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Second Quarterly Progress Report

April 28, 1967

Martin Marietta Corporation
Baltimore, Maryland

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SYSTEMS FOR NUCLEAR AUXILIARY POWER (SNAP) -
ISOTOPIC SNAP PROGRAM (M - 3679, 51st. Ed.)

SNAP 29
POWER SUPPLY SYSTEM
SECOND QUARTERLY PROGRESS REPORT

SPECIAL REVIEW
DETERMINATION
Class: U
Revisers: KAW
Date: 3-2-82

April 28, 1967

Contract AT(29-2)-2062

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FOREWORD

This is the second quarterly progress report, describing the technical status of the SNAP 29 development program. It covers the work accomplished by Martin Marietta Corporation during the period from January 1, 1967 through March 31, 1967 under Contract AT(29-2)-2062 for the U. S. Atomic Energy Commission.

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SNAP 29 Radioisotope Thermoelectric Generator

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SUMMARY

A. RADIOISOTOPE THERMOELECTRIC GENERATOR SYSTEM

The preliminary material evaluation effort was continued on the fuel block with the evaluation of carbon/graphite composites from Super-Temp Corporation and Union Carbide.

Five vendors have responded to an invitation to bid on supplying fuel block material. Their bids will be evaluated during the next quarter. Work continued on oxidation protective coatings; of the three coatings tested during this period, none completely prevented oxidation.

Thermal analysis was completed on the following: generator heater test fixture, thermoelectric module static and dynamic test fixtures.

The 2N-3P cupped couples were selected for use in the thermoelectric module design.

Work was initiated on an automatic data acquisition system for processing data from module performance tests.

The thermal control system shutter design was modified to reduce weight and heat losses.

A hydrostatic burst test was performed on the NaK reservoir (thermal control system). As a result, it appears that tubing wall thickness can be reduced, thereby effecting a weight savings.

A reduction in the meteoroid penetration probability criteria (from 0.9999 to 0.999) has resulted in a weight reduction in the radiator subsystem.

Design revisions have been made on the RTG housing structure.

B. SYSTEM EVALUATION AND INTEGRATION

The SNAP 29 Program Plan was reissued (MND-2062-500) to provide for redefinition of program objectives. The plan will be delivered to the AEC in April 1967.

A Systems Design Review Board was established to provide for a formal technology control program.

The first documentary film was completed during this report period.

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C. RELIABILITY

The reliability goal, previously established at a value of 0.993 for operation of the RTG for a 144-day period, has been revised to 0.992. The revision results from a reliability/weight tradeoff study related to meteoroid armor protection for the radiator subsystem.

In accordance with the redirection of the SNAP 29 program, the individual reliability tasks and schedule associated with those tasks have been modified. Task identity has been retained to provide continuity with prior efforts.

Prior to redirection, the Reliability Test Plan (MND-2062-3233-1) was completed and submitted to AEC.

D. AEROSPACE GROUND EQUIPMENT

Preliminary RTG/FHM interface configuration layouts compatible with the present RTG configurations were completed.

A configuration layout of the compliance mechanism, using a multiple ball spline system was completed.

E. SAFETY EVALUATION AND INTEGRATION

A list of assumptions to be used in the SNAP 29 nuclear safety re-evaluation was prepared. The list included assumptions concerning the operational phases, the fuel form, the methods of safety analysis, the acceptance criteria to be used and the fuel block design.

The analysis of the shallow-water release hazard was completed using PDN techniques. Comparison of the results with preliminary acceptance criteria indicated hazard measures about three orders of magnitude below the criteria.

The hypersonic aerodynamic coefficients were computed for use in studying the six-degree-of-freedom re-entry motion of the fuel block. Three basic edge shapes were considered. Modified Newtonian theory was used in the computations; induced and viscous forces were neglected.

The preliminary wind tunnel test program was completed; three tests were performed. Two hypersonic force and moment tests were completed, one at the Martin Marietta Hot Shot Tunnel and one at the Arnold Engineering Development Center. A hypersonic aeroheating test was completed at the Hot Shot Tunnel.

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The impact test and analysis program was revised. Emphasis is now placed on conducting full-sized block tests to define the magnitude of the impact problem.

The first phase of the burial test and analysis program was completed. Capsule temperature of about 2400° F was measured when the test specimen, having a power input equal to about 75% of beginning-of-life fuel inventory, was buried one inch deep in sand. Analyses have been completed to compare with the test results.

F. MOUND LABORATORY EFFORT IN SUPPORT OF SNAP 29

Capsule development tests in support of SNAP 29 were continued by Mound Laboratory. Experimental results to date indicate that tantalum and tantalum-10% tungsten are compatible with gadolinium polonide at a nominal temperature of 1000° C for at least 150 days. The polonide moderately attacks the Haynes-25 alloy within a 50-day period.

Polonium-210 is not compatible with the three capsule materials that have been tested.

Work continued on tantalum matrix studies, and effort was initiated on columbium-752 as a possible matrix material.

A comparison of TIG versus EB welding is being made, with burst tests planned using both techniques.

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I. INTRODUCTION

SNAP 29, a radioisotope thermoelectric generator (RTG) system, is designed to produce a nominal electrical power output of 500 watts for 90 days after launch with polonium-210, at an overall weight of 500 pounds or less. The system is made up of:

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

For reporting purposes, the chapters of this report have been established to coincide closely with the Phase I Technical Report (MND-3612-7).

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

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(5) CP 1700--Heat Rejection

(6) CP 1900--Structures, Shielding and Instrumentation.

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 new program plan (MND-2062-500).

Fig. I-1. SNAP 29 Master Program Schedule

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II. RADIOISOTOPE THERMOELECTRIC GENERATOR SYSTEM (CP-1000)

The SNAP 29 RTG system design goal is the delivery of a generator of 500 pounds or less with a nominal power level of 500 watts(e) at the end of life. The design life of the system is 144 days, including 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 1700--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 (assembly qualification testing). On the basis of assembly qualification tests, further design refinements will be effected in order to optimize the system design toward the goal of 1 watt/lb. These changes will appear in the prototype system configuration, which will demonstrate the operational capability of the RTG on a system level and affirm the applicability of the system to flight hardware.

A. SUMMARY OF SIGNIFICANT ACCOMPLISHMENTS

During the first quarter of 1967, a significant amount of time was expended in revising program plans, manpower and schedules in response to revisions in program scope and funding.

Included in the accomplishments for the past quarter are:

- (1) The preliminary material evaluation effort was continued on the fuel block with the evaluation of carbon/graphite composites from Super-Temp Corporation and Union Carbide.
- (2) The electrical heat source test program was reviewed and revised in order to reduce program costs.

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- (3) A thermal analysis of the generator heater test fixture was completed.
- (4) 2N-3P cupped couples were selected for use in the thermoelectric module design.
- (5) Thermal analyses of the thermoelectric module static and dynamic test fixtures were completed.
- (6) The piston heat sink ΔT tests were initiated and results are presented herein.
- (7) Additional data have been taken from the S/N-1 research module.
- (8) The S/N-4 research module testing was completed.
- (9) Work was initiated on an automatic data acquisition system for processing data from module performance tests.
- (10) A set of thermoelectric module performance maps was generated.
- (11) The thermal control system shutter design was modified to reduce weight and heat losses.
- (12) The boilerplate shutter vibration test was completed.
- (13) A new vendor was selected for the shutter actuator.
- (14) A hydrostatic burst test was performed on the NaK reservoir (thermal control system).
- (15) The shutter emergency heat dump capability was investigated.
- (16) A hydraulic analysis of the module manifold was completed.
- (17) The bimetallic tube test was initiated in this period.
- (18) Design of the skeleton radiator was completed.
- (19) Radiator hydraulic parametric studies were initiated in this period.
- (20) A reduction in the meteoroid penetration probability criteria has resulted in a weight reduction in the radiator subsystem.
- (21) Design revisions have been made on the RTG housing structure.

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 500-watt(e) power
- (2) To be capable of intact re-entry of fuel capsules
- (3) To provide fuel containment as required to meet safety criteria.

The heat source consists of the fuel capsules and the fuel block. The capsules consist of an inner liner which contains the fuel, a strength member and an oxidation barrier or cladding to protect the strength member. The completed capsule assemblies are inserted into a fuel block which measures approximately 33 inches wide by 37 inches long and 1 inch thick.

The development program for the heat source will consist of a materials and component developmental phase (CP 1140), an assembly qualification phase (CP 1130) and a prototype phase (CP 1120). The basic philosophy in the sequential development plan is to develop materials and components which can be integrated into subsystems and then into the final, complete RTG system.

1. Summary of Significant Accomplishments

The preliminary material evaluation effort was continued with the evaluation of carbon/graphite composites from Super-Temp Corporation and Union Carbide. The Super-Temp material is a pyrolytic graphite impregnated felt. Numerous thermal and mechanical property tests have been performed on samples having a bulk density between 68 and 78 pounds per cubic foot. Structurally this material will perform satisfactorily for the fuel block material. The material furnished by Union Carbide was a fully graphitized chopped-fiber composite. Mechanically, it is inferior to Union Carbide's ATJ series of solid graphite.

Three oxidation protective coatings, Sylcor R-512, vapor-deposited boron nitride and vapor-deposited silicon carbide, have been tested on Poco, Super-Temp and CARB-I-TEX materials. Poco graphite appeared to be the most promising graphite for use with these coatings. Coatings on the Super-Temp and CARB-I-TEX materials retarded, but did not fully prevent, oxidation of the graphite.

Eight graphite carbon suppliers were invited to bid on a Phase I effort to supply the fuel block material. The competition will consist of the evaluation by Martin Marietta of supplier-furnished fuel blocks (two per supplier) and supplier-furnished test specimens. Five of these vendors responded and their quotations are now being evaluated.

2. Preliminary Materials Evaluation

a. Technology review

A thorough literature search was accomplished during this period embracing ceramics, refractory metals, graphite, carbon and platinum. Oxidation protective and thermally emissive coatings and bonding methods were covered, along with the basic materials. Approximately 600 references were collected and arranged in 26 categories. These references are being abstracted and integrated into an annotated bibliography. Data pertinent to fuel block and capsule development will thus be available in an accessible form. The report will also contain recommendations based on the literature search.

Mechanical and thermal property data currently available for heat source materials have been compiled into a material properties list. This list enables the groups involved with SNAP 29 to be assured of uniformity in use of material properties data. This information has been documented in Martin Marietta Report 1140-119.

b. Materials evaluation testing

(1) Super-Temp graphite reinforced felt. Super-Temp pyrolytic graphite reinforced felt is composed of carbon or graphite felt, vapor-phase impregnated with pyrolytic carbon. The impregnated felt is available in densities to 100 pounds per cubic foot, depending on the degree of deposition. Pyrolytic boron nitride or silicon carbide coatings can be deposited on the material during manufacture. This material is comparable to CARB-I-TEX in mechanical and thermal properties and may offer superior shear strength and coatability.

Tests have been planned for material of 80- and 100-pound-per-cubic-foot nominal density and have been completed for the 80-pound-per-cubic-foot material. The 100-pound-per-cubic-foot material has not been received because of fabrication difficulties reported by the manufacturer.

The test material was received as two blocks of nominal dimensions 8 x 10 x 0.4 inches. The blocks were not flat and the thickness was not uniform. The actual average density was 72 pounds per cubic foot for one block and 78 pounds per cubic foot for the other block. The blocks were more dense at the edges than in the middle.

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TABLE II-1

Thermal and Mechanical Tests of Super-Temp Reinforced
Pyrolytic Graphite Felt (60 to 80 lb/ft³ density range)

Test	Specimen Type and Direction	Test Temperature (°F)	Number of Specimens	Remarks
Flexure	A	76	2	Crosshead speed = 0.020 in./min Flexural strength and Young's modulus in bending (E_b) were computed
		1350	5
	B	76	3	
		1350	10	
Compression	B	1600	4	Crosshead speed = 0.020 in./min
		2000	4	Ultimate shear strength computed
Shear	C	1600	6	Crosshead speed = 0.020 in./min Ultimate shear strength computed
Thermal conductivity	B	800 to 2000	1	Measured with cut-bar apparatus
	C		2	
Thermal diffusivity	C	800 to 2200	1	Measured by flash method
Coefficient of expansion	B	76 to 2000	2	Linear expansion measured with quartz tube dilatometer
	C		1	
Specific heat (C_p)	C	800 to 2000	11	Heat content versus temperature measured with drop calorimeter
Total normal emittance	Virgin surface C	76	1	Reflectance measured at room temperature. Emittance computer for temperature range of 800° to 3600° F
	Machined surface C		1	
Density	--	--	--	Density of all above specimens was measured

NOTE: (1) An argon atmosphere was used to protect the specimens from oxidation at elevated temperatures.

TABLE II-2

Flexure Test Data for Super-Temp Reinforced Pyrolytic Graphite Felt

Test Tem- perature (°F)	Direction ⁽¹⁾	Density (lb/ft ³)	Flexural Strength	Modulus of Elasticity (psi x 10 ⁻⁶)	Averaged Properties		
					Density (lb/ft ³)	Flexural Strength (psi)	E (psi x 10 ⁻⁶)
Block No. 1							
1350	B	70.26	6000	0.98	69.57	6044	1.03
1350	B	69.63	5810	0.99			
1350	B	69.27	5630	1.04			
1350	B	68.65	5870	1.04			
1350	B	70.05	6910	1.10			
76	B	69.91	5000	1.12	70.45	4855	1.13
76	B	70.98	4610	1.14			
1350	A	76.67	6840	1.29	75.29	7586	1.37
1350	A	76.63	7060	1.35			
1350	A	76.40	8400	1.48			
1350	A	75.73	7370	1.45			
1350	A	71.01	8260	1.26			
76	A	75.59	7350	1.33	76.00	6940	1.35
76	A	76.41	6530	1.36			
Block No. 2							
1350	B	77.71	7470	1.23	77.98	7044	1.25
1350	B	78.26	7220	1.28			
1350	B	78.26	6330	1.24			
1350	B	78.02	6410	1.19			
1350	B	77.65	7790	1.29			
76	B	78.02	5920	1.28	78.02	5920	1.28

NOTE: (1) Specimen orientation

(2) Bulk density from measured weight and volume

(3) Computed from beam flexure formula,
or = Mc/I ; M = maximum moment(4) Computed from beam deflection formula,
 $y = PL^3/48EI$

(5) Three point loading

(6) Nominal specimen dimensions: 4 in.
x 0.35 in. x 0.35 in.

The tests performed are enumerated in Table II-1. Results of these tests are given in Tables II-2 and II-3 and Figs. II-1 to II-5. Super-Temp reinforced felt in the density tested appears adequate structurally for fuel block requirements. Its thermal properties are suitable except for thermal conductivity, which is lower than is desirable.

TABLE II-3

Compression Test Data for Super-Temp Reinforced
Pyrolytic Graphite Felt (average density = 76 lb/ft³)

Test Temperature (°F)	Direction (1)	Compression Strength (psi)	Average Compression Strength (psi)
1600		10,050	13,530
1600		16,220	
1600		15,510	
1600		12,340	
2000		15,600	15,347
2000		16,670	
2000		13,770	
2400		*	
	B	*	*

NOTE:

(1) Specimen orientation

(2) Nominal specimen dimensions: 0.375 in. dia x 0.75 in. long. Specimens cut from Block No. 1.

*Bearing plate failed.

(2) Union Carbide PT-0178. PT-0178 is a carbonized composite manufactured by Union Carbide Corporation, having strength properties superior to those of the PT-0154 grade reported on in the last quarterly report.

This material has been tested for the following properties: flexural strength and modulus of elasticity at 76° F and at 1350° F; thermal conductivity and linear thermal expansion as functions of temperature. Results of the flexure tests are presented in Table II-4. The flexural strength of this material is low compared to ATJ and to other composite materials under consideration.

Fig. 11-1. Thermal Expansion of Super-Temp Reinforced Pyrolytic Graphite Felt Versus Temperature

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- B-direction; $\rho = 73.7 \text{ lb/ft}^3$
- C-direction; $\rho = 64.3 \text{ lb/ft}^3$

Specimen for cut-bar thermal conductivity measurement

Fig. II-2. Thermal Conductivity of Super-Temp Reinforced Pyrolytic Graphite Felt Versus Temperature

Fig. II-3. Heat Content (corrected) of Super-Temp Reinforced Pyrolytic Graphite Felt, 75.9 lb/ft³ Average Density

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Fig. II-4. Room Temperature Spectral Reflectance--Super-Temp Reinforced Pyrolytic Graphite Felt, as Received Surface, Looking at A-B Plane, $\rho_{\text{ave}} = 73 \text{ lb/ft}^3$

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Fig. 11-5. Room Temperature Spectral Reflectance--Super-Temp Reinforced Pyrolytic Infiltrated Graphite Felt, Smooth Ground, Looking at A-B Plane, $\rho_{\text{ave}} = 73 \text{ lb/ft}^3$

TABLE II-4

Flexure Test Data for Union Carbide PT-0178 Graphite
(and ATJ graphite for comparison)

Test Temperature (°F)	Direction ⁽²⁾	Density ⁽²⁾ (lb/ft ³)	Flexural Strength ⁽³⁾ (psi)	E ⁽⁴⁾ (psi x 10 ⁻⁶)	Averaged Properties		
					Density ⁽²⁾ (lb/ft ³)	Flexural Strength ⁽³⁾ (psi)	E ⁽⁴⁾ (psi x 10 ⁶)
76	X	78.77	2870	1.03	78.77	2870	1.03
1350	PT-0178	79.83	3580	1.15	79.56	3392	1.04
1350		80.20	3340	1.04			
1350		79.08	3270	1.03			
1350		79.10	3500	1.00			
1350		79.61	3270	0.96			
1350	ATJ	With grain	109.75	1.01	110.38	5370	1.06
1350		With grain	110.26	1.03			
1350		With grain	111.13	1.15			

NOTE:

- (1) Specimen orientation for PT-0178 molding force is in Z direction
- (2) Bulk density from measured weight and volume
- (3) Computed from beam flexure formula, or = Mc/I , M = maximum moment
- (4) Computed from beam deflection formula, $y = PL^3/48EI$
- (5) Three point loading
- (6) Nominal specimen dimensions: 4 in. x 0.38 in. x 0.38 in.

c. Oxidation coatings for graphite

Three oxidation protective coatings, Sylcor R-512, vapor-deposited boron nitride and vapor-deposited silicon carbide, have been screened in dry air at 1400° F, on three types of substrate; Poco graphite, CARB-I-TEX 500 and Super-Temp PG-felt. Results of these screening tests are presented in Tables II-5 and II-6 and Figs. II-6 to II-8. The Sylcor R-512 and boron nitride coating were most effective on Poco graphite. Boron nitride on Super-Temp was only partially effective in reducing oxidation rates. None of the coatings tested was effective in reducing the oxidation of CARB-I-TEX to acceptable levels.

Great Lakes Carbon Corporation submitted samples of their HLM grade graphite substrate which was treated to retard oxidation. Test specimens oxidized completely, leaving only a small residue after 63 hours in air at 1400° F.

d. Graphite bonding

Three graphite bonding cements, C-9 and C-34, manufactured by Union Carbide Corporation, and P-514, manufactured by Great Lakes Carbon Corporation, were used to fabricate shear and tension specimens from ATJ and Poco graphite. The bonded specimens were cured in accordance with the manufacturers' specifications and were tested at 76° F. The results of these tests are shown in Table II-7. None of the cements gave satisfactory results with Poco graphite, but all were successful with ATJ graphite. Isolated results with Poco graphite indicate that refinement of the bonding technique could also produce good results with this material.

3. Preliminary Fuel Block Development

The selection of the fuel block material and its supplier will be based upon preliminary competition (Phase I) between two or three suppliers. The competition will consist of the evaluation by Martin Marietta of supplier-furnished fuel blocks (two per supplier) and supplier-furnished test specimens (approximately 140 per supplier).

The potential suppliers listed below were contacted and invited to bid on the Phase I effort in accordance with MN-10295-1, dated February 10, 1967, "Preliminary Specification for SNAP 29 Fuel Block." All of those contacted except Poco Graphite Inc. attended briefings during the weeks of February 12th and 19th:

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TABLE II-5
Oxidation Test Results for Graphites Coated with Sylcor R -512

<u>Sample</u>	<u>Sample Weight (gm)</u>	<u>Break Temperature (°C)</u>	<u>Surface Area (in. ²)</u>	<u>Weight Loss (mg)</u>	<u>% Loss</u>	<u>Time for Loss (min)</u>	<u>% Loss/Minute</u>	<u>Loss/Minute (mg)</u>	<u>Loss/in. ² (mg)</u>	<u>Loss/in. ²/Minute (mg)</u>
Poco										
No. 1	0.89602	--	0.421	None	--	--	--	--	--	--
No. 2	0.88404	--	0.419	40	4.5	252	0.0178	0.159	95	0.377
CARB-I-TEX										
No. 1	0.76351	700	0.452	15	2	54	0.037	0.28	35.3	0.658
No. 2	0.73848	730	0.423	42.5	5.8	360	0.016	0.118	101	0.281
No. 3	0.68851	720	0.382	--	--	--	--	--	--	--

NOTE:

- (1) Appearance of sample remained unchanged after test.
- (2) Three of the above samples exhibited a 2.5 mg weight gain while going to temperature (above weight loss is net over test time).

TABLE II-6
Oxidation Test Results for Graphites Coated with Vapor-Deposited
Boron Nitride

Sample and No.	Sample Weight (gm)	Break Temperature (°C)	Surface Area (in. ²)	Weight Loss (mg)	Loss (%)	Time for Loss (min)	Loss/ Minute (%)	Loss/Minute (mg)	Loss/in. (mg)	Loss/in. ² / Minute (mg)
Super-Temp										
No. 1	0.75511	650	0.412	37.5	5	122	0.04	0.3	91	0.75
No. 2	0.76551	650	0.414	40	5.23	118	0.044	0.34	96.6	0.81
No. 3	0.75996	550	0.413	50	6.6	136	0.05	0.368	121	0.90
No. 4	0.59586	620	0.414	12.5	2.1	111	0.017	0.1	30.2	0.245
No. 5	0.58710	620	0.414	11	1.87	113	0.017	0.097	26.6	0.235
No. 6	0.77030	--	0.415	30	3.9	100	0.039	0.3	72	0.72
CARB-I-TEX										
No. 1	0.63180	670	0.412	100	16	66	0.24	1.52	242	3.67
No. 2	0.60521	650	0.397	100	16.5	70	0.236	1.43	252	3.6
No. 3	0.62270	660	0.409	100	16	65	0.246	1.54	245	3.77
No. 4	0.63411	570	0.423	100	15.8	84	0.188	1.19	237	2.82
No. 5	0.67140	630	0.436	100	14.9	75	0.2	1.33	237	3.16
No. 6	0.61858	600	0.404	100	16.2	80	0.2025	1.25	248	3.1
Poco										
No. 1	0.85349	--	0.428	None ⁴	--	--	--	--	--	--
No. 2	0.86048	760	0.429	5	0.58	100	0.0058	0.05	12	0.12
No. 3	0.85008	--	0.429	None ⁴	--	--	--	--	--	--
No. 4	0.86119	760	0.433	7.5	0.87	100	0.0087	0.075	17	0.17
No. 5	0.88030	--	0.439	None ⁴	--	--	--	--	--	--
No. 6	0.82470	--	0.431	None ⁴	--	--	--	--	--	--

NOTE:

- (1) Coating on CARB-I-TEX samples was crazed after testing
- (2) Coating on Super-Temp samples indicated peeling after testing
- (3) Seven of the above samples exhibited as much as 3 mg weight gain while going to temperature (above weight loss is net over test time)
- (4) Test time at temperature = 100 minutes

Fig. II-6. Isothermal Mass Loss in Air at 1400° F--Boron Nitride-Coated Super-Temp Felt

Fig. II-7. Isothermal Mass Loss in Air at 1400° F--Coated CARB-I-TEX 500

Fig. II-8. Isothermal Mass Loss in Air at 1400° F--Coated Poco Graphite

TABLE II-7

Room Temperature Shear and Tension Test Data
for Bonded ATJ and Poco-AXF Graphite

<u>Bond Material</u>	<u>Specimen Material</u>	<u>Ultimate Stress (psi)</u>	<u>Remarks</u>
Shear tests:			
C-9	ATJ	905	Graphite failure
C-9		973	Graphite failure
C-9		902	Graphite failure
C-34		1142	Bond failure
C-34		981	Bond and graphite failure
C-34		780	Graphite failure
P-514		1007	Bond and graphite failure
P-514		1049	Graphite failure
P-514	ATJ	770	Graphite failure
C-9	Poco AXF	--	Failed during bond and cure
C-9		--	Failed during bond and cure
C-9		--	Failed during bond and cure
C-34		671	Bond failure
C-34		460	Bond failure
C-34		--	Damaged prior to test
P-514		547	Bond failure

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TABLE II-7 (continued)

<u>Bond Material</u>	<u>Specimen Material</u>	<u>Ultimate Stress (psi)</u>	<u>Remarks</u>
P-514	↓	1155	Bond failure
P-514	Poco-AXF	379	Bond failure
Tension tests:			
C-9	ATJ	1082	Bond failure
C-34	↓	814	Bond failure
P-514	ATJ	837	Bond failure
C-9	Poco-AXF	1848	Bond failure
C-34	↓	304	Bond failure
P-514	Poco-AXF	1063	Bond failure

Notes:

- (1) Manufacturers' identification for carbon bonding materials. Bonds cured in accordance with manufacturer recommendations.

Serial No.	Manufacturer
C-9	Union Carbide
C-34	Union Carbide
P-514	Great Lakes Carbon

(2) Specimen configuration

- (1) 3M Company
2501 Hudson Road
Building 551
St. Paul, Minnesota 55119
- (2) Super-Temp Corporation
11120 S. Norwalk Boulevard
Sante Fe Springs, California 90670
- (3) Carborundum Company
Graphite Products Division
Blank and Walmore Rds
Sanborn, New York 14132
- (4) LTV Aerospace Corporation
Astronautics Division
P. O. Box 6267
Dallas, Texas 75222
- (5) Union Carbide Corporation
Carbide Products Division
270 Park Ave
New York, New York 10017
- (6) HITCO, Incorporated
1600 W. 135th St
Gardina, California 90249
- (7) Great Lakes Carbon Corporation
Graphite Products Division
579 Fifth Avenue
New York, New York 10017
- (8) Poco Graphite Incorporated
P. O. Box 1524
Garland, Texas 75041

Proposals in response to the request for quotation were received from the first five listed suppliers and these are currently being evaluated. The last three suppliers have declined to bid.

4. Capsule Cladding Development

- a. Preliminary evaluation of compatibility of noble metals with soil burial at high temperature

An oven test was conducted to determine the compatibility of pure platinum foil (0.001 inch thick) with ordinary construction sand in the presence of a proposed fuel block material, Poco AXF graphite. As a control, a segment of the platinum foil was placed between two Poco AXF graphite blocks and heated with the platinum-sand-graphite combination.

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The oven was preheated to 2660° F, and the specimens (at room temperature) were then inserted into the oven. Following an initial drop of oven temperature to 2620° F, the oven restabilized at approximately 2680° F 14 minutes after specimen insertion. The specimens were removed from the oven 50 minutes after insertion and allowed to cool undisturbed to room temperature prior to inspection.

As Fig. II-9 indicates, the sand-graphite-platinum combination caused severe corrosion of the platinum, while the platinum-graphite combination did not visibly affect the platinum. This result was not unexpected, since it is well known that platinum is extremely susceptible to contamination in a reducing environment. Experience has shown platinum-rhodium alloys to exhibit the same behavior in a similar environment. Pt-6% Rh/Pt-30% Rh thermocouples have been noted to corrode and melt within a few minutes while measuring temperatures above 2600° F in elastomeric silicon ablaters such as ESA 3560 HF, which contains high percentage of both carbon and silicon. Spectrographic analysis was performed on segments of a Pt-Rh thermocouple which had melted at 2600° F, and showed much higher concentration of silicon (1 to 10% rough approximation) than the normal traces.

Since Pt-Rh alloys are solid solutions, the resistance to contamination in reducing atmospheres would seem to be only as efficient as the least resistant constituent of the alloy. As noted in Ref. II-1*, "The binary systems of a platinum metal with one of the nonmetals boron, silicon or phosphorus (Me-X in which Me = Ru, Rh, Pd, Os, Ir, Pt; X = B, Si, P) are characterized by a comparatively low eutectic temperature which decreases with increasing atomic number of the metal within each transition period." The following chart lists the eutectic temperatures of the various Me-X combinations (from Ref. II-2). **

	<u>B</u>	<u>Si</u>	<u>P</u>
Ru	-	1488° C	1425° C
Rh	-	1389° C	1300° C
Pd	Low	800° C	807° C
Os	-	--	--
Ir	-	--	---
Pt	-	830° C	588° C

*1. Arunsson, B., Rundquist, S., "Borides, Silicides, and Phosphides of Platinum Metals," Platinum Metals Review, Vol. 5, No. 3, July 1961.

**2. Hansen, M., Constitution of Binary Alloys, McGraw-Hill Book Co., Inc., 1958.

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Fig. II-9. Platinum Foil--0.001 in. Thick--(a) In Sand and Graphite (Poco AXF), (b) Sandwiched Between Graphite--Oven Temperature 2680° F, 50 Minutes

A literature survey is being continued in an effort to complete the above chart. With the present minimal information, it appears that Pt is not compatible with soils at temperatures above 2500° F, in the presence of all the nonmetals B, Si, P and many more, including carbon.

b. Capsule emissivity coating

Tests of emissivity coatings are in progress. Molybdenum disilicide and hafnium-27% tantalum coatings are being applied. The total emittance of an iridium black coating on aluminum (furnished by J. Bishop and Company) has been determined to be 0.6 at 76° F and 0.8 at 2500° F. It is planned to evaluate this coating on Pt-20% Rh.

5. Capsule Closure Weld Development

Fuel capsule weld development effort carried out at Martin Marietta consisted of the fabrication of Ta-10W weld samples, during which parameters for EB and TIG welding processes were established simultaneously with Mound Laboratory. These samples were destructively tested at Martin Marietta as the parameters were evolved, until satisfactory sets were established. Using these parameters, another set of samples was fabricated and then forwarded to Mound Laboratory, where they will be machined into tensile and bend specimens for subsequent tests by Mound Laboratory.

A design improvement to the tungsten electrode was also made which will permit a greater repetition of weld starts, while maintaining the quality of the weld.

In accordance with the fuel capsule weld development plan, Martin Marietta-designed weld fixtures of copper were fabricated in duplicate, one set of which was sent to Mound Laboratory for their work, thus establishing common working conditions.

In the TIG welding development, it was found that gas purity was extremely critical, particularly with respect to oxygen and nitrogen, maximum oxygen and nitrogen content being 40 PPM to avoid interstitial contamination. During actual welding of the samples, the average content of nitrogen was approximately 12 ppm, oxygen 40 ppm, and interstitial contamination was eliminated.

The development of parameters proceeded by TIG and EB welding flat plates 6 inches long x 2 inches wide x 0.020 inch thick to a center piece as shown in Fig. II-10. Two such plates were butt welded to a center piece 0.060 inch thick with 0.020-inch thick steps. The weld was made by overlapping the step and performing an overlap butt weld, as shown in Fig. II-11.

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Fig. II-10. Weld Detail Configuration

Fig. II-11. Welded Assembly

These specimens were then sectioned at each end and the center to provide metallographic data on the beginning, end and central part of each weld. Examinations at 50X determined the penetration, weld configuration and grain structure of the weld and adjacent base material. These photographs are shown in Fig. II-12 (EB welds) and Fig. II-13 (TIG welds). Parameters were determined by evaluating grain growth and depth of penetration in addition to the presence of obvious defects, such as cracks and insufficient penetration.

A tungsten electrode point configuration evaluation was carried on simultaneously to measure the effect that arc initiation/point shape has on weld repeatability and quality. A configuration was determined which repeated the arc and quality eight times more than the original typical flat tip point. These designs are shown in Fig. II-14. This improvement stems from the elimination of erosion factors in the tip area.

The next phase of Ta-10W welding development consisted of comparing welding parameters and samples established concurrently by Martin Marietta and Mound Laboratory. The photomicrographs and parameters were examined and discussed at a Mound-Martin Marietta meeting, and a set of parameters were established for both types of welds. These parameters are shown in Fig. II-15.

Using these parameters, Martin Marietta and Mound fabricated a set of samples from which Mound would make and test tensile and bend test specimens. These tests would provide a general evaluation of both weld types.

The follow-on-tube weld parameter and burst test program is intended to establish parameters for TIG and EB welding of liner end caps and to test these welds under burst test conditions.

6. Design

The capsule weld development effort was continued, the objective being to establish the parameters to be used by Martin Marietta and Mound Laboratory in welding capsule subassemblies to be used in the SNAP 29 impact program and, ultimately, for the isotopic heat sources. A test plan for evaluation of liner closure design was prepared. Designs for five different style end caps (Dwg 466A1141016) are being considered. It is planned to manufacture burst test capsules of each design. Upon completion of the burst test, each of the capsule cap designs will be evaluated and a final selection of cap design and weld method will be selected. It is anticipated that the cap design will be established within the next report period. The above burst test plan was formulated and reviewed with Mound Laboratory personnel to ensure compatibility with their welding and encapsulation techniques.

Fig. II-12. Specimen Sections Showing Details of EB Welds

Fig. II-13. Specimen Section Showing Details of TIG Welds

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Fig. II-14. TIG Welding Electrode Configuration

Tungsten Inert Gas

	Mound	Martin Marietta
Weld current	160 amperes	160 amperes
Shield gas flow	Argon, 15 cfh	Argon 8 to 10 cfh
Rate of travel of work piece	9 in. /min	9 in. /min
Arc gap	0.045 inch	0.045 inch
Distance of hold-down to joint	0.100 inch	0.100 inch
Position of electrode	0.010 to 0.015 inch off center of piece	0.010 to 0.015 inch off center of piece
Tungsten configuration

Electron Beam

Voltage	110 kv	120 kv
Current	4.0 ma	5 ma
Rate of travel of work piece	20 in. /min	14 in. /min
A-c deflection	0.020 inch approx	0.020 inch approx
Focal length	6 inches	1.5 inches
Distance of hold-down to joint	0.070 inch	0.061 inch

Fig. 11-15. Weld Parameters

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TABLE II-8
Fuel Block Materials Investigated

<u>Case</u>	<u>Material</u>	<u>Thermal Conductivity (Btu/hr-ft-°F)</u>
1	Poco graphite, solid	21
2	Poco graphite, lightening holes	21
3	CARB-I-TEX graphite, solid	3 against grain 3 with grain
4	CARB-I-TEX graphite, solid	3 against grain 12 with grain
5	CARB-I-TEX graphite, lightening holes	3 against grain 3 with grain
6	CARB-I-TEX graphite, lightening holes	3 against grain 12 with grain
7	Columbium honeycomb	33

NOTE:

- (1) Grain direction is along surface of fuel block. Against the grain is in thickness direction.

A fuel block section (Dwg 466A1141014) has been designed for preliminary dynamic structural evaluation. This block will be used in a dynamic test to determine load magnification over the vibration spectrum and, particularly, magnification at resonance frequency. To expedite initiation of the test and to obtain early data on dynamic response, the test block will be made from ATJ graphite. It is planned to release for manufacture simulated capsules to be used in conjunction with the dynamic test block dwg, 466A1141014, during the preliminary dynamic test.

7. Operational Thermal Analysis

a. Parametric operational analysis

The primary purpose of this task was to investigate several prospective fuel block materials and to determine their capabilities for this application. The materials investigated are listed in Table II-8 together with their respective thermal conductivity values. This task was continued from the previous reporting period.

Each material was analyzed parametrically, using the mathematical model discussed in the previous quarterly report (MND 2062-12-6-II, pages VII-15 to -31). During this period, the analysis was extended to study the effects of fuel block thickness and fuel block-to-hot-plate gap size. This analysis was based on the configuration of Case 3, solid CARB-I-TEX (k = 3 with and against the grain). Table II-9 shows how the fuel block thickness affects the fuel block and hot plate temperature gradients. An increase in this dimension results in a significant decrease of both gradients. The effect of the gap size between the fuel block and hot plate is shown in Table II-10. The fuel block gradient is insensitive to this parameter, while the hot plate temperature gradient is seen to be more sensitive to changes in this parameter (gap size), than to changes in the fuel block thickness.

TABLE II-9

Effect of Fuel Block Thickness on Temperature Gradients

Fuel block thickness (in.)	0.696	1.00	1.50
Transverse temperature gradient (°F)			
Fuel block	240	165	100
Hot plate	28	20	13

Hot plate-to-fuel block gap = 0.625 inch

TABLE II-10

Gap Size Effect

Gap size--fuel block-to-hot plate (in.)	0.50	0.625	1.25
Transverse temperature gradient (°F)			
Fuel block	240	240	240
Hot plate	43	28	8

Fuel block thickness = 0.696 inch

b. Water burial analysis

An analysis of the capsule temperature in a water environment was performed. Surface temperatures of the fuel capsule (submerged and resting on the bottom of a body of water) were found to be less than 25° F above the saturation temperature of the water. Under the most adverse water burial conditions, this analysis revealed that the surface heat flux of the capsule was less than the maximum heat flux required for nucleate boiling; therefore, the insulating effect of film boiling and the associated elevated surface temperature will not occur. These results are independent of capsule configuration changes and are relatively insensitive to changes in the thermal inventory of the fuel.

8. Re-entry Thermal Analysis

a. Analytical techniques

A concentrated effort has been made to add versatility and refinements to the analytical models being used for re-entry thermal analysis. This has resulted in switching from the TAP III program to the Martin Marietta FB 127 program. Completion of this work will permit a more accurate prediction of temperature histories during re-entry than has hitherto been obtained. Modifications were made to the FB 127 program which simplified the input necessary for anisotropic type problems. These changes have been made and checked out.

The following standard analytical models, shown collectively in Fig. II-16, are being established on the FB 127 computer program for analysis of the fuel block and capsules. As can be seen, three of these models are derivatives of a typical corner segment of the fuel block.

All dimensions in inches

Model A (3-D)

Model B (3-D)

Model CI (3-D)

Model CII (sliding 2-D model)

Fig. II-16. Schematic of Model Development

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The standard model, designated as Model A, is isometrically shown in Fig. II-17. Details regarding the elemental breakup are shown in Fig. II-18.

Models B, C-I and C-II each gives more localized detail than its predecessor, as schematically shown in Fig. II-16. It is intended that these models will be used only when it is desired to obtain increased accuracy over that of Model A. In these cases, the results calculated by the preceding model will be used as boundary conditions. Model B and C-I are incomplete. Model C-II is shown in Fig. II-19.

b. Re-entry analysis

Maximum edge and block (location directly above capsule end) heating rate histories for initial entry angle design cases of $\gamma = -0.1^\circ$ and -3° are presented in Figs. II-20 and II-21. Both tumble and broadside re-entry modes have been used, with ballistic coefficients of 11 and 5.14, respectively. A nominal full-sized block with a design factor of 1.25 applied on all aerodynamic heating rates has been used. The heating distribution applied for tumble analysis was obtained by using the tumble distribution back from the leading edge together with an overlay of the broadside distribution perpendicular to the leading edge. The heat from the fuel source has been taken as 25,400 watts (BOL value).

Analysis has also been made using a CARB-I-TEX block and re-entry angle of $\gamma = -0.1^\circ$ for both the tumble and broadside cases. These temperature histories are presented in Figs. II-22 and II-23. It is quite obvious that the broadside mode is less severe in regard to capsule temperatures. Surface thermal gradients at the time of peak temperature for both modes are shown in Figs. II-24 and II-25. A thermal gradient through the thickness of the fuel block for the broadside mode is shown in Fig. II-26.

For reference, the results obtained using the outdated longer-edge widths are shown in Fig. II-27, while results based on the present dimensions are shown in Fig. II-28. These results were obtained using Poco material properties and tumbling re-entry from an initial entry angle of $\gamma = -0.1^\circ$.

c. Analysis of oxidation effects on re-entry temperatures

The TAP III and FB 127 analysis programs used thus far do not account for the thermal chemical effect at the externally heated surfaces. Since the carbon/graphite fuel block materials at elevated temperatures ($T > 900^\circ \text{F}$) in the presence of air will oxidize, these thermo-chemical effects should be evaluated. The temperature field within the fuel block will be perturbed due to oxidation in the following ways:

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Fig. II-17. Thermal Model A (isometric view)

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Fig. 11-18. Thermal Model A Elemental Breakup

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Fig. II-19. Thermal Model CII (2-D)

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Fig. II-20. SNAP 29 Fuel Block Heat Rate History for Initial Entry
Angle of -0.1° at 400,000 ft

Fig. II-21. SNAP 29 Fuel Block Heat Rate History for Initial Entry
Angle of -3° at 400,000 ft

Fig. II-22. Peak Surface and Capsule Temperature Histories

Fig. II-23. Peak Surface and Capsule Temperature Histories

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Material	CARB-I-TEX
Model	A
Initial angle	$\gamma = -0.1^\circ$
Ballistic coefficient	11 (tumbling)
Block thickness	1 inch
Edge dimension	2.14 x 3.06 inches
Cladding emissivity	0.85

Fig. II-24. Profile of Peak Surface Temperatures

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Fig. 11-25. Profile of Peak Surface Temperature

Fig. II-26. Peak Internal (edge) Temperature Gradient

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Fig. II-27. Peak Surface and Capsule Temperature Histories

Fig. II-28. Peak Surface and Capsule Temperature Histories

- (1) Vapor products being released into the boundary layer effectively block the convective heat input during re-entry.
- (2) As surface material is reduced by oxidation (combustion), an additional heat input is realized.
- (3) The outer surface will recede.

As a check of these effects and to support our selection of FB 127 as a design tool, analysis is being performed using the T-CAP-IV program, which does simulate these thermal phenomena, but is limited in capacity.

(1) Analytical models

The analytical models generated on both programs are 2-D segments as shown in Figs. II-29 and II-30. These models are geometrically congruent both in a physical and an analytical sense. Analysis has been made using ATJ graphite because of its well established thermal properties. A 4.359 inch edge distance (measured from capsule centerline) has been used.

All cut boundaries have been treated as adiabatic walls. The surface boundary conditions for the T-CAP-IV model include convection, re-radiation, combustion and blockage. The program also has an element shrinking routine to account for surface recession. The surface boundary conditions for the FB 127 model considers heat convection and re-radiation. A surface emissivity value of 0.85 has been used, and fuel heat is based on a 25,400-watt (BOL) value.

(2) Re-entry analysis

Analysis results at this time are available for an initial re-entry angle of $\gamma = -0.1^\circ$ with tumbling about an axis perpendicular to the capsules. Temperature histories are shown in Figs. II-31 and II-32 for the edge location and capsule location programs, respectively. Capsule temperatures predicted by the FB 127 program are seen to be higher. However, insufficient data are available at this time to draw any conclusions on the inherent conservatism in FB 127 results.

Figure II-33 depicts the order of magnitude recession values expected during the thermal phase of re-entry. They should not be construed to be the worst case, however, as all cases have not been evaluated as yet.

Fig. II-29. Oxidation Recession Thermal Model

Fig. II-30. Oxidation Recession Thermal Model Elemental Breakup

Fig. II-31. Effect of Oxidation Surface Recession on Peak Surface Temperature

Fig. II-32. Effect of Oxidation Surface Recession on Capsule Temperature

f. Material sensitivity study

Parametric studies are being made of the various design variables in order that design data can be obtained that will expedite the evaluation of design changes.

A three-dimensional TAP III model (one inch thick) has been used as the nominal case in all analysis thus far. The nominal re-entry mode used is tumbling about an axis perpendicular to the capsule centerline from an initial entry angle of $\gamma = -3^\circ$.

To date, only preliminary results have been obtained on heat capacity and thermal conductivity. The range of variations which have been considered are shown in Figs. II-34 and II-35. Parametric results are presented in Figs. II-36 and II-37 for two locations on the fuel block.

9. Structural Analysis and Test

a. Natural frequencies, stresses and deflections normal to the block during launch

A corporation-written digital program, Number SB-038, "A Digital Program for Static and Dynamic Analysis of Complex Structures," commonly called the Kaufman-Hall program, was used to determine stresses, deflections and natural frequencies of a carbon fuel block. This program uses the force method of solution for redundant structures, and makes extensive use of matrix algebra.

Since the block and the loading normal to its surface are both symmetrical, one-quarter of the block was used; and this, in turn, was represented by 98 grid points (14 x 7) connected by bending members and twist panels.

Since block damping is still unknown, the dynamic capability of the machine program was not used (except for determining undamped natural frequencies and mode shapes). Instead, the dynamic load was represented by a uniform load factor of 35 g over the surface of the block.

Since block support elasticity was unknown, all support points were assumed simple, i. e., rigid with respect to forces in the normal direction, but not able to resist forces in the plane of the plate or moments in any direction. Support configurations evaluated were:

- (1) Four-corner support
- (2) Long-edge support

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Fig. II-33. Total Oxidation Surface Recession Back from Edge at $t = 1297$ Seconds

Fig. II-34. Thermal Properties of Various Graphite Materials Compared with Variations Taken on Nominal Graphite Values

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Fig. 11-35. Variations Being Considered in Thermal Capacity

Fig. 11-36. Peak Edge Temperature Sensitivity to Material Thermal Property Variations

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- (3) Long edges supported by flexible beams, each of which is supported at two points
- (4) Six-point support (four corners and the centers of the long edges).

The following structural characteristics were assumed.

- (1) Block dimensions (inches): 36.87 x 32.96 x 1.00
- (2) Dynamic loading: 35 g
- (3) Poisson's ratio: 0.2
- (4) Capsule stiffness (EI): 140,000

Results of the analyses are shown in Table II-11. $\sigma_{\max}$ and $\tau_{\max}$ are the maximum tensile and shear stresses (respectively) in the plane of the plate. $\sigma_{\max}$ and $\tau_{\max}$ at each grid point result from Mohr's circle, which is determined from the tensile and shear stresses parallel to the X and Y axes (outputs from the machine analyses). The locations of these maximum stresses can be determined from Fig. II-38. FS is the factor of safety; FS = allowable stress/actual stress. S_{yz} is the maximum shear stress due to bending and f_1 is the undamped natural frequency of the block in its first mode of vibration. Only the deflection for the CARB-I-TEX block was determined. In this case, $\sigma_{\max} = 0.167$ inch.

If the preliminary static load factors are valid, there is no flexural stress problem except in the case of the four-corner support, for which materials such as ATJ are too weak. Most other material-support combinations are satisfactory, depending on the factor of safety required.

The first mode natural frequencies are at or above the minimum frequencies required (40 cps), except in the case of four-corner support. These frequencies are based on the vendor-published values for Young's modulus. Since the actual values for these proposed materials might be as low as 0.8×10^6 , it is probable that many of these materials will not satisfy the specified minimum.

The static deflection was not computed in the first four cases of Table II-11, as it was not suspected that deflections would be very large. However, the extremely low elastic modulus of these materials allows deflections which are quite large. The last case in Table II-11 shows a static deflection of about 1/6 inch for CARB-I-TEX 500 due to

$T_{\text{nominal}} = 2241^{\circ} \text{ F}$

Material	Nominal graphite
Model	TAP-III (3-D model)
Initial angle	$\gamma = -3^{\circ}$
Ballistic coefficient	11 (tumbling)
Block thickness	1 inch
Edge dimension	3 x 4.359 inches
Cladding emissivity	--

Fig. II-37. Peak Capsule Temperature Sensitivity to Material Thermal Property Variations

Fig. II-38. Fuel Block Stress Locations

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TABLE II-11
Structural Analysis

<u>Inputs</u>				<u>Results</u>							
<u>Material</u>	<u>Supports</u>	<u>Density</u> (lb/in. ³)	<u>E (psi</u> <u>x 10⁻⁶)</u>	<u>$\sigma_{\max}$</u> (psi)	<u>Location</u>	<u>$\tau_{\max}$</u> (psi)	<u>Location</u>	<u>S_{yz}</u> (psi)	<u>Location</u>	<u>FS</u>	<u>f₁</u> (cps)
Poco	a	0.06785	2.0	5030	d	2515	d	--	a	2.8	24
Poco	d	0.06785	2.0	2940	c	1470	c	--	a	4.8	37
Poco	b	0.06785	2.0	2840	c	1420	c	110	d	4.9	41
Poco	c	0.06785	2.0	2840	c	1420	c	110	d	4.9	40
CARB-I-TEX	c	0.05053	2.7	2390	b	1195	b	83	d	7.4	51

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35 g static load. This is well within the allowable 1/2-inch maximum deflection required by the current design configuration. However, this does not allow for any factor of safety, since the 35 g is the limit load factor and the 1/2-inch deflection value cannot be exceeded without actual contact between the block and other generator components. Supposing that a factor of safety of 1.5 is appropriate and is applied to the load factor, increasing it from 35 to 52.5 g. This would result in a static deflection of 1/4 inch, still well within the limit. The E

value used for the CARB-I-TEX analysis was 2.7×10^6 psi. This is the highest elastic modulus of all the carbon-graphite materials studied thus far. The E value was taken from the manufacturer's data listed for CARB-I-TEX 500. This E value does not agree with experimental values obtained by flexural tests of CARB-I-TEX 500. These tests

were performed in-house and resulted in an E value of 1.37×10^6 psi, only half that of the manufacturer's listed value. This discrepancy is cited to point out the need for more accurate data. If the test value is correct, deflections in a CARB-I-TEX block will be about 1/2 inch for 52.5 g load factor. This is right on the limit, but can be considered safe if the factor of safety of 1.5 is adequate. However, other materials which are up to 35% heavier will deflect up to 20% more than this (for the same modulus). Some of the materials also have elastic moduli as low as 0.8×10^6 psi. This would boost deflections considerably, up to approximately one inch for the worst materials. It should be evident that deflections are critical, perhaps more critical than stresses or frequencies. Potential solutions to this problem include:

- (1) Increased clearance between the block surfaces and other generator components.
- (2) Selection of a block material which has a high Young's modulus and low density.
- (3) Stiffening the fuel block by drilling lightening holes and pressing, brazing or otherwise fitting thin-walled tubes into these holes.
- (4) Let the fuel capsules stiffen the block by making them in one piece instead of two.
- (5) Support the block on all four edges--perhaps some picture frame-type arrangement which is ejected with the block and burns off during re-entry.

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b. Effect of lightening holes, material density, and block thickness on flexural stresses

The means of support in the current design configuration is such that the fuel block may be considered a simply supported beam subjected to a uniformly distributed load. The formula $S = Mc/I$ shows that the maximum stress is proportional to the maximum moment, which, in turn, is proportional to the weight of the block. Therefore, the weights of 24 hypothetical blocks were calculated to cover the cases, density = 0.04, 0.05, 0.06 and 0.07 pound per cubic inch, $t = 1.0$ inch, 1.125 inches and 1.250 inches, and each case with and without lightening holes. The maximum flexural stress in each of these cases was then computed, using the computer results reported in the preceding section. This was done by simple proportion. For the i th block

$$S_{\max_i} = S_{\max_{\text{Poco}}} \left(\frac{W_i}{W_{\text{Poco}}} \right)$$

where $S_{\max}$ is the maximum tensile stress (or shear stress as the case may be) and W is the weight of the block. The subscript (Poco) refers to the output values from the preceding section. The fuel block support configuration used was simply supported on the long edges by mounting channels, each of which was mounted to supporting structure at two points.

For this analysis, $S_{\max_{\text{Poco}}} = 1060$ psi in the Y-direction at the center of the block.

The results are plotted in Fig. II-39. It should be remembered that these results apply only to the case of long edge support with 50 g static load. The effect of the modulus of elasticity and Poisson's ratio on these stresses is negligible, however, they do affect the deflection and natural frequencies of the block.

As expected, the less dense the material, the lower the stress. The lightening holes reduce stress by about 5 or 6%. The stress in a 1-1/8 inch thick block is 12.2% less than the corresponding stress in a 1-inch thick block. The stress in a 1-1/4 inch thick block is 24.5% less than the corresponding stress in a 1-inch thick block.

c. Fuel block preload

The SNAP 29 fuel block support system is configured in such a way that no tension forces can be applied to the block. This means that the supports must always exert compressive loads on the block, even during vibration when the reactions are oscillating about an equilibrium (static) value. If the reaction does go to zero during vibration, a dangerous chatter condition could result.

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Fig. 11-39. Stresses for Fuel Block Simply Supported on the Long Edges

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In order to find the amount of preload necessary analytically, the reactions are assumed to be capable of exerting tension or compression on the block. With this support system, the vibratory inertia loads are applied to the block. In order to eliminate a tension reaction at any point, a compressive load (the preload) equal to or greater than this tension reaction must be applied at this point. This should be done at all points where there is a tension reaction. The net result is that there are no tensile reactions even in the vibration state. Since the vibratory loading condition was in equilibrium, the resultant of all the superimposed preloads must be zero. This restriction might necessitate increasing the magnitude of some of the preloads, since their directions are specified by the support configuration.

The inertia loading is of two types: the vibratory, which could be in any direction, and the static thrust, which is always parallel to the centerline of the booster and perpendicular to the centerline of the capsules. The preload must be sufficient to handle any possible inertia loading direction.

Reactions were determined by means of Digital Program SB-038, previously identified. The assumptions made were:

- (1) Rigid supports, elastic block.
- (2) Directions of the reactions are as shown in Fig. II-40.
- (3) Worst possible inertia loading conditions are those shown in Fig. II-40.
- (4) Block weight = 1.0 pound.
- (5) Young's modulus = 2×10^6 .
- (6) Poisson's ratio = 0.2.
- (7) Vibration design load factor rms = 35 g
- (8) Static thrust design load factor rms = 12 g.

The g-loading used in the Y-direction was 50 g [$50 \approx 1.5 \times 35$]. The g-loading used in the X-direction was 70 g [$70 \approx 1.5 (35 + 12)$]. For Loading Conditions 3 and 4, a force was desired which was at 45° with respect to the X-Y axis, but was also the resultant of a 50 g load at any angle and a 20 g load in the X-direction. Such a load is 62.2 g per Fig. II-41. The X and Y components of 62.2 are both 44.

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Fig. II-40. Loading Conditions and Reactions

Fig. II-41. Determination of 45° Inertia Force

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Table II-12 shows the results of applying the inertia loads in Fig. II-40 to the block, which has tension-compression supports. The negative values are tension according to the reaction directions indicated in Fig. II-40. Note that the true reactions can be obtained by multiplying the values in Tables II-12 and II-13 by the true weight of the block, since these results are for a one-pound block. Since the preload must be able to handle all loading conditions, the highest negative value in each column of Table II-12 is chosen as critical. However, since R_1 and R_6 are symmetrically opposite, the highest negative value in both these columns combined is chosen; similarly for R_2 and R_5 , R_3 and R_4 . These are titled "Indicated Preload" in the table.

In order to keep the block in equilibrium, the preloads must be applied in pairs or couples. R_1 and R_6 produce a clockwise couple; R_2 and R_5 produce a clockwise couple; while R_3 and R_4 produce a counter-clockwise couple. The balance between these couples is not satisfied by the results given in Table II-12. There is a net clockwise couple acting; therefore, R_3 and R_4 must be increased to balance the forces acting on the block. This all depends on the geometry involved, but for R_3 and R_4 spaced as in Fig. 4, the required value is given by:

$$R_3 = \frac{R_1 d_{1-6} + R_2 d_{2-3}}{d_{3-4}}$$

$$= \frac{47.7 (2.88) + 23.8 (19)}{19.0}$$

$$R_3 = R_4 = 30.9 \text{ g}$$

The final tabulation of required preloads is given at the bottom of Table II-12.

In order to evaluate the reactions due to the combined effect of preload and inertia load, the inertia load reactions listed in Table II-12 are added to the reactions due to preload listed at the bottom of Table II-12. These results are presented in Table II-13. Of these, the most critical is R_1 (or R_6), Loading Condition 1. For example, a 100-pound block would have 9570 pounds bearing on this corner. The bearing stress on the block at the corner could be critical depending on the bearing strength of the block material.

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TABLE II-12
Preload Reactions

<u>Loading Condition</u>	<u>X-Y Load Components</u>	<u>R₁ (g)</u>	<u>R₂ (g)</u>	<u>R₃ (g)</u>	<u>R₄ (g)</u>	<u>R₅ (g)</u>	<u>R₆ (g)</u>
1	70, 0	48	-20.6	-11.35	6.11	26.8	-47.8
2	0, 50	0.26	-12.4	-12.57	12.13	12.55	0.59
3	44, 44	30.3	-23.8	-18.15	14.5	27.8	-29.5
4	44, -44	29.9	-2.04	3.92	-6.82	5.78	-30.4
Indicated preload	--	47.7	23.8	18.15	18.15	23.8	47.7
Balanced preload	--	47.7	23.8	30.9	30.9	23.8	47.7

TABLE II-13
Total Reactions

<u>Loading Condition</u>	<u>R₁ (g)</u>	<u>R₂ (g)</u>	<u>R₃ (g)</u>	<u>R₄ (g)</u>	<u>R₅ (g)</u>	<u>R₆ (g)</u>
1	95.7	3.2	19.55	37.0	50.6	0.0
2	48.0	11.4	18.33	43.0	36.35	48.3
3	78.0	0.0	12.75	45.4	51.6	18.2
4	77.6	21.76	34.82	24.1	29.58	17.3
Maximum reaction	95.7	21.76	34.82	45.4	51.6	48.3

- d. Some carbon material properties computed from the results of recent flexural tests

Recent preliminary flexural tests of CARB-I-TEX 500 and Poco AXF-5Q beams resulted in load-deflection curves (Fig. II-42) which provide the opportunity of analytically determining Young's modulus and ultimate stress.

Young's modulus can be determined from the deflection equation for a centrally loaded simply supported beam:

$$\delta_{\text{CL}} = \frac{P_{\text{CL}} L^3}{48EI} \quad \therefore \quad E = \frac{P_{\text{CL}} L^3}{48\delta_{\text{CL}} I}$$

Since the Poco specimen failed in tension, its ultimate flexural stress can be determined from the bending stress equation:

$$\sigma = \frac{Mc}{I} = \frac{PLc}{4I}$$

Since the CARB-I-TEX specimen failed by interlaminar shear, its ultimate shear stress can be determined from the shear-stress-due-to-bending equation:

$$f_s = \frac{V}{Ib_1} \int_{y_1}^c y \, dA$$

where V is the shear force perpendicular to the beam at the point in question, b_1 is the width of the beam cross-section at that point, y_1 is the distance from the neutral axis to the same point, c is the distance from the neutral axis to the outer fiber and y is measured from the neutral axis.

Values for the required parameters are:

$$\left. \begin{array}{l} D = 0.66 \\ b = 2.7 \\ h = 1.0 \\ b_1 = 2.175 \end{array} \right\} \text{Refer to Fig. II-43.}$$

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Fig. 11-42. Load Versus Deflection

Fig. 11-43. Beam Cross-Section

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$$\left. \begin{array}{l} y_1 = 0.2 \\ I = 0.2157 \\ A = 2.358 \end{array} \right\} \text{Refer to Fig. II-43.}$$

L = length between simple supports = 11.5 inches

ν = Poisson's ratio = 0.2

$$\left. \begin{array}{l} P_{\text{CL}} \\ \delta_{\text{CL}} \end{array} \right\} \text{Refer to Fig. II-42.}$$

The Poco test results compare quite favorably with the manufacturer's published values, considering the fact that only one specimen was tested. The CARB-I-TEX test results are in question.

<u>Material</u>	<u>Type of Value</u>	<u>Young's Modulus</u>		<u>Ultimate Strength</u>
Poco	Manufacturer's	1.9×10^6	Flexural	14,000
	In-house test	1.73×10^6		11,330
CARB-I-TEX	Manufacturer's	2.7×10^6	Shear	Not given
	In-house test	1.37×10^6		338

The discrepancy in CARB-I-TEX Young's moduli could be due to a partial delamination prior to test, since the manufacturer's value for Young's modulus of CARB-I-TEX is between values computed for a solid cross-section. If the beam was initially cracked as shown in Fig. II-43 and as suggested by pretest X-rays, then the area moment of inertia should be based upon two cross-sections

$$I = \frac{b}{12} (h_1^3 + h_2^3).$$

In this case, where $h_1 = 0.3$ and using an average b , E turns out to be 4.07×10^6 .

e. Re-entry thermal stresses

The thermal stresses occurring during re-entry were investigated using the Kaufman-Hall structural analysis computer program. A preliminary peak temperature gradient typical for Poco graphite was used

to determine the thermal expansions in the mathematical model (see Fig. II-44). The coefficient of thermal expansion for Poco was also used, since it is the largest coefficient of the graphite materials under consideration. Most of the other materials have coefficients of 1/2 to 1/10 of this value. Therefore these stresses apply only to Poco, which probably represents the worst condition. The mathematical model representing 1/4 of the block is composed of 91 grid points connected by 235 structural elements, which consist of tension-compression members and shear panels. Thermal deformations in tension-compression members are machine computed from the equation:

$$\delta_T = \alpha_T \Delta T L$$

where

δ_T = the thermal deformation in inches

α_T = the coefficient of thermal expansion in in. / in. / °F,

ΔT = the difference between the temperature of a specific point and the temperature of the central portion of the blocks

L = the length of the element

The following assumptions and data were used:

- (1) Symmetrical temperature distribution about the long and short axes. This permits the use of a quarter plate. Symmetry about the long axis was automatic, since the temperature distribution is constant along the heated edge. Symmetry about the short axis implies that both short edges are heated, which is, in fact, the case for the tumbling mode under consideration.
- (2) There are no out-of-plane forces acting (i. e., no warping or buckling). This is because the temperature distribution is assumed constant through the thickness.
- (3) The capsules are free in their holes and carry no load. In the case of Poco, it makes little difference if the capsules are fastened or brazed-in, because the coefficient of thermal expansion for the capsule material is nearly equal to that of the block material.
- (4) Coefficient of thermal expansion, $\alpha_T = 4.9 \times 10^{-6}$ in. / in. / °F.
- (5) Modulus of elasticity, $E = 2.0 \times 10^6$ psi

(6) Poisson's ratio, $\nu = 0.2$

(7) Thermal deformations, $\alpha \Delta T L$ (nonzero values only)
 ΔT taken from Fig. II-44

The results are summarized in Table II-14.

TABLE II-14
Re-entry Thermal Stresses

<u>Stress (psi)</u>		<u>Location</u>	<u>Direction</u>
Maximum X-Y shear (in-plane)	1,460	Corner of heated edge	X-Y
Maximum compression	11,810	Center of heated edge	Parallel to heated edge
Maximum tension	1,640	3 inches from heated edge	Parallel to heated edge

These stresses are not excessive for Poco graphite, and, since the other graphites have much lower coefficients of thermal expansion, their stress levels will be proportionately lower for similar elastic moduli.

As more accurate temperature distributions, material properties and design geometry are available, this analysis will be extended. The case where capsules with a high coefficient of expansion are brazed or otherwise fastened to a block with a low coefficient will be considered. This condition will cause compression of the capsules as the temperature increases. Also to be considered are the cases of the long edge heating and both long and short edge heating simultaneously.

f. Re-entry stresses (excluding thermal)

The method of analyzing nonthermal re-entry stresses was thoroughly presented in Vol. II of the previous quarterly report for a columbium honeycomb fuel block. The same method applies to a solid carbon block and is only summarized here.

The fuel block is divided into 100 segments. The instantaneous aerodynamic pressure acting on the block causes a linear deceleration and angular acceleration, which in turn causes an inertia force on each segment. The net result of the inertia force and the aerodynamic force acting on each segment causes shear forces and bending moments to be applied to the adjacent segments. These forces and moments produce fiber stresses which are superimposed on the fiber stresses due to centrifugal force.

The following assumptions and parameters were used:

- (1) The only forces considered are inertia, aerodynamic pressure and centrifugal force.
- (2) The weight vector is ignored because its direction is constantly changing during spin and it is conservative to ignore it. The conservatism is obvious because the weight component in the plane of the plate creates no stress in free fall and the weight component perpendicular to the plane of the plate decreases the magnitude of the inertia force, which is the predominant force causing bending moments.
- (3) The fuel block spins during re-entry about an axis which passes through the cg (see Fig. II-45) and is parallel to the channels and perpendicular to the relative wind line.
- (4) Aerodynamic pressure distributions are as shown in Fig. II-46. (P_o = stagnation pressure, α = plate angle of attack). Pressures on the ends of the plate and at the center point were obtained from IDC 3323-6. The distribution between the ends was not computed but is considered to be flat. To check out all possibilities, both flat and uneven distributions were assumed.
- (5) The weight distribution is also shown in Fig. II-46. The particular distribution shown is for Cases 1 and 7, of Table II-15, and the other cases have similar weight distributions. The locations of the lightening holes and the fuel/capsules are easily identified.
- (6) Number of fuel channels = 12
- (7) $b = 32.96$ inches
- (8) $L = 36.876$ inches
- (9) $h = 1.000$ inch
- (10) $l_b = 2.796$ inches
- (11) $D_p = 0.67$ inch
- (12) Fuel/capsule weight = 37 lb
- (13) $\omega = 240$ rpm

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Fig. II-46. Aerodynamic Pressure and Weight Distributions

TABLE II-15
Re-entry Stresses Excluding Thermal Stresses

Pressure Distribution	Angle of Attack (deg)						
	Flat				Uneven		
	15	30	45	90	45	30	15
1	94.32	101.59	104.87	91.88	141.53	124.84	100.12
2				72.29	104.64		
3				72.27	139.79		
4				53.12	101.63		
5				81.10	140.69		
6				64.65	103.50		
7				94.79	<u>155.79</u>		
8				70.13	114.16		
9	90.84	103.55	117.65	103.10	<u>151.31</u>	125.10	96.05
10				73.58	109.39		
11				96.61	<u>154.14</u>		
12				70.67	112.50		

Description of configurations:

1. Poco, 1.0 inch thick with lightening holes
2. Poco, 9/8 inch thick with lightening holes
3. CARB-I-TEX, 1.0 inch thick with lightening holes
4. CARB-I-TEX, 9/8 inch thick with lightening holes
5. ATJ, 1.0 inch thick with lightening holes
6. ATJ, 9/8 inch thick with lightening holes
7. Poco, 1.0 inch thick with no lightening holes
8. Poco, 9/8 inch thick with no lightening holes
9. CARB-I-TEX, 1.0 inch thick with no lightening holes
10. CARB-I-TEX, 9/8 inch thick with no lightening holes
11. ATJ, 1.0 inch thick with no lightening holes
12. ATJ, 9/8 inch thick with no lightening holes

(14) p }
(15) w } See Fig. II-46.

(16) Material densities { 0.06785 (Poco)
0.05053 (CARB-I-TEX)
0.06244 (ATJ)

(17) Stagnation pressure = 2.14 psi.

Results are presented in Table II-15. It is concluded that the re-entry phase at this spin rate (240 rpm) does not appear to be critical, since a maximum stress of 156 psi is less than 5% of the design stress for the launch phase. However, further preliminary analysis indicates that the spin rate becomes critical at 1000 rpm when combined with thermal stresses. The maximum deviation between the stresses produced by flat and uneven pressure distributions is 26%. The block with no lightening holes produces the higher stresses due to the higher weight. The highest density block (Poco) has the highest stress, again due to the higher block weight.

g. Material property tests

Testing of ATJ and CARB-I-TEX graphite specimens was initiated for the purpose of measuring material damping coefficients and the elastic and shear dynamic moduli. The "Magnatest FM-500 Elastomat" instrument is utilized for obtaining the specimens' bending, axial and torsion mode frequencies and damping coefficients. The resulting test data were used in simple beam frequency formulas for obtaining preliminary estimates of elastic and shear dynamic moduli. A refinement of this elastic modulus value was obtained by means of a beam vibration analysis, which included the effects of shear stiffness and rotary inertia obtained from the test data mentioned above.

The material specimens consist of uniform rectangular bars machined to the dimensions shown in Fig. II-47(a).

To date, only testing of the ATJ specimen has been completed. The modified value of elastic modulus produced an analytical fundamental frequency. Results for the ATJ material at room temperature are summarized below.

E (elastic modulus) = 1.08×10^6 psi

G (shear Modulus) = 0.45×10^6 psi

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C/C_c (damping coefficient) = 0.003

ν (Poisson's ratio) = 0.20

The above value of E modulus is in agreement with documented "cross-grain" data for ATJ graphite.

Test data will also be obtained for an ATJ specimen with drilled holes (see Fig. II-47(b)). These data are to be used for evaluating the effects of the holes on structural stiffness. Future testing for material properties is planned and will be part of the vendor fuel block evaluation. Damping coefficients and dynamic moduli will be experimentally determined for each of the proposed fuel block materials at room temperature and elevated temperatures.

Fig. II-47. Test Specimen Configurations

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

This control point deals with the development and design of an electrical heat source which simulates the thermal characteristics of the isotopic heat source. The design and development consist of selecting candidate heating elements from a survey of vendor proposals, integrating the elements into a heater block design, conducting environmental screening tests on electrical heater designs and, finally, selecting the best design. This approach will be used to select an electrical heat source for testing of thermoelectric modules as well as for testing and demonstration of complete generator systems.

Effort continued during this reporting period on the component development of the electrical heat source for the thermoelectric modules (CP 1240) and component development of the generator electrical heat source (CP 1230).

1. Summation of Accomplishments

a. Component Development--CP 1240

A review of the module electrical heater screening test program was performed, with primary emphasis on cost reduction. It was determined that the number of test fixtures required can be reduced by combining some heater tests with certain thermoelectric module tests (CP 1340). As a result of this, the Watlow heaters, since they are the first heaters available for test, will be tested concurrently with the CP 1340 thermoelectric modules. Since these heaters provide the heat source for the modules during the module environmental tests, the actual quantity of heaters tested is greater for the Watlow design than for the TRW and Sanders heaters. A summary of the module electric heater tests is presented in Table II-16.

During this period, the module heater test specification was generated, reviewed and released. Heater block assembly drawings were generated and released. Basically, the heater block consists of a solid Inconel slab with holes bored to receive the cylindrical heaters. The heater sheaths as well as all exposed surfaces of the Inconel block are oxidized to enhance radiant heat transmission. At the juncture of the electrical leads and the heater sheath, the leads will be inserted in a boron nitride plug which serves the dual purpose of minimizing the possibility of electrical arcing and providing additional support for the leads during vibration and shock testing.

TABLE II-16

Module Electrical Heat Source Tests

<u>Test</u>	<u>Heater Test Quantity</u>		
	<u>Watlow</u>	<u>TRW</u>	<u>Sanders</u>
Component Development Tests			
Vibration, 3 axes	18	3	3
Shock, 3 axes	3	3	3
Thermal vacuum	6	5	5
Thermal cycle	18	5	5
Life	18	5	5

* Heaters are tested as part of T/E module tests, and quantities determined by module test requirements.

The test fixture required for the heater tests was investigated and it was decided to utilize the design of the dynamic module test fixture, replacing the module with a heat sink to remove the heat generated by the heaters. Drawings were released for the heat sink assembly, test fixture and instrumentation.

b. Assembly Qualification--CP 1230

During this period the generator heater test specification was completed, reviewed and released. A summary of the heater tests appears in Table II-17. The generator heater block assembly and test fixture drawings were released.

A thermal analysis of the generator heater test fixture was performed during this period.

The objective of this thermal analysis was to determine the adequacy of the generator heater thermal performance test fixture. This fixture will be used for tests specified in the electrical heater block element test specification, 466A1233000. Air and vacuum environments will be experienced during testing.

The two points of interest in this investigation were the heat losses from the test fixture surfaces and the heat transferred from the electrical heat source to the heat sink. The losses from the fixture surface were estimated by making a heat balance for the test fixture skin. The heat input to the skin occurs by conduction through Min-K 2000 insulation and stainless steel brackets. The heat carried away from the skin is transported by the convective and radiative modes in air and the radiative mode in vacuum.

TABLE II-17
Generator Electrical Heat Source Tests

<u>Test</u>	<u>Heater Test Quantity</u>		
	<u>Watlow</u>	<u>TRW</u>	<u>Sanders</u>
Component Development Tests			
Vibration, 3 axes	3	3	3
Shock, 3 axes	3	3	3
Thermal vacuum	5	5	5
Thermal cycle	5	5	5
Life	5	5	5

In order to estimate the highest temperatures that might occur on the fixture surface, the area used in the heat transfer calculations was the projected area of the electrical heat source and brackets on the fixture surface. Along with the resulting maximum temperatures came estimates of minimum heat losses. If the maximum heat losses and minimum surface temperature had been desired, the entire fixture surface area would have been used.

The heat transferred from the electrical heat source to the coolant tank was calculated on the assumption that convection and radiation predominate. The coolant tank temperature was assumed to be $\sim 100^{\circ}\text{F}$. Because of the high heat source temperatures, variation of 100°F from the assumed tank temperature results in $\sim 5\%$ change in the amount of heat transferred. For the purpose of simplification, the 100°F tank temperature will be a sufficient approximation. The effect of instrumentation on the heat losses is insignificant and will be neglected for this analysis. Figure II-48 is a schematic drawing of the model for this analysis.

Figures II-49 and II-50 present the heat transferred from the test fixture as a function of the surface temperature, parametric in emissivity, convection coefficient and heat source temperature. Figure II-49 gives the results for an air environment. From the intersection points of the curves, it is seen that heat losses will be in the range of 250 to 400 watts and the surface temperature will be about 300 to 500°F .

Figure II-50 shows the results for a vacuum environment before the insulation outgasses. During outgassing the thermal conductivity of Min-K 2000 decreases $\sim 50\%$. However, since $\sim 85\%$ of the heat being conducted to the fixture surfaces travels through the stainless steel brackets, the 50% reduction does not significantly affect the overall

Fig. II-48. Model for Analysis

T_{HS} = heat source surface temperature
 h_c = convection coefficient (Btu/hr-ft²-° F)
 T_{amb} = 70° F
 ϵ = fixture surface emissivity

Fig. II-49. Heat Transferred Versus Fixture Surface Temperature--Generator Electric Heat Source Element Performance Test Fixture

Fig. 11-50. Heat Transferred Versus Fixture Surface Temperature-- Generator Electrical Heat Source Element Performance Test Fixture

total. Consequently, the figure shows that, in a vacuum environment, the heat losses will be ~200 to 350 watts and the surface temperatures will be ~400 to 700° F.

Figures II-51 and II-52 show the amount of heat transferred to the coolant tank from the heat source. Figure II-51 applies to an air environment where radiation and convection will occur. The thermal inventory to be experienced is 10,000 watts. The specified heat source surface temperature, in air, is 1500° F according to the heater element test specification (466A1233000). Under these conditions, Fig. II-51 indicates that an effective emissivity greater than 1.0, which is impractical, must exist between the heat source and coolant tank surfaces. Figure II-52 presents the heat transferred by radiation and convection. In the vacuum tests, the thermal inventory specified is 10,000 watts and the heat source surface temperature is 1700° F. From Fig. II-52, these conditions indicate that an effective emissivity of ~0.8 will be necessary. Figure II-53 indicates that high emissivity coatings would be needed on the heat source surface and the surface it sees in the direction of the coolant tank.

Table II-18 presents a summary of the results from Figs. II-49 and II-50.

TABLE II-18
Heat Losses from Test Fixture

<u>Test Environment</u>	<u>Fixture Surface Temperature</u>	<u>Heat Losses from Fixture</u>
Air	~300 to 500° F	~250 to 400 watts
Vacuum	~400 to 700° F	~200 to 350 watts

From Table II-18, it can be seen that the test fixture could experience surface temperatures as high as 700° F.

Figures II-51 and II-52 indicate that the specified thermal inventory of 10,000 watts, with a maximum of 12,000 watts, and a heat source surface temperature of 1500° F, in an air environment, are unattainable in the present generator heater element test fixture design (466A1230001).

The results of this analysis indicate that a more detailed review of the heater test fixture is in order. During the next reporting period, the analysis and supporting assumptions will be reviewed with the intention of improving the accuracy of the results. On the basis of the tentative conclusions based on this analysis, consideration will be given to the following:

Fig. II-51. Generator Electrical Heat Source Element Test Fixture Heat Transferred Versus Heat Source Temperature

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Fig. II-52. Generator Electrical Heat Source Element Performance
Test Fixture Heat from Heat Source to Coolant Tank

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Fig. 11-53. $\bar{\epsilon}_{eff}$ Versus ϵ_2

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- (1) Redesign steel supporting brackets (including possible material substitutions) to reduce heat losses and, hence, test fixture surface temperatures.
- (2) Maintain a 2000-watt/heater thermal input and let the heat source operate at the resultant temperature.
- (3) Reduce the number of heater elements per test fixture from 5 to 2 and use a high emissivity coating (0.85 or greater) on the heater block surface only.
- (4) Reduce the number of heater elements per test fixture from 5 to 3 and use an emissivity coating of 0.85 or better on both the coolant tank surface and the heat source surface.

c. Manufacturing and tooling

Satisfactory machining and gun-drilling of heater blocks was demonstrated.

Machining of various Inconel 600 heater blocks was started.

Fabrication was started on Water-Cooled Test Fixture No. 466A1240001.

D. T/E MODULES SUBSYSTEM--CONTROL POINT 1300

The objective of this task is to develop a thermoelectric module subsystem which will withstand the design dynamic and environmental conditions and produce nominal power output of 500 watts(e) over the mission life with high reliability.

The thermoelectric module subsystem is designed, fabricated, inspected and tested under this task. The basic philosophy in achieving flight prototype modules 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 demonstration and qualification. Modules will then be fabricated and acceptance tested for use in prototype RTG units. Commensurate with this philosophy, the work effort has been broken down into the following control points:

- (1) CP 1340--Thermoelectric Modules Subsystem, Component Development
- (2) CP 1330--Thermoelectric Modules Subsystem, Assembly Qualification
- (3) CP 1320--Thermoelectric Modules Subsystem, Prototype System

A module consists of thermoelectric couple assemblies, the hot plate, and cold-end hardware (springs, pistons, etc.), the Min-K insulation, electrical penetrations and the module side skins. The T/E module subsystem consists of an assembly of eight thermoelectric modules.

Under Control Point 1340, four couple designs were tested:

- (1) Configuration A--2N-2P, with iron shoes
- (2) Configuration B--2N-3P, with iron shoes
- (3) Configuration C--2N-2P, bonded to iron encapsulation cups with stainless steel or nickel shoes.
- (4) Configuration D--2N-3P, bonded to iron encapsulation cups with stainless steel or nickel shoes.

On the basis of performance and thermal cycling tests (reported in the first quarterly), Configuration C and D were selected. Further evaluation resulted in the selection of Configuration D (with nickel shoes) on the basis of long-term stability and strength. Twelve modules utilizing this couple design will be performance tested and exposed to thermal

cycling, Each module will contain 90 couples. Concurrent with this effort, seven structural simulation modules will be vibration and shock tested to identify failure mode characteristics.

1. Summation of Accomplishments

a. Design modifications

The basic design of the T/E module assembly is the same as presented in the first quarterly report. However, Veeco hi-vacuum valves have now been added to the backfill and evacuation vent tubes to the electrical chamber. These valves will facilitate outgassing, and final filling of the module and will also permit evaluation of module internal atmospheric conditions during performance testing. These valves will not be installed on dummy (dynamic test) modules scheduled for vibration and shock testing.

The selected couple design consists of 2N-3P elements with iron cups bonded to a nickel hot shoe. Figure II-54 presents the selected couple design. Previously, this design showed 2-mil counterbores in the element ends for acceptance of the bonding wafer. However, the selected design does not require a bonding wafer for the N element, and tests revealed increased strength and better performance when the elements were not counterbored. Therefore, the P element also is not counterbored. Any excessive run-out of the bonding material during bonding operations is controlled by precise sizing of the bonding wafer.

The cold end hardware design has not changed since the last report period but investigations are being performed in an effort to reduce the weight of the heat sink. At present, the heat sink design is all stainless steel with integrated NaK coolant channels. Methods are under investigation to develop a bimetallic heat sink design of aluminum with stainless steel coolant channels. The limiting factor in the bimetallic design is the ability to obtain a satisfactory bond at the aluminum-stainless steel interface. If a satisfactory bond can be achieved, a substantial weight reduction can be effected. One bonding method was attempted, using a tin-silver alloy solder. Ultrasonic evaluation of the bond revealed a satisfactory bond initially; however, due to differential thermal expansion coefficients of the materials, separation occurred during the cooling process. Additional methods and materials will be evaluated in this continuing effort.

b. Test specifications

The dummy module environmental test specification was prepared during this quarter. This specification defines the vibration and shock tests to which the dummy T/E module will be exposed in order to evaluate design integrity under these environments. The scope of testing is intended to provide the following:

- (1) Evaluation of the dynamic behavior of the module
- (2) Determination of mechanical properties which cannot be computed, such as damping
- (3) Evaluation of the dynamic design adequacy.

Also prepared during this period was the test specification for the T/E module performance and thermal cycling test. This specification defines the parameters to determine the operational performance, the temperature distribution and the effect of repetitious thermal cycling on the service life of the thermoelectric module design.

c. Hot plate emissivity coating tests

During the reporting period, effort continued on screening of promising coatings for the module hot plate. Currently, three candidate systems are under consideration for this application:

- (1) Plasma-sprayed $\text{CaO} \cdot \text{TiO}_2$
- (2) Stably oxidized A-286 hot plate
- (3) Black "Hot Paint" (U.S. Paint Corp.).

Results to date are shown in Table II-19.

TABLE II-19
Hot Plate Emissivity Coating Test Results

<u>Coating</u>	<u>Specimen</u>	<u>ϵ</u>	<u>T (° F)</u>	<u>Remarks</u>
Stable Oxide	Hemispherical cup	0.91	993	Air quench after 1800° F
		0.93	1184	
	Hemispherical cup	0.86	993	Oil quench after 1800° F
		0.89	1195	
		0.88	1210	
Plasma-Sprayed $\text{CaO} \cdot \text{TiO}_2$	Hemispherical cup	0.919	991	0.006 inch thick
		0.921	1204	
Black Hot Paint	Hemispherical cup	0.89	977	Color change to reddish brown
		0.87	1175	
	Disc	0.927	1000	
		0.929	1200	

A hot plate is currently being prepared in order to evaluate its response to the stable oxide process. It may be necessary to provide a means of minimizing warpage during heatup and cooldown. Results of this study will be reported in the next quarterly report. The black hot paint appears promising, since it is easy to apply and is characterized by a high emissivity. Currently, two additional specimens are under test to further evaluate the problem of discoloration.

d. T/E module static test fixture analysis

The module static test fixture of Drawing 466A1344057 will be used for test in vacuum and in air. This study was performed to determine the adequacy of the fixture design when subjected to the two environments previously mentioned.

The assumptions made for this study are as follows:

- (1) The test fixture assembly was oriented as shown in Fig. II-55.
- (2) The module cold plate temperature was 300° F and remained constant in air and vacuum.
- (3) The losses through the instrumentation may be neglected because they are small when compared to the other losses.
- (4) The thermal conductivity of Min-K 2000 decreases by 50% as a result of outgassing.

A heat balance was made for the external surfaces of the test fixture assembly. The heat transferred to the surfaces, by conduction through the Min-K insulation, must be equal to the heat transferred away from the surfaces. In a vacuum, the heat was assumed to be transferred away by radiation, and in air the heat was assumed to be transferred away by convection and radiation. The study was parametric in surface emissivity, convection coefficient and heat source temperature. The radiation losses from the constant temperature module cold plate amounted to 34.6 watts and the convection losses amounted to 45.7 watts.

Fig. 11-54. Selected Couple Design

Fig. 11-55. Model of Static Test Fixture

Figure II-56 shows the results (points of intersection of curves) for a vacuum environment before and after outgassing. It can be seen that the greatest heat loss occurs before the insulation outgasses. During this time, the surface temperature will be ~300 to 450° F and the heat losses, ~160 watts. During outgassing, the conductivity will decrease by ~50%, resulting in a surface temperature of ~150 to 300° F and thermal losses of ~80 watts.

Figure II-57 shows the test fixture thermal behavior in an air environment. This curve indicates that the thermal losses will be ~175 watts and the surface temperature will be ~150 to 300° F.

The test fixture thermal efficiency is defined as the percentage of the thermal input that is rejected to the module coolant.

$$\eta_{th} = \frac{Q_{coolant}}{Q_{coolant} + Q_{loss}}$$

The current module reference design, of 1.35 gpm of NaK coolant and a temperature rise of 40° F, results in approximately 1450 watts being carried away by the coolant.

On this basis, Table II-20 was developed.

TABLE II-20
Static Test Fixture Heat Loss

<u>Environment</u>	<u>T_{SF}</u> <u>(° F)</u>	<u>Q_{loss}</u> <u>(watts)</u>	<u>η_{th}</u> <u>(%)</u>
Air at 70° F	~150 to 250	~175	~89
Vacuum before outgassing	~300 to 450	~160	~90
Vacuum after outgassing	~150 to 300	~90	~94

Losses from module cold plate

$$T_{CP} = 300^{\circ} \text{ F}$$

Vacuum: $Q_{radiation} = \sim 35 \text{ watts}$

Air: $Q_{convection} = \sim 46 \text{ watts}$

$Q_{total} = \sim 81 \text{ watts}$

NOTE: Heat losses from cold plate are included in curves of Figs. II-56 and II-57

Fig. II-56. Heat Transferred Versus Fixture Surface Temperature in Vacuum-T/E Module Static Test Fixture

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Fig. II-57. Heat Transferred Versus Fixture Surface Temperature--
T/E Module Static Test Fixture

In a vacuum, the heat losses before the Min-K outgasses will be about 160 watts, and the surface temperature range will be between 300° and 400° F. After the Min-K outgasses, the thermal conductivity of the insulation will decrease by about 50%. This will yield about 90 watts in heat losses and decrease the fixture surface temperature range to between 150° and 300° F. The fixture thermal efficiencies before and after outgassing will be about 90% and 94%, respectively.

In air, the heat losses will be about 175 watts and the surface temperature range will be between 150° and 250° F. The fixture thermal efficiency will be about 89%.

The efficiencies mentioned above are rather high. This trend will occur in the actual tests because the heat source is supported solely by the Min-K insulation. Consequently, there is no "thermal short" between the heat source and the fixture, and a minimum amount of heat is lost.

This study was undertaken to estimate the thermal behavior of the test fixture, and the results should be employed in light of this fact. The results do indicate that the static test fixture design configuration will be adequate for T/E module tests in air and vacuum.

e. T/E module dynamic test fixture analysis

The module dynamic test fixture will be used for tests in air. This study was performed to determine the thermal adequacy of the fixture design when subjected to the environment mentioned above and to normal operating temperatures of the T/E module and the electrical heat source.

The assumptions made for this study are as follows:

- (1) The test fixture assembly was oriented as shown in Fig. II-58.
- (2) The amount of heat required by the T/E module including its losses was 1700 watts. This requires a heat source temperature of ~1500° F. (Using $\epsilon = 0.85$ for the hot plate, and $\epsilon = 0.60$ for the heat source, and only radiation between the heat source and module.)

Fig. II-58. Model of Dynamic Test Fixture

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- (3) The temperature was assumed to be uniform over the entire housing of the test fixture except for the areas where the six brackets connect the heat source to the housing. At these points, the temperature was assumed to be uniform, but higher.
- (4) The heat losses through the insulation were assumed to be one-dimensional for the entire cross-sectional area of the insulation instead of for the projected area of the heat source. For this reason, the estimated heat losses for the insulated areas are high.
- (5) Heat was removed from the housing surface by means of natural convection and by radiation where the geometric view factor was taken to be 1.0 and the surroundings were taken to be at 70° F.
- (6) The losses through the instrumentation may be neglected because they are small when compared to the other losses.

A heat balance was made for the external surfaces of the test fixture assembly. The heat transferred to the surfaces, by conduction through the Min-K 2000 insulation and the brackets, must be equal to the heat transferred away from the surfaces. In air the heat was assumed to be transferred away by convection and radiation. The study was parametric in surface emissivity, convection coefficient and heat source temperature. The convection coefficients were calculated using the following formulas from Heat Transfer by J. P. Holman, for the desired outside skin temperature of the test fixture and an environment temperature of 70° F.

- (1) Simplified equations for free convection from various surfaces to air at atmospheric pressure.

- (a) Vertical planes

$$h = 0.29 (\Delta T/L)^{1/4}$$

- (b) Horizontal heated plate facing downward

$$h = 0.12 (\Delta T/L)^{1/4}$$

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(c) Horizontal heated plate facing upward

$$h = 0.27 (\Delta T/L)^{1/4}$$

(2) Empirical relation for free convection

$$\frac{\bar{h}L}{k_f} = \overline{Nu}_f = C (Gr_f \cdot Pr_f)^M$$

There was a ± 0.2 Btu/hr-ft² - °F variation imposed on the calculated value of h to be used in the parametric analysis.

Figure II-59 shows the results (points of intersection of curves) for the parametric analysis of the insulated sections of the fixture. The heat losses range from ~55 to 70 watts, and during this time the surface temperature will be ~90° F to 125° F.

Figure II-60 shows the results (points of intersection of curves) for the parametric analysis of the bracket areas of the fixture. It is reasonable to assume that the heat at the brackets will spread out through the housing over three times the area of the bracket connection and, therefore, the heat losses will range below ~144 watts to 179 watts. During this time the surface temperature will be ~375° F to 500° F.

Figure II-61 shows the results of an ideal analysis where the heat conducted through the insulation-and-brackets-combine to form an uniform temperature over the whole housing which is equal to

$$T_{\text{ideal}} = T_{\text{insulation surface}} = T_{\text{brackets surface}}$$

The heat losses range from ~235 watts to 280 watts, and the housing skin temperature ranges from ~130° F to 200° F.

The test fixture thermal efficiency is defined as the percentage of the thermal input that is used by the module

$$\eta_{\text{th}} = \frac{Q_{\text{module}}}{P_{\text{in}}} = \frac{Q_{\text{module}}}{Q_{\text{module}} + Q_{\text{lost}}}$$

For the parametric analysis, this results in an efficiency range of 85% to 89.5%.

Combining the heat lost through the insulation and the heat lost through the brackets results in a heat loss of ~200 to 300 watts. The

Fig. II-59. Parametric Thermal Analysis Between Heat Conducted Through Test Fixture (dynamic) at the Insulated Areas and the Heat Radiated and Convected Away from the Outer Skin

Fig. II-60. Parametric Thermal Analysis Between Heat Conducted Through Test Fixture (dynamic), at the Brackets, and the Heat Radiated and Convected Away from the Outer Skin for Multiples of the projected Bracket Area.

Fig. II-61. Parametric Thermal Analysis Between Heat Conducted Through Test Fixture (dynamic) and the Heat Radiated and Convected Away from the Outer Skin

fixture surface temperature will vary from $\sim 100^\circ$ to 500° F, with hot spots ranging from $\sim 375^\circ$ F to 500° F located in the areas of the six brackets which connect the heat source to the housing. These conditions exist for testing in air.

The ideal analysis results in a uniform temperature over the entire surface of the fixture ranging from $\sim 130^\circ$ F to 200° F. The heat losses range from ~ 235 watts to 280 watts.

The efficiency range of 85% to 89.5 % seems rather high, since there is a "thermal short" between the heat source and the fixture housing at the brackets. It is possible there will be more heat lost when the fixture is clamped down for testing. In this case, the efficiencies will be lower.

The results indicate that the dynamic test fixture will be adequate for T/E module tests in air.

f. Power requirements for T/E module electrical heaters

An analysis was performed to establish the power requirements for the development testing of T/E modules.

For the environmental modules operating at a hot junction temperature of 1050° F and a cold junction temperature of 350° F, the thermal energy required has been calculated as a function of contact resistivity and is shown in Table II-21

TABLE II-21

Thermal Energy Which Must Enter T/E Elements
to Maintain $T_H = 1050^\circ$ F and $T_C = 350^\circ$ F
(3M property data (2N and 3P))

<u>$C_T (\mu\Omega\text{-cm}^2)$</u>	<u>Q_{el} (watts)</u>
-1500	1620
0	1540
+1500	1480

If we consider the expected contact resistivity of $\sim 0 \mu\Omega\text{-cm}^2$, the thermal energy required is ~ 1540 watts.

Recent SNAP 19 closed generator system testing indicates that the predicted thermal energy required to maintain given junction temperatures

may be low by ~10%. Therefore, the above determined energy should be increased by ~150 watts, resulting in a requirement of ~1690 watts to enter the T/E elements.

The total energy required to enter the T/E module is obtained by adding the module parasitic heat losses to the above values. The parasitic heat losses were estimated as ~170 watts for the 2N and 3P T/E module.

Further heat losses are experienced through the module test fixture. The heat losses through the static test fixture were estimated as ~150 → 200 watts.

The total thermal energy required of the electrical heaters can be obtained by adding the above components as shown below in Table II-22.

TABLE II-22

Matched Load Thermal Requirements of Module Electrical Heaters

<u>Component</u>	<u>Heat Required (watts)</u>
Q entering T/E elements (3M property data calculated)	~1540
Adjustment factor due to SNAP 19 hardware thermal data	~150
Losses through T/E module	~170
Losses through the test fixture	<u>~175</u>
Total	~2035

During environmental module testing, E-I performance parametric curves will be obtained. Due to the change in Peltier cooling as the current varies, the thermal energy entering the T/E elements requirement will increase by ~25% (~425 watts) if a short circuit is approached. Therefore, the minimum electrical power supply requirements for each module is ~2.4 kw of thermal energy from the electrical heaters.

g. Piston heat sink ΔT tests

Piston heat sink ΔT tests were initiated during this reporting period. Due to instrumentation and testing problems, the entire test series was not completed. Table II-23 shows a summary of test results. It is seen that the temperature drop from the cold junction to the coolant channel

wall position was about 100° to 120° F at design conditions ($Q_{rej} \sim 6.5$ watts, $T_{cj} = 350^\circ \text{F}$, interface pressure ~ 75 psi). The largest ΔT existed across the cylindrical gap between the piston and the heat sink ($\sim 50^\circ$ to 60°F). A significant ΔT in the heat sink ($\sim 40^\circ$ to 50°F) was also recorded.

Table II-24 shows test results when a filler was used in the cylindrical piston-heat sink interface. The table indicates that the type of filler employed did not significantly affect the results. A comparison of Tables II-23 and II-24 indicated that the application of a filler on the cylindrical piston-heat sink interface did not have a significant affect on the temperature gradient across the interface. This result was not consistent with previous testing experience. It was suggested that this anomaly be clarified by further testing.

TABLE II-23
SNAP 29 Piston Heat Sink ΔT Test Results

<u>Run</u>	<u>1</u>	<u>10</u>	<u>25</u>	<u>4</u>	<u>22</u>
Q (watts)	5.6	4.2	6.3	3.0	4.5
T_{cj} ($^\circ \text{F}$)	353	295	403	346	399
P (psi)	60	60	60	60	60
ΔT_1 ($^\circ \text{F}$)	17	13	19	12	14
ΔT_2 ($^\circ \text{F}$)	55	47	62	35	46
ΔT_3 ($^\circ \text{F}$)	46	37	54	28	37
$\Sigma \Delta T$ ($^\circ \text{F}$)	118	97	135	75	97

NOTE: ΔT_1 = piston-cold junction ΔT (across lead disc)

ΔT_2 = piston-heat sink ΔT (cylindrical interface)

ΔT_3 = heat sink ΔT to coolant channel wall

TABLE II-24

SNAP 29 Piston Heat Sink ΔT Test Results Using
Cylindrical Interface Fillers

<u>Run</u>	<u>32</u>	<u>32⁽²⁾</u>	<u>32</u>	<u>2</u>
Q (watts)	5.3	5.0	5.5	5.3
T _{cj} (°F)	352	332	359	349
P (psi)	75	75	75	75
ΔT_1 (°F) ⁽¹⁾	22	14	17	22
ΔT_2 (°F) ⁽¹⁾	49 ⁽³⁾	44 ⁽³⁾	47 ⁽⁴⁾	48 ⁽⁵⁾
ΔT_3 (°F) ⁽¹⁾	33	30	34	32
$\Sigma \Delta T$ (°F)	104	88	98	102

NOTE: (1) ΔT_1 = piston-cold junction ΔT

ΔT_2 = piston heat sink ΔT

ΔT_3 = heat sink ΔT

(2) Air environment at 15 psia

(3) Aluminum grease filler

(4) Wakefield thermal compound 120 filler

(5) ΔT was apparently influenced by residual grease

This anomaly will be further investigated in the cold-end hardware ΔT module tests.

h. Research module S/N 1

The purpose of this evaluation was to determine the long-term performance characteristics exhibited by Martin Marietta Research Module S/N 1. This module, which is made up of 84 2N-2P (SNAP 19 type) $D_N = D_P = 1/2$ inch couples in two series-parallel circuits, is still under test; however, only 10,018 hours of performance data are covered in this analysis. Tests on this module were performed in accordance with Test Specification 466A1343029. It should be noted that, after approximately 4900 hours of testing, the heater in the module test fix-

ture failed (shorted out). After replacement of the heater and also the thermal insulation in the test fixture which was affected by the heater failure, the test was resumed.

The effect of long-term performance testing on Martin Marietta Research Module S/N 1, with respect to net power output, contact resistivity, open-circuit voltage and internal resistance, is summarized below. It should be noted that data taken after 88 hours of testing were used as the reference point for this evaluation, due to initial problems in obtaining desired operating temperatures and thermal stability at the start of testing. It should also be noted that the values of power, contact resistivity and open-circuit voltage indicated in Items (1) through (3) below, are based on hot and cold junction temperatures reflected by Thermocouples 10 and 5, respectively, in lieu of the average value of the six hot junction and five cold junction thermocouples monitored during the test. The basis for this method will be discussed in detail.

- (1) Power--The absolute values of net output power and the percent degradation in power with respect to time are tabulated below:

<u>Time (hr)</u>	<u>Net Power* (watts)</u>	<u>Degradation (%)</u>
88	69.60	--
3,574**	62.68	9.9
10,018	49.79	28.5

*These values represent the normalized net power output of the module for the design hot and cold junction temperatures of 925° and 350° F, respectively.

- (2) Contact resistivity--The values of contact resistivity (C_T) determined from measured junction temperatures (using the temperature values reflected by Thermocouples 5 and 10), measured output module power and calculated I^2R efficiency are tabulated:

<u>Time (hr)</u>	<u>C_T ($\mu\Omega\text{-cm}^2$)</u>	<u>I^2R</u>
88	500	0.89
3,574	1700	0.911
10,018	4200	0.922

As indicated above, the C_T reflected by the performance test data at 3574 hours is well below the maximum design C_T of $4500 \mu\Omega\text{-cm}^2$ for the SNAP 29 module. Based upon the rate of change in C_T over the last 5000 hours of testing, the design value of C_T ($4500 \mu\Omega\text{-cm}^2$) would be exceeded in 1000 additional test hours. However, it should be indicated that the increase in C_T with time, as reflected by the above values, is optimistic, since both the hot and cold junction temperatures were operated at $\sim 90^\circ$ and 50° F below their SNAP 29 design values.

- (3) Open-circuit voltage--Due to variations in hot and cold junction temperatures, and subsequently the variation in ΔT across the elements when E_{OC} measurements were taken, the effect of long-term performance testing on E_{OC} was evaluated by comparing measured E_{OC} with predicted E_{OC} (based on 3 M property data) using the hot and cold junction temperatures reflected by Thermocouples 5 and 10. The absolute values of E_{OC} (predicted and measured) and the percent deviation with respect to time are tabulated below:

<u>Time (hr)</u>	<u>E_{OC} Predicted (volts)</u>	<u>E_{OC} Measured (volts)</u>	<u>Deviation (measured versus predicted) (%)</u>
88	5.325	5.628	+5.6
3,574	5.80	5.780	-0.3
10,018	6.325	5.969	-5.6

Replacement of the heater and thermal insulation in the module test fixture after approximately 4900 hours of testing

resulted in slightly higher hot junction temperatures (~30° F) and subsequently higher measured E_{OC} values (i.e., cold junction temperature remained essentially unchanged).

- (4) Internal resistance--The absolute values of internal resistance (R_I) and percent deviation with respect to time are tabulated below:

<u>Time (hr)</u>	<u>R_I (ohms)</u>	<u>Deviation (%)</u>
88	0.123	--
3,574	0.152	+23.8
10,018	0.174	+41.5

In order to obtain a true evaluation of the performance of Martin Marietta Research Module S/N 1, as reflected by life test data, primary consideration had to be given to hot and cold junction operating temperatures. Examination of test data indicated that significant variations existed in the six hot junction thermocouple readings. From the test data (using only data points recorded at ~500-hour increments from time = 88 hours to 10,018 hours), it was determined that the maximum and minimum deviations in T_H readings were 125° and 50° F, respectively. This represented an average deviation of 75° F for the 22 data points examined. In addition, two of the hot junction thermocouples failed during the test program. Based on the wide variation exhibited in the T_H thermocouple readings and the failure of two thermocouples during the test program, it was considered that use of an arithmetic average of hot junction thermocouple readings for evaluating module performance may not necessarily provide accurate or meaningful results.

To further examine this aspect, a comparison was made of measure open-circuit voltage (E_{OC}) with predicted E_{OC} determined from 3 M (2N and 2P) property data for an 84-couple, parallel-series module. For this comparison, the average of all five cold junction thermocouple readings was used, along with the average of the four operable hot junction thermocouple readings (i.e., Thermocouples 9 and 11 failed during the test programs). Table II-25 lists the results of this comparison.

TABLE II-25
Measured Open Circuit Voltage Versus
Predicted E_{OC}

<u>Time (hr)</u>	<u>T_H (avg) (°F)</u>	<u>T_C (avg) (°F)</u>	<u>E_{OC} Predicted (volts)</u>	<u>E_{OC} Measured (volts)</u>	<u>Deviation (%)</u>
88	828	268	6.05	5.628	-6.97
2,202	888	295	6.625	6.239	-5.8
4,058	869	295	6.375	5.789	-9.2
5,996	909	289	6.925	6.230	-10.0
8,081	900	280	6.875	6.090	-11.3
10,018	890	267	6.850	5.969	-12.9

$$*\% \text{ deviation} = \frac{E_{OC} (\text{meas}) - E_{OC} (\text{pred})}{E_{OC} (\text{pred})} \times 100\%$$

Due to the large deviation between measured and predicted E_{OC} on previous SNAP 19 generator data and SNAP 29 couple data (~2%), the following method was used to determine the most representative value of hot junction temperature reflected by the test data:

- (1) Using the arithmetic average of the five cold junction thermocouple readings and the measured open-circuit voltage (E_{OC}), effective average hot junction temperatures for the 22 data points were determined from 3 M (2N and 2P) property data for an 84-couple, parallel-series module.
- (2) A comparison of these T_H values with the six T_H thermocouple readings indicated that the temperatures reflected by Thermocouple 10 (located in the center of the module) were most representative of the true hot junction temperature. These temperatures are illustrated graphically in Fig. II-62, along with the average of the six T_H thermocouple readings.

Based upon the above analysis, the hot junction temperature values reflected by Thermocouple 10 were used for further evaluation of module performance.

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Fig. II-62. Research Module S/N-1 Hot Junction Temperature Versus Time

Due to variations in T_H and T_C and, subsequently, the ΔT between hot and cold junctions for different data points, it was considered necessary to evaluate E_{OC} by comparing the measured value against the predicted value based on 3 M (2N and 2P) property data (using Thermocouple 10 for T_H and average T_C thermocouple readings). Since cold junction Thermocouple 5 and hot junction Thermocouple 10 were attached directly to the same couple, a similar comparison of E_{OC} predicted and E_{OC} measured was also performed. The two values of predicted E_{OC} determined above, along with the measured E_{OC} , are illustrated graphically in Fig. II-63.

A partial summary of the results of this comparison is given in Table II-26 tabulated in terms of a ratio between measured and predicted E_{OC} .

TABLE II-26
Ratio Between Measured and Predicted E_{OC}

Time (hr)	$\frac{E_{OC} \text{ (measured)}}{E_{OC} \text{ (predicted)*}}$	$\frac{E_{OC} \text{ (measured)}}{E_{OC} \text{ (predicted)**}}$
88	1.08	1.06
2,202	0.99	1.002
4,058	0.97	0.98
5,996	0.977	0.97
8,081	0.967	0.96
10,018	0.95	0.94

* E_{OC} (predicted) using Thermocouple 10 and T_C (avg).

** E_{OC} (predicted) using Thermocouples 10 and 5 for hot and cold junction temperatures, respectively.

As the above table indicates, an almost insignificant difference exists between using the temperature reflected by Thermocouple 5 for T_C or that reflected by the average of the five cold junction thermocouple temperatures. Over the design life of a SNAP 29 module (~3500 hours),

Fig. II-63. Research Module S/N-1 Measured and Predicted Open Circuit Voltage Versus Time

Thermocouple 5 offers better results, but for extended testing (over 6000 hours), the average cold junction temperature offers better results. However, since cold junction T/C 5 and hot junction T/C 10 were directly attached to the same couple during the test program, it was determined that the junction temperatures reflected by T/C 5 and 10 should be used for any further evaluation of module performance. Using the above criteria with respect to hot and cold junction temperature value (i.e., $T_H = T_{10}$ and $T_C = T_5$), the effect of life testing on contact resistivity and power was evaluated in the following manner:

- (1) Using the dimensions of the hot shoe, cold straps and internal wiring, the total resistance (13.5×10^{-3} ohms) associated with the above components was calculated.
- (2) Using the value determined in (1) above, the I^2R efficiency (η_{I^2R}) was determined from the formula

$$\eta_{I^2R} = \frac{1 - 13.5 \times 10^{-3} \text{ ohms}}{R_I}$$

where R_I is the measured internal resistance at each of the 22 data points evaluated.

This I^2R efficiency value is used to determine net power or gross power according to the following relationship:

$$P_{\text{net}} = \eta_{I^2R} P_{\text{gross}}$$

- (3) Using the graph of gross power versus T_H for various T_C and C_T (based on 3 M property data) and the measured values of R_I (for use in above formula), net power, T/C 5 and T/C 10 temperatures, the C_T for each of the 22 data points was determined.
- (4) Using the same graph indicated in (3) above, a graph of gross power versus C_T for design hot and cold junction temperatures of 925° and 350° F was plotted. Using the C_T determined in (3) above, the normalized gross and, subsequently, net power was determined.

The C_T determined by the method described in (3) above is illustrated graphically in Fig. II-64, along with the total measured internal resistance. The normalized power in addition to the hot and cold junction temperatures (T/C 10 and T/C 5) used to derive C_T and power are illustrated graphically in Fig. II-65.

For purposes of comparison, the differences which would result from using average T_H and T_C thermocouple readings or using T/C 10 and T/C 5 on normalized power and contact resistivity were determined. These results are shown in Table II-27.

TABLE II-27
Average T/C Readings Versus Normalized
Power and Contact Resistivity

	Time (hr)	Normalized Net Power (watts)	Contact Resistivity ($\mu\Omega\text{-cm}^2$)
Using	88	69.60	500
T/C 5 and	3,574	62.68	1700
T/C 10	10,018	49.79	4200
Using	88	61.23	1700
average	3,574	51.02	3900
T_H and	10,018	44.07	6100
T_C^*			

* T_C (avg) temperatures determined from average of Thermocouples 1, 2, 3, 4 and 5.

T_H (avg) temperatures determined from average of Thermocouples 6, 7, 8 and 10.

The contact resistivity, derived in accordance with the manner described in (3) above, for time = 4058 hours and using average T_H and T_C temperature values was determined to be $4300 \mu\Omega\text{-cm}^2$. Based on the SNAP 29 design criteria ($4500 \mu\Omega\text{-cm}^2$), this module would not continue to meet the SNAP 29 goals over a period in excess of 4100 hours of operation.

Fig. 11-64. Research Module S/N-1 Internal Resistance and Contact Resistivity Versus Time

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Fig. 11-65. Research Module S/N-1 Individually Measured Hot (T/C No. 10) Junction and Cold (T/C No. 5) Junction Temperatures Plus Normalized Power Based on Those Temperatures as a Function of Time

i. Research Module S/N 4

The primary purpose of the S/N 4 module tests was to obtain preliminary data on a SNAP 29-type module configuration. The areas of prime interest were cold-end hardware characteristics, power circuit performance and electrical heat source characteristics. (The latter was only of academic interest at this time, since the heat source used in this test simulated the Haynes-25 honeycomb fuel block.) Table II-28 is a summary of the S/N 4 module performance test results.

TABLE II-28
Summary of S/N 4 Performance

Date	2/16/67	3/1/67
Elapsed operating time (hr)	43	354
Average hot junction temperature (°F)	927	925
Average cold junction temperature (°F)	341	335
Average coolant temperature (°F)	271	267
ΔT (cold junction to coolant) (°F)	70	68
Coolant flow rate (gpm)	1.07	1.12
Module pressure drop (psi)	--	~0.25
Input power (w)	1140	1140
Output power (w)	44.9	44.3
Open-circuit voltage, E_{OC} (measured) (v)	5.49	5.47
Open-circuit voltage, E_{OC} (predicted) (v)	6.75	6.78
Load voltage, E_L (v)	2.74	2.73
Load current, I_L (a)	16.4	16.2
Load resistance, R_L (m Ω)	167	169
Internal resistance, R_i (m Ω)	168	169
Contact resistivity ($\mu\Omega$ -cm ²)	~6000	~6000

A correction factor for dimension difference and heat rejected differences must be applied to the measured ΔT , between the cold junction and coolant, before an estimated ΔT for the SNAP 29 cold-end hardware is obtained. After being adjusted, the ΔT is ~90° F (2N-2P T/E elements) and ~115° F (2N-3P T/E elements) for the SNAP 29 module reference design.

The cold end of the S/N 4 module was overheated during outgassing. This could have affected the thermocouples and the cold junction-T/E bonds. Because of the overheating and the discrepancy between the predicted and measured open-circuit voltages, the results from S/N 4 tests should not be considered conclusive.

The temperature distribution in the heat source is illustrated in Fig. II-66. The gradient along the axis of the heaters varied from 50° to 80° F. There was a relatively large gradient between heater elements (~150° F) which was caused by the low thermal conductivity of the honeycomb.

j. Automatic data acquisition system

A list of calculated parameters to be used in the evaluation of module performance test data was compiled and issued. The parameters will be calculated using a partial set of data read directly into the computer from the test console. The computer printout will be employed in the continuous monitoring of module performance during testing. The data and calculation requirements are summarized.

(1) Power (primary circuit) (watts) $P = E_L I$

(2) Power (pump circuit) (watts) $P_P = E_L I$

(3) Load resistance (primary circuit) $R_L \text{ (mr)} = \frac{E_L}{I}$

(4) Average hot junction temperature (°F) $\bar{T}_H = \frac{\sum_{i=1}^N T_{H_i}}{N}$

(5) Average cold junction temperature (°F) $\bar{T}_C = \frac{\sum_{i=1}^N T_{C_i}}{N}$

(6) Average coolant temperature (°F) $\bar{T}_B = \frac{T_{BI} + T_{BO}}{2}$

(7) T/E element temperature drop (°F) $\Delta T_{T/E} = \bar{T}_H - \bar{T}_C$

(8) Cold-end hardware temperature drop (°F) $\Delta T_{CE} = \bar{T}_C - \bar{T}_B$

Fig. 11-66. S/N 4 Module Heat Source Temperature Profile

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(9) Adjust power (primary circuit) (watts) $P_{adj} = P \left[\frac{\Delta T_{ref}}{\Delta T_{T/E}} \right]^2$

$\Delta T_{ref} = T_H - T_C$ -- The hot and cold junction temperatures are listed in Table IV of the test specification (466A1343042) for all test units.

(10) Elapsed operating time (hours) T -- Time zero corresponds to the first data recorded at steady-state conditions.

(11) Adjusted power ratio (watts) $PR = \frac{P_{adj}(\text{time } t)}{P_{adj}(\text{time } 0)}$

(12) Degradation (%) $\Delta P = [1 - PR] 100$

(13) Degradation rate $\left[\frac{\%}{1000 \text{ hr}} \right] D = \frac{\Delta P}{T}$

Additional considerations

If E_{OC} is transmitted to computer add:

(14) Internal resistance (primary circuit)(m Ω) $R_I = \frac{E_{OC} - E_L}{I}$

(15) Load ratio $x = \frac{R_L}{R_I} = \frac{\text{load resistance}}{\text{internal resistance}}$

If x is not in limits $0.9 < x < 1.1$, star the value for x.

Should the difference between the individual hot or cold junction temperatures be greater than 50° F, the calculations listed from (7) to (12) shall be performed for each temperature reading.

The calculations listed will not apply during the time when the modules are not at steady-state conditions. During the thermal cycling, the hot junction and cold junction temperatures will be recorded as noted in the test specification (466A1343042).

Thermocouple numbers should be an integral part of all temperature printouts from the computer.

k. T/E module performance maps

A set of performance maps for the SNAP 29 modules have been generated. These data are for 2N-3P couples ($D_N = D_P = 0.56$ in.), 90 couples per module. Maps were also generated for single couples (2N-3P) of 0.56-inch diameter. The performance data are presented in Figs. II-67 through II-73.

l. Manufacturing and tooling

T/E Module Assembly N0023138-009 was completed and delivered to Engineering.

Manufacturing processes and tooling were completed using 0.560-inch diameter elements with Ni hot shoes and Ni-plated cups.

Manufacturing development was completed for determining the optimum method, temperature and sequence for assembling T/C wires to couples.

The thickness of nickel coating on cups and the elimination of the counterbore on P-elements was resolved.

Piston hardcoating and finishing processes were completed and submitted to Engineering.

A process for cleaning lead parts was developed using an electro-polish technique.

In the T/E conversion assembly, a process was developed whereby an ultrasonic welder was used to achieve the bond of the strap and wire without using solder or brazing material.

A nondestructive test process was developed for the module piston tester. A controlled sample of pistons is to be made to validate fabrication methods.

Fabrication effort was started on the following:

- (1) Dummy module hot plate (466A1344059)
- (2) Module test fixture (static) (466A1344057)
- (3) Module thermal cycle test fixture (466A1344045)
- (4) Module dynamic test fixture (466A1344060).

Fig. II-67. E_{OC} (predicted) for SNAP 29 T/E Module (primary circuit)

Fig. II-68. Power Versus Operating Parameters--SNAP 29 Module (primary circuit)

Fig. II-69. Power Versus CT--SNAP 29 Module (primary circuit)

Fig. II-70. Q (elements) Versus Operating Parameters SNAP 29 Module

Fig. 11-71. E_{OC} (predicted) for One Couple

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Fig. II-72. Single Couple Power Versus Operating Parameters

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Fig. II-73. Single Couple Power Versus CT

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E. THERMAL CONTROL SUBSYSTEM-- CONTROL POINT 1500

The objective of the effort within this control point is to develop a thermal control subsystem which will maintain the thermoelectric module hot junction temperatures within 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 shutters.

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

- (1) CP 1540--Component Development
- (2) CP 1530--Assembly Qualification
- (3) CP 1520--Prototype System

This report includes the work conducted under CP 1540 and CP 1530 which was accomplished during this reporting period.

1. Summation of Accomplishments

a. Shutter design

The basic shutter design has undergone several changes. These consist of the use of titanium honeycomb structure to reduce weight and the addition of perforations to the insulation retention foil in order to reduce the parasitic heat losses through the foil. A 0.010-inch thickness of "Astro-Quartz" cloth has been placed between the Min-K insulation and the perforations to prevent the Min-K from flaking out during vibration. The shutter assembly drawing was completed and released during this period. The present shutter design is shown in Fig. II-74. In addition, the shutter thermal performance test fixture and shutter vibration test fixture assembly drawings were completed and released.

b. Shutter vibration test

Vibration tests were completed on the stainless steel boilerplate shutter. The purpose of these tests was to perform a resonance survey, determine magnification factors and determine structural integrity under dynamic loading conditions. Visual post-test inspection of the shutter revealed local deformation in the insulation retaining foil and some signs of wear in the area of the bearings. Neither of these factors is considered to indicate a failure. Detailed analysis of the test results is incomplete at present and a discussion of the results will be deferred until the next quarterly report. Figures II-75 through II-77 show the test setup and the damaged foil.

Fig. 11-74. Shutter Assembly

Fig. II-75. Boilerplate Shutter Test Setup

Fig. II-76. Boilerplate Shutter Test Setup

Fig. II-77. Damaged Foil on the Shutter

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c. Actuator design

A major perturbation occurred in this area due to the bankruptcy of the originally selected vendor, the Aeroflex Corporation. It was therefore necessary to reopen procurement for the actuator. Bids were received from three vendors; Flexonics Division of Calumet and Hecla Corporation, The Flexible Metal Hose Company and the Metal Bellows Corporation. The Flexible Metal Hose Company proposal was accepted. Delivery of these units is expected early in the next reporting period.

The actuator cycling test fixture and the actuator pressure test fixture assembly drawings were released during this period. In addition, the actuator pressure test and cycling test specifications were completed, reviewed and released. A NaK filling process specification for use with the actuator cycling test was also completed, reviewed and released.

A preliminary performance analysis of the actuator system was completed during this period. The purpose of this analysis was to determine the basic equation of motion as a function of reservoir temperature in order to evaluate the basic factors which affect the performance of the actuator. The resultant equation of motion is

$$\Delta X = \frac{V_T}{A} \left[\frac{\Delta T (\phi - 3C) - 3C^2 \Delta T^2 - C^3 \Delta T^3}{1 + \frac{K}{A^2} \left(\frac{V_T}{E} + \frac{1}{K_S} \right)} \right]$$

where:

ΔX = the external travel of the actuator (inches)

V_T = the total volume of NaK in the system (cubic inches)

A = the net actuator area (square inches)

ΔT = the change in temperature ($^{\circ}F$)

ϕ = the cubical expansion coefficient of NaK ($\text{in.}^3/\text{in.}^3\text{-}^{\circ}F$)

C = the linear expansion coefficient of the reservoir material ($\text{in.}/\text{in. -}^{\circ}F$)

K = the external linkage spring constant, referred to the actuator ($\text{lb}/\text{in.}$)

E = the bulk modulus of NaK

K_S = the combined structural spring constant of the tubing and bellows.

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d. NaK reservoir hydrostatic test

A hydrostatic burst test at room temperature conditions was performed on the T/C system NaK reservoir and tubing assembly, Dwg. 466A1541014-049. The purpose of this test was to establish a margin of safety of the assembly tubes and welded joints.

The data obtained were:

Burst pressure: 12,000 psig

Yield pressure: approximately 10,000 psig

Failure occurred on one of the main reservoir tubes. Both reservoir tubes were deformed such that their diameter increased from 0.625 inches to an average of 0.715 inches (approximately).

As related back to operating temperature (925° F), the following design parameters are obtained:

P_{ult} = 7550 psig (based upon a decrease to 63% of ultimate strength at 925° F)

P_{yield} = 7550 (approximately) (based upon a decrease to 75% of yield strength at 925° F)

Calculated stress at burst was 73,500 psi average.

Based upon these results (CP 1530), the design will be changed to reduce the reservoir tube wall thickness from 0.049 to 0.032 inch.

At 925° F, the ultimate pressure is 4840 psig and the yield pressure is 4810 psig (approximately) for an 0.032-inch wall thickness tube. These values are based upon the test results. A reduction in tube wall thickness will result in a decrease in system stiffness. However, this should be relatively minor, since the overall system stiffness is determined primarily by the 1/8-inch tubes, the bellows and the NaK itself, all of which have less stiffness than the 0.032-inch wall thickness reservoir tubes. The calculated spring constant for the 0.032-inch tubes is 31,390 lb/in. referred to the actuator.

Figures II-78 and II-79 show the reservoir test specimen after hydrostatic test.

e. Linkage and bearing design

During this reporting period, the basic linkage assembly drawing was completed and released, and the bearing/lubricant test fixture design was completed and released. In addition, the bearing test specification, as well as process specifications for the application of Molykote X-15 and tungsten diselenide dry film lubricants were completed, reviewed and released.

Fig. II-78. Reservoir Test Specimen

Fig. II-79. Detail of Failure in Reservoir Test Specimen

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The initial design of a separable linkage was completed and is currently being reviewed. The initial linkage design depended upon the breaking of tubing during housing and fuel block separation. This design will provide a separable joint in the linkage, thereby eliminating the need to break tubing in order to separate the fuel block. Initial design and coordination efforts were initiated during this period for the assembly-qualification tests. Two basic types of tests are involved: a reliability test, wherein eight complete subsystems will be vibrated to failure; and a performance test, wherein three complete subsystems will be subjected to vibration and shock tests to qualification levels, following which they will receive thermal vacuum performance tests.

f. Shutter thermal/weight studies

Analytical studies were conducted to examine the potential improvement in the shutter end-of-life parasitic heat loss and shutter weight. Three design variations were compared with the current titanium H/C, Min-K 2000 encased in 2-mil Haynes-25 foil shutter design. Design variations considered included perforation of the Haynes-25 foil along the sides of the shutter to minimize the conductive heat short from the inside shutter face to the outside shutter face and the substitution of either Johns-Manville Dyna-Quartz insulation or the new Min-K-2002 insulation for the current Min-K-2000 insulation. In order to protect the insulation from abrasion in the perforated designs, it was assumed to be wrapped in a 5-mil thick tight weave ceramic cloth called Astro-Quartz cloth (J. P. Stevens Co., New York). The heat leak associated with this cloth wrap was also included in the thermal calculations. Table II-29 presents a comparison of the three systems examined with the current system for a fixed fuel block temperature of 1500° F and a constant insulation thickness of one inch.

TABLE II-29

Comparison of T/C Shutter Designs

Shutter System (Ti H/C structure)	Parasitic Heat Loss (Btu/hr) (watts)*	% EOL		
		$\frac{Q \text{ Foil}}{Q \text{ Total}}$ (%)	Weight Savings (lb)	Thermal Inventory (14 kw)
1. Min-K 2000 (1-inch thick) with unperforated Haynes-25 foil (2 mils)	2005 (589)*	24.6	0 (ref.)	4.2
2. Min-K-2002 (1 inch thick) encased in Astro-Quartz cloth and 22% perforated Haynes-25 foil (2 mils)	1645 (482)*	24.9	0	3.45

TABLE II-29 (continued)

	Shutter System (Ti H/C structure)	Parasitic Heat Loss (Btu/hr) (watts)*	$\frac{Q_{\text{Foil}}}{Q_{\text{Total}}} (\%)$	% EOL	
				Weight Savings (lb)	Thermal Inventory (14 kw)
3.	Dyna-Quartz (1 inch thick) encased in Astro-Quartz cloth and 22% perforated Haynes-25 foil (2 mils)	3020 (885)*	11.5	8-9	6.32
4.	Min-K 2002 (0.47 inch) encased in Astro-Quartz cloth and 22% perforated Haynes-25 foil (2 mils)	3020 (885)*	>11.5	6-6.5	6.32

Fuel block temperature = 1500° F,

$\epsilon_{\text{foil}} = 0.3$

$\epsilon_{\text{fuel block}} = 0.85, \epsilon_{\text{Ti H/C}} = 0.2$

The lowest parasitic heat loss, for the one-inch thick designs, was exhibited by the Min-K 2002 with perforated foil design. For the same weight as the Min-K-2000 design, an 18% reduction in heat leak was calculated.

Approximately 8 to 9 pounds of weight can be saved using the Dyna-Quartz design, but this approach exhibited nearly double the heat leak of the Min-K-2002 design. Further, for equal thermal performance, only 2 to 3 pounds of weight could be saved by utilizing the Dyna-Quartz insulation over using the Min-K 2002 insulation.

The indication is that the Min-K-2002 should be considered to replace the Min-K-2000, and the foil should be perforated to as high a percent as practical above the 22% mean perforation studied. It is obvious, however, that even large increases in perforation percentage will lead to diminishing returns, because the percentage of the total represented by this thermal short at 22% perforation is relatively small to begin with (as noted in Table II-29).

g. Shutter emergency heat dump capability

In the event of a heat rejection subsystem coolant failure, the thermal control shutters must be capable of dumping (or rejecting) the entire thermal inventory of the fuel block either in the atmosphere or in the vacuum of space (worst condition), while preventing the fuel block

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material from rising above its melting temperature. The reference design shutter (i.e., the 1/2-inch thick, 6Al-4V, titanium honeycomb structure, insulated with 1-inch thick Min-K 2000 encased in 2-mil Haynes-25 foil) was used as the basis for the analyses, and all six shutters were assumed wide open (90° position). In the 90° position, about 30% of the total opening (30 inches x 33.7 inches) is blocked by the shutters themselves. Table II-30 presents the equilibrium fuel block temperatures for the emergency cases in air and vacuum and for a fictitious pre-emergency case, assuming the shutters are full open and the required thermal rejection rate is normal. It is to be noted that under the worst thermal condition (vacuum-full inventory), the emergency heat dump capability of the reference shutter design is sufficient to limit the fuel block maximum temperatures to well below 1700° F.

TABLE II-30

6Al-4V Ti H/C-Min-K 2000 Shutter Design-Emergency Heat
Rejection Capability (90° open position)

Condition	Thermal (kw) Inventory Rejected	Fuel block Temperature (°F)
Emergency-vacuum	25	1661
Emergency-vacuum	20	1544
Emergency-air	25	1431
Emergency-air	20	1312
Normal-vacuum	12	1301
(wide open)	10	1221

Fuel block $\epsilon = 0.85$ Outside shutter $\epsilon = 0.8$
Inside shutter $\epsilon = 0.6$ Air convective coefficient $h = 3 \text{ Btu/hr-ft}^2\text{-}^\circ\text{F}$

h. Manufacturing and tooling

Fabrication of NaK system assembly (Dwg 466A1541014) was completed.

Thermal vacuum shutter bearing (test specimen) was completed.
(Ref. Dwg. 466A1544002).

The following items are in work:

Fabrication of bearings (466A1541001) is in process.

Honeycomb core for shutter structure is in process.

Fabrication of bearing specimen test fixture (466A1544004-009) is in process.

Thermal controls titanium shutters (466A1541011-009 and -019) were started.

The fabrication of thermal controls linkages (466A1541004) is in process.

Titanium shutter performance test fixture (466A1544001) was started.

The fabrication of titanium shutter vibration test fixture (466A1544005) is in process.

Fabrication of the thermal control cycling test fixture (466A1544006) is in process.

Thermal control pressure test fixture (466A1544007) is in process.

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F. HEAT REJECTION SUBSYSTEM--CP 1700

The objective of this effort is to develop a subsystem capable of dissipating the waste heat from the thermoelectric module subsystem to a space environment during its mission life. As a result of the recent reprogramming effort, discussed in the Introduction, the flight hardware phase has been eliminated from this control point.

This subsystem consists of the electromagnetic pump, radiator, two accumulators, a coolant manifold assembly and required interconnecting tubing.

The heat rejection subsystem will be designed, fabricated, inspected and tested under this control point.

The effort is divided into the following subtasks:

CP 1740--Heat Rejection Subsystem--Component Development

CP 1730--Heat Rejection Subsystem--Assembly Qualification

CP 1720--Heat Rejection Subsystem--Prototypes.

The effort during this past quarter was concentrated on CP 1740 and on reprogramming the effort on all three control points. Control Point 1740 effort includes the design, fabrication, inspection and testing of the heat rejection subsystem components on an individual basis. The importance of this effort has been increased by the fact that in the new program, the qualification of the heat rejection (H/R) subsystem will not occur until the time of the breadboard and prototype system tests. Component development will be undertaken in the following areas:

- (1) Electromagnetic pump
- (2) Accumulator
- (3) Space radiator
- (4) T/E coolant manifold.

In the first quarterly progress report, a summary of the Phase I H/R subsystem design was presented together with the significant design changes which had taken place in the interim. The following reports the progress made in the development of each component of the heat rejection subsystem during this quarter.

1. Summation of Accomplishments

a. Electromagnetic NaK pump

The development plan for the NaK pump, which is being developed under subcontract to MSAR, remains essentially the same in the new program. However, the number of deliverable pump units has been reduced by four to reflect the elimination of the flight hardware.

The effort this quarter has been primarily confined to completion of the test specification which covers the NaK (EM) pump development tests to be conducted at Martin Marietta, Baltimore Division. These tests include life performance tests (4 units), overtemperature tests (3 units) and vibration and shock tests (9 units each). A description of these tests is given in the revised SNAP 29 Program Plan. In addition, the MSAR-generated development test procedures for the following pump tests were reviewed:

- (1) Performance tests (MSAR 67-46)
- (2) Life tests (MSAR 67-44)
- (3) Wetting tests (MSAR 67-45)
- (4) Gas entrainment tests (MSAR 67-43).

b. Accumulators

Preliminary design and analysis of the accumulator were completed during the quarter. The design is based on the assumption of a hot (300° F) fill of the NaK coolant system, and the expansion of the NaK fluid to 400° F in operation. The final component design will be predicted upon the results of the MSAR gas entrainment tests conducted in the pump development program, and the results of the bellows cycle life and vibration tests conducted under CP 1740.

The test specification for the bellows cycle life and vibration tests was completed this quarter. The bellows cycle life will be determined by simultaneously cycling nine units in a closed loop hydraulic (oil) system at the operating temperature of 320° F. Nominal stroke lengths of 0.125, 0.25 and 0.50 inch will be used on three units each. Cycling will be conducted at 3 cps until failure or sufficient reliability data are obtained. The vibration tests will be conducted (under operating conditions) with the bellows compressed to a stroke of 0.50 inch at 320° F. After establishing the most critical of the two major axes (only two axes because of symmetry) by vibration to failure of two units, six additional bellows will be vibrated to failure in the critical axis. The vibration

tests will consist of nondestructive resonance tests, and sinusoidal and random vibration tests as outlined in the revised Program Plan.

c. Coolant manifold

Preliminary design and analysis of two manifold configurations which will be tested hydraulically under CP 1740 were completed this quarter. Both designs consist of a 1-1/4-inch diameter (16-mil wall) stainless steel inlet header, and two 1-inch diameter (16-mil wall) stainless steel outlet headers. The two designs differ primarily in the diameter of the eight feeder tubes from the inlet header to the modules and the eight feeder tubes from the modules to the outlet headers, i.e., one design incorporates 5/8-inch diameter (16-mil wall) feeders and the other 1/2-inch diameter feeders.

The hydraulic test plan consists of screening out one of the two configurations during the water flow tests and subsequently testing the remaining one with NaK as the hydraulic fluid. The screening criteria will be primarily the pressure drop since the drop through this element of the circuit must be kept to within 0.2 to 0.25 psi because radiator pressure drop is expected to be about 0.5 psi.

The test specifications for the water and NaK hydraulic tests of the manifold were completed this quarter. The eight parallel-connected modules will be simulated in these tests using a dummy cold plate made of mild carbon steel. For the water tests, NaK 78 Reynolds numbers will be simulated using water at a temperature of 165° F; and flow distribution will be inferred from pressure drop measurements. For the NaK tests, the design inlet temperature of 280° F will be used, and the flow distribution will be determined directly through the use of a portable EM flowmeter.

The results of the preliminary hydraulic analysis of the two manifold designs will now be discussed.

Based on the module manifold model presented in Fig. II-80, the calculated pressure drop across each of the eight modules, including the inlet and exit feeder lines, is presented in Table II-31. In addition, the calculated NaK weight flows through each module and the total pressure drop from the manifold supply header to the exit headers are presented in Table II-31. Two feeder line sizes with 1/2- and 5/8-inch diameters are compared therein. It can be noted that a 48% reduction in total pressure drop (i.e., from 0.328 to 0.170 psi) resulted when the change from 1/2- to 5/8-inch diameter feeder tubes was made. For the 1/2-inch design, the variation in calculated flow from the nominal (or ideal) flow of 0.16 lb/sec per module was about $\begin{smallmatrix} +1 \\ -2 \end{smallmatrix}\%$ for the left bank of modules (i.e., modules 1 through 4), and $\begin{smallmatrix} +2.75 \\ -1 \end{smallmatrix}\%$ for the right bank of modules

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(5 through 8). The change from 1/2- to 5/8-inch diameter did have an effect on the flow distribution as shown in Table II-31, but the major effect was the reduction in total pressure drop as noted previously.

d. Space radiator

The space radiator component development (CP 1740) plan has been altered to meet the objectives of the revised program. The primary changes involve a deletion of component development related to vehicle interface requirements (e.g., the launch pad heat exchanger design and testing) while increasing the scope of radiator hydraulic analyses and testing to consider radiator diameters from 5 to 10 feet.

TABLE II-31

Module Manifold Hydraulic Results

<u>Module</u>	<u>1/2-in. OD x 0.016-in. Wall Feeders</u>		<u>5/8-in. OD x 0.016-in. Wall Feeders</u>	
	<u>Pressure Drop Including Feeders</u>		<u>Pressure Drop Including Feeders</u>	
	<u>(psi)</u>	<u>M (lbm/sec)</u>	<u>(psi)</u>	<u>M (lbm/sec)</u>
1	0.318	0.1587	0.140	0.1547
2	0.307	0.1593	0.140	0.1561
3	0.295	0.1612	0.127	0.1606
4	0.291	0.1568	0.124	0.1606
5	0.308	0.1599	0.141	0.1564
6	0.299	0.1608	0.131	0.1587
7	0.292	0.1644	0.124	0.1668
8	0.290	0.1589	0.122	0.1668
Total manifold including modules Pressure drop = 0.328 psi			Total manifold including modules Pressure drop = 0.170 psi	

The radiator component development test plan consists of hydraulic, vibration, shock and thermal vacuum tests. To satisfy the requirements for the parametric hydraulic data, four skeleton radiators (header and radiator tubes only) will be built and hydraulically tested using water.

Two will be selected for testing using NaK 78. The parametric hydraulic test plan is discussed in detail under Paragraph f. In addition, six one-half radiator assemblies will be fabricated for component testing, one for shock to failure tests, four for vibration to failure tests and one for thermal vacuum testing. Additional development effort for the radiator component includes emittance tests on candidate radiator thermal control coating schemes and manufacturing development of the techniques required to fabricate and assemble the radiator elements, i.e., skin-fin, meteoroid armor and coolant and header tubes. The thermal efficiency of the skin-armor coolant tube bonds are being determined in a small thermal vacuum test apparatus under the bimetallic tube test program. This test was initiated in the latter part of March and data are not available for presentation.

Test specifications were completed this quarter for the H₂O and NaK hydraulic tests and for the radiator shock and vibration tests.

The Advanced Materials Technology Laboratory is continuing process development to combine aluminum armor and stainless steel tubing for the radiator application. Difficulties have arisen in maintaining the distance between the tubing wall and the outside surface of the armor within acceptable limits while casting full length (7 ft) sections. The addition of more material to counteract the adverse effect of tolerances would result in an undesirable weight increase.

A method of diffusion bonding between the steel and aluminum is currently being attempted. Results to date on one-foot segments have been successful. This effort will continue during the next reporting period.

e. Radiator coating development

Emittance tests on promising candidate coatings continued this quarter. Three coatings that were tested appear to have the potential for satisfying the SNAP 29 system requirements. These are in (tentative) order of preference.

<u>No.</u>	<u>Coatings</u>	<u>Infrared Emittance</u>		
		<u>Normal</u> <u>(300°-400° F)</u>	<u>Total</u> <u>(300°-400° F)</u>	<u>Solar</u> <u>Absorptance</u>
1	ZnO + K ₂ SiO ₄	0.93 to 0.944	0.92 to 0.91	0.279
2	Hot paint, white	0.845 to 0.841	0.87 to 0.84	0.300
3	ZrO ₂ + NaSiO ₃	0.904 to 0.952	0.87 to 0.95	0.368 to 0.384

An analysis of the sensitivity of required radiator area to both emittance and solar absorptance was performed. The results, presented in Fig. II-81, show the area (normalized) required as a function of ϵ and α_s/ϵ ratio for a typical polar orbit involving near maximum solar input.

Coating No. 1 would be the obvious choice based on high emittance and relatively low solar absorptance. In the final selection, other factors such as ease of application and repair, durability and compatibility with the space environment will be considered.

f. Radiator hydraulic parametric studies

Studies involving the hydraulic aspects of various radiator-RTG configurations for integration with space vehicles with diameters from 5 to 10 feet were initiated this quarter. Primary emphasis was given to split radiators in a wrap-around configuration, but the parametric information being generated is applicable to radiators of any aspect ratio whether split or not.

In accordance with CCP-2062-7 (EDCS M00010), the following range of parameters are being investigated:

Total pressure drop (psi)	0 to 2.0
Radiator tube diameter (in.)	3/16 to 1/2
Header diameter (in.)	1/2 to 1-1/2
Tube spacing and number	Variable
Flow rates, NaK (lb/hr)	3000 to 6000
Flow loss coefficients	Variable
Tube and header lengths	Variable
NaK coolant temperatures (°F)	250 to 350

One of the objectives of these studies is to obtain sufficient data to permit a rapid prediction of overall radiator pressure drop for any configuration within the matrix, and to establish criteria for predicting the incipience of flow maldistribution in any given configuration. A second objective of these studies is the selection of four radiator hydraulic configurations for verification tests, including the establishment of actual flow loss coefficients for space radiators of this type. The selection criteria for these configurations are:

- (1) Minimum weight per unit heat rejected

Fig. II-80. Current Module Manifold Hydraulic Model

Fig. II-81. Radiator Area Requirements Versus Coating Properties

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- (2) Predicted pressure drops within the present SNAP 29 EM pump design (i. e., radiator $\Delta p \sim 0.5$ psi)
- (3) Predicted flow maldistribution within acceptable values (currently considered to be ± 5 to 10%).

To establish the range of parameters to be considered, the configurations shown in Table II-32 were considered. As indicated by Table II-32, the mounting position of the RTG and the location of the fuel loading door influence the percentage of the circumference which is available for mounting the radiator panels. The smaller this percentage, the longer the radiator tubes become in order to reject given amount of heat. In addition, if a pressure drop restraint is imposed, the radiator tube diameter must be increased. This results in increased panel weight due to greater armor, NaK and tube weight. On the other hand, greater usable circumferences result in configurations which are more likely to present flow distribution problems because of longer header runs and shorter radiator tubes.

The ability to predict a priori whether a given radiator tube and header geometry will present flow distribution problems is of considerable value in assisting the design selection. For space radiators employing liquid metals, such as NaK, the controlling resistance for heat rejection is the thermal radiation resistance. Therefore, a certain percentage of flow maldistribution is acceptable since the overall heat rejection capability is not significantly reduced by small variations in flow between tubes. A preliminary estimate is that the overall effect of a 10% flow maldistribution is about a 1.5 to 2% reduction in heat rejection capability.

A large number of radiator hydraulic configurations (analyzed on the computer) were studied to determine what criteria would predict the degree of flow maldistribution.

Qualitatively, it can be observed that flow maldistribution will increase when the resistance to the flow in the radiator headers increases relative to the resistance to the flow in the radiator tubes. The configuration geometric criterion, which was found to estimate the degree of flow maldistribution, is as follows:

$$\frac{\left(V^2 \frac{L}{d_i} \right)_{\text{tube}}}{\left(V^2 \frac{L}{d_i} \right)_{\text{header}}} = \left(\frac{d_{Hi}}{d_{ti}} \right)^4 \frac{L_t}{L_H} \frac{4}{(N_t)^2}$$

TABLE II-32
Configuration Considerations

<u>Number of RTG</u>	<u>RTG Mounting Relative to Radiator</u>	<u>RTG Mounting Attitude</u>	<u>Diameter (in.)</u>	<u>Usable Circumference for Radiator 5 to 10 ft</u>	<u>Notes</u>
1	Below	Vertical	5	183 to 372 in.	Attitude does not affect usable cir- cumference
1	Below	Horizontal	10		
1	Symmetrical	Vertical	5	114 to 302 in.	
			10		
2	Below	Vertical	5	177 to 366 in.	
2	Below	Horizontal	10		
2	Symmetrical	Vertical	5	39 to 228 in.	
			10		
2	Tandem Symmetrical	Vertical, RTG op- posing	5	114 to 302 in.	Same as 1. RTG symmetrical
			10		

*Usable circumference--After allowance is made for that part taken up by RTG, fuel loading door and interconnecting tubing runs.

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where

d_{Hi} = header tube inside diameter

d_{ti} = radiator tube inside diameter

L_t, L_H = radiator tube and header tube length, respectively

N_t = total number of radiator tubes

When this ratio is equal to or greater than 0.4 to 0.6, acceptable flow distribution should result. Variations in flow loss coefficients cause the spread in this value. In general, the lower the flow loss coefficients, the higher this ratio should be. Figure II-82 shows the approximate relationship between this criterion and the flow maldistribution. In any event, regardless of the actual flow loss coefficients, as long as the criterion ratio is above the band indicated by Fig. II-82, the flow maldistribution should be within acceptable limits. In addition, studies to date have shown that radiator flow maldistribution is not affected by operation at different average radiator temperatures with a given configuration.

This does not mean that locally cool or hot tubes in a given radiator configuration would not affect flow distribution, but that the differential momentum pressure drops associated with these variations are so small for liquid metals that their effect is relegated to second and third order effects as far as the overall flow distribution is concerned.

Previous studies have shown that the friction-induced pressure drop is by far the major component of the total radiator pressure drop for liquid metal systems. From a force balance on an element of fluid in a closed duct, the friction drop can be expressed as:

$$-dp_f = \frac{2fG^2}{\bar{\rho} g_c} \frac{dL}{d_i} \quad (1)$$

This expression, after integration and substitution of equivalent expressions for the friction factor, f , and mass flow per unit area variable, becomes

$$P = C_1 \frac{u^{0.2}}{\bar{\rho}} \left(\frac{\dot{w}}{N_t} \right)^{1.8} \frac{L}{d_i^{4.8}} \quad (2)$$

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Fig. II-82. Flow Maldistribution Criterion Radiator Parametric Hydraulic Studies

where

$$f \approx \frac{K}{N_{Re}^{0.2}} \text{ in turbulent flow regime}$$

$$G = \frac{\dot{w}}{A} = \frac{4\dot{w}}{\pi d_i^2}$$

$$N_{Re} = \frac{G d_i}{\mu} = \frac{4\dot{w}}{\pi \mu d_i}$$

K, C₁ are constants.

While Eq (2) applies to the nominal flow through a single tube of length, L, the nominal pressure drop through one of the parallel radiator tubes is a controlling factor in the overall pressure drop of the radiator, including the headers. The main factors modifying this pressure drop are the flow maldistribution and the flow loss coefficients. Accordingly, the computer runs made on a large number of radiator hydraulic configurations were analyzed to determine an analytical expression which would predict the total pressure drop within a few percent for any configuration within the matrix being considered.

The analytical expression determined was as follows:

$$\Delta P = 0.237 \frac{\left(\frac{\dot{w}}{N_t}\right)^{1.8} (1 + K_t)^{0.2} (1 + F)^{2.2} L_t}{144 d_i^{4.8}} \text{ psi} \quad (3)$$

where

L_t = radiator tube length (in.)

W = total flow rate (lb/sec)

N_t = total number of radiator tubes

K_t = average flow loss coefficient for radiator tubes

d_i = inside diameter of radiator tubes (in.)

F* = maximum positive flow maldistribution fraction, i.e.,

$$\frac{\dot{w}_{\max} - \dot{w}_{\text{nom}}}{\dot{w}_{\text{nom}}}$$

*Maximum flow rate in any one of parallel radiator tubes normally occurs in first tube encountered in header run for radiator configurations being considered here.

The usefulness of Eq (3) is twofold. First, its use permits the evaluating of the effect of varying parameters such as flow rate, radiator tube diameter, tube length, etc., on the overall pressure drop. For example, Fig. II-83 presents the dependence of pressure drop on nominal flow rate per tube and tube length for a fixed radiator tube diameter. The sensitivity of pressure drop to varying flow maldistribution and flow loss factors is also indicated in Fig. II-83 for two nominal flow rates. Alternatively, Fig. II-84 indicates the dependence of the radiator pressure drop on the radiator tube inside diameter and tube length for a fixed nominal flow rate per tube. Plots such as these are being prepared to cover the range of parameters outlined previously.

The second use of Eq (3) is in the optimization of radiator configurations when a pressure drop restraint is imposed on this component of the hydraulic circuit. That is, the radiator geometry (tube length, spacing, diameter, etc.) which provides the maximum heat rejection per unit of pressure drop can be determined as a function of the major design variables. By way of illustration, using approximate expressions for pressure drop and heat rejection capability, Fig. II-85 shows the tendency of the maximum heat rejected per unit pressure drop to optimize at tube spacings of 9 to 10 inches. Additionally, previous weight studies (see Monthly Progress Report, 12/66) have also shown that the maximum heat rejected per unit weight occurs at nearly the same tube spacing distances. It should be pointed out that Fig. II-85 is approximate, however, and is being refined based on the hydraulic and heat transfer data which have been compiled.

Assuming that the proposed flow maldistribution criterion, and Eq (3) are verified by the proposed hydraulic test program on skeleton radiator configurations, they should be useful tools in pre-establishing the acceptability of any contemplated radiator configuration. The necessary design refinements may then be made by detailed analysis of perturbations about this design point.

The selection of configurations for the parametric hydraulic tests involves simultaneously imposing several constraints upon the configuration. These restrictions are as follows:

- (1) Available circumference which is dependent upon radiator diameter and RTG location as previously discussed. This may not be a constraint, however, when the remaining restraints are imposed, since the desired configuration may not require the entire usable circumference.
- (2) Maximum allowable flow maldistribution.
- (3) Maximum allowable pressure drop for the radiator.

Fig. II-83. Radiator Parametric Hydraulic Studies

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Fig. II-84. Radiator Parametric Hydraulic Studies

LEGEND:

$$T_R = 300^\circ \text{F}$$

$$\epsilon = 0.90$$

$$t_F = 0.025 \text{ inch}$$

$$\dot{Q} = 5980 \text{ lb/hr}$$

$$\dot{Q} = 4608 \text{ lb/hr}$$

$$\dot{Q} = 3020 \text{ lb/hr}$$

$$L_F = \text{fin length, inch}$$

$$d_t = \text{tube ID, inch}$$

Correction Factors			
Root Temperature (°F)	Fin Length (in.)	Skin Thickness (in.)	$\frac{F(Q)_t}{F(Q)_{0.025}}$
300	4	0.015	0.835
300	4	0.040	1.105

Correction Factors					
Root Temperature (°F)	Fin Length (in.)	$\frac{F(Q)_T}{F(Q)_{300}}$	Root Temperature (°F)	Fin Length (in.)	$\frac{F(Q)_\epsilon}{F(Q)_{0.90}}$
250	3	0.785	300	4	0.96
250	4	0.80	300	4	0.95
250	5	0.826	250	4	0.9575
350	3	1.25	250	4	0.95
350	4	1.223	300	5	0.9675
350	5	1.18	300	3	0.85
			350	4	0.85
					0.9625

Fig. II-85. Radiator Parametric Studies

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(4) Heat rejection capability required, or in other terms the radiator area required.

(5) Minimum radiator weight.

It is obvious that satisfaction of all these restraints is not generally possible. However, simultaneous satisfaction of three or four of these can be analytically achieved, and the penalties for not achieving the remaining ones assessed and traded off. For example, as shown earlier, the flow distribution constraint can be expressed as:

$$\frac{d_{Hi}}{d_{ti}} \frac{L_t}{L_H} \frac{4}{(N_t)^2} = C_1 \approx 0.4 \text{ to } 0.6 \quad (4)$$

where

$$L_H = S \times N_t / 2$$

S = tube spacing

Likewise, the pressure drop constraint can be expressed as:

$$\Delta p = \frac{0.237}{144} \frac{\left(\frac{\omega}{N_t}\right)^{1.8} (1+F)^{2.2} (1+K_T)^{0.2}}{d_{ti}^{4.8}} = C_2 \approx 0.5 \text{ psi} \quad (5)$$

where

F = 0.1 or 10% or as selected.

The radiator area, A_R , can be expressed as:

$$A_R = \frac{S \times N_t \times L_t}{144} \text{ (ft}^2\text{)} \quad (6)$$

Simultaneous solution of Eqs (4), (5) and (6) for given requirements such as flow rate, area, pressure drop, maximum flow maldistribution, etc., results in a simple expression of geometric parameters which satisfies the restraints. For example, $N_t^{0.8} d_{ti}^{2.8} = C_3$, and is shown in the table.

A_R (ft ²)	N_t	d_{ti} (in.)	L_t (in.)	S (in.)	For
110	36	0.224	60.8	7.22	$C_1 = 0.5$
110	40	0.217	63	6.27	$C_2 = 0.5$
110	44	0.212	65.8	5.45	$W = 1.28$ lb/sec
					$F = 0.1$

After several iterations, the area which is dependent on the spacing can be adjusted to match the known heat rejection capability (from thermal analyses), or, alternatively, the spacing can be adjusted to match the known area requirements.

g. Radiator thermal analysis

The radiator thermal analysis has been computed utilizing a Martin Marietta 7094 digital program which is, in part, based on a North American Company program as reported in ASD-TDR-63-222, "Digital Programs for Establishing Steady-State Space Radiator Performance." The radiator area is determined for a given heat rejection requirement. The sensitivity of the radiator area to variations in heat rejection requirement and NaK flow rate is shown in Fig. II-86 for a fixed radiator exit temperature of 280° F and a high solar input type orbit. It can be noted that changing the flow rate about 45% results in a required area change of only 4.5%, since the radiation thermal resistance is controlling the heat transfer. For a fixed radiator inlet temperature, the trend shown by the three flow curves in Fig. II-86 would be reversed. On the other hand, if the flow rate is fixed, the sensitivity of the radiator area to variations in radiator exit temperature is shown in Fig. II-87 for a given configuration and orbit condition. Thus, a 10° F decrease in radiator exit temperature results in a required area increase of 5%.

h. Weight studies

The reference design radiator configuration is a 65-inch diameter radiator composed of 24 tubes with 5/16-inch outside diameters and 1-inch diameter headers.

Some preliminary studies on the effect of reducing the radiator aspect ratio (header length/tube length) on the weight of the 65-inch diameter radiator configuration were conducted. To determine the trends, the number of radiator tubes and their diameters were held constant while the header length, tube spacing and length were varied. For a fixed heat rejection requirement, decreasing the tube spacing (thus increasing fin efficiency) decreases the radiator area (and thus skin weight) required to reject a fixed amount of heat. Likewise, the header

Fig. II-86. Heat Rejected Versus Radiator Dimensions

Fig. II-87. Heat Rejected Versus Radiator Dimensions

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tube weight, contained NaK weight and header armor weight decrease. Unfortunately, however, the area decrease is not large enough to prevent lengthening the radiator tubes. The weight added by the longer radiator tubes, contained NaK and armor, as well as a slight increase in interconnecting tubing and armor weights, tend to compensate for the header weight reductions. The net result for this configuration is that the total weight reduction attendant with header length reduction is about 1.7 lb/ft. This reduction is somewhat optimistic, however, since the pressure drop restraints would require slight increases in the tube diameters to compensate for the longer tube lengths.

The alternative approach to this is to increase the number of tubes while decreasing header length, tube diameter and tube spacing. This approach is even less attractive from the weight reduction standpoint than the aforementioned one since the reduction in tube diameter is not sufficient to compensate for the added number of tubes and their associated NaK and armor weights. The net result is that the total system weight is nearly the same or slightly heavier, despite the reduced area due to higher fin efficiency.

i. Meteoroid protection studies

The initial radiator armor requirements for meteoroid protection were based on a probability of no meteoroid penetration of 0.9999. During this reporting period, this penetration criterion was reduced to 0.999. The following discussion outlines the procedure employed for establishing the protection requirements and identifies the subsystem weight reduction associated with the reduced reliability requirement.

The Aerospace meteoroid environment and penetration criterion as presented in Ref. II-3* are used as a basis for the armor requirements. The SNAP 29 orbital mission life is taken to be 90 days.

The meteoroid environment used in the Aerospace criterion is based on that proposed by NASA-MSB Bulletin EC-1 of November 8, 1963 for near-earth, cislunar and near-lunar space. In general, the meteoroid flux is composed of sporadic and shower meteoroids. The sporadic meteoroids constitute about 80% of the total and appear to have random orbits (i.e., unpredictable paths, velocities or locations). It has been established, however, that more than 50% of these orbits are within ± 15 degrees of the ecliptic plane so that the leading edge of the earth encounters more than the trailing edge. The shower meteoroids, on the other hand, constitute about 20% of the total flux, and appear to have a common

*Ref. II-3. Frost, V. C., "Aerospace Meteoroid Environment and Penetration Criterion," Report No. TOR-269 (4560-40)-2, Aerospace Corporation, 17 August 1964.

elliptic orbit which the earth, in its orbit, intersects once or twice a year. The shower fluxes may be one to five orders of magnitude higher than the normal sporadic flux background during those periods and last for hours to days, but these high fluxes are not encountered every year.

The yearly average sporadic meteoroid flux versus mass relation selected for the Aerospace criterion is presented in Fig. II-88 (taken from Ref. II-3). Since the mission time considered herein is less than one year (90 days for the SNAP 29 mission), the annual variation of both the sporadic and shower fluxes must be considered. However, to avoid constraining the vehicle to specific launch periods or mission duration, the Aerospace criterion is based on the maximum combined sporadic and shower fluxes for any time interval during the year (these critical combinations are presented in Ref. II-3). Thus, in the Aerospace criterion, for a specific mission duration, there is only one sporadic flux design ratio, K , and one shower flux design ratio, E , which is based on the yearly average rate of sporadic meteoroids.

The handling of each type flux is different in this criterion. The sporadic flux is assumed omnidirectional and therefore is assumed to have the potential for incidence on the entire vulnerable area, whereas the shower flux is assumed essentially unidirectional and thus is potentially incident only on the projected vulnerable area.

A vehicle in earth orbit is partially shielded from the meteoroid flux depending upon the orbit altitude. The design values for this shielding factor S_E , are presented in Fig. II-89. In addition to earth shielding, the vehicle configuration may provide additional shielding for the vulnerable area. For the anticipated SNAP 29 RTG vehicle configuration, an estimated design value for the vehicle interference factor, S_I , of 0.6 is being utilized (where $S_I = 1.0$ is zero shielding effect).

The penetration criterion proposed by the Aerospace document is based on the work of J. L. Summers (NASA TND-94) for single plate walls, a meteoroid velocity of 30 km/sec and a meteoroid density of 0.5 gm/cm^3 . For the SNAP 29 system, it has been assumed that a design to prevent penetration of the meteoroids is adequate since spalling of the coolant tube inner face should not cause any serious flow or plugging conditions; i. e., the NaK (EM) pump has no internal moving parts in the coolant stream.

Analysis. The required effective aluminum meteoroid armor thicknesses shown in Figs. II-90 and II-91 were computed using the following equations:

Fig. II-88. Aerospace Meteoroid Environment for Near Earth Orbit

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Fig. II-89. Meteoroid Protection Earth Shielding Factor Versus Orbit Altitude

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Fig. II-90. Aluminum Meteoroid Armor Thickness Versus H/R Subsystem Vulnerable Area

Fig. 11-91. Aluminum Meteoroid Armor Thickness H/R Subsystem
Vulnerable Area

- (1) The design meteoroid flux rate, N , is given by,

$$N = \frac{1 - P_{(x=0)}}{S_F (KA_V + FA_{V_p}) \tau} = \frac{\text{particles}}{\text{ft}^2\text{-day}}$$

where

- $P_{(x=0)}$ = the probability of no penetrations
 $1 - P_{(x=0)}$ = expected or average number of penetrations
 S_F = total shielding factor = $S_E \cdot S_I$
 S_E = earth shielding factor (see Fig. II-89)
 S_I = vehicle shielding (interference) factor
 τ = mission duration, days (90 days, SNAP 29)
 A_V = vulnerable area (ft^2)
 A_{V_p} = projected vulnerable area (ft^2)
 F = ratio of average rate of shower meteoroids during the time interval to the yearly average rate of sporadic meteoroids = 0.53 for 90 days (Ref. II-3)
 K = ratio of average rate of shower meteoroids during the time interval to the yearly average rate of sporadic meteoroids = 1.665 for 90 days (Ref. II-3).

- (2) The maximum meteoroid mass for which the armor must be designed is determined from the mass flux curve (Fig. II-88) or from the following equation which is valid for meteoroids with mass equal to or greater than 10^{-7} gram (as indicated in Fig. II-88).

$$\log_{10} m = \frac{10.423 + \log_{10} N}{-1.34} \text{ grams}$$

- (3) The required effective thickness of the meteoroid armor is thus given by:

$$t = 148 K_1 \left(\frac{m}{E\rho} \right)^{1/3} \text{ inches}$$

where

$K_1 = 1.5$ to prevent penetration

$= 2.25$ to prevent spalling of inner face

E = Young's modulus of armor at operating temperature (psi)

ρ = density of armor material (lb/in.³)

For aluminum armor to prevent penetration only, this equation becomes:

$$t = 2.18 m^{1/3} \text{ inches}$$

- (4) The effectiveness of metals other than aluminum which can be considered part of the meteoroid armor (e.g., the stainless steel coolant tubes) can be estimated from:

$$\frac{t_{Al}}{t_m} = \left[\frac{\rho_m}{\rho_{Al}} \frac{E_m}{E_{Al}} \right]^{1/3} \text{ inches}$$

Thus, stainless steel is approximately twice as effective as aluminum as far as meteoroid protection is concerned.

j. SNAP 29 meteoroid armor requirements

The total vulnerable area of the heat rejection system for the current RTG vehicle configuration (65-inch diameter radiator) has been estimated at 8.4 ft². This includes the projected frontal area of the radiator tubes, headers and exposed interconnecting tubing between the radiator and the RTG. Figure II-92 shows a design meteoroid effective aluminum thickness of 0.27 inch based on a nominal orbit altitude of 250 nautical miles, a vehicle interference factor of 0.6, and a probability of no penetration of 0.9999. The actual thickness of aluminum armor required for the radiator tubes is about 0.215 inch and for the radiator header tubes is 0.238 inch when the effectiveness of the radiator panel skin-fin and stainless steel coolant tubes are taken into account.

A reduction in the probability of no meteoroid penetration to 0.999 will effect a weight savings. In addition, a small change in the overall reliability (0.993 to 0.992) will result. The effect of this change on the required effective aluminum armor thickness is also shown in Fig. II-92. The effective thickness for the change is 0.151 inch of aluminum compared to 0.27 inch, a 44% change in thickness. An estimate of the potential weight savings in armor weight alone was made for the current configuration. This is given in the tabulation.

Fig. II-92. Meteoroid Armor Required Versus Heat Rejection Subsystem Vulnerable Area--90-Day Earth Orbit Mission

P (x = 0)	Required Effective Armor Thickness (Al) (in.)	Weight (lb)			Total Armor
		Radiator Tube Armor	Header Armor	IC Tube	
0.9999	0.27	40.3	25	3.41	68.7
0.9990	0.151	29.3	13.2	1.49	<u>44</u>
Estimated savings					24.7 lb

The weight savings indicated is about 36% of the original armor weight, but this value should be considered only as a preliminary estimate since configuration changes will affect the vulnerable area and thus the armor weight.

k. Manufacturing and tooling

Bimetallic round tube test panel (Drawing 466A1741012-039) was completed. The bimetallic tubing vacuum test fixture (Drawing 466A1744400) was also completed.

A manufacturing development program to cast the aluminum armor for the stainless steel tubing is in process. The casting tool has been completed and trial pours will be made upon availability of stainless steel tubing.

Tools and facilities for the alternate engineering designs for NaK system manifold, interconnecting tubing and radiator assembly are being planned by Manufacturing Engineering.

A test fixture for testing of radiator panel specimens was started by Manufacturing.

Manufacturing development which provides the best flow and manufacturing methods for forming and welding of thin wall stainless steel tubing for the radiator was completed.

Tooling for the NaK system manifold, interconnecting tubing and radiator assembly has been ordered by Manufacturing Engineering for fabrication of detail parts and weldments of thin wall stainless steel tubing.

The hydraulic radiator mockup (Drawing 466A1744403) was started as were the heat sink mockup assemblies (Drawing 466A1741019).

Heat rejection manifold assemblies (Drawings 466A1741017 and 466A1741018) were started.

G. STRUCTURES, SHIELDING, ELECTRICAL AND INSTRUMENTATION SUBSYSTEMS--CP 1900

This control point includes the RTG housing structure, electrical circuitry, shielding and instrumentation. The task is divided into the following subtasks:

CP 1940--Subsystem Component Development

CP 1930--Subsystem Assembly Qualification

CP 1920--Prototype Subsystem.

1. Summary of Accomplishment

a. RTG housing structure design

The basic RTG housing structure design has undergone several conceptual stages in design evolution. At the end of the previous reporting period the RTG housing design was based upon the concept of a housing which was split in a vertical plane. The halves were joined by a V-band clamp type device on the external perimeter. This is basically a strap clamping the halves through a wedge. Cutting or burning off of the strap at any point in its length would permit the housing halves to separate, allowing the fuel block to eject. The basic housing structure in this concept was composed of titanium honeycomb. As the fuel block support studies progressed, it became desirable to move the separation plane from the centerline. This caused the V-band clamp type strap to become increasingly less efficient, since it depended upon hoop tension in the strap. Since the basic shape of the housing is rectangular, the strap design became less efficient because its shape departed from a circle.

At this point in the design evolution, the use of flexible linear shaped charge (FLSC) or primacord, was investigated as a means of providing both a cutting action and a separating impulse. An explosive train of this type normally requires an initiating electrical signal. A typical cross section of this type structure is shown in Fig. II-93. Studies were made which indicated that the FLSC could be initiated by sensing temperature with a detonator. This could be accomplished at several points within the train, thereby providing a higher degree of reliability. It was estimated that auto-ignition utilizing temperature would occur at approximately 700° F. Since this was a relatively new concept of utilizing an explosive, a vendor (Ensign-Bickford Corporation) was contacted to determine the feasibility of the design approach. The vendor stated that this appeared feasible and suggested the use of lead-azide as the temperature sensitive element in the explosive train.

The entire concept was reviewed at this point in order to obtain the lightest, lowest cost housing design. It was decided to investigate other

means of separating the housing as well as to study the basic housing structure to reduce weight and cost. The costs involved in the use of ordnance devices are relatively high because of the large amount of testing required.

As a result of this investigation, the present design was evolved. This does not utilize ordnance and departs from the titanium honeycomb construction. This results in a weight savings of approximately 40 pounds. The present concept also differs from the previous ones in the method of separating the housing. In this concept, the horizontal shutter support beams are keyed in place by two side doors. When the doors are opened the beams (and shutters) are freed and driven away from the housing by springs. This creates a free opening in the front of the structure through which the fuel block is ejected by springs. The two doors are also spring loaded, and locked closed by means of a cable. The cables are fixed to aluminum and fittings which are designed to burn away during re-entry, thereby freeing the cables and permitting the ejection sequence to occur. This sequence is shown in Fig. II-94, and the housing is shown in Figs. II-95(a) and (b).

Fuel block loading is accomplished by means of a door within one of the side doors. This fuel block loading door is also used to preload the fuel block in both vertical and horizontal axes. By tightening up on the latches, the door is pushed against the fuel block which is seated in vee-shaped support beams. The amount of the preload can be controlled by the torque on the latch bolts. The preload for the booster longitudinal axis is obtained from wedge-type supports at opposite corners of the fuel block. A screw-jack type mechanism is used for this purpose.

The layout design is approximately 80% complete. The RTG hosing test specification (shock and vibration tests) was written, reviewed and released during this period.

b. RTG housing thermal analysis

In support of the design evolution of the RTG housing structure, several analyses have been performed. The initial housing design, based on the use of the titanium honeycomb housing shown in Fig. II-96, was analyzed to determine the parasitic heat loss and key structural temperatures. The total end-housing parasitic heat losses varied from a low value of 430 watts(t) for an assumed outside surface emittance of 0.6 and fuel block temperature of 1300° F to 791 watts(t) at $\epsilon = 0.6$ and fuel block temperature of 1700° F. A more significant result was the large difference in heat flow to an external surface node from an adjacent insulation node in comparison with a neighboring honeycomb surface node. For instance, for the surface honeycomb Node 15 in Fig. II-96 and assumed conditions of outer surface emittance of 0.3 and fuel

Fig. II-93. Typical FLSC Installation

Fig. II-94. Fuel Block Ejection Sequence

Fig. II-95(a). RTG Housing Assembly

Fig. 11-95(b). RTG Housing Assembly Exploded View

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Fig. II-96. End Housing--Side Thermal Model

block temperature of 1500° F, the heat flow from the insulation Node 14 to Node 15 amounted to 37.4 watts(t) while from the honeycomb Node 10 it was 211.2 watts(t) or nearly six times as much. Therefore, the parasitic heat loss is dominated more by the heat short through the titanium structure than the heat flow through the insulation.

To evaluate the effect of insulation thickness or heat losses, a cursory analysis was conducted on an end housing configuration with tubular edge member supports. Neglecting the heat flow through the titanium housing structure, the results shown in Fig. II-97 indicate that little heat loss reduction is obtained when more than two inches of insulation thickness is used. At this point, a major design change in the edge member housing was initiated.

A subsequent configuration considered for the top and bottom of the generator housing is shown in Fig. II-98. From a thermal standpoint, it was necessary to determine what temperature the box tube support structure would reach and thus dictate the type of material to be used. A first analysis assumed an open titanium tube separated from the hot zones by Min-K-2000 insulation. The internal and external surface emittances were varied along with the fuel block temperature. The critical nodes were assumed to be Nodes 9 and 10 which are closest to the heat source. The initial computer runs indicate that the maximum temperature of 256° F was obtained on Node 10 with an assumed fuel block temperature of 1700° F. For fuel block temperatures of 1500 and 1300° F, Node 10 was 229° and 199° F, respectively.

In conclusion, it appears that aluminum may be used for the box tube structure but other factors such as weldments to other titanium structural elements must be considered before this step should be taken.

Another configuration briefly considered is shown in Fig. II-99. Two variations in this configuration were analyzed in a preliminary way. One variation used Min-K-2000 insulation in the cross hatched area, and a second assumed the entire volume of the Haynes-25 fuel support bracket was filled with Min-K-2000 insulation. The objective of the analyses was to determine if the temperature of the structure at the junction between the Haynes-25 bracket and the titanium box structure (circled point) was below the structural strength limitation of the titanium material (i. e., 800° to 900° F). With insulation in the cross hatched area only the estimated structural temperature is above 1400° F based on optimistic assumptions. With insulation in the entire area, the estimated temperature at the same point is above 1100° F, which is still considered unacceptable.

Accordingly, a third side support configuration, shown in Fig. II-100, was analyzed to determine the feasibility of using titanium for the box structure. A 1/8 inch thick insulation layer of Fiberfrax was assumed as the insulation between the Haynes-25 fuel block support member and

Assumptions:

Min-K-2000 insulation

$K = 0.015 \text{ Btu/hr-ft-}^\circ\text{R}$

Haynes-25 strut supports

One-half inch OD, 0.032-inch wall thickness

$\Delta T = 1000^\circ \text{ F}$

Fig. II-97. End Housing Insulation and Support Heat Losses

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Fig. II-98. Top and Bottom End Housing Thermal Model

Fig. II-99. Generator Housing and Fuel Block Side Support

Fig. II-100. Generator Housing and Fuel Block Side Support

the titanium structure. Based on this preliminary analysis, the temperature at the point circled is of the order of 600° to 700° F which is well within the structural limitations. A more detailed analysis will be performed if this design is selected, since the heat losses for this configuration will be rather high, probably in excess of 1000 watts.

c. Test loop instrumentation effort

Design and analysis for instrumentation of all of the test loops has been completed. Development and qualification testing of the SNAP 29 components will be done on the following loops:

- (1) Permanent water test loop
- (2) Permanent NaK test loop
- (3) Portable water test loop
- (4) Portable NaK test loop
- (5) NaK EM pump performance test loop
- (6) NaK bimetallic tube/panel test loop

The permanent test loop installations were provided with the capability to test complete RTG assemblies as well as radiators and T/E module units. The design of the test loop data acquisition system was completed and a telemetry format for performance and status measurement data was formulated.

d. T/E module instrumentation design

The design and analysis for the T/E modules (CP 1940) was completed. Internal instrumentation for all functional modules was standardized in order to achieve quantitative, as well as qualitative data, comparison for all measurement data. The modules were instrumented with monitor sensors for life performance, temperature cycling, thermal vacuum, vibration and shock tests.

Instrumentation design has been completed and released for the following items:

- (1) Electric heater tests (performance, cycling, dynamic test)
- (2) Bearing tests (cycling and thermal vacuum)
- (3) Shutter tests (vibration and thermal vacuum)

- (4) Actuator tests (cycling, overpressure and overtemperature)
- (5) Bimetallic tube tests
- (6) Hydraulic mockup radiator tests
- (7) Manifold hydraulic tests.

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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. Four subtasks are utilized in performing this effort:

- (1) CP 3210--Systems Interface Evaluation
- (2) CP 3220--Vehicle and Launch Complex Integration (deleted due to redefined program)
- (3) CP 3230--Reliability (reported in Chapter IV)
- (4) CP 3240--Systems Test Integration.

A. SUMMARY OF SIGNIFICANT ACCOMPLISHMENTS

The SNAP 29 Program Plan (MND-2062-500) was completely revised due to program redefinition and will be delivered to the AEC in April 1967. This plan describes the development and fabrication effort associated with providing a 500-watt(e), polonium-210-fueled radioisotope thermoelectric generator (RTG) and its associated aerospace ground equipment (AGE).

A Systems Design Review Board was established to control the engineering change functions necessary for a formal technology control program.

The documentary film was completed (and reviewed by AEC) during this report period.

B. SYSTEM INTERFACE EVALUATION--CONTROL POINT 3210

The objectives of the systems 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. This effort is facilitated through review, intimate understanding and adherence to all system design criteria.

Implementation is obtained by evaluation of system design criteria for overall engineering compliance. From the RTG and AGE system designs, constraints and performance criteria are prepared, published

and maintained. Design changes are controlled, coordinated and implemented under this control point for all actions of the Technology Review Board.

Independent engineering design reviews at all levels of component, subsystem and system design are conducted on an informal but continuing basis by evaluation of analytical techniques, drawings, test specifications, processes and test procedures. Formal design reviews are coordinated and corrective action is implemented by development studies and design change actions.

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

1. SNAP 29 Program Plan

The SNAP 29 Program Plan (MND-2062-500-1, -2 and -3) was completely revised, and will be delivered to the AEC in April 1967. The program plan describes the necessary effort from component development through prototype fabrication that is required to provide a nominal 500-watt(e), polonium-210-fueled, radioisotope thermoelectric generator (RTG) power supply plus associated aerospace ground equipment (AGE). It includes program management, component and system development, design and evaluation, system demonstration and system delivery, including launch support.

To better define the detail program objectives and technical approach and to arrive at manageable units for planning and control, the program is divided into five major tasks. Each task is further delineated into control points (successively smaller pieces of hardware or services). The corresponding work breakdown structure, based upon the PERT/ Cost accumulation concept and prepared to the fourth indenture level, is presented in Table III-1. For identification purposes and to permit data processing accumulation of planned and actual costs, control points or summary numbers have been assigned to the end item subdivisions as shown in Table III-1. In addition, these control points permit a totally integrated control system for drawings, equipment, test specifications, documentation, correspondence and PERT/Time detail networks.

The control points consist of four digits and were devised to conform with both program requirements and Martin Marietta drawing systems for the RTG and AGE. The control point for the SNAP 29 program, Level One, is 3000. At Level Two, the first digit is 1 for the RTG system, 2 for the AGE system or 3 for management data. At Level Three for the RTG and AGE, the third digit (1, 2, 3 . . . 9) designates the appropriate subsystem. At Level Four for the RTG and AGE, the third

TABLE III-1

SNAP 29 Program Breakdown Structure and Control Points

<u>Level One</u>	<u>Level Two</u>	<u>Level Three</u>	<u>Level Four</u>
3000 SNAP 29 System	3100 Program Management and Control	3110 Program Administration	
		3120 Interface Control and Configuration Management	
		3130 PERT/TIME/COST	3131 PERT/TIME Integration 3132 PERT/COST
	3200 Systems Evaluation and Integration	3210 Systems Interface Evaluation	3211 End Item Specification and Evaluation 3212 Engineering Design Reviews 3213 Logistics
		*3220 Vehicle and Launch Complex Integration	*3221 Vehicle Integration Evaluation *3222 Launch Complex Integration Evaluation
		3230 Reliability	3231 Reliability, Planning and Analysis 3232 Design Evaluation and Approval 3233 Test Program Definition and Evaluation
		3240 Systems Test Integration	3241 Test Programs
		3250 General Support Technology	3242 Systems Tests
	3300 Safety Evaluation and Integration	3310 Safety Evaluation	3311 Ground Handling and Launch Complex 3312 Accident Models 3313 Nuclear Safety Analyses 3314 Integration and Reports
		3320 Aerodynamic Analyses and Tests	3321 Aerodynamic Analyses 3322 Fuel Block Tests 3323 Generator and Preloading Door Tests 3324 Satellite/Generator Tests
		3330 Impact and Burial Tests and Analysis	3331 Exploratory Impact Tests and Analyses 3332 Specimen Development 3333 Failure Evaluation 3334 Qualification Tests 3335 Fuel Block Burial Tests and Analyses
		3340 Thermal and Structural Tests and Analyses	3341 Aerothermodynamic Analyses 3342 Structural Analyses and Testing 3343 Radiant Heat Tests 3344 Fuel Block Ejection Tests
1000 RTG System		1100 RTG Heat Source (Isotopic) Subsystem	*1110 Operational 1120 Prototype 1130 Assembly Qualification 1140 Component Development
		1200 Heat Source (Electrical) Subsystem	*1210 Operational 1220 Prototype 1230 Assembly Qualification 1240 Component Development
		1300 I/L Modules Subsystem	*1310 Flight 1320 Prototype 1330 Assembly Qualification 1340 Component Development
		1500 Thermal Control Subsystem	*1510 Operational 1520 Prototype 1530 Assembly Qualification 1540 Component Development
		1700 Heat Rejection Subsystem	*1710 Operational 1720 Prototype 1730 Assembly Qualification 1740 Component Development
		1900 Structures, Shielding and Instrumentation Subsystems	*1910 Operational 1920 Prototype 1930 Assembly Qualification 1940 Component Development
2000 AGE System		2200 Fuel Handling Machine	*2210 Operational 2220 Prototype 2240 Component Development
		2400 Transportation and Handling Equipment	*2410 Operational 2420 Prototype
		2600 Field Test Equipment	*2610 Operational 2620 Prototype
		*2800 Launch Complex Support Equipment	*2810 Launch Site Modification Equipment

* No effort is planned to be accomplished under these tasks

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digit, 1, represents the operational system, 2, the prototype system, 3, assembly qualification, and 4, component development. No effort is planned on the operational system during the current phase of the contract.

2. Documentary Film

Production was completed on the motion picture documentary film for SNAP 29. This color film, entitled "SNAP 29," a development program summary, highlights the development aspects relating to the evolution of the SNAP 29 from engineering feasibility to system hardware. The 24-minute film is a balanced presentation of "live" subject matter and animation depicting the conceptual subjects.

3. Engineering Design Reviews--CP 3212

An independent evaluation of the RTG system and subsystem is conducted under this control point in order to formulate system optimization criteria and tradeoff parameters, monitor and control the system performance, and evaluate and coordinate desired or actual changes and interface requirements (internal as well as external to the RTG). In order to synthesize reasonable system requirements, analyses will be conducted to examine various mission and operational modes for which the RTG may be used. Economic analyses relating power, weight, area, operational costs, reliability, etc., to space program costs will also be conducted.

RTG features which are operationally desirable from the spacecraft point of view will be examined to assess their impact upon the RTG designs as well as RTG utilization. The benefits of RTG suggested improvements will also be monitored and carefully evaluated from an airborne system point of view. The RTG weight will be monitored to ascertain the RTG weight versus performance status. Weight saving efforts will be evaluated, comparative weight statements will be prepared, and weight and mass property records will be maintained. Engineering design reviews for the RTG systems (including potential AGE, LV and S/C interfaces) will be conducted periodically. Recommendations will be coordinated and acted upon by appropriate design development and Systems Design Review Board actions. Changes to the system performance parameters, interface requirements and results of engineering evaluations and interpretations of technical requirements will be documented.

The Systems Design Review Board (SDRB) now supercedes the Engineering Review Board activities in order to meet the requirements of the redefined program. The SDRB objective will be the establishment of design criteria baselines with the ultimate posture of a functional RTG by fiscal year 1969. SDRB will define and resolve engineering design problems and test requirements and, in addition, will

establish the advisability of each change concerning technology identification baselines, including design criteria test plan and procedures, processes, AEC scope redefinition, data requiring AEC approval and other program-specified data. System and subsystem design reviews will be conducted on a periodical basis. Engineering activities as outlined by the flow diagram in Fig. III-1 may be summarized as follows:

- (1) Engineering Design Change Schedule (EDCS)--An initiating change control document that completely identifies the nature of a requested change. It is prepared by any member of the project engineering staff.
- (2) Systems Design Review Board chairman--Evaluates EDCS proposed change and coordinates the requirements with the affected engineering groups. A uniform control number is assigned to the EDCS, which continues to be related to all aspects of that change throughout the life of the program.
- (3) Systems Design Review Board--A technical committee drawn from the pertinent disciplines performing reviews and evaluations of proposed changes to the program baseline. This committee is made up of an SDRB chairman and engineering personnel representing Design, Analysis, Reliability, Nuclear Safety, Materials, System Test and Logistics.
- (4) Engineering Technical Director--Reviews Engineering Change Board recommendations and approves or rejects these recommendations.
- (5) Technology Review Board--Chaired by Technology and Data Management, this board considers the impact of the proposed change on the program, task, level or effort, schedule and technology.
- (6) Engineering Incorporation--A task leader is assigned to incorporate the change upon receipt of direction from the program director.

Technology and Data Management was defined to implement engineering functions keynoted in the SNAP 29 change control flow diagram (Fig. III-2).

Systems Design Review Board action was completed during this period for the following Engineering Design Change Schedules:

FIG. III-1 Engineering Change Functions

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Fig. III-2. Technology and Data Management Change Control Plan

EDCS Uniform
Control Number

Title

Status

M00002	Replace nickel alumide coating with Al-90 coating for fuel block	Approved by AEC CCP-2062-2
M00006	New design criteria requirements for thermal vacuum chamber to accommodate revised RTG configurations	To be submitted as part of CCP-2062-7
M00007	RTG fuel block program requirements, redirection thereof	Incorporated in program redefinition CCP-2062-7
M00008	RTG radiator meteoroid penetration (armor) protection, weight reduction thereof	Incorporated in program redefinition CCP-2062-7
M00009	Revise Electrical-Electromagnetic Interference Control Plan, MND-2062-3200-3, submittal date thereof	Incorporated in program redefinition CCP-2062-7
M00010	Parametric analyses, design and hydraulic testing of five-to ten-foot radiators, proposal thereof	Incorporated in program redefinition CCP-2062-7

4. Program Design Reviews

A formal plan will be described for conducting program design reviews in the next quarterly progress report.

These design reviews will be initiated for assembly qualification and prototype phases.

C. FLIGHT VEHICLE AND LAUNCH COMPLEX INTEGRATION--
CONTROL POINT 3220

Due to redefinition of the SNAP 29 program, which deletes the flight articles, effort under this heading has been stopped.

D. SYSTEM TEST INTEGRATION--CONTROL POINT 3240

The system test integration objectives are: to coordinate and implement an integrated test program for the RTG and AGE system, to definitize test requirements, schedules and test facilities consistent with programmatic, reliability and safety requirements, and to maintain adequate procedures, operations and reporting for all test programs.

The T/E Life Cycle Test Report, 466A1343625, and the Deep Thermal Cycle Test Report, 466A1343626, have been released.

The thermal vacuum chamber adapter tool configuration has been defined, and Specification 466S20N1 was issued defining all the requirements. This procurement specification was sent to subcontractors who are technically capable in this area. Bidders' proposals were evaluated and the competition has been narrowed down to two subcontractors: Tenney Engineering, New Jersey, and Bethlehem Corporation, Pennsylvania. Final vendor selection is expected in April 1967 and is contingent on AEC approval. The target date for installation is October 1967.

The thermoelectric module flow loops and associated test facilities have been assessed. The transient loop was eliminated without detrimental effect to the redefined program. The steady-state loop has been designed, procured, and built. Checkout functions are being conducted for operational readiness by mid-April 1967. Test hardware is expected in May 1967. Figures III-3 through III-9 show the loop fabrication progress.

The NaK flow loop has been designed, and major procurement is on order. Fabrication and installation are continuing for the completion in May.

The generator systems test area has been refurbished and is complete for all systems checkouts, such as breadboards and preprototypes.

Test Summary:

Test specifications released	29
Test procedures released	12
Tests in progress	5
Tests completed	7
Reports issued	2

Tests in Progress:

Emissivity fuel block	(CP 1140)
Aging tests, Haynes	(CP 1140)
Emissivity test, radiator	(CP 1740)
Bi-metallic tubing	(CP 1740)
Emissivity hot plate	(CP 1340)

Completed Tests:

T/E life cycle test	(CP 1340)
T/E deep thermal cycle	(CP 1340)
Research Module No. 1	(CP 1340)
Research Module No. 3	(CP 1340)
Research Module No. 4	(CP 1340)
Piston heat sink test	(CP 1340)
Preliminary shutter vibration test	(CP 1540)

Specific test results influencing design standards are being reported under the individual control points.

Fig. III-3. Steady-State Loop
Mechanical Installation

Fig. III-4. Steady-State Loop Control
Console Installation

Fig. III-5. Control Console
Installation

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Fig. III-6. Steady-State Loop Final Installation

Fig. III-7. Steady-State Loop Final Installation

Fig. III-8. Steady-State Loop Final Installation

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Fig. III-9. SNAP 29 Module Test Loop

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IV. RELIABILITY--CONTROL POINT 3230

The implementation of the SNAP 29 reliability program has been proceeding in accordance with the tasks and schedule presented in the plan. The plan establishes 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, it has been broken down into three major work areas:

- (1) Planning and System Analysis--Control Point 3231
- (2) Design Evaluation and Approval--Control Point 3232
- (3) Test Program Definition and Evaluation--Control Point 3233

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

A. SUMMARY OF SIGNIFICANT ACCOMPLISHMENTS

Redefinition of the SNAP 29 program has been completed. This redefinition involves a change to a level-of-effort type contract and reflects a development program as opposed to a production hardware program. The objective of the revised program is to demonstrate the SNAP 29 technology by fabrication of an RTG which will produce a nominal 500 watts of electrical power for a 144-day period at an overall weight of 500 pounds and to verify compatibility of the AGE by actual loading of an electrically operated heat source into an RTG. Greater program emphasis will be placed on heat source development and thermoelectric technology.

The reliability program plan (MND-2062-3231-1), among others, has been deleted from documentation requirements. The Reliability effort is included in the revised SNAP 29 program plan (MND-2062-500-2), and reference to the reliability program plan as an entity is no longer required. The reliability test plan (MND-2062-3233-1) has been completed and submitted to AEC for technical approval. The results of the work accomplished are presented herein.

The reliability goal established at a value of 0.993 for operation of the RTG for a 144-day period has been revised to 0.992. The revision results from a reliability/weight tradeoff study related to meteoroid armor protection for the radiator subsystem.

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

1. Reliability Planning

The redefinition of the SNAP 29 program to a level-of-effort contract reflects technology development as opposed to a production hardware program. The individual Reliability tasks and the schedule associated with those tasks have been modified accordingly. Figure IV-1 is a summary of the Reliability tasks to be accomplished during the program period of March 1 through October 31, 1967, with milestone start and finish dates. Task identity has been retained to provide continuity with prior efforts.

2. System Analysis

a. Meteoroid armor reliability requirements

A tradeoff study was submitted and approved by the US Atomic Energy Commission covering reliability requirements versus thickness of the micrometeoroid armor for the heat rejection subsystem radiator (NaK) coolant lines.

The Phase I technical conclusions report, MND 3612-7-1, specified an RTG reliability requirement of 0.998 at a 50% confidence level. In order to achieve that reliability requirement, a value of 0.9999 was required for the probability of no meteoroid penetration in the NaK coolant lines. Current reliability for the RTG is established as a high reliability goal, with a numerical value of 0.992, which includes the effects of this study.

The effect on the reliability goal associated with various probabilities of no meteoroid penetration is as follows:

<u>RTG System Reliability Goal</u>	<u>Probability of No Meteoroid Penetration</u>
0.9928 (0.993)	0.99999
0.9927 (0.993)	0.9999
0.9918 (0.992)	0.999
0.9829 (0.983)	0.99

As a result of this study, it was recommended that the design of the exposed surfaces of the coolant lines of the space radiator be based on a 0.999 probability of no meteoroid rupture.

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Control Point and Task No.	Title	1967							
		M	A	M	J	J	A	S	O
3231-1	Reliability Program Plan	Task completed							
-2	Program Control and Review								
-3	Subcontractor/Supplier Program								
-4	Indoctrination Program	No effort prior to 10-31-67							
-5	System Reliability Analysis								
3232-1	Detail Design Analysis								
-2	Critical Parts Program								
-3	Parts/Material Program								
-4	Human Engineering, Safety and Maintainability	No effort prior to 10-31-67							
-5	Design Review Program								
-6	Failure Data, Analysis and Corrective Action								
3233-1	Reliability Test Plan	Task completed							
-2	Test Monitoring, Data Analysis and Assessment								
-3	Final Reliability Assessment	No effort prior to 10-31-67							

Fig. IV-1. Reliability Tasks and Schedule

Determination of armor thickness utilizes Aerospace Meteoroid Environment and Penetration Criterion, Report No. TOR-269 (4560-40)-2 by V. C. Frost, Aerospace Corporation, 17 August 1964. Using this criterion, a system weight reduction of 36% or 24.7 pounds, in armor protection is estimated for a change from 0.9999 to 0.9990. A detailed discussion of this study is given under Control Point 1700 in this report.

b. Reliability goal and apportionment

The reliability goal for the RTG has been revised to 0.992 as a result of the meteoroid armor weight tradeoff study. The goal is defined as the probability that no catastrophic failure will occur during the 144 days of operation, which includes the 54 days of prelaunch and 90 days in orbit. Revised apportionment values at the subsystem- and where available, component-level, are presented in Table IV-1. The symbol designations included in the table provide continuity with the previous quarterly report. Entries in the prediction column reflect inputs developed under Control Points 3232 and 3233.

c. Reliability mathematical model

The initial development of the RTG goal and apportionment utilized a mathematical model based on the operational life of 144 days without differentiation of environmental levels during that time span. A mathematical model has been developed to account for these conditions and to provide for system assessment.

The SNAP 29 operational life profile, a time history together with the attendant environmental levels, provides the foundation for the reliability mathematical model. The overall life profile has three intervals, each having substantially constant environment levels identified as prelaunch, boost and orbit. These intervals are designated operational phases for the purpose of presentation of the mathematical model. A fourth interval, associated with Safety Engineering, provides for inclusion of events occurring during the postorbital phase. The postorbital phase is not currently included in the reliability mathematical model.

The operational profile extends from completion of fabrication through orbital flight. The phases, and their related events, are:

(1) Prelaunch

(a) Acceptance

Operational tests

Production Monitoring tests

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TABLE IV-1

<u>Symbol</u>	<u>Item</u>	<u>Operational Life (144 days)</u>	<u>Prediction Operational Life (144 days)</u>
	Radioisotopic Thermoelectric Generator	0.992	
R _{HS}	Isotopic heat source S/S	1.0000	
R _F	Fuel matrix	1.0000	
R _{FC}	Fuel capsule	1.0000	
R _{FB}	Fuel block	1.0000	
R _{CO}	Emissivity coating	1.0000	
R _{TM}	T/E modules subsystem	0.9962	
R _{TE}	Primary power	0.9999	0.999915
R _{EW}	Wiring	0.9995	
R _I	Insulation	0.9995	
R _{GS}	Gas seals	0.9975	
R _{EC}	Emissivity coating	0.9999	
R _S	Structure	0.9999	
R _{TC}	Thermal control subsystem	0.9990	
R _{SH}	Shutters	0.9996	
R _{INT}	Interconnections	0.9995	
R _A	Actuator system (redundant)	0.9999	
	Actuator system (non- redundant)	0.99004	

TABLE IV -1 (continued)

<u>Symbol</u>	<u>Item</u>	<u>Operational Life (144 days)</u>	<u>Prediction Operational Life (144 days)</u>
	Actuator	0.99433	0.99379
	Arm pins	0.99622	
	Welds (actuator mtg)	0.99996	
	Reservoir, tube welds	0.99951	
R _{HR}	Heat rejection subsystem	0.9969	
R _{PP}	Pump power	0.9995	
	Power source, pump	0.99999	
	Wiring	0.99951	
R _{PU}	NaK pump	0.9999	
R _{NK}	Space radiator tubing	0.9990	
	Radiator tubes and welds	0.99980	0.99980
	Manifold tubes and welds	0.99985	0.95848
	Accumulators	0.99940	
	Crimps	0.99998	
R _{RC}	Space radiator and fin coating	0.9995	
	Structure, skin and armor	0.99990	
	Emissivity coating	0.99970	
	Structure mounting	0.99990	
R _M	Meteoroid penetration	0.9990	
R _{SSI}	Structures, shielding and instrumentation	0.9997	

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(b) Associate Contractor Integration

Transportation and storage

Compatibility tests

(c) Launch complex integration

Transportation and storage

Checkout

Countdown

(2) Boost

(a) Ignition

(b) Boost

(3) Orbit

Development of the mathematical model is carried to the degree (subsystem component or part level) necessary to ensure an accurate representation of the system design configuration. The mathematical model includes those AGE systems determined to be critical for accomplishing countdown and launch.

The actual computation of quantitative values is accomplished by utilizing techniques based on the premise that during a given time interval, any component will reach the end of the interval in either of two states; namely, in tolerance or out of tolerance. The probability of surviving the interval in the desired state is the reliability, and the probability of becoming out of tolerance during the interval is (1-reliability). Since each component has two possible states at the end of the interval, and since the system is comprised of N units, there are 2^N possible states for the system. Some of these states constitute successful operation of the system, the others, unsuccessful. The probability of occurrence of any system state is the product of the unit probabilities (reliability or unreliability) for the unit condition dictated by that state. Since some of the system states constitute system success, and since the probability of occurrence of these states can be calculated, the overall probability of system success (system reliability) can be determined as the sum of the probabilities of the system success states.

SNAP 29 system reliability calculations will be made on a computer, utilizing the Markov chain technique. This technique considers any system state as comprised of the states of the components. In a time interval, the components can remain in the same state or change to a different state, such as to a failure state. In some cases, successful operation could depend upon a component's changing through several different states in succession as during countdown, launch and orbit phases.

The prelaunch, launch and orbital states are presented in Table IV-2, which is an illustration of the possible events and order of these events. The reliability mathematical model presented and the programming written around it constitute a minimal level. The technique used is readily adaptable to adding states as required during development of the program.

TABLE IV-2
Prelaunch (Phase I) States

<u>No.</u>	<u>State</u>	<u>Description</u>	<u>Remarks</u>
1	Operational check-out	Electrically heated, NaK pump in operation, RTG providing power to load	Launch vehicle, satellite vehicle, RTG final check-out, 14 days to launch
2	Instrumentation failure	Incorrect or no readings and/or controls	Failure of RTG sensors, link to AGE, or readout equipment. Possibility repairable. May not be mission failure.
3	Ground support failure	Fuel handling failure. Cooling failure.	Possible safety problem. Possible repair and change to State 1 or 5.
4	RTG failure	Mechanical, temperature or electrical failure	Some possibility of repair depending on nature of failure and time.
5	Ready for launch (launched)	Isotopically fueled and operating within temperature and electrical tolerances	Prelude to Phase II can occur only as last event in Phase I

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TABLE IV-2 (continued)

Boost (Phase II) State

<u>No.</u>	<u>State</u>	<u>Description</u>	<u>Remarks</u>
1	All OK	Power, voltage within specification. Temperature, ΔT at desired levels. Instrumentation properly functioning.	
2	Hot side overheat or too cool	Thermal control subsystem failure, shutters open or closed or breakup.	
3	Cold side overheat	Loss of cooling	Plumbing failure, radiator rupture, power failure, insufficient cooling
4	Primary power failure, degradation T/E	>1/8 loss of power or voltage (excessive degradation)	Shorts, open circuit
5	Mission failure-- structural breakup	Module or RTG structural breakup	*Gas seal failures excluded
6	Instrumentation failure	False or no readings	Not a mission failure

Orbit (Phase III) State

1	Power failure T/E couples	Excessive degradation catastrophic failure	Shorts, open circuit
2	Power failure (module failure)	Loss of couple, pressure, gas environment, heat transfer	Excessive creep cracks, broken feedthroughs
3	Heat rejection failure	Cold end overheat	Loss of NaK or NaK pumping
4	Thermal control failure	Hot side overheat or too cool	NaK control system fails to close shutters, shutters locked open
5	Structural, shielding failure	RTG breakup	

TABLE IV-2 (continued)

<u>No.</u>	<u>State</u>	<u>Description</u>	<u>Remarks</u>
6	Instrumentation failure	Incorrect or no ground readings	Not a mission failure
7	System OK operation	Power and voltage to specified temperatures at nominal levels	

* If a gas seal fails, the power will probably not fail until after the boost phase, due to the short time interval.

C. RELIABILITY ANALYSIS AND CHANGE CONTROL--
CONTROL POINT 3232

1. Primary Power Reliability

The reliability of the RTG primary power source, which consists of 688 of the 720 thermoelectric couples, has been predicted, based on operational experience of SNAP 3, SNAP 7 and SNAP 9 RTG's. The prediction is developed from Martin Marietta designed and fabricated RTG's having series-connected thermoelectric couples of either 2N-2P or 2N-3P lead telluride elements. Data from generators with redundant couple circuits are not included, since a failure in an individual couple cannot be positively identified from measurement of the power output. A couple is considered, for the purpose of this analysis to include the thermoelectric couple, hot shoes, cold-end straps, pistons, springs, insulating disks, and associated bonds and brazes.

Table IV-3 is a summary of the operating T/E couple hours accumulated on individual RTG's. The single failure is attributed to the T/E couple, although no verified proof exists. In this case, a SNAP 9 generator exhibited an open-circuit condition, and couple failure was assumed the most probable cause.

The observed (catastrophic) failure rate (λ), based on the combination of all data, is 0.00338%/1000 hours and is obtained by dividing the number of failures by accumulated operating hours. It is recognized that a different failure rate may exist for laboratory, storage, transportation, launch and orbit conditions. However, with only a single failure considered, a combination of data constitutes a more realistic approach. A single failure does not permit development of a different failure rate for each environment. Table IV-3 presents the approximate accumulated couple hours by location and environment.

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TABLE IV-3

Summary of Operating T/E Couple Hours by Location

<u>Location</u>	<u>Couple Hours</u>	<u>Failures</u>
Laboratory and test	14,795,800	1
Ground operation	5,899,000	0
Underwater operation	4,989,400	0
Orbit operation	<u>3,936,900</u>	<u>0</u>
Total	29,621,100	1

Reliability, in considering the SNAP 29 primary power system, must take into consideration two fundamental types of failure; (1) degradation failures, where performance decreases as a function of time and (2) catastrophic failures, characterized by a discontinuous drop in performance below prescribed limits.

A failure of a T/E couple, either in the open or short mode, results in a degradation in power output. Although the open failure mode has by far a higher probability of occurrence than the shorting mode, the effect on power is identical. It results in a power output loss of 0.29% for each couple failure. The probability of two couple failures in the open mode occurring in the same series group and the failure rate associated with the couples determine the catastrophic failure probabilities.

The probability of the RTG primary power source to meet the 144-day operational requirements is determined from the expression:

$$R_{TE} = \sum_{L=0}^{i=j} L^i \frac{N!}{i! (n-i)!} R_C^{(2N-i)} (1-R_C)^i$$

L = 0

where

- i = summation index
- N = number of series groups (344)
- L = parallel elements per series group (2)
- j = number of allowable failures

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

λ = failure rate (0.00338%/1000 hr)

t = operating time (3456 hr)

The expression states the probability of having up to j failures distributed in the different T/E couple series groups.

The parametric curves, Fig. IV-2, are developed from the above expression, allowing for failures by initially adding the same number of couples (overdesign) to compensate for these failures. Individual curves are given for one to three failures. Four or more failures fall approximately on the same line.

The predicted catastrophic failure rate is 0.999915 (based on two failures) as compared to the apportioned value of 0.9999 for the 144 days of operation and is applicable.

All evidence indicates that the SNAP 29 T/E couple design should be equal to or better than that experienced on the earlier SNAP programs.

The reliability of the NaK actuator is assessed based on the predicted reliability for the cycling and overtemperature tests discussed in Chapter IV.

The predicted reliability for each of the three (3) cycling tests and stroke lengths must be combined for the assessment of the actuator reliability due to cyclic operations. The combined numerical reliability is obtained as follows:

$$R_C = R_{C1} \cdot R_{C2} \cdot R_{C3} = (0.9975) (0.9990) (0.9973) = 0.9938$$

The assessment of the reliability of the NaK actuator is based on the combination of the cycling tests and the overtemperature test-predicted reliability. The combined numerical reliability is obtained as follows:

$$R_A = R_C \cdot R_T = (0.9938) (0.999998) = 0.99379$$

This reliability assessment of the NaK actuator, based on test-oriented predicted reliabilities, compares favorably with the apportioned reliability of 0.99433.

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Fig. IV-2. Prediction of RTG Primary Power

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

1. Reliability Test Plan

The SNAP 29 reliability test plan was prepared primarily to identify those specific tests that will yield reliability assessment data, i. e., (1) to aid in verifying that the SNAP 29 RTG power output performance goal has been achieved, including degradation characteristics, and (2) to aid in establishing a confidence level that the RTG will be able to survive the cyclic and operational environments of the mission.

In addition, the plan identifies other tests which will contribute supporting data to the reliability assessment and analysis program.

Reliability demonstration tests of a classical, statistical approach are not attempted, due to cost and schedule considerations. The major portion of the reliability data will be accumulated at the component and subsystem test levels from life tests and dynamic environmental tests. Where practical, the standard test program as planned was modified or extended to provide data of a more statistical nature to enhance the reliability assessment program.

The assessment of reliability requires the establishment of a base-line to assure the quality level of all test hardware. All specimens being subjected to reliability tests are therefore subjected to the production monitoring and acceptance tests normally performed on operational hardware prior to entering the reliability test program. Consistent with normal Corporation procedures, all failures which are encountered must be reported and processed through the Marin Marietta automatic reporting system (MARS). All failures attributed to such causes as test equipment malfunctions and operating errors will not be included in the failure rates used in the reliability assessment procedures. Accountable failures will include out-of-tolerance degradation drifts in addition to complete performance malfunctions.

Table IV-4 indicates those tests which are primary contributors to reliability tests data inputs. Table IV-5 indicates the types of test that are considered as candidates for providing supporting data for reliability assessment analysis.

2. Reliability Assessment Approach--NaK Actuator

The prediction of the NaK actuator reliability and the approach to its assessment is determined from the cycling and overtemperature tests.

The NaK Actuator Cycling test (Drawing No. 466A1543020) prescribes the following test requirements:

TABLE IV-4
Reliability Test Summary

<u>Subsystem/Test No.</u>	<u>Test Article/Quantity</u>	<u>Test Description</u>
Heat source S/S (isotopic)	Fuel blocks	
1	3	Vibration test to failure in most critical axis.
2	3	Shock test to failure in most critical axis.
Heat source S/S (electrical)	Heater elements	
1	5	Thermal cycle heater elements determine critical cyclic rate
2	5	Determine operating life characteristics of heater elements
Heat source S/S (electrical)	Complete electrical heat source	
1	3	Shock test to failure to determine critical axis.
2	6	Shock test to failure in most critical axis.
3	3	Vibration test to failure to determine critical axis.
4	6	Vibration test to failure in most critical axis.
Thermoelectric module S/S	Thermoelectric module	
1	6	Determine power output and degradation under laboratory environments.
2	1	Determine most critical axis under shock and test to failure.
3	9	Shock test to qualification level
4	3	Vibration to failure to determine failure levels, failure modes for each axis and the more critical axis for vibration.
5	3	Vibration qualification test.
6	10	Thermal vacuum qualification test.
7	14	Vibration to failure for margin of safety in all axes.
Thermal control S/S actuator tests	Bellows (NaK) actuator	
1	3	Determine shallow cycle performance behavior of bellows at a 1 cps rate.
2	3	Determine medium cycle performance behavior of bellows at a 1 cps rate.
3	3	Determine deep cycle performance behavior of bellows at a 1 cps rate
4	3 (plus 3 from No. 3 and 1 from vendor qualification test)	Determine failure burst pressure of bellows
Thermal control S/S	Complete thermal control S/S and supporting framework	
1	8	Vibration test to failure in most critical axis.
Heat rejection S/S pump tests	NaK pump	
1	4	Operating life test at nominal operating temperature.
2	9	Vibration to failure in each axis.
3	9	Shock test to failure in each axis.
Heat rejection S/S accumulator tests	Accumulator bellows and housing	
1	9	Determine cyclic life to failure of accumulator bellows.
2	8	Vibration to failure in two critical axes.
Generator structural S/S tests	Generator housing plus simulated component loading	
1	1	Determine most critical axis of failure under vibration inputs to failure
2	1	Determine failure level to shock inputs along the thrust axis

TABLE IV-5

Supporting Reliability Assessment Tests

<u>Subsystem/Control Point Test Identification</u>	<u>Test Article/Quantity</u>	<u>Test Objective or Description</u>
Heat Source Subsystem (CP-1130)	Full-scale fuel block	
Qualification Test	1	Vibration qualification test.
Thermal Control Subsystem (CP-1530)	Complete subsystem	
Qualification Tests	8	Qualification of the subsystem under vibration tests in each of 3 orthogonal axes and under thermal cycling in a vacuum
System Tests (CP-3242)	Complete generator system, including heat rejection and thermal control subsystems	
Prototype Qualification Test	1	Performance, vibration and thermal vacuum tests. Qualification level tests under shock, vibration, thermal vacuum and acceleration environments
Prototype Reliability Sequential Test	1	Performance, vibration and thermal vacuum tests. Sequential life tests for accumulation of data and degradation characteristics of the system level.

- NOTES: (1) Subsystem qualification tests under CP 1200, 1300 and 1700 have been included under Reliability Tests (Chapter V), since the hardware is common to both qualification and reliability tests.
- (2) The reliability sequential test has been included in this section, since many of the reliability assessment analyses have been performed earlier in the program and will be verified by data from the tests on this system.

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NaK Actuator Cycling Test Requirements

<u>Cycle Rate (cps)</u>	<u>Piston Stroke (in.)</u>	<u>Actuators on Test</u>	<u>No. of Cycles per Actuator</u>	<u>Accumulated Test Cycles Required</u>
1	0.188 ± 0.125	3	115,200	345,600
1	0 to 0.188	3	3,000	9,000
1	0 to 0.375	3	150	450

The 0.188 ± 0.125-inch piston stroke represents the cyclic behavior of the bellows actuator during the orbital paths. The 0 to 0.188-inch piston stroke represents the cycle conditions between the ambient laboratory temperature and the nominal system operating temperature which will be encountered during ground testing. The 0 to 0.375-inch stroke represents the maximum operating range of the shutters from 0 to 90° which may occur during verifications tests and once in orbit if a failure occurs.

Reliability prediction for each cyclic test is based on a 90-day orbital mission and a nominal 90-minute orbit; it is estimated that there would be approximately 1440 cyclic operations during this period. These cycles result from expansion and contraction of the NaK, due to alternate exposures of the RTG to earth's shadow and sunlight. Using the exponential distribution function for predicting the reliability ($\bar{R} = e^{-\lambda c}$) and the test requirements, the bellows actuator reliability for these conditions is predicted as follows:

$$\bar{R} = e^{-\lambda c} \qquad \lambda = \text{failure rate} = \frac{\text{number of failures}}{\text{total cycles}}$$

$$c = \text{number of operational cycles}$$

Assuming a zero failure condition (0.6 per the Chi-square estimations),

$$R_{C1} = e^{-\frac{(0.6)(1440)}{345,600}} = e^{-0.0025} = 0.9975$$

The predicted reliability for this cyclic test operation is therefore estimated as 0.9975.

Based on an estimate that there will be approximately 15 test cycles during the operational hardware checkout, the predicted reliability will be 0.999 for this cyclic operation.

$$R_{C2} = 3^{-\lambda c} = e^{-\frac{(0.6)(15)}{9000}} = e^{-0.001} = 0.999$$

Based on an estimate that there may be two occasions where the bellows actuator will be subjected to the full stroke length (once during checkout, and once during the mission), the predicted reliability will be 0.9973 for this cyclic operation.

$$R_{C3} = e^{-\lambda c} = e^{-\frac{(0.6)(2)}{450}} = e^{-0.00266} = 0.9973$$

The NaK Actuator-Overtemperature Test Specification 466A1543017 prescribes that the bellows actuators will be tested to failure at increasing pressure increments of 100 ± 10 psig, starting at 1000 ± 10 psig, while maintaining the actuator assembly at a nominal operating temperature of $400 \pm 10^\circ$ F.

The reliability prediction for the bellows actuator overtemperature tests to failure will be based on the safety margin technique between a predetermined reliability boundary condition and the mean pressure failure level of the actuators tested. The safety margin reliability determination is calculated as follows, and the relationship is indicated by the following figure:

Load/Strength Safety Margin Relation

$$\text{Safety margin} = \frac{\text{Mean strength } (M_S) - \text{Reliability boundary } (R.B.)}{\text{Standard deviation } \sigma_s}$$

The safety margin, as the number of standard deviations between the reliability boundary and the mean strength or failure level, is then applied to the normal probability curve to determine the numerical reliability value.

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The reliability boundary has been established as the maximum pressure required by the bellows actuator to drive the thermal control shutters to the 90° open and locked position under conditions where a single actuator must perform this function due to failure of the redundant parallel unit. This pressure has been determined to be at 138 psi (approximately). Based on an assumption that the mean failure pressure level will be at least 1500 psi and arbitrarily assuming a standard deviation of 300 psi, the safety margin and corresponding numerical reliability is evaluated as follows:

$$SM = \frac{1500 - 138}{300} = 4.54 \quad \bar{R}_T = 0.999997$$

3. Reliability Assessment Approach--Accumulator Bellows Cycling Test

The Test Specification 466A1743026 specifies that three accumulators each will be subjected to three different bellows cycling compression strokes at a single cyclic rate. The accumulator housing and bellows will be pressurized to the nominal operating pressure and maintained at the nominal operation temperature throughout the test.

It has been estimated that the accumulator bellows will be subjected to a shallow compression cycle about once per orbit of the Earth, or a total of 1440 during the mission. This shallow cycle results from expansion and contraction of the NaK due to alternate exposure of the RTG to sunlight and shadow of the Earth. During ground test operations, it is estimated that the accumulator will be subjected to a maximum of 15 deep cycles resulting from expansion and contraction of the NaK during cycles from nominal laboratory temperatures to the nominal operational temperatures and return. The relationship of the test requirements and the operational requirements is indicated in Table IV-6.

TABLE IV-6

Accumulator Cyclic Performance

<u>No. of Test Units</u>	<u>Bellows Stroke Travel (in.)</u>	<u>Type of Cycle</u>	<u>Estimated Operational Requirements</u>
3	0.125	Shallow	1440
3	0.250	Intermediate	--
3	0.500	Deep	15

Figure IV-3 is presented as the optimum method of determining the reliability and stress characteristic for the accumulator bellows. This characteristic (S/N curve) is applicable where the bellows is cycled to failure and the failure distribution determined from the results at each stroke travel rate. This curve, in conjunction with estimated operating cycles, may then be used to develop the reliability by safety margin techniques.

An alternate method for obtaining numerical reliability values from the test is through the development of a failure rate by exponential distribution function and the use of the expression:

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

where:

R = probability of performance

λ = failure rate per cycle

c = No. of operating cycles

This method to demonstrate the apportioned goal of 0.9994 would require approximately one million cycles. A number of cycles less than one million is considered acceptable, since a bellows leak of the gas from the accumulator into the NaK is not expected to degrade the NaK pumping action or radiator cooling efficiency below acceptable levels. A test to verify the performance of the NaK pump under various gas percentages is scheduled during the development program.

The test (see Fig. IV-4) establishes that a minimum of 43,200 shallow cycles (14,400 per bellows) be performed on the three accumulator bellows. This will provide a demonstrated reliability of approximately 0.980. In demonstrating the deep cycle reliability, a minimum of 8640 deep cycles (2880 per bellows) is required, which provides a reliability of approximately 0.999. Since there is no current operational requirement for the intermediate cycle, a minimum of 43,200 intermediate cycles (14,400 per bellows) is specified on these units to provide a higher number of accumulated cycles for mission reliability estimates and future applications. It is expected that a higher reliability may be verified due to possible combination of the data.

E. DESIGN REVIEW

No action this period.

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Fig. IV-3. S/N Curve for Accumulator Cycling Life

Fig. IV-4. Accumulator Reliability Assessment

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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 effort will be to develop equipment which will provide reliable handling of the RTG to ensure its proper operation.

Program redefinition deletes the requirement for operational AGE, Control Points 2210, 2410 and 2610, and also deletes the effort on launch site modification equipment, Control Point 2800.

The objective of the present programmed effort is to demonstrate the AGE capability of checking out the RTG and loading and unloading electrical and isotopic (simulated) heat sources. The demonstration will be conducted at the Martin Marietta facility, and the tasks to be conducted are:

- (1) 2200--Fuel Handling Machine (FHM)
- (2) 2400--Transportation and Handling Equipment
- (3) 2600--Field Test Equipment

A. SUMMARY OF SIGNIFICANT ACCOMPLISHMENTS

Preliminary RTG/FHM interface configuration layouts compatible with the present RTG configuration were completed. This arrangement provides for an air-operated RTG-to-compliance section latch with a lanyard release backup system on the top and bottom of the compliance section. RTG door operating and latch mechanisms are also mounted on the compliance section. The gripper assembly provides jaws for gripping the fuel source and contains the mechanism for applying final preload.

A configuration layout of the compliance mechanism, using a multiple ball spline system, was completed.

Figure V-1 shows the latest configuration of the compliance section.

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Fig. V-1. Compliance Section

B. FUEL HANDLING MACHINE

The FHM now will be developed in two steps. First, a development test of the heat source gripper assembly and compliance portions of the FHM will be conducted. The results of this test will be used to construct a prototype FHM which will be subjected to extensive testing with a payload simulator. The FHM and simulator will be used to demonstrate the capability of fueling and defueling the RTG.

C. TRANSPORTATION AND HANDLING EQUIPMENT

The transportation and handling equipment consists of those items of AGE required to transport and handle the RTG and the isotopic heat source.

The transportation and handling items are:

- (1) RTG shipping container
- (2) RTG handling fixture (in-plant)
- (3) Fuel transportation cask

The RTG shipping container is required to demonstrate the feasibility of producing such an item.

The RTG handling fixture (in-plant) is required for transporting the RTG between test areas at the Martin Marietta facility.

The fuel transportation cask is required for heat balance, handling and shielding tests at Mound Research Laboratories (MRL). It will also be used for demonstration purposes.

D. FIELD TEST EQUIPMENT

The field test equipment consists of the following units, which are required to check out the RTG and AGE:

- (1) RTG field test equipment
- (2) FHM checkout equipment
- (3) Special tools
- (4) NaK ground service equipment

The field test equipment will be used for demonstration purposes.

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VI. SAFETY EVALUATION AND INTEGRATION--
CONTROL POINT 3300

A. SUMMARY OF SIGNIFICANT ACCOMPLISHMENTS

There were six major items of work accomplished during this reporting period. These consisted of:

- (1) Definition of the assumptions to be used in performing the SNAP 29 safety re-evaluation
- (2) Analysis of the shallow-water release hazard, using PDN (probability, dose, number of people) techniques
- (3) Computation of the six-degree-of-freedom motion of the fuel block
- (4) Completion of the preliminary aerodynamic wind tunnel testing
- (5) Revision of the impact test and analysis program
- (6) Completion of the first phase of the burial testing program.

A summary of each of these items follows; the details of these and other accomplishments will be discussed under separate control points.

The nuclear safety of the SNAP 29 system received preliminary analyses during the last quarter of 1966. Plans were made during this reporting period to re-evaluate the nuclear system safety. A list of assumptions to be used in the re-evaluation was prepared. The list included assumptions concerning the operational phases, the fuel form, the methods of safety analyses, the acceptance criteria to be used and the fuel block design. A meeting was held with Sandia personnel to discuss the assumptions and establish action items.

The analysis of the shallow-water release hazard was completed, using PDN techniques. Comparison of the results with preliminary acceptance criteria indicated hazard measures below the criteria.

The hypersonic aerodynamic coefficients were computed for use in studying the six-degree-of-freedom re-entry motion of the fuel block. Three basic edge shapes were considered. Modified Newtonian theory was used in the computations; induced and viscous forces were neglected.

The preliminary wind tunnel test program was completed. Three tests were performed. Two hypersonic force and moment tests were completed, one at the Martin Marietta Hot Shot Tunnel and one at the Arnold Engineering Development Center. A hypersonic aeroheating test was completed at the Hot Shot Tunnel.

The impact test and analysis program was revised. Emphasis is now being placed on conducting full-sized block tests to define the magnitude of the impact problem. Through correlation with test results, analytical techniques will be developed to predict the behavior of the fuel block under various impact environments.

The first phase of the burial test and analysis program was completed. Capsule temperatures of about 2400° F were measured when the test specimen, having a power input equal to about 50% of beginning-of-life fuel inventory, was buried one inch deep in sand. Analyses have been completed to compare with the test results. Some systematic errors indicate the need for more accurate thermal conductivity data for sand at high temperatures.

B. SAFETY EVALUATION--CONTROL POINT 3310

1. Nuclear Safety Re-evaluation Studies

a. Nuclear safety assumptions

The list of assumptions to be used in re-evaluating the SNAP 29 nuclear safety was prepared (Report 3314-20a). A topical outline of the contents of the assumption list follows:

A. Operational Phase

1. Handling
2. Transportation
3. Storage
4. Launch
5. Ascent trajectory
6. Orbital operation
7. Re-entry

B. Fuel Form

1. Type and amount
2. Released form
3. Solubility
4. Release rate

C. Analyses

1. PDN methods
2. High-altitude release
3. Deep-ocean diffusion
4. Shallow-ocean diffusion
5. Fresh-water diffusion
6. Soil migration and aquifer contamination
7. Plant and animal uptake
8. Resuspension

9. Ground and low-altitude diffusion
10. Internal dose calculations
11. External dose calculations

D. Criteria

E. Design

1. Fuel block
2. Fuel capsules
3. Generator.

Assumptions made in each of these areas represented engineering judgment and reflected state-of-the-art knowledge. The list of assumptions was discussed with Sandia personnel on March 13 and 14, 1967. General agreement on methods to be used was obtained. Significant areas of concern included:

- (1) Combined thermal, mechanical and chemical environment present after a launch abort
- (2) Evaluations of the shrapnel impact probabilities
- (3) Assumed release rates from punctured capsules
- (4) Safety acceptance criteria.

b. Shallow-water release

The shallow-water release hazard was evaluated in relation to the Sandia acceptance criteria. The criteria can be written as

$$\langle N \rangle_A \leq \frac{0.05}{D}$$

where $\langle N \rangle_A$ is the acceptable expected number of people receiving a whole-body dose of D rem. Most internal organs affected by polonium-210 are more resistant than the whole body to radiative damage. The equation applying to most internal organs is

$$\langle N \rangle_A \leq \frac{0.15}{D}$$

The Poisson distribution was used to describe the probability of individuals having different fish consumption rates. An occurrence probability for releasing 1000 curies of polonium-210 in shallow coastal water was estimated to be 4.7×10^{-9} (Report 3314-4). The following results were obtained when $\langle N \rangle$ was computed from the product of the occurrence probability and the number of people involved:

<u>Dose (rem)</u>	<u>N (million people)</u>	<u><N></u>	<u><N>_A</u>
0	18.0	0.085	--
0.1	15.5	0.073	1.5
0.2	11.5	0.054	0.75
0.25	7.8	0.037	0.6
0.3	3.6	0.017	0.5
0.4	0.2	0.001	0.38
0.5			0.3

In all cases, the expectation on N is below the acceptable levels. The number of people, N, refers to those exceeding the specific dose shown. These data are shown graphically in Fig. VI-1.

The body model for use in computing internal doses from an ingested, inhaled or skin-absorbed nuclide was completed and programmed for the IBM 7094. The dynamic behavior of the body organs was as expected, and the quantity of nuclide in each body organ was traced for the first 30 days following an assumed inhalation.

Due to the difficulty in tracing the probability logic through the various compartments of the body when using the UNITRAC programming method for single, discrete particles, it was decided to reserve this work and results for use in evaluating hazards involving only large numbers of fuel particles. Since the preliminary results of the USNRDL code indicate that hazards will most likely exist from exposures to single particles, work was temporarily suspended in favor of a solution using Monte Carlo techniques.

c. Dose-response relationship

A study of low-level dose-response relationships was undertaken. The study has pointed out that there exist no data for human leukemogenic responses (from whole body and bone irradiation) from doses below 100 rads. Among those cases involving large numbers of people with exposures to varying amounts of low-radiation doses, the two significant sets of data arise from the Japanese explosions and the former practice of treating a disease called ankylosing spondylitis by irradiating the spine with X-rays. The Japanese data are confused by the lack of accurate determinations of shielding factors from nearby buildings, whereas in the analysis of the spondylitic patient, responses are hampered by statistical interpretation and lack of a control group. While certain animal experiments have produced significant responses at levels of dose as low as 16 rads, there have been no experiments specifically designed to show relationships for extrapolation of animal data to man.

Fig. VI-1. Acceptance Criteria, $\langle N \rangle_A$ and Expectation, $\langle N \rangle_D$ Versus Kidney Dose

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A literature review has disclosed no conclusive evidence to support any specific dose-response function with which the extrapolation to low doses can be made. Rather, there exist data from both animals and man that suggest: (1) a linear dose-response relationship up to several hundred rads or (2) a skewed sigmoid curve or integrated gaussian function of dose versus effect. These two relationships are shown in Fig. VI-2.

d. Direct ingestion studies

Development of a direct ingestion model continued. This study involves the investigation of plant uptake of polonium-210 through the roots and by absorption through the foliage. Suitable probabilistic approaches are being evaluated. This work will continue next period.

e. Vehicle failure probabilities

A study was made of launch vehicle failures, their probability of occurrence and their effect on the payload. The investigation considered the types of launch vehicle failures which could result in a mission abort and cause an explosion and fireball on the launch pad or during flight. The period of time considered begins with liftoff of the launch vehicle and extends through the end of second stage burn at +465 seconds.

Based on reliability studies for a standard launch vehicle (in this case a five-segment Titan III with a two-stage liquid core) it was determined that the probability of mission success for injecting a radioactive device into a planned orbit is 0.94. The total failure probability of 0.06 has been divided into: on- or near-ground (up to pitch program at +10 seconds), with a failure probability of 0.004; and high-altitude, with a failure probability of 0.056. In the course of determining these figures, approximately 650 modes of failure were considered, and probability of occurrence figures were assigned to each failure mode.

A condition wherein the RTG must pass through the fireball and impact in the center of the residual burning would subject the RTG to the most extreme thermal environment. Certain types of failure in the solid rocket motor system could result in a tip-over on the launch pad, with the RTG impacting some distance from the center of the conflagration. Data from previous airborne incidents indicate that the payload section of vehicles generally continues along the flight path and escapes the severe destruct environment.

The launch vehicle subsystems considered in the failure mode analysis include structures, propulsion/pressurization, flight controls, electrical, tracking/flight safety, malfunction detection, solid rocket motors and

Fig. VI-2. Dose-Response Relationship

liquid rocket motors. The effects of each failure identified have been established for the early boost phase.

Figure VI-3 shows the probability of failure occurrence by time periods from liftoff through Stage II burn. Any failure prior to Stage 0 ignition was not considered in this portion of the launch vehicle analysis but was discussed in Report 3314-4, Initial Safety Evaluation for SNAP 29 System, November 7, 1966.

In a Sandia review of our initial safety evaluation, certain of our failure probability figures (launch pad fireballs and other aborts) were not compatible with Sandia's conclusions. A further review was made of available documentation on this subject in an attempt to find a similar study which would either support or disprove our figures. A NASA document released in 1964, covering 594 firings of all liquid propellant vehicles to that time, showed an on-pad fireball occurrence of 0.9%. Our figure for a similar occurrence for a Titan III vehicle is 0.2%; no data have been found that would degrade this probability of occurrence figure.

f. Downwind diffusion

The diffusion climatology at WTR has been studied. This type of climatology is concerned with the characteristics and distribution of the meteorological parameters pertinent to the diffusion of pollutants released into the atmosphere.

Since, at present, there are no time specifics for SNAP 29 operations, the on-shore and off-shore wind flow was analyzed by season, month and time of day (in 3-hour intervals) to provide probabilities for the safety analyses. In addition, statistics have been obtained on surface-based and subsidence inversion conditions as a function of month and time of day. The correlation between on- and off-shore wind flow and inversions has also been specified.

g. Impact probabilistics

A study of impact probabilistics was started during February. Using a random impact distribution with latitude and longitude, an equal impact probability of 1/648 may be assigned to each area of the earth's surface 10° in latitude by 10° in longitude. Each of the 648 areas is taken to be composed of shallow water, deep water, ice, sand, mountains, farmland and urban areas. In each individual 10° by 10° area, the probability of impacting one of these media is proportional to the fraction of the surface area covered by the medium. Assigned to each 10° by 10° area there is also a population. Populations are being assigned to the areas first. Once the populations have been assigned, maps will be consulted for the accurate determination of the impact probabilities in

regions of high population. In oceans and sparsely populated areas, the required accuracy is not as great. Probabilities are being written directly on loading forms in preparation for eventual computerization.

h. Plans for next quarter

During the next quarter, emphasis will be placed on completing the safety re-evaluation. Following this work, safety requirements will be defined and detailed model development will be started for detailed hazard investigations.

C. AERODYNAMIC ANALYSES AND TESTS--CONTROL POINT 3320

The prime areas of aerodynamic analyses conducted during this reporting period were: estimation of force and moment data for six degrees of freedom (Report 3321-9) and the development of an error analysis program for the hybrid digital-analog computer. Considerable effort was also expended in evaluating the wind tunnel data obtained to date and putting it into proper form for use in the six-degree-of-freedom program.

1. Estimated Force and Moment Data

The estimated hypersonic static aerodynamic characteristics required for a six-degree-of-freedom dynamic motion re-entry study were completed during this period. These aerodynamic coefficients were derived by using modified Newtonian hypersonic theory; hence, nose-induced and viscous-induced effects have been neglected. The Martin Marietta Newtonian Force and Plot program was utilized in the study. This program possesses the capability of determining projected views of arbitrary shaped bodies at arbitrary orientations. The program accepts the coordinates of a series of planes which approximate the shape of the body and calculates the local pressures based on modified Newtonian theory. These pressures are integrated to determine the vehicle aerodynamic coefficients as a function of vehicle altitude. Simultaneously, a plot tape is generated which produces a view of the body projected on a plane normal to the velocity vector. Any reference area, reference length and center of moments may be chosen. The results are plotted in graphic form as a function of attitude.

For the SNAP 29, the attitude is defined as a function of η , the wind tunnel pitch angle, and ξ , the wind tunnel roll angle. These angles are defined in the coordinate system shown in Fig. VI-4.

The edge shapes investigated for the flat plate fuel block are shown in Fig. VI-5. The hemicylindrical edge shape is considered to be the basic shape for the fuel block. In all cases, the body is considered to

Fig. VI-3. Probability of Catastrophic Failure Occurrence for a Standard Launch Vehicle

Fig. VI-4. Reference Coordinate System

Hemicylindrical Edge-shape Design

Flared Edge-shape Design (10° ramp)

Flared Edge-shape Design (20° ramp)

Bulbous Edge-shape Design (10° ramp)

Bulbous Edge-shape Design (20° ramp)

Fig. VI-5. Various Edge Shape Designs

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be oriented with its longest edge perpendicular to the flow when η and ξ are equal to zero. The computed aerodynamic coefficients are presented in Figs. VI-6 through VI-20.

Figures VI-6 through VI-11 show the normal, axial and side force coefficients, and the roll, pitch and yaw moment coefficients for the hemicylindrical edge. Figures VI-12 through VI-17 show the computed results for the 10° flared edge.

The 10° flared edge incorporates isotropic compression on the windward side to preclude separation. The thick blunt edge is for the purpose of increasing the damping if the block spins edge-on. A 20° flare is also shown in Fig. VI-5. This shape allows the same edge thickness with the structural and weight advantage of a narrower edge member. This configuration was examined for a limited set of conditions with roll angle, ξ , equal to zero. The results are compared with other edge designs in Figs. VI-18 through VI-20. Wind tunnel tests will show the amount of ramp permissible without flow separation.

The bulbous edge designs, depicted in Fig. VI-5, combine the features of a thicker hemicylindrical edge and flare. Again, only the zero roll cases were examined. The aerodynamic coefficients for 10° and 20° ramps are included in Figs. VI-18 through VI-20 for comparative purposes.

The static aerodynamic characteristics determined by Newtonian theory show that the basic fuel block with cylindrical edges is stable when broadside to the wind. On the other hand, any edge with a ramp tends to make the block stable with the edge to the wind. But Newtonian theory excludes consideration of nose-induced and viscous-induced effects. Hence, the adverse pitching moment found for the flared and bulbous edges must be compared with wind tunnel data in conjunction with six-degree-of-freedom trajectory calculations before final conclusions can be reached.

A comparison of the pitching moment coefficient results from the AEDC Tunnel B tests and the modified Newtonian values for the various fuel block edge shapes is shown in Fig. VI-21. The comparison is limited to $\xi = 0^\circ$; hence it is a function only of the total angle of attack, η . The fuel block configurations with the basic hemicylindrical edge shape, wind tunnel model, E^1 , and the bulbous edge with a 10° ramp, E^2 , were tested through the entire total angle of attack range of 0 to 90°. The other models, bulbous edge-20° ramp, E^3 , flared edge-10° ramp, E^4 , and flared edge-20° ramp, E^5 , were tested only through a total angle of attack range of 0 to 35°.

Fig. VI-6. Normal Force Coefficient Versus Total Angle of Attack for Various Roll Angles--Hemicylindrical Edge

Fig. VI-7. Axial Force Coefficient Versus Total Angle of Attack for Various Roll Angles--Hemicylindrical Edge

$$A_{\text{ref}} = 10.56 \text{ ft}^2$$

Fig. VI-8. Side Force Coefficient Versus Total Angle of Attack for Various Roll Angles--Hemicylindrical Edge

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Fig. VI-9. Yawing Moment Coefficient Versus Total Angle of Attack for Various Roll Angles--Hemicylindrical Edge

Fig. VI-10. Rolling Moment Coefficient Versus Total Angle of Attack for Various Roll Angles--Hemicylindrical Edge

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Fig. VI-11. Pitching Moment Coefficient Versus Total Angle of Attack for Various Roll Angles--Hemicylindrical Edge

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Fig. VI-12. Normal Force Coefficient Versus Total Angle of Attack for Various Roll Angles--Flared Edge (10° ramp)

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Fig. VI-13. Axial Force Coefficient Versus Total Angle of Attack for Various Roll Angles--Flared Edge (10° ramp)

Fig. VI-14. Side Force Coefficient Versus Total Angle of Attack for Various Roll Angles--Flared Edge (10° ramp)

Fig. VI-15. Yawing Moment Coefficient Versus Total Angle of Attack for Various Roll Angles--Flared Edge (10° ramp)

$$A_{ref} = 10.56 \text{ ft}^2$$

$$l_{ref} = 3.38 \text{ ft}$$

Fig. VI-16. Rolling Moment Coefficient Versus Total Angle of Attack for Various Roll Angles--Flared Edge (10° ramp)

Fig. VI-17. Pitching Moment Coefficient Versus Total Angle of Attack for Various Roll Angles--Flared Edge (10° ramp)

- ① Hemicylindrical edge
- ② Flared edge (10° ramp)
- ③ Bulbous edge (10° ramp)
- ④ Flared edge (20° ramp)
- ⑤ Bulbous edge (20° ramp)

Fig. VI-18. Normal Force Comparisons of the Various Edge Shape Designs at $\xi = 0^\circ$

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Fig. VI-19. Axial Force Comparisons of the Various Edge Shape Designs at $\xi = 0^\circ$

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Fig. VI-20. Pitching Moment Comparisons of the Various Edge Shape Designs at $\xi = 0^\circ$

Fig. VI-21. Pitching Moment Data/Theory Comparisons of the Various Edge Shape Designs at $\xi = 0^\circ$

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The test data for all the edge shapes result in an unstable pitching moment trend at $\eta = 0^\circ$. The results obtained with shapes E^1 and E^2 at high total angles of attack demonstrate a trim condition at $\eta = 90^\circ$. Additional trim values are shown for models E^2 and E^3 at $\eta \approx 19^\circ$ and for shapes E^4 and E^5 at $\eta \approx 12^\circ$. The E^2 and E^3 shapes produced no change in values of pitching moment coefficient from those obtained for the E^1 edge shape at total angle of attack values below 10° . The values of the pitching moment coefficient for E^1 and E^2 show small differences for angles of attack above 50° . Hence, the major effect on pitching moment due to thickening the cylindrical edge, using a ramp or flare, appears to be limited to the total angle of attack range from 10° to 50° . The E^4 and E^5 edge shapes show stronger stabilizing moments at low angles of attack than do E^2 and E^3 . Although the stable angle is not zero, as predicted by Newtonian theory, this effect was inferred from the computed values.

2. Hybrid Error Analysis Program

The TB-095 error analysis program is a six-degree-of-freedom digital computer program which propagates the error in the state vector about a nominal re-entry trajectory as a function of the uncertainty of the initial conditions. Options are also provided permitting inclusion of uncertainties in aerodynamic characteristics, atmospheric densities and other model parameters which may contribute to errors in the state vector. If desired, the standard deviations from the norm can be set equal to zero. In this case, rapid computations of six-degree-of-freedom trajectories and dynamic motions are possible.

Two important assumptions are made which place limits on the permissible magnitude of the standard deviations: (1) the error in each component of the state vector is treated as if its distribution were gaussian; (2) the error distribution is described completely by one number, the standard deviation. The mean of the standard deviation is assumed to be the nominal value. These assumptions require linear responses in the state vector during the computation of the trajectory. To ensure this linearity, the size of the errors which the program can meaningfully handle is limited.

The planetary model assumes a rotating, oblate earth surrounded by an atmosphere. The atmosphere can be 1959 ARDC, 1962 standard or in exponential form. It is possible to input atmospheric winds as a function of altitude.

In its present form, the program utilizes quaternion equations to avoid the singularities associated with the more conventional methods

of computing the Euler angles. This is necessary since the motion involves complete freedom in angular excursion.

The error analysis program described above has been completed. It is the first part of a two-phase approach to the solution of the dynamic re-entry problem. This digital program was written to check the feasibility of using the hybrid (digital-analog) computer instead of the IBM 7094 machine as an expedient. As a result, the state transition matrix in this digital program is calculated using finite difference methods which would be used on the analog.

The second phase of the study involves the determination of the feasibility of computing the error analysis by means of the hybrid rather than the IBM 7094 digital computer. The feasibility will be dependent upon the accuracy and repeatability of the analog and the sensitivity of the finite difference method for obtaining the state transition matrix to small error.

3. Aerodynamic Tests

a. Hypersonic force and moment tests of edge shapes

A wind tunnel test of 10 and 17% scale models of the SNAP 29 fuel block has been conducted in the Martin Hot Shot Tunnel (at a Mach number of 20) to determine the aerodynamic characteristics of the fuel block with four edge-shape configurations. Testing was completed in December. Results of the test, shown in Figs. VI-22 and VI-23, indicate that:

- (1) The measured values of normal force coefficient and pitching moment coefficients are similar but larger in magnitude than those estimated using Newtonian theory.
- (2) The pitching moment coefficient curve is more negative than estimated values.
- (3) The maximum value of normal force coefficient was measured at an angle of attack of 90° and had a value of 1.66.
- (4) The differences in measured results for the four edge shapes shown in Fig. VI-24 were small, except for the axial force coefficient, where the flared edge blunt configuration was consistently greater over the measured angle of attack range.

b. Hypersonic aeroheating tests of edge shapes

A wind tunnel test of 30% scale models of a section of the SNAP 29 fuel block has been conducted in the Martin Hot Shot Tunnel at a Mach number of 20. The purpose of the test is to determine the heat distribution around the edges and corner of the fuel block for four edge-shape

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Fig. VI-22. Force and Moment Data for Edge Shapes E^1 and E^2 ,
 $\alpha = 0^\circ$ to 90°

Fig. VI-23. Variation of C_A , C_N and C_m with Angle of Attack (0 to 15°) for Edge Shapes E^1 , E^2 , E^3 and E^4

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Fig. VI-24. SNAP 29 Fuel Block Edge-Shape Configurations Tested in AEDC Tunnel B

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configurations. The four thin skin models which were instrumented with from 34 to 50 thermocouples were tested during February and March 1967 through an angle of attack range of 0° to 90° and yaw angles of 0° to 45°. Seventy-six test runs were made. Data reduction is scheduled for completion during the next period.

c. Preliminary hypersonic edge member test

A wind tunnel test of 25 and 50% scale models of the SNAP 29 fuel block has been conducted (at a Mach number of 8.0) in AEDC Tunnel B to obtain six-component force and moment data for five edge-shape configurations. The tests were completed in January 1967. The edge shapes tested are shown in Fig. VI-24. Representative data obtained from the test are shown in Figs. VI-25 through VI-37. The following conclusions were derived from the results obtained:

- (1) The shape of the edge does not have an appreciable effect on the predominant force component, C_N .
- (2) The hemispherical edge E^1 showed a stable trim point only at 90° angle of attack.
- (3) All the enlarged edges E^2 , E^3 , E^4 and E^5 displayed stable trim points at low angles of attack. The trim points occurred at approximately 12° and 19° for the blunt and rounded edges, respectively.
- (4) Yawing moments on the capsule were so insignificant that they could not be measured with any degree of accuracy.
- (5) A sudden rise in axial force appeared at about 25° angle of attack for the E^3 , E^4 and E^5 shapes.
- (6) The interaction of the secondary shock of all the enlarged edge shapes may produce a local heating problem.
- (7) The magnitude of the pitching moments was greater than predicted.
- (8) The design of the models proved to be very good in that the presence of the balance and its fairing appeared to have little or no influence on the forces being measured.

4. Future Investigations

The following items are scheduled for study during the next quarter:

Fig VI-25. Variation of Normal Force Coefficient for E^1

Fig. VI-26. Variation of Pitching Moment Coefficient for E^1

Fig. VI-27. Variation of Rolling Moment Coefficient for E^I

Fig. VI-28. Variation of Axial Force Coefficient for E^1

Fig. VI-29. Variation of Side Force Coefficient for E^1

Fig. VI-30. Variation of Normal Force Coefficient for E^2

Fig. VI-31. Variation of Pitching Moment Coefficient for E^2

Fig. VI-32. Variation of Rolling Moment Coefficient ξ^2

Fig. VI-33. Variation of Axial Force Coefficient for E^2

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Fig. VI-34. Variation of Side Force Coefficient for E^2

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Fig. VI-35. Forces and Moments for E^3

Fig. VI-36. Forces and Moments for E^4

Fig. VI-37. Forces and Moments for E^5

- (1) The force and moment data obtained from the Martin Hot Shot and AEDC Tunnels will be evaluated. This involves comparison with theoretical estimates and resolving any apparent discrepancies.
- (2) Development of the HEAP program has progressed to the point of making some trial runs on the hybrid analog computer. A thorough evaluation of this concept with respect to its accuracy and applicability will be made. If the applicability is not as broad as originally anticipated, further plans must be made for conducting the abort studies so as to be compatible with the PDN analysis.
- (3) An estimation will be made of the free molecular and transitional aerodynamic force and moment coefficients. It is not possible to experimentally determine free molecular coefficients; hence, they will be derived by theoretical means. Transitional coefficients will be based on theory and existing experimental data. Computer runs will be made to determine the importance of employing variable aerodynamics on the overall re-entry analysis.
- (4) Although theoretical damping coefficients are often questionable, it is planned to estimate their magnitude for a tumbling fuel block. By introducing damping terms into the motion study being run on the computer, it is possible to ascertain their effect on the dynamic action of the fuel block. The results of this study will be used to determine the need for additional wind tunnel tests of a rotating body.
- (5) An abort study plan for an RTG is intimately related to the launch vehicle and launch trajectory. At the present time, only a representative launch trajectory is available. It assumes the launch vehicle is a Titan IIIC booster fitted to an Agena D. A preliminary abort plan is being outlined for this assumed launch mode.
- (6) Trajectory, aerodynamic heating and abort studies will be made in support of the SNAP 29 Safety Re-evaluation Report.

D. IMPACT AND BURIAL TESTING AND ANALYSES-- CONTROL POINT 3330

1. Impact Testing and Analyses

A new exploratory impact test program was developed for the graphite fuel block concept. Emphasis has been placed on a program which

will provide supporting empirical data for dynamic analysis. This will enable development of analytical models and techniques which, once established, will be capable of evaluating impact problems associated with the SNAP 29 system without a major supporting test effort.

As in the previous plan, the impact test program will be conducted in four parts. The first phase will be exploratory and involve evaluation of different fuel matrix configurations, preliminary establishment of an impact shock environment, and definition of the basic problems associated with the impact of a SNAP 29 fuel block. The second phase will involve development of both analytical and experimental test models to represent the actual fuel block in impact test evaluations. The third phase will determine the impact failure criteria for the fuel block test models, and the final phase will examine both full-scale models and qualified fuel blocks under impact conditions to lend final confidence to analyses and verify the structural integrity of the SNAP 29 fuel block design.

The exploratory impact test phase (CP 3331), which will be initiated immediately, consists of three tasks:

- (1) Shock environment. The objective of this test is to obtain a preliminary definition of the shock environment occurring at the impact-end of a simulated fuel capsule enclosed in graphite.
- (2) Matrix form evaluation. This test will evaluate the comparative structural responses of different fuel matrices currently being considered by Mound Laboratory.
- (3) Fuel block impact. This test will be conducted to identify the basic problems associated with fuel block impact and to more fully establish an impact shock environment for follow-on tests. Both helicopter drop tests and high velocity full-scale impact tests will be performed.

In a follow-on to the graphite evaluation impact tests performed in December 1966 (Report No. 1140-91), a single impact of a graphite block (Type PT-0178) was performed at the Martin Marietta rocket sled facility. The specimen measured 1 x 2.7 x 10 inches and was impacted cold at a velocity of 153 fps. The overall response at impact appeared similar to that observed with ATJ and Poco graphite in the December test; that is, considerable pulverization occurred initially, followed by a brittle shattering.

A new exploratory impact test program was developed for the graphite fuel block concept.

2. Burial Testing and Analyses

Preliminary burial tests were started during this report period. The first burial test was terminated due to heater failure on February 21, 1967, 148.5 hours after initiation of the test. The test involved measuring the temperature distributions on a 0.75-inch square x 16 inch long graphite specimen and throughout the sand bin. A 16-inch quartz lamp inserted in the specimen was used as the heat source. The specimen was buried one inch below the sand surface. The heat input started at 500 watts and increased in 100-watt intervals, following attainment of steady-state conditions at each preceding power level. At the time of termination, the power level was 800 watts. Specimen temperatures exceeded 2250° F.

No visible trace of the graphite remained after the test, and preliminary indications are that it underwent complete oxidation. No unusual physical change of the sand, such as fused silica, was noticed. A slight discoloration of the sand was observed in the area around the specimen. A soil analysis to determine any chemical reactions is required. The possibility of having such an analysis performed at Martin Marietta is being investigated.

The quartz heating lamp appeared to have suffered no chemical reaction, although it was broken at several places, probably due to thermal stresses during the cooling phase.

Subsequent tests have been performed using an Inconel specimen of the same dimensions as the graphite specimen. Tests at burial depths of 1, 2, 4 and 6 inches have been completed in sand. The 11-inch burial test is now in progress. The test results are summarized in Figs. VI-38 and VI-39. A comparison of the preliminary analytical results with the test results is shown in Figs. VI-40 to VI-45. A key for interpreting the graphs is shown in Fig. VI-46. It is anticipated that refinements to be made in the thermal model, along with the use of better physical property data, will provide better agreement between test and analytical results.

The next series of tests will involve a partial block (Inconel) consisting of five fuel channels. Similar burial depths will be evaluated.

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

No effort has been expended on this control point during this reporting period; none is planned for the next quarter.

Fig. VI-38. Burial Test Data--Temperature Versus Power

Fig. VI-39. Burial Test Data--Depth Versus Capsule Temperature

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Fig. VI-40. Sand Temperature Versus Vertical Distance

Fig. VI-41. Sand Temperature Versus Distance Along a 45° Line

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Fig. VI-42. Sand Temperature Versus Horizontal Source Distance

Fig. VI-43. Sand Surface Temperature Versus Horizontal Distance

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Fig. VI-44. Sand Surface Temperature Versus Time

Fig. VI-45. Sand Temperature Versus Time

Fig. VI-46. The Five Requested Conditions

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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-2062-3, "RTG Power Nuclear Fuel and Fuel Encapsulation Interface Specification, SNAP 29 Power Supply System."

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

1. Fuel Compatibility

- a. C capsule program
- b. T capsule program
- c. E capsule program

2. Fuel Properties (Po and GdPo and Ta matrix)

- a. 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
- b. Information constraining final confirmation of fuel form
 - (1) Fuel form vapor pressure
 - (2) Phase equilibrium of $\text{GdPo}_{1-x}\text{Pb}_x \left| \begin{matrix} x=1 \\ x=0 \end{matrix} \right.$ system
 - (3) Gas blanket experiment
 - (4) Fuel matrix studies

3. Material Weld and Braze Studies for Capsules and Heat Source
(combined Martin Marietta/Mound program)

- a. TIG versus EB weld of capsule liner (strength members). Evaluate weld and joint efficiency by tensile and burst tests
- b. Qualify Martin Marietta/Mound welding of Ta inner liners
- c. Establish capsule clad weld process
- d. Establish capsule/fuel block braze process.

4. Establishment and Qualification of a Cold Encapsulation Line, Including Tantalum Matrix Preparation

- a. Fixture development and line installation
- b. Capsule weld development, 40 samples
- c. Unit cells and heat source braze development, 2 fuel blocks, 6 unit cells, 6 half-unit cells
- d. Impact specimens fabricated to final fuel block specifications and for use in Martin Marietta Reliability Impact Test Program, 160 unit cells.

5. Establishment and Qualification of a Fuel Loading and Hot Encapsulation Facility

- a. Fixture development and line installation
- b. Hot capsule fabrication, 4 samples
- c. Production development heat source fabrication, one
- d. Production demonstration heat source fabrication, one.

6. Prepare Final Heat Source End Item Specification to Incorporate

- a. Martin Marietta heat source performance and design requirements
- b. Martin Marietta heat source drawings
- c. Mound capsule, fueling and heat source fabrication procedures
- d. Mound quality control procedures.

7. Fuel and Assembly Heat Sources as Authorized

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A. SUMMARY OF SIGNIFICANT ACCOMPLISHMENTS

This portion of the SNAP 29 quarterly report represents Mound Laboratory accomplishments from December 1, 1966 through February 28, 1967.

1. Fuel Compatibility

a. C capsule program

A portion of the controlled 1000° C polonium-210 fuels compatibility test series, designated as the C capsule tests, has direct applicability to the SNAP 29 development program. The 1000° C test series of polonium-210 metal and gadolinium polonide with tantalum and tantalum-10% tungsten is the portion discussed herein.

The capsules used in these studies are cylindrical containers, 0.5 inch high and 0.25 inch in diameter, with 0.020-inch thick walls. They are loaded with approximately 250 curies of polonium-210 in either fuel form. The internal volume of the capsule is 0.26 cm³, which permits a nominal power density of 950 curies/cm³ or 30 watts/cm³. The sealed compatibility couples are placed in containers of either Haynes-25 or Hastelloy-X for oxidation protection during the heat period. Test periods of 50, 150 and 200 days are carried out on sets of couples consisting of three loaded and one control blank.

To date, 71 capsules have been placed under heat test at 1000° C. The 50-day test period for the couples tantalum-gadolinium polonide, tantalum-polonium-210, tantalum-10% tungsten-gadolinium polonide, tantalum-10% tungsten-polonium-210 and Haynes-25-polonium-210 has been completed and reported in the first SNAP 29 Quarterly Progress Report, MND-2062-12-6.

Table VII-1 summarizes the 1000° C compatibility test results. Parameters for C capsules remaining under test are given in Table VII-2.

(1) Tantalum-polonium-210. The 200-day test couples C20, C23, C24 (control specimen) and C26 have been removed from heat test and are currently being evaluated.

Of this group, only C23 has been metallographically examined. It showed complete penetration of the 20-mil cross section. Two other capsules heated for 200 days, C22 and C28, were examined. Capsule C22 was completely penetrated. Capsule C28, a control specimen, showed no attack. A third tantalum-polonium-210 couple, C25, was removed after 168 days because of bulging in the outer body. It was completely penetrated also.

TABLE VII-1

Summary of Compatibility Test Results with Polonium-210 Fuel Forms at 1000° C*

Container Material	Fuel Form	Test Power Density (watts/cm ³)	Test Period (days)	Capsule Number	Metallographic Observations
Ta	Po	12	150	C11	No detectable attack
		26	50	C13	No detectable attack
		29 to 35	50	C12, C17	Complete intergranular penetration
		28	77	C15†	Complete intergranular penetration
		36	131	C16†	Complete intergranular penetration
		20	168	C25†	Complete intergranular penetration
		35	187	C23†	Complete intergranular penetration
		31	200	C22†	Complete intergranular penetration
Ta	GdPo	18 to 29	50	C36, C37, C38, C39	No detectable attack
		18 to 29	150	C38, C41, C42	No detectable attack
Ta-10W	Po	31	27	C57†	No detectable attack
		28 to 34	50	C56† C58†	Inner and outer surface attack
Ta-10W	GdPo	19 to 27	50	C52†, C53†, C54	No detectable attack
Haynes-25	Po	11 to 47	50	C65, C66, C67	2 mils maximum inner surface attack
Haynes-25	GdPo	27 to 36	50	C73, C77, C78	4 mils maximum inner surface attack

* Capsules loaded and sealed while under an argon environment. The capsule is then sealed inside a strength-type container of Haynes-25 alloy.

† Removed from heat testing before scheduled time because of bulging in outer oxidation body

† Weld area contained microcracks

TABLE VII-2

Polonium-210 Fuel C-Series Compatibility Samples Under Test*

Sample No.	Container Material	Fuel Form	Power Density on Date of Loading (watts/cm ³)	Power Density on Date of Heating (watts/cm ³)	Neutron Count Rate (n/sec/c)	Total Days Heated at 1000° C (2/28/67)
C47	Ta	GdPo	13.1	12.7	796.9	171
C48	Ta	GdPo	45.4	45.4	44.6	177
C71	Haynes-25	Po	42.4	41.4	53.8	146
C72	Haynes-25	Blank	--	--	--	146
C74	Haynes-25	GdPo	32.8	32.3	63.7	129
C75	Haynes-25	GdPo	53.8	53.0	118.2	129
C79	Haynes-25	GdPo	26.5	24.9	83.6	101
C80	Haynes-25	Blank	--	--	--	101
C81	Haynes-25	GdPo	28.3	26.5	107.2	101

* Capsules loaded and sealed while under an argon atmosphere. The capsule is then sealed inside a strength-type container of Haynes-25 alloy.

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(2) Tantalum/gadolinium polonide. The 150-day test period capsules C38, C41, C42 and C43 (control specimen, i. e., no fuel) were metallographically examined.

No corrosion was observed in any of the four test specimens. Figure VII-1 shows the interior surface of Specimen C42, upon which the polonide had rested during the 150-day exposure. The condition of this interior surface is similar to that of Control Specimen C43 shown in Fig. VII-2.

Two tantalum/gadolinium polonide specimens, C44 and C45, have been removed from testing after 200 days/ exposure. Specimens C47 and C48, identical to C44 and C45, respectively, will complete 200 days of testing during the next report period.

(3) Tantalum-10%-tungsten/gadolinium polonide. Fifty-day compatibility data for the tantalum-10%-tungsten/gadolinium polonide couple were previously reported in the first SNAP 29 quarterly report. The remaining 200-day capsules have been removed from the furnace and are being prepared for metallographic examination.

(4) Haynes-25/polonium-210. Metallographic evaluation of the Haynes-25/polonium-210 Specimens C65, C66 and C67, and Control Specimen C68, was completed. These specimens were heat tested for 50 days at 1000° C.

Some corrosion of the Haynes-25 by the polonium-210 was observed. The form of the corrosion is that of an adherent, dark, etched and porous surface layer. Figures VII-3, VII-4 and VII-5 are photomicrographs of this corrosion product. The thickness of the corrosion zone varied from about 2 mils maximum at the base of the Haynes-25 capsule to less than 1 mil at the top of the capsules. (The capsules are 1/2 inch high). The precipitates present in Control Specimen C68 (see Fig. VII-6) were not observed next to the corroded regions of these specimens. Maximum depth of the depleted zone in the corroded regions was 2 to 3 mils. An absence of precipitates next to the corroded surfaces suggests that a change in composition of the Haynes-25 alloy has occurred. Such a compositional change would be expected to affect the mechanical properties of the alloy.

Two Haynes-25/polonium-210 capsules, C69 and C70, were removed from testing after 135 days at 1000° C. The outer capsules which contained these capsules, also Haynes-25, had bulged. This indicates that the polonium-210 has escaped from the inner test capsule.

Two additional specimens are still being heat tested.

Bottom interior surface

Base of Ta capsule

Fig. VII-1. Compatibility Capsule C42 After 150 Days' Exposure to Gadolinium Polonide at 1000° C. No Visible Corrosion

Interior surface

Cross section of Ta capsule

Fig. VII-2. Appearance of Inner Surface of Control Capsule C43 After 150 Days at 1000° C. No Fuel was Loaded into this Capsule

Corrosion zone

Depleted zone

Haynes-25 capsule base

Interior side wall

Fig. VII-3. Base of Compatibility Capsule C66 Showing Region of Maximum Polonium Attack

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Capsule interior

Corrosion zone

Haynes-25

Fig. VII-4. Cross Section of Compatibility Capsule C66 Approximately 1/4 Inch from Base

Porous corrosion zone

Haynes-25

Fig. VII-5. Cross Section of Compatibility Capsule C65 Showing Appearance of Haynes-25/Polonium Corrosion Zone at a Higher Magnification than Previous Figures

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(5) Haynes-25/gadolinium polonide. Gadolinium polonide was found to attack Haynes-25 similarly to the polonium-210 metal. Figures VII-7 through VII-9 show the appearance of the corroded Haynes-25 alloy after 50 days at 1000° C. The as-polished photomicrograph, Fig. VII-8, shows the porous nature of the corrosion zone. The microstructure of Control Specimen C76 is shown in Fig. VII-12.

Polonide attack of the Haynes-25 varied from top to bottom of the capsules. Maximum corrosion of about 4 mils occurred at the base of Specimens C73 and C77. Less corrosion was observed in Specimen C78. Approximately 1 mil of corrosion was measured at its base, with negligible attack occurring near the top (see Figs. VII-10 and VII-11). Specimen C78 contained the lowest power density of the three capsules. Depletion of the substrate precipitates was also observed next to the corrosion zone in these specimens.

(6) Conclusions. Experimental results to date indicate that tantalum and tantalum-10%-tungsten are compatible with gadolinium polonide at a nominal temperature of 1000° C for at least 150 days. The polonide moderately attacks the Haynes-25 alloy within a 50-day period.

Polonium-210 is not compatible with the three candidate capsule materials that have been tested. Haynes-25 is the most corrosion resistant of the three.

b. T capsule program

Prior to the C capsule test series, a series of 10 general RTG design capsules was fabricated with the combination of either polonium-210 metal or gadolinium-polonide fuel in tantalum and Haynes-25 containers. The design, and the test criteria and results for four of the capsules in this series have been designated as the T capsule tests. The results of these tests are presented in the first SNAP 29 quarterly report, MND-2062-12-6.

c. E capsule program

A new series of tests, designated the E series, has been initiated. 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. The present status of this test is reported in Table VII-3. The inner container design for this series is the same used in the C series of tests (internal volume 0.26 cm³).

After the inner capsule is loaded with gadolinium polonide powder and closed by TIG welding, it is placed in a tantalum-10%-tungsten strength body. The inner capsule is wrapped with a single sheet of one-mil tantalum foil and shimmed at both ends to give a tight fit within

Haynes-25

Interior of
capsule

Fig. VII-6. Appearance of Inner Surface of Control Capsule C68 After 50 Days at 1000° C. No Fuel was Loaded into this Capsule

Corrosion zone

Haynes-25 base
of capsule

Fig. VII-7. Base of Compatibility Capsule C73 Showing Region of Maximum Gadolinium Polonide Attack

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Porous corrosion zone

As-polished
Haynes-25

Fig. VII-8. Compatibility Capsule C73 Showing Gadolinium Polonide Attack of the Haynes-25 Approximately 1/4 Inch from Base of Capsule

Corrosion zone

Haynes-25

Fig. VII-9. Compatibility Capsule C77 Gadolinium Polonide Attack of the Haynes-25 Approximately 1/4 Inch from Base of Capsule

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Corrosion zone

Depleted zone

Haynes-25

Fig. VII-10. Compatibility Capsule C78 Indicating Possible Decreased Gadolinium Polonide Attack at a Lower Fuel Power Density. Section Located Approximately 1/4 Inch from Base of Capsule.

Interior surface

Haynes-25

Fig. VII-11. Interior Surface of Compatibility Capsule C78 Near Top of Capsule. Attack was Negligible at Top of Capsule

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TABLE VII-3

Polonium-210 Fuel E-Series Compatibility Data at 1500° C
in a Vacuum Environment

<u>Sample Number</u>	<u>Test Power Density (watts/cm³)</u>	<u>Neutron Emission Rate (n/sec/c)</u>	<u>Total Days Heated at 1500° C (2/28/67)</u>
E1	Blank	--	33
E2	27.0	337.4	33
E3	31.0	87.5	14

the tantalum-10%-tungsten strength body. This ensures that the test capsule is constrained during heat testing. The presence of the tantalum foil barrier also eliminates any consideration of capsule contamination due to incompatibility of tantalum and tantalum-10% tungsten.

The above assembly is then placed in a high temperature vacuum furnace for the heat test. Periodically, the furnace is shut down and the vacuum chamber filled with helium gas. Observation of the capsule assemblies in the inert atmosphere should reveal any containment failure (presence of the characteristic alpha particle gas ionization glow).

At the present time, two fueled capsules are being tested. The results of these tests are summarized in Table VII-3. No visual capsule failures have occurred at this time.

2. Fuel Properties (Po and GdPo and Ta matrix)

a. Information constraining safety conclusions

(1) Gaseous environment reactivity. Knowing the behavior of polonium-containing fuel forms in an air environment is highly desirable because of the biological hazard associated with their use in radioisotopic power source applications such as the SNAP 29 program. It is desirable to understand the mechanism and rate by which the toxic radioisotope is released to the atmosphere and vicinity surrounding a capsule when there is partial or total loss of containment of the fuel. Experiments are being conducted in which fuels and capsules containing fuel are subjected to air at or near standard conditions so that the exact nature of the effect of atmospheric oxygen upon the radioisotopic fuel can be studied. The first experiment was reported in SNAP 29 First Quarterly Progress Report, MND-2062-12-6. Additional tests are being planned but have been delayed due to priority effort on fuel form investigations.

(2) Aqueous environment reactivity. No report to date.

(3) Mechanical stability of Ta matrix --GdPo system using GdTe simulation. No report to date.

(4) Thermal conductivity of Ta matrix. No report to date.

(5) Capsule burial studies. Actual radioisotope fueled capsule burial studies may be conducted at a later date to demonstrate fuel release mechanisms.

b. Information constraining final confirmation of fuel form.

(1) Fuel form vapor pressure. No report to date.

(2) Phase equilibrium of $\text{GdPo}_{1-x}\text{Pb}_x$ $\left| \begin{smallmatrix} x=1 \\ x=0 \end{smallmatrix} \right.$ system. No report to date.

(3) Gas blanket experiment. This experiment was completed and reported in SNAP 29 First Quarterly Progress Report, MND-2062-12-6.

(4) Fuel matrix studies. The incorporation of GdPo into an annular configuration of compacted tantalum powder has been demonstrated to be a practical high temperature polonium-210 fuel form. Because of the high density of tantalum, there has been concern about the weight added to the capsule by the tantalum compact when using this type of fuel form. The use of a metal of lower density as the matrix material would be desirable, especially in applications such as the SNAP 29, where the 1000° C fuel operating temperature does not limit the matrix materials to refractory metals. However, compatibility of the metal with GdPo and the liner material is a requirement.

Tantalum matrix studies. Sufficient matrices can be produced by a linear compaction technique to support the SNAP 29 program. This pressing technique does place several restrictions on the final product. At present, the length of each matrix must be less than 0.400 inch and close to 60% theoretical density for a fuel element measuring about 0.4 inch in diameter. The length restriction means that about 20 inches of matrices can be prepared per shift. A density variation of greater than 10% across the length is obtained if the bulk density is pressed greater than 60% theoretical. It was felt that the density should remain between 60 and 70% in order to get good reaction yields of polonium and gadolinium.

Examination of other loading techniques for linear compaction indicates that a single metal powder addition results in the longest possible matrix with less than 10% density variation. Matrices longer than 0.400 inch begin to compact at the extremities, and end densities up to 80% are obtained.

Since it is desirable to press the matrices between 60 and 70% theoretical density, studies are being initiated to determine the strength of such items. Approximately 200 matrices are being prepared for impact and vibration tests.

Matrix work was performed in the following areas:

- (1) Improvement of length of rings by the multiple addition method
- (2) Timing of production of single rings
- (3) Improvement of length of rings pressed with single additions
- (4) Production of sufficient number of rings for vibration and impact tests.

Fabrication techniques. It was found that rings could be successfully pressed with about 2.0 grams of tantalum to 60% density, with 0.040-inch wall thickness. These rings did not have high density ends, so a second addition of powder was attempted. A higher pressure was required to maintain a 60% theoretical density, but this increased the density at the ends. Less material was tried in each addition, but this still did not lower the end density sufficiently.

Since the multiple addition method was not successful, the length of single rings was improved by adding more material initially and pressing to a slightly higher pressure. These rings are approximately 0.38 inch in length, 0.040 inch in wall thickness and weigh about 2.7 grams. The density gradient was 8%.

Production of matrices. Since accurate knowledge of the time involved in mass producing rings was not known, a study was undertaken to determine production rates. The study involved only two operations in which 2.0-gram rings were mass produced. The amount of material added to the die was weighed roughly before pressing. The rings were all measured and weighed after pressing and the individual densities determined. It was found that rings could be produced by this procedure equivalent to a fuel element 20 inches long per day. Mass production techniques pushed the density gradient up to 8%, instead of the 5% reported last month.

Strength tests. Approximately 200 matrices are being produced for vibration and impact tests to determine if the 60% theoretical density matrices are strong enough to withstand re-entry and launch conditions. The actual tests have not been formulated at this time and will be the subject of a meeting with Martin Marietta Corporation and Mound Laboratory. The linear compaction technique appears to be satisfactory only at low densities of 60 and 65%. If such densities are not acceptable

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from a strength standpoint, then another matrix preparation technique may be necessary. Of course, there are other factors to consider such as loading efficiency, rate of reaction with polonium and ease of handling. The fuel matrix specification is complicated, and more information is needed before a final specification can be written.

It was found that the best way of producing rings was by the single addition method. The length of these rings is approximately 0.4 inch, and about 50 can be produced per day (about 20 inches).

Following the completion of the current production of 200 rings, work will be undertaken to complete the hydride system so that the GdTa rings can be produced and tested for density gradients. Many of these rings will also be required for reactivity tests, for which a fuel loading machine is being fabricated at this time. The fuel loading device will be used to determine a design for the encapsulation line fuel loading area and for polonium distribution studies on matrix elements.

Matrix studies related to tantalum powder specification. Examination of the loading conditions show that some restriction should be placed on the shape and surface area of the tantalum powder used in matrix preparation. Values for surface area and bulk and tap densities were measured on two different lots of tantalum powder. In general, powders with high surface area are easier to press. The study has not yet established a minimum acceptable surface area.

Production of rings for shock and vibration tests was interrupted until tantalum tubing can be furnished for such tests. Martin Marietta Corporation has agreed to locate the tubing for these tests.

Work this period included studies of particle shapes and the effect of wall thickness on density gradients. It was indicated from reactivity studies that -100 +110 mesh powder would produce useful matrices, so experiments were conducted with this particle size range. Attempts to press powder which had been pressed and then reground failed to produce the same results with identical pressing conditions, so a study of powder characteristics was undertaken.

Tantalum powder of -100 +115 mesh was separated from the supply of powder. Amounts were then pressed by the same method used for the -80 +200 mesh powder. The final pressure was 21,000 psi. The density gradient was determined as previously reported, and was found to have similar deviations of 8%.

New plungers were fabricated for the existing die set for a study of rings with 0.100-inch wall thickness. A higher total force was needed but the applied pressure to the ends was lowered to 13,500 psi. The density gradient resulting was still about 8% for powders of -80 +200 mesh and -100 +115 mesh.

Even though pressing methods were the same, reground powder failed to result in rings of the same average density. The cause was traced to the particle shape. Samples of fresh powder were separated by means of a shake table into flat and round particles. These were then pressed using identical methods. The rounder particles produced rings of slightly lower average density. Samples were also submitted for surface area measurements. The results have not been reported as yet. The rings had similar density gradients.

Tantalum powder samples from Kawecki Chemical Company and Kennametal, Incorporated were investigated. The Kawecki Chemical Company sample could easily be pressed into rings. The Kennametal powder, when pressed, resulted in rings with extremes in density. These matrices had very high densities at the ends, but fell apart at the center. Surface areas of both powders were determined by a Perkin-Elmer Sorptometer.

Bulk and tap densities were also determined. The bulk density refers to the density of particles after pouring into a vessel from a determined height (20 mm). Tap density refers to the density of particles which have been tapped to their minimum bulk volume. In this case 35 taps through a free fall of one inch were used to obtain the tap density.

Tantalum powder specifications were investigated with two samples of powders, and these studies will be continued on the two remaining samples available. Significantly different values were obtained for the two samples for their surface area and their bulk and tap densities.

	<u>Kawecki Company</u>	<u>Kennametal</u>
Surface area	19.38 m ² /gm	3.14 m ² /gm
Bulk density	3.48 gm/cm ³	7.86 gm/cm ³
Tap density	4.13 gm/cm ³	8.87 gm/cm ³

Production of thin-walled rings was discontinued until tantalum tubing of the correct dimension can be found. Production of thin-, as well as thick-walled, rings will continue when shock and vibration capsules can be supplied.

Using identical pressing methods, matrices pressed from -100 +115 mesh tantalum powder were found to have similar density gradients to those pressed from -80 +200 mesh powder.

An increase in the matrix wall thickness did not significantly change the density gradient of the walls.

Powder with tap or bulk density of less than 5 gm/cm³ may be sufficient to produce matrices with less than 10% density gradient in the walls.

Columbium-752 studies. The substitution of the columbium alloy Cb-752 for tantalum as the matrix material for positioned isotopic fuel forms of polonium-210 is being evaluated. Tests indicate rings compacted of -50 +200 mesh filings of Cb-752 are stable to ejection from the die. Ring densities varied from 61 to 75% of theoretical density corresponding to pressing pressures of 56,000 to 224,000 psi.

Cb-752 is an alloy which could possibly be used as the matrix material. The density of Cb-752 is about 46% less than the density of tantalum and the alloy retains its strength at temperatures several hundred degrees above the temperature required for use in a SNAP 29 capsule.

If Cb-752 is to be considered as the matrix material for positioned isotopic fuel forms of polonium-210, the pressing parameters and mechanical strength of rings compacted of the alloy need to be studied. Preliminary results of such investigations are given in this report.

The Cb-752 used in the test was obtained from the Haynes Stellite Company as a 0.030-inch sheet. A -50 +200 mesh fraction of metal filings was made from the sheet in the laboratory. The metal filings were degassed to 950° C in vacuo before being compacted into rings. The filings were compacted at pressures of 56,000, 140,000, 196,000 and 224,000 psi. The rings were formed by a stainless steel die enclosed in a removable casing. The casing permitted compaction of the rings in a helium atmosphere. The density of the rings was determined by weighing and calculating their volume, using micrometer measurements.

It was found that rings stable to ejection from the die can be compacted with Cb-752 filings. Results of these studies plus results of similar studies on tantalum powder are given in Fig. VII-13. The results are very similar, although the unpressed tantalum powder has a somewhat lower percent theoretical density than the Cb-752 powder. Nevertheless, rings compacted with Cb-752 filings seem to be less stable than rings compacted from tantalum powder. This observation is based upon removing rings from the die and handling of the rings. Tests are in progress to evaluate the mechanical stability of rings compacted of Cb-752 filings. Tests are also being conducted to determine the density and crush strength of rings compacted from a powder mixture containing a ratio of 10:1 by volume Cb-752 filings to gadolinium powder. Reaction of these rings with polonium will also be evaluated.

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Interior surface

Haynes-25

Fig. VII-12. Appearance of Inner Surface of Control Capsule C76 After 50 Days at 1000° C. No Fuel was Loaded into this Capsule

Fig. VII-13. Percent Theoretical Density as a Function of Pressing Pressure for Rings Compacted from Cb-752 Filings and for Rings Compacted from Ta Powder

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Matrix fueling studies. Experiments have been initiated to estimate the amount of molecular scattering that will be encountered during the process of fueling a SNAP 29 capsule. Although the experiments are incomplete at this time, initial results indicate a certain amount of scattering is unavoidable and that the amount of scattering is strongly dependent upon the geometry of the system.

Production of annular tantalum matrices containing GdPo has raised the problem of developing a technique for loading the matrices with polonium. The most direct method appears to be vacuum distillation of polonium into the inner capsule containing the TaGd matrices. Primary concern in such an operation is the amount of polonium scatter (the fraction of the polonium vaporized which escapes the capsule). The significance of redistillation may be quite important in the case of polonium, since there are indications that polonium metal, in any sizable quantity, is usually above its melting point even in a water-cooled trap. In an effort to qualitatively determine the amount of scatter in this type of operation, vacuum distillation experiments were performed using mercury. It is believed that the time spent in the vacuum might significantly affect the amount of scattering due to re-evaporation of mercury condensed on the inside of the capsule. Tests will be run to determine the rate of evaporation of mercury from the capsule. These types of experiments will later be modified by using polonium.

A small nozzle was machined from stainless steel and threaded to fit the standard Mound calorimetry vial in which the metal will be stored after separation from bismuth. A furnace was fabricated to heat the calorimetry vial and nozzle. This assembly was mounted in a bell jar, along with a cooling block containing a capsule with the same inside diameter as the proposed matrices.

The calorimetry vial was loaded with a given amount of mercury, the nozzle placed a measured distance in the copper tube and the vial nozzle assembly then heated under a vacuum of about 2×10^{-4} torr. The amount of mercury which was scattered was subsequently determined by weighing the vial, nozzle assembly and the copper tube.

The results of these tests indicate a general trend of lower scattering when the nozzle is placed farther into the capsule, as would be expected.

More data will have to be collected before a meaningful correlation can be obtained to show relations between scatter and the distance the nozzle is placed into the capsule or between scatter and the time the system is under high vacuum.

3. Material Weld and Braze Studies for Capsule and Heat Source

a. TIG versus EB welding

As part of the SNAP 29 development program, a joint Mound-Martin Marietta effort has been initiated to develop a weld procedure suitable for seal welds on the tantalum-10% tungsten capsule. Two methods of welding are under investigation; these are electron beam and tungsten inert gas. This study is designed to develop parameters, fabricate specimens for tensile, bend and burst analysis, and ultimately evaluate the relative weld efficiencies of the two methods for joining this refractory alloy.

Prior to beginning the TIG welding phase of the program, a gas analysis of the welding hood atmosphere was carried out. The glove box used for this work had previously been thoroughly cleaned and all pipe joints on the vacuum and inert gas system treated with epoxy sealant to assure a leaktight system. The ultimate vacuum attainable after this treatment was 5×10^{-3} torr.

Two sets of mass spectrometer samples of the helium hood atmosphere were analyzed; the second set was taken after the first showed the gaseous impurity levels to be above those agreed upon in the weld development plan. Slight changes were made in the atmosphere cycling procedure; also, the inert gas backfilling line couplings were covered with a sealant in an attempt to improve the quality of the atmosphere before carrying out the second gas analysis. The first set of samples was taken after an overnight pumpdown, backfill with helium gas and one additional pumpdown and backfill. The second set of samples was taken after an overnight pumpdown and two additional atmosphere cycles. Individual samples were withdrawn immediately after backfilling the hood with fresh gas following the final evacuation, after one hour, after two hours, with argon shield gas flowing through the welding torch, and with shield gas flowing and an arc struck. In the second set, an additional sample was taken directly from the helium cylinder to establish a background purity on the inert gas. The results of the gas analyses are tabulated and shown in Table VII-4. The first set of samples had air contamination levels ranging from a minimum of 0.8% on the first sample taken to 2.5% on the last. The second analysis showed a much improved atmosphere, with only two samples having any traces of air. On the basis of the second results, the atmosphere was felt to be of sufficient quality to proceed with the weld parameter development.

Test weld specimens were prepared using each method. The weld parameters established for the two methods are given in Table VII-5. Samples of each set of welds were submitted for metallographic examination. The photomicrographs are presented in Figs. VII-14 and VII-15.

TABLE VII-4
Gas Analyses of TIG Welding Hood Atmosphere

<u>Sample No. and Description</u>	<u>Trial No.</u>	<u>Gas Components, Mole %</u>				
		<u>He</u>	<u>N₂</u>	<u>O₂</u>	<u>H₂O</u>	<u>Ar</u>
1. After final helium backfill	1	99.2	0.7	0.1	0.0	--
	2	100.0	0.0	0.0	0.0	--
2. One hour after final backfill	1	97.9	1.6	0.5	0.0	--
	2	100.0	0.6	0.0	0.0	--
3. Two hours after final backfill	1	97.9	1.7	0.4	0.0	--
	2	99.6	0.3	0.1	0.0	--
4. With argon torch shield gas flowing	1	97.6	1.9	0.5	0.0	0.0
	2	48.9	0.0	0.0	0.0	51.1
5. With arc struck	1	95.4	1.9	0.6	0.0	2.1
	2	93.0	0.1	0.1	0.0	6.9
6. Helium cylinder gas	2	100.0	0.0	0.0	0.0	--

TABLE VII-5
Test Weld Parameters

Tungsten Inert Gas

Amperage	160
Shield gas and flow rate	Argon, 15 cfh
Rate of travel of weld specimen	9 in. /min
Arc gap	0.045 in.
Position of cooling block from joint	0.100 in.
Position of electrode in relation to joint	0.010 to 0.015 in. on heavy material

Electron Beam

Voltage	110 kv
Amperage	4.0 ma
Rate of travel of weld specimen	20 in. /min
A-C deflection	0.020 in.
Focal length	6 in.

Fig. VII-14. Electron Beam Test Weld

Fig. VII-15. Tungsten Inert Gas Test Weld

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One problem was realized on the TIG welding specimens, a tendency for the 20-mil sheet to warp rather badly. This could result in the formation of gaps between the thinner sheet and the 60-mil center strip, thus making the fabrication of uniform tensile and bend specimens difficult. Changing the position of the hold-down clamps on the cooling block was tried as a means of eliminating this problem, with only a slight improvement. The photomicrograph (Fig. VII-14) shows a gap between the thin member and the center strip; this resulted from poorly fitting parts and can undoubtedly be eliminated by assuring that the pieces fit together better prior to welding.

Welding parameters for assembling bend and tensile specimens have been investigated. As anticipated, metallographic examination on test welds shows a larger heat affected area and grain structure in the TIG specimens. A meeting was held between Martin Marietta and Mound personnel at which weld parameters for the TIG and EB specimens were agreed upon.

Because of apparently successful results from the 1500° C GdPo-Ta compatibility tests, the urgency for a satisfactory TIG weld (to prevent high temperatures during liner assembly) was reduced. Also, redefining the SNAP 29 program relieved the weld development schedule requirements. On this basis, the tensile test effort was dropped by Martin Marietta and specimens were turned over to Mound Laboratory for evaluation.

The burst test phase of the weld development program will be initiated. Both TIG and EB weld methods will be utilized for these specimens. Detailed plans are in process.

b. Ta inner liner weld qualifications

Tantalum tubing and end caps, fabricated to the Mound Laboratory recommended design, will be supplied to Mound for qualification of the Ta weld process.

c. Capsule clad weld process

No effort to date.

d. Capsule/fuel block braze process

No effort to date.

4. Cold Encapsulation Line

The line is currently established and effort has been initiated on tooling fixtures, including chill blocks, etc.

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5. Hot Encapsulation Line

(1) Inner capsule heating characteristics. Experimental studies have been conducted to simulate the temperature characteristics of a SNAP 29 inner capsule to determine effective handling and cooling procedures for the SNAP 29 encapsulation line. Data were reported in SNAP 29 First Quarterly Progress Report, MND-2062-12-6.

6. Heat Source End Item Specification

No effort to date.

7. Fuel and Assemble Heat Source

No effort to date.

NOTE:

The data contained in this chapter have been excerpted from Mound Laboratory (SNAP 29) Monthly Reports for December 1966, January 1967 and February 1967.

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