temperatures, the tantalum inner liner will readily creep out to contact the outer liner and this volume becomes available.

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E. BASELINE DESIGN

Thus far two ground rules have been established:

- (1) The wall thickness will be a minimum of 0.0243 inch based on a limiting weldment capability of 0.027 inch nominal or 0.030 inch maximum. The maximum permissible thickness represents the minimum stress state for a finite diameter.
- (2) The internal pressure design proposed is based on 1000 days from encapsulation. This is a conservative approach since an additional probability could be applied for the deboost system.

In addition, a geometry capability of four, five and six capsules per module is possible within a capsule diameter upper bound.

By applying Eqs. (A-1), (A-4) and (A-5), the required inside diameter of the outer liner is found as a function of re-entry temperature, allowable stress level, wall thickness and the number of capsules per module. Typical results are represented by Fig. A-2. The next logical step in the evaluation is to establish an acceptable stress level. From the test data in Table A-1, design ultimate tensile stresses of 21,000 and 15,000 psi are elected for 2700° and 3000° F, respectively. These values seem indicative of potential minimum values. The trend of the data of Fig. A-1 coupled with these stress limits establishes UTS versus temperature between 2400° and 3000° F. Taking 2/3 of UTS, the allowable stress is given by Fig. A-3. Applying stress limits per Fig. A-3, re-entry temperature versus the inside diameter of the liner for four, five and six capsule/module systems can be obtained (Fig. A-4).

As was briefly discussed earlier, the results of an error analysis provide re-entry temperatures in terms of percent of 3σ error. This relationship is given per Fig. A-5.

With Figs. A-4 and A-5, the inside diameter to percent error for each system can be related. This relationship is illustrated by Fig. A-6.

It can be seen that an inside diameter of 0.810 inch for a 6 capsule/module system represents 100% of the 3σ error. Increasing ID above 0.810 inch and obtaining in excess of 100% is of no value. The positive slope of the three curves clearly indicates that the highest liner diameter represents the most optimum flexibility at the highest percent error level. Note that this diameter for four capsules per module is equivalent to ~65% of the error. It is reasonable to assume the error probability can be reduced to this level through fuel block development via aerodynamic and aerothermal testing programs. Thus, the design goal or baseline outer liner ID is 0.810 inch minimum (0.813 in. nominal).

An alternate approach was considered which consisted of minimizing the inner diameter, retaining this as a constant value and increasing only the number of capsules per module in order to accommodate flexibility. This was accomplished by plotting the approximate heat source weight versus percent of original 3σ error with the capsules per module at both constant and variable ID with a constant number of capsules (see Fig. A-7). The approximate weights as a function of ID and number of capsules per module are provided by Fig. A-8 where the weight calculations were based upon the following assumptions:

FIG. A-2. ALLOWABLE STRESS VERSUS INSIDE DIAMETER OF OUTER LINER AND WALL THICKNESS

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FIG. A-3. FULLY ANNEALED Ta-10%W ALLOWABLE STRESS
AT ELEVATED TEMPERATURE

FIG. A-4. RE-ENTRY TEMPERATURE VERSUS INSIDE DIAMETER OUTER LINER AND CAPSULES PER MODULE

FIG. A-5. RE-ENTRY TEMPERATURE VERSUS PERCENT OF $3\bar{\sigma}$ ERROR

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FIG. A-6. INSIDE DIAMETER OUTER LINER VERSUS PERCENT OF 3σ ERROR FOR 4, 5 AND 6 CAPSULES PER MODULE

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FIG. A-7. CAPSULE SIZE EFFECTS ON HEAT SOURCE WEIGHT FOUR-MODULE SYSTEM

FIG. A-8. APPROXIMATE HEAT SOURCE WEIGHT VERSUS OUTER LINER ID

- (1) The fuel block dimensions are 31.67 x 19.85 inches by a thickness of block which gives a minimum wall thickness over the capsule holes of 0.215 inch for all cases.
- (2) The edges of the fuel block were not rounded.
- (3) All corners of the fuel block have a 1.3 x 45 degree chamfer.
- (4) Density of graphite = 0.0625 lb/in.³.
- (5) The block is covered completely with a clad whose thickness is 0.012 inch.
- (6) The four-capsule/module block has 1/2-inch lightening holes between all capsule holes except for the larger diameter capsules. In the latter case, 1/4-inch diameter lightening holes are included. In addition, no lightening holes are included in any of the five- or six-capsule/module designs.
- (7) Weights do not include the weight of an emissivity coating.

F. CLADDING STRUCTURAL CAPABILITY

The cladding, external and concentric to the outer liner, is made from fully annealed platinum-20% rhodium. The required ID is 0.885 inch, minimum. The nominal wall thickness and flat end cap or closure thickness are 0.020 and 0.026 inch, respectively.

It is assumed that the internal pressure within the clad is derived from one atmosphere at encapsulation and a potential temperature excursion during re-entry to ~3000° F. This would represent the maximum possible loading. At constant volume, the pressure corresponding to 3000° F would be,

$$P_f = 14.7 \frac{(3000 + 460)}{(200 + 460)} = 77 \text{ psi}$$

The 200° F applied is the approximate temperature of the cladding during fueling with the application of a chill block.

The maximum circumferential or hoop membrane stress for a minimum wall of 0.018 inch becomes,

$$\sigma_{\theta} = \frac{pR}{t} = \frac{77 \left(\frac{0.885 + 0.018}{2} \right)}{0.018}$$

The Mises stress intensity is only 1670 psi. Figure A-9 provides available ultimate tensile strength of Pt-20% Rh (fully annealed) as a function of temperature. Extrapolating slightly, the UTS at 3000° F appears to be ~4000 psi. Two-thirds of this value is 2670 psi >1930 psi. To date, the yield at this temperature has not been located in the literature. At room temperature, the yield strength is in the vicinity of 11,000 psi and elongation is 37%. This implies a material of high elongation and a steep slope of strain hardening. This should be even more prevalent at high temperature. Thus, should the wall yield slightly, the pressure will drop off since the initial gas volume is extremely small and catastrophic failure is indeed remote.

FIG. A-9. UTS AT ELEVATED TEMPERATURES FOR FULLY ANNEALED Pt-20% Rh

Figure A-10 provides the discontinuity stresses as a function of end cap thickness on the basis of an applied unit internal pressure. With a end cap thickness of 0.026 inch, some of the discontinuity stresses at the cylinder-end cap interface were very high initially. Although for a highly ductile material these stresses decrease considerably by plastic flow, a thicker end cap is recommended. This may be of even greater importance to lessen the bending stresses at the center of the end cap.

If thicker end caps are to be applied, the capability to withstand load is dependent upon a full penetration weld for the full extent of cap thickness. If this cannot be demonstrated, the 0.026-inch cap would probably provide greater capability. In either case, burst tests are recommended.

REFERENCES

- A-1. Ruwe, D., "Fuel Block and Capsule Design Considerations." Martin IDC 3314-67, February 16, 1968.
- A-2. Schumann, F., "Structural Evaluation of Welded Fuel Capsule Liners--SNAP 29." Martin IDC 1140-273, February 13, 1968.
- A-3. Schumann, F., "Stress Criteria for the SNAP 29 RTG System." Martin IDC 1000-234, February 14, 1968.

- σ_{Zi} = longitudinal discontinuity stress including membrane--inside surface
 σ_{Zo} = longitudinal discontinuity stress including membrane--outer surface
 $\sigma_{\theta i}$ = hoop discontinuity stress including membrane--inner surface
 $\sigma_{\theta o}$ = hoop discontinuity stress including membrane--outer surface
 σ_{ec} = maximum bending stress in end cap
 $\sigma_{\theta m}$ = maximum hoop membrane stress

FIG. A-10. DISCONTINUITY STRESSES IN CYLINDER WALL AND MAXIMUM BENDING STRESS IN END CAP OF CLAD VERSUS CAP THICKNESS

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APPENDIX B

POST-TEST EXAMINATION OF DROP TEST CAPSULES

A. FABRICATION

Thirty simulated capsules (including matrix, inner-liner liner and clad) were fabricated as defined in Table B-1. Leaks were detected in five of the inner-liner assemblies. Three of the five were outgassed and helium backfilled and resealed prior to capsule sintering.

During the fabrication of two inner liners with dummy matrices, S/N 3 and 4A, leaks greater than 10^{-5} cm³/sec were discovered in the welded end cap areas. These two inner liners were inadvertently stored in an air atmosphere at room temperature for two or three days. The two inner liners were then seal welded in an inert atmosphere into their outer liners which were leak checked and found to contain no detectable leaks. When these circumstances were reviewed, it was questionable as to whether the two inner liners and matrices could be used, since this additional amount of oxygen and nitrogen within the inner liners would combine with the matrices during a later sintering process.

A series of calculation was made to determine the maximum additional O₂ and N₂ which might be present in the inner liners due to the exposure to air.

Assumption

Assume the capsule leak was large enough and the exposure to air long enough that the complete void volume within the inner liner was filled with air (conservative).

Capsule length = 14 inches

ID of fuel matrix = 0.367 inch

$$\begin{aligned}\text{Capsule void volume} &= \frac{\pi}{4} (0.367)^2 \times 14 = 1.48 \text{ in.}^3 \\ &= 24.25 \text{ cm}^3\end{aligned}$$

Matrix dimensions

OD = 0.517 inch

ID = 0.367 inch

$$\text{Matrix volume} = \frac{\pi}{4} (0.517^2 - 0.367^2) 14 = 1.46 \text{ in.}^3$$

since the matrix is $\approx 60\%$ of theoretical density

$$\begin{aligned}\text{Fuel matrix void volume} &= 0.40 (1.46) = 0.585 \text{ in.}^3 \\ &= 9.6 \text{ cm}^3\end{aligned}$$

$$\text{Total void volume} = 24.25 + 9.6 = 33.85 \text{ cm}^3$$

TABLE B-1
Fuel Capsule Fabrication

Heat Source		Inner Liner			Matrix			Liner		Clad			Sinter Atmosphere 1000° C
Test	S/N	S/N	Leak Test	Remarks	Lot/N	Size	Composition	S/N	Leak Test	S/N	Leak Test	Remarks	
Drop	4	1	NDL	--	3	S	Ta	1	NDL	2	NDL	--	Argon
		2	NDL	--	3	L	Ta	2	NDL	7	NDL	Center hole repaired (2), Pt-6% Rh	Argon
		4A	LL	OK per 1140-243	3	L	Ta	4	NDL	4	NDL	Center hole repaired (1), Pt-6% Rh	Argon
		5	NDL	--	3	S	Ta	5	NDL	1	NDL	--	Argon
		6	NDL	--	2	L	Ta+GdTe	6	NDL	9	NDL	--	Argon
		7	NDL	--	2	L	Ta+GdTe	7	NDL	5	NDL	End cap weld repaired (2)	Argon
		8	NDL	--	2	L	Ta+GdTe	8	NDL	3	NDL	Center hole repaired (1)	Argon
		9	NDL	--	2	L	Ta+GdTe	9	NDL	6	NDL	Center hole repaired (7)	Argon
Drop	3	3	LL	OK per 1140-243	3	L	Ta	3	NDL	8	NDL	--	Air
		11	NDL	--	3	L	Ta	11	NDL	11	NDL	--	Air
		13	NDL	--	3	L	Ta	13	NDL	13	NDL	End cap repaired (4), Pt	Air
		14	NDL	--	4	S	Ta	14	NDL	22	NDL	--	Air
		15	NDL	--	3	L	Ta	15	NDL	14	NDL	End cap repaired (3), Pt	Air
		17	NDL	--	3	L	Ta	17	NDL	17	NDL	End cap repaired (5), Pt	Air
		20	LL	Outgassed, He backfill	3	L	Ta	20	NDL	28	NDL	--	Air
		21	LL	Outgassed, He backfill	4	S	Ta	21	NDL	21	NDL	--	Air
		23	NDL	--	4	S	Ta	23	NDL	23	NDL	--	Air
		24	NDL	--	4	S	Ta	24	NDL	24	NDL	--	Air
		25	NDL	--	4	S	Ta	25	NDL	20	NDL	EB end cap weld repaired (3), Pt-6% Rh; center hole (1)	Air
		26	NDL	--	4	S	Ta	26	NDL	26	NDL	--	Air
Hot Vibration		10	NDL	--	2	L	Ta+GdTe	10	NDL	10	NDL	Center hole repaired (5), Pt	Argon
		12	NDL	--	3	L	Ta	12	NDL	12	NDL	End cap weld repaired (3), Pt	Not completed
		16	NDL	--	1	L	Ta+GdTe	16	NDL	30	NDL	--	Not completed
		18	NDL	--	3	L	Ta	18	NDL	18	NDL	End cap weld repaired (3), Pt	Not completed
		19	LL	Outgassed, He backfill	3	L	Ta	19	NDL	19	NDL	End cap weld repaired (3), Pt	Not completed
		22	NDL	--	4	S	Ta	22	NDL	16	NDL	EB end cap repaired (3), Pt-6% Rh	Not completed
		27	NDL	--	4	S	Ta	27	NDL	27	NDL	--	Not completed
		28	NDL	--	1	L	Ta+GdTe	28	NDL	25	NDL	--	Not completed
		29	NDL	--	1	L	Ta+GdTe	29	NDL	29	NDL	--	Not completed
		30	NDL	--	1	L	Ta+GdTe	30	NDL	15	NDL	EB end cap repaired (9), (3) Pt-6% Rh	Not completed

1. NDL represents no detectable leak, 10^{-7} to 10^{-9}
LL represents large leaks, greater to 10^{-5}
2. Matrix sizes
S--small, 0.512 inch OD and
L--large or 0.518 inch OD
3. Leak location for the clad welds are identified
along with the number of rewelds required.
Filler material used was Pt-6% Rh or pure Pt.

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B-2

One mole of air (O_2 and N_2) at standard temperature pressure (STP) weighs ≈ 29 grams.

Also 1 mole of air at STP occupies a volume of 22.4 liters.

Also 1 liter = 1000 ml = 1000 cm^3 .

Therefore, the weight of air present within the inner liner equals

$$\frac{33.85}{22,400} = \frac{X}{29}$$

$$x = \frac{29 (33.85)}{22,400} = 0.044 \text{ gram of air}$$

Thus, since air by weight is $\approx 23\% O_2$, there are $(0.23) (0.044) = 0.0101$ gram of O_2 in the inner liner.

Assumption: Simulated fuel matrix is 100% tantalum.

From this, matrix volume = $1.46 \text{ in.}^3 = 24.0 \text{ cm}^3 \text{ Ta}$.

Since the matrix is 60% dense

$$0.60 (24.0) = 14.4 \text{ cm}^3 \text{ of Ta.}$$

The density of Ta = 16.6 gm/cm^3 .

Therefore, $14.4 (16.6) = 239$ grams of Ta in the inner liner.

Thus $0.010 \text{ gm } O_2 / 239 \text{ gm Ta} = 41.8 \times 10^{-6}$ parts O_2 / parts of Ta or ≈ 42 ppm of O_2 in the Ta if all of the O_2 present is combined with the Ta.

Thus, since the allowable O_2 content of the tantalum powder in the fuel matrices would be 1000 ppm, the additional 42 ppm was considered negligible.

For nitrogen: $75/23 (42) \approx 137$ ppm of N_2 .

The allowable N_2 content of the tantalum powder in the fuel matrix was 100 ppm.

The addition of 137 ppm more than doubled this value for nitrogen and the effect of the additional N_2 on the actual fuel form could not be determined. (The actual fuel form will not be exposed to air as these test capsules were.) However, for test purposes, it was believed that the additional N_2 content would not have an adverse effect on the test results, and the two capsules were used as planned.

In addition, a large number of the clad welds required repair before a no detectable leak (NDL) condition was obtained. These leaks appeared distributed between the TIG weld end cap, the EB end cap weld and the backfill center hole. In some cases pure Pt or Pt-6% Rh filler was required to obtain a final seal.

B. POST-TEST EVALUATION

Although a general inspection procedure was set up, actual implementation of the destructive portion of the inspection plan deviated from the original for several reasons. Difficulty was encountered in removing the clad/liner/inner liner tubing from the straight and especially from the distorted capsules.

The capsule serial numbers and their relative locations within the two drop test blocks are shown in Figs. B-1 and B-2. Capsules which showed post-test leaks in the nondestructive examination (S/N 6 only) and a select number of deformed capsules (S/N 14, 20 and 4) were destructively examined.

1. Nondestructive examination

A reconstruction of the drop test blocks and their capsules showed that each of the drop test blocks impacted on or near one of its corners. This caused the capsules nearest to the impact point to bend (maximum of ~90 degrees) while those further removed from the impact quadrant of the block had a lesser amount of or no distortion. The visual (aided by a 30X microscope) examination of the capsules revealed the presence of cracks in the clad of only one capsule, S/N 6. These cracks, which were further verified during leak testing, were in the vent hole weld of the TIG end cap. The cracking present in this area of this particular capsule does not come as a surprise when one considers the fabrication history of this capsule. Quality control records show that this particular vent hole had been repaired seven times before a leak proof capsule was obtained. In connection with the welding of the cladding end caps (which includes the sealing of the small vent holes in the TIG end cap), it should be mentioned that a number of irregularities existed which, under ordinary circumstances, would not have been present.

- (1) The cladding weld parameters had not been developed prior to the capsule fabrication program, and therefore a minimum of parameter weld development was performed in advance of the actual capsule cladding welding.
- (2) A center burst had previously been discovered in the bar stock material from which the cladding end caps were machined. Although it was believed that defect-free material had been used for making the caps, the material still remains suspect.
- (3) It was discovered that the axial clearances between the liner and clad tubes were not adequate in all cases. This lack of sufficient axial clearance caused, in some cases, a lifting or unseating of an end cap during the actual welding of the cladding caps. This phenomenon resulted in some poorly welded end caps which, in turn, required repair welding.

Since the fabrication of these capsules, a number of steps have been taken to prevent any recurrence of similar events in the future fabrication of capsules. Axial clearances have been made sufficient, the necessity of having a vent hole (which proved quite difficult to weld) in one of the cladding end caps has been eliminated, future material will be procured to rigid specifications which will eliminate the use of any questionable material, and the previously scheduled capsule clad weld parameter development will have taken place and produced sound welding parameters.

In general, the visual inspection showed that the capsules in the half of the fuel block opposite the impact side had no visible external damage. The remainder of the capsules located in the impact side of the block showed various amounts of scratches, bends, dents, etc.

Numbers are capsule serial numbers.

FIG. B-1. DROP TEST BLOCK FOR S/N-3

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FIG. B-2. DROP TEST BLOCK FOR S/N-4

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Radiographs of the capsules showed, in general, that a capsule with no visible external distortion also had no broken fuel matrices. In cases of capsule distortion, the radiographs generally showed broken fuel matrices and, in places, voids where fuel matrices had been before breaking into pieces and moving to other locations within the capsule. Axial clearances and, in some cases, lack of them can also be seen in the pictures. Sufficient axial clearances are generally present between the inner liner and liner while what appears to be insufficient clearances are generally present between the liner and the clad.

Dye penetrant and leak checking showed no cracks or leaks in the cladding of any of the capsules except for the cracks in the vent hole of the TIG clad end cap (capsule S/N 6) previously mentioned.

The dimensional check of the capsules showed essentially no change in the capsule diameters of the nondistorted capsules; however, the camber of these capsules became more pronounced.

2. Destructive Examination

a. Capsule S/N 6

The cracks previously detected in the TIG end cap vent hole weld were reverified when no helium was found present inside the clad. A helium leak check of the vent hole weld showed a definite leak and pinpointed its location to be in the vent hole weld area only. All other leak and dye penetrant checks on the clad, liner and inner liner disclosed no cracks, leaks or other irregularities.

Intergranular attack (Fig. B-3) was observed on the inner and outer surfaces of the tantalum inner liner. The inner liners of all of the other sectioned capsules in this inspection program showed this same type of intergranular attack. A piece of as-received tantalum tubing was sectioned, polished and mounted and is shown in Fig. B-4. This as-received piece of tubing also shows a small amount of intergranular attack. A possible explanation for the condition of the surfaces of the as-received material is that the drawing and heat treating operations to which the tubing was subjected caused the initiation of a slight amount of intergranular attack (thought to be characteristic of tantalum). It is then proposed that this initial attack of the surfaces was furthered by a telluride (which was present in all of the capsules) reaction during the sintering of the capsules at 1000° C for four hours. Since no real efforts were made at any time during the fabrication of the capsules to clean the outer surfaces of the tantalum tubing, it is reasonable to assume that small amounts of telluride were present on the outer surfaces of the tubing, explaining the attack seen on the outer surfaces.

No irregularities were observed in the Ta-10%W material or the Pt-20% Rh material (other than the previously mentioned cracks in the vent hole of the Pt-20% Rh end cap).

A section through one inner liner and cap weld shows that even though the weld bead external surface (Fig. B-5) appears normal, this does not indicate that a 100% weld penetration was achieved. In fact, this particular weld penetration was only 0.014 inch deep out of a possible 0.027 inch (see Fig. B-6). This unwelded joint area caused by incomplete weld penetration is not desirable from a stress or crack propagation point of view.

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FIG. B-3. TANTALUM INNER LINER FROM
S/N 6 SHOWING SURFACE ATTACK

FIG. B-4. TANTALUM INNER LINER
AS-RECEIVED TUBING

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FIG. B-5. TANTALUM END CAP WELD FROM S/N 6

FIG. B-6. TANTALUM INNER LINER END CAP WELD AREA FROM S/N 6

FIG. B-7. TRANSVERSE SECTION SHOWING ATTACK ON INNER LINER AND UNAFFECTED LINER OF S/N 20

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b. Capsule S/N 20

All of the leak and dye penetrant checks performed on the clad, liner and inner liner disclosed no cracks, leaks or other irregularities.

The same type of intergranular attack (Fig. B-7) of the inner liner inner and outer surfaces was observed on this capsule as was discussed under capsule S/N 6.

No irregularities were observed in the Ta-10%W or the Pt-20% Rh material.

c. Capsule S/N 4

All of the leak and/or dye penetrant checks performed on the clad, liner and inner liner disclosed no cracks, leaks or other irregularities. However, as a short piece of the liner was being removed from the TIG end (end opposite the eared cap) of the capsule, an inner liner end cap and a tantalum fuel shim fell away from the capsule. A comparison of the weld bead sizes of the inner liner cap which fell (Fig. B-8) and the inner liner cap on the opposite end (Fig. B-9) of the same inner liner shows that the cap which fell off did have a smaller weld bead. (Note: The cap which fell off was replaced on the end of its mating tubing, and it was determined that all of the weld bead was on the cap and none remained on the tubing.) However, the statement that "a smaller weld bead definitely indicates a smaller amount of weld penetration" is not necessarily true. Figure B-10 shows the weld penetration that was present in the end cap which did not come off. A weld penetration of ~90% was obtained. In an attempt to explain why the end cap of the inner liner was unattached, two possibilities can be presented. One is that the smaller weld bead did result in a smaller weld penetration, and the impact on the capsule was sufficient to break the cap loose (This capsule was one of the most severely distorted capsules. See Fig. B-2.) The second possibility could be that the large grain size (the weld is only one grain deep at the intersection of the opposite end cap and wall in Fig. B-10) is indicative of a very brittle weld which broke under impact.

The same type of intergranular attack of the inner liner inner and outer surfaces was observed on this capsule as was discussed under capsule S/N 6. In addition to this attack, the transverse sections shown in Figs. B-11, B-12 and B-13 show a tearing of the inner liner outer surface apparently caused by the severe bend radius in an area of maximum tube deformation. The tears do not appear to be deeper than ~10% of the wall thickness and are observed only in the sharp bend areas.

No irregularities were observed in the Ta-10%W or the Pt-20% Rh materials.

d. Capsule S/N 14

All of the leak and/or dye penetrant checks performed on the clad, liner and inner liner disclosed no cracks, leaks or other irregularities.

The same type of intergranular attack of the inner liner inner and outer surfaces was observed on this capsule as was discussed under capsule S/N 6. In addition to this attack, Figs. B-14 and B-15 show a tearing of both the inner liner and liner outer surfaces apparently caused by the severe bend radius in an area of maximum deformation. The tears do not appear to be deeper than ~10% of the inner liner wall thickness nor deeper than ~5% of the liner wall thickness and are observed only in the sharp bend areas. No other irregularities were observed in the liner material. The Pt-20% Rh material showed no irregularities.

FIG. B-8. S/N 4 INNER LINER END CAP WELD

FIG. B-9. S/N 4 INNER LINER END CAP WELD

FIG. B-10. LONGITUDINAL SECTION THROUGH INNER LINER CAP WITH
"4A" MARKED ON IT

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FIG. B-11. TRANSVERSE SECTION OF S/N 4 THROUGH AREA OF MAXIMUM DEFORMATION

FIG. B-12. TRANSVERSE SECTION OF S/1 THROUGH AREA OF MAXIMUM DEFORMATION

FIG. B-13. TRANSVERSE SECTION SHOWING ATTACK ON INNER LINER. THIS IS THE AREA OF MAXIMUM DEFORMATION

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B-12

FIG. B-14. TRANSVERSE SECTION OF S/N 14
THROUGH AREA OF MAXIMUM
BEND

FIG. B-15. LINER AND INNER LINER OF
S/N 4 AT MAXIMUM BEND
AREA

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B-13

When a short piece of the clad on the TIG end of the capsule was removed from the capsule, the end cap of the liner came off with it. This liner end cap was wedged tightly up against the underside of the cladding end cap and was not loose as was the inner liner cap of capsule S/N 4. Since the liner end cap was wedged up inside the clad, it is not clear whether the liner cap separated from its tubing due to the drop test impact forces or whether it was broken off in the attempt to remove the short piece of clad tubing. It was not unusual to have to use pliers to grip and remove short pieces of either liner or clad tubing. Pliers were used to remove this short piece of clad tubing. In any case, it is apparent that the weld was not adequate. In sectioning this clad/stuck liner cap composite, the clad separated from the liner end cap and therefore both were mounted separately. Figures B-16 and B-17 show a section through the cladding. Note the cracks in the clad vent hole and peripheral weld. Figure B-16 shows an area in the side wall just beneath the end cap that could have been the place where the liner cap was wedged in. Pre-test radiographs do, in fact, show what appears to be a very tight fit between liner cap and clad. If the clearances were such that the liner end cap was right up against the underside of the clad cap as the clad cap was being welded, the heat from welding could have joined the two caps together in certain areas and could also have remelted the original liner end cap weld in some areas. Looking at Figs. B-18 and B-19, which are views of the upper and lower faces of the stuck liner end cap, a burnt or remelted weld area can be seen. This had to be remelted since it would not have passed inspection if it had been the original weld. This substantiates the possibility that the liner end cap weld could have been reheated to very high temperatures all around its periphery and, in fact, was actually remelted in one area. Reheating a heat-affected weld area could cause the weld to become relatively more brittle. In fact, Figs. B-20 and B-21, which are cross sections of the cap shown in Figs. B-18 and B-19, show large grain size in the weld area. They also show that the cap broke in the area of the weld. Although this discussion is only a theory as to what happened, the photographs do show that something unusual did occur to the liner end cap during the welding of the capsule.

Figure B-22 shows a section taken through the inner liner end cap weld on the TIG end of the capsule. Large grain sizes are apparent in the heat affected area and appear to be typical of the inner liner weld.

C. SUMMARY AND RECOMMENDATIONS

The visual and radiographic examination of all of the capsules (some of which were severely distorted) did not show any through cracks or splits in any of the three capsule materials. The destructive examination of four of the 20 drop test capsules showed that the capsules (except for the vent hole weld of capsule S/N 6) did not develop any through cracks or splits in any of the three capsule materials. In general, if a capsule was dimensionally undistorted (as was the case if it was in the half of the block opposite to the impact area), its simulated fuel matrices remained unbroken. On the other hand, if the capsule was bent its fuel matrices were generally broken and small pieces randomly dispersed throughout the inner liner.

Additional observations made during the examination were as follows:

- (1) Intergranular attack was always present on both the inner and outer surfaces of the tantalum inner liner material. A piece of as-received tantalum tubing was examined and showed the same intergranular attack but in lesser amounts.

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FIG. B-16. SECTION OF S/N 14 THROUGH
Pt-20%Rh CLAD AT TIG WELD END

FIG. B-17. Pt-20%Rh WELD ON TIG WELD END OF S/N 14

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FIG. B-18. TOP VIEW OF LINER END CAP WELD FOR S/N 14

FIG. B-19. BOTTOM VIEW OF LINER END CAP WELD FOR S/N 14

FIG. B-20. SECTION THROUGH Ta-10%W LINER OF S/N 14

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FIG. B-21. Ta-10%W LINER END CAP WELD OF S/N 14

FIG. B-22. INNER LINER WELD OF S/N 14

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B-17

- (2) In the maximum bend areas of the severely distorted capsules, a small amount of tearing was noticed on the outer surfaces of the liner and inner tubing. This tearing did not penetrate the wall thickness by more than 10%.
- (3) Large grain sizes were observed in the weld heat affected zone of both the tantalum and tantalum-10% tungsten material.
- (4) Little or no axial clearance was observed between the liner and clad capsules.
- (5) Incomplete TIG weld penetration of the end cap/tubing wall joints was observed.

As a result of these observations, the following steps are recommended in order to evaluate potential problem areas:

- (1) Investigate the cause of the intergranular attack on the as-received tantalum tubing. Determine if this attack is typical for tantalum and if so, what effect, if any, the fuel may have in accelerating this attack at elevated temperatures.
- (2) Investigate the tearing phenomenon observed in the outer surfaces of the tantalum and tantalum-10% tungsten tubing. Determine what effect elevated temperatures may have on this tearing.
- (3) Determine if the extremely large grain sizes observed in the welded areas are typical for tantalum and tantalum-10% tungsten. In addition, determine if the large grains are indicative of relatively brittle welds.
- (4) Investigate the effects of reheating a welded area from the standpoint of brittleness, etc. This reheating could occur during the welding of adjacent end caps.
- (5) Allow sufficient axial clearances between the inner liner, liner and clad tubes to accommodate tolerances and weld shrinkage.
- (6) Ensure that full penetration welds are being obtained from the weld parameters presently being developed. Full penetration should be defined to include melting of the entire original joint.

D. TEST DATA SUMMATION

The following contains the specific details regarding the tests conducted.

1. Nondestructive Examination (all drop test capsules)

The following outlines the steps taken in the nondestructive examination. The weight of the capsules before and after testing were recorded.

<u>Capsule No.</u>	<u>Before Testing</u>	<u>After Testing</u>
1	726.3	722.7
2	722.4	720.3
3	721.5	718.8
4	718.8	719.0
5	728.3	726.4
6	718.9	725.4
7	720.2	728.4
8	718.7	721.6
9	725.2	719.0
11	721.57	722.2
13	721.87	723.55
14	721.1	720.9
17	721.62	722.5
20	710.2	709.3
21	713.8	713.8
22	715.9	715.8
23	725.2	725.5
24	717.5	717.6
26	719.4	719.4
28	722.5	722.4

After the capsules were weighed the fuel block was reconstructed and the fuel capsules placed in their proper orientation and photographed. Figures B-23 through B-26 show the various views. The capsules numbers are actually block position numbers in these figures.

Close-up photographs of all of the damaged fuel capsules were taken. These are shown in Figs. B-27 through B-40. Capsule numbers are actually block position numbers in these figures.

Following this, the fuel capsules were cleaned and a visual and microscopic examination was performed (30X). All details that were observed, were recorded according to serial number. Observations made were as follows:

S/N

- 1 Heavy scratches on the TIG end of the tube. Scratches and small dents along the length of the tube.
- 2 Tube bent and twisted to 90-degree angle. Tube kinked three places on the inside radius and one tab on the EB end cap bent.
- 3 Minor scratches on the tube.

FIG. B-23. S/N 4 RECONSTRUCTED BLOCK

FIG. B-24. DAMAGE THAT OCCURRED ON
IMPACT OF S/N 4

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FIG. B-25. RECONSTRUCTED BLOCK OF S/N 3

FIG. B-26. DAMAGE THAT OCCURRED
DURING IMPACT OF S/N 3

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FIG. B-27. S/N 3, CAPSULE 1

FIG. B-28. S/N 3, CAPSULE 1

FIG. B-29. S/N 3, CAPSULE 2

FIG. B-30. S/N 3, CAPSULE 2

FIG. B-31. S/N 3, CAPSULE 13

FIG. B-32. S/N 3, CAPSULE 13

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FIG. B-33. S/N 4, CAPSULE 14

FIG. B-34. S/N 3, CAPSULE 14

FIG. B-35. S/N 3, CAPSULE 17

FIG. B-36. S/N 3, CAPSULE 17

FIG. B-37. S/N 4, CAPSULE 13

FIG. B-38. S/N 4, CAPSULE 13

FIG. B-39. S/N 4, CAPSULE 14

FIG. B-40. S/N 4, CAPSULE 14

FIG. B-41. CRACKS ON TIG END CAP OF S/N 6

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S/N

- 4 Tube bent and twisted 90 degrees with five kinks on the inside of the bend.
Tab on the EB end cap was twisted.
- 5 No visible damage.
- 6 Cracks on the center of the TIG end cap welds and parent metal.
Small flat spot on the side of the tube in the weld area.
EB end cap tab is bent.
- 7 No visible signs of damage.
- 8 Tabs on EB end cap bent--one toward the tube and the other away from the tube.
The capsule bent and twisted approximately 45 degrees. The tube kinked one inch from the TIG end of the tube and had a large dent on the opposite side.
There was a rough edge on the EB end cap and heavy scratches near the center of the tube.
- 9 No evidence of damage.
- 11 One tab on the EB end cap twisted and bent down toward the tube and there were deep marks on the edge of the cap near the bent tab.
Tube bent and twisted approximately 45 degrees with the flat area approximately two inches long.
- 13 Both tabs on the EB end cap are bent up and away from the tube.
The capsule is bent and twisted approximately 20 degrees about four inches from the TIG end cap.
Large dents were found in the side of the tube three inches from EB end cap and there were deep abrasions in the weld area of the TIG end cap.
- 14 One tab on the EB end cap bent and twisted down against the tube.
The capsule was bent and twisted 90 degrees. The tube is flattened over an 11-inch length starting approximately three inches from the TIG welded end.
Deep scratches were found in the tube along a four-inch area starting in the TIG weld.
- 17 The TIG end cap was concave while the weld in the center of the cap shows signs of impact.
- 20 This capsule is bent approximately 30 degrees starting approximately five inches away from the EB weld end of the tube.
One tab on the EB end cap bent down toward the tube and there were deep scratches in the TIG end cap weld.
- 21 There was no visible damage on S/N 21, 22, 23, 24, 26 and 28.

Following the visual comparison all fuel capsules were sniffed for helium leaks and all welded ends of the capsules were leak checked. No detectable leaks were found on any capsules except S/N 6. It had a large leak in the center of the TIG end cap at the vent closure weld. Cracks were also visible as can be seen on Fig. B-41.

The next step was to radiograph the fuel capsules to show clad/liner/inner liner clearances and the condition of the fuel matrices. Some typical radiographs showing matrices and end clearances can be seen in Figs. B-42, B-43, B-44 and B-45.

The capsules were placed in an evacuated chamber over night and then backfilled with helium for four hours. Following this the welded ends of the capsules were leak checked. No detectable leaks were found except for one in S/N 6 on the TIG end in the area of the vent hole closure.

After this a gross leak check was performed on capsules not showing any leaks. No detectable leaks were found except for the one already located on S/N 6.

The next step was to dye penetrant check the clad weld areas of the capsules. There were surface imperfections indicated in the TIG peripheral weld on capsules S/N 1, 3, 4, 5, 6, 7 and 9. Surface imperfections were indicated in the EB welded area on capsules S/N 17, 20, 21, 28, 1 and 9. These imperfections did not penetrate through the weld to cause leaking.

All the nondamaged capsules were dimensionally inspected to drawing 466A1141017 and all deviations were recorded from as-built dimensions as well as straightness and bow. Table B-2 gives the results of this inspection.

Essentially, the diameters of the capsules remained unchanged, but in a good many instances the straightness of the capsules changed considerably.

TABLE B-2
Dimensional Inspection

S/N	Diameter (in.)		EB	Diameter After (in.)	
	Before	After		Center	TIG
1	0.654/0.663	0.6487/0.663	0.662/0.663	0.654	0.6487/0.656
2	0.665/0.667	0.6175/0.7075	0.668/0.668	0.6175/0.7075	0.655/0.659
3	0.654/0.668	0.653/0.668	0.667/0.668	0.653/0.655	0.655/0.656
4	0.654/0.664	0.486/0.818	0.664/0.666	0.486/0.818	0.653/0.657
5	0.654/0.668	0.653/0.668	0.667/0.668	0.653/0.655	0.654/0.655
6	0.654/0.666	0.6535/0.6655	0.6635/0.6655	0.654	0.6535/0.6548
7	0.654/0.665	0.654/0.665	0.665	0.654/0.654	0.654/0.656
8	0.654/0.663	0.609/0.665	0.663/0.665	0.650/0.660	0.609/0.660
9	0.654/0.662	0.654/0.664	0.6625/0.664	0.654/0.655	0.654/0.655
11	Not	0.652/0.718	0.663/0.665	0.667/0.718	0.652/0.654
13	recorded.	0.652/0.667	0.663/0.667	0.652/0.655	0.654/0.657
14	↑ ↓	0.555/0.746	0.665/0.661	0.555/0.746	0.652/0.654
17		0.654/0.666	0.663/0.666	0.654/0.655	0.654/0.657
20		0.615/0.700	0.660/0.665	0.615/0.700	0.652/0.655
21		0.652/0.662	0.661/0.662	0.653/0.655	0.652/0.655
22		0.653/0.664	0.663/0.664	0.653/0.656	0.654/0.658
23		0.654/0.663	0.661/0.663	0.654/0.655	0.654/0.656
24		0.653/0.660	0.659/0.660	0.653/0.656	0.654/0.655
26		0.653/0.660	0.659/0.660	0.654/0.655	0.653/0.655
28		0.653/0.663	0.662/0.663	0.653/0.655	0.654/0.655

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FIG. B-42. RADIOGRAPH SHOWING MATRICES AND END CLEARANCES

FIG. B-43. RADIOGRAPH SHOWING MATRICES AND END CLEARANCES

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FIG. B-44. RADIOGRAPHS TAKEN TO SHOW CLEARANCES AND CONDITION OF FUEL MATRICES

FIG. B-45. RADIOGRAPHS TAKEN TO SHOW CLEARANCES AND CONDITION OF FUEL MATRICES

TABLE B-2 (continued)

S/N	EB End		Center		TIG End	
	Before	After	Before	After	Before	After
1	0.003	0.008	0.003	0.008	0.002	0.014
3	0.002	0.018	0.000	0.016	0.003	0.022
5	0.008	0.025	0.003	0.025	0.001	0.024
6	0.004	0.003	0.003	0.007	0.000	0.014
7	0.009	0.005	0.005	0.004	0.005	0.003
9	0.007	0.005	0.003	0.009	0.003	0.012
17	Not	0.002	Not	0.002	Not	0.001
21	recorded	0.003	recorded	0.003	recorded	0.003
22	↑	0.006	↑	0.010	↑	0.020
23	↓	0.003	↓	0.007	↓	0.013
24	↓	0.012	↓	0.010	↓	0.012
26	Not	0.001	Not	0.0015	Not	0.005
28	recorded	0.006	recorded	0.007	recorded	0.002

Seven capsules have been deformed due to testing.

2. Destructive Examination

Following the nondestructive examination of all of the fuel capsules, a select number of damaged capsules were chosen for further destructive examination. The examination performed on each of these capsules is presented. To help clarify some of the steps performed during the examination, it is recommended that the reader refer to the photograph shown in the figure listed beside each of the capsules examined.

a. Capsule S/N 6 (Fig. B-46)

Figure B-46 shows that this capsule was not deformed except for a small amount of bending of one of the capsule ears.

- (1) The Pt-20% Rh cladding was cut open approximately three inches from the lower end. No helium was detected within the clad.
- (2) A helium leak check was run on the lower end (TIG weld end) of the cladding tubing. This check disclosed no leaks in the tubing except in the center of the end cap where the vent hole was sealed by TIG welding. Figure B-41 shows the cracking present in the area of the vent hole on the lower end cap.
- (3) A helium leak check was run on the upper end (EB weld) of the cladding tubing. This check disclosed no leaks.
- (4) With the liner still intact, as shown in Fig. B-46, the entire surface of the liner was dye penetrant checked. No indications of leaks were found.
- (5) The liner was then cut open approximately three inches from the lower end. Helium was detected within the liner. In addition, a characteristic matrix odor was detected within the liner tubing although later examination showed the inner liner was not breached.

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FIG. B-46. S/N 6 CAPSULE SHOWING VERY LITTLE DEFORMATION

FIG. B-47. TRANSVERSE SECTION THROUGH INNER TANTALUM LINER OF S/N 6

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- (6) Both pieces of the liner were helium leak checked. No leaks were detected.
- (7) The inner liner was cut open (steps from this point on not shown in Fig. B-46) approximately three inches from the lower end. Helium was detected within the inner liner.
- (8) Both pieces of the inner liner were helium leak checked and no leaks were found.
- (9) Transverse cross sections of both the inner liner (Figs. B-47 and B-3) and the liner (Fig. B-48) were made. Figure B-3, which is a higher magnification of a portion of Fig. B-47, shows intergranular attack of the tantalum inner liner on both the inner and outer surface. The inner surface attack appears more pronounced than that of the outer surface. No irregularities were observed with regard to the Ta-10%W liner material.
- (10) Figures B-49 and B-5 are photographs showing weld beads of both inner liner end caps. A cross section of the end cap in Fig. B-5 is shown in Fig. B-6. This section shows an incomplete weld penetration. Of a total available thickness of 0.027 inch, the weld penetration is only 0.014 inch.

b. Capsule S/N 20 (Fig. B-50)

- (1) The Pt-20% Rh cladding was cut open. However, the initial cut was inadvertently made through the liner as well as the cladding at a point approximately three inches up from the lower end (TIG weld end) of the capsule. As the initial cut was made, helium was detected. Since the initial cut was made simultaneously through both the clad and liner, the source of the helium detected cannot be attributed to either the liner or the clad or both.
- (2) The short (~3 in.) piece of liner was removed from its mating piece of clad.
- (3) The two three-inch pieces of liner and clad were helium leak checked and none were detected in either piece.
- (4) The EB welded end of the clad was removed approximately three inches from the end. A helium leak check of this piece was performed. No leaks were detected.
- (5) The remainder or central portion of the clad was removed from the capsule. Due to the distorted shape of the capsule, it was necessary to rip the cladding off (Fig. B-50).
- (6) The remainder of the liner (only 3 in. was removed from TIG end) was dye penetrant checked. No indications were observed.
- (7) The EB end of the liner was cut off approximately three inches from the capsule end and then helium leak checked. No leaks were detected.
- (8) The central portion of the liner was helium leak checked in place on the capsule. No leaks were detected.

FIG. B-48. TRANSVERSE SECTION
OF Ta-10%W LINER OF S/N 6

FIG. B-49. TANTALUM END CAP WELD ON S/N 6

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FIG. B-50. DAMAGE WHICH OCCURRED DURING IMPACT OF S/N 20

FIG. B-51. TRANSVERSE SECTION THROUGH LINER

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- (9) At this stage of the inspection, approximately three inches of inner liner on each end of the capsule was exposed. A dye penetrant check was made on each end of the inner liner covering approximately three inches. No indications were observed.
- (10) The EB end of the inner liner (~ 1 in. from end) was cut off. Helium was detected within the inner liner.
- (11) The one-inch piece of inner liner was helium leak checked. No leaks were detected.
- (12) The entire remainder of the inner liner was helium leak checked and no leaks were detected.
- (13) A transverse cross section through the most severely bent portion of the inner liner/liner tubing was taken (Fig. B-51 and B-7). As can be seen on the 100X photo (Fig. B-7), there is some amount of intergranular attack on both the inner and outer surfaces of the tantalum inner liner while the Ta-10%W liner exhibits no irregularities.

c. Capsule S/N 4 (Fig. B-52)

- (1) The Pt-20% Rh cladding was cut open at a point approximately one inch from the EB welded end of the clad. As the cut was made, helium was detected.
- (2) The one-inch piece of cladding was helium leak checked. No leaks were detected.
- (3) An attempt was then made to helium leak check the remainder of the cladding, which at this point was still a part of the capsule. However, before leak checking the clad tubing, the leak detector machine requires that a vacuum better than approximately 15 microns must be attained within the ID of the clad tubing. This partial vacuum could not be reached even after pulling a vacuum on the cladding overnight in a weld box. This method of checking the integrity of the clad was therefore abandoned and another method used which will be described in the steps which follow.
- (4) An approximately one-inch long piece of the cladding was cut from the TIG end of the capsule. This piece was helium leak tested. No leaks were detected.
- (5) The remainder of the Pt-20% Rh clad was leak checked in place on the capsule by using the test setup shown.

FIG. B-52. CAPSULE S/N 4 FOLLOWING IMPACT

FIG. B-53. LONGITUDINAL SECTION OF S/N 4 THROUGH AREA OF MAXIMUM DEFORMATION SHOWING CORROSION ON INNER LINER

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A helium atmosphere was placed all around the clad. No leaks were detected in the clad.

- (6) About one inch up from the end the TIG end of the liner was cut off. As the cut was made, helium was detected. After the cut was made and as the short piece of liner tubing was being removed from the inner liner tubing, the TIG end-end cap of the inner liner and a tantalum spacer came off. They just fell out as opposed to having been jammed down inside of the short one-inch piece of liner tubing and being pulled off along with the liner tubing.
- (7) In removing the short piece of liner tubing from the TIG end of the capsule, the end cap weld was inadvertently damaged. A leak check showed that this end of the liner leaked. The reasoning which lies behind the statement that the end cap leaked due to damage caused during removal of the short piece of liner tubing is as follows. Step 5 shows pictorially how the cladding was leak checked. Looking at that sketch and assuming for a moment that helium was present within the liner, if the liner end cap weld (or any other portion of the liner) leaked, then helium would have been detected during the completion of Step 5. The fact that helium was present within the liner was proven in Step 6 when the first cut through the liner was made and helium was detected.
- (8) The remainder of the liner tubing was helium leak checked as shown in the sketch. No leaks were detected.

- (9) The remaining end of the liner was removed and leak checked. No leaks were detected.
- (10) Since the TIG end inner liner end cap had previously come off (Step 6) the remainder of the inner liner was leak checked in the manner shown in the following sketch. No leaks were detected.

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- (11) A transverse cross section through the clad, liner and inner liner was made in an area of maximum deformation and was shown in Figs. B-11 Through B-13. These pictures show a tearing of the inner liner outer surface primarily in the area of severest bends. Intergranular attack on both the inside and outside (a smaller amount on the outside than on the inside) surfaces of the inner liner material is also visible in the photographs. No irregularities are observed in the liner and cladding material.
- (12) A longitudinal cross section through the clad, liner and inner liner was made in the second worst area of deformation as shown in Figs. B-53 and B-54. No cracking is visible, but evidence of intergranular corrosion of the inner liner, mainly on the inner surface, is present. No irregularities are present in either the liner or clad material.
- (13) A photograph was taken of the weld area of the inner liner end cap which inadvertently came off when removing a small piece of the liner tubing in that area. This was shown previously in Fig. B-8. Another photograph was taken of the intact, opposite end, inner liner end cap showing, in particular, the weld size and was shown in Fig. B-9. This photo actually shows the inner liner positioned within the outer liner (outer liner end cap has been removed). A comparison of the weld bead size of both caps shows that the cap which fell off (Fig. B-8) has a smaller weld bead buildup than the opposite end cap (Fig. B-9). Figure B-10 showed a section through the inner liner cap which did not fall off. The weld penetration in this end cap appears adequate and is quite close to being a full penetration weld. A cross section through the inner liner end cap which fell off is shown in Fig. B-55. A relatively small weld bead can be seen. It also appears that the cap broke away from the tubing in an area through the weld. The grain size in the heat affected zone does not appear significantly different from other inner liner grain sizes observed in this destructive examination.

d. Capsule S/N 14 (Fig. B-56)

- (1) The Pt-20% Rh cladding was cut open approximately two inches up from the TIG end of the capsule. As the clad was breached, helium was detected.
- (2) The two-inch long piece of cladding was removed from the TIG end of the capsule. However, as it was removed, the end cap of the liner came off with it. This end cap did not fall off as in Step 6 for Capsule S/N 4 but was stuck inside of the two-inch piece of cladding up against the underside of the end cap. A view of the liner tubing/end cap separation area is shown in Figs. B-57 and B-58.
- (3) The clad was leak checked in its entirety and no leaks were detected.
- (4) The liner was leak checked in the manner depicted in the sketch. No leaks were detected.

FIG. B-54. LONGITUDINAL SECTION OF S/N 4
SHOWING ATTACK ON INNER LINER IN
REGION OF MAXIMUM DEFORMATION

FIG. B-55. SECTION THROUGH Ta INNER LINER
END CAP OF S/N 4 THAT FELL OFF

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FIG. B-56. S/N 14 FOLLOWING TESTING

FIG. B-57. BROKEN END OF LINER TUBING OF S/N 14

FIG. B-58. LINER TUBING END CAP
SEPARATION AREA OF S/N 14

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- (5) A cut was now made through the liner and inner liner on the TIG end of the capsule (end where liner end cap came off). As the cut was made through the inner liner, helium was detected.
- (6) A cross section was made through the clad and stuck liner end caps. Upon sectioning, the liner end cap fell out of the clad, indicating each was mounted separately. Figures B-16 and B-17 were photographs of a section through the clad TIG end cap. Cracking is noticeable in the clad end cap both in the vent hole area as well as the peripheral weld. Figure B-18 was a view of the liner end cap which was wedged inside of the clad. This picture shows an area which appears to have been exposed to high temperatures following the initial welding of the liner end cap. Figure B-19 showed the same liner end cap from the opposite side after cutting into two pieces but prior to mounting. Figures B-20 and B-21 showed a cross section of the liner end cap.
- (7) A section taken through the inner liner end cap on the TIG end of the capsule was shown in Fig. B-22. Large grains can be seen in the heat affected zone.
- (8) Figures B-59 and B-60 show cross sections through the clad and liner materials, respectively, on the EB end of the capsule. No irregularities were observed in either weld.
- (9) Figures B-14 and B-15 were photographs of a transverse section of the capsule through an area of maximum distortion. Intergranular attack on both the inner and outer surfaces of the inner liner was observed. In addition, the initiation of tearing on the outer surfaces of both the inner liner and liner in the maximum bend areas is evident. No other irregularities were observed.

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FIG. B-59. Pt-20% Rh EB WELD OF S/N 14

FIG. B-60. S/N 14 EB WELD OF LINER END CAP

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APPENDIX C

FUEL CAPSULE DESIGN ANALYSIS

The Parametric Thermal Analysis Study (SNAP 29 MND2062-12-7) evaluated a series of capsule thermal inventories, capsule temperatures and fuel block temperatures as a function of selected component design and material characteristics. The capsule geometry was held constant because it was assumed for the parametric thermal analysis that the matrix could absorb required fluctuations in thermal inventory within the then geometric design and fabrication constraints (capsule diameter--0.657 inch; length--14.290 inches). A preliminary analysis indicated that the 0.657-inch diameter capsule, as is, could not contain more than approximately 1100 watts. A parametric study was then undertaken to determine what changes in the internal capsule design would permit the thermal inventory to be increased. The fuel loading capacity of a fuel capsule is dependent upon the following parameters.

- (1) Matrix-to-fuel volume ratio limits (fabrication)
- (2) Matrix and fuel densities (material properties)
- (3) Percent of matrix theoretical density (fabrication)-- K_v
- (4) Heat flux
- (5) Specific thermal power (property of fuel)-- P_w
- (6) Matrix volume-- V_m
- (7) Dimensions and structural constraints.

These parameters were combined analytically to determine the capsule maximum thermal inventory capacity.

The matrix volume can be expressed as

$$V_m = (VR) V_{fuel} \quad (C-1)$$

where

V_m = volume of matrix

V_{fuel} = volume of fuel

Therefore,

$$\frac{W_m}{\rho_m} = (VR) \frac{W_{fuel}}{\rho_{fuel}} \quad (C-2)$$

where

W = weight

Rearranging,

$$\frac{W_m}{W_{fuel}} = (VR) \frac{\rho_m}{\rho_{fuel}} = MR \quad (C-3)$$

where MR is defined as the weight mixture ratio.

The bulk density (d) of a mixture of two ingredients is

$$d = \frac{MR + 1}{\frac{MR}{\rho_m} + \frac{1}{\rho_{fuel}}} \quad (C-4)$$

The product of the bulk density of the mixture and the nonvoid volume (V_x) of the fuel matrix yield the total weight (W_T) of the fuel matrix;

$$W_m + W_{fuel} = W_T = d V_x \quad (C-5)$$

where

$$V_x = K_v \cdot V_m$$

Substituting Eq (C-3) into Eq (C-5) and solving for fuel weight yields

$$W_{fuel} = \frac{V_x}{(MR + 1)} \quad (C-6)$$

The maximum total thermal inventory per capsule is

$$Q_{tot/cap} = P_W \cdot W_{fuel} \quad (C-7)$$

The results of the parametric analysis for a given capsule inner liner ID showed that neither matrix OD nor matrix-to-fuel volume ratio significantly affect the fuel loading capacity for the range of values considered. Since a heat flux on the order of 60 watts/in.² has previously been accepted, this factor is not significant either. The important design parameter is matrix thickness. These results are shown in Fig. C-1. The helium pressure buildup within the capsule during the mission is dependent upon the internal void volume and, hence, matrix volume for a given inner liner ID, the temperature and the strength of the capsule structural liner. The maximum thermal capacity of the fuel capsule is ultimately dependent upon a tradeoff between helium pressure generation, peak re-entry temperature, void volume and liner ultimate tensile strength. A parametric study of these latter factors has been performed by the Stress and Dynamics Group.

A. CAPSULE INTERNAL He PRESSURE AND TEMPERATURE

Helium gas is generated within the inner liner of the fuel capsule through radio-isotope decay. The parametric study involving thermal inventory (void volume), internal pressure, re-entry temperature design point, capsule dimensions and structural liner thickness is discussed in another section of this report. In support

Notes: Fuel matrix density = 62% theoretical
Fuel matrix length = 13.83 in.

FIG. C-1. CAPSULE THERMAL INVENTORY VERSUS FUEL MATRIX DESIGN PARAMETERS

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of this latter study, an equation for helium pressure buildup was provided for inclusion in a computer code developed to analyze a range of the parameters listed.

Capsule internal pressure varies with temperature, time, initial thermal inventory and internal void volume. The ideal gas law was assumed, thus

$$PV = RT \quad (C-8)$$

or,

$$P = \rho RT = \frac{M_g}{V_g} RT = \frac{M_{He}}{V_V} \frac{R'}{M_{He}} T \quad (C-9)$$

where

- M_g = mass of helium = M_{He}
- V_g = volume of helium = void volume (V_V) (in. ³)
- R' = universal gas constant
- T = temperature (°R)
- M_{He} = molecular weight of helium.

The maximum number of moles of helium generated (which occurs at $t = \infty$) is

$$M_{He} = M_i \frac{M_{He}}{M_{Po}} \quad (C-10)$$

where

- M_i = mass of isotope, Po-210
- M_{Po} = molecular weight of isotope.

At any time, t , the percentage of the maximum number of moles of helium generated is a function of the nuclear decay characteristics of the fuel (assuming 100% of the helium generated is released to the cavity). Thus,

$$M_{He} = M_i \frac{M_{He}}{M_{Po}} (1 - e^{-\lambda t}) \quad (C-11)$$

where

- λ = $\ln 2.0$ /fuel half life (days)
- $M_i = \frac{PW}{454 C}$
- PW = thermal power (watts)
- C = thermal power per unit weight of fuel (watts/gm)

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Combining Eqs (C-9), (C-10) and C-11) and all constants, yields

$$P = 1.350 \times 10^{-3} (1 - e^{-\lambda t}) \frac{T PW}{V_v} \quad (C-12)$$

where

t is measured in days

T in °R

PW in watts

V_v in cubic inches.

The structural parametric study and an analysis of fabrication tolerances resulted in the capsule design described in another section. The internal pressure-time relationship and the capsule temperatures for this recommended capsule design were determined and reported in Appendix A. The minimum void volume for this design is approximately 5.05 cubic inches.

A plot of capsule internal pressure as a function of the time of re-entry for this capsule design is shown in Fig. C-2. The maximum pressure will be approximately 1000 psia.

Capsule temperatures also have been determined for the recommended capsule design. The two temperatures of primary interest are those for the oxidation clad and the inner liner. These temperatures were determined for the planned mission period under full power load conditions and for an extension of the in-orbit time to 1000 days under open circuit conditions. The latter temperatures were determined to support the capsule creep/rupture study performed by the Stress and Dynamics Group. The temperatures were determined based on the following RTG characteristics:

Fuel block bore temperature (° F)	1530
Fuel block bore and capsule clad emissivity	0.80
Fuel block material	ATJ graphite
Fuel block bore-capsule clad environment	Vacuum
Capsule fuel length (in.)	12.83
Hot plate temperature (° F)	1212
Maximum capsule thermal inventory at t _o (watts)	1100

The results of this temperature evaluation are presented in Fig. C-3 which shows the component temperature decay with time.

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FIG. C-2. CAPSULE HELIUM PRESSURE AS A FUNCTION OF TIME AT PEAK RE-ENTRY CONDITIONS

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FIG. C-3. CAPSULE TEMPERATURES--FOUR-MODULE WITH FOUR CAPSULES/MODULE RTG

APPENDIX D

AEROTHERMAL ANALYSIS OF FUEL BLOCK

A. RE-ENTRY HEATING ANALYSIS

Five modular fuel block configurations were analyzed for tumbling, broadside and oscillating ± 45 degrees about broadside re-entry modes using heating data obtained from the Arnold Engineering Development Center (AEDC) Tunnel B tests. The nominal peak capsule temperatures are presented in Table D-1.

TABLE D-1

Peak Capsule Temperatures of Modular Fuel Blocks During Re-entry

Block Size		Peak Capsule Temperatures (°F)			
<u>Dimensions*</u> <u>(in.)</u>	<u>Number of</u> <u>Modules</u>	<u>Broadside</u>	<u>Oscillating</u> <u>± 45 Degrees</u> <u>About Broadside</u>	<u>Tumbling</u>	<u>Edge-On</u> <u>Spin</u>
35.63 x 32.68	8	2250	2140	2280	3060
27.20 x 32.68	6	2250	2150	2280	3000
18.79 x 32.68	4	2290	2180	2300	2890
18.79 x 17.39	2 (lo AR)	2400	2250	2350	2800
10.35 x 32.68	2 (hi AR)	2330	2250	2360	2700

*All blocks one inch thick

Temperatures based on:

1. ATJ graphite fuel block
2. AEDC Wind Tunnel B test data
3. Nominal heating rates
4. Vacuum gap between block and capsule
5. Initial conditions:

Flight path angle (deg)	-3
Altitude (ft)	400,000
Velocity (fps)	25,690
Block temperature (°F)	1600
Capsule temperature (°F)	1800

6. Nominal EOL fuel inventory.

The four-module size SNAP 29 fuel block was analyzed for spinning re-entry at various fixed pitch angles and for various degrees of oscillation about the edge-on spin re-entry mode. Fixed pitch angles of 90 (edge-on spin), 80, 70 and 45 degrees were examined. Oscillating pitch angles of 90 degrees ± 10 , ± 30 and ± 45 were also considered. The initial flight path angle was -3 degrees for all cases. Peak capsule temperatures obtained during this study are shown in Fig. D-1.

B. RE-ENTRY HEATING TESTS

Plasma arc tests have been conducted to evaluate the effects of constant heat fluxes on two fuel block materials, ATJ graphite and Carb-I-Tex 500 needled graphite and on reduced size models of the SNAP 29 fuel block assembly. The following describes the results for the four specimen designs utilized in these tests.

1. Material Screening Specimens

These specimens were tested to evaluate the material characteristics such as spallation, cracking, etc. Four 2.5-inch diameter by 4-inch long ATJ graphite specimens and two 2.5-inch diameter by 3.5-inch long Carb-I-Tex graphite specimens were tested. Visual examination of the specimens and post-test X-rays have not shown any signs of spallation or cracking. Surface recessions for the conditions tested ranged from 0.015 to 0.155 inch.

2. End Closure Specimens

These specimens were tested to evaluate the capsule retainer designs. Five ATJ graphite specimens and six Carb-I-Tex graphite specimens were tested. Each material utilized a different capsule retainer design. Of these, the one piece type used in the Carb-I-Tex specimens was the most satisfactory and the easiest to fabricate. This design maintained a tight fit after test whereas the two-piece type exposed several plug threads. Either type is considered to have performed satisfactorily.

3. Correlation Specimens

These specimens are reduced size models (6.5 x 6.5 x 1 in.) of the SNAP 29 fuel block assembly. Twelve ATJ specimens were tested to obtain temperature history data at approximately 12 internal locations. A 10-mil thick C-34 bonded joint was also evaluated. No excessive erosion was found in the area of the joint on any of the 12 specimens tested. The temperature data were of good quality.

4. Additional Bond Evaluation Specimens

Two additional uninstrumented ATJ graphite specimens measuring 6.5 by 6.5 by 1.2 inches were tested to evaluate the effects of the equivalent broadside heating environment on a 0.030 inch thick C-34 bonded joint. No indication of a failure in the bonded joint was found.

A summary of plasma arc test conditions and some results are shown in Table D-2 and D-3.

Four-module, ATJ graphite fuel block

Initial conditions:

Path angle = -3 deg

Altitude = 400,000 ft

Velocity = 25,690 fps

Nominal EOL fuel inventory = 535 watts/capsule

Nominal aero heating rate

FIG. D-1. PEAK CAPSULE TEMPERATURE DURING RE-ENTRY--SPINNING ABOUT FIXED PITCH ANGLE AND OSCILLATING ABOUT EDGE-ON SPIN

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TABLE D-2

Summary of Plasma Arc Screening Test Conditions and Results

Specimen No. 446A1141-	Graphite Type	Stagnation Heat Flux (Btu/ft ² -sec)	Total Enthalpy (Btu/lbm)	Stagnation Pressure (atm)	Test Time (sec)	Peak Surface Temperature (°F)	Total Recession (in.)
026-009-1	ATJ	255	10850	0.0739	170	3760	0.111
026-009-2	ATJ	255	10850	0.0739	170	3800	0.086
027-009-1	Carb-I-TeX	255	11128	0.0739	170	4250	0.103
027-009-2	Carb-I-TeX	255	11128	0.0739	170	4250	0.097
026-009-3	ATJ	255	10850	0.0739	280	3820	0.155
026-009-4	ATJ	255	11128	0.0739	280	3920	0.144
026-009-6	ATJ	86	9775	0.0110	250	2200	0.023
026-009-7	ATJ	86	9775	0.0110	250	2140	0.015
027-009-3	Carb-I-TeX	86	9500	0.101	250	3000	0.015
027-009-4	Carb-I-TeX	86	9500	0.101	250	2980	0.015
026-009-8	ATJ	86	9775	0.110	350	2160	0.025
026-009-9	ATJ	86	9500	0.101	350	2280	0.020
026-009-10	ATJ	450	12270	0.1790	90	4520	0.065
026-009-11	ATJ	450	12270	0.1790	90	4430	0.067
027-009-5	Carb-I-TeX	450	12270	0.1790	90	4860	0.075
027-009-6	Carb-I-TeX	450	12304	0.1680	160	4860	0.095
026-009-5	ATJ	450	12304	0.1680	160	4580	0.107
026-009-12	ATJ	450	12304	0.1680	160	4560	0.119

*First 12 specimens tabulated were tested during last quarter.

TABLE D-3

Test Parameters for End Closure and Correlation Specimens

Specimen No.	Desired Test Conditions					Actual Test Conditions			
	Stagnation Heat Flux (Btu/ft ² -sec)	Integrated Heat Flux (Btu/ft ²)	Total Enthalpy (Btu/lbm)	Stagnation Pressure (atm)	Time (sec)	Stagnation Heat Flux (Btu/ft ² -sec)	Total Enthalpy (Btu/lbm)	Stagnation Pressure (atm)	Time (sec)
466A1141-									
050-009-1	575	77,100	13,000	0.70	135	496	12,233	0.0338	155
-2	↑	161,000	↑	↑	280	496	12,233	0.0338	323
-3		161,000			280	496	12,233	0.0338	323
028-029-1		77,100			135	487	12,590	0.0322	330
-2		161,000			280	Damaged in facility--not tested			
-039-1		77,100			135	500	12,959	0.027	155
-2		161,000			280	487	12,590	0.0322	330
042-019-2		161,000			280	496	12,535	0.026	323
-009-1		161,000			280	510	12,295	0.031	315
-029-1		161,000			280	510	12,942	0.026	315
-2	575	161,000	13,000		280	496	11,930	0.027	323
050-009-4	225	77,100	10,000		340	225	10,600	0.0082	350
-5	↑	100,000	↑		450	225	10,600	0.0082	450
-6		100,000			450	225	10,600	0.0082	450
028-009-1		77,100			340	230	10,182	0.0089	340
-019-1		100,000			450	230	10,182	0.0089	435
042-019-1		77,100			340	230	10,638	0.0083	340
-009-2		77,100			340	235	10,289	0.009	340
-029-3		77,100			340	230	10,182	0.0089	340
-4	225	77,100	10,000	0.70	340	237	10,038	0.0089	340
-049-1	120	8,900	12,000	0.038	75	115	12,473	0.0103	80
-039-1	120	8,900	12,000	0.038	75	115	12,957	0.0095	80
-049-2	80	18,500	10,000	0.030	230	110-115	12,353	0.0081	222
-039-2	80	18,500	10,000	0.030	230	83	10,235	0.0083	222

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APPENDIX E

SYSTEMS CONSIDERATIONS IN SELECTING RTG SIZE

A. INTRODUCTION

The purpose of this study is to evaluate the total RTG system characteristics as a function of the number of thermoelectric modules. The evaluation will be combined with a similar safety study to technically evaluate the selection of minimum system size (i.e., minimum number of modules) for SNAP 29. The majority of the information compiled in this study has been previously published as noted throughout the text. Detailed information on each subject can be obtained by referring to the referenced documents.

B. SYSTEM CONSIDERATIONS

The system considerations which are investigated in this study include:

- (1) T/E module configuration
- (2) Heat source configuration
- (3) Thermal inventory required
- (4) Heat rejection subsystem requirements
- (5) RTG system weight
- (6) RTG system net power output
- (7) System power per unit weight.

The T/E module configuration is discussed because the module places constraints on the heat source and heat rejection subsystems. After investigating the resultant changes in the heat source and heat rejection subsystem, the total system characteristics, i.e., efficiency, power, weight and power per unit weight are examined and a conclusion drawn.

1. T/E Module Characteristics

The three different T/E modules which are candidates for the SNAP 29 system are:

- (1) Diaphragm module (Drawing 466A1341060)
- (2) BeO piston-Be heat sink module (NSK 861)
- (3) Aluminum piston-stainless steel heat sink modules (466A1341000).

Significant characteristics of these modules are summarized in Table E-1. The overall dimensions of the modules were obtained by referring to the referenced drawings.

TABLE E-1
SNAP 29 Module Characteristics

Module Configuration	Overall Dimensions (in.)	ΔT_H^* (°F)	ΔT_C^{**} (°F)	EOL P_N (W) at 1050 to 350
Diaphragm	8.4 x 15.2 x 2.2	125	40	93 to 100
BeO piston-Be heat sink	8.4 x 15.2 x 2.7	85	85	93 to 100
Al piston-SS heat sink	8.4 x 15.2 x 2.7	75	105 120	93 to 100

$$\Delta T_H^* = T_{HP} - T_H$$

$$\Delta T_C^{**} = T_C - T_R$$

a. Hot and cold end temperature gradients

The temperature drops between the hot plate and hot junction are calculated values for the diaphragm and BeO piston-Be heat sink modules whereas experimental data were used for the aluminum piston-stainless steel heat sink module. A preliminary thermal analysis of the diaphragm module (Ref. E-1) is being updated to include a reference helium gap thickness of 0.010 inch and more complete data on the ΔT across the mica gaps. This analysis (Ref. E-2) indicates an expected ΔT_H of 125° F. The ΔT_H for the BeO-Be heat sink module was obtained by subtracting the helium gas gap temperature drop from the diaphragm module calculation.

The cold end temperature drop (ΔT_C) consists of the temperature drop between the cold junction and the heat pipe evaporator outside wall plus the ΔT through the heat pipe. Experimental data indicate that a heat pipe ΔT of approximately 15° F might be expected. The ΔT through the diaphragm module cold stack has been measured by Martin Marietta manufacturing to be approximately 25° F. Therefore ΔT_C for the diaphragm module is obtained by combining the components to give a 40° F temperature drop. The BeO piston-Be heat sink ΔT_C is obtained by adding the approximate 70° F calculated in Ref. E-3 to the 15° F heat pipe drop to obtain an overall ΔT_C of 85° F. The aluminum piston-stainless steel heat sink ΔT_C is given as a range in Table E-1. This range is based on the approximate measurement of 90° F cold end ΔT of the 1340 modules presently on test (Ref. E-4), the 15° F temperature drop through the heat pipe, plus the uncertainty in the added temperature drop for adapting this module to a heat pipe heat rejection scheme. This uncertainty in thermal consequences of adapting the module to a heat pipe scheme was considered to be between 0° and 15° F, giving an overall ΔT_C range from 105° through 120° F.

b. Power output

The expected end-of-life net power output of the module is based on the present module tests (Ref. E-4). These tests indicate that the end-of-life (day 144) power could range from approximately 93 (watts(e) (S/N -001) to approximately 100 watts(e) (S/N -003, -005) for operation at a 1050° F hot junction temperature and a 350° F cold junction temperature.

2. Heat Source Characteristics

A summary of the heat source physical characteristics for each size system considered is shown in Table E-2. The fuel block overall dimensions are independent of module type and reflect the new capsule baseline design discussed in Ref. E-5. Heat source weights are shown for both with and without a nickel oxidation clad of 0.018 inch thickness. The heat source applies to the four-capsule per module concept and the 2 inch edge member requirement.

TABLE E-2
Fuel Block Size

<u>No. of Modules</u>	<u>Block Overall Dimensions (in.)</u>			<u>Heat Source Weight (lb)</u>	
	<u>Thickness</u>	<u>Horizontal</u>	<u>Vertical</u>		<u>Including 0.018-in. Ni clad</u>
2	1.19	17.8	19.8	34.2	38.2
2 _{HAR}	1.19	31.7	11.4	34.7	38.7
4	1.19	31.7	19.8	62.6	70.7
6	1.19	31.7	28.3	90.4	102
8	1.19	31.7	36.8	118	134

3. System Thermal Requirements

The end-of-life thermal inventory which is selected to give the desired hot and cold junction temperature consists of the heat which enters the T/E elements plus the parasitic heat loss. The Martin Marietta T/E digital code was used to determine the required thermal energy which must enter the T/E elements ($Q_{T/E} =$

1645 watts from Ref. E-6) to maintain a hot junction temperature of 1050° F and a cold junction temperature of 350° F. The parasitic heat losses through the module (Refs. E-2 and E-7), through the housing (Ref. E-8) and through the shutters (Ref. E-9), have been investigated in detail. The parasitic heat losses through the shutters have been measured (Ref. E-10) and determined to be higher than predicted. The test results in an increased (above predicted) system thermal requirement of ~1%.

Table E-3 summarizes the thermal requirements of the various size systems for each type of module assuming $T_H = 1050^\circ \text{F}$ and $T_C = 350^\circ \text{F}$. Parasitic heat losses for each system were obtained by combining the results of the referenced studies. These are shown in Table E-3. The diaphragm and BeO piston-Be heat sink modules have approximately (within analytical accuracy) the same parasitic heat losses. The parasitic heat losses shown in Table E-3 are analytical predictions and must be increased to account for the experimental results of the shutter parasitic heat loss test. The required $Q_{\min \text{ EOL}}$ was obtained by combining the parasitic heat losses and the T/E elements thermal requirement. The system efficiency was determined by dividing the electrical power output by $Q_{\min \text{ EOL}}$. The range shown in Table E-3 reflects the expected range in electrical power output of each T/E module (see Table E-1).

TABLE E-3
Thermal Requirements of SNAP 29

Total System
($T_H = 1050^\circ \text{F}$, $T_C = 350^\circ \text{F}$)

<u>Modules (No.)</u>	<u>$Q_{\text{parasitic}}$ (watts)</u>	<u>$Q_{\text{min EOL}}$ (watts) Total</u>	<u>η_{system} (%)</u>	<u>Q_{tol}^* Max/Capsule (watts)</u>
<u>SS Heat Sink Module</u>				
2	813	4,100	4.50 to 4.90	1110
2 low AR	1070	4,360	4.30 to 4.60	1180
4	1520	8,100	4.60 to 4.90	1100
6	2315	12,200	4.60 to 4.90	1100
8	2931	16,100	4.60 to 5.00	1090
<u>Drop & Beryll, Heat Sink Module</u>				
2	849	4,140	4.50 to 4.80	1120
2 low AR	1106	4,400	4.20 to 4.60	1190
4	1592	8,200	4.60 to 4.90	1100
6	2423	12,300	4.60 to 4.90	1100
8	3075	16,200	4.60 to 4.90	1100

*Four capsules per module.

The last column of Table E-3 shows the maximum thermal inventory per capsule at time of loading, assuming that four capsules per module are used. A $\pm 5\%$ fueling loading tolerance was assumed in determining the thermal loading. These data show that the 1100 watts used in the capsule baseline design selection is consistent with the system requirements.

4. Heat Rejection Requirements

The waste heat which flows through the T/E module and subsequently must be rejected by the heat pipe heat rejection subsystem was discussed in Refs. E-11 and E-12. These reports discussed the heat rejection requirement for the diaphragm module when arranged in a six-module system. The heat rejection requirements for a single module were presented as a function of T/E module cold junction temperature (assuming a constant hot junction temperature). The results of a parametric study of the radiator sizing was presented and is shown in Fig. E-1. This figure shows the heat pipe length requirement (assuming radiator height is the same as module height) as a function of cold end ΔT_C and T/E module cold junction temperature for three values of the Martin Marietta experimental adjustment factor, Q_{19} , which corrects for the heat passing through the T/E elements. Recent data in the 1340 Control Point T/E module tests indicate that the Q_{19} factor should be no higher than 10%. Therefore, the Q_{19} equal to 10% curve is used for the remainder of this study. The difference in the heat losses for the three candidate modules was very small (Table E-3) and therefore the diaphragm module heat rejection requirements will be used for all modules in this study.

One example of the use of Fig. E-1 is to consider RTG integration with a 10-foot diameter vehicle. Figure E-2 shows the resulting cold junction temperature as a function of cold end ΔT_C and the corresponding range in expected T/E module net power. The cold end ΔT noted in Table E-1 for each module is indicated on Fig. E-2. As expected, the module cold junction temperature increases and the net power decreases as the cold end ΔT increases. Accompanying the increased cold junction temperature is a decreased thermal inventory requirement (assuming $T_H = \text{constant}$) as shown in Ref. E-12. This report shows an approximate 8 $\rightarrow$ 9% decrease in module (and thus the RTG system) thermal inventory requirement by an increase in T_C of 50° F.

A second example of RTG-vehicle integration schemes is to provide a radiator large enough, through increased heat pipe length, to assure a cold junction temperature of 350° F. Using Fig. E-1, the required heat pipe length can be determined for each module as shown in Table E-4. The minimum diameter vehicle and the heat rejection subsystem weights are also shown for each candidate module. The bottom row in Table E-4 is the radiator configuration from the previous example. It is shown for comparison purposes.

5. RTG System Weight

A summary of the individual subsystem and total RTG system weight for each type of candidate module, for systems having two, four, six or eight T/E modules per system and for the two methods of vehicle integration with the RTG heat rejection subsystem is given in Table E-5. The system weights do not include weights associated with vehicle integration (i. e., thermal insulation between radiator and vehicle, RTG support beams, etc.).

FIG. E-1. TOTAL HEAT PIPE LENGTH VERSUS COLD JUNCTION TEMPERATURE, ΔT COLD JUNCTION-TO-FIN ROOT AND $Q19$ CORRELATION FACTOR

FIG. E-2. POWER AND COLD JUNCTION TEMPERATURE
AS A FUNCTION OF COLD END ΔT

TABLE E-4
Heat Rejection Subsystem Size and Weight
to Obtain $T_C = 350^\circ \text{F}$

$T_H = 1050^\circ \text{F}$, $Q_{19} = 10\%$, $P = 93 \rightarrow 100 \text{ watts(e)}$

<u>Module Configuration</u>	<u>Heat Pipe Length (ft)</u>	<u>Minimum Diameter* of Vehicle (ft)</u>	<u>$W_{\text{hr/mod}}$ (lb)</u>
Diaphragm	17.3	11.1	27
Be heat sink	21.4	13.8	34
SS heat sink	23.7	15.3	37
10-ft diameter** vehicle radiator	15.3	10.0	24

*Assuming they can be stacked.

**Radiator presented for comparison purposes.

The heat source weights presented in Table E-5 were discussed in a previous section. The heat source subsystem weight is considered independent of the type of module employed and the heat rejection integration scheme. These weights are considered relatively insensitive to the small variations in thermal inventory requirements. Since the fuel capsule is designed for the higher thermal inventory requirements, this assumption should be valid.

The thermoelectric module weights for the various modules are based on the weight study reported in Ref. E-13. The aluminum piston-stainless steel heat sink module weights presented in Table E-5 reflect the estimated weight required to adapt this module to a heat pipe heat rejection scheme. The reduced weight of the diaphragm module over the BeO piston-Be heat sink module reflects the continual module development effort.

The thermal control subsystem weights are identical to those reported in Ref. E-13.

The heat rejection subsystem weights are identical to the previous weight estimate for the 15.5-foot length heat pipe except for the elimination of a panel stiffening frame. The heat rejection subsystem weights for integration schemes requiring heat pipes longer than 15.5 feet were obtained by assuming the weight was proportional to radiator area requirements. The heat pipe evaporator weight is included in this subsystem and not in the T/E module subsystem.

The RTG structural housing weights are those reported in Ref. E-13.

6. System Electrical Power Output

Table E-6 includes a summary of the net system electrical power output for the three types of modules using the two RTG-vehicle integration schemes discussed in Section B-4. The power given under each integration scheme is shown as a range and does not account for the expected I^2R power losses associated with inter-module wiring. The system powers shown for the 15.5-foot heat pipe length are based on the individual module power range developed in Section B-1. The systems

TABLE E-5
RTG Weight Summary¹

Subsystem (ft)	Two-Module Generator						Four-Module Generator						Six-Module Generator						Eight-Module Generator					
	Al Pistons		BeO Pistons		Diaphragm		Al Pistons		BeO Pistons		Diaphragm		Al Pistons		BeO Pistons		Diaphragm		Al Pistons		BeO Pistons		Diaphragm	
	15.5	23.7	15.5	21.4	15.5	17.25	15.5	23.7	15.5	21.4	15.5	17.25	15.5	23.7	15.5	21.4	15.5	17.25	15.5	23.7	15.5	21.4	15.5	17.25
CP 1100 Heat Source	38.7	38.7	38.7	38.7	38.7	38.7	70.7	70.7	70.7	70.7	70.7	70.7	101.6	101.6	101.6	101.6	101.6	101.6	133.55	133.55	133.55	133.55	133.55	133.55
CP 1300 T/E Modules	78.0	78.0	59.4	59.4	52.0	52.0	156.0	156.0	118.8	118.8	104.0	104.0	234.0	234.0	178.2	178.2	156.0	156.0	312.0	312.0	237.6	237.6	208.0	208.0
CP 1500 T/C System	12.0	12.0	12.0	12.0	12.0	12.0	17.1	17.1	17.1	17.1	17.1	17.1	22.0	22.0	22.0	22.0	22.0	22.0	30.8	30.8	30.8	30.8	30.8	30.8
CP 1800 Heat Rejection System	48.0	74.0	48.0	68.0	48.0	54.0	96.0	148.0	96.0	136.0	96.0	108.0	144.0	222.0	144.0	204.0	144.0	162.0	192.0	296.0	192.0	272.0	192.0	216.0
CP 1900 Structures	49.2	49.2	49.2	49.2	49.2	49.2	65.5	65.5	65.5	65.5	65.5	65.5	91.0	91.0	91.0	91.0	91.0	91.0	110.6	110.6	110.6	110.6	110.6	110.6
Total RTG Weight (lb)	225.9	251.9	207.3	227.3	199.9	205.9	405.3	457.3	368.1	408.1	353.3	365.3	592.6	670.6	536.8	597.7	514.6	532.6	778.95	882.95	704.55	784.55	674.95	698.95

¹Based on 13,584-inch overall capsule length (room temperature)
Four capsules/module
Weights shown for two heat pipe lengths (lengths include portion in module)
Vehicle mounting structure and thermal insulation not included
Clad = 0.018 inch thickness of nickel

shown which employ the aluminum piston-stainless steel heat sink module are based on an assumed cold end ΔT_C of 105° F. Also shown in Table E-6 is the approximate load voltage (assuming series module connections) associated with the number of modules in a system. One of the most important points shown in Table E-6 is that one T/E module does not necessarily produce 100 watts(e). Therefore, a four-module system is not necessarily a 400-watt(e) system.

7. System Power per Unit Weight

The system weight and power per unit weight (P/W) are shown in Table E-6. The total system weight is that developed in Section B-5. The resultant system power per unit weight shows the following general trends:

- (1) High module performance ($C_T = 375 \mu\Omega\text{-cm}^2$) versus low module performance ($C_T \approx 750 \mu\Omega\text{-cm}^2$) increased P/W by ~ 0.03 to 0.10 watt/lb.
- (2) Operation at a $T_C = 350^\circ\text{F}$ instead of that obtained on a 10-foot diameter vehicle increased P/W by:
 - (a) 0.02 watt/lb for diaphragm module
 - (b) 0.04 to 0.06 watt/lb for BeO piston-Be heat sink module
 - (c) 0.06 to 0.08 watt/lb for Al piston-SS heat sink module.
- (3) The diaphragm module has a 0.08 to 0.16 watt/lb advantage over the BeO piston-Be heat sink module.
- (4) The BeO piston-Be heat sink module has a 0.09 to 0.13 watt/lb advantage over the Al piston-SS heat sink module.
- (5) Increasing the number of modules per system has the following effect:
 - (a) Two to four modules increases P/W by 0.08 to 0.14 watt/lb
 - (b) Four to six modules increased P/W by 0.01 to 0.03 watt/lb
 - (c) Six to eight modules increases P/W by 0.01 to 0.03 watt/lb.
- (6) Only the diaphragm module system indicates that 1 watt/lb can be achieved.

C. CONCLUSIONS

Results of this study indicate that system characteristics are not significantly affected by the numbers of modules in a system. Therefore, the selection of the number of modules in the SNAP 29 system must be based on criteria other than RTG system characteristics (i.e., desired power level of potential missions, safety considerations, etc.).

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TABLE E-6
SNAP 29 Modular System Performance Parameters
($T_H = 1050^\circ \text{F}$, $Q_{19} = 10\%$)

	<u>Diaphragm Module</u>				<u>BeO Piston-Be Heat Sink</u>				<u>Al Piston-SS Heat Sink**</u>			
Vehicle diameter (ft)	(10)		11.1		(10)		13.8		(10)		15.3	
Heat pipe length (ft)	15.5		17.3		15.5		21.4		15.5		23.7	
Average cold junction temperature ($^\circ\text{F}$)	368		(350)		400		(350)		416		(350)	
C_T ($\mu\Omega\text{-cm}^2$)*	375	750	375	750	375	750	375	750	375	750	375	750
Two-module (7 volts)												
Power (watts)	189	176	200	186	170	158	200	186	163	152	200	186
Weight (lb)	200	200	206	206	207	207	227	227	226	226	252	252
P/W (watt/lb)	0.95	0.88	0.97	0.90	0.82	0.76	0.88	0.82	0.72	0.67	0.79	0.74
Four-module (14 volts)												
Power (watts)	378	352	400	372	340	316	400	372	327	304	400	372
Weight (lb)	353	353	365	365	368	368	408	408	405	405	457	457
P/W (watt/lb)	1.02	1.00	1.10	1.02	0.92	0.86	0.98	0.91	0.81	0.75	0.88	0.82
Six-module (21 volts)												
Power (watts)	568	528	600	558	509	474	600	558	490	456	600	558
Weight (lb)	515	515	533	533	537	537	598	598	593	593	671	671
P/W (watt/lb)	1.10	1.03	1.13	1.05	0.95	0.88	1.00	0.93	0.83	0.77	0.89	0.83
Eight-module (28 volts)												
Power (watts)	757	704	800	744	679	632	800	744	653	608	800	744
Weight (lb)	675	675	699	699	705	705	785	785	779	779	883	883
P/W (watts/lb)	1.12	1.05	1.15	1.07	0.96	0.90	1.02	0.95	0.84	0.78	0.90	0.84

* $C_T = 375 \mu\Omega\text{-cm}^2$ = expected EOL conditions for S/N 003 and S/N 005.

$C_T = 750 \mu\Omega\text{-cm}^2$ = EOL condition if performance is similar to S/N 001 performance.

** ΔT_C assumed equal to 105°F .

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APPENDIX F EFFECT OF RECENT 3P MATERIAL ON SYSTEM PERFORMANCE

Recent property measurements (2B series) on 3P elements have shown changes in α , ρ and K from 3M published data (TEDP 00-59) and from earlier SNAP 29 measurements (7B series). These changes have a pronounced effect on the thermoelectric efficiency and hence the same effect on system efficiency. The Martin Marietta thermoelectric code results show that a system built using 2B series elements would be ~5% less efficient than one using 7B series elements.

The thermal conductivity and electrical resistivity data for 3P used in the thermoelectric code comparison are shown in Figs. F-1 and F-2. The Seebeck coefficient used for the 2B series was 0.95 of that used for TEDP 00-59 and the 7B series. This was assumed the same as the ratio of Martin Marietta measured to predicted Seebeck voltages at $T_H = 900^\circ\text{F}$ and $T_C = 150^\circ\text{F}$. The 2N properties used were the same in all cases (i.e., TEDN 00-34). A tabulation of thermoelectric code results is shown in Table F-1. The 00-34/00-59 and 7B series results are essentially the same. The 2B series results, however, show a thermoelectric efficiency that is ~5% lower than the 7B (and 00-34/00-59) series. Since the P/NA for a given contact is approximately the same in all three cases, the power for a given contact resistivity would be the same for a couple or module constructed of any of the materials described by the property data.

The decrease in T/E efficiency of the 3P material means that the system efficiency will drop by the same percentage. For SNAP 29 which controls the hot junction temperature, this means that, unless the fuel inventory is increased ~5%, the shutters will close approximately seven days earlier than anticipated. After the shutters are closed, further decay of the isotope would cause the hot junction temperature (hence power) to decrease below the design value.

TABLE F-1

Summary of T/E Code Results

Material Properties	$T_H = 1050^\circ\text{F}$ Element length = 0.50 inch		$T_C = 350^\circ\text{F}$		$P = 100\text{ w}$ $V = 28\text{ v}$	
	34/59		Lot 7B Data		Lot 2B Data	
$C_T (\mu\Omega\text{-cm}^2)$	0	750	0	750	0	750
$\eta_{T/E} (\%)$	7.35	6.93	7.34	6.92	7.00	6.57
N	290.7	293.6	290.9	293.6	300.1	302.2
$D_N (\text{in.})$	0.303	0.310	0.302	0.310	0.360	0.314
$D_P (\text{in.})$	0.287	0.301	0.287	0.300	0.273	0.287
$P/NA (\text{w/in.}^2)$	2.514	2.325	2.517	2.327	2.526	2.324
$Q/NA (\text{w/in.}^2)$	34.219	33.552	34.318	33.643	36.085	35.386

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FIG. F-1. 3M LEAD TIN TELLURIDE TEGS-3P COLD PRESSURED AND SINTERED ELECTRICAL RESISTIVITY

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FIG. F-2. THERMAL CONDUCTIVITY--3M-3P MATERIAL

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APPENDIX G

SPRING EVALUATION

A. FUEL BLOCK SEPARATION SPRINGS

Four spring assemblies are included in the structure to provide the motive power to separate the fuel block from the RTG housing at the end of life. The springs are sized to provide a velocity of 4 fps to a 100-pound (eight-module size) fuel block.

The original design is shown in Fig. G-1. Each of these springs provides a maximum force of 128.7 pounds in the compressed position, and has a working stroke of 2.00 inches, resulting in a spring rate of 64.35 lb/in. The weight of each of these assemblies is 2.0 pounds, resulting in a total weight of 8.0 pounds per RTG.

By changing to a nested spring design, also shown in Fig. G-1, the overall length is reduced by 2-3/8 inches and the weight per cartridge is reduced to 1.36 pounds, resulting in a net weight reduction of 2.56 pounds per generator.

Another possible spring device which may be applied for fuel block separation consists of a torque tube spring. This is shown in Fig. G-2. The weight per cartridge is 0.8 pound, resulting in a weight reduction of 4.80 pounds per RTG, from the original spring design.

An additional spring concept was considered for fuel block ejection. This consisted of a cantilever beam spring. This became impractical because the length becomes unreasonable (>20 in.) when attempting to maintain a reasonable slope to the elastic curve. As the length increases the section properties also increase resulting in increased weight. For these reasons no further evaluation was performed on this concept.

Table G-1 provides a summary of the results of this evaluation. Based upon the total weight savings of 4.80 pounds per RTG as well as upon its relative simplicity, the torsion bar spring design is selected for this application.

TABLE G-1
Fuel Block Separation Springs

Design Concept	Spring Properties			Configuration	Weight per Assembly (lb)	Number of Assemblies	Total Weight per RTG (lb)
	Maximum Load	Stroke	Spring Rate				
Single helical compression spring cartridge	128.7	2.00	64.35	0.218 diameter wire, 1.625 OD, 20.25 coils	2.0	4	8.0
Nested helical compression spring cartridge	128.7	2.00	64.35	0.177 diameter wire, 1.625 OD, 12.5 coils, 0.135 diameter wire, 1.188 OD, 15.75 coils	1.36	4	5.44
Torsion bar spring	128.7	2.00	64.35	0.5 OD x 0.035 wall thickness titanium tube 15.7 in. long	0.8	4	3.20

FIG. G-1. FUEL BLOCK EJECTION SPRING

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FIG. G-2. TORSION BAR SPRING

B. SHUTTER EJECTION SPRINGS

Four spring assemblies are also included in the structure to provide a shutter separation velocity of 1 fps relative to the fuel block (5 fps relative to the stationary portion of the RTG housing), based upon a 40-pound (8-module size) thermal control system weight.

The original design is shown in Fig. G-3. This provides a maximum load of 2.25 pounds, a stroke of 2.00 inches, and a spring rate of 1.125 lb/in. The total weight of this design is 0.38 pound which results in a weight per RTG of 1.52 pounds.

A nested spring design was investigated primarily in order to shorten the assembly. However, it results in a unit weight of 0.29 pound which is a net savings of 0.36 pound per generator. This design is also shown in Fig. G-3. The use of a nested spring design shortens the assembly by 1-1/8 inches, and will therefore be incorporated in the RTG structure design.

C. DOOR OPENING SPRINGS

Two spring cartridge assemblies were originally included in the structure to insure that the two hinged sides of the housing spring open to allow the fuel block to separate.

A torsion spring, mounted to the hinge, was investigated and proved feasible. This results in a cleaner appearance to the structure, as well as fewer parts, and will therefore be incorporated in the housing design.

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FIG. G-3. SHUTTER EJECTION SPRING

APPENDIX H

FIREBALL CLEARANCE STUDY

The purpose of this study is to determine the feasibility and weight penalties involved in ejecting the SNAP 29 fuel block from the vehicle in the event of a major launch pad catastrophe. This requires that the fuel block be ejected with sufficient velocity to clear the radius of a fireball prior to first ground impact.

Since the study is general in nature, it is based upon the four-, six- and eight-module size RTG fuel blocks. In addition, it is based upon clearing three different fireball radii: 100 feet, 350 feet and 650 feet.

For purposes of this study, the RTG is assumed to be installed in a vehicle as shown in Fig. H-1. The geometry is based upon a Titan III vehicle. Three ejection concepts are evaluated in this study as follows:

- (1) Horizontal ejection in the existing plane of ejection. This requires the least modification to the RTG housing structure, since it requires only a size increase in the existing ejection devices.
- (2) Angular ejection in the existing plane of ejection. This is similar to (1) above, except that the fuel block is ejected at an optimum angle from the horizontal. This results in a minimum energy requirement for a given range and mass. It does, however, require more modification to the RTG structure than does horizontal ejection.
- (3) Side ejection. In this concept the fuel block is ejected horizontally in the direction of loading. This would result in a complete redesign of the RTG structure and may affect the loading concept.

In all cases, these ejection devices would also be used for normal ejection prior to, or during, the re-entry phase.

Horizontal ejection of the fuel block utilizing the present separation concept is not feasible for either the 350- or 650-foot radius fireball because of the excessively long ejector strokes required to limit the acceleration within the structural capability of the fuel block. The approximate weights for pneumatic and pyrotechnic ejectors to clear a 100-foot radius are shown in Table H-1. These do not include modifications required to the structure. The weights of the pyrotechnic devices were extrapolated from existing designs and are based on the use of titanium.

Ejection at an angle of 45° considerably reduces the energy requirements, and therefore requires smaller ejectors. However, even in this case, it does not appear feasible to clear the 650-foot radius with the six- or eight-module size fuel blocks, or the 350-foot radius with the 8-module size fuel block. The results of this study are summarized in Table H-2. The weights shown do not include any modifications to the structure. These would be considerable and would include a frame to provide the reaction to the vertical component of the acceleration force. In addition, the housing structure and general arrangement of the RTG must be modified somewhat to permit the actuators to penetrate through the back of the structure.

A third concept was evaluated to determine a means of reaching the 650-foot radius objective. This consists of a fly-away concept wherein the fuel block is propelled by means of rocket motors. The major problem imposed by this concept is that of stability. This eliminates the direction used in the two previous concepts

FIG. H-1. VEHICLE GEOMETRY

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and results in separation occurring in the loading plane, opposite to the direction of fuel block loading. However, even in this case, unequal burning or thrust can result in unstable flight and, for this reason, is not considered practical. In addition, it results in a complete redesign of the RTG housing structure.

It is to be emphasized that this study is concerned with the effects of high velocity separation only on the RTG. The effects upon the launch vehicle must also be considered. These include load effects on the launch vehicle created by reactions to the ejectors, and space considerations imposed by the protrusion of the ejectors into the launch vehicle. This latter consideration, for example, prevents the RTG from being mounted in the tankage area of the vehicle.

ANALYSIS

The following analyses are based on the assumption that air resistance is negligible. This results in a simpler analysis, which serves the purpose of this study. Should these concepts prove feasible, a subsequent, more detailed analysis including variables such as air resistance, etc., will be required. In addition, the analyses are based upon the following physical constants and restraints:

- (1) Mass. The mass to be ejected consists of the fuel block and the major portion of the thermal control subsystem, as well as a portion of the RTG housing. This results in the following constants for the various RTG sizes:

Four-module, 2.16 slugs

Six-module, 3.07 slugs

Eight-module, 4.09 slugs

- (2) Maximum allowable acceleration. The structural limits of the fuel block are:

Four-module, 112.9 g

Six-module, 54.87 g

Eight-module, 32.5 g

These are based on acceleration in a direction normal to the flat face of the fuel block.

1. Horizontal Ejection

This represents the simplest of the various ejection concepts, since it is based upon the existing separation scheme.

The horizontal ejection velocity required for a given fireball radius is determined by the free fall time from 101 feet: $t = \sqrt{\frac{2H}{g}}$ where g is the acceleration of gravity. This results in a time of 2.502 seconds. The horizontal velocity required for a given radius is then determined by: $v = \frac{R}{t}$. These are shown in Table H-1 and Fig. H-2 for the case $\alpha = 0$.

FIG. H-2. RANGE VERSUS INITIAL VELOCITY FOR VARIOUS EJECTION ANGLES

The minimum ejector stroke is determined by the limiting acceleration and the kinetic energy required. These result in $S = \frac{V^2}{2a}$. However, the stroke determined by this method is based on the assumption that acceleration is constant throughout the stroke. This is not true for either mechanical (springs) or expanding gas (pneumatic or pyrotechnic) type actuators. In the case of mechanical springs, the maximum acceleration is twice the average acceleration over the stroke. In the case of expanding gas type actuators, the peak acceleration is approximately three times the average acceleration. Table H-1 and Fig. H-3 present the stroke lengths for an average acceleration equal to the limit acceleration, and Fig. H-4 presents stroke limits for both mechanical and expanding gas ejectors. The results of this evaluation indicate that it is impractical to achieve 350- and 650-foot radii with either mechanical or expanding gas ejection for the four-, six- or eight-module size fuel blocks. For this reason, a weight analysis is made only for the 100-foot radius. The weight of springs only (not including shafts and modifications to structure) to eject the four-module block to clear 100 feet is approximately 90 pounds. For this reason, mechanical springs are considered unfeasible. The approximate weights for pneumatic and pyrotechnic actuation systems are shown in Table H-1. The weights shown for the pyrotechnic system are based on extrapolations of existing hardware.

2. Angular Ejection

In this concept, the fuel block is ejected from the RTG housing in the same plane as in horizontal ejection, except that it is ejected at an upward angle. An angle of 45° from the horizontal results in a minimum velocity requirement for a given range.

Neglecting air resistance, the resulting trajectory is a parabola defined by:

$$y = x (\tan \alpha) - \frac{g x^2}{2 V_0^2 \cos^2 \alpha}$$

with $\alpha = 45^\circ$, this reduces to:

$$y = x - \frac{g x^2}{4 V_0^2}$$

Figure H-2 presents a plot of range versus initial velocity, V_0 , for several ejection angles. Figure H-5 presents a trajectory plot for both 0° and 45° ejection angles for a 650-foot range.

A summary of the various pertinent characteristics, including velocity, kinetic energy, ejector strokes and approximate system weights is presented in Table H-2. The weights shown do not include modifications to the RTG housing. Pyrotechnic ejector weights are based on extrapolations of existing designs and could probably be reduced to approach those of the pneumatic ejectors upon detailed design.

One of the modifications required to the RTG housing design in order to utilize this concept would include a frame around the fuel block to react the ejector loads. In addition, the basic RTG structure would require beefing up to react the ejection loads. These were not included in the weight summary, since they require extensive detail layout and stress analysis.

FIG. H-3. AVERAGE ACCELERATION VERSUS EJECTOR STROKE--FOUR-, SIX- AND EIGHT-MODULE SIZE FUEL BLOCKS

FIG. H-4. MAXIMUM ACCELERATION VERSUS STROKE--FOUR-, SIX- AND EIGHT MODULE BLOCKS

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FIG. H-5. EJECTION TRAJECTORIES

3. Fly-Away Ejection

In this concept, two solid propellant rocket motors are utilized to "fly" the fuel block out of the housing. The plane of separation is in the loading plane, in a direction opposite to the loading direction. This represents the most stable configuration for fly-away without incorporation of an escape tower structure. However, even in this case, unequal burning and thrust of the rocket motors would result in instability. The general arrangement of this concept is shown in Fig. H-6. A weight summary is presented in Table H-3, based on extrapolations of a Hercules Powder Company M-37 solid propellant motor. These weights do not include modifications to the RTG housing, which would have to be completely redesigned to accommodate the configuration.

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FIG. H-6. GENERAL ARRANGEMENT--FLYAWAY SYSTEM

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TABLE H-1
Horizontal Ejection

Fuel Block Size	Mass (slugs)	Acceleration Limit (ft/sec ²)	Free Fall Time (sec)	Horizontal Velocity (fps)			Kinetic Energy (ft-lb)			Ejector Stroke (ft, minimum)			Ejector Stroke (expanded gas system)			Approximate Weight 100-Foot Radius	
				100-Foot Radius	350-Foot Radius	650-Foot Radius	100-Foot Radius	350-Foot Radius	650-Foot Radius	100-Foot Radius	350-Foot Radius	650-Foot Radius	100-Foot Radius	350-Foot Radius	650-Foot Radius	Pneumatic System	Pyrotechnic System
4	2.16	3630	2.502	39.93	139.8	259.5	1.72 x 10 ³	2.11 x 10 ⁴	7.25 x 10 ⁴	0.22	2.69	9.28	0.66	8.07	27.84	8.0	9.0
6	3.07	1767	2.502	39.95	139.8	259.5	2.46 x 10 ³	3.14 x 10 ⁴	10.4 x 10 ⁴	0.452	5.54	19.1	1.356	16.62	57.3	12.8	11.0
8	4.09	1048	2.502	39.95	139.8	259.5	3.27 x 10 ³	4.01 x 10 ⁴	13.81 x 10 ⁴	0.761	9.34	32.2	2.283	28.02	96.6	17.3	16.5

TABLE H-2
Angular Ejection

Fuel Block Size	Mass (slugs)	Acceleration Limit (ft/sec ²)	α (deg)	Initial Velocity (fps)			Kinetic Energy (ft-lb)			Minimum Ejector Stroke (ft) (a = const)		
				100-Foot Radius	350-Foot Radius	650-Foot Radius	100-Foot Radius	350-Foot Radius	650-Foot Radius	100-Foot Radius	350-Foot Radius	650-Foot Radius
4	2.16	3630	45	20.02	46.8	67.3	434	2370	4890	0.055	0.302	0.624
6	3.07	1767	45	20.02	46.8	67.3	618	3360	6940	0.113	0.62	1.282
8	4.09	1048	45	20.02	46.8	67.3	819	4480	9260	0.191	1.046	2.16

Fuel Block Size	Ejector Stroke (expanded gas system)			Approximate System Weights (lb)					
	100-Foot Radius	350-Foot Radius	650-Foot Radius	Pneumatic System			Pyrotechnic System		
4	0.165	0.906	1.872	4.5	7.0	12.0	5.4	10	13.4
6	0.339	1.86	3.846	4.7	9.5	--	6	10.8	--
8	0.573	3.138	6.48	6.0	--	--	8	--	--

TABLE H-3
Flyaway Ejection Summary

Fuel Block Size	Range (ft)	Impulse (lb-sec)	Motor Weight (lb)	Structure and Insulation (lb)	Total Weight (lb)
4	650	562	9.1	15.4	24.5
6	650	798	14.5	16.7	31.2
8	650	1063	18.1	17.9	36.0