99
10-30-72
Dist in C-92h

Dr # 2336 UC-33

AI-AEC-13039 ✓

SNAP REACTOR PROGRAMS
PROGRESS REPORT
MAY-JULY 1972

AEC Research and Development Report

THIS DOCUMENT CONFIRMED AS
UNCLASSIFIED
DIVISION OF CLASSIFICATION
BY Jack H. Kahn/wer
DATE 11/6/72

MASTER

Atomics International
North American Rockwell

P.O. Box 309
Canoga Park, California 91304

DISTRIBUTION OF THIS DOCUMENT IS UNLIMITED

R7370

DISCLAIMER

This report was prepared as an account of work sponsored by an agency of the United States Government. Neither the United States Government nor any agency Thereof, nor any of their employees, makes any warranty, express or implied, or assumes any legal liability or responsibility for the accuracy, completeness, or usefulness of any information, apparatus, product, or process disclosed, or represents that its use would not infringe privately owned rights. Reference herein to any specific commercial product, process, or service by trade name, trademark, manufacturer, or otherwise does not necessarily constitute or imply its endorsement, recommendation, or favoring by the United States Government or any agency thereof. The views and opinions of authors expressed herein do not necessarily state or reflect those of the United States Government or any agency thereof.

DISCLAIMER

Portions of this document may be illegible in electronic image products. Images are produced from the best available original document.

NOTICE

This report was prepared as an account of work sponsored by the United States Government. Neither the United States nor the United States Atomic Energy Commission, nor any of their employees, nor any of their contractors, subcontractors, or their employees, makes any warranty, express or implied, or assumes any legal liability or responsibility for the accuracy, completeness or usefulness of any information, apparatus, product or process disclosed, or represents that its use would not infringe privately owned rights.

Printed in the United States of America
Available from

National Technical Information Service
U.S. Department of Commerce
5285 Port Royal Road
Springfield, Virginia 22151

Price: Printed Copy \$3.00; Microfiche \$0.95

SNAP REACTOR PROGRAMS
PROGRESS REPORT
MAY-JULY 1972

—NOTICE—

This report was prepared as an account of work sponsored by the United States Government. Neither the United States nor the United States Atomic Energy Commission, nor any of their employees, nor any of their contractors, subcontractors, or their employees, makes any warranty, express or implied, or assumes any legal liability or responsibility for the accuracy, completeness or usefulness of any information, apparatus, product or process disclosed, or represents that its use would not infringe privately owned rights.

Atoms International
North American Rockwell

P O Box 309
Canoga Park, California 91304

CONTRACT: AT(04-3)-701
ISSUED: OCTOBER 15, 1972

MASTER

DISTRIBUTION OF THIS DOCUMENT IS UNLIMITED

DISTRIBUTION

This report has been distributed according to the category "Systems for Nuclear Auxiliary Power (SNAP) Reactor – SNAP Program," as given in the Standard Distribution for Classified Scientific and Technical Reports, M-3679.

CONTENTS

	Page
I. Introduction	9
A. Program Scope and Objectives	9
B. Summary of Progress	9
II. Reactor-Shield	11
A. Reactor Engineering	11
1. Introduction and Summary	11
2. Fuel Element Design	11
3. Core Design and Analysis	15
4. Assembly Sequence Studies	19
5. Nuclear Analysis	27
B. Fuel Development	29
1. Fuel Element Assembly and Test	29
2. Burnable Poison	35
3. Cladding Properties	36
C. Reactor-Shield Component Development	37
1. Reflector Drive System	37
2. Prototype Drive Actuator	39
3. Cable Harness	41
III. Reactor-Thermoelectric System	45
A. Engineering	45
1. Introduction	45
2. Design Point Selection	47
3. Launch Environment	63
4. Radiator	66
5. Power Conversion System	75
6. Pump Configuration	79
7. Volume Accumulator	83
8. Startup and Full Power Operation	87
B. Liquid Metal Components	91
1. NaK Pump	91
2. Radiator Design and Development	93
3. Volume Accumulator Unit	101

CONTENTS

	Page
C. Thermoelectric Converter Development	102
1. Reactor Power Modules	102
2. Pump Tubular Power Modules	113
3. Supporting Technology Development	117
References	147

TABLES

1. Reactor-Shield Parameters	27
2. Fuel Length Trade Study, 85-Element Core	28
3. Number of Fuel Elements Tradeoff	28
4. Summary of Parametric Studies, Weight Sensitivity Coefficients, 85-Element Core — 16-in. Fuel Length	29
5. 5-kwe Thermoelectric Reactor Design Summary	30
6. 5-kwe Thermoelectric Reactor Reactivity Requirements	31
7. 5-kwe Thermoelectric Reactor Nuclear Characteristics	31
8. Hastelloy X Element Permeation Data	33
9. Burnable Poison Compatibility Test Status	36
10. System Design Point Summary, Space Environment	49
11. System Weights	50
12. Independent Design Variables	58
13. Dependent Design Variables	58
14. System Design Variables	59
15. Thermal Design Variables	61
16. Accelerations	65
17. Comparison of TEM-X to Reference Power Module	77
18. Power Module Improvement	79
19. Power Module Requirements	80
20. Comparison of D-C Pumps	81
21. Comparison of A-C vs D-C Pumps	85
22. Comparison of Startup Modes	89
23. Reactor Incidents	90

TABLES

	Page
24. Dual Throat Pump Summary	91
25. Volume Accumulator Unit and System NaK Pressures	102
26. Refractory-Clad Reactor Module Performance Stability Summary	108
27. Pump Module Performance Stability Summary	116
28. Process and Design Features of TEM-15 Series Modules	122
29. TEM-15 Performance Analysis Summary	125
30. A Comparison of Calculations and BOL Data from Four Recent TEM-15 Modules	130
31. A Comparison of Calculated and Measured TEM-15B S/N-4D Performance Data	136
32. Summary of Seals Made with Lead Base Glass Plus Feldspar (Melt)	141

FIGURES

1. Envelope of Peak CBD Load Responses	12
2. Internal Reflector Response to 2.4° F/sec Startup Ramp	14
3. Finned Element Friction Factor Data	16
4. Simplified GEOM Flow Chart	17
5. Force Balance Model	18
6. Reactor Assembly Sequence — Reflector-Control Verification Test	20
7. Reactor Assembly Sequence — Install Channel Exit Thermocouple Assembly	21
8. Reactor Assembly Sequence — Fuel Bundle Assembly	22
9. Reactor Assembly Sequence — Complete Core Assembly	23
10. Reactor Assembly Sequence — Install Core in Vessel	24
11. Reactor Assembly Sequence — Criticality Tests	25
12. Reactor Assembly Sequence — Reactor Vessel Closure	26
13. 5-kwe Reactor — Reactivity vs Time	32
14. Hastelloy X Biaxial Stress Rupture, Heat C9591 in Helium	38

FIGURES

	Page
15. Hastelloy X Biaxial Stress Rupture in NaK at 1300° F, Heat C9591	38
16. Actuator Reference Design	40
17. Actuator Torque Output	42
18. Variation of Actuator Torque per Tooth vs Tooth Width	43
19. Primary Loop NaK Temperatures, Space	46
20. Secondary Loop NaK Temperatures, Space	48
21. Shield Parametrics	52
22. Power Module Parametrics, 130% TEM-X Performance	54
23. System Weight Trade-Off	60
24. Internal Acoustic Spectrum	62
25. Titan-Sustained Accelerations	64
26. Schematic of Radiator Fin Cross Section	68
27. Radiator Dimensions	69
28. Thermal Emittance of Candidate Coating Systems vs Temperature	70
29. TEM-X SN-3 Tubular Thermoelectric Module	76
30. Power System Configuration	78
31. Dual Throat D-C Conduction Pump	82
32. Volume Accumulator Unit Reference Design	84
33. Potential Startup Modes	88
34. D-C Pump Performance	92
35. Two-Throat and Bus Assembly — Development Pump	94
36. Effect of Coolant Tube Parametrics on Radiator Pressure Drop	96
37. Effect of Coolant Tube Parametrics on Radiator Weight	97
38. Effect of Fin Material on Radiator Weight	98
39. Radiator Heat Rejection Capability	100
40. Power Output vs Elapsed Time for Refractory-Clad Reactor Power Modules	104
41. Overall Efficiency vs Elapsed Time for Refractory-Clad Reactor Power Modules	105

FIGURES

	Page
42. Open Circuit Voltage vs Elapsed Time for Refractory-Clad Reactor Power Modules	106
43. Internal Resistance vs Elapsed Time for Refractory-Clad Reactor Power Modules	107
44. Power Output vs Elapsed Time for Recent Refractory- Clad Reactor Power Modules	110
45. Overall Efficiency vs Elapsed Time for Recent Refractory- Clad Reactor Power Modules	111
46. Electrical Current Data from Prototype Pump Power Modules	112
47. Power Output Data from Prototype Pump Power Modules	114
48. EM Pump Module 1168/522 Parametric: Efficiency vs Current	118
49. EM Pump Module 1168/522 Parametric: Circuit Length vs Current	119
50. Pump Module 1135/567 Parametric: Efficiency vs Current.	120
51. Pump Module 1135/567 Parametric: Circuit Length vs Current	121
52. A Comparison of Power Output Data from Early TEM-15A Series Modules	126
53. A Comparison of Overall Efficiency Data from Early TEM-15A Series Modules	127
54. A Comparison of Power Output Data from Recent TEM-15A Series Modules	131
55. A Comparison of Overall Efficiency Data from Recent TEM-15A Series Modules	132
56. A Comparison of Power Output Data from TEM-15B, Tungsten Foil Barrier Modules	134
57. A Comparison of Efficiency Data from TEM-15B, Tungsten Foil Barrier Modules	135
58. TEM-15B S/N-4D Couple Design	136
59. A Comparison of Power Output Data from Improved Performance Stability TEM-15 Series Modules	138
60. A Comparison of Overall Efficiency Data from Improved Performance Stability TEM-15 Series Modules	140

BLANK

I. INTRODUCTION

A. PROGRAM SCOPE AND OBJECTIVES

The activities covered in this report are in support of the overall program objective to develop and demonstrate the technology required for the design of reliable long-life unmanned reactor thermoelectric power systems in the power range of 1 to 10 kwe utilizing a U-ZrH reactor and a compact tubular lead telluride thermoelectric converter. To accomplish this overall objective, a 5 kwe ZrH/TE system will be developed, fabricated, and tested in an unmanned (shadow shielded) configuration. The test will be conducted in the existing AEC facility (Building 059) at the North American Rockwell Nuclear Development Field Laboratory, Santa Susana, California.

Specifically, the nuclear power system shall be designed for a flight environment having the following design objectives:

- 1) Five-year life.
- 2) Five-kwe net unconditioned power at the power system/spacecraft mating plane in a space environment at end of life.
- 3) Compatible with unmanned type spacecraft and launch vehicles such as TITAN class boost vehicles.
- 4) Radiation dose at the payload mating plane of 10^6 rads, gamma, and 10^{12} nvt, neutrons, avg.
- 5) Use of ZrH reactor and PbTe compact thermoelectric tubular module technology.
- 6) Automatic startup and control capability.

B. SUMMARY OF PROGRESS

The conceptual design and analyses of the 5-kwe ZrH reactor thermoelectric nuclear power system neared completion at the end of the reporting period.

Reactor parametric studies were performed to determine the effect of various reactor configurations on the overall system weight and performance. An 85-element core having a fuel diameter of about 1 in. and a 16-in. active length was selected for more detailed study.

Hydrogen permeation measurements were performed on Hastelloy X cladding tubes. The permeation test procedure was modified to minimize the effects of surface oxidation on the permeation rate of fuel elements.

Three ball screw assemblies were obtained for preliminary performance evaluations. These assemblies will be operated at temperature and in vacuum to study alternate dry-film lubrication methods.

The conceptual configuration of the control drive actuator was established. The actuator will be about 5 in. in diameter by 3-1/2 in. long, will take a 7.2-degree step and have an output torque of about 120 oz-in.

A system design point was selected for additional study. The net electrical power output is held constant at 5.0 kwe during the 5-year lifetime. The reactor outlet temperature increases from 1140 to 1200° F over the five years to compensate for degradation of the thermoelectric converters. A near minimum weight configuration was selected at 1785 lb with a radiator area of 246 ft².

The preferred radiator design has aluminum fins bonded to stainless-steel coolant tubes. The fins are coated with the AI-93 emittance coating material.

A single (dual throat) d-c conduction pump was selected for the primary and secondary coolant loops. Both welded and nested formed bellows are being considered for the volume accumulator.

Thermoelectric power modules continued on test with output power degradation rates ranging from 1.0 to 5.8% per 10,000 hours for operating times up to 10,449 hours, hot cladding temperatures as high as 1140° F average, and including up to 59 thermal cycles.

Three new refractory-clad power modules were placed on test during the quarter, while five others were tested continuously. Major emphasis was placed on the evaluation of performance data from these modules.

II. REACTOR-SHIELD

A. REACTOR ENGINEERING

1. Introduction and Summary

Conceptual design and analyses of the 5-kwe reactor neared completion at the end of the current report period. Parametric studies of system weight and performance as a function of number of fuel elements and other geometry variables were completed early in the quarter. An 85-element core having a fuel element diameter of about 1 in. and a 16-in. active length was selected for more detailed consideration.

Information obtained by supporting tests of fuel element vibration response and core pressure drop in the laminar-transition-turbulent flow regimes was evaluated.

The response to white noise input was used to develop a method for deriving the fuel element vibration response to an arbitrary spectrum of excitation.

The mechanical design activity included studies of reactor assembly sequence to determine the pros and cons of various methods of loading the unitized core into the reactor vessel. Pertinent to these studies was the plan to conduct a reflector acceptance test at temperature and in vacuum prior to loading of fuel. It was also necessary to consider the approach to critical and the critical test plans in the assembly sequence studies.

2. Fuel Element Design

a. Fuel Element Vibration Tests

The fuel element has an internal axial clearance to provide space for thermal- and radiation-induced expansion of the fuel material. A column buckling device (CBD) is used to minimize this clearance during launch but yet allow for long-term fuel growth by creep buckling of the slender support column.

6531-51118

Figure 1. Envelope of Peak CBD Load Responses

The vibrational response of the fuel must be known to size the support column of the CBD. A vibration test of an instrumented structural mockup of the fuel element was performed to generate these response data. The mockup element was made of 1-in. OD by 0.028-in.-wall tubing containing four 4-in.-long ZrH fuel slugs. The test fixture provided for water immersion of the fuel element to simulate the effect of NaK. The test runs were made for various combinations of internal clearance between the CBD and fuel as well as for clearance between the fuel element and grid plate. The test runs consisted of a sinusoidal sweep, three levels of white noise, and an unattenuated Titan launch spectrum. The actual vibration spectrum at the grid plate must be determined by an analysis of the entire payload system. Therefore, a correlation was established between the white noise response characteristics and the unattenuated Titan spectrum so that the response to any other shaped spectrum may be generated.

The test results showed the load response to be essentially independent of the degree of looseness of the CBD (zero to 0.010-in. clearance) and at the grid plate attachment (zero to 0.020-in. clearance). The results of the peak load responses as a function of white noise level are shown in Figure 1.

b. Internal Reflector Thermal Stress Analysis

The internal reflector design consists of beryllium oxide bodies canned in stainless steel. They are contoured to fill the space between the basically hexagonal fuel element array and the cylindrical core can. An analysis was performed to determine the behavior of the internal reflectors when subjected to thermal transients. The study used a 2.4° F/sec NaK temperature ramp which approximates the most severe rate of change during a typical flight type reactor startup.

Three different internal reflector designs were evaluated using a finite element thermal analysis. Temperature distributions in the stainless-steel reflector can were obtained for the cases of:

- 1) NaK bonding between the BeO and the can
- 2) 3-psi Helium backfill
- 3) Vacuum between the BeO and the can.

6531-51119

Figure 2. Internal Reflector Response to 2.4°F/sec Startup Ramp

These temperature distributions were analyzed to determine the deflection of the reflector and the necessary restoring load for each of the cases as shown in Figure 2.

3. Core Design and Analysis

a. Coolant Pressure Drop Correlation

The hobbled fin pressure drop tests reported in the last quarter were extended to lower Reynolds (Re) numbers (100 to 2500). In order to obtain these data, it was necessary to incline the manometers to accommodate extremely low ΔP (down to 0.5 in. oil, 1.2 specific gravity). The low pressure drops were not sufficient to overcome a surface tensions and/or fluid inertia effect, and it was necessary to use a venting procedure to allow the manometers to settle a true reading.

Figure 3 shows the low Re test data and that obtained earlier for $Re > 2500$. The correlation shown gives a good fit without the necessity of using an exponent which varies with Re. Since the data are for the total flow through a 19-element bundle, the composite effect yields a smooth transition from turbulent to laminar flow. The data scatter in the laminar regime can be attributed mostly to the manometer limitations noted above.

b. Thermal-Mechanical Deflection Modeling

Heretofore, stress analysis of the fuel element has been accomplished by utilizing a thermal map of each element in a code separate from that which computed the thermal map. A mirror image model of the element was assumed to compute load and stresses, resulting in a fairly high degree of conservatism. (See the past two quarterly progress reports, AI-AEC-13023 and 13032, for more detailed descriptions of the models.)

The stress analysis calculations are now being incorporated in the thermal analysis GEOM. This will permit determination of the exact element shape by using an iterative procedure. Figure 4 shows a simplified schematic of the revised analysis. Each force balance iteration results in a new channel configuration, thus new temperature gradients, until the elements are stabilized in the configuration which the actual core would assume in operation.

Figure 3. Finned Element Friction Factor Data

6531-5418

Figure 4. Simplified GEOM Flow Chart

Figure 5. Force Balance Model

Figure 5 shows the force balance model. The bow due to load and the manufacturing bow occur in a single plane. The manufacturing bow is assumed sinusoidal. The load bow is computed with the element acting as a simply supported beam. The thermal bow is computed by integrating moments along the element.

The force balance is obtained by using the free deflections and element spring constants to determine element orientation at the contact plane. Where contact occurs, loads are calculated which are necessary to attain this orientation. Resultant bows are then calculated to determine flow areas for the next iteration. Stresses are also calculated resulting in a single output describing the core thermally, hydraulically, and mechanically.

4. Assembly Sequence Studies

Reactor assembly options were evaluated and the mechanical design requirements identified. Because the core instrumentation leads penetrate the vessel at the shield end, the reactor is most readily assembled prior to attachment to the shield. The basic steps in the sequence include:

- 1) Reflector-control system verification test -- a complete operational checkout in vacuum with an electrical core heat simulator
- 2) Bench assembly of the core, using poison splines in the coolant channels to insure subcriticality
- 3) Insertion of the core into the vessel
- 4) Criticality test (poison spline removal) and nuclear calibration
- 5) Reactor vessel closure.

The baseline sequence is illustrated by Figures 6 through 12. Reactor design options under study, such as a single bulkhead feedthrough of in-channel instrumentation and installation of the core into the shield end of the vessel, would require some modification of the assembly sequence from that illustrated.

6531-5001

Figure 6. Reactor Assembly Sequence — Reflector-Control Verification Test

Figure 7. Reactor Assembly Sequence - Install Channel Exit Thermocouple Assembly

6531-5003

Figure 8. Reactor Assembly Sequence -
Fuel Bundle Assembly

Figure 9. Reactor Assembly Sequence - Complete Core Assembly

6531-5004

6531-5005

Figure 10. Reactor Assembly Sequence —
Install Core in Vessel

6531-5006

Figure 11. Reactor Assembly Sequence – Criticality Tests

Figure 12. Reactor Assembly Sequence – Reactor Vessel Closure

5. Nuclear Analysis

The nuclear analysis performed during the quarter consisted of parametric studies on the 5-kwe reactor. The objectives of the studies were to minimize the reactor envelope and the reactor plus shield weight. The parameters which were varied are summarized in Table 1.

TABLE 1
REACTOR-SHIELD PARAMETERS

Minimum Reactor Envelope
Fuel Dimensions
Number of Elements
Core ΔT
Cladding Thickness
Interelement Spacing
Reflector Configuration
Minimum Shield Weight
Gamma and Neutron Shield Shape
Scattering Shadow Angle
Attenuation of Equipment

Fuel element length for an 85-element core was varied over the range of 14 to 17 in. A summary of the results are given in Table 2. It may be seen that the variation in reactor plus shield weight is small.

The results obtained from varying the number of fuel elements are given in Table 3. The 85-element core has the minimum weight for cores having reasonable fuel temperatures. The 28-lb increase for the 73-element core occurs because a hexagonal array of 73 elements cannot be packed as efficiently as 85 elements. This may be seen from the variation in internal reflector thickness shown in Table 3.

The remainder of the parameters were evaluated for an 85-element core, 16 in. long. Weight coefficients were determined for each parameter. These are shown in Table 4. These coefficients were used in the overall system analysis to arrive at the preliminary design of a 5-kwe reactor.

TABLE 2
FUEL LENGTH TRADE STUDY
85-ELEMENT CORE

Fuel Length	14 in.	15 in.	16 in.	17 in.
Pitch (in.)	1.094	1.057	1.026	0.999
Vessel OD (in.)	11.26	10.88	10.57	10.30
Midplane Reflector Thickness (in.)	2.05	2.12	2.19	2.26
Midplane Envelope (in.)	15.36	15.12	14.95	14.82
Relative Reactor Weight (lb)	-15	-9	*	+11
Relative Shield Weight (lb)	+14	+6	*	-4
Relative Reactor Plus Shield Weight (lb)	-1	-3	*	+7
EOL Nominal Peak-Average Fuel Temperature (° F)	1295	1290	1286	1283

*Comparison case

TABLE 3
NUMBER OF FUEL ELEMENTS TRADEOFF

Number of Elements	55	73	85	121
Pitch (in.)	1.233	1.095	1.026	0.893
Reflector Thickness, Midplane (in.)	2.19	2.19	2.19	2.19
Active Core Diameter (in.)	9.61	9.82	9.93	10.31
Internal Reflector Thickness (in.)	0.275	0.442	0.261	0.274
Vessel OD (in.)	10.28	10.82	10.57	10.98
Reactor Envelope (in.)	14.66	15.20	14.95	15.36
Relative Reactor Plus Shield Weight (lb)	-31	+28	*	+44
BOL H/Zr (δ -Fuel)	1.64	1.65	1.66	1.67
Cold BOL Radial Gap (mils)	7.0	6.1	5.8	5.3
EOL Nominal Peak-Average Fuel Temperature (° F)	1326	1297	1286	1267

*Comparison case

TABLE 4
SUMMARY OF PARAMETRIC STUDIES
WEIGHT SENSITIVITY COEFFICIENTS
85-Element Core - 16-in. Fuel Length

Variable	Weight Coefficients		
	Reactor	Shield	Reactor and Shield
Cone Half Angle (lb/deg)	2.4	29.0	31.4
Reactor Thermal Power (lb/kwt)	0.41	1.38	1.79
Cladding Thickness (lb/mil)	1.95	1.05	3.00
Interelement Spacing (lb/mil)	0.55	0.45	1.00
Core ΔT (lb/deg)	0.22	0.14	0.36
Reactor Outlet Temperature (lb/deg)	0.096	0.066	0.162
EOL Reactivity Margin (lb/\$)	9.0	5.4	14.4
Prepoison Loading (lb/\$)	2.4	1.4	3.8
Minimum Reflector Thickness (lb/in.)	7.0	21.8	28.8

A summary of the design is given in Table 5. The reactivity requirements for 5 years of full power operations are given in Table 6. The nuclear characteristics of the reactor are contained in Table 7. Figure 13 shows the variation in reactivity over time.

B. FUEL DEVELOPMENT

1. Fuel Element Assembly and Test

Three groups of developmental fuel elements were assembled. Smooth, 28-mil-wall cladding was used for the initial group of six fuel elements assembled. The second group of nine fuel elements was assembled using 28-mil-wall, finned cladding. The third group of elements was another batch of eight smooth, 28-mil-wall clad elements. These are the last fuel elements to be assembled from 0.540-in. ID cladding. Permeation testing was in progress at the close of the report period.

TABLE 5
5-kwe THERMOELECTRIC REACTOR DESIGN SUMMARY

Number of Elements	85
Active Fuel Length (in.)	16
Lattice Pitch (in.)	1.025
Spacing (mils)	20
Cladding Thickness (mils)	25
Glass Thickness (mils)	3 (max)
Gap Thickness (mils)	7
Fuel Diameter	0.935
Active Core Diameter (in.)	9.92
Active Core Volume (liters)	20.3
Core Vessel OD (in.)	10.566
Reflector Midplane Thickness (in.)	2.446
Reactor Midplane Envelope (in.)	15.457
Taper Angle (deg)	8.5
Segment Stroke (in.)	4.0
Movable Angle (deg)	235

TABLE 6
5-kwe THERMOELECTRIC REACTOR REACTIVITY
REQUIREMENTS
(\$)

Temperature Defect	2.18
Power Defect	0.04
Xe ¹³⁵	0.18
Hydrogen Redistribution	0.10
Total Recoverable Losses	2.50
U ²³⁵ Burnout	0.82
U ²³⁶ Burnin	0.31
U ²³⁸ Burnout	-0.02
Fission Products	0.92
Sm ¹⁴⁹ Burnin	0.94
Hydrogen Loss	1.16
Residual Prepoison	0.61
Total Nonrecoverable Losses	4.74
Total Requirements	7.24

TABLE 7
5-kwe THERMOELECTRIC REACTOR
NUCLEAR CHARACTERISTICS
(\$)

Cold, Unpoisoned Reactivity	7.74
Prepoison Loading	3.00
Recoverable Losses	2.50
Hot, Poisoned Reactivity	2.24
Nonrecoverable Losses	4.74
Prepoison Burnout	2.39
EOL Reactivity Margin	0.50
Control Worth	7.74
BOL Shutdown Margin	3.00

6531-5311

Figure 13. 5-kwe Reactor - Reactivity vs Time

Modification of fabrication and inspection tooling to accommodate larger cladding sizes was initiated and was essentially completed for the interim 0.651-in. ID cladding size. Welded and drawn Hastelloy X cladding of this diameter, in both 28- and 66-mil wall thicknesses, was received. This cladding will be used for process development pending receipt of cladding being procured to the reactor design requirements.

Thermal testing of a group of early Hastelloy X developmental elements continued. These elements have 20-mil-wall cladding, segmented fuel with an H/Zr of 1.81, and a 0.1-atm helium backfill. Permeation data are summarized in Table 8.

TABLE 8
HASTELLOY X ELEMENT PERMEATION DATA

Element Number	As-Measured Permeation [cc(STP)/hr at 1300° F]						
	Initial Test	Post Scan	Post Ramp	1000-hr Aging	2000-hr Aging	3000-hr Aging	4000-hr Aging
X-7	0.13	-	0.16	0.25	0.40	0.26	0.24
X-8	0.17	-	0.22	0.23	0.26	0.31	0.31
X-11	0.13	-	0.18	0.24	0.34	0.32	-
X-15	0.14	0.13	0.16	0.11	0.23	0.22	-
X-17	0.20	0.19	0.24	0.20	0.29	0.34	-
X-19	0.25	0.19	0.21	0.19	0.25	0.40	-
X-10*	0.18	-	0.18	0.08	0.14	0.13	-
X-13*	0.17	-	0.18	-	0.16	0.15	-

*Elements X-10 and X-13 are aged at 1050° F; all others aged at 1300° F.

Permeability measurements of 15-, 28-, and 50-mil-wall Hastelloy X and Incoloy 800 cladding were completed. The data indicate that the permeability constants for both Hastelloy X and Incoloy 800 may be taken as:

$$C = 2,900 \text{ cc-mm/hr-cm}^2\text{-atm}^{1/2}$$

$$Q = 14,750^\circ \text{ R (16,275 cal/mole) } ,$$

where the metal permeability, Ω , is defined as

$$\Omega = C e^{-Q/T}$$

T in °R

$$\Omega \text{ in } \frac{\text{cc-mm}}{\text{hr-cm}^2\text{-atm}^{1/2}} \cdot$$

Using these current permeation constants, which also apply to Hastelloy N based on test data from one membrane, existing ex-reactor, long-term endurance permeation data were analyzed to determine the long-term (10,000 to 20,000 hours) barrier performance trends. Data from 43 S8DR-type fuel elements and seven 15-mil-wall Incoloy 800 elements were analyzed. In addition, short-term (~2000 hours) data from first generation Hastelloy X type fuel elements were also analyzed. The equivalent bare metal index, A_D (Reference AI-AEC-13023), was employed as the measure of the barrier performance. These ex-reactor results were compared to the deduced S8DR in-reactor barrier performance.

Based on the study, the following conclusions were reached.

- 1) The SCB hydrogen barrier shows no indications of degradation (based on long-term A_D trends) with time.
- 2) The SCB hydrogen barrier shows no indications of degradation due to thermal cycling.
- 3) The SCB hydrogen barrier shows signs of degradation (based on long-term A_D trends) with time and thermal cycling when applied to a cladding substrate, such as thin-walled Incoloy 800, which yields and allows plastic strain to occur.
- 4) The SCB hydrogen barrier performance in S8DR showed an early life increase in A_D with the apparent A_D remaining constant (i. e., no degradation with time and/or thermal cycling) thereafter. The early life damage or increase in A_D is assigned to surface oxide-film removal by the NaK coolant and/or slight mechanical damage during early life fuel element instabilities.

Based upon assessment of these barrier performance trends, it has been recommended that future reactor barrier performance projections be based upon the mean ex-reactor A_D for the given fuel element design.

Permeation testing procedures were modified in an attempt to prevent any oxidation film from forming on the surface of fuel elements during test. In the past, light oxidation has occurred which reduces the hydrogen permeation from elements. The basic criteria now used are: (1) vacuum of at least 5×10^{-5} torr, (2) minimize test system in-leakage (2×10^{-3} torr/hr or less), (3) liquid hone elements to remove surface oxides prior to test, (4) wrap Hastelloy X foil around fuel elements as an oxygen getter, and (5) limit test time to 6 hours or less. This procedure has been used in testing several elements. The permeation rates were reproducible and the elements upon removal from the test furnace were bright, with no indication of oxide film.

Construction of a new four-unit permeation test system and permeation scan system was completed and checked out. Both systems have demonstrated capability of operation at 5×10^{-5} torr in the test retort with less than 2×10^{-3} torr/hr in-leakage. Modification of an old 16-unit permeation test system is now in progress to upgrade it to this capability. All permeation tests during the interim will be performed in the new four-unit or scan permeation test systems.

Permeation testing was completed on two Hastelloy X fuel elements after vibration testing to nine times anticipated Titan III-C launch loads. The data indicate that, with a 0.005-in. internal gap between vibration suppressor and end cap, no damage was incurred at any vibration test level. With 0.015-in. internal gap, there appears to be some initial damage which increased during the final 900%, Titan III-C inputs.

2. Burnable Poison

Long-term thermal testing continued on burnable poison coated fuel rods, assembled in fuel elements. Five additional elements were assembled and placed on test during the report period. Two of these elements contain four 2-in.-long fuel rod segments, each coated on the cylindrical surface with a 0.7-micron film of Gd_2O_3 and on the ends with a 1.0-micron film of Y_2O_3 . Coating the ends with Y_2O_3 serves to completely isolate the fuel from exposure to the

SCB ceramic barrier and should further reduce any fuel barrier reaction. One element is being tested isothermally at 1300° F, while the other element is in a thermal gradient, with the cold end at 1000° F and the hot end at 1300° F. This thermal gradient will cause redistribution of the hydrogen and swelling of the fuel rods at the cold end. The other three fuel elements placed on test are similar to the preceding two elements except that the burnable poison coatings on the cylindrical surfaces contain a mixture of 0.8-micron Er_2O_3 and 0.7-micron Gd_2O_3 . Two of these elements are being tested at the same conditions as the two elements containing the fuel rods with no Er_2O_3 in the coatings. The remaining fuel element is on test at 1400° F. The test status for these five elements and early elements still on test is shown in Table 9.

TABLE 9
BURNABLE POISON COMPATIBILITY TEST STATUS

Fuel Element Number	Accrued Time (hr)	Target Time (hr)	Temperature (° F)	Fuel Rod Coatings
9194	13,411	10,000+	1300	Gd, Y(Eu, Sm)
6-3	3,941	5,000	1400	Er, Gd
6-4	3,941	10,000	1400	Er, Gd
X-24	1,223	10,000	1300	Gd, Y
X-29	1,223	10,000	Gradient 1000 to 1300	Gd, Y
X-31	1,223	5,000	1400	Er, Gd, Y
X-32	1,223	10,000	Gradient 1000 to 1300	Er, Gd, Y
X-33	1,223	10,000	1300	Er, Gd, Y

3. Cladding Properties

Biaxial stress rupture testing in helium of 20-mil-wall cladding from Hastelloy X Heat C9591 continued. All 1300° F tests were completed and all but one 1200° F test specimen have ruptured. A plot of stress vs rupture time for all data obtained is shown in Figure 14. Four of the 1300° F specimens were coated with SCB ceramic; all other specimens were given simulated thermal treatment

(STT), i. e., the SCB coating cycle but in inert atmosphere with no coating. There was virtually no distinguishable difference in strength, with both coated and STT specimens falling on top of each other in the data plot.

Biaxial stress rupture testing in 1300° F flowing NaK continued on 20-mil-wall Hastelloy X cladding. Eight specimens are being tested: six STT type and two SCB coated. All but one STT-type specimens have now ruptured. Current data are plotted in Figure 15. There is little discernible difference between the strength of the cladding tested in NaK and that shown in Figure 14 for the helium tests.

The heat of Hastelloy X-280 was received in the form of 2-1/8-in.-diameter bar (4950 lb) and 1-1/8-in.-diameter bar (620 lb). This material will be used in fabricating the fuel elements for the 5-kwe reactor core.

C. REACTOR-SHIELD COMPONENT DEVELOPMENT

1. Reflector Drive System

The pre-conceptual design of the sliding reflector reactor control system was completed during the quarter and the drive system component sizes and requirements were defined.

a. Drive Components

The reflector drive screw parameters were defined and a prototype screw designed. The specialized application necessitated the use of high strength, high temperature materials as Inconel 718 for the screw and ball nut, and tungsten carbide (WC) for the ball bearings. The screw lead angle and pitch diameter will be 0.75 in. All guide bearing surfaces will be coated with Al_2O_3 and both guide bearing surfaces and ball screw surfaces coated with MoS_2 dry-film lubricant.

Commercial ball screws fabricated of 17-4Ph with lead angle and pitch diameter of 0.5 in. were procured for preliminary performance evaluation. Tests will duplicate reflector operating conditions (800° F and 10^{-5} torr air) including continuous stepping on startup and rapid shutdown, and periodic stepping after long dwells during normal operation. Lubrication effects will be studied with one ball screw run dry (no lubrication), one with bearing surfaces

Figure 14. Hastelloy X Biaxial Stress Rupture, Heat C9591 in Helium

Figure 15. Hastelloy X Biaxial Stress Rupture in NaK at 1300°F, Heat C9591

coated with sprayed MoS₂ dry-film lubricant (0.0004 to 0.0011 in. thick), and one with sputtered MoS₂ (~4000° A thick). The thermal-vacuum test fixtures for the tests are being fabricated, and the ball screws are being coated with MoS₂.

b. Reflector Materials

Room temperature spectral emissivity of anodized beryllium test coupons have been measured and 12 coupons have completed 1000 hours thermal stability testing at 1150° F and 10⁻⁶ torr. The measured emissivity values for the new anodized samples are comparable to the archive S8DR anodized samples. Six additional coupons have been measured for total emissivity at 800 and 1150° F and will be added to the thermal stability test. Total emissivity data analysis has not been completed.

c. Reflector Model

An operating model of the reactor reflector and control system has been designed and is being fabricated. The model will have one reflector segment fully operational for demonstration and evaluation of reflector design. The end-of-life shutdown system will be mocked up, but will not initially be operational. As prototype components become available, they will be incorporated into the assembly resulting in a full engineering model.

2. Prototype Drive Actuator

A direct-current, variable-reluctance, stepping actuator, energized in discrete pulses, is to be used in the reflector drive system to position the reflector sectors. The S8DR actuator design configuration must be altered so that the new actuator can fit within the allowable reactor envelope and performance must be somewhat upgraded.

a. Analysis and Design

Based upon S8DR actuator test data for tooth widths up to 0.060 in. wide and test data on wider teeth currently in progress (see below), parametric studies were performed to arrive at an optimum actuator design, meeting drive system requirements. Parameters assumed to be fixed were the actuator diameter and length, approximately 5.00 in. and 3.4 in. (including brake), respectively (see Figure 16). Variables were rotor diameter, tooth width, and teeth per pole.

FEATURE	199 ELEMENT R _x (S8DR)	85 ELEMENT R _x
OVERALL ENVELOPE		
DIAMETER (in.)	4.12	5.0
LENGTH (in.)	7.25	3.38
TORQUE		
ACTUATOR OUTPUT (oz-in.)	75	120
BRAKE HOLDING (oz-in.)	600	120
MATERIALS		
MAGNETIC IRON	HIPERCO-27	HIPERCO-27
BEARING BUSHING	CARBON-GRAPHITE	CARBON-GRAPHITE
BEARING SHAFT	Al ₂ O ₃ COATING	Al ₂ O ₃ COATING
COIL BOBBIN	Al ₂ O ₃	Al ₂ O ₃
COIL MAGNET WIRE (SOLID)	OXALLOY-28	OXALLOY-28
COIL LEAD WIRE (STRANDED)	OXALLOY-28	OXALLOY-28
COIL WIRE INSULATION	FIBERGLASS	FIBERGLASS
COIL ENCAPSULANT	AI-1 CEMENT	AI-1 CEMENT
COIL FAB PROCESS	WET WINDING	WET WINDING

72-A13-12-14B

Figure 16. Actuator Reference Design

Based on S8DR assembly and test experience, an air gap of 0.008 in. was selected. Materials used in the study are as shown in Figure 16.

Typical results of this study are as shown in Figure 17, in which flux density and current density are plotted against developed torque. It can be seen that, for a six-teeth per pole design, a tooth width of 0.085 in. provides an actuator with minimum current density at reasonably low flux densities. For other values of teeth per pole, slightly different optimum points were found. Overall drive system studies have shown that a combination of drive gear ratio and ball screw lead angle in which 1° of actuator motion equals approximately 0.00075 in. of reflector sector motion, provides the optimum ratio of actuator developed torque-to-system required torque within overall space available. Thus, the desired reflector sector control step of ~ 0.0054 in. requires 7.2° of actuator rotation. This rotation is readily achieved by a four-step sequence (complete electrical revolution) of a six 0.085-in.-wide teeth per pole design. Accordingly these parameters were selected for the actuator conceptual design.

b. Supporting Development

Tests have been run with S8DR actuator parts modified to provide both 0.090 and 0.120-in.-wide teeth. The results of tests with a nominal air gap of 0.005 in. are plotted in Figure 18 with data previously obtained for small width teeth and normalized to an air gap of 0.005 in. Data are plotted for 600 ampere turns. It is seen that the torque per tooth flattens out beyond 0.080-in. Since the number of teeth that can be accommodated in a given air gap diameter is inversely proportionate to the tooth width, total torque for a rotary actuator will peak and then diminish as the tooth width increases.

3. Cable Harness

Cables are required to supply power to the actuator and limit switch. To insure high mechanical integrity and avoid outgassing of organic materials, ceramic-insulated, sheathed cables will be used. Tests are in progress on single conductor cables to evaluate sealing techniques and the effect of preflight humidity. Twelve unsealed samples of 0.125-in.-diameter cable and six cable samples with two different types of sealed ends have been exposed to various humidity conditions. Periodic resistance measurements have been made.

6531 5509

Figure 17. Actuator Torque Output

6531-5510

Figure 18. Variation of Actuator Torque per Tooth vs Tooth Width

These test conditions have included 40, 70, and 95% humidity at 90°F, 40% humidity cycling between 50 and 170°F, 70% humidity cycling between 50 and 170°F, and exposure to typical laboratory and manufacturing area low bay, high bay, and sheltered outdoor environments. After a maximum of 2 months of testing, minimum measured resistance was 25,000 ohms, which is well above the minimum requirements.

BLANK

III. REACTOR THERMOELECTRIC SYSTEM

A. ENGINEERING

1. Introduction

The 5-kwe ZrH Reactor Thermoelectric System design requirements have been defined as follows:

- 1) The system configuration shall be compatible with a typical launch vehicle for use in an unmanned space application.
- 2) The system design shall be consistent with typical Titan III launch environments and be capable of space operation under the space environment characteristic of a 600-n. mi polar orbit.
- 3) The system design life shall be 5 years.
- 4) The radiation shield shall attenuate reactor radiation to 10^{12} neutrons/cm² and 10^6 rads gammas at the power system-payload interface.

To satisfy these system design requirements, the power conversion system utilizes tubular PbTe power conversion modules to produce 5 kwe at 30 vdc. These thermoelectric modules are utilized in a system employing two coolant loops. The primary loop circulates NaK at elevated temperature from the reactor to the hot side of the thermoelectric modules. The secondary loop circulates NaK at a reduced temperature and removes the heat transferred across the thermoelectric modules to a space radiator. Circulation is provided in both loops by a dual-throat, d-c conduction electromagnetic pump.

The power system is designed to produce a constant power output over its design life of 5 years. The control system is based on sensing the electric current generated by the power system. Since output voltage is maintained constant, controlling on current is the same as controlling on power.

To achieve a 5-year life, the power system temperatures have been limited to a nominal 1200° F. Therefore, the system is designed to produce 5-kwe power at 30 vdc at the end of 5 years without the reactor temperature exceeding 1200° F. To overcome degradation in the performance of the thermoelectric modules, the system is initially operated at a reduced temperature, and the

72-JU14-2-105A

Figure 19. Primary Loop NaK Temperatures, Space

system temperatures are allowed to drift upward to maintain constant power output. Nominal NaK temperatures in the primary and secondary loops at both beginning-of-life (BOL) and end-of-life (EOL) conditions are illustrated in Figures 19 and 20, respectively. Nominal system performance characteristics and design features are presented in Table 10. The initial system weight estimates are presented in Table 11.

2. Design Point Selection

The design point selection was based on a system modeling technique developed to calculate dimension and weight characteristics as a function of several distinct sets of input variables. Three types of modeling input exist, namely: (1) general system characteristics and configuration data, (2) individual component and subsystem parametrics, and (3) independent design variables. These input variables define an operational statepoint which determines an associated system configuration and thermal performance. This output encompasses the modeling dependent variables.

Two configurations were considered for the 5-kwe system: (1) an all conical system, and (2) a conical-cylindrical system. With conical and conical-cylindrical geometries, the thermoelectric and liquid metal components are placed as near the shield as possible. The mass of this equipment can then be utilized as shielding. This placement minimizes the total amount of radioactive primary loop NaK and maximizes the radioactive NaK to baseplane distance. The primary loop piping runs are minimized, thus reducing their pressure drop, weight, volume, and pumping power requirement. All systems considered incorporated this placement of the thermoelectric and liquid metal components.

System component degradation is assumed to occur throughout life. This degradation is compensated through life by increased reactor power and outlet temperature (to assure constant output power and voltage). All systems evaluated were designed to meet end-of-life requirements with specified component degradation. Decreased temperature operation provides increased design margin at beginning of life.

72-JU14-2-104A

Figure 20. Secondary Loop NaK Temperatures, Space

TABLE 10
SYSTEM DESIGN POINT SUMMARY, SPACE ENVIRONMENT

Parameter	BOL		EOL
Electrical Power at 30 vdc (kw)			
Gross	5.13		5.13
Net at Mating Plane		5.0	
Reactor Thermal Power (kw)	86.2		93.0
Reactor Outlet Temperature (° F)	1140		1200
Flow Rate (lb/sec)			
Primary Loop	4.71		4.67
Secondary Loop	2.85		2.84
Pressure Drop (psi)			
Primary Loop	0.84		0.83
Secondary Loop	1.25		1.25
Converter Efficiency (%)	6.83		6.29
Radiator Area (ft ²)		246	
Base Diameter (ft)		6	
System Overall Height (ft)		22.3	
Radiator Cone Half-Angle (°)		8	
Number of Thermoelectric Modules			
Power		16	
Pump		3	
Total Weight (lb)		1785	

TABLE 11
SYSTEM WEIGHTS
(Wet weights where applicable)

System	Weight (lb)	
Reactor		482
Vessel	34	
Reflector and Drive Train	155	
Core	284	
Piping	9	
Radiation Shields		361
Gamma Ray	138	
Neutron (LiH)	188	
Neutron Casing	35	
Thermoelectric Power Modules		192
Pump Assembly		77
Pump Bodies	55	
Thermoelectric Modules	22	
Volume Accumulators (3)		38
Piping		127
Primary Loop	61	
Secondary Loop	66	
Radiator		452
Fins and Armor	216	
Tubes	133	
Structure	90	
Miscellaneous	13	
Electrical Wiring		46
Power Transmission	10	
Auxiliary Pump Power	9	
Actuator	8	
Instrumentation	19	
Miscellaneous		10
Total		1785

a. Component and Subsystem Parametrics

The individual parametrics associated with each component or subsystem are discussed below. Fixed technical parameters for each have been determined by component trade studies which indicated the applicability of the parameter constraint to the 5-kwe system.

(1) Reactor Subsystem

The reactor parametrics were based on an 85-element, all delta-phase fuel core design. A two-segment, sliding, tapered reflector was incorporated to allow a conical shaping of the reactor subsystem. The reactor outlet temperature was constrained to 1200° F for EOL system operation.

Several auxiliary constraints were applied to the reactor subsystem. The minimum coolant flow in the core was limited to assure adequate flow distribution. The maximum core temperature rise was limited to assure an adequate margin of safety on fuel element stresses. These constraints are evaluated for the operating design state points of interest and are criteria in the final design point selection.

The reactor parametrics were modeled as illustrated below:

$$\left. \begin{array}{l} \text{Reactor Weight} \\ \text{Reactor Vessel OD} \\ \text{Reactor Envelope} \end{array} \right\} \quad \begin{array}{c} \text{As a} \\ \text{Function} \\ \text{of} \end{array} \quad \left\{ \begin{array}{l} \text{Reactor Power} \\ \text{Conical Half Angle} \\ \text{Fuel Length} \\ \text{Core Temperature Rise} \end{array} \right.$$

(2) Shield Subsystem

The shield parametrics were based on a design utilizing a tungsten alloy gamma shield and an encased lithium hydride neutron shield. The shield parametrics were modeled as shown below:

$$\left. \begin{array}{l} \text{Shield Weights} \\ \text{Shield Lengths} \\ \text{Shield Diameters} \end{array} \right\} \quad \begin{array}{c} \text{As a} \\ \text{Function} \\ \text{of} \end{array} \quad \left\{ \begin{array}{l} \text{Reactor Power} \\ \text{Conical Half Angle} \\ \text{Reactor Dimensions} \\ \text{Separation Distance} \\ \text{(core to mating plane)} \\ \text{PCS Attenuation Factors} \end{array} \right.$$

6531-5601

Figure 21. Shield Parametrics

The PCS attenuation factors (gamma and neutron) are additionally a function of the number of power modules, the pump weight, the volume accumulator weight, and the conical half angle (as it effects the PCS "packing factor" within the conical section). Typical shield parametrics are illustrated in Figure 21.

(3) Power Modules

The power module parametrics were based on a design with 1.3 times the performance of the TEM-X module. This performance multiplier is an estimate of the expected advancements in module state-of-the-art development. The module is also expected to have a module degradation rate of 1.25%/year, or 6.25% over life (on efficiency and power). The power module parametrics were modeled as illustrated below.

$$\left. \begin{array}{l} \text{Matched Load Efficiency} \\ \text{Matched Load Power} \\ \text{Matched Load Voltage} \end{array} \right\} \begin{array}{c} \text{As a} \\ \text{Function} \\ \text{of} \end{array} \left\{ \begin{array}{l} \text{Average Hot Cladding} \\ \text{Temperature} \\ \text{Average Cold Cladding} \\ \text{Temperature} \\ \text{Number of p-n Couples} \end{array} \right.$$

Typical parameters are shown in Figure 22. Note that usage of an integral number of power modules defines a unique set of hot and cold cladding temperatures for a 5-kwe power requirement.

(4) Electromagnetic NaK Pump

The pump includes the magnets, throats, busbars, and associated structure which makes up the pump assembly but excludes the pump modules. Pump parametrics are based on a dual-throat pump configuration (primary and secondary throats combined in one pump).

Auxiliary constraints have been placed on the pump design. The electrical current requirement for the pump must not exceed 2000 amp to retain state-of-the-art pump module compatibility. The pump can utilize a maximum of three TEM-14A type modules for operation.

The pump body parametrics were modeled as shown on the following page:

6531-5602

Figure 22. Power Module Parametrics,
130% TEM-X Performance

The primary loop requirements dictate the pump design because the primary loop hydraulic power requirement exceeds that for the secondary loop in all workable system designs.

(5) Pump Modules

The thermoelectric pump module parametrics are based on a design employing TEM-14A material and TEM-14A radial dimensions. Changes in module active length and number of p-n couples allow for pump and module current and voltage compatibility. This design assumes a module degradation rate of 1.75%/year, or 8.8% over life (on efficiency and power).

The pump module parametrics were modeled as illustrated below:

(6) Radiator-Structure Subsystem

The radiator-structure is defined as the radiator fins, meteoroid protection armor, stainless-steel tubing, radiator coolant (NaK), structural rings and stringers, and associated bracketry and fasteners. The radiator-structure subsystem parametrics were based on a design employing a bonded trapezoidal fin-tube cross section. Radiator emissivity and absorptivity characteristics are based on using AI-93 coating.

The radiator must meet combined thermal and structural requirements. Dual radiator parametrics were incorporated to insure this design goal. The

radiator thermal requirement parametrics were modeled as illustrated below:

In addition, the structural requirements for the radiator were parametized as shown below:

The greater of these two weight requirements was used in the system weight calculations. The radiator exhibits a minimum weight for a design which is a compromise between thermal and structural requirements.

(7) Piping Subsystem

The primary and secondary piping subsystem parametrics are based on designs which maximized utilization of the PCS components for shielding purposes. Previous trade studies have indicated the applicability of this approach to the present system design. Stainless-steel tubing with 0.020-in. wall was employed in the piping design.

The piping system was designed to provide a higher flow rate to the power modules than to the pump modules which would attempt to utilize the inherent higher efficiency of the power modules. Higher flow rates result in higher hot cladding and lower cold cladding temperatures.

The piping parametrics for each loop were modeled as follows:

$$\left. \begin{array}{l} \text{Piping Weight} \\ \text{NaK Weight} \end{array} \right\} \text{As a Function of} \left\{ \begin{array}{l} \text{Loop Flow Rate} \\ \text{Loop Pressure Drop} \\ \text{Conical Half Angle} \\ \text{System Dimensions} \end{array} \right.$$

(8) Volume Accumulators

The volume accumulator parametrics were based on the redundant gas-backed design, utilizing one unit in the secondary loop and two units in the primary loop. The volume accumulator parametrics were modeled as shown below:

$$\left. \begin{array}{l} \text{Total Weight (Three Units)} \end{array} \right\} \text{As a Function of} \left\{ \begin{array}{l} \text{Total Piping Weight} \\ \text{Primary Design Pressure} \\ \text{Secondary Design Pressure} \end{array} \right.$$

(9) Wiring Subsystem

The wiring subsystem parametrics are based on a design employing aluminum power transmission lines. The wiring subsystem parametrics were modeled as follows:

$$\left. \begin{array}{l} \text{Power Transmission Line Weight} \\ \text{Pump Power Line Weight} \\ \text{Miscellaneous Wiring Weight} \end{array} \right\} \text{As a Function of} \left\{ \begin{array}{l} \text{Line Length} \\ \text{Allowable Power Losses} \\ \text{Allowable Voltage Drops} \end{array} \right.$$

b. Independent Design Variables

Specification of the design variables determines a system statepoint within the scope of the assumed requirements and component parametrics. Thus, the independent variables are the basis for the trade study. Those variables required to define such a statepoint are given in Table 12. The range associated with each was established to include all workable designs.

TABLE 12
INDEPENDENT DESIGN VARIABLES

Variable	Range
Primary Loop ΔT ($^{\circ}F$)	50 to 125
Primary Loop ΔP (psi)	0.5 to 1.5
Radiator Fin Effectiveness	0.75 to 0.95
Number of Power Modules	10 to 20
Conical Half Angle ($^{\circ}$)	5 to 10
Base Diameter (ft)	5 to 9
Total System Height (ft)	20 to 30

TABLE 13
DEPENDENT DESIGN VARIABLES

Variable	
Radiator Area	ft ²
Conical Structure Height	ft
Primary Loop Flow	lb/sec
Secondary Loop Flow	lb/sec
System Heat Balance	kwt
System Temperature Distribution	$^{\circ}F$
Reactor Thermal Power	kwe
Pump Module Input Power	kwt
Secondary Loop ΔT	$^{\circ}F$
Secondary Loop ΔP	psi
Component Weights	lb
System Weight	lb

c. Dependent Design Variables

For each established system statepoint, a set of dependent variables is generated which describes the system configuration and thermal performance. A summary of the dependent variables associated with this study appears in Table 13.

d. Results of System Trades

The following sections demonstrate the system sensitivity to some of the independent design variables. This system trade study demonstrated the variables can be separated into two sets as shown in Table 14. The system minimum weight characteristics with respect to the thermal design variables are identical for all performance/geometrical design statepoints. Thus the design point selection can be performed in two distinct stages.

TABLE 14
SYSTEM DESIGN VARIABLES

Thermal Design Variables	Performance and Geometrical Design Variables
Primary Loop ΔT	System Configuration
Primary Loop ΔP	Radiator Area
Radiator Fin Effectiveness	Conical Half Angle
	Base Diameter

(1) Thermal Design Variable Selection

Analysis of the system weight sensitivity to the thermal design variables resulted in the selections appearing in Table 15.

(2) Configuration Selection

The system design point configuration selection is conical-cylindrical although the conical system exhibits a slightly lower weight. The weight penalty associated with this selection is approximately 10 lb, or $\sim 0.5\%$ of the total system weight. However, the system envelope realizes a ~ 0.8 ft reduction in height ($\sim 3.5\%$ reduction), and an 8-in. reduction in base diameter ($\sim 10\%$ reduction).

AI-AEC-13039
60

Figure 23. System Weight Trade-off

72-JU20-2-201A

TABLE 15
THERMAL DESIGN VARIABLES

Parameter	Conclusions	Selection
Primary ΔT	System weight is minimized for the maximum possible ΔT . Minimum flow is limited to insure adequate Reynolds number. Temperature rise is limited to provide adequate fuel element stress margins.	90° F
Primary ΔP	System weight is minimized for the minimum possible ΔP . Pump current requirement is less than 2000 amperes. Pump requires three TEM-14A type modules. Minimum pressure drop is limited to insure good reactor flow distribution.	0.83 psi
Fin Effectiveness	System weight is minimized. Radiator fin design is optimized.	0.86

(3) Base Diameter Selection

The system design point base diameter is 72 in. This selection is made on the basis of minimum weight.

(4) Radiator Area Selection

The system design point radiator area is 246 ft². Minimum system weight is exhibited for a radiator area of ~260 ft². The weight penalty associated with this selection is approximately 15 lb, or ~0.8% of the system weight. This selection is biased toward minimum system envelope. This area reduction decreases the total height by ~1 ft (~4.5% reduction). The trade between system weight, radiator area, and cone angle is shown in Figure 23.

(5) Cone Half-Angle Selection

The system design point cone half-angle is 8.0 degrees. Minimum system weight occurs at ~7.5 degrees as shown in Figure 23. The weight penalty associated with this selection is approximately 5 lb, or ~0.3% of the system weight. The selection of 8.0 degrees is again biased toward minimum system envelope.

6531-1001

Figure 24. Internal Acoustic Spectrum

A ~ 0.6 -ft reduction in system height is realized for this selection ($\sim 2.5\%$ reduction).

3. Launch Environment

Launch of the system into orbit induces a unique environment that changes continuously with time and altitude. The launch begins with vehicle engine ignition and lasts until the final stage is separated and the 5-kwe system is in orbit. The 5-kwe system is at, or near, room temperature during launch. An assumed orbit has been established as circular, polar, and at 600 nautical miles. Assuming that the combined weight of the spacecraft and the 5-kwe system is 3200 lb, the Titan III-B with Burner II for the third stage is selected as the reference launch vehicle.

The launch offers five distinct environmental conditions:

- 1) Acoustic
- 2) Low frequency acceleration
- 3) Sustained accelerations
- 4) Thermal
- 5) Shock.

a. Acoustic Environment

The acoustic energy levels are shown in Figure 24. The curve represents an envelope of maximum values. Launch pad echo and direct rocket motor noise for 5 sec, and aerodynamic turbulence at Mach 1 and maximum Q stages of flight for about 55 sec, are the major contributing sources. The levels shown are for the acoustic pressures inside of the nose shroud and bearing directly on the 5-kwe system. It is a requirement for the nose shroud design that the noise be attenuated to less than 145 db.

Most payload components will not respond significantly to 145 db sound pressure levels. However, large panels may be excited, and components mounted directly on them can see some adverse displacements.

b. Low Frequency Acceleration

The sound pressure levels at ignition through liftoff and at Mach 1 and maximum Q excite the Titan airframe and payload fairing. A portion of this vibration

Figure 25. Titan-Sustained Accelerations

6531 1002

is mechanically transmitted to the payload at low frequencies. The fundamental resonances of the Titan are approximately 1, 3, and 5 Hz for the first three modes.

Similarly, the airframe is excited at staging or engine cutoff.

The accelerations associated with maneuver and staging are shown in Table 16.

TABLE 16
ACCELERATIONS
(g)

	Expected Level (limit)		Design Level (ultimate)	
	Axial	Transverse	Axial	Transverse
Low Frequency Maximum	6.0	1.6	7.5	2.0
Low Frequency First Stage Burnout*	8.0	1.0	10.0	1.25
Thrust Maximum	13.0	0.5	16.25	0.625

*Includes thrust

c. Sustained Acceleration

Forward acceleration is dependent on the vehicle engine thrust and total residual weight. The acceleration increases during each stage burn due to decreasing weight. At staging, there is both a reduction in weight and a reduction in thrust. Figure 25 shows the Titan accelerations. The maximum value is 13.0 g at the end of Stage II burn and represents a configuration without a third stage. Maximum axial sustained acceleration with the related transverse accelerations are also shown in Table 16.

d. Thermal Environment

The thermal environment is typified by three separate phases: prelaunch pad environment, thermal radiation from shroud heating, and free molecular heating following shroud ejection.

The prelaunch phase is controlled by air conditioning or heating in accordance with the requirements of the payload. The 5-kwe system may need heating to prevent coolant freezing.

Radiation from the shroud prior to its ejection is dependent on the mission, shroud configuration insulation, emissivity coating, and time of ejection. Although local heating of the nose shroud may reach 420° F, the system can be protected by local insulation.

The shroud is normally ejected at 400,000 ft. The payload may experience free molecular heating up to 700,000 ft depending on its trajectory. The effect is not expected to be significant, but if required, the thermal environment can be controlled by trajectory shaping, nose shroud ejection time, and/or auxiliary insulation.

e. Shock

The shock loads in flight originate with the pyrotechnic separation and ejection of the nose shroud and with the separation of the payload. Both sources occur over 400,000 ft and in an atmosphere comparable to a vacuum. Thus, the shock loads are transmitted mechanically and can only affect the reactor and other components by traveling up the radiator. The shock loads are attenuated by an inverse square of the distance relationship.

It is anticipated that the 5-kwe system will be sufficiently remote from the shock of shroud and payload separation that the effect will be attenuated to insignificant levels.

4. Radiator

The radiator is designed to operate in the temperature region 500 to 700° F for 5 years in a space environment. In addition, the radiator must be capable of performing under simulated space conditions during system ground test.

a. General Description

The radiator is shaped as a conical frustum atop a cylindrical section. The small diameter of the conical section is approximately 2 ft, 4 in. The base of the cone and the cylinder is 6 ft in diameter. The cone axial height is approximately 13 ft. The cylinder axial height is approximately 4 ft.

A schematic of a radiator fin cross section is shown in Figure 26. Each fin section consists of an aluminum radiating fin with a trapezoidal cross section. Stainless-steel coolant tubes are bonded to the rear surface of the radiating fin. An extra thickness of aluminum is located between the radiating surface and the stainless-steel coolant tubes. This thickness of aluminum serves as the additional material required to achieve the specified meteoroid armor protection, and is designated as the shaved armor thickness.

Overall features of the radiator system are shown in Figure 27.

b. Radiator Fin Surface Coating

The radiator fin surface requires a coating which is effective in rejecting internal heat and reflecting solar input, while operating in the temperature region 500 to 700° F for 5 years. The thermal emittance values of the candidate coating systems are shown in Figure 28 as a function of temperature. AI-93 has a significantly higher emissivity than the other candidates and is the coating selected for the 5-kwe system.

c. Meteoroid Protection

The following equation is used to determine the armor thickness required to protect the radiator tubes from damage by micrometeoroids for near earth orbits.

$$\delta = \gamma_R a \left(\frac{\rho_p}{\rho_a} \right)^{1/2} \left(\frac{V_p}{C_a} \right)^{2/3} \left(\frac{6}{\pi \rho_p} \right)^{1/3} \left(\frac{E \alpha A_v t}{-\ln P(o)} \right)^{1/3 \beta} \left(\frac{2}{3n\theta\beta + 2} \right)^{1/3 \beta} \left(\frac{T}{T_R} \right)^{1/6}$$

where

δ = armor thickness

γ_R = room temperature material cratering coefficient

a = rear surface damage thickness factor

ρ_p = meteoroid average density

ρ_a = armor density

V_p = meteoroid average velocity

6531-5901

Figure 26. Schematic of Radiator Fin Cross Section

Figure 27. Radiator Dimensions

6531-5903

Figure 28. Thermal Emittance of Candidate Coating Systems vs Temperature

C_a = sonic velocity in armor

E = armor earth shielding factor

α = meteoroid flux constant

A_v = vulnerable area

t = mission time

$P(o)$ = design probability of no critical damage

n = damage factor for oblique impact

θ = penetration constant

β = meteoroid flux constant

T = armor temperature

T_R = armor room temperature.

The meteoroid flux is given by the equation:

$$N_t = \alpha m^{-\beta}$$

where

N_t = meteoroid flux, particles/ m^2 -sec

m = minimum meteoroid mass, gm.

The cratering coefficient (γ_R) for particles with $10^{-6} \leq m \leq 10^0$ gm for use in the meteoroid flux model are:

$$\alpha = 10^{-14.37 \frac{-gm^\beta}{m^2 \text{ sec}}}$$

$$\beta = 1.213$$

The meteoroid particle density is 0.5 gm/cm^3 and the meteoroid average velocity is 20 km/sec . The values of n and θ used in the required armor thickness equation were taken from Reference 1 as:

$$n = 1.0$$

$$\theta = 2/3 \quad .$$

The earth shielding factor, E , is determined using the equation given in Reference 2.

$$E = (1 + \cos \theta) / 2$$

where

$$\cos \theta = R / (R + H)$$

R = radius of shielding body

H = altitude above surface of shielding body.

The materials constants a and γ_R in the required armor thickness equation vary for different materials and as a function of temperature. Values for γ_R were taken from Reference 3 for various materials as:

Target Material	Material Cratering Coefficient γ_R
356-T51 aluminum	2.15
7075-T6 aluminum	2.00
2024-T6 aluminum	1.70
Columbium-1% zirconium	1.81
Type 316 stainless steel	2.19
A-286	1.77
Inconel-718	1.85
L-605	2.00
Vanadium	1.71
Tantalum	1.77
TZM-molybdenum	2.00

These γ_R values for the given material are at room temperature. They are corrected to operating temperature by the $(T/T_R)^{1/6}$ term in the armor thickness equation.

The damage thickness parameter, a , for stainless-steel tubes inside aluminum armor was set equal to 1.5. This corresponds to the stainless-steel liner being dimpled upon impact with the dimple height between 10 and 20% of the tube diameter.

Using these various equations and parameter values, the armor thickness required to protect the radiator coolant tubes was determined for various non-critical damage probabilities.

The assumed mission dependent input values were as follows:

vulnerable area = 30 ft^2

mission time = 5 yr

orbital altitude = 600 n. mi.

The 600 n. mi orbital altitude yielded an earth shielding factor, E , value of 0.7631. The 30 ft^2 vulnerable area corresponds to the projected surface area of the fifty 0.4375-in.-OD coolant tubes used in the 240 ft^2 radiator.

The results of this series of calculations showed that an armor thickness of 0.125 in. is required to provide the coolant tubes with a noncritical damage probability of 0.99. The $P(o)$ value of 0.99 was selected to maximize radiator reliability without producing a large increase in radiator system weight.

The 0.125-in. armor thickness represents that amount of aluminum that must be placed on the outer surface of the coolant tubes to provide a 0.99 non-critical damage probability. This thickness includes the radiator fin thickness.

d. Fin Materials

A variety of fin materials were evaluated. Nonmetallic materials in general were rejected as having poor thermal conductivity, and not being capable of withstanding the temperatures of operation. Copper is too heavy, and titanium has poor thermal conductivity. On this basis, the aluminum alloys as a group were examined closely. Within the group of aluminum alloys, there are a number of choices, each alloy having a unique combination of properties.

One of the requirements is for a metallurgical, high thermal conductance bond between the fluid carrying Type 316 stainless-steel tube and the aluminum radiator panel. This condition imposes a double restraint: (1) selection of a suitable brazing alloy that will provide a metal-to-metal joint that will be stable for 5 years, and (2) selection of a radiator alloy that will be suitable for the stainless steel - aluminum brazing operation. Several of the aluminum alloys that had attractive strength values had melting ranges at or below the brazing temperatures and as such were not applicable.

During the study, concern was expressed over the possibility of the evaporation of magnesium from the aluminum alloy which could then plate out (vapor deposit) on components internal to the radiator shell and potentially result in electrical short circuits.

Based on the calculated loss of magnesium from a radiator alloy with 1% Mg and the area of the inner surface of the radiator, the loss would be about 500 cc or a pint of material in the 5 years exposure. Accordingly, the use of magnesium bearing alloys was considered undesirable.

A discussion of each of the aluminum alloys considered follows:

<u>1100</u>	An alloy having high thermal conductivity, a high melting range, can be brazed, and is available but has low mechanical strength
<u>2011</u>	A magnesium free alloy but available only as bar stock and having lead, zinc, and bismuth as additions for free machining purposes and considered suitable
<u>2024</u>	A high strength alloy susceptible to stress corrosion and containing magnesium
<u>2219</u>	A low magnesium alloy that maintains mechanical strength at temperatures up to 600° F better than 1100 or 3003 but due to low melting range would not be suitable for brazing
<u>3003</u>	An alloy with thermal conductivity slightly inferior to 1100 but with higher mechanical strength
<u>3004</u>	An alloy similar to 3003 but with higher mechanical strength and 1% nominal magnesium content

- 5052 A high strength work hardening alloy that contains magnesium
- 6061 A high strength heat treatable alloy that contains magnesium
- 7075 An alloy that can be heat treated to high strengths but is susceptible to stress corrosion and contains magnesium.

The manufacturing, prelaunch, and space service conditions impose requirements on material selection that exceed the conventional range of properties developed for engineering purposes. The specific selection of the materials is, of necessity, a compromise of a number of applicable factors. For these reasons two alloys were selected for the radiator of the SNAP reactor. The Alloy 3003 condition 'O' was selected for the brazed portion of the radiator surfaces. This alloy possesses the high thermal conductivity required for this component but has low mechanical strength. The Alloy 2219 condition T62 was selected for the longitudinal and circumferential support structure. These selections were based on the assumption that the aluminum radiator panels would be brazed to the stainless-steel fluid conducting tube and then the assembly of the panels and support structure would be accomplished by mechanical fasteners. The outer surface of the assembled radiator would then receive the emittance coating. The supporting structure would be anodized to minimize any tendency for evaporation of high vapor pressure elements during service in space.

e. Structural Requirements

The radiator also serves as the main structural system and structural requirements are imposed on the radiator throughout the full life sequence of the system. To minimize weight, an aerospace type of structure is used. This normally means that maximum strength-to-weight-ratio materials and minimum cross sections are required. This structure shares its requirements with the system performance function as a radiator and consequently, there must be a compromise between thermal conductivity and strength.

5. Power Conversion System

The design of the 5-kwe power system incorporates 16 tubular thermoelectric power modules. A cross section through one end of a module is illustrated in Figure 29. The design of the reference module for the 5-kwe system is

Figure 29. TEM-X SN-3 Tubular Thermoelectric Module

72-JU19-2-117

similar to the Westinghouse TEM-X-S/N3 module which is currently being tested; however, changes have been made to the TEM-X design in order to achieve improved module performance characteristics. The design features of the TEM-X module and the reference module design are given in Table 17. These changes are expected to increase module efficiency from 5.15 to 6.83% under system operating conditions. Table 18 illustrates the total potential for improvements in module efficiency which could accrue from design changes in the TEM-X module.

TABLE 17
COMPARISON OF TEM-X TO REFERENCE POWER MODULE

	TEM-X-3 Module	Reference Module
Radial Dimensions (mils)		
Inner Cladding	90	90
BN Inner Insulating Ring	25	25
Mo Hot Side Conductor Ring	30	30
Thermoelectric Washer	142	142
Mo Cold Side Conductor Ring	20	20
BN Outer Insulating Ring	34.5	34.5
Outer Cladding	105.4	105.4
Axial Dimensions		
Thermoelectric Washers (mils)	187	146
Mica Insulating Washer (mils)	10	1.5
Foil Between Mica (mils)	0.5	None
Circuit Length (in.)	15	15
EBVD on Mica	None	Yes
Number of Couples	39	50
Performance (1085/507° F)		
Power - Matched Load (watts)	300	325
Efficiency - Matched Load (%)	5.05	6.55
Voltage - Matched Load (volts)	2.8	3.58

Figure 30. Power System Configuration

72-JU22-2-508

TABLE 18
POWER MODULE IMPROVEMENT

	Maximum Efficiency at 1085/507° F
TEM-X	5.15
Ternary*	6.16
Segmenting	6.38
EBVD — Thin Mica	7.49
Increased Radius	7.66
Decreased End Loss — Increase Length	7.96
Expected Improvement — Reference Module	6.83

*Includes adjustment for experimental vs calculated performance (1.08).

The reference module performance requirements are given in Table 19 for both beginning-of-life and end-of-life conditions. Each of the sixteen modules produces 325 watts at 3.86 volts under load. Two strings are wired in parallel. Each string has eight modules connected in series to produce 30.9 volts.

The modules are connected hydraulically in parallel in both the primary and secondary NaK loops by means of four circular manifolds. Figure 30 shows the module arrangement and the four manifolds (two primary and two heat rejection).

6. Pump Configuration

Several pump configurations were considered for application in the 5-kwe system. Only electromagnetic pumps were considered because of their attractive features for this application. The options investigated were:

- A-C Induction Pumps
- D-C Conduction Pumps
- Dual-Throat, d-c Conduction Pump

TABLE 19
POWER MODULE REQUIREMENTS

	BOL	EOL
$\bar{T}_H$ (° F)	1087	1144
$\bar{T}_C$ (° F)	506	525
Number of Modules	16	16
Number of Parallel Strings	2	2
System Voltage (volts)	30.9	30.9
Volts per Module (volts)	3.86	3.86
Total Power (kwe)	5.13	5.13
Power per Module (watts)	325	325
Efficiency (%)	≥6.83	≥6.29
Power Degradation (%)	0.0	≥6.25
Efficiency Degradation (%)	0.0	≥6.25
$\dot{M}$ - Primary (lb/sec)	0.265	0.265
ΔP - Primary (psi)	0.32	0.32
$\dot{M}$ - Secondary (lb/sec)	0.160	0.160
ΔP - Secondary (psi)	0.421	0.421

In all cases, the pump design requirements were set at:

<u>Parameter</u>	<u>Primary Loop</u>	<u>Secondary Loop</u>
NaK Flow Rate (lb/sec)	4.67	2.85
Pump Head (psi)	0.96	1.44
Throat Temperature (° F)	1110	589

A comparison of the two d-c conduction pump concepts is presented in Table 20. Employing a single d-c pump in each loop, powered by its own individual set of pump modules, the pump system weight totals 186 lb. This includes 57 lb for the pump bodies, 30 lb for the pump modules, and an incremental radiator weight of 99 lb. The incremental radiator weight is included in the weight tabulation to account for the additional radiator and piping required to reject the thermal power consumed by the pump modules.

TABLE 20
COMPARISON OF D-C PUMPS

Parameter	Dual Throat	Single Throat	
		Primary	Secondary
Voltage (volts)	0.204	0.136	0.094
Current (amps)	2000	1875	2121
Power In (watts)	407	255	193
Pump Bus Efficiency (%)	16.0	12.8	13.7
Hydraulic Power (watts)	65	32.5	26.3
Bus Length (in.)	25	18	18
Bus Efficiency (%)	67	60	60
Shunt Heat Loss (watts)	700	NIL	NIL
Weight (lb)			
Pump	46	32	25
Converter	21	15	15
Radiator	<u>82</u>	<u>51</u>	<u>48</u>
Total Pump System	149	186	
Throat Dimensions (in.)			
Width	1.1	1.0	0.6
Height	0.55	0.6	0.6
Length	2.5	2.4	1.6

6531-5904

Figure 31. Dual Throat D-C Conduction Pump

In comparison, the dual-throat, d-c conduction pump system only weighs 149 lb. The weight reduction results from the use of common bus bars and the realization of a higher pump system efficiency when the pump current is shunted through both pump throats. A schematic of the dual throat pump is presented in Figure 31.

Based on the above evaluation of the two d-c pump concepts, the dual-throat, d-c conduction pump was selected for comparison with an a-c induction pump designed for the same service.

The relative design and performance characteristics of the dual-throat, d-c conduction pump and an a-c induction pump are presented in Table 21. The dual-throat, d-c conduction pump is more efficient and lighter than the a-c pump. Further, the d-c pump has more desirable flow characteristics and should exhibit a higher reliability. For these reasons, the dual-throat, d-c conduction pump was selected for use in the 5-kwe system.

7. Volume Accumulator

A trade study was undertaken to determine the optimum volume accumulator unit (VAU) design. Factors evaluated included reliability, size, bellows materials, configurations, bellow and types, pressurization methods, bellows arrangements, number of units in the system, bellows natural frequency, bellows instability pressure, relative cost, complexity, and weight.

The reference design chosen (Figure 32) utilizes welded bellows and gas pressurization, with double containment of the NaK. The void volume between the primary and the secondary containment bellows is evacuated. The bellows material choice is Inconel 718 material. Identical units are utilized in both the primary and secondary loops. The following paragraphs summarize the studies performed to arrive at this reference design.

a. Bellows Material

Inconel 718, AM 350 (850 SCT), and Type 300 series stainless steels were evaluated. AM 350, however, has low ductility and is susceptible to stress corrosion. The Type 300 series stainless-steel materials have low strength resulting in an excessive number of bellows convolutions, and a large, heavy VAU.

6531-5420

Figure 32. Volume Accumulator Unit Reference Design

TABLE 21
COMPARISON OF A-C vs D-C PUMPS

Parameter	Dual-Throat D-C Pump	A-C Pump
Efficiency (%)		
Pump	16	6
Inverter	-	85
Converter	4	6
System	0.64	0.31
Weight (lb)		
Pump	46	100 (50 lb ea)
Inverter	-	20 (10 lb ea)
Converter and Radiator	103	220
System	149	340
Controllability		
Inherent	Flow Will Follow Power	Flow Will Follow Reactor Power Above Inverter Diode Voltage
External*	Not Practical	Inverter Voltage or Frequency
Reliability	Higher Than A-C $R'_T \times R_c$	$R_T \times R_W \times R_{in} \times R_c$
	$R_{T1} = R$ Throat and Bus Joint	$R_T = R$ Throat
	$R_c = R$ Converter	$R_{IN} = R$ Inverter $R_c = R$ Converter

*No system requirement

Inconel 718 was chosen based on high ductility, state-of-the-art bellows fabrication capability, and minimum weight and bellows higher reliability because of lower bellows weld length.

b. Bellows Type

Welded, standard formed, and nesting formed bellows types were evaluated. The standard formed bellows resulted in excessive VAU length and weight, caused by the stroke limitation of this type of bellows.

The nesting formed bellows was retained as an alternate to the welded bellows, pending completion of the fabrication evaluation testing results.

The welded bellows was chosen as the reference design because it results in minimum size and weight, while meeting the VAU reliability objectives.

c. Pressurization

Gas charge and helical core spring, pressurization alternate methods were evaluated. The gas charge pressurization method was chosen over the spring pressurization method because of lower VAU cost and weight, and less complexity of fabrication and assembly, with a negligible difference in VAU reliability.

d. Number of VAU's

Concepts using two VAU's (one primary and one secondary), three identical VAU's (two primary and one secondary), and six identical VAU's (four primary and two secondary), were evaluated.

The six-identical VAU's concept requires a helical coil spring for pressurization, consequently the VAU total weight was excessively high, and the cost and complexity was high compared to the other approaches.

The two-VAU approach was not selected because of high fabrication and development cost for two VAU's of different sizes. The three-identical-VAU approach was chosen because of the higher relative reliability, lower fabrication and development cost, and small weight penalty over the two-VAU approach.

e. Other Alternates

Other concepts studied included alternate bellows arrangements and alternate secondary volume (between primary and secondary NaK containment bellows) fill.

The alternate bellows arrangement studied utilizes a second set of half-length bellows which operate by stroking in opposite directions and accumulating NaK in the resulting cavity between bellows. This arrangement was not selected because the added complexity and fabrication cost did not offset the small weight and potential reliability advantages of the concept.

The reference design VAU has evacuated a volume between primary and secondary containment bellows. The alternate of NaK-filling the secondary volume was studied, but resulted in the added complexity of VAU fabrication and NaK filling, and increased weight with no reliability advantage.

8. Startup and Full Power Operation

Startup and full power operation are mainly governed by the control and safety systems. The ground test system has slightly different requirements than the flight system. The ground test control system provides the capability for manual startup and control as well as automatic startup and control. The ground test system also provides safety interlocks, and emergency shutdown capability. The flight control and safety system provides the capability for automatic startup and control and provides an EOL timer for shutdown.

The important parameters that influence reactor startup are reactivity insertion rate, shutdown margin, source strength, coolant flow rate, and initial system temperature. The sensitivity of the startup temperature and power transient to variations of these parameters has been investigated to determine the impact on system design. Four startup schemes were considered to be likely to result in suitable startup transients. These are: (1) a single continuous reactivity rate, (2) dual continuous reactivity rate, (3) a single reactivity rate with hold after criticality, and (4) dual rate with hold between rate changes. The reactor power as a function of time for typical startups of each type is shown in Figure 33, and typical system performance parameters are listed in Table 22. These data represent the current range thought to have the least impact on the system design.

An initial study has been made of the safety (protection) system for the ground test system. Table 23 contains a list of conceivable reactor incidents and calculated consequences based on the current ground test safety system concept. The consequences listed in that table do not appear to be unreasonable based on the remote likelihood of occurrence of the individual events.

6531 5008

Figure 33. Potential Startup Modes

TABLE 22
COMPARISON OF STARTUP MODES
(5% flow, 50¢ subcritical)

Parameter	Single Rate Continuous	Dual Rate Continuous	Single Rate With Hold Time	Dual Rate With Hold Time
Time to Full Power (hr)	3 to 12	4 to 6	2-3/4 to 3-1/2	2-1/2 to 3-1/4
Time to First Generation Electric Power (hr)	2 to 8-1/2	3 to 5	2 to 2-3/4	<2 to 2-1/2
Time to Self-Sustaining Pump, 10% (hr)	2 to 4	2 to 4	1-1/4 to 2	<1 to 1-1/2
Initial Reactor Power Transient (%)	25 to 90	25 to 50	35 to 50	35 to 45
Power Oscillations Upon Reaching Rated Power (%)	<10	<15	<15	<15
Battery Weight (lb)	55 to 140	65 to 100	50 to 65	45 to 60
Peak Coolant ΔT (° F)	180 to 350	180 to 250	130 to 220	130 to 220
Peak Inlet Coolant Temperature Rate of Change (° F/min)	110 to 525	110 to 220	50 to 150	50 to 150
Reactivity Insertion Rate(s) (¢/sec)	~0.005 to 0.02	~0.005 to 0.010/0.025	~0.025	~0.05/0.025
Hold Time (min)	-	-	20 to 60	20 to 60
Relative Controller Startup Complexity (Reliability)	1	2	2A	3
Excess Reactivity at Hold (¢)	-	-	5 to 15	5 to 15

TABLE 23
REACTOR INCIDENTS

Incident	Potential Cause	Protection	Power Level (%)	Core ΔT ($^{\circ}$ F)	Peak Outlet Temperature ($^{\circ}$ F)	Peak Coolant Rate of Change ($^{\circ}$ F/min)
Ramp Reactivity Insertion (0.5¢/sec) at Startup	Control Malfunction or Operator Error	Period Scram or	<1	0	NA	0
		Power Level Scram	240	98	200	+57
Ramp Reactivity Insertion (0.5¢/sec) at Power	Control Malfunction or Operator Error	Power Level Scram or	125	83	1203	-75
		Outlet Temperature Scram	330	128	1280	+180
Loss of Coolant Flow	Failure of EM Pump	Flow Scram or	NA	470	1266	-120
		Outlet Temperature Scram	NA	550	1350	-120
Loss of Electrical Load	Electrical	NA*	NA	NA	1222	+155
Reactor Scram (5¢/sec)	Instrument Error or Malfunction Operator Error	NA	NA	NA	NA	-75
Loss of Primary Coolant	Rupture of NaK Loop	Coolant Flow	Temperature excursion will probably rupture fuel element cladding.			

*Not applicable

B. LIQUID METAL COMPONENTS

1. NaK Pump

a. Prototype Pump Analysis and Design

Performance analysis of a dual-throat permanent magnet electromagnetic type pump assembly continued as the 5-kwe reactor-thermoelectric converter system conceptual design evolved. At the end of this report period, the performance requirements and design characteristics were as summarized below in Table 24. The performance characteristics of this pump design are shown in Figure 34, where primary and secondary throat-bus efficiency and throat pressure head are shown as a function of NaK flow rate.

TABLE 24
DUAL THROAT PUMP SUMMARY

Voltage (volts)	0.172
Current (amp)	1545
Flux Density (gauss)	2350
Pump Efficiency, Throat and Bus (%)	13.3
Electric Input Power (watts)	266
Throat Dimensions (in.)	
Height	0.6
Width	1.36
Length	2.50
Weight (lb)	55
NaK Flow Rate, Primary/Secondary (lb/sec)	4.67/2.85
NaK Head Rise (psi)*	0.96/1.44
Throat Temperature, EOL (° F)	1110/589

*Includes 15% margin

This design was determined from optimization studies (see Section III-A-6) conducted to provide a minimum pumping system weight. The pumping system includes the pump, converter module, heat system radiator, piping, and associated structure. The design was also influenced by the current and voltage characteristics of the thermoelectric converter modules from which it obtains its power.

6531-5421

Figure 34. D-C Pump Performance

b. Development Pump Tests

The fabrication of the developmental dual-throat, d-c conduction pump was completed. The dual throats and multiple bus assembly of this pump are shown in Figure 35.

The developmental pump test will be performed in a vacuum chamber while hydraulically connected to the PCA NaK test loops. An external current power supply will be used to provide power to the pump. Testing will be conducted through a range of flows from 0 to 5 lb/sec, temperatures from ambient to 1100° F, and electrical currents from 0 to 2000 amp. The range of pressures developed across the pump throats during these tests is expected to vary from 0 to 4 psi. A thermal shroud will be installed within the vacuum chamber to raise the test region ambient temperature to about 600° F, which is representative of that expected during 5-kwe reactor-thermoelectric system tests.

The modification of the PCA NaK test loops and of an existing vacuum chamber is in progress. All materials required for the pump test and the test installation were received. A pump test plan and a test procedure were prepared.

The design of the developmental pump-thermoelectric converter assembly and test installation was started. Three TEM 14-A converter modules will be mated to the developmental pump to simulate the prototype pump-thermoelectric converter configuration.

2. Radiator Design and Development

Based on a radiator concept using aluminum fins bonded to stainless-steel tubes, trade studies were conducted to determine the optimum tube diameter, number of tubes, and the fin geometry. The results of these studies follow.

a. Coolant Tube Diameter

A series of calculations was made to determine the pressure drop through the radiator and the radiator system weight for various numbers of coolant tubes. Two tube sizes were considered in these calculations, 0.4375- and 0.375-in. OD, both with a 0.020-in. wall thickness. The number of coolant tubes was varied from 44 to 58. In each case the coolant tubes were assumed to be equally spaced around the circumference of the radiator-structure.

6-23-72

6531-4021A

Figure 35. Two-Throat and Bus Assembly – Development Pump

The following input parameters were used in this analysis:

Power to be rejected	94.8 kw
Area	240 ft ²
Inlet temperature	620° F
Emissivity	0.9
Solar absorptivity	0.4
Fin material	Al-3003.

The results are shown in Figures 36 and 37. As can be seen in Figure 36, the larger coolant tube size yielded a pressure drop approximately 0.12 to 0.20 psia lower than the 0.375-in.-OD coolant tubes, depending upon the number of tubes. From Figure 37, the radiator weight using the 0.4375-in.-OD coolant tubes was approximately 20 lb heavier than the corresponding radiator employing the smaller coolant tubes. This weight increase is partially offset by a 5-lb decrease in secondary loop piping system weight. The use of larger coolant tubes in the radiator permits the use of smaller pipes in the remaining portions of the secondary coolant system.

The U-shaped radiator weight curves (Figure 37) show the influence of fin root thickness as the number of coolant tubes increase. As the coolant tube spacing decreases, the required fin root thickness also decreases. More weight is saved initially by the decrease in fin root thickness than is added by increasing the number of coolant tubes. At approximately 48 to 50 tubes, the decreased fin root thickness weight savings does not compensate for the additional coolant tube weight, and radiator system weight tends to increase.

From the results of this analysis, it was decided that the larger coolant tube should be used in the system design. The decreased pressure drop through the radiator with the larger tubes warrants the increased radiator weight. The larger coolant tubes will also minimize the chance of system plugging by NaK coolant at lower radiator temperatures.

b. Number of Coolant Tubes

Figure 38 shows the results of a series of calculations that were made to determine the radiator weight as a function of coolant tube number. These

Figure 36. Effect of Coolant Tube Parametrics on Radiator Pressure Drop

Figure 37. Effect of Coolant Tube Parametrics on Radiator Weight

6531-5907

Figure 38. Effect of Fin Material on Radiator Weight

calculations were made using the three types of aluminum that were considered for the radiator fin material; AL-1100, AL-3003, and AL-2219. The following input parameters were used:

Power to be rejected	94.8 kw
Area	240 ft ²
Inlet temperature	620° F
Emissivity	0.9
Solar absorptivity	0.4.

The coolant tube size used in this analysis for all three types of aluminum was 0.4375 in. OD with a 0.020-in. wall thickness. A meteoroid armor thickness of 0.125 in. was used in all cases considered. This thickness provides a non-critical meteoroid damage probability of 0.99 for a 5-year mission life for the stainless-steel-lined radiator fin configuration.

The weight curves in Figure 38 exhibit a characteristic U shape. Each of the three types of aluminum has a different thermal conductivity at the 600° F operating temperature. AL-1100 has the highest thermal conductivity and AL-2219 has the lowest. As the thermal conductivity decreases, the point of minimum weight shifts toward increased number of coolant tubes.

Using AL-3003 as the reference fin material, the radiator weight reaches a minimum value at approximately 48 coolant tubes. A lower pressure drop can be obtained by increasing the number of coolant tubes to 50, with a slight increase in radiator weight. The reference number of coolant tubes was, therefore, set at 50.

c. Performance in Space

A 600-n. mi constant sun shade polar orbit was used to determine the radiator thermal performance in space. This orbital attitude is considered to provide the maximum environmental thermal flux to the radiator surface as follows:

Direct solar	146 Btu/ft ² -hr
Direct earth	13.8 Btu/ft ² -hr
Earth reflected	9.5 Btu/ft ² -hr.

6531-5908

Figure 39. Radiator Heat Rejection Capability

AI-AEC-13039

In this analysis, the radiator surface was assumed to be revolving around its axis at 3 revolutions/min. Based on emissivity and solar absorptivity values of 0.9 and 0.4 respectively, the radiator will reject 94.8 kw. Also, the thermal performance of the proposed radiator should not degrade significantly due to surface coating changes. The heat rejection capability of the radiator as a function of surface emissivity and absorptivity is shown in Figure 39.

d. Summary of Trade Study Results

The following results were obtained from the various trade studies:

- 1) The available literature has shown AI-93 to be the best candidate thermal emittance coating. Surface coating degradation during space operation should not seriously affect system performance.
- 2) Based upon the meteoroid model and mission requirements, a total aluminum thickness of 0.125 in. should be adequate to protect Type 316 stainless steel coolant tubes.
- 3) The aluminum alloy 3003 condition 'O' was selected for the fin portion of the radiator surfaces. The alloy 2219 condition T62 was selected for the longitudinal and circumferential support structure.
- 4) Due to a decreased pressure drop through the radiator and to minimize the chance of system plugging by NaK coolant at lower radiator temperatures, it was concluded to use Type 316 stainless steel coolant tubes 0.4375 in. OD by 0.020-in. wall in the radiator system.
- 5) Based upon system weight and radiator coolant pressure drop studies, the reference number of coolant tubes was set at 50.

3. Volume Accumulator Unit (VAU)

a. Analysis and Design

Analysis of the conceptual design (see Section III-A-7) has been completed to determine operating characteristics.

The VAU gas charge pressure at 100° F is 6.8 psia, resulting in an initial NaK pressure for both the primary and secondary NaK loops, of 4 psia. The NaK loops will be pressurized to 3.6 psia initial pressure at a system storage temperature of ~55° F (see Table 25).

TABLE 25
VOLUME ACCUMULATOR UNIT AND SYSTEM NaK PRESSURES

	Primary Loop		Secondary Loop	
	Gas	NaK	Gas	NaK
Initial Fill (100° F)	6.8	4.0 psi	6.8	4.0 psi
Minimum Storage (55° F)		3.6		3.6
Operating (66° F)	22.9	24.9	24.6	27.2
Operating (609° F) (With failed primary containment)	18.6	19.6	19.8	21.2

b. Supporting Development

Five test bellows capsules, four welded and one formed, are being procured for fabrication and quality assurance process evaluation.

Two three-convolution Inconel 718 bellows capsules were received and are being subjected to helium leak check and radiographic examination. Material samples from the same lot, and heat treated along with these bellows, are being machined into test specimens for tensile testing at operating temperature. The other two capsules are still being fabricated. One 10-convolution Inconel 718 nesting-formed bellows capsule is also being fabricated. Search is in progress to select a source and method for heat treating the formed bellows capsule while maintaining the "bright" finish.

Following helium leak check and radiographic examination, the bellows will be nondestructively examined, then subsequently will be metallographically examined to evaluate the fabrication and inspection processes.

C. THERMOELECTRIC CONVERTER DEVELOPMENT

1. Reactor Power Modules

a. Introduction

This report summarizes the work done at Westinghouse Astronuclear Laboratory on the Compact Thermoelectric Converter System Technology Program during the months of May, June, and July 1972. During the past quarter, five refractory-clad reactor power modules were tested continuously and three new

modules placed on test. Major emphasis was placed on the evaluation of performance data from these modules and on the generation of module parametric calculations in support of reactor/thermoelectric systems studies.

All reactor power modules continued to operate with power output degradation rates ranging from 1.0 to 5.8% per 10,000 hours for operating times up to 10,449 hours, hot cladding operating temperatures as high as 1140° F average (1190° F peak), and including up to 59 thermal cycles per module.

b. TEM-X S/N-3 Series Module

Of the seven refractory-clad TEM-X S/N-3 series power module tested during the past quarter, the first four (S/N-3C, 3D, 3E, and 3F) employ end caps which are backfilled with an inert gas (argon) to prevent air leakage around the exposed boron nitride electrical pin insulators. TEM-X S/N-3C and S/N-3E have accrued 10,449 and 8,617 hours of static testing at WANL, respectively, at $\bar{T}_H = 1085^\circ\text{F}$, $\bar{T}_C = 507^\circ\text{F}$ test conditions with the former being cycled 59 times. Electrical power output, overall efficiency, open circuit voltage, and internal resistance life test data from these modules are presented in Figures 40 through 43, respectively, along with similar data from loop tested modules. TEM-X S/N-3D and 3F are on test in the liquid metal loop system at Atomics International. The latter module was operated initially at $\bar{T}_H = 1085^\circ\text{F}$, $\bar{T}_C = 507^\circ\text{F}$ temperatures and was then taken to elevated $\bar{T}_H = 1140^\circ\text{F}$, $\bar{T}_C = 523^\circ\text{F}$ operating temperatures as is clearly distinguishable in the Figures 40 through 43 data. Both reactor power modules on loop test are currently being operated with 100° F axial temperature drops.

The excellent degree of performance stability of these refractory clad modules is quite evident from the data shown in Figures 40 through 43. A statistical analysis (linear curve fit) of all life test data from TEM-X series modules is presented in Table 26.

Of some concern is the fact that the resistance and power degradation rate data from TEM-X S/N-3D are somewhat higher than similar data from modules being tested at the same average temperatures in static test stands (see Table 26). Recently an analysis was performed to determine the effects of axial temperature profile on degradation rates. This analysis indicated that a module operating at $\bar{T}_H = 1085^\circ\text{F}$ in a liquid metal loop system with a 200° F primary

Figure 40. Power Output vs Elapsed Time for Refractory-Clad Reactor Power Modules

Figure 41. Overall Efficiency vs Elapsed Time for Refractory-Clad Reactor Power Modules

Figure 42. Open Circuit Voltage vs Elapsed Time for Refractory-Clad Reactor Power Modules

Figure 43. Internal Resistance vs Elapsed Time for Refractory-Clad Reactor Power Modules

TABLE 26

REFRACTORY-CLAD REACTOR MODULE PERFORMANCE STABILITY SUMMARY

Module	$\bar{T}_H/\bar{T}_C(^{\circ}\text{F})$	Testing Time (Hours)	Rate of Change: % Per 10,000 Hours				Thermal Cycles	
			R_g	V_{oc}	P_e	η_o	Rapid	$30^{\circ}\text{F}/\text{min.}$
TEM-X S/N-3C	1085/507-S	10449	3.3	0.7	-1.8	-0.6	6	53
TEM-X S/N-3D	1085/507-L	7232	6.1	1.1	-3.7	-2.7	5	53
TEM-X S/N-3E	1085/507-S	8617	3.3	0.8	-1.7	-0.7	6	0
TEM-X S/N-3F	{ 1085/507-L	1011	6.0	2.5	-1.0	+4.1	5	1
	{ 1140/523-L	5013	9.0	1.4	-5.8	(A)		
TEM-X S/N-3G	1085/507-S	1915	9.6	4.4	-0.7	+2.8	3	0
TEM-X S/N-3H	1140/523-S	1675	5.2	1.5	-2.2	-1.9	1	1
TEM-X S/N-3J	1175/535-S	483	(B)	(B)	(B)	(B)	3	0
TEM-X S/N-4	1085/507-S	398	(B)	(B)	(B)	(B)	0	0

L Indicates Loop Test
 S Indicates Static Test
 (A) Not calculated due to flow meter fluctuations
 (B) Insufficient data for meaningful analysis

All test data included through July, 1972.

temperature drop should exhibit higher degradation rate than an identical module operated in a static test stand. Quantitatively, based on degradation rate activation energies determined from TEM-15A series module data, the loop test module should have a resistance degradation rate 45% higher than a static tested module, and a power output degradation rate 30% higher. The observed degradation rates are in reasonable agreement with this analysis.

A second group of refractory-clad modules is identical to the four modules, discussed above, except that glass-to-metal seals were incorporated at each pin base to allow testing in an air environment. The three "glass seal" TEM-X S/N-3 modules on test to date, are being operated at three different temperature levels to experimentally determine the relationship between degradation rates and operating temperatures. Figures 44 and 45 present overall efficiency and power output data from S/N-3G, 3H, and 3J as function of testing time. The data indicate, as the calculations predicted, that although there are substantial power output increases associated with each increase in hot side temperature, the accompanying efficiency increase is negligible. This phenomenon is caused by the rapid increase in lead telluride thermal conductivity as a function of increasing temperature. It must be remembered, however, that these tests are being conducted assuming fixed radiator operation, such that the cold cladding temperature increases as the hot side temperature is increased (at about one third the rate).

The operating temperatures and corresponding linear curve fit degradation rates for these three modules are presented in Table 26. None of the indicated degradation rates are felt to be particularly meaningful because of the uncertainties inherent in this type of analysis for relatively short testing times.

TEM-X S/N-3J was off test much of the past reporting period as a result of an erratic power supply. The difficulty is associated with feedback between control circuits of power supplies operating on the same a-c line circuit. Laboratory modifications have been made to eliminate the problem and the module was recently returned to $\bar{T}_H = 1175^\circ \text{F}$, $\bar{T}_C = 535^\circ \text{F}$ operating temperatures.

An additional reactor power module, TEM-X S/N-3K, was shipped to Atomics International during the month of June. This module differs from the previous three reactor modules in that the glass-to-metal seals were not installed. TEM-X S/N-3K will be tested in the liquid metal loop system and

Figure 44. Power Output vs Elapsed Time for Recent Refractory-Clad Reactor Power Modules

Figure 45. Overall Efficiency vs Elapsed Time for Recent Refractory-Clad Reactor Power Modules

Figure 46. Electrical Current Data from Prototype Pump Power Modules

under vacuum conditions. The test will simulate the effects of the failure of glass seals during space environment operation. The test plan for this module is being modified to call for $\bar{T}_H = 1140^\circ\text{F}$, $\bar{T}_C = 523^\circ\text{F}$ with 100°F axial temperature gradients.

Drafting work on TEM-X S/N-3M, the first reactor module of the FY 73 program, has been completed and the module released for fabrication. This module will be identical to TEM-X S/N-3G, 3H, and 3J and will be tested in the liquid metal loop system at Atomics International.

c. High Voltage Refractory-Clad Module (TEM-X S/N-4)

The next generation reactor power module is directed at demonstrating that tubular converters can deliver power levels equivalent to that of the TEM-X S/N-3 series (300 watts) at substantially higher voltage levels while maintaining an acceptable power degradation rate. To accomplish this, TEM-X S/N-4 was designed with a reduction in thermoelectric washer thickness from 0.188 in. (as in TEM-X S/N-3 series modules) to 0.090 in. This provides approximately a factor of two increase in voltage.

Although heatup data were not reduced in time for inclusion in this report, the module is currently on static test at $\bar{T}_H = 1085^\circ\text{F}$, $\bar{T}_C = 507^\circ\text{F}$ operating conditions. Good agreement was achieved between calculated and predicted beginning-of-life (BOL) performance. The module has accrued 398 hours of testing time, which is insufficient to allow performance stability analyses.

2. Pump Tubular Power Modules

a. TEM-14A Pump Modules Performance Summary

Two tubular modules designed to deliver power to an electromagnetic pump continued on test at WANL during the past quarter. These prototype pump modules, designated as TEM-14A S/N-1 and TEM-14A S/N-2, are being endurance tested at nominal average cladding temperatures of $\bar{T}_H = 1125^\circ\text{F}$, $\bar{T}_C = 570^\circ\text{F}$.

Electrical current and electrical power output data from the pump modules are presented in Figures 46 and 47, respectively. Performance levels achieved in S/N-2, designed with the number of nickel electrical pins increased to 12 from 6 to minimize electrical resistance, are in excellent agreement with the design levels (110 watts at 500 amp).

Figure 47. Power Output Data from Prototype Pump Power Modules

Table 27 presents a summary of performance data from the electromagnetic pump modules. Degradation rates for four parameters were determined from a statistical analysis of experimental data from these modules.

The long-term stable performance capabilities of the tubular module incorporating lead telluride thermoelectric materials are clearly demonstrated by the performance data from the TEM-14 series pump modules. Successful performance following the fifty controlled thermal cycles on S/N-3, a previously tested pump module, and the various uncontrolled shutdowns of all three modules indicate that the TEM-14 design is relatively insensitive to thermal cycling.

b. TEM-14B Pump Modules

Detail design and drafting work was initiated on three new pump power modules during the past quarter. These modules will be nearly identical to the TEM-14A series modules except that 50% radial thickness tungsten foil diffusion barriers will be incorporated. Because of the small number of couples (four), the thermal penalty associated with the incorporation of foil barriers is unmeasurably small. However, the performance stability improvement resulting from the incorporation of foil barriers should be measurable.

These modules, which will be designated TEM-14B S/N-1, 2, and 3, will also include many mechanical design improvements incorporated into reactor modules since early 1969, when the last pump power module was fabricated. These modules will be utilized in an integrated pump test to be conducted at Atomics International.

c. Pump Module Parametric Analysis

During the past quarter, several pump module parametric studies were completed. This effort was undertaken in support of the 5-kwe demonstration thermoelectric reactor system design being conducted at Atomics International.

These calculations were performed to determine axial dimensions and numbers of couples for optimum TEM-14 pump modules having various voltage and current characteristics. For the first parametric study, the existing TEM-14 radial dimensions were held fixed and $\bar{T}_H = 1168^\circ\text{F}$, $\bar{T}_C = 522^\circ\text{F}$ cladding temperatures were selected on the basis of preliminary system analysis results

TABLE 27
PUMP MODULE PERFORMANCE STABILITY SUMMARY

Module	Testing Time (Hours)	Rate of Change: % Per 10,000 Hours				Thermal Cycles	
		R_g	V_{oc}	P_e	η_o	Rapid	30°F/Min.
TEM-14A S/N-1	17847	*	*	-4.8	-6.2	9	0
	12003**	0.9	-1.2	-3.2	-3.2	5	1
TEM-14A S/N-2	22431	4.3	-1.9	-4.4	-5.9	13	1

* No open circuit data taken for S/N-1 during first 17847 hrs. of testing.

** S/N-1 data taken after reinstrumentation.

$\bar{T}_H = 1125^\circ\text{F} / \bar{T}_C = 570^\circ\text{F}$ (both modules)

All data included through July 10, 1972.

received from Atomics International. The object of the study was to indicate the effects of pump module current and load voltage requirements on overall pump module efficiency and circuit length (hence module weight).

To complete the analysis, a computer code was developed to calculate the number of thermoelectric couples and washer axial thicknesses required to produce a given load voltage and current. The code was then used to calculate optimum TEM-14 parameters for voltages ranging from 0.1 to 0.3 volt, and load currents ranging from 500 to 900 amp.

Results of the study are presented in Figures 48 and 49. Figure 48 shows the effect of operating requirements on overall module efficiency. For a fixed load voltage, efficiency varies inversely with current. This is because the module power output increases only linearly with current, whereas the Joule power losses (I^2R) in the electrical access pins (a significant parameter in pump module operation) increases with the square of the current. For a fixed current, efficiency varies directly with load voltage since the Joule power losses in the electrical access pins remain constant while the module power output is increased.

The relationship between circuit length and operating requirements is shown in Figure 49. The characteristic shapes of the parametric curves shown in this figure result from the fact that circuit length varies directly with the electrical power requirement for a fixed set of operating temperatures.

An additional set of similar calculations was completed for $\bar{T}_H = 1135^\circ\text{F}$ and $\bar{T}_C = 567^\circ\text{F}$ operating temperatures. Figure 50 shows the effect of the operating requirements on overall module efficiency, while the relationship between circuit length and operating requirements is shown in Figure 51. These figures exhibit the same characteristic slopes seen in Figures 48 and 49; however, the curves are shifted downward as a result of the reduced module radial temperature drop.

3. Supporting Technology Development

a. Sublength Modules

The TEM-15 series modules were designed as a part of the support technology development phase of the Compact Thermoelectric Converter System

Figure 48. EM Pump Module 1168/522 Parametric: Efficiency vs Current

Figure 49. EM Pump Module 1168/522 Parametric: Circuit Length vs Current

Figure 50. Pump Module 1135/567 Parametric: Efficiency vs Current

Figure 51. Pump Module 1135/567 Parametric: Circuit Length vs Current

TABLE 28
PROCESS AND DESIGN FEATURES OF TEM-15 SERIES MODULES
(Sheet 1 of 2)

Module	Process and Design Innovation
15A S/N-1	Same as 15A-4 except 300 lb axial preload and EB welded.
15A S/N-4	Baseline design: 36 couples, .050 inch T/E washers, .005 inch mica, .001 inch mica radial clearance, 1100 lb axial preload, TIG welded, hydrogen purged, tungsten insert under p-type washer with axial clearance at assembly.
15A S/N-4A	Same as 15A-4. (Destructively examined after processing.)
15A S/N-4B	Same as 15A-4. (Thermal cycled at 2.5°F/min ramp rate.)
15A S/N-4C	Same as 15A-4. (Thermal cycled at 30°F/min ramp rate.)
15A S/N-4D	Same as 15A-4. (Steady state endurance tested.)
15A S/N-4E	Same as 15A-4 except not hydrogen purged.
15A S/N-4F	Same as 15A-4 except not hydrogen purged and tungsten insert clearance eliminated.
15A S/N-4G	Same as 15A-4F. (Tested at $\bar{T}_H = 1025^\circ\text{F}$, $\bar{T}_C = 480^\circ\text{F}$)
15A S/N-4H	Same as 15A-4F except leak-tight glass seals incorporated.
15A S/N-4J	Same as 15A-4F except processing conditions modified.
15A S/N-5	Same as 15A-4 except 300 lb axial preload, EB welded, and .010 inch mica radial clearance incorporated.
15A S/N-8	Same as 15A-4 except .010 inch mica radial clearance incorporated.
15B S/N-4	Same as 15A-4 except each intercouple insulator consists of .0004 inch W foil (full radius) sandwiched between two .0025 inch mica washers.
15B S/N-4A	Same as 15B-4 except each intercouple insulator consists of .0004 inch W foil (1/2 radius) sandwiched between two .005 inch mica washers (33 couples).
15B S/N-4B	Same as 15B-4 except W foil prevented from extending beyond mica at O. D. and tungsten insert clearance eliminated.
15B S/N-4C	Same as 15B-4B except W foil reduced from full to 1/2 radius.
15B S/N-4D	Same as 15B-4B except W foil extended through inner conductor ring.

Technology Program. These modules were designed with radial dimensions identical to those of reactor power modules, but with circuit lengths reduced to 4.0 in. from 15.0 in. The purpose for fabricating and testing these modules is to evaluate the effects of design, processing, and fabrication modifications on module performance. All TEM-15 series modules tested to date have been operated in static test facilities at WANL.

A list of all the process and design features of each TEM-15 series module fabricated, or scheduled for fabrication to date is presented in Table 28. Twelve TEM-15 series modules were tested continuously and three additional modules were placed on test during the last quarter.

Table 29 lists the operating conditions of all TEM-15 series modules tested during this reporting period and summarizes the results of a degradation analysis. Degradation rates were determined using all data recorded through July 17, 1972.

(1) TEM-15A Series Modules

TEM-15A series modules were designed to investigate the influence of minor component dimensional variations, airtight power pin seals, and the hydrogen purge operation on module performance stability; the influence of weld shrinkage on circuit alignment and mica deformation; and the influence of axial pre-load on mica deformation and circuit alignment in a module incorporating standard 0.005-in.-thick mica insulators. No diffusion barriers have been incorporated in any of these modules.

Seven TEM-15A series modules were endurance tested during the past quarter. Electrical power output and overall efficiency data from the first four of these modules are represented in Figures 52 and 53, respectively, as a function of elapsed time. All of these modules were tested at $\overline{T}_H = 1125^\circ\text{F}$, $\overline{T}_C = 570^\circ\text{F}$ operating conditions for testing times ranging from 10,146 to 21,607 hours. As can be seen in the figures, excellent long-term performance reproducibility has been achieved in all four modules, indicating that none of the minor design, processing, or fabrication modifications investigated in this series of experiments has had a gross effect on module performance. To date, none of the modules has indicated a tendency to deviate from the characteristic linear power degradation despite the long testing times.

TABLE 28
PROCESS AND DESIGN FEATURES OF TEM-15 SERIES MODULES
(Sheet 2 of 2)

Module	Process and Design Innovation
15C S/N-4	Same as 15A-4 except each intercouple insulator consists of 2000 - 4000Å Mo barriers deposited between two .0025 inch mica washers.
15C S/N-4A	Same as 15A-4F except each intercouple insulator consists of 500-1500Å tungsten barriers EB deposited between two .0025 inch mica washers.
15C S/N-8	Same as 15C-4 except .010 inch mica radial clearance incorporated.
15D S/N-4	Same as 15A-4 except .010 inch mica insulators incorporated. (33 couples)
15E S/N-4	Same as 15A-4 except APX-4 type PbTeSe substituted for TEGS-2P material.
15F S/N-4	Same as 15A-4F except ANX-6 type PbTe-GeTe substituted for Ge n-type material.
15H S/N-4	Same as 15A-4F except W inserts used under n-type washers at the inner conductor rings.

TABLE 29
TEM-15 PERFORMANCE ANALYSIS SUMMARY

Module	Test Conditions $\bar{T}_H/\bar{T}_C(^{\circ}\text{F})$	Testing Time	Rate of Change: % Per 10,000 Hours				Thermal Cycles	
			R_g	V_{oc}	P_e	η_o	Rapid	$30^{\circ}\text{F}/\text{min.}$
TEM-15A S/N-1	1125/570	21,365	32.1	1.4	-17.6	-15.2	12	1
TEM-15A S/N-4D	1125/570	21,607	32.3	1.6	-17.4	-15.0	11	2
TEM-15A S/N-4F	1125/570	10,146	29.2	1.6	-20.2	-15.8	5	2
	1200/637	454	*	*	*	*		
TEM-15A S/N-4 G	1025/480	5,051	10.1	2.6	-4.5	-2.5	5	0
	1075/525	336	*	*	*	*		
TEM-15A S/N-4H	1125/570	1,240	28.5	3.2	-21.4	-17.2	1	1
TEM-15A S/N-4J	1125/570	305	*	*	*	*	0	0
TEM-15A S/N-5	1125/570	21,315	30.6	2.1	-16.7	-14.0	13	51
TEM-15B S/N-4A	1125/570	15,855	14.2	2.6	-7.3	-6.5	7	1
TEM-15B S/N-4B	1125/570	10,765	17.8	1.5	-12.7	-10.5	6	1
TEM-15B S/N-4C	1125/570	7,026	22.5	3.1	-13.7	-10.0	5	0
TEM-15B S/N-4D	1125/570	279	*	*	*	*	0	0
TEM-15C S/N-4A	1125/570	7,350	22.6	2.8	-14.5	-10.4	4	2
TEM-15C S/N-8	1125/570	18,864	24.9	2.0	-14.4	-12.0	10	1
TEM-15D S/N-4	1125/570	19,915	19.9	2.4	-11.3	-8.6	9	1
TEM-15H S/N-4	1125/570	11,153	22.5	2.5	-14.1	-12.3	5	1

All data included through July 10, 1972.

* Insufficient data to allow meaningful stability analysis.

Figure 52. A Comparison of Power Output Data from Early TEM-15A Series Modules

Figure 53. A Comparison of Overall Efficiency Data from Early TEM-15A Series Modules

TEM-15A S/N-5 has accrued 7119 hours of steady-state operation since completion of 50 controlled thermal cycles. The time at which the cycles were performed is indicated in Figures 52 and 53. Analysis of the data shows that the degradation rates have remained essentially unchanged from the pre-cyclic test levels.

Two new TEM-15A S/N-4 series modules were fabricated, processed, and placed on test during the past quarter. The first of these modules, TEM-15A S/N-4H includes all end closure design modifications required to incorporate glass-to-metal seals at the electrical pin bases in a manner identical to that used in the TEM-X design. The purpose of this experiment is to determine if the elimination of air leakage at the pin bases has a measurable effect on performance levels or stability. The module is being tested at $\bar{T}_H = 1125^\circ \text{F}$, $\bar{T}_C = 570^\circ \text{F}$ temperatures.

The second new module, TEM-15A S/N-4J, is defined as a process experiment module. The objective of this experiment was to determine the minimum hot autoclave temperature required to achieve full compaction utilizing the full 30,000-psi pressure capability of the autoclave facility at WANL and to determine the effects of processing at lower temperatures on module performance stability.

In the current hot autoclave cycle, full compaction is achieved by the gas pressure sintering operation carried out at 20,000 psi and 700°C (1292°F). Stress analysis indicated that by increasing the pressure, the temperature required to achieve full compaction could be substantially reduced. This reduction in temperature could, in turn, reduce the amount of pre-operational tellurium diffusion, hence reduce module degradation. Based on dimensional data from three previous TEM-15A series modules (S/N-4F, 4G, and 4H), a 0.020-in. radial outer cladding reduction represents full compaction.

After completing the room temperature isostatic compaction, TEM-15A S/N-4J was subjected to three subsequent hot autoclave cycles. The first cycle, with a maximum pressure of 20,000 psi at 400°C (752°F), represented a 300°C reduction in process temperature while maintaining standard pressure. Measurements of the outer cladding outer diameter after this cycle indicated an average radial reduction of 0.017 in. compared to an expected reduction of

0.014 in. A second cycle at 30,000 psi and 400° C (752° F) was then completed. Diametrical measurements taken of the outer cladding after completing the cycle indicated a total of 0.0183-in. radial reduction from assembly dimensions. This was within 0.0015 in. of the predicted value.

A third and final cycle was completed at 30,000 psi and 445° C (833° F). The temperature for this cycle was selected to remain below the melting point of tellurium. Cladding measurements taken after this cycle indicated a total of 0.019-in. radial reduction from assembly dimensions. Because of the close agreement between these data and similar data from previously processed modules, it was decided that further processing would result in negligible additional diametrical change.

Room temperature measurements indicated an internal resistance within 1.2 milliohms of the average of 8.5 milliohms observed for TEM-15A modules.

Table 30 lists a comparison of beginning-of-life test data from both new modules (S/N-4H and 4J) with similar data from all other electrically identical TEM-15 series modules on test at $\bar{T}_H = 1125^\circ \text{F}$, $\bar{T}_C = 570^\circ \text{F}$. Also shown are the calculated parameters for these operating conditions. Within experimental accuracy, the BOL performance of all modules closely correlates with the calculated levels. These data indicate that the glass-to-metal seal (TEM-15A S/N-4H) and the modified processing conditions (TEM-15A S/N-4J) have had no effect on initial performance.

Electrical power output and overall efficiency data from the two new modules are presented in Figures 54 and 55, respectively. Insufficient testing times have been accrued by these modules to assess the effects of the modifications on performance stability.

During the past quarter, operating temperatures for two TEM-15A series modules were modified. The modifications were made to generate test data which will allow an experimental determination of the effects of temperature on degradation in the operating range from $\bar{T}_H = 1025^\circ \text{F}$ to $\bar{T}_H = 1200^\circ \text{F}$.

TEM-15A S/N-4F operating temperatures were increased to $\bar{T}_H = 1200^\circ \text{F}$, $\bar{T}_C = 637^\circ \text{F}$ from $\bar{T}_H = 1125^\circ \text{F}$, $\bar{T}_C = 570^\circ \text{F}$. The effects of the change in operating temperatures on power output and efficiency for this module are shown in Figures 52 and 53, respectively. TEM-15A S/N-4G operating temperatures

TABLE 30
A COMPARISON OF CALCULATIONS AND BOL DATA
FROM FOUR RECENT TEM-15 MODULES

	V_{oc} (Volts)	R_g (M- Ω)	P_e (Watts)	Q_{in} (Watts)	η (Pct.)
TEM-15A S/N-4F	4.73	87.6	63.8	1716	3.72
TEM-15C S/N-4A	4.78	86.1	66.4	1798	3.69
TEM-15A S/N-4H	4.72	88.2	63.0	1764	3.57
TEM-15A S/N-4J	4.76	88.4	64.2	1759	3.65
Calculated	4.76	86.6	65.6	1749	3.75

Note: $\bar{T}_H = 1125^\circ\text{F}$ and $\bar{T}_C = 570^\circ\text{F}$, matched load.

Figure 54. A Comparison of Power Output Data from Recent TEM-15A Series Modules

Figure 55. A Comparison of Overall Efficiency Data from Recent TEM-15A Series Modules

were increased to $\bar{T}_H = 1075^\circ\text{F}$, $\bar{T}_C = 525^\circ\text{F}$ from the original level of $\bar{T}_H = 1025^\circ\text{F}$, $\bar{T}_C = 480^\circ\text{F}$. Power output and overall efficiency data from this module recorded at both operating temperature levels are shown in Figures 54 and 55, respectively. Degradation rates associated with operation at the previous temperatures have been well established.

After establishing a $\bar{T}_H = 1075$, $\bar{T}_C = 525^\circ\text{F}$ degradation rate (3,000 to 5,000 hours of additional testing time are estimated to be required), operating temperatures of TEM-15A S/N-4G will be further increased to $\bar{T}_H = 1125^\circ\text{F}$, $\bar{T}_C = 570^\circ\text{F}$. This test sequence will determine if a module, having been operated for significant testing times at lower temperatures, will assume a degradation rate consistent with the higher test temperatures. $\bar{T}_H = 1125^\circ\text{F}$, $\bar{T}_C = 570^\circ\text{F}$ degradation rates have been well established. This experiment would establish the validity of the degradation rate measured at the increased temperature conditions, both for TEM-15A S/N-4G and 4F.

In each case, the average cold cladding temperatures were selected to maintain an inner-to-outer cladding differential thermal expansion equal to that existing at standard $\bar{T}_H = 1125^\circ\text{F}$, $\bar{T}_C = 570^\circ\text{F}$ operating conditions.

(2) TEM-15B Series Modules

The TEM-15B designation applies to all sublength reactor technology modules incorporating tungsten foil diffusion barriers positioned between two mica electrical insulators. Three TEM-15B series modules continued on test throughout this reporting period and one additional module was placed on test. The individual design features of each module are listed in Table 28.

Electrical power output and overall efficiency data from the first three tungsten foil barrier modules are presented in Figures 56 and 57, respectively. Although the power output and efficiency levels of the TEM-15B series modules are somewhat lower than those of the nonbarrier TEM-15A series modules as a result of the tungsten foil shunt heat losses, greatly improved performance stability has been achieved as can be seen in Table 29.

Both the data plots and the degradation analysis show that the performance stability of the 0.010-in. mica module (TEM-15B S/N-4A) is substantially better than that of either module incorporating 0.005-in. mica. In addition, a comparison between S/N-4B and 4C verifies that substantial performance

Figure 56. A Comparison of Power Output Data from TEM-15B, Tungsten Foil Barrier Modules

Figure 57. A Comparison of Efficiency Data from TEM-15B, Tungsten Foil Barrier Modules

Figure 58. TEM-15B S/N-4D Couple Design

TABLE 31
A COMPARISON OF CALCULATED AND MEASURED TEM-15B S/N-4D
PERFORMANCE DATA

	<u>Calc.</u>	<u>B. O. L.</u>	<u>542 Hours</u>
Open Circuit Voltage (volts)	4.46	4.50	4.47
Internal Resistance (m Ω)	84.9	296	175
Power Output (watts)	58.4	17.1	28.5
Heat Input (watts)	1817	1860	1859

improvements can be achieved with little or no power degradation penalty by terminating the foil barriers midway radially in the thermoelectric washers. Temperatures beyond the midpoint are too low to sustain tellurium diffusion.

Degradation rate data and destructive examination of tungsten foil barrier modules indicate that the primary residual mode of degradation in the TEM-15B series modules is tellurium transport around the ends of the tungsten foil. During the past quarter, an additional foil barrier module, TEM-15B S/N-4D, was fabricated, processed, and placed on test. This module incorporates the couple design shown in Figure 58. Tellurium transport around the ends of the tungsten foil was combatted by extending the tungsten foil radially inward to the inner boron nitride insulator as shown. This required fabricating the inner conductors as two separate rings. Because of the uncertainties involved in the introduction of axial electrical interfaces, this experiment was defined to determine the effects of increasing the tellurium diffusion path length, but was not intended to identify a couple design to be incorporated into a reactor module.

Room temperature, post-processing, resistance data from TEM-15B S/N-4D was about 2 milliohms higher than the theoretical resistance based on the sum of the bulk resistance of all individual components at room temperature. The difference, which is typically zero for tubular modules, was ascribed to the interface resistance contribution of the two axial inner conductor ring-tungsten foil electrical interfaces in each couple.

Heatup data from this module indicated that this axial interface component rose steadily as the inner cladding temperature increased. At operating temperatures, the interface component was more than twice the value of the bulk resistance. A comparison of calculated and experimental TEM-15B S/N-4 performance data is shown in Table 31. In all previous modules, none of which have axial electrical interfaces, the total interface contribution is negligible with respect to the bulk material resistance. The increasing interface resistance in this module results from the fact that the Inconel inner cladding expands much more rapidly than the inner conductors tending to reduce axial interface pressures.

Figure 59. A Comparison of Power Output Data from Improved Performance Stability TEM-15 Series Modules

Since the module was placed on test, the total resistance has dropped as is shown in Table 31. This effect is caused, presumably, by tellurium flow from the thermoelectric washer into the interface between the conductor rings and tungsten foil. However, it is doubtful that the effect of reduced intercouple tellurium transport will ever be evaluated based on experimental data. The success of the experiment, then, will be based on the results of a destructive examination of the module after it has accrued sufficient testing time.

(3) Additional Modules

In addition to the tungsten foil barrier and nonbarrier modules, four one-of-a-kind modules were tested continuously during the past quarter. These are the last four modules listed in Table 29. TEM-15C S/N-4A, TEM-15C S/N-8, TEM-15D S/N-4, and TEM-15H S/N-4, whose individual design features are listed in Table 28, all have accrued substantial testing times and all continued to operate without change during the past quarter. These modules indicate that various degrees of performance stability improvements can be achieved through the use of electron-beam, vapor-deposited barriers; tungsten n-washer contacts; and increased mica-washer thicknesses. Power output and overall efficiency data from these modules are presented in Figures 59 and 60, respectively.

Of particular interest in this group of modules is the fact that TEM-15C S/N-4A, a tungsten EBVD barrier module, continues to exhibit performance stability as low as has been observed for tungsten foil barrier modules (see Table 29). This is of significance due to the substantially improved performance associated with elimination of the tungsten foil barriers and their associated shunt heat losses. TEM-15C S/N-4A has a beginning-of-life efficiency level 18% higher than TEM-15B S/N-4C and 37% higher than TEM-15B S/N-4B.

b. Glass Seal Development

During this reporting period, another glass seal failed between 3498 and 4028 hours at the 500° C test temperature. The data point lies slightly above the projected lifetime vs $1/T$ line previously reported. A complete summary of all seals is shown in Table 32. The two autoclaved end mockups were still HLT after the times shown, although the external appearance of the glass was poor. Accordingly, the 3634-hour seal was designated to be destructively examined to establish, if possible, the glass configuration at the bottom of the annulus.

Figure 60. A Comparison of Overall Efficiency Data from Improved Performance Stability TEM-15 Series Modules

TABLE 32
SUMMARY OF SEALS MADE WITH LEAD BASE GLASS PLUS FELDSPAR (Melt 1)

	Cycles		Life Test - 500°C (932°F)		
	Number Complete	Number Helium Leak Tight	Total Hours	Hours - Last Check HLT	
NM-S 12	25	25	3392	2693	Terminated
NM-S 13	5	5	4153	4028	Still on test
8P-9LA	50	50	4028	3498	Terminated
8P-10A					Test Complete
8P-11LA			3318	2642	Terminated
8P-12LA			3318	2642	Terminated
8P-18L			3392	2693	Terminated
8P-19L	54	54			Test Complete
MEM-2			3634	3634	Terminated
MEM-3			2999	2999	On Hold
		Test Temp. (°C)			
8P-14LA		600	26	18	Terminated
8P-15LA		570	103	40	Terminated
8P-20LA		540	581	513	Terminated

NM - Single Pin Mockup
8P - Eight Pin Mockup
MEM - Module End Mockup
HLT - Helium Leak Tight

This mockup, MEM-2, was examined metallographically by cutting out an axial portion of the pin seal. Although it was possible to observe the good adherence in the nickel-to-glass seal region, most of the glass chipped out during polishing. Hence, it was not possible to evaluate how well the annular cavity was filled by the glass. Additional pin seals and an autoclaved end mockup have been placed on test at 400° C.

A large batch of glass was formulated and reduced to powder. This material will be used to fabricate pre-forms which will be used to seal future modules. The use of pre-forms will insure that a uniform amount of glass will be applied to each pin annulus and the glass will be confined to a specific space without overlap onto the retaining ring. A sample of this glass has been sent for chemical analysis to confirm the composition. In addition, an eight-pin mockup fabricated during July remained helium leak-tight after 70 cycles, but leaked after 77 cycles. Life testing of other seals is continuing to further qualify this new batch of glass prior to its use on powder modules and these seals will be summarized in the next report.

c. Ternary Materials

(1) P-Type

TEM-15E S/N-4 was a 4-in. tubular thermoelectric module identical in design dimensions to the TEM-15A S/N-4 modules developed under the Compact Thermoelectric Converter System Technology Program. Built under Westinghouse Product Development funding, the module contained standard n-type lead telluride for the n-type washer, and a lead telluride-lead selenide (APX 4) alloy for the p-type washer. The APX 4 alloy was based on that composition reported by RCA^(4,5) to possess a power conversion efficiency superior to p-type lead telluride. The specific composition formulated was $\text{PbTe}_{0.96}\text{Se}_{0.05}\text{Na}_{0.01}$.

The TEM-15E S/N-4 was fabricated and processed in the same manner as conventional TEM-15A S/N-4 type modules. Subsequently, the module was placed on static test, and the performance of TEM-15E S/N-4 demonstrated that the thermal conductivity of the ternary PbTeSe was about 20% lower than conventional p-type lead telluride. However, the electrical resistivity of the module was higher than was calculated. In addition, the change in electrical properties proceeded at a rate which was faster than that established by the

TEM-15A S/N-4 series. Accordingly, the module was removed from test after 1000 hours and scheduled for destructive examination.

The module was stripped of its static test appurtenances and cut into a number of sections to facilitate various modes of examination. Some of the module thermoelectric washers were converted into electrical test bars to permit making Seebeck coefficient and electrical resistivity measurements as a function of temperature. Other portions of the assembly have been subjected to hermetic end seal tests, Seebeck probe tests, electron microprobe, X-ray diffraction examination, and conventional metallographic examination. In addition, a number of thermoelectric material specimens were submitted for spectrographic and precision chemical analyses.

The experimental work and data analysis have been essentially completed. A detailed report is being prepared to document the findings. The results and conclusions of the evaluation are itemized below.

- 1) No evidence was found to indicate an incompatibility problem between the PbTeSe alloy and the tungsten conductor ring.
- 2) Electrical measurements of bars fabricated from p-type washers removed from the TEM-15E S/N-4 show a resistivity higher than that previously measured on Q-bars from the PbTeSe melt used to make the p-type washers.
- 3) Experiments have shown that a reduction in the Te/Na ratio below a value of one by as little as 0.025 can significantly raise the electrical resistivity of the p-type material. The net effect of reducing the Te/Na atom ratio is to decrease the free carrier concentration. This behavior is also common to p-type, Na-doped lead telluride.
- 4) It is postulated that the initial high resistance of the TEM-15E S/N-4 was a result of tellurium loss during the relatively high 650°C heat treatment of the washers, particularly because of their high surface-to-volume ratio. Additionally, some tellurium was probably lost during the autoclave processing step.
- 5) The evidence of tellurium diffusion as observed during electron microprobe examination shows that tellurium has migrated from the p-type

alloy, reducing the Te/Na atom ratio. Electrical probing data show that not only is the corner of the n-type lead telluride washer affected near the hot conductor ring, but also the adjacent corner of the p-type alloy shows a change in properties. This latter effect is attributed to loss of free carriers due to tellurium migration. Hence, it is concluded that the degradation of this module exceeded previous comparable modules because the p-type material was changing as well as the n-type.

- 6) Electrical measurements made on bars fabricated from surplus n-type washers and washers reclaimed from the module indicate no meaningful changes compared to Q-bars from the original n-type lot. However, it would be difficult to evaluate a 2.5% change if we assume the total measured 5% resistance change in the module was equally caused by the n- and p-type materials.

As a result of the examination, a new formulation of the ternary material was decided upon. The new alloy has a higher Te/Na atom ratio with a specific composition of $\text{PbTe}_{0.965}\text{Se}_{0.05}\text{Na}_{0.01}$. Some melts of this material have been prepared, the ingots reduced to powder, and test bars pressed for electrical property measurements.

(2) N-Type

The TEM-15F S/N-4 was identical in design dimensions to the TEM-15E S/N-4 described above. The thermoelectric material used was p-type PbTe and the n-type ternary PbTe-GeTe alloy. The specific composition as formulated was $\text{Pb}_{0.95}\text{Ge}_{0.05}\text{Te}(\text{PbI}_2)_{0.0007}$. BOL test data on this module showed the internal resistance and thermal conductivity to be higher than expected based on TEMOD calculations. The overall degradation rate was higher than comparable modules using n- and p-type lead telluride. The module was removed after 4865 hours at test conditions of $\bar{T}_H = 1125^\circ\text{F}$, $\bar{T}_C = 570^\circ\text{F}$. Destructive examination of this module was carried out in a manner similar to that described above for the TEM-15E S/N-4.

Analysis and evaluation of the data are in process. The following statements summarize the preliminary findings and tentative conclusions.

- 1) Chemical analysis of washers made from the ternary material confirms previously reported suppositions,⁽⁶⁾ drawn from the available data, that GeTe was lost during processing. As a result, the lattice conductivity was higher than calculated.
- 2) Electron microprobe examination disclosed reaction products of $\text{Ge}_x\text{Te}_y\text{Mo}_z$ and Ge_xMo_y at the lead-germanium telluride/molybdenum conductor ring interface.
- 3) Electrical probing of the n-type washer shows a significant portion of the ternary ANX material has been significantly altered with respect to its initial electrical properties.
- 4) The initial n-type composition will have to be altered to increase the free carrier concentration of material which retains the correct 5% germanium telluride concentration.

d. Inner Cladding Joint Qualification

An order was placed for six Ta-10W/316 SS tandem extruded joints to provide specimens for this task. Delivery is scheduled for the first week in October.

The equipment previously used for ultrasonic evaluation of the joint bond region has been reactivated, and preliminary checkout work was performed to ensure proper functioning of both equipment and inspection personnel. A need for some fixturing modifications was indicated during the checkout work, and these are currently being implemented. As a backup to ultrasonic evaluation, should we decide that the latter is an inadequate technique, the possibility of substituting an eddy-current test is being investigated.

Vacuum furnace and cycle control equipment for performing thermal cycling tests and 100-hour isothermal annealing tests are being located and scheduled for use on this task.

A study was initiated of the available information and data from previous and in-progress programs which will permit making realistic estimates of the intermetallic bond region growth rate at temperatures in the vicinity of 1200° F. These estimates will be used to select three temperatures at which specimens will be annealed and actual growth rates measured.

e. Diffusion Barriers

The delivery date for the electron-beam vapor deposition system, set at mid-August when the purchase order was placed, has been moved back to the end of September by the vendor. The starting point for this task will thus be delayed by about 6 weeks.

High purity tungsten vapor source material has been procured, and substrate holding fixtures for thermoelectric washer coating were designed and fabricated (mica holding fixtures are being delivered with the deposition system).

REFERENCES

1. I. J. Loeffler, S. Lieblein, and N. Clough, "Meteoroid Protection for Space Radiators," ARS Space Power Systems Conference, Santa Monica, California, September 25-28, 1962
2. N. Clough and S. Lieblein, "Significance of Photographic Meteor Data in the Design of Meteoroid Protection for Large Space Vehicles," NASA-TN D-2958, August 1965
3. N. Clough, S. Lieblein, and A. R. McMillan, "Crater Characteristics of 11 Metal Alloys under Hyper-Velocity Impact Including Effects of Projectile Density and Target Temperature," NASA-TN D-5135, April 1969
4. "Improved Compatible Materials for T/E Power Generation," NY00-3886-4, August 1969
5. I. Kudman, Private meeting, RCA Laboratories, Princeton, N. J., February 11, 1970
6. "SNAP Reactor Program Progress Report, May-July 1971," AI-AEC-13011, September 15, 1971