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SNAP REACTOR PROGRAMS
PROGRESS REPORT
AUGUST-OCTOBER 1972

AEC Research and Development Report

20706

Atomics International
North American Rockwell

P.O. Box 309
Canoga Park, California 91304

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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-Thermoelectric 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) 5-year life
- 2) 5-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, average
- 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 of the 5-kwe ZrH-Thermoelectric system and associated major components was completed during the quarter. Operating at a 1135° F NaK outlet temperature at beginning of life, the 85-element reactor produces 91 kwt; while, at end of life and 1200° F, it produces 99.4 kwt. Reactor outlet temperature is adjusted over life to constantly produce 5 kwe to the electrical bus. Overall efficiency of the system is 6.59 and 6.05% at BOL and

EOL respectively. The system requires a cylindrical-conical radiator of 273 ft² to reject the cycle waste heat, and weighs ~1867 lb. Sixteen thermoelectric power modules and three pump modules are used in the thermal-to-electrical power conversion.

A reactor inlet plenum design, consisting of diffusion vanes and a horizontal plenum baffle which provided considerably less than $\pm 5\%$ flow variation, was selected as the reference approach. Conceptual designs of the sliding-segment control reflector system, electric stepper motors, reactor vessel, core, and associated instrumentation were also completed. A multiple insertion rate startup of the reactor appears to offer the best means of limiting peak power and temperature transients in the core.

Lockalloy was selected as the reference fin material for the radiator, to achieve a better thermal expansion match with the stainless steel tubing to which it must be bonded. The expansion mismatch between aluminum fins and stainless steel tubing originally considered was large enough to result in severe design problems. The load-carrying capability of the Lockalloy is superior to aluminum, and the panel strength of the Lockalloy radiator gives a design margin of 1.37 over the anticipated dynamic launch loads.

Testing of the dual-throat development pump was initiated, and performance of the primary throat was ~20% higher, and the secondary 5% higher, than the original design points. The lower pressure developed in the secondary throat is the result of the MMF generated by the bus current reacting with the permanent magnet field. The four simulated pump throat joints have undergone 12 thermal cycles from elevated to room temperature, with no evidence of change in electrical resistance.

The nested-formed bellows was selected over welded bellows for use in the volume accumulator units, in order to minimize cost and maximize reliability. Metallographic and radiographic examination of the first two welded bellows capsules indicated weld inclusions, voids, and cracks in the circumferential and end welds.

During the quarter, eight refractory-metal-clad power modules were tested. All power modules continued to operate with power output degradation rates ranging from 1.7 to 7.7%/10,000 hr. The two TEM-14A pump modules remained

on test at nominal average temperature of $\overline{T}_H = 1125^\circ\text{F}$ / $\overline{T}_C = 570^\circ\text{F}$, and power degradation remains in the 3.1 to 4.8%/10,000 hr range.

II. REACTOR-SHIELD

A. REACTOR ENGINEERING

1. Introduction and Summary

The conceptual design of the 5-kwe reactor was completed during the quarter. The thermal and nuclear performance of the conceptual configuration was evaluated, with emphasis on quantifying performance uncertainties and establishing operating margins. The current conceptual design has a total nominal cold, unpoisoned excess reactivity of \$7.74. This exceeds the expected requirements by \$1.26. The fuel element structural margins were quantified by the development and use of an extensive force balance treatment of the entire 85-element core.

The thermal environment of each element was determined as a function of flow channel geometry, coolant mixing, etc., and iteratively coupled with a force balance routine to determine interelement contact stresses. Numerous initial configurations, representing both random and pessimistic arrangements, were evaluated.

A significant milestone, accomplished during the quarter, was the completion of inlet plenum hydraulic testing and selection of a reference inlet plenum design. Earlier tests showed the need for some method of flow control to achieve the objective of less than $\pm 5\%$ coolant pressure variation at the inlet grid plate.

A configuration of diffuser vanes and a horizontal plenum divider (baffle) evolved, which was determined to provide considerably less than the $\pm 5\%$ variation.

By the end of the quarter, the conceptual mechanical designs of the sliding-segment control reflector system, electric stepper motors, reactor vessel, core, and associated instrumentation were completed, subjected to formal design review, and approved.

2. Nuclear Analysis

The nuclear analysis during the quarter was directed toward an evaluation of reactivity margins, uncertainties in reactivity requirements, shim methods and requirements, and temperature coefficients.

The reactor core is designed to have sufficient reactivity for 5-years continuous operation at 110 kw and 1200° F outlet temperature, with an end-of-life (EOL) margin of \$0.50. It is expected that the reactor will be operated at lower power levels and outlet temperatures. The nuclear characteristics and reactivity requirements for the "design to" conditions and the expected nominal operating conditions are shown in Tables 1 and 2. An additional EOL reactivity margin of \$0.76 is obtained by operating the reactor at the reduced power and outlet temperature.

The estimated uncertainty in the nominal 5-year reactivity requirements are shown in Table 3. The combined uncertainty of \$0.92 was obtained by adding the uncertainties in uranium burnup, fission products, and prepoison burnout, and then taking the root sum square of this total with the remaining uncertainties.

Because of the uncertainty in predicting reactivity, provision for shimmiing reactivity is required. The method of shimmiing, in the past, has been the addition or removal of beryllium plates on the control drums. For the 5-kwe reactor, which does not have control drums, different shimmiing methods are required. Two which are being considered are shown in Figure 1. To increase reactivity, beryllium will be added to the upper fixed position of the reflector. To decrease reactivity, the gap between the reflector and core vessel will be increased by placing shims beneath the pads which contact the core vessel when the reflector halves are brought together. The flexibility obtained by providing $\pm \$2$ reactivity shim capability is shown in Table 4. Only in the case of control worth being \$2 low is there insufficient reactivity for 5-years operation.

Temperature coefficients of reactivity were evaluated over the 5-year life. The results are shown in Figure 2 and Table 5. The coefficients become more negative with time, due to the burnout of the gadolinium prepoison. This effect is partially offset by the burnin of samarium fission product.

3. Thermal-Hydraulic-Stress Analysis

Prior to this quarter, cladding stress analysis had been conducted on a "mirror image" two-element basis, using element temperature distributions derived from the thermal analysis code. In order to attain more accurate temperatures and stresses, a force balance, as described last quarter, was

TABLE 1
5-kwe REACTOR NUCLEAR CHARACTERISTICS

	Design To	Nominal
Power (kwt)	110	94
Average Outlet Temperature (° F)	1200	1150
Cold, Unpoisoned Reactivity (\$)	7.74	7.74
Prepoison Loading (\$)	3.00	3.00
Recoverable Losses (\$)	2.50	2.36
Hot, Poisoned Reactivity (\$)	2.24	2.38
Nonrecoverable Losses (\$)	4.74	4.12
Prepoison Burnout (\$)	2.39	2.24
EOL Reactivity Margin (\$)	0.50	1.26
Control Worth (\$)	7.74	7.74
BOL Shutdown Margin (\$)	3.00	3.00

TABLE 2
5-kwe REACTOR REACTIVITY REQUIREMENTS

	Design To	Nominal
Temperature Defect (\$)	2.18	2.09
Power Defect (\$)	0.04	0.03
Xe ¹³⁵ (\$)	0.18	0.15
Hydrogen Redistribution (\$)	<u>0.10</u>	<u>0.09</u>
Total Recoverable Losses (\$)	2.50	2.36
U ²³⁵ Burnout (\$)	0.82	0.69
U ²³⁶ Burnin (\$)	0.31	0.26
U ²³⁸ Burnout (\$)	-0.02	-0.02
Fission Products (\$)	0.92	0.78
Sm ¹⁴⁹ Burnin (\$)	0.94	0.90
Hydrogen Loss (\$)	1.16	0.75
Residual Prepoison (\$)	<u>0.61</u>	<u>0.76</u>
Total Nonrecoverable Losses (\$)	<u>4.74</u>	<u>4.12</u>
Total Requirements (\$)	7.24	6.48

TABLE 3
REACTIVITY LIFETIME UNCERTAINTIES

Item	Value (\$)	Uncertainty	
		(%)	(\$)
Temperature Defect	2.09	20	0.42
Power Defect	0.03	20	0.01
Xe ¹³⁵	0.15	10	0.02
Hydrogen Redistribution	<u>0.09</u>	50	0.05
Total Recoverable	2.36		
Burnup and Fission Products	1.71	15	0.26
Sm ¹⁴⁹ Burnin	0.90	15	0.14
Hydrogen Loss	0.75	50	0.38
Prepoison at EOL	<u>0.76</u>	20	0.45
Total Nonrecoverable	<u>4.12</u>		—
Total Requirements	6.48		0.92

Figure 1. Shim Methods

TABLE 4
SHIM FLEXIBILITY

Postulated Occurrence	Compensatory Action Taken	Margins (\$)	
		Shutdown	EOL
Values as Predicted	None	3.00	1.26
Hot BOL ρ \$2 Low	None	5.00	-0.74
	Add 1.05 in. Be to Stationary Reflector	3.00	1.26
Hot BOL ρ \$2 High	None	1.00	3.26
	Increase Reflector Gap 0.13 in.	2.78	1.48
Control Worth \$2 Low	None	1.00	1.26
	Increase Reflector Gap 0.13 in.	2.78	-0.52
Control Worth \$2 High	None	5.00	1.26
ρ Swing \$1 Low	None	3.00	2.26
ρ Swing \$1 High	None	3.00	0.26

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Figure 2. Time Behavior of 5-kwe Reactor Partial Coefficients

*RAPID TRANSIENT - USED FOR SAFETY ANALYSIS

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TABLE 5
COMPONENTS OF COEFFICIENTS

	BOL	EOL
U^{238} Doppler	-0.016	-0.016
Zr Doppler	-0.011	-0.011
Thermal Spectrum Shift	0	-0.04
Axial Expansion	<u>-0.043</u>	<u>-0.043</u>
Total Fuel Coefficient (Static)	-0.07	-0.11
Inlet	-0.05	-0.05
Outlet	0	0
Reflector Heating	<u>-0.03</u>	<u>-0.03</u>
Total Isothermal	-0.15	-0.19

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Figure 3. Stress Scoping Study Safety Margin Summary

incorporated into the thermal code and is now operational. The code uses an iterative procedure to account for deflections due to manufacturing tolerances, thermal gradients across the element, and loads imposed by neighboring elements. Many options for positioning fuel rod and manufacturing bow are available, permitting study of either random or forced orientation.

The code assumes a design, such that element contact is confined to a specific axial location. Manufacturing bow is assumed sinusoidal, and load bow is computed as a simply supported beam. The core design considers 20-mil cladding-cladding, and 10-mil fin-cladding, spacing, to allow some unimpeded thermal movement. The neutral elements are pinned at both ends for core stability, and the finned elements have a 10-mil radial hot-end looseness to reduce stresses and simplify assembly. While a firm tolerance envelope for finished elements has not yet been set, S8DR experience indicates a radial runout of 6 mils or less is attainable, with the maximum bow confined to the middle third of the element.

Using these variables, a stress scoping study was conducted. Half of the runs made considered completely random variation of parameters. The other half forced parameters, in an attempt to determine maximum stresses. The runs were made at the specified design conditions of 110 kwt and 100° F axial coolant ΔT , rather than the actual BOL or EOL operating conditions. The BOL power distribution was used for conservatism.

It was found, in the early runs, that edge neutral elements were most heavily stressed. This condition was due to the fact that these elements lack mixing on the outer periphery. The simple expedient of adding hot-end looseness to these elements halved the stresses.

In practically all cases run, less than half the elements are in contact; in some cases, as low as 15%. Figure 3 summarizes the stress results of the study. For seven different random geometry patterns, all elements ($7 \times 85 = 595$) had a margin >1 . For 10 forced-geometry studies, only 8 elements out of 850 had margins <1 , with the minimum being 0.85. One single-forced-cluster case was studied which used 23.5-, rather than 25-mil thick, cladding. This resulted in 17 elements with margins <1 , the minimum being 0.52. Considering the extremes of the forced cases, the core structural design is deemed conservative.

Figure 4. Reactor Inlet Plenum Hydraulic Test Section

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4. Core Hydraulic Tests

Flow behavior in the reactor inlet plenum was investigated with a hydraulic model. Hardware from a previous test was modified to provide a 1.13-scale model of the plenum. Figure 4 is a photograph of the reactor inlet plenum hydraulic test section. Prior to the testing, calculations were performed with a potential flow model to determine the ideal flow streamlines through the plenum. Similar calculations were made for the plenum with two horizontal baffles present, which separated the flow directly out of the two inlet lines from the grid plate. The ideal flow streamline locations were used to predict the proper location of spreading vanes for the inlet flows. The goal of the inlet plenum development was to achieve a pressure variation at the reactor inlet grid plate of $\leq \pm 0.008$ psi ($\pm 5\%$ of the reactor core ΔP).

Variation of grid plate surface pressure was determined with pressure taps in the grid plate surface which connected to a multiple-tube manometer board. Maximum water flow in the test loop was sufficient to give a velocity almost twice the NaK coolant velocity, but a Reynolds number of only $\sim 60\%$ of the NaK Reynolds number. Pressure data taken at different flows were converted to equivalent NaK pressures, and extrapolated to the proper Reynolds numbers. The Reynolds numbers used for the correlations were those for flow in the inlet lines, the design value being 1.85×10^5 .

Initial comparison of the maximum grid plate pressure variation (maximum ΔP) is made (Figure 5a) for the base case (empty plenum), the case with horizontal baffles, and the case with vanes at the inlets. (The lines drawn through the data points all have the same slope, which is the statistically averaged slope from data of the first 12 configurations.) Even though the baffle case shows some improvement, and the vane case even more improvement, over the base configuration, all three configurations result in considerably more than the allowable 0.016 psi at the prototype Reynolds number.

Figure 5b shows the additional improvement obtained by changing the convex (dished in) plenum head to a flat one. A more significant improvement was made by combining the flat head, vanes, and baffles in Configuration F. This configuration resulted in an acceptable pressure variation (Figure 5b). Although a flat plenum head gives acceptable results, it is structurally unsatisfactory.

a. Configurations A, B, and D

b. Configurations A, E, and F

c. Configurations A, F, and H

Figure 5. Comparison of Maximum Grid Plate Pressure Variations

Therefore, a concave (dished out) plenum head was combined with the vanes and baffles for Configuration H. The results from this configuration gave a ΔP which was only 50% of the allowed pressure variation (Figure 5c).

To improve structural stability, the two horizontal baffles were combined into one baffle with a center slot (Figure 6). The combination of this baffle with a concave head and spreading vanes contoured to fit close to the head was designated as Configuration I. The ΔP variation was greater than that allowed, and an active vortex was found in the plenum. Therefore, Configuration I was unacceptable (Figure 7a). The baffle was further slotted, to make it more comparable to Configuration H. This was Configuration K, and was acceptable (Figure 7b). Finally, the plenum height was increased by 0.15 in., or 17%, between the baffle plate and the grid plate. This resulted in a noticeable improvement (Figure 7b). Figure 7c shows the result of eliminating a 0.05-in. mismatch between the plenum head and the roof of the plenum inlet. The mismatch existed during the testing of Configurations I, J, K, and M. The corrected result, Configuration L, shows a slight improvement, and was the recommended configuration for the reactor plenum.

To provide an alternate design, a series of tests was conducted with a 1/2-in. thick orifice plate. The orifice plate upper surface was located at the same position in the plenum as the baffle plate upper surface had been. The plenum depth was increased to compensate for the increased orifice plate thickness over the baffle plate. Two orifice plate hole patterns were tested, one with 199 holes and one with 591 holes. Orifice holes were 1/8 in. in diameter, with inlets chamfered 45° to 1/32 in. The results are shown in Figure 7d as Configurations O (199 holes), and P (591 holes). In the 199-hole configuration, the fluid jets from the orifice holes did not dissipate in the 1 in. settling distance allowed. The results were quite unacceptable. In the 591-hole orifice case (Configuration P) the jets were further dissipated, but the results were just barely acceptable. Spreading vanes were installed on the inlet face of this last orifice plate. These results are shown in Figure 7d as Configuration R. Configuration R has been selected as the backup to the baffle vane configuration, Configuration L.

The effect of the various vane and baffle configurations on the test section ΔP was undetectable. The orifice plates, however, did increase the ΔP .

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Figure 6. Horizontal Baffle Plate, Quarter Section

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a. Configurations A, I, and H

b. Configurations I, K, and M

c. Configurations M and L

d. Configurations A, L, O, P, and R

Figure 7. Comparison of Maximum Grid Plate Pressure Variations

TABLE 6
INDIVIDUAL TEST RESULTS

Configu- ration	Configuration Description	Reynolds No. Power	Extrapolated NaK ΔP (psi)	NaK ΔP for 1.815 N _{Re} Power (psi)
A	Basic plenum	2.074	0.051	0.039
B	Horizontal baffles	2.051	0.043	0.034
C	Inlet vanes	1.958	0.044	0.039
D	Modified vanes	1.795	0.030	0.031
E	"D" with flat head	1.577	0.023	0.028
F	"B" with vanes and flat head	0.908	0.007	0.013
G	"F," but with convex head	1.833	0.019	0.019
H	"F," but with concave head	1.618	0.007	0.008
I	Concave head, vanes contoured to head, slotted baffle	2.418	0.033	0.020
J	"I" with full-width slot	1.672	0.012	0.013
K	"J" with outer vanes cut down	1.593	0.010	0.012
L	"K" with grid plate 0.15 in. lower, and head mismatch eliminated	1.581	0.006	0.008
M	"K" with grid plate 0.15 in. lower	1.733	0.009	0.009
O	Orifice plate with 199 holes	1.881	0.083	0.078
P	Orifice plate with 591 holes	1.990	0.019	0.016
R	"P" with vanes	2.008	0.015	0.012

In equivalent NaK ΔP , the 199-hole orifice added 0.32 psi, and the 591-hole orifice 0.03 psi, to the system ΔP .

The results of all 16 configurations tested are summarized in Table 6. The table shows the variation of the log ΔP vs log N_{Re} for each configuration. The extrapolated NaK grid plate pressure variation was determined for both the individual configuration correlation slopes and for the statistically average slope (1.815) of the first 12 configurations. By either comparison method, the recommended vane-baffle configuration, Configuration L, and the backup orifice plate configuration, Configuration R, are within the acceptable limits of ≤ 0.016 psi pressure variation at the grid plate.

5. Mechanical Design

The conceptual design layout drawings were completed and are included as Figures 8 through 14. These were reviewed and approved as the basis for initiation of preliminary design. The data which form the basis for the design are tabulated in Table 7.

Specific design features defined during the period are: (1) the baffles and vanes for flow distribution in the inlet plenum, (2) mechanical integration of the core instrumentation, and (3) the configuration and mechanical integration of the internal reflectors. In other respects, the design reflects a refinement of the design features defined in the preconceptual phase.

A structural evaluation of the reactor vessel and piping was completed. This study, using stress and/or strain criteria from the ASME Code for Nuclear Components (Section III), established configuration and component thicknesses required to withstand the NaK system pressure, the thermal stresses induced during startup and shutdown, and the mechanical load stresses induced during launch of a flight system. The acceleration of creep induced by neutron irradiation was predicted, using a radiation creep correlation established by AI for the design of the LMFBR. A vessel shield thickness of 0.045 in. (minimum) was established, to limit the radial elastic plus creep strain to 0.015 in. during 50,000 hr of operation at 1250° F with 32 psi internal pressure.

TABLE 7
REACTOR DESIGN DATA

Number of Elements	85
Spacing Pitch (in.)	1.025
Fuel Length (in.)	16
Element Spacing (mils)	20
Cladding Thickness (mils)	25
Cladding-Fuel Gap (mils)	6
Core Vessel ID (in.)	10.50
Cone Half Angle (°)	8.5
Midplane Diameter (in.)	15.492

6. Shield Design

a. Radioactive NaK Activity

The use of NaK as a reactor coolant requires consideration of coolant activation in the design of the system shielding. The most important activation reactions in NaK involve the capture of neutrons by Na^{23} (100% isotopic abundance) to produce Na^{24} , and by K^{41} (6.91% isotopic abundance in potassium) to produce K^{42} . Na^{24} decays by beta emission, with a half-life of 15.0 hr, and emits one 2.75-Mev and one 1.37-Mev gamma rays per disintegration. The K^{42} decays by beta emission, with a half-life of 12.4 hr, and emits a 1.52-Mev gamma ray in 18% of the disintegrations.

The radiation level from the radioactive NaK coolant is much lower than that from the reactor. However, in the present 5-kwe reactor system design, a large fraction of the radioactive (primary) NaK loop is not shielded. Thus, the dose rate contribution from the radioactive NaK loop below the shield could be a large fraction of the total gamma-ray dose rate at the dose plane.

The NaK activity was calculated, using the APC (buckling iteration ANISN) code. The Na^{24} and K^{42} activities were calculated to be 0.78 and 0.32 Ci/kwt respectively. Using these activities, the radiation level from the NaK was calculated to be 2.88 rad/hr at the dose plane. This value represents 13% of the total gamma-ray dose.

REVISIONS		
REV	DESCRIPTION	DATE
A	SEE REVISION RECORD	

NOTES: UNLESS OTHERWISE SPECIFIED ON THIS DRAWING

1. REACTOR TO MEET ALL REQUIREMENTS OF 55652N200001 REACTOR SYSTEM-5 KW REACTOR-T.E. SYSTEM.

2. REFERENCE DOCUMENTS:

- A. RTI-652-240-002 NUC PERF ANAL.
- B. RTI-652-240-001 EN POWER DISTR.
- C. RTI-652-240-012 THERM PERF ANAL.
- D. RTI-652-240-009 CORE VSL & PIPING STRUCT ANAL.
- E. RTE-652-360-001 HEAT X FER TEST
- F. R-N652200030 RA ASS'Y SEQUENCE

Figure 8. Conceptual Reactor Layout (Dwg. R-N652200001)

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#040101755

SECURITY CLASSIFICATION
R-N652200001

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Figure 9. Conceptual Core Layout
(Dwg. R-N652210001)

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Figure 11. Conceptual Internal Reflector Layout (Dwg. R-N652210002)

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Figure 13. Preliminary Reflector Drive Actuator Layout (Dwg. R-N652220095)
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Figure 14. Conceptual Test Backup Shutdown Relatch Mechanism Layout (Dwg. R-N65220033)

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b. Selection of Gamma-Ray Shield Material

The weight of the gamma-ray shield represents a significant fraction of the total shield weight. Thus, a careful selection of a gamma-ray shield material may result in a large weight saving. Since the gamma-ray shield is placed immediately below the reactor core, the secondary gamma-ray generation in the shield due to neutrons is a very important factor.

Various high-density shield materials were considered: U-8Mo, Ta-10W, tungsten alloy (95% W), lead, and borated stainless steel. The Ta-10W was rejected due to excessive weight, and lead was rejected due to its very low melting point (620° F). The U-8Mo shield resulted in a low shield weight; however, it was rejected due to its higher internal heating (due to fast fission), and concern about metallurgical instability at high temperatures.

One-dimensional transport calculations were performed to compare tungsten alloy, stainless steel, borated steel (with natural and B^{10} enriched B_4C), and tungsten alloy with a thin sheet of borated steel between it and the LiH shield. The relative shield weights are presented in Table 8. Further 2-D perturbation theory, using DOT code calculations, were performed to shape the gamma-ray shield by varying the thickness and radius. Relative weights are presented in Table 8. Since the shield weight using borated steel is less than that using tungsten alloy, it has been selected as the reference shield material, with tungsten alloy as an alternate.

TABLE 8
GAMMA-RAY SHIELD MATERIALS COMPARISON

Material	Relative Shield Weight	
	1-D Analysis	2-D Analysis with Shaping
W Alloy	1.00	1.00
SS	1.02	
SS + 2 wt % B C	0.72	0.88
SS + 2 wt % $B_4^{10}C$	0.70	
W Alloy + SS + 2 wt % $B_4^{10}C$	0.75	

TABLE 9
HASTELLOY X DEVELOPMENTAL ELEMENT
PERMEATION DATA

Element No.	Element Type	Permeation Rate [cc(STP)/hr at 1300° F]
X-93	Neutral	0.13
-94	Neutral	0.14
-95	Neutral	0.15
-96	Neutral	0.18
-97	Neutral	0.17
-98	Neutral	0.12
X-63	Finned	0.17
-64*	Finned	0.14
-65*	Finned	0.11
-66*	Finned	0.10
-67	Finned	0.11
-68*	Finned	0.15
-69*	Finned	0.18
-70*	Finned	0.11
-71	Finned	0.18
X-72*	Neutral	0.09
-74*	Neutral	0.09
-75*	Neutral	0.14
-76*	Neutral	0.10
-77*	Neutral	0.11
-78*	Neutral	0.15
-79	Neutral	0.09
-80	Neutral	0.09

*Selected for thermal aging test program. For these particular elements, the barrier performance index (A_D) is equivalent to the measured leak rate at 1300° F.

B. FUEL DEVELOPMENT

1. Fuel Element Assembly and Test

Three groups of 0.540-in. ID by 28-mil wall developmental fuel elements were permeation tested, and selected elements began a 20,000-hr thermal aging test program with periodic thermal transient interrupts. The permeation testing was conducted, using the modified procedures (described in AI-AEC-13039) designed to prevent cladding oxidation effects. All elements contained segmented fuel with 1.66 H/Zr and 0.1 atm He, and permeation data are tabulated in Table 9.

As a continuation of the permeation test upgrading effort, the standard leak systems and the McLeod gauge used for calibration of standards were thoroughly checked out, recalibrated, and the data statistically analyzed for correlation coefficients and reproducibility. This will be a continuing effort, particularly in light of the precision and accuracy required to obtain accurate data in the low range of leak rates currently being measured.

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 He backfill. Permeation data are tabulated in Table 10.

Modification of the fabrication and inspection tooling to accommodate the interim 0.651-in. ID cladding was completed. Several groups of the 0.651-in. ID cladding, both neutral and finned, were processed through the coating and welding operations. EB weld parameters were established for the coated butt weld of this configuration.

Three lots of 0.651-in. ID tubing were hobbled, to check out the modified tooling and to compare the performance of a carbide hob cutter against the previously used tool steel cutter. Table 11 summarizes the hobbing data obtained to date. Groups 1, 2, and 3 (previously reported in AI-AEC-13032) reflect the 0.540-in. ID cladding hobbled with a tool steel cutter. Group 4 represents two processing lots of 0.651-in. ID cladding run consecutively using the new carbide cutter. It should be noted that the 2 σ wall variation for the 21 tubes in Group 4 is essentially the same as for smaller quantities processed with the

TABLE 10
HASTELLOY X DEVELOPMENTAL ELEMENT PERMEATION DATA
DURING THERMAL TESTING

Element No.	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	5000-hr Aging
X-7	0.13	-	0.16	0.25	0.40	0.26	0.24	0.27
X-8	0.17	-	0.22	0.23	0.26	0.31	0.31	0.29
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	0.22	
X-17	0.20	0.19	0.24	0.20	0.29	0.34	0.32	
X-19	0.25	0.19	0.21	0.19	0.25	0.40	0.33	
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 at 1300° F.

TABLE 11
HOBBED HASTELLOY X CLADDING

Group	Tubes	Starting Wall		Hobbed Wall	
		Thickness (in.)	2σ	Thickness (in.)	2σ
1	6	0.0507	0.001	0.0282	0.0014
2	4	-	-	0.0268	0.0016
3	9	-	-	0.0293	0.0012
4	21	0.0635	0.0008	0.0289	0.0014

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Figure 15. Hob Tool Wear Study (Hastelloy X tubing)

tool steel cutter. The results of the tool wear comparison between the tool steel and carbide hobs is shown graphically in Figure 15. The third processing lot of 0.651-in. ID cladding was hobbled with the objective of achieving an average hobbled wall thickness closer to the desired nominal of 0.028 in. Data for this group are not yet available.

Fabrication of the test equipment and instrumentation for conducting fuel element cyclic load tests was completed, and is in the final checkout phase. This apparatus will permit the cyclic application of lateral side loads to a fuel element under an isothermal temperature profile. Hydrogen permeation rates will be measured continuously during the test, as well as applied loads, load relaxation, and element deflection.

2. Burnable Poison

Thermal aging was continued of fuel elements containing burnable-poison-coated fuel rods. Coating of fuel rods was by vapor deposition, in all cases. The status of the tests is shown in Table 12.

TABLE 12
BURNABLE POISON COMPATIBILITY TEST STATUS

Element Number	Accrued Time (hr)	Target Time (hr)	Temperature (° F)	Fuel Rod Coatings
9194	15,600	10,000+	1300	Gd, Y(Eu, Sm)
6-3	6,150	5,000	1400	Er, Gd
6-4	6,150	10,000	1400	Er, Gd
X-24	3,430	10,000	1300	Gd, Y
X-29	3,430	10,000	Gradient 1000 to 1300	Gd, Y
X-31	3,430	5,000	1400	Er, Gd, Y
X-32	3,430	10,000	Gradient 1000 to 1300	Er, Gd, Y
X-33	3,430	10,000	1300	Er, Gd, Y

Procedures and calibration curves were established for x-ray fluorescence (XRF) measurement of Gd_2O_3 coatings on fuel slugs. Solutions of gadolinium in 6N nitric acid, range 0 to 1.4 mg Gd_2O_3 /ml, were prepared as the calibration standards. XRF analyses of the standards produced a linear function over the range, as shown in Figure 16. The data, which consist of three 40-sec counts per standard, were fit by least squares to derive the following equation:

$$\text{Gd}_2\text{O}_3(\text{mg/ml}) = 0.02422 + 8.23176 \times 10^5 (\text{XRF counts} - \text{background}) \quad \dots (1)$$

XRF measurements were then made on Gd_2O_3 -coated fuel rods. The specimens were 0.529-in. diameter rods, vapor coated with a range of Gd_2O_3 thicknesses. XRF analyses were performed at four points, 90° displaced, about the circumference of each rod. The Gd_2O_3 coating was then leached off with 6N nitric acid. The Gd_2O_3 content was then determined, using the standards calibration equation given (Equation 1). This weight was divided by the originally coated area to convert the data to mg/cm^2 on the coated fuel rod. The data are shown in Table 13. A least squares fit of the data results in the following equation, which is applicable directly to 0.529-in. diameter fuel:

$$\text{Gd}_2\text{O}_3(\text{mg/cm}^2) = -0.13726 + 5.3834 \times 10^{-4} (\text{XRF counts} - \text{background}) \quad \dots (2)$$

A plot of this equation and the data points is shown in Figure 17, which illustrates the linearity and reproducibility of this analytical method. The correlation allows direct, nondestructive determination of Gd_2O_3 concentration on a 0.529-in. diameter fuel rod.

3. Cladding Evaluation

Stress-rupture testing of 8 Hastelloy X tubing samples in 1300° F flowing NaK was completed. Rupture-life and ductility data are shown in Figure 18. Evaluation of the data indicates no significant difference in strength between "NaK" and "helium" samples. Some loss in ductility appears to have resulted from the NaK exposure.

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Figure 16. X-Ray Fluorescence Readings for
Standard Gd_2O_3 Solutions

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TABLE 13

COMPARISON OF XRF READINGS FOR FUEL ROD SURFACES
AND FOR ROD SURFACE LEACH LIQUORS

Rod Designation	XRF Signal Rod Surface (counts/40 sec)	Rod Coated Surface Area (cm ²)	XRF Signal Leach Liquor (10 ml) (counts/40 sec)	Leach Liquor (mg Gd ₂ O ₃ /ml)	Fuel Rod Surface Concentration (mg Gd ₂ O ₃ /cm ²)
B-1-B	920	9.77	3943	0.349	0.357
B-4-A	1045	9.96	4839	0.423	0.425
A-4-B	1165	9.92	5638	0.488	0.492
A-4-B	1330	9.85	6469	0.557	0.565
B-1-A	1466	9.87	7516	0.643	0.651

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Figure 17. X-Ray Fluorescence Reading (Fuel rod surface) Calibration Curve for 0.529-in. Diameter Fuel Rod

Figure 18. Stress Rupture of Hastelloy X Tubing at 1300° F in Flowing NaK and Helium

Figure 19. Hastelloy X 1200° F Tensile Test Data as Function of Heat Treatment and Aging

Hastelloy X tubing samples from Heat E9504 completed 10,000-hr aging at 1200° F, and were tensile tested at 1200° F. This tubing was in two heat treat conditions: (1) simulated process (SP), which includes the coating process environment and time-temperature cycles, and (2) simulated thermal treatment (STT), which is the coating process time-temperature cycles but in neutral environment. The data obtained from this tubing are shown in Figure 19, together with previously reported data.

Tensile tests have also been performed at 1200° F on Hastelloy X weld samples in the as-processed and aged conditions. These samples consist of 20-mil thick SCB-coated tubes, joined together with an electron beam weld. The data obtained from these samples are given in Table 14.

TABLE 14
1200° F TENSILE TESTS ON HASTELLOY X TUBING SAMPLES
WITH AN EB WELDMENT

Sample Number	Sample Condition	0.2% Yield Strength (ksi)	Ultimate Tensile Strength (ksi)	Elongation (% in 2 in.)
13-14	SCB Coated	40	82	27
15-16	SCB Coated	39	78	20
17-18	SCB Coated	39	79	25
19-20	SCB Coated	53	98	12
21-22	Aged 1000 hr at 1200° F	54	92	19
1-2	SCB Coated	44	83	31
3-4	Aged 1000 hr	48	85	27
5-6	at 1300° F	46	85	23

Two samples of as-processed Hastelloy X tubing have been removed from the NaK corrosion loop — Sample No. 40 after 500 hr, and No. 39 after 980 hr. This loop is operating at 1400° F with a 90° F ΔT , and a NaK velocity in the

sample chamber of 1.9 fps. No significant weight loss or change in wall thickness could be measured on these samples. Metallography performed on these samples is presented in Figure 20. The visual appearance of the "depleted zone" indicated in this figure was produced by electrolytically etching in 10% oxalic acid. X-ray diffraction and fluorescence performed on these samples indicate that no phase change has occurred, and that the surface was depleted of roughly 50% Cr and Mo. Electron microprobe analyses will also be performed on these samples, to define the depletion gradient.

The purchase order for drawing the 5-kwe reactor cladding tubing requirements was placed. Approximately 344 ft of 2-1/8 in. diameter Hastelloy X-280 bar were gun drilled to form tube hollows, inspected, accepted, and delivered to the tube drawing vendor. The estimated tubing yields from this material are 1350 ft of 0.955-in. ID by 75-mil wall, and 1150 ft of 0.955-in. ID by 25-mil wall. These quantities will be sufficient for cladding characterization tests, process development, and fabrication of two core loadings of fuel elements.

C. REACTOR-SHIELD COMPONENT DEVELOPMENT

1. Reflector Drive System

a. Drive Component Tests

The fabrication and assembly of the test fixtures for the evaluation of the dry film lubricant on the ball tracks of the ball screw assembly were completed. A commercial ball screw assembly with sodium-silicate-bonded MoS₂ applied to the surface was assembled into the fixture, and the preliminary checkout and thermal mapping are being conducted. Frictional characteristics under thermal-vacuum conditions will be measured. The sputter coating of MoS₂ on the surface of the second commercial ball screw assembly is being completed. Similar tests will be conducted on this unit, to determine the comparative performance between the two methods of applying the MoS₂ lubricant.

The component fabrication of the developmental ball screw assembly, designed to meet the requirements of the prototype sliding reflector drive system, was initiated. The mounting configuration of these units will permit them to be installed and tested in the same fixture now being used for the previously described commercial units.

9745-4

a. Before Test

DEPLETED ZONE

9745-3

b. After 500 hr

1 mil

9745-2

c. After 980 hr

Figure 20. Exposure of Hastelloy X to 1400° F Flowing NaK (90° F Δ T, 1.9 fps flow rate, as-processed material)

b. Drive System Tests

A test assembly, to conduct performance evaluation of the sliding reflector control and drive system, is being designed. This test assembly includes a fixture which simulates the main beryllium reflector half, to which are mounted prototype components of the reflector drive system. These components, designed to the requirements of the conceptual design, include the anodized beryllium reflector sector, ball screw assembly, bearing assemblies, bearing support, drive coupling, drive actuator, sector anti-rotation device, and the ground test sector anti-gravity springs. Individual heater controls are provided to maintain the expected operating temperature of the control sector, with the ball screw and its guide bearing, ball screw support bearing, and the drive actuator. The test assembly will be oriented in the reactor system ground test attitude during the thermal vacuum performance mapping and endurance testing.

The detail design of the test fixture and of the drive components are in progress.

c. Reflector Materials

Investigations are being conducted to compare the performance of beryllium anodized by the currently available process to that anodized by the process originally used, but now unavailable, on past SNAP reactor reflectors. The basic parameters of the current process are very similar to those of the original.

Measurements made on newly anodized beryllium samples at room temperature, 800, and 1150°F produced emittance characteristics comparable to those of the S8DR reflector QA samples.

The thermal stability test at 1150°F and 10^{-6} torr have now completed over 2800 hr. The test had been initiated with 8 new and 4 S8DR anodized samples. After 1250 hr, 2 each of the new and S8DR samples were removed, and 7 additional new samples were added. Again at the end of 2750 hr, 4 of the new samples were removed, and the test restarted for long-term endurance. Room-temperature emittance measurements on the 1250- and 2750-hr samples indicated essentially no change from the initial values, for either the new or S8DR process.

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Figure 21. Control Drive Actuator

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 basic S8DS design, altered to upgrade the performance and fit within the allowable reactor envelope, is to be utilized.

Based upon the optimization studies previously reported (AI-AEC-13039), the selected actuator basic dimensions are 5-1/8 in. OD, and a length of 5.2 in., including the first stage of gear reduction, which is built into the actuator. The selected tooth width for the actuator is 0.085 in., and the resultant step size is 1.8°/pulse. Actuator configuration is shown in Figure 21. The expected actuator outputs are:

Dynamic Stepping Torque	107 oz-in. (Design Requirement = 100 oz-in.)
Scram Torque	80 oz-in. (Design Requirement = 70 oz-in.)
Brake Torque	225 oz-in. (Design Requirement = 180 oz-in.)

The flux densities, coil current densities, and stress design factors are comparable to those of the S8DR design. A design layout has been completed, and detail drawings are being made.

3. Cable Harness

Cables are required to supply power to the actuator and limit switch. To insure high electrical integrity and avoid outgassing of organic materials, ceramic-insulated metallic-sheathed cables were selected. The cable assemblies must not be affected adversely by preflight humidity, and must be capable of surviving a number of bending cycles during assembly and test, including those occurring during scram in the ground test reactor.

Tests to determine humidity effects on insulation resistance are being performed by exposure of samples to typical unairconditioned high-bay conditions, airconditioned low-bay conditions, and a protected outdoor environment. These samples were made from single-conductor 0.125-in. OD cable with unsealed ends, or ends sealed with either a commercial or an AI high-temperature cement. Results of the environmental exposure testing to date are shown in Table 15. Similar test samples were subjected to 40, 70, and 95% humidity at

TABLE 15
RESULTS OF ENVIRONMENTAL EXPOSURE TESTING

Sample No.	Sealant	Test Location	Test Time (days)	Minimum Insulation Resistance ($k\Omega$)
1	None	Low Bay	126	543
2	AI	Low Bay	126	250
3	Comm.	Low Bay	126	416
4	None	Low Bay	199	227
5	None	Low Bay	199	166
6	None	Low Bay	199	227
7	None	High Bay	126	257
8	AI	High Bay	126	208
9	Comm.	High Bay	126	192
10	None	High Bay	199	138
11	None	High Bay	199	166
12	None	High Bay	199	125
13	None	Outdoors *	126	6.2
14	AI	Outdoors	126	16.6
15	Comm.	Outdoors	126	119
16	None	Outdoors	199	83
17	None	Outdoors	199	86
18	None	Outdoors	199	73

*Under overhang

90° F, 40% humidity with thermal cycling between 50 and 170° F, and 70% humidity with thermal cycling between 50 and 170° F. The minimum insulation resistance measured for any sample under these conditions has exceeded 50,000 Ω . Sealing of the cable ends did not result in any significant improvement of insulation resistance.

Selected samples of cable, after humidity chamber exposure, were installed in a 10^{-5} torr vacuum chamber, and have been undergoing thermal cycle testing to 1100° F. On initial heatup, the insulation resistance on all

cable samples rose to the 100 k Ω range; and, after 30 thermal cycles, all samples are in the M Ω range. Thermal cycling is continuing.

The actuator and limit-switch circuitry, for which the cable harness is to be used, can tolerate insulation resistances down to 1000 Ω . On the basis of data obtained to date, it would appear that no special cable sealing techniques are necessary.

A cable bending and twisting fixture has been designed and built to verify the reflector half separation spring force required to insure backup scram capability when overcoming the cable harness flexing forces, and to establish adequate cable bending fatigue life. Checkout is now in progress.

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.

The power conversion system utilizes 16 PbTe power conversion modules to produce 5 kwe at 30 v dc. These thermoelectric modules are utilized in a system employing two coolant circulation subsystems. 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 that heat transferred across the thermoelectric modules to a space radiator. Circulation is provided in both loops by a dual-throat, dc conduction electromagnetic pump. This pump receives electric power from three compact thermoelectric modules.

The power system is designed to produce a constant power output at constant voltage over its design life of 5 years. The control system is based on sensing the electric current (equivalent to electric power) generated by the system.

2. System Design and Performance Characteristics

The power system temperatures have been limited to a nominal 1200° F. The system is designed to produce 5 kwe of power at 30 v dc at the end of 5 years, without the reactor temperature exceeding this temperature. To

compensate for degradation in the performance of the thermoelectric modules, the system is initially operated at a reduced temperature, with temperature driftup occurring through life 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 22 and 23, respectively. Nominal system performance characteristics and design features are presented in Table 16. The initial system weight estimates are presented in Table 17.

Since the power module degradation modes are diffusive, with an associated activation energy, data from TEM-X thermoelectric modules on test has been fitted to the equation:

$$\text{Degradation Fate} = A \exp(-Q/RT) \quad \dots (3)$$

where Q is the activation energy and A is the frequency factor. The results of this analysis are presented in Figure 24. The data band encloses all TEM-X data, after corrections for increased axial temperature gradients and overtemperature operation are performed.

The reference power module degradation rate (power and efficiency) is assumed to behave similarly (identical slope on Arrhenius plot), with reduced magnitude. Integration of this temperature-dependent degradation phenomenon into the system simulation model allows calculation of realistic time-dependent system performance, as shown in Figure 25.

More detailed analyses of system weight sensitivities, envelope sensitivities, component interactions, component designs, and system performance have resulted in several modifications to the system design point over the reporting period.

a. Conical Half-Angle Selection

Trade studies have shown that the conical-cylindrical system exhibits minimum weight characteristics at a conical half-angle of 7.0 to 7.5° (within the constraints of the radiator areas considered — 225 to 300 ft^2). The conceptual design point utilizes a conical half-angle of 8.5° , thus realizing a small weight penalty and a slight decrease in system envelope (height).

Figure 22. Primary Loop NaK Temperatures, Space

Figure 23. Secondary Loop NaK Temperatures, Space

TABLE 16
SYSTEM DESIGN POINT SUMMARY, SPACE ENVIRONMENT

Parameter	BOL		EOL
Electrical Power at 30 v dc (kw)			
Gross	5.13		5.13
Net at Mating Plane		5.0	
Reactor Thermal Power (kw)	91.0		99.4
Reactor Outlet Temperature (° F)	1135		1200
Flow Rate (lb/sec)			
Primary Loop	4.73		4.73
Secondary Loop	2.88		2.88
Pressure Drop (psi)			
Primary Loop	0.80		0.80
Secondary Loop	1.26		1.26
Converter Efficiency (%)	6.59		6.05
Radiator Area (ft ²)		273	
Base Diameter (ft)		6	
System Overall Height (ft)		23.4	
Radiator Cone Half-Angle (°)		8.5	
Number of Thermoelectric Modules			
Power		16	
Pump		3	
Total Weight (lb)		1867	

TABLE 17
FLIGHT SYSTEM WEIGHTS
(Wet weights where applicable)

System	Weight (lb)	
Reactor		497
Vessel	38	
Reflector and Drive Train	160	
Core	290	
Piping	9	
Radiation Shields		347
Gamma Ray	98	
Neutron (LiH)	195	
Neutron Casing	54	
Thermoelectric Power Modules		192
Pump Assembly		79
Pump Bodies	55	
Thermoelectric Modules	24	
Volume Accumulators (3)		68
Piping		144
Primary Loop	65	
Primary Loop Expansion Joints	6	
Secondary Loop	65	
Secondary Loop Expansion Joints	3	
Foil Insulation	5	
Radiator		495
Fins and Armor	197	
Tubes	133	
Structure	152	
Miscellaneous	13	
Electrical Wiring		45
Power Transmission	11	
Startup Pump Power	4	
Actuator	9	
Instrumentation	21	
Total		1867

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Figure 24. Power Module Degradation Model

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Figure 25. 5-kwe System Operation — Nominal Reference Degradation

b. Shield Design

The gamma shield material has been changed from tungsten to borated stainless steel. In addition, the shape of the gamma shield has been "tailored" to meet the conical-cylindrical system dose requirements at the mating plane. A significant weight savings is realized through these modifications.

Detailed neutron shield analysis is currently in progress; however, the shield size and internal structure have been increased, due to modifications of the system configuration.

c. Radiator Analysis

Further analysis of transient-induced stresses in the radiator fin has resulted in the utilization of a low-density, high-thermal-conductivity alloy as the fin and armor material (Lockalloy, an aluminum-beryllium alloy). Lockalloy exhibits a thermal expansion coefficient closely matching that of stainless steel (the radiator structural material). The resultant radiator weight change is included in the conceptual design weight summary.

d. Piping System

Analysis of primary and secondary system fill-and-drain procedures resulted in modifications to both piping systems (i. e. , relocation of manifolding). The resultant conceptual design piping system includes all modification necessary for fill-and-drain operations.

Detailed stress analysis of the piping system indicated that expansion joints should be incorporated in both the primary and secondary loop designs. A reference double-containment nested-formed bellows expansion joint has been designed for use in both loops. Resultant increases in piping system weight are included in the design summary.

e. Volume Accumulator Design

The increased expansion volume associated with the piping design dictates the use of other than a welded bellows unit (to minimize weld length). The conceptual design volume accumulators are designed with nested-formed bellows. The resultant volume accumulator size and weight increase is included in the conceptual design.

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Figure 26. Power Module Radial Dimension Trade Study

f. Power Module Design

Analyses were performed to evaluate the system weight sensitivity to the power module radial and axial dimensions.

System weight sensitivity to the module radial dimensions was studied for systems exhibiting minimum weight vs area characteristics. The results of this trade study are shown in Figure 26, where system weight is shown as a function of module radial dimensions.

As can be seen in Figure 26, a 50 to 60 lb weight savings can be realized through the use of 18 modules with dimensions of 0.450 in. ID and 1.110 in. OD (as compared to the reference system utilizing 16 modules with dimensions of 0.750 in. ID and 1.660 in. OD). However, this dramatic change in module design could result in unknown changes in module fabrication, performance, and degradation. In that the 5-kwe system should utilize current state-of-the-art fabrication procedures and test data, a major modification of the power module would seem outside the intent of the design. The conceptual design point utilizes 16 modules with TEM-X radial dimensions.

A system was also investigated which utilized power modules with an active circuit length of 20 in. (compared to the reference module, with 15 in.). This length modification caused only minor perturbations in the system thermal performance, in that the number of modules is decreased (from 16 to 12) to compensate for the length increase. A slight increase in module efficiency was realized, because of the reduced end losses (25% less "ends"). However, changes in shielding and piping which result from this modification tend to minimize the weight savings from the efficiency increase. Thus, no weight penalty or benefit exists for the axial length increase. The conceptual design point utilizes power modules with a 15 in. active circuit length.

3. Preliminary Margin Assessment

Design margin has been included at the component or subsystem level, and the following paragraphs summarize the various allocations.

a. Reactor

The reactor is designed for continuous 5-year operation at 110 kw, 1200° F outlet temperature, and 100° F core temperature rise. In that the system EOL

design point reactor power is <100 kwt (at 1200° F outlet temperature and 95° F core temperature rise), a margin has been incorporated into the reactor design. In addition, BOL system operation occurs at reduced reactor power levels and reduced outlet temperatures.

b. Piping System

The primary and secondary piping systems are designed to provide certain pressure drops at specified flow rates. All piping system pressure drop calculations are performed with "worst-case" piping performance parameters.

c. Pumping System

The electromagnetic NaK pumping system is designed to provide certain pressure heads in each throat at specified flow rates. For each loop, the pressure head includes a 15% design margin above the specified piping system pressure drop.

d. Radiator System

Margin is incorporated into the thermal design of the radiator system, in that "worst-case" performance parameters for the AI 93 emissivity coating are utilized in the radiator design calculations. The radiator performance is based on an emmissivity of 0.90 (the "nominal expected" is 0.91) and an absorptivity of 0.50 (the "nominal expected" is 0.40). The system is assumed to operate with "worst-case" environmental heat input in a maximum sun-shade orbit.

e. Volume Accumulators

The 5-kwe system incorporates three identical volume accumulators — two in the primary loop, and one in the secondary loop. The volume accumulator displacement is designed to the maximum of: (1) one-half the primary loop expansion volume, or (2) the total secondary loop expansion volume. The conceptual design volume accumulator displacement is one-half of the primary loop expansion volume.

The nominal system peak EOL outlet temperature is 1200° F. The outlet temperature scram setpoint is 1250° F. The primary expansion volume is based upon a volume-averaged primary loop temperature (at 1250° F outlet) plus a 50° F design margin. The resultant design exhibits an effective 100° F temperature margin for volume expansion corresponding to 81 in.³ The secondary

loop expansion volume requirement is based upon the peak average secondary loop temperature plus a design margin of 50° F. This peak average temperature occurs at a reactor outlet temperature of 1250° F. The calculated volume displaces ~70% of the design capacity of the volume accumulator. Thus, the secondary loop volume accumulator exhibits significant design margin.

f. "Attainable" System Performance

In that the pump system and radiator system design margins present additional usable performance, an inclusion of this addition can result in more benign system operation. This mode of operation will be defined as "attainable" performance.

Table 18 presents a comparison of system BOL and EOL operation in a space environment, for "required" and "attainable" component performance and constant system electrical power output. "Required" performance is defined as follows:

- 1) Pump pressure head 15% greater than the piping pressure drop
- 2) Radiator emissivity of 0.9 (lowest expected value)
- 3) Radiator absorptivity of 0.5 (highest expected value)
- 4) Environmental thermal input of 102 Btu/hr-ft² (worst-case orbit).

"Attainable" performance is defined as follows:

- 1) Pump pressure head equal the piping pressure drop
- 2) Radiator emissivity of 0.91 (nominal expected value)
- 3) Radiator absorptivity of 0.4 (nominal expected value)
- 4) Environmental thermal input of 75 Btu/hr-ft² (nominal orbit)

Thus, utilization of the inherent component performance results in lower temperature system operation and increased system life capability.

4. Piping System

The piping system is subjected to stresses caused by the launch accelerations and the differential thermal expansion, which occurs when the system temperature is raised to operating levels. The launch environment occurs with

TABLE 18
SYSTEM OPERATIONAL STATEPOINTS IN SPACE

	BOL		EOL	
	Description			
	"Required"	"Attainable"	"Required"	"Attainable"
<u>Reactor</u>				
Power Demand (kwt)	91.0	90.1	99.4	97.7
Outlet Temperature (° F)	1135	1121	1200	1181
Core Temperature Rise (° F)	87	84	95	91
<u>Power Modules</u>				
Hot Cladding (° F)	1080	1067	1140	1124
Cold Cladding (° F)	501	493	523	513
Efficiency (%)	6.59	6.65	6.05	6.14
Degradation (%/year)	-	-	1.25	1.25
Power In (kwt)	77.87	77.13	84.86	83.50
Power Out (kwe)	5.13	5.13	5.13	5.13
Voltage Out (v)	30.8	30.8	30.8	30.8
<u>Pump Modules</u>				
Hot Cladding (° F)	1080	1067	1140	1124
Cold Cladding (° F)	501	493	523	513
Efficiency (%)	3.20	3.23	2.90	2.94
Degradation (%/year)	-	-	1.75	1.75
Power In (kwt)	9.68	9.58	10.47	10.31
Power Out (kwe)	0.310	0.309	0.304	0.303
Current (amp)	1580	1574	1560	1556
<u>Pump-Piping</u>				
Primary Flow (lb/sec)	4.72	4.84	4.73	4.86
Primary Head (psi)	0.80	0.84	0.80	0.84
Secondary Flow (lb/sec)	2.88	3.00	2.88	3.00
Secondary Head (psi)	1.26	1.37	1.26	1.36
<u>Radiator</u>				
Power In (kwt)	84.8	83.9	92.9	91.3
Inlet Temperature (° F)	562	550	590	576
Average Temperature (° F)	489	481	512	501
Temperature Drop (° F)	131	124	144	136
<u>Thermal Losses</u>				
Piping Loss (kwt)	1.08	1.04	1.25	1.21
Shunt Losses (kwt)	2.38	2.32	2.75	2.65

the system relatively cold, and does not impose difficult problems with attachments and flexures. The expansion properties of the secondary loop and its supporting structure are similar, and the resulting stresses are relatively low. The primary loop, however, is subjected to large thermal gradients. The differential growth between the piping and the supporting structure must be accommodated by either high stresses or flexure of the piping.

A typical thermal cycle is shown in Figure 27. During startup, at Time 1, the piping has reached its maximum length, since it is at its operating temperature, the radiator is at operating temperature. However, the shield temperature is lagging. This represents the maximum differential growth in the primary loop, as shown in Figure 28. After the shield has stabilized at operating temperature (Time 2 in Figure 27), a lesser but prolonged differential exists, also shown on Figure 28. Finally, after shutdown, the loops have cooled but the shield has maintained its high temperature (Time 3 in Figure 27), resulting in a reversed, but lower-magnitude, differential.

A most direct routing of primary loop piping between the reactor and the thermoelectric manifold is shown on Figure 29. Indicated stresses at several points are shown, and are substantially over the allowable of 25,000 psi.

Several design alternatives were studied to correct the high stresses, including:

- 1) Spring mounting, or disengage piping from structure after launch
- 2) Use low-expansion material for pipes
- 3) Increase piping flexibility with additional loops and bends
- 4) Use bellows in piping to increase flexibility
- 5) Combination of preceding.

Each method has disadvantages. However, the one with the most advantages proved to be the one incorporating bellows in the piping.

Spring mounting required a very high rate spring to withstand the launch acceleration forces. Then, during thermal loading, the high forces generated were nearly as large as those forces generated in fixed mounting. The differentials generated between the primary and secondary loop cannot be accommodated by spring mounting.

Figure 27. Typical Thermal Cycle

LINE	EXPANSION (in.)		
	① END OF LOOP TRANSIENT (STARTUP)	② STEADY OPERATION	③ END OF LOOP TRANSIENT (SHUTDOWN)
RETURN	-0.499	-0.355	+0.144
SUPPLY	-0.589	-0.445	+0.144
PUMP INLET	-0.110	-0.110	0

Figure 28. Primary Loop Expansions

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Figure 29. Base Configuration (Minimum bends -- Type 316 stainless steel pipes)

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Figure 30. Type 316 Stainless Steel Pipe With Loops

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Figure 31. Type 316 Stainless Steel Pipe With Spirals

Post-launch disconnecting would require very high reliability for remote-disconnecting mechanisms. The forces generated between the primary and secondary loop in this approach could not be easily accommodated.

A number of low-expansion materials were evaluated. However, each proved unsatisfactory for the following reasons: (1) unavailable in tubes, (2) low ductility and/or low strength at 1200° F, (3) poor weld characteristics, or (4) insignificant improvement over Type 316 stainless steel.

The loop or bend approach was workable. However, it required a significant increase in the length between the reactor and the thermoelectric manifold. This meant increased pipe weight and complexity, increased NaK volume, increased volume accumulator size, increased shield weight, and stress levels not significantly lower than the allowable. Figure 30 shows the use of a loop with its associated stresses, and Figure 31 shows the use of spirals. In both cases, the end forces on the reactor and thermoelectric nozzles were low. However, stresses in the pipes remained close to the allowable.

Finally, the bellows approach, shown on Figure 32, made the greatest improvement in stresses without the associated effects on length and weight. Although the spring rate of 338 lb/in. and stress levels up to 20,000 psi are shown, the use of bellows offers design flexibility and a small increase in bellows length, and has a significant effect on reducing its spring rate and associated stresses.

5. System Hydraulics

The reference pump design characteristics are as follows:

Coolant Loop	Pump Design Requirements
Primary	0.92 psi Head at 4.73 lb/sec Coolant Flow Rate
Secondary	1.45 psi Head at 2.88 lb/sec Coolant Flow Rate

The piping systems are sized to provide hydraulic resistances of 0.8 psi (primary loop) and 1.25 psi (secondary loop) at the design flow rates of 4.73 lb/sec and 2.88 lb/sec, respectively, thereby allowing a 15% design pump head margin in both the primary and secondary loops.

6531-5328

Figure 32. Piping System Incorporating Bellows for Flexibility

The hydraulic resistance of each loop is comprised of the following:

- 1) Piping Friction Losses
- 2) Piping Bend Losses
- 3) Contraction-Expansion Losses
- 4) Tee Losses
- 5) Expansion Joint Unit Losses (Primary loop only).

Friction losses are calculated, assuming smooth tube conditions. Bend losses are based on an equivalent length of straight piping for the bends, as presented in Perry's "Chemical Engineers Handbook." Contraction-expansion losses are based on loss coefficients presented in Perry. Tee losses (manifolds) are based upon experimental loss coefficients measured by Baker and Michel at ORNL (Union Carbide Report No. K-998). Expansion joint unit losses are based upon the 1964 experimental data of AI.

The reactor ΔP is based upon the current projections of reactor ΔP presented in Section II-A. The thermoelectric pump and power module ΔP 's are calculated by assuming the spiral flow paths, produced by an internal mandrel with a helical projection machined on its OD and a helical wire wrap spaced on the OD of the modules, to be a series of 180° bends. The validity of the assumption has been demonstrated by agreement of calculated results with existing Westinghouse flow data on a 4-pack module configuration.

Flow maldistributions among the modules and radiator tubes (i. e., those multiple flow paths connected in parallel across common manifolds) are reduced by requiring the ΔP across the modules and radiator tubes be at least twice the sum of the inlet and outlet manifolds ΔP 's serving the modules and radiator tubes. This approach is, again, based upon the Westinghouse 4-pack module flow tests.

Assigned allowances on the calculated hydraulic losses are currently as follows:

TABLE 19
SYSTEM HYDRAULICS

	ΔP (psi)	Re
Primary Loop		
Reactor	0.21	2,600 (min.)
Modules		
Pump	0.16	43,000
Power	0.13	28,000
Heat Exchanger	0.07	210,000
Piping		
Manifolds	0.08	8,200 (min.)
Piping	0.19	47,000 (min.)
Margin	<u>0.12</u>	-
Total	0.80	-
Secondary Loop		
Radiator	0.19	11,000
Modules		
Pump	0.47	10,000
Power	0.21	31,000
Piping		
Manifolds	0.24	2,700 (min.)
Piping	0.44	25,000 (min.)
Margin	<u>0.17</u>	-
Total	1.25	-

Friction Losses	+10%
Bend Losses	+10%
Contraction-Expansion Losses	+20%
Tee Losses	+20%
Expansion Joint Unit Losses	+10%

In addition, an allowance of +10% is assigned to the reactor ΔP . No uncertainty is assigned to the pump - power module ΔP , since the mandrel swirler and wire wrap pitches can be tailored to meet the required ΔP across the modules.

The current hydraulics of the reference conceptual design piping systems are summarized in Table 19. The primary loop piping system weighs ~ 116 lb (wet), and contains $\sim 2.2 \text{ ft}^3$ of NaK. The secondary loop wet weight is ~ 73 lb, and contains $\sim 1.8 \text{ ft}^3$ of NaK. Flow maldistributions in the modules and radiator tubes are estimated to be $< 5\%$.

6. Control System

A functional design of the automatic control system has been prepared. This system provides for automatic startup and control of the reactor, meeting the objectives listed in Table 20.

Consideration of startup methods was concentrated on three approaches. These are: (1) the single reactivity insertion rate, (2) the single rate with hold above critical, and (3) the multiple insertion rate. These approaches are shown schematically in Figure 33.

The single-rate method is simplest, consisting only of a steady insertion until the power control range is reached. The system behavior for a typical insertion rate, as determined by a simulation study, is shown in Figure 34. A more complete summary of the results of the simulation studies of single-rate startups is listed in Table 21.

As shown in the table, the objective that the coolant temperature rise be $< 150^\circ \text{F}$ and the startup time be less than ~ 12 hr cannot be met by the single-rate startup. This is demonstrated graphically in Figure 35, where it is seen that the insertion rates for acceptable temperature rises and acceptable startup

TABLE 20
PRELIMINARY AUTOMATIC CONTROL SYSTEM OBJECTIVES

Startup

Limit coolant temperature rise through core to 150°F

Limit rate of temperature rise to $150^{\circ}\text{F}/\text{min}$

Accommodate $\pm 10\%$ reactivity uncertainty due to undetermined differences between ground test and flight reactors

Startup time less than ~ 12 hr

Startup accomplished by programmed insertion of the control segments

Operating Control

5-year life

Electrical power generation controlled to $\pm 2.5\%$

Shutdown

By elapsed time at 5 years

By ground command

6531-5329

Figure 33. Startup Approaches

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Figure 34. Startup Transient - Single Rate
(0.01 ϵ /sec 5% flow)

TABLE 21
SUMMARY OF SINGLE-RATE STARTUPS

Reactivity Rate (ϵ /sec)	0.05	0.025	0.01	0.005	0.001*
Time to Full Power (hr)	1.14	2.33	5.9	12.0	(60)
Initial Power Transient (% of full power)	217	112	47	29	8
Reactor Power Overshoot at Full Power (%)	25	12	5	3 [†]	3 [†]
Peak Coolant Temperature Rise (°F)	(560)	(400)	(250)	(182)	82
Peak Rate of Inlet Coolant Temperature Rise (°F/min.)	(1440)	(720)	(220)	94	13

*Extrapolated results

[†]Limited by 0.6 ϵ reactivity steps

() Indicates reason for rejection

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Figure 35. Startup With Single Reactivity Rate

TABLE 22
SUMMARY OF SINGLE-RATE WITH HOLD STARTUPS

Reactivity Rate (ϵ /sec)	0.025 in all cases			
Reactivity at Hold (ϵ)	5	10	15	25
Time to Full Power (hr)	3.33	2.84	2.67	2.67
Peak Coolant Temperature Rise ($^{\circ}$ F)	48	130	(215)	(400)
Peak Rate of Inlet Coolant Temperature Rise ($^{\circ}$ F/min.)	6	34	144	(720)

6531-5331

Figure 36. Startup With Single-Rate and Hold
Effect of Uncertainty in Criticality
(Reactivity rate = 0.025 ϵ /sec)

times do not overlap. Thus, the single-rate startup was determined to be unacceptable, under the established objectives.

The single-rate with hold above critical is a modification of the single-rate startup, in which reactivity addition is intentionally halted prior to the initial power transient, and then resumed to drive the reactor up to power. The simulation studies for this method are summarized in Table 22.

Taking 10¢ reactivity as the most appropriate hold point results in meeting the established requirements, except for accommodation of the $\pm 10\%$ reactivity uncertainties. The effect of this uncertainty on the peak coolant temperature rise is shown in Figure 36. Addition of this uncertainty leads to a reactivity at the hold of 20¢, which results in a temperature rise of $\sim 300^\circ\text{F}$. Subtraction of the uncertainty leaves the reactor critical at very low power during the hold, with the result that the startup transient is produced by reactivity addition during the second ramp. This also causes an unacceptably high coolant temperature rise. Therefore, this startup method was also rejected.

The multiple rate startup consists of a rapid rate to bring the reactor near critical, a slow rate through critical to provide a well behaved initial power rise, and then a moderate rate to drive the reactor to full power.

Simulation studies for several combinations of insertion rates are shown in Table 23.

In the fourth arrangement shown, control steps are limited to the slowest rate to provide adequate time for the system to respond. This method satisfies all of the established startup objectives. Details of this startup are shown in Table 24. Results of a simulation are shown in Figure 37. (In the simulation, the third insertion rate was used in the control phase, rather than switching to the final rates. With the current design, the power oscillations following the first dip at full power would be considerably reduced.)

Control of the reactor power is based on comparing the current generated by the thermoelectric power modules with reference points set above and below the current, corresponding to the desired power level. If this current is below the lower reference point, reactivity is inserted, at a maximum rate of one step in 128 sec, until the current is above this reference. Withdrawal of reactivity, if the current is above the upper reference, is similarly controlled.

TABLE 23
SUMMARY OF MULTIPLE-RATE STARTUPS

Reactivity Rate (ϕ /sec)				
1st	0.05	0.05	0.05	0.25
2nd	0.01	0.01	0.005	0.004
3rd	0.025	0.05	0.05	0.035
4th				0.004
Time to Full Power (hr)	2.57	1.82	2.40	2.89
Peak Coolant Temperature Rise ($^{\circ}$ F)	(250)	(250)	(189)	148
Peak Rate of Inlet Coolant Temperature Rise ($^{\circ}$ F/min.)	(220)	(220)	141	75
Reactor Power Overshoot at Full Power (%)	12	25	25	3

TABLE 24
REFERENCE STARTUP

	Rate (ϕ /sec)	Switch At	Time (sec)	Cumulative Insertion (ϕ)
Initial Rate	0.25	50 ϕ	200	50
Second Rate	0.004	74.4 ϕ Additional	6100	74.4
Third Rate	0.035	5 kwe	4100*	182.9
Final Rate	0.004		Balance of Life	
Total Startup Time			10,400 (2.89 hr)	

*Switching to final rate at attainment of 5 kwe does not change this result for nominal values, but reduces either the actual startup time or the power overshoot for off-nominal cases, compared to time-controlled switching.

72-O17-48-178A

Figure 37. Reference Startup

6531-5511

Figure 38. Flight Control System

The initial jump, or drop, in power following a step is $<1\%$. The long-term change is $\sim 0.5\%$. Thus, the reference points may be set even nearer to the desired current than the required $\pm 2.5\%$ without introducing oscillation due to the dead-band limits.

A block diagram of the flight control system is shown in Figure 38. The startup command enables a clock to begin pulse generation. The pulses are divided to establish time intervals for the various insertion rates. These insertion commands are passed to the actuator power switches at the rate selected by the step counter. Following attainment of full power, the startup circuit is disabled, and subsequent actuator commands are generated only as required by the current switches.

Provision is made for control of the reactor, including startup, by means of ground command. This mode of operation temporarily disables the automatic controller, but automatic operation may be restored at any time desired.

Because of the nearly linear reactivity worth of the control segment with position, automatic startup may be accomplished almost as well, even in the event of failure of one segment. Segments are stepped alternately, and so loss of one segment would increase the time required, but insertion rate switching points are determined cumulatively, so they will still occur at the intended reactivities. Insertion rates would be halved by loss of a segment, resulting in more benign power and thermal transients than provided by normal operation.

7. Factory-to-Test Sequence

The factory-to-test sequence is shown schematically in Figures 39 and 40. Figure 39 shows those operations to be performed in the assembly facility, Building 013, and Figure 40 shows those operations to be performed in the test facility, Building 059. Evolutionary changes to the factory-to-test sequence may occur, as the system test program and the system design become more defined.

The reflector assembly (actuators beryllium segments, drive screw sub-assembly, etc.) will be completed and electrically checked out in Building 013, and then the reflector assembly will be transferred to Building 062 for a hot thermal acceptance test under vacuum. After completion of the thermal acceptance test, the reflector assembly will be transferred to Building 059 for the dry

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Figure 39. System Assembly - Building 013

Figure 40. System Assembly - Building 059

critical testing. In a parallel operation, the power system, minus reactor - reflector assembly, will be assembled in Building 013. The power conversion system (pumps, thermoelectric modules, connecting piping, etc.) and the radiator assembly will be fabricated in semi-parallel operations, and then both will be instrumented and the reflector assembly power cabling will be routed through the radiator assembly. With the radiator in a nose up vertical position, the power conversion system will be lifted up into and mated with the radiator assembly to form a PCS-radiator subassembly. All weld joints are designed to permit x-ray examination. In addition to x-ray, all joints can be dye-penetrant and helium-leak checked.

The gamma attenuation of the PCS-radiator assembly will then be determined by positioning several (probably 4, but using only 1 at a time) different-energy gamma sources at the radiator-shield interface and mapping the resulting dose at the mating plane. Mapping the resulting dose as each source is moved over the plane of the radiator-shield interface will provide the PCS-radiator attenuation for that gamma energy. Choosing sources which cover the full gamma energy range will allow the determination of the gamma attenuation that can be expected for the PCS-radiator assembly during nuclear operation.

The shield and its instrumentation will then be attached to the PCS-radiator assembly to form the power system, which will then be given a complete electrical checkout. After completion of the electrical checkout, the power system will be placed on a transfer dolly (in a horizontal position) and moved to Building 059. Once in Building 059, the system will be lifted off the transfer dolly, the dolly will be lowered into the test vault, and then the power system will be placed on the dolly again. All in-vault handling of the power system will be performed with the transfer dolly and the vault crane.

While the power system assembly is taking place in Building 013, preparation for receiving and testing the system in Building 059 will also be taking place. Building 059, which was previously used for testing the S8DR system, requires some modifications (removing the S8DR NaK systems, upgrading the TV and remote handling equipment, upgrading the vault seals, etc.), and the installation of a computer data acquisition and control system, to prepare the facility for the power system. After the facility modifications are completed, the facility systems (vacuum system, nitrogen system, etc.) will be restarted

and fully checked out. When the core vessel and reflector assembly are received, they will be mated with the core vessel, mounted vertically on a stationary loading table in the vault, and then connected through the facility wiring to the ground and flight control and safety systems. These systems can then be fully checked out, preparatory to core loading and dry critical testing.

In a parallel operation, the fuel elements, grid plates, and internal reflectors (with poison splines in the coolant channels) will be assembled into a core bundle. The reflector assembly will then be mechanically disconnected from the core vessel, but will not be disconnected from the facility wiring. The core vessel will then be slid over the core bundle, the bundle will be clamped within the core vessel, and both will be inverted. The core vessel will then be re-attached to the loading table and reflector assembly. The poison splines will be removed in a stepwise approach to critical, and the dry critical tests will be performed. The dry critical tests will consist of a verification of the expected shutdown margin and excess reactivity (with shimming, if necessary), complete calibration of the control segments, and possibly flux mapping with U^{235} foils. After completion of the dry critical tests, the reflector assembly will be removed, and the reactor inlet plenum and NaK lines will be welded on, the in-core thermocouple penetrations will be seal welded, and the assembly will be prepared for mounting to the power system.

After the core assembly will be mounted to the power system, the reflector assembly will be installed, and the reactor - reflector assembly wiring will be connected to the system wiring. The system will then be connected to the facility wiring, and a complete electrical checkout will be performed. After completion of the electrical checkout, a thermal shroud will be installed about the system, and the system will be installed reactor down in the facility vacuum chamber. The NaK loading lines and system electrical cables will be attached through a short floor-level vacuum vessel spool piece, and a short electrical recheck performed. The NaK systems will then be evacuated, filled (primary system first) with NaK, and heated to the maximum temperature permissible in air commensurate with not oxidizing the thin foil of the flexible copper pump buses. In order to detect small NaK leaks, the temperature and/or time has to be sufficient to achieve wetting of the containment metal. During this heating, continuous visual and TV viewing of the system will be performed to immediately

detect any malfunction, and strain gages and other devices will be monitored to verify proper differential motion of the system. The total program has not been formulated at present, but the NaK loading and thermal acceptance test program, coupled with the component and subsystem acceptance testing and inspection, are to assure that any potential weak spots in the NaK containment system will be caught and corrected before any system damage occurs. Weak spots in the NaK system are not restricted to leaks; it is an objective of the acceptance test to detect excessive stresses in the piping, excessive or insufficient extension of the expansion joints, and other thermomechanical problems of this nature.

After the system temperature is increased above the range where significant copper oxidation occurs, it is expected that the vacuum chamber dome will have to be installed and a vacuum atmosphere established. TV and other remote monitoring of the system will be maintained to detect any problems, and monitoring for NaK in the vacuum chamber will be initiated. The primary and secondary system NaK will be continuously cold trapped, while circulating at $>600^{\circ}\text{F}$ through each loop and its loading system, until the oxide content in each loop is reduced to <10 ppm. The system temperature will then be lowered to room temperature, the vacuum chamber opened, the thermal shroud removed, and the vacuum chamber will be re-sealed. Using the loading system heaters, the power system will be taken to or near normal operating conditions, to verify that the system is capable of producing 5 kwe, and a set of nuclear acceptance tests will be performed. The nuclear acceptance tests will consist of: (1) critical determinations at ambient and elevated temperature, (2) control segment calibrations at ambient and elevated temperatures, (3) temperature coefficient measurements, (4) heat loss and instrument cross calibrations, (5) setting control system current switches, (6) verification of scram times at ambient and elevated temperature, and (7) verification of backup shutdown system operation at ambient and elevated temperature.

At the conclusion of the nuclear acceptance test program, the system will be shut down, the vacuum chamber opened, the NaK lines temporarily pinched off, and the system will be removed from the vacuum vessel. Permanent pinch-off welds will then be made and capped, and the system will be examined. At this point, a flight system would have the reflector assembly removed, and both

the system and reflector assembly would be packaged for and shipped to some launch facility. For the ground system, a ring shield will be installed in the vacuum vessel and the system will be reinstalled. The system and installation will be thoroughly checked out, and the vacuum vessel will be permanently sealed. The loading and checkout equipment would be removed from the vault after the remote handling preparations for post-test disassembly were completed.

The vault will then be permanently sealed, and the power test phase will be initiated. This phase will consist of three startups and a 3- to 5-year endurance run. The first startup, with stops at 25, 50, 75, and 100% of power, will be performed to calibrate the nuclear instrumentation. After completion of the instrument calibration and final setting of alarms and scram points, a series of slow one-step segment oscillations, out and in, will be performed to measure the power coefficient. The reactor will then be shut down, and a simulated auto-startup will be made manually to verify system response to the auto-controller rates and rate change points. A complete flight verification auto-startup will then be made, and an endurance run of >3 years will be initiated. At the conclusion of the endurance run, the system will be shut down, and the system will be disassembled, using remote handling techniques.

8. Reliability

Reliability activities in support of the 5-kwe system design have included: (1) preparation of a failure modes effects and criticality analysis, and (2) allocation of reliability goals to the sub-system level. A summary of these activities follows:

- 1) The results of the failure modes effects and criticality analysis are tabulated in Table 25. The failure modes are tabulated for each major sub-system, and, in certain cases, for equipment items within the sub-system.

The criticality classes of failure have been defined, relative to their effect on capability to produce power for the 5-year time period. No attempt is made to define partial time or partial power failure classes. These criticality definitions are as follows:

TABLE 25
FAILURE MODE AND EFFECTS ANALYSIS ON 5-kwe
THERMOELECTRIC SYSTEM
(Sheet 1 of 3)

Item	Failure	Effect	Class No.	Remarks
<u>Heat Transport System</u>				
Piping, Primary Loop	Leak (piping, expansion joints)	System failure due to loss of heat transport medium (NaK)	IV	Code IV is assigned, because of the definition of failure, "Gas to NaK exposure."
	Plug, total	System failure - flow stoppage of heat transport medium (NaK)	IV	
	Plug, partial	Partial loss of output power due to reduced heat transport capability from reactor to thermoelectric converter	III	
Piping, Secondary Loop	Leak	System failure due to loss of heat transport medium (NaK)	IV	
	Plug, total	System failure - flow stoppage of heat transport medium	IV	
	Plug, partial	Reduction of power output due to reduced heat transport capability from thermoelectric converter to radiator	III	
Volume Accumulator Primary Units or Secondary Unit	External NaK leak	Loss of system operation	IV	
	Internal NaK leak	No effect on system operation	I	
	(1) Leak of primary bellows (2)	No effect on system operation	I	
	(2) Leak of secondary bellows	Possible trouble and loss of system due to gas entry into NaK system	IV	
	(3) Leak of both primary and secondary bellows in same unit of primary loop			
	(4) Leak of both bellows in both primary units	Loss of system due to system pressure loss	IV	
	(5) Leak of both bellows in secondary unit	Loss of system due to system pressure loss	IV	
Pump, Primary or Secondary	(6) External gas loss from one primary unit or the secondary unit	No effect on system operation	I	
	(7) Gas loss from both primary units	Loss of system due to pressure loss	IV	
	Throat leak	System failure - loss of heat transport medium	IV	
	Throat restriction	Reduced power output due to flow restriction	III	
	Loss of power to throat - electrical circuit fault or power modules failure	Can range from reduced power output to total system loss, dependent on failure cause (e. g. bond complete failure, open circuit, whereas module degradation reduces pump capability, and thus results in reduced power output)	III	

TABLE 25
FAILURE MODE AND EFFECTS ANALYSIS ON 5-kwe
THERMOELECTRIC SYSTEM
(Sheet 2 of 3)

Item	Failure	Effect	Class No.	Remarks
Pump, Primary or Secondary (Continued)	Loss of magnetic flux across throat	Loss of system, if total flux is lost — reduced flow, and thus reduced power output, if partial flux loss occurs	IV or III	
<u>Reactor Subsystem</u>				
Reactor Fuel Elements (85)	Loss of reactivity	Decrease of electric power to total loss. Depends on rate of loss and compensation capability of segments	II to IV	
Segment Drive	(1) Loss of both in launch	Loss of system	IV	
	(2) Loss of one in launch	Loss of system	IV	
	(3) Loss of one after startup	Depends on the time of failure with respect to the life. No effect, if near the end of life. Eventual loss of power, if it occurs sufficiently early in life.	I II to IV	
	(4) Loss of both after startup	Eventual loss of power output depends on time of loss	II to IV	
Segment Assembly	Same as drive (1, 2, 3, 4)	Same as for drive	II to IV	
Segment Release Mechanism (electrically actuated at end of life)	Premature release	System loss	IV	
Instrumentation Limit Switch (2)	Open or short Open	No effect — information only No effect — information only	I I	
Wiring	Open or short of power	See preceding for drive loss	II to IV	
	(1) To one drive, launch		I	
	(2) To both drives, launch		IV	
	(3) To one drive, in operation		II to IV	
	(4) To both drives, in operation		II to IV	
Reactor Vessel	Leakage	System loss	IV	
<u>Power Conversion Subsystem</u>				
Thermoelectric Converter Subsystem	Wiring open in one leg	50% power loss	II, III	Assumed series
	Wiring short, ground	Power reduction	III	Connection of modules with two legs in parallel and no diodes

TABLE 25
FAILURE MODE AND EFFECTS ANALYSIS ON 5-kwe
THERMOELECTRIC SYSTEM
(Sheet 3 of 3)

Item	Failure	Effect	Class No.	Remarks
Thermoelectric Converter Subsystem (Continued)	Wiring open, full power module degradation	100% power loss	IV	Wiring short. Location and wiring method determines amount of power loss
	Module short, internal	Power reduction	II	
	Module short, ground	Power reduction	III	
	Module open circuit	Loss of 50% power	III	
	Module thermal resistance increase	Power reduction	II	
	Module electrical resistance increase	Power reduction	II	
<u>Shield</u>	Loss of hydrogen	No effect on power system operation, but could decrease neutron protections below Nuclear Power Unit (NPU)	*	*Code failure depends on protection requirement below shield.
<u>Radiator-Structure</u>				
Radiator	Loss of heat rejection (1) Emissivity coating loss (2) Thermal transfer loss	Reduced power output	III	
	Leakage	System loss	IV	
	Plugging	Depends on magnitude. Could reduce power output	III to IV	
Structure	Rupture, yielding elastic deflection	Failure of structure is most critical during launch phase, but the failure is not considered code Class I unless a secondary failure occurs that fails an operative component of the system (i. e., pipe leak, pump damage, control failure, wiring open or short, etc.)	I	Since radiator piping is part of structure, its failure in launch would be a Category IV failure.

<u>Code</u>	<u>Definition</u>
IV	Catastrophic failure of the system, resulting from a single failure (Catastrophic failure is defined as an immediate loss of power, or a very rapid degradation of power)
III	Reduced electrical power output from system that cannot be compensated for by increasing reactor power or temperature
II	Reduced electrical power output from the system that can be compensated for by increasing reactor power or temperature. (However, probably results in a reduced life, dependent on the time the event occurs in the mission.)
I	Noncritical — little or no effect on the system.

- 2) The allocation of reliability goals to each major subsystem is as shown in Figure 41. The values shown have been generally confirmed by the "Delphi" process. There are three principal subsystems of concern: (a) the power conversion system (degradation modes of failure), (b) the reactor subsystem (loss of reactivity), and (c) the heat transport system (loss of NaK containment).

B. LIQUID METAL COMPONENTS

1. Radiator Design and Fabrication Development

This section describes the thermal radiator configuration for the 5-kwe reactor system, using Lockalloy-38 as the radiator fin material. The performance requirements for the reference radiator are:

Area	= 273.2 ft ²
Net Power Rejected	= 92.9 kw
Inlet Temperature	= 590° F
ΔT	= 144° F

NOTE THE PRECEDING ARE THE INITIAL GOALS, NOT ESTIMATES OF
THE PRESENT RELIABILITY

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Figure 41. Basic 5-kwe Thermoelectric System
Block Diagram

AI-AEC-13046

Emissivity = 0.90

Solar Absorptivity = 0.50

Environmental Heat Input = 102 Btu/ft²-hr.

Equation 4 is the reference meteoroid armor equation that is being used to establish the amount of armor required to protect the fin-tube radiator assembly coolant tube from meteoroid impact.

$$\delta = \gamma_R a \left(\frac{\rho_p}{\rho_a} \right)^{1/2} \left(\frac{V_p}{C_a} \right)^{2/3} \left(\frac{6}{\pi \rho_p} \right)^{1/3} \left(\frac{E \alpha A_v t}{-\ln P_o} \right)^{B/3} \left(\frac{2}{3nB+2} \right)^{B/3} \left(\frac{T}{T_R} \right)^{1/6} \dots (4)$$

where:

δ = Armor thickness

γ_R = Room temperature cratering coefficient

a = Rear surface damage thickness factor

ρ_p = Meteoroid average density (0.5 gm/cm³)

ρ_a = Armor density

V_p = Meteoroid average velocity (20 km/sec)

C_a = Sonic velocity in armor

E = Armor earth shielding factor (0.7631)

α = Meteoroid flux constant (10^{-14.37} gmB/m²-sec)

A_v = Vulnerable area

t = Mission time

*Equation Reference: NASA TND-4411

Shielding Factor: NASA-SP-8013

Temperature Correction: NASA-TND-5135

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Figure 42. Lockalloy Armor Thickness vs Noncritical Damage Probability

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Figure 43. Radiator System Weight vs Number of Coolant Tubes

P_O = Design probability of no critical damage

n = Damage factor for oblique impact (1.0)

θ = Penetration constant (0.667)

B = Meteoroid flux constant (1.213)

T = Armor temperature

T_R = Room temperature

Equation 4 also gives the reference meteoroid flux constants used in the armor calculations. A room temperature cratering coefficient of 2.06 is used for Lockalloy. This value was determined by composition weighting the cratering coefficients of aluminum and beryllium.

Substituting the preceding input values into the armor equation, the required Lockalloy armor thickness was determined as a function of the noncritical damage probability, P_O . The results of this calculation are shown in Figure 42. With a reference noncritical damage probability of 0.99 and a mission lifetime of 5 years, the required Lockalloy armor thickness is ~ 0.076 in.

Based upon an armor thickness of 0.076 in., a study was made to determine the characteristics of the radiator fin cross section. The general radiator structure configuration consists of a conical radiator section and a cylindrical radiator section with a combined area of 273.2 ft^2 .

Figure 43 shows the total radiator system weight as a function of the number of coolant tubes. Figure 43 has a broad minimum between 44 and 50 coolant tubes.

Figure 44 illustrates the total power rejected from the radiator as a function of constant fin thickness for 44, 46, 48, and 50 coolant tubes. The total power rejected for a given number of coolant tubes increases with increasing fin thickness. The dashed line in Figure 44 designates the required power rejection of 92.9 kw. The required constant fin thickness, as determined by thermal performance, for the various number of coolant tubes are as follows:

Figure 44. Radiator Power Rejected vs Constant Radiator Fin Thickness

Figure 45. Fin-Tube Cross-Section Dimensions

<u>Number of Coolant Tubes</u>	<u>Constant Fin Thickness (in.)</u>
44	0.069
46	0.063
48	0.058
50	0.053

From Figure 43, it can be concluded that any of the number of coolant tubes may be used without a large weight penalty. Forty-eight coolant tubes are used as the reference value, providing symmetry.

The reference radiator, using 48 coolant tubes, has a constant fin thickness of 0.058 in. An additional thickness of Lockalloy-38 of 0.018 in. is required between the coolant tube and the inner surface of the radiator to provide sufficient meteoroid armor.

The detailed fin tube cross sections in the cylindrical section and at the top of the conical section are shown in Figure 45, and an isometric of the radiator with key dimensions is shown in Figure 46. The dimensions of the reference radiator are as follows:

Minor Diameter	= 29.71 in.
Major Diameter	= 72 in.
Cone Height	= 141.5 in.
Cylinder Height	= 72.9 in.
Area	= 273.2 ft ²
Coolant Tube OD	= 0.4375 in.
Coolant Tube Wall Thickness	= 0.020 in.
Coolant Tube Spacing:	
At top of cone	= 1.944 in.
At cone midplane	= 3.328 in.
In cylindrical segment	= 4.712 in.

Figure 46. Radiator Dimensions

a. Radiator Design

The radiator fin effectiveness is shown in Figure 47 as a function of the radiator axial length. Since the radiator is of constant thickness and the fin width varies up the cone, the fin effectiveness varies from ~ 0.92 at the top of the radiator cone to ~ 0.825 in the cylindrical portion of the radiator. The total radiator average fin effectiveness is 0.847.

The approximate temperature profiles of a radiator fin at the top of the conical section, cone midplane, and at the cylinder midplane are shown in Figure 48. The figure shows that a small temperature drop exists between the coolant and the fin root at each of the sections. The temperature drop between the fin root and the fin edge for each section is as follows:

<u>Section</u>	<u>ΔT Between Fin Root and Edge (° F)</u>
Top of Cone	~ 12
Cone Midplane	~ 24
Cylinder Midplane	~ 30

b. Radiator Structural Analysis

Lockalloy was selected as the radiator fin material for the conceptual design, because of the close match in thermal expansion rate between Lockalloy and stainless steel. This characteristic of the two materials results in relatively low stresses and strains when the two materials are bonded together. Figure 49 shows the stress-strain curves for Lockalloy and Type 304L stainless steel, and the strains induced in each material in going from 775° F to room temperature. No plastic strain occurs, and subsequent cycling to 600° F all but eliminates the residual stresses. The Type 304L stainless steel was selected for the tubing application primarily because it resulted in a better thermal expansion match with the Lockalloy than did Type 316 stainless steel, and the low carbon content should result in a better titanium barrier coating, discussed later.

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Figure 47. Radiator Fin Effectiveness vs Axial Length (Constant fin thickness of 0.076 in.)

6531 5424

Figure 48. Radiator Fin Thermal Profiles (Constant fin thickness of 0.076 in.)

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Figure 49. Stress-Strain of Lockalloy and Type 304L Stainless Steel and Mechanical Strains in Bimetallic Cooled From 775° F to Room Temperature

6531 4742

Figure 50. Lockalloy Radiator Panel Strength vs Applied Acceleration Loads

Figure 51. Thermal Expansion From Room Temperature for 3003 Aluminum and Type 316 Stainless Steel

Figure 52. Stress-Strain of 3003 Aluminum and Type 316 Stainless Steel

The ability of the Lockalloy radiator to sustain launch-induced acceleration loads is greatly improved over the previously reported aluminum structure. This is largely due to its high modulus of elasticity (30×10^6 psi for Lockalloy, compared to 10×10^6 psi for aluminum). Figure 50 shows both the maximum design loading due to acceleration and the capacity of the Lockalloy structure. Note that the margin is lowest at station S_0 , but is substantially higher than is required.

The aluminum - stainless steel radiator design presented in the previous quarterly progress report was analyzed extensively before a switch was made to the Lockalloy fin material. The analysis centered on the stress-strain relationships set up in a bonded aluminum - stainless steel composite, and the potential ratcheting conditions which are established due to thermal cycling the radiator from room temperature to normal operating temperatures. This condition results from the large difference in the coefficients of expansion for these two materials.

The candidate materials for this consideration were 3003 Aluminum for the fins, brazed to Type 316 stainless steel tubes. The thickness of the fins was determined on the basis of thermal performance, and the thickness of the material outside the tube was determined by meteoroid protection requirements. The tube diameter of 7/16 in. OD and a wall thickness of 0.020 in. were established primarily for performance and fabricability.

Since the radiator is an integral structural component, and since it is subjected to thermal loading prior to a room-temperature launch environment, the thermal effects on configuration stability are significant.

The stress-strain curves and expansion coefficients for the two materials are shown in Figures 51 and 52 respectively.

The first thermal loading for the composite occurs in the brazing operation. At some high temperature (775°F assumed), the stainless steel and the aluminum lock together. At this temperature, both aluminum and stainless steel are of equal lengths. As the materials cool, the aluminum tends to contract at a

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Figure 53. Mechanical Strain of Asymmetric Fin-Tube Panel Cooling From 775° F to Room Temperature

greater rate than the stainless steel, due to its higher coefficient of expansion. However, in the composite, both materials remain at equal lengths. Thus, the aluminum is mechanically strained in tension, and the stainless steel is mechanically strained in compression. The total force, $P = \int \sigma dA$, in the aluminum equals that force in the stainless by the relationship:

$$P = \frac{(\alpha_{Al} - \alpha_{SS})\Delta T A_{Al} E_{Al} A_{SS} E_{SS}}{(A_{Al} E_{Al} + A_{SS} E_{SS})} , \quad \dots (5)$$

where:

α = coefficient of thermal expansion

A = cross-sectional area

E = modulus of elasticity

ΔT = temperature differential.

From this relationship, it was determined that the tubes strained 0.32% in compression, or 0.21% more than the strain at yield. It was therefore strained into the plastic range. The aluminum was strained 0.04% in tension, and remained elastic. Figure 53 shows the effect of cooling from 775° to room temperature on the strains of the two metals.

The cross section which had been selected was asymmetric. Since the forces in the aluminum and stainless steel were not concentric, there would be a tendency for the brazed panel to bow out of its plane. It was determined that, in a temperature range between 70 and 600° F, the panel would bow to a radius of 325 in. Several samples were measured at radii of 79, 81, and 119 in. Note that these were not the same cross section as the reference design, but that the bow radii were indicative of potential problems.

6531-4746

Figure 54. Mechanical Strain of Symmetric Fin-Tube Bimetallic Panel Cooling From 600°F to Room Temperature

6531-4747

Figure 55. Stress-Strain of Symmetric Fin-Tube in One Thermal Cycle From 600°F to Room Temperature to 600°F

It was therefore determined that a symmetric configuration would be more appropriate to prevent bowing, although this would require additional weight, and would change the ratio of areas. It was assumed, at this time, that some method could be established to make the fin-tube panel stress free at 600° F, or by preloading the tube during brazing. In any case, 600° F was selected as the most desirable stress-free temperature.

A revised calculation on relative strain of the aluminum and stainless steel showed that, in cooling from 600° F to room temperature, the tube would strain mechanically 0.21%, or 0.10% beyond the yield stress, and into the plastic range. The fins would strain 0.05% in tension, and remain elastic. Figure 54 shows the strains associated with the 600° to room temperature cycle.

Although it is not desirable to allow NaK containment to strain into the plastic range, stress shakedown or nonratcheting is acceptable. Thus, it was necessary to determine the status of strain on subsequent cycles. Figure 55 shows the effect on the strain of the tube with repeated applications of the 600° F to room temperature and room temperature to 600° F cycle. At 600° F in the braze cycle, both materials are stress or strain free at Point 0 of Figure 55. As the composite cools to room temperature, the fin (aluminum) strains to +0.05% at Point 1, and the tube strains to -0.21, or -0.10% beyond the elastic limit, also at Point 1. Subsequent heating to 600° F causes the fin to reduce its strain in tension, and have a terminal strain of -0.03% at Point 2. Similarly, the tube will unload at a rate equivalent to its elastic modulus, and terminate elastically at +0.03% at Point 2. If there were no long-time effects, further thermal cycling would not have further plastic strain, and the fin would cycle between -0.03 and +0.05%, and the tube would cycle between -0.21 and +0.03%.

However, the 3003 Aluminum has low creep strength at 600° F, and tends to relax rapidly. With extended holding at 600° F, the aluminum and stainless steel tend to unload, ultimately reaching an essentially stress-free condition with the tube permanently set at 0.10%, the amount of plastic strain it experienced in the first leg of the cycle. The aluminum has also gone through a 0.10%

strain, due to the relaxation, both materials now at Point 3. This represents a new starting point at 600° F, and it can be seen that, with each succeeding cycle, there would be a 0.10% additional strain in the tubes, and they would ratchet.

The preceding evaluation was done for the ideal case of isothermal cycling. It should be noted that both axial and circumferential temperature gradients exist during operational phases. Thus, variable creep rates exist throughout the radiator, and it is inconceivable that the stress-strain relationship beyond the first half of the first cycle can be predicted. It was advisable to employ an alternative design. Either the use of materials that have higher elastic ranges and low creep, or that have a better match on thermal expansion rates, would solve the problem.

A slightly better match could be achieved by using Type 304L stainless steel, rather than Type 316 stainless steel. However, only slight improvement could be achieved in the stresses.

c. Fabrication Development

Samples of stainless steel bonded to aluminum were obtained from three vendors, Lockheed, Solar, and Explosive Fabricators. The Lockheed sample was composed of a 3003 Aluminum fin, brazed with B Al-Si₄ (aluminum - 11 to 13 wt% silicon eutectic) to a B Al-Si₄ tinned, Type 316 stainless steel tube. The Solar samples were composed of 3003 Aluminum fins, brazed with B Al-Si₄ to titanium-plated Type 304 stainless steel tubing by two different brazing processes. The Explosive Fabricators sample was composed of a heavy-walled 2024 Aluminum tube, explosively bonded to the OD of a Type 304 stainless steel. Due to extensive radial cracks at the interface between the aluminum and stainless steel in the explosively bonded sample, the process was dropped from further consideration.

The process approach used by Lockheed was to tin the stainless steel tube with aluminum - silicon braze alloy, prior to puddle brazing the tube for assembly. The tinning was performed by hand, and it required considerable skill to just melt the brazing alloy and yet flow the metal without overheating. The process resulted in a minimum time at temperature. Wetting was achieved by wire brushing the stainless steel tubes to remove oxides as it was tinned.

Joining of the aluminum fin material to the braze-coated stainless steel tube was accomplished by clamping the fin material to a hot platten (800 to 990° F) and puddle brazing the tube to the fin. Excess braze was then milled away to the final configuration.

The fabrication process used by Solar was to plate the stainless steel tube with titanium, prior to brazing the tube to the aluminum fin. The plating is accomplished in a fused-salt bath at ~1550° F. Brazing of the stainless to the aluminum was accomplished by two different processes. The first process was inert-atmosphere brazing, using a flux to allow the braze to wet the preformed fin surface and the titanium-plated stainless steel tube surface. The second process was a resistance pressure bonding technique, in which pressure was applied by an electric current-carrying roller to the joint area, containing pre-placed braze material. The heat produced by current flowing at the fin-braze-tube interface caused the braze to melt, and join the tube to the fin. The process was carried out with an inert cover gas.

A comparison of room-temperature mechanical and physical properties for 3003 Aluminum and Lockalloy is shown in Table 26. As can be seen, the Lockalloy is lower in density, is nearly three times stiffer, and is seven times stronger. For the radiator fin material, Lockalloy appears to be an ideal material. Sample quantities have been ordered for additional mechanical properties tests and for braze studies.

d. Testing

Brazed assemblies produced by Lockheed and Solar were tested for bond integrity after thermal cycling from room temperature to 600° F, and two Solar samples were thermally aged to determine the efficacy of the titanium plating to reduce diffusion of the braze material into the stainless steel.

Figure 56 shows two photomicrographs of a Lockheed sample, before and after one thermal cycle from room temperature to 600° F at a heating rate of 6.4° F/min, at which time the furnace power was turned off and the sample was allowed to cool for 16 hr to a temperature of 110° F before removal from the furnace. The as-received sample, shown in Figure 56a, has not been etched; the thermal-cycled sample (Figure 56b) has been etched, hence the difference in appearance of the braze material. Bond failure occurred at the base of the

BRAZE-
STAINLESS
DIFFUSION
ZONE

9739-3

a. As Received (250X)

BOND
FAILURE

9742-3

b. After One Thermal Cycle (250X)

Figure 56. Lockheed Sample

TABLE 26
MECHANICAL AND PHYSICAL PROPERTIES OF FIN MATERIALS
AT ROOM TEMPERATURE

	3003 Aluminum (Al - 1.2 Mn - 0.12 Cu)	Extruded Lockalloy (Be - 38 Al)	
Density (lb/in. ³)	0.099	0.076	
Thermal Conductivity (Btu/hr-ft ² -°F/ft at room temperature)	112	123	
Thermal Expansion [(in./in./°F) x 10 ⁶]	12.7	9.0	
Melting Point (°F)	1200	1193	
Tensile Modulus (10 ⁶ psi)	10	27	
Room Temperature Tensile Properties		Longitudinal	Transverse
Ultimate (ksi)	16	56	56
Yield (ksi)	6	44	44
Elongation (%)	30	7-10	1.5-2

fin-tube interface. The tinned stainless steel tube above the aluminum fin still had a continuous bond with the aluminum-silicon eutectic. Earlier thermal cycling of test samples showed the same results, and further tests were discontinued.

The two types of Solar samples were also subjected to thermal cycling between room temperature and 600°F, with the results shown in Figures 57 and 58. The flux-brazed Solar sample was subjected to 10 rapid thermal cycles from 100 to 600 to 100°F in 10 min cycles. Debonding of the sample is shown in the Figure 57b photomicrograph. As will be noted, the separation occurred between the titanium plating and the stainless steel, indicating that the strongest bond existed between the titanium and the braze material. The fluxless-brazed Solar sample was subjected to 1 thermal cycle from room temperature to 600°F at a heating rate of 9.5°F/min, and slow cooling overnight to

9724-3

a. As Received (250X)

9742-1

b. After Ten Thermal Cycles (500X)

Figure 57. Flux-Brazed Solar Sample

9732-1

a. As Received (500X)

9742-2

b. After One Thermal Cycle (250X)

Figure 58. Fluxless-Brazed Solar Sample

103° F. Figure 58 shows photomicrographs of the as-received and thermal-cycled sample. The fluxless-brazed sample separated between the braze and the titanium plating, indicating greater strength in the stainless-titanium interface than between the braze and the titanium.

Upon completion of the thermal cycle test of the Solar samples, the vendor was contacted about the bond separation. It was indicated that the titanium plating did not appear as normal, and that the steel must be well characterized to determine whether the carbon and nitrogen are sufficiently low to prevent the formation of brittle intermetallics with the titanium. For this reason, low-carbon Type 304L stainless steel has been selected as the reference radiator tubing material.

In addition to the thermal cycle tests of the Solar material, one sample each of the flux-brazed and fluxless-brazed were thermally aged at 1020° F for 72 hr. The time and temperature were chosen to indicate the amount of diffusion that would be expected if the activation energy for the diffusion were 28 kcal over the operating temperature of 600° F for a period of 40,000 hr. The titanium effectively acted as a diffusion barrier, although separation of the tube and fin material occurred in the same zones as in the thermally cycled samples.

2. Thermoelectric Pump Assembly

a. Prototype Pump Engineering

Performance analysis of the prototype pump assembly was continued. Performance and design characteristics have changed little during this report period. The resistances of the current collector rings at the ends of the pump thermoelectric modules are now included in the total pump resistive load. Table 27 summarizes the present end-of-life performance requirements of the pump and the thermoelectric modules.

Additional details were added to the conceptual design layout of the prototype thermoelectric pump assembly. These are reflected in the updated isometric drawing shown in Figure 59. The thermal analysis showed that the peak magnetic temperatures would not exceed 750° F, and could be maintained below 700° F with a judicious selection of thermal barriers in the design. The

TABLE 27
THERMOELECTRIC PUMP ASSEMBLY PERFORMANCE
CHARACTERISTICS - EOL

Voltage (v)	0.195
Current (amp)	1560
Magnetic Flux Density (G)	2380
Overall Efficiency (%)	0.35
Total Hydraulic Pump vs Power (w)	37
Thermal Power In (kw)	10.5
Throat Dimensions (in.)	
Height	0.6
Width	1.4
Length	2.5
Weight (lb)	55
NaK Flow Rate (lb/sec)	
Primary Throat	4.73
Secondary Throat	2.88
Primary Modules Total	0.52
Secondary Modules Total	0.32
Pressure Rise (psi)*	
Primary Throat	0.92
Secondary Throat	1.44
Temperatures (° F)	
Primary Throat	1105
Secondary Throat	590
Primary Modules Average	1136
Secondary Modules Average	518

*Includes 15% design margin

heat transferred from the primary loop to the secondary loop through the buses between the throats and between throats and modules is ~1 kw.

Stress analysis of the prototype design is continuing.

72-017 48 322A

Figure 59. Electromagnetic Pump and Thermoelectric Converter

Figure 60. Dual-Throat NaK Pump Assembly

6531-4033CN

b. Development Pump Tests

Tests are being conducted on a dual-throat NaK pump assembly, as shown in Figure 60, and samples of Type 316 stainless steel to copper braze joints in the form of simulated bus-throat and coupons.

(1) Pump Tests

Modification of the PCA NaK test loops in Bldg. 023, Santa Susana, and installation of the developmental pump in the vacuum chamber on this loop, were completed. Testing was performed at ambient temperature, at intermediate points, and at the design temperatures of 1050° F in the primary throat and 660° F on the secondary throat, for pump currents of 400, 800, 1200, 1600, and 1800 amp. Results of testing conducted thus far show that, in general, performance is slightly better than predicted. Figures 61a and b show the developed pressure and function of flow rate for varying values of pump current for the primary and secondary throats at their respective design temperatures. A comparison of the original design points for this pump against present empirical data is shown in Figure 62. Figure 63 shows the pressure developed across each throat at design temperature, as a function of current applied to the pump. The lower pressure developed in the secondary throat is due to the effect of the mmf generated by the return bus current reacting with the permanent magnet field in a direction to reduce the field established by the magnets. In the primary throat, this same induced field is in a direction such that the field is improved, but only slightly, because of saturation of the magnet yokes. Additional testing on this pump will be conducted at the design temperatures of the prototype pump (1110-590° F), and will include a minimum of 10 thermal cycles to ambient and steady-state endurance testing at design temperature.

(2) Braze Joint Tests

Four each copper bus - stainless steel throat sections, similar in configuration to those used in the development pump, were installed within furnaces in vacuum chambers. Two of these assemblies are being cycled between 1100° F and ambient, and two are being cycled between 1200° F and ambient. Resistance across the throat braze joints is being measured at ambient and at operating temperatures, as a function of operating time and number of thermal cycles.

6531-5425

a. Primary Throat ($T = 1050^{\circ}\text{F}$)

6531-5426

b. Secondary Throat ($T = 660^{\circ}\text{F}$)

Figure 61. Head vs Flow for Development Pump

6E31 5427

Figure 62. Development Pump Performance

6531 5428

Figure 63. Pressure vs Current for Development Pump

After 12 thermal cycles, there is no evidence of change in resistance in any of the braze joints. These throat assemblies will be examined after 50 thermal cycles.

Twenty diffusion-couple coupons, consisting of bonded stacks of OFHC copper sheets (alternating thicknesses of 0.015 and 0.005 in. by 0.5-in. square), brazed to Type 316 stainless steel plates (1.0 by 0.5 by 0.25 in.), have been fabricated. Half of the coupons were brazed using Nicoro 80^{*} braze material, the same as used on the pump throats, and half were made using Gemco[†] braze material. Tests will be conducted in vacuum with specimens of each type at 1100, 1200, 1400, and 1600° F for varying lengths of time.

Destructive examination of both throat assemblies and coupons will be performed at the conclusion of testing to assess the quality of the braze joints and diffusion bonds.

3. Volume Accumulator Unit (VAU) and Expansion Joint Unit (EJU)

a. Prototype Accumulator Engineering

The VAU design layout was completed for the latest system reference design (Figure 64). The nesting-formed-bellows type was selected, in place of the welded-bellows type, to minimize VAU cost and complexity and maximize inherent VAU system reliability. The unit consists of two redundant bellows in series, which results in double NaK containment. Overpressure for the system is achieved by pressurizing the inside of the top bellows to the desired level with static inert gas. A leak in the primary bellows results in only a slight reduction in the overpressure of the unit.

b. Bellows Tests

Two welded-bellows capsules and one nesting-formed-bellows capsule, with heat treatment tensile test samples, were received for bellows evaluation testing. Two additional welded-bellows capsules, from a second welded bellows manufacturer, are forthcoming. The three capsules received to date have been helium leak checked, radiographically inspected, and nondestructively tested.

*Nicoro 80 (Western Gold & Platinum) = 81.5 Au, 16.5 Cu, 2.0 Ni

†Gemco (Western Gold & Platinum) = 87.75 Cu, 12.0 Ge, 0.25 Ni

Tensile testing of samples, heat treated with the bellows, has been completed. Metallographic examination of the capsules has been started.

All three bellows capsules successfully met the helium leaktightness requirement ($<10^{-9}$ Std cc/sec). Radiographic inspection of the welded bellows capsules showed indications of one inclusion in each bellows (one ~ 0.010 in. in diameter, and one ~ 0.020 in. in diameter). The radiographic examination of the formed bellows attachment welds showed indications of inclusions ~ 0.005 in. in diameter. The spring rates for the welded bellows were within 12% of calculated values, and, for the formed bellows, was within 5% of calculated values. The effective areas were within 5% of calculated values. The tensile-test results of the heat treated samples were within the material specification requirements for both parent material and welded material.

The preliminary metallographic examination of the welded-bellows capsules from supplier No. 1 indicated unacceptable weld bead quality, symmetry, size, and roughness. One convolution weld of one capsule was cracked, and one bellows - end plate weld of the other capsule was cracked.

c. Prototype Expansion Joint Engineering

The EJU design layout was completed for the latest piping system reference design. The nesting-formed-bellows type was selected as the bellows type for the EJU's to be used in the supply and return NaK lines of the preliminary loop.

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 quarter. During the past quarter, eight refractory-clad reactor power modules were tested. 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.

A list of design features of each TEM-X Series module fabricated, or scheduled for fabrication, is presented in Table 28.

All reactor power modules continued to operate with power output degradation rates ranging from 1.7 to 7.7%/10,000 hr, for operating times up to 12,632 hr, hot cladding operating temperatures as high as 1175° F average (1225° F peak), and including up to 60 thermal cycles per module.

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

Four of the eight refractory-clad TEM-X S/N-3 Series power modules tested during the past quarter (S/N-3C, 3D, 3E, and 3F) were fabricated with end caps which are backfilled with an inert gas (argon) to prevent air leakage around the exposed boron nitride electrical pin insulators. These four modules have accrued operating times ranging from 7853 to 12,632 hr.

Electrical power output, overall efficiency, open circuit voltage, and internal resistance life test data from this group of modules are presented as functions of testing time in Figures 65a through d. TEM-X S/N-3C and 3E are on static test at WANL. TEM-X S/N-3D and 3F are on test in the liquid metal loop system at Atomics International, with 100° F axial temperature drops.

TEM-X S/N-3E test temperatures were raised to $\bar{T}_H = 1140^\circ \text{F}$ / $\bar{T}_C = 523^\circ \text{F}$, from $\bar{T}_H = 1085^\circ \text{F}$ / $\bar{T}_C = 570^\circ \text{F}$, during the past reporting period. This change in operating conditions simulates the effects of increasing the reactor outlet temperature in a reactor-thermoelectric space power system having a fixed radiator. Data presented in Figures 65a through d indicate that the module

TABLE 28
DESIGN FEATURES OF TEM-X (REFRACTORY-CLAD) MODULES

Serial No.	Length (in.)	No. Couples	Mica Thickness (in.)	Diffusion Barrier	Hermetic Seal	Remarks
2	15	79	0.005	None	None	Demonstrated fix to cyclic distortion problem.
3	15	38	0.010	1/2 W foil	None	Destructively examined after processing.
3A	15	38	0.010	1/2 W foil	None	Clad leak demonstrated hermetic seal requirement.
3B	15	38	0.010	1/2 W foil	None	Duplicated TEM-X S/N-3A test.
3C	15	38	0.010	1/2 W foil	End Cap	Long term cyclic test at 1085°F/507°F
3D	15	38	0.010	1/2 W foil	End Cap	Long term cyclic test at 1085°F/507°F in loop.
3E	15	38	0.010	1/2 W foil	End Cap	Inner conductor insert lengths extended.
3F	15	38	0.010	1/2 W foil	End Cap	Identical to 3E.
3G	15	38	0.010	1/2 W foil	Glass Seal	Test to determine effects of temperature on degradation.
3H	15	38	0.010	1/2 W foil	Glass Seal	Identical to 3G.
3J	15	38	0.010	1/2 W foil	Glass Seal	Identical to 3G.
3K	15	38	0.010	1/2 W foil	None	Test to simulate loss of hermetic seal during space operation.
3M	15	38	0.010	1/2 W foil	End Cap	Three-piece inner conductor eliminates GEnl/Mo interface.
4	15	74	0.010	None	Glass Seal	High Voltage module.

a. Power Output vs Elapsed Time

Figure 65. Reactor Power Modules Incorporating End Caps
(Sheet 1 of 4)

b. Overall Efficiency vs Elapsed Time

Figure 65. Reactor Power Modules Incorporating End Caps
(Sheet 2 of 4)

c. Open-Circuit Voltage vs Elapsed Time

Figure 65. Reactor Power Modules Incorporating End Caps
(Sheet 3 of 4)

d. Internal Resistance vs Elapsed Time

Figure 65. Reactor Power Modules Incorporating End Caps
(Sheet 4 of 4)

responded to the change in test temperatures in a manner very similar to TEM-X S/N-3F, which was subjected to the same test change after 1000 hr of operation at the initial conditions. During the 10,000 hr of operation at $\overline{T}_H = 1085^\circ\text{F}/\overline{T}_C = 507^\circ\text{F}$, TEM-X S/N-3E overall performance stability trends were very similar to those of TEM-X S/N-3C, operated at identical temperatures.

The excellent degree of performance stability of these refractory-clad modules is quite evident from the data shown in Figures 65a through d. A statistical analysis (linear curve fit) of all life test data from TEM-X Series modules is presented in Table 29. No major changes from previously established performance stability trends have been observed.

The second group of TEM-X S/N-3 Series modules is identical to the four modules discussed previously, with the exception that glass-to-metal hermetic seals were incorporated at each electrical access pin base to allow testing in an air environment. Testing of the three "glass seal" modules, TEM-X S/N-3G, 3H, and 3J, was interrupted during the past quarter to allow inspection of the seals. Although it is impossible to perform a conclusive leak check on a fabricated module, no deterioration of the glass was detected in a visual inspection. Subsequently, the three modules were returned to their previous operating conditions for continued endurance testing.

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. The basic performance parameters from all three modules are presented as functions of testing time in Figures 66a through d. 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 $\sim 1/3$ the rate).

Operating temperatures and corresponding linear curve fit degradation rates for the "glass seal" modules are presented in Table 29. Because of the

TABLE 29

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	P_e	η_o	Rapid	30°F/ min.
TEM-X S/N-3C	1085/507-S	12,632	3.4	-1.7	-0.8	6	53
TEM-X S/N-3D	1085/507-L	9,062	7.0	-4.9	-4.6	7	53
TEM-X S/N-3E	{ 1085/507-S	10,007	3.9	-2.3	-1.3	6	0
	{ 1140/523-S	793	7.9	-4.8	+2.3		
TEM-X S/N-3F	{ 1085/507-L	1,011	6.0	-1.0	+4.1	6	2
	{ 1140/523-L	6,842	8.7	-6.2	(A)		
TEM-X S/N-3G	1085/507-S	3,547	7.8	-2.8	-1.2	3	1
TEM-X S/N-3H	1140/523-S	3,210	7.1	-4.2	0.0	1	2
TEM-X S/N-3J	1175/535-S	2,048	10.1	-7.7	-6.8	9	1
TEM-X S/N-4	1085/507-S	1,168	9.5	-1.8	+2.4	1	0

L Indicates Loop Test (All test data through October 9, 1972)

S Indicates Static Test (All test data through October 16, 1972)

(A) Not calculated due to flow meter fluctuations

a. Power Output vs Elapsed Time

Figure 66. Reactor Power Modules Incorporating Glass Seals
(Sheet 1 of 4)

b. Overall Efficiency vs Elapsed Time

Figure 66. Reactor Power Modules Incorporating Glass Seals
(Sheet 2 of 4)

c. Open-Circuit Voltage vs Elapsed Time

Figure 66. Reactor Power Modules Incorporating Glass Seals
(Sheet 3 of 4)

d. Internal Resistance vs Elapsed Time

Figure 66. Reactor Power Modules Incorporating Glass Seals
(Sheet 4 of 4)

short testing times, the uncertainties in degradation rates for each of the parameters are much greater than those of the previously discussed modules. However, these data indicate that the sensitivity of the degradation rates to operating temperatures is nearly the same as that previously established in TEM-15 technology modules, as expected.

TEM-X S/N-3K, shipped to Atomics International during the previous quarter, is being installed in the liquid metal loop system. The hard vacuum testing of this module, built without glass-to-metal seals, will simulate the worst-case effects of the failure of glass seals during space environment operation. Performance levels and stability trends in this module are expected to duplicate those of the sealed modules.

The design of TEM-X S/N-3M was modified during the past quarter to incorporate the three-piece inner conductor ring design employed in a previous sub-length module, TEM-15H S/N-4. Performance data from the sub-length module indicate that the elimination of the molybdenum - thermoelectric material interface at the n-leg hot junctions produces a measurable reduction in degradation. Modified inner conductor ring components have been placed on order, and assembly of the TEM-X S/N-3M will be resumed in November.

c. TEM-X S/N-4 Series Module

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 modification allowed the number of couples to be increased from 38 to 74, while maintaining the 15.0-in. circuit length. This provides approximately a factor of two increase in voltage. Rationale behind this selection was discussed in a previous Quarterly Progress Report, AI-AEC-13023. The module incorporates glass-to-metal pin base seals identical to those of TEM-X S/N-3G, 3H, and 3J.

TEM-X S/N-4 has accrued a total of 1168 hr of operation at $\bar{T}_H = 1085^\circ\text{F}$ / $\bar{T}_C = 507^\circ\text{F}$ test temperatures. Power output, overall efficiency, open circuit voltage, and internal resistance life test data from this module are presented in Figures 66a through d, respectively, along with similar data from the other glass seal modules. Both the power output and overall efficiency data from this module can be seen to duplicate similar data from TEM-X S/N-3G, which

is also operating at $\overline{T}_H = 1085^\circ\text{F}/\overline{T}_C = 507^\circ\text{F}$. These two parameters are relatively insensitive to the modifications incorporated into the TEM-X S/N-4 design. The voltage and resistance data shown in Figures 66c and d, respectively, reflect the extreme sensitivity of these parameters to the number of couples in the module. The voltage of TEM-X S/N-4 is greater than that of S/N-3G, by the ratio 74/38 (the ratio of the numbers of couples in the modules). The resistance of the module varies as the square of the number of couples. The basic advantage of the higher-voltage module is the reduced matched load current, which reduces bus Joule heating losses. Also, with higher-voltage modules, fewer series-wired modules are required to achieve a given system voltage requirement. System reliability increases as the number of series-wired modules decreases.

Although degradation rates for TEM-X S/N-4 are listed in Table 29, the results are not felt to be particularly meaningful, due to the limited amount of data available. Degradation rates for this module are expected to be somewhat greater than those of TEM-X S/N-3G, because of the relationship between degradation and thermoelectric washer thickness. However, because the degradation rates of both modules are very low, as the data shown in Figures 66a through d indicate very clearly, it will take a considerable amount of testing time to separate the effects of the design change from the effects of experimental uncertainty.

d. Reactor Module Parametric Analysis and Preliminary Design

During the past quarter, the effort to provide parametric performance information, as required in support of the 5-kwe system design being conducted at Atomics International, was continued. Additional parametric performance calculations were generated and transmitted to Atomics International, for both the reactor power module and pump module, in this reporting period. A discussion of the pump module effort is presented in a separate section of this report.

Preliminary systems efforts to date have indicated the end-of-life (5-year) reactor power module performance requirements listed in Table 30. To meet the specified requirements, it has been assumed that the reference module for the system will incorporate ternary materials and thin-film electron beam vapor

TABLE 30
PRELIMINARY MATCHED-LOAD EOL 5-kwe
REACTOR MODULE REQUIREMENTS

	<u>Per Module</u>	<u>System</u>
Average Module Inner Clad Temperature (°F)	1140	---
Number of Modules	---	16
Power Output (watts)	320.6	5130
Load Voltage (volts)	3.86	30.9
Total Power Degradation (pct.)	6.25	6.25

deposit (EBVD) diffusion barriers, allowing the use of 0.0015-in. intercouple mica insulators. It has also been assumed that the reference module will degrade at a rate of 1.4%/10,000 hr. This results in a total power degradation of 6.25%, over a 5-year life.

Calculations indicate that a reactor power module incorporating ternary materials and EBVD diffusion barriers would have a BOL matched load efficiency of 6.50%, when operated at $\bar{T}_H = 1085^\circ\text{F}/\bar{T}_C = 507^\circ\text{F}$. This is a 30% increase above the 5.0% efficiency level demonstrated by current TEM-X Series reactor power modules operating at the same temperatures.

Module degradation is accounted for analytically, by increasing the resistivity of the n-leg thermoelectric material by the factor necessary to cause a 6.25% reduction in power output at a fixed set of operating temperatures. Although voltage changes have been observed during operation of modules, these changes are much smaller in magnitude than the resistance changes. In addition, the experimental data indicate that the voltage initially increases, goes through a maximum, and then tends to decrease with time. These changes occur at a rate such that, over a 5-year period, the total voltage change should be negligible.

It can be shown that, in the absence of voltage changes, a 6.25% reduction in power relates to a 6.67% increase in resistance. A 6.67% increase in module resistance, in turn, relates to a 14.6% increase in n-leg resistivity. On this basis, a factor of 1.146 was applied to the resistivity of the n-type ternary material for the calculation of EOL performance.

Figure 67. 5-kwe Reactor Module EOL Performance Parametric Calculations

To meet the system voltage and power requirements shown in Table 30, assuming the system consists of two strings of 8 series-connected modules operating into a matched load resistance, the modules must have the axial component dimensions required to produce a current of 83 amp. A 15-in. module, incorporating 51 thermoelectric couples, meets these requirements when operated at $\bar{T}_H = 1140^\circ\text{F}/\bar{T}_C = 523^\circ\text{F}$ under matched load conditions.

The electrical performance requirements specified previously can be met by specifying $\bar{T}_C$ and varying circuit length, or by maintaining the circuit length of 15 in. and varying $\bar{T}_C$. Parametric calculations were generated for the latter conditions. Results are presented in Figure 67. As can be seen, the power output per module requirement specified in Table 30 can be met with average cold cladding temperature of 548°F .

In addition to the performance calculations discussed previously, an effort to calculate allowable mechanical loads for the reference reactor module was completed during the past quarter. In this analysis, four types of loads were considered: (1) pure tensile loads on the inner cladding, (2) pure compressive loads on the inner cladding, (3) bending moments applied to the inner cladding, and (4) bending moments applied to the outer cladding.

For each type of load, the critical module components were selected which could limit the load due to mechanical failure or excessive strain. The maximum permissible load for each component was then computed. Based on these results, the most critical component was determined.

The results indicated that the module should be restricted to an axial tensile load of 700 lb at operating temperatures. This limit is imposed to prevent creep rupture of the stretch neck in the end-closure region of the module. In addition, the module should be restricted to a bending moment of 180 in.-lb, to limit the deflection at the center of the inner cladding to 0.002 in. Larger deflections of the inner cladding could affect module performance by increasing degradation rates, which are related to circuit component deformation. Because of the lockup between the inner and outer claddings, there is no appreciable difference between a bending moment applied to the inner and outer claddings.

These results contain no safety factors, and must be treated as such. However, based on preliminary calculations, these loads are substantially above expected loads in the 5-kwe system.

a. Electrical Current Data
 Figure 68. Prototype Pump Power Modules
 (Sheet 1 of 2)

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 68a and b, 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 w at 500 amp).

Table 31 presents a summary of performance data from the electromagnetic pump modules. Degradation rates for each parameter were determined from a statistical analysis of experimental data from these modules. Previously established degradation trends for both modules continued during the past reporting period.

The long-term 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 50 controlled thermal cycles on S/N-3, a previously tested pump module, and the various controlled shutdowns of all three modules, indicate that the TEM-14 design is relatively insensitive to thermal cycling.

b. TEM-14B Pump Modules

The TEM-14B Series modules are nearly identical to the TEM-14A Series modules, except that 50% radial thickness tungsten foil diffusion barriers have been 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.

Fabrication of three new pump power modules, TEM-14B S/N-1, 2, and 3, was initiated in early October. These three modules are to be incorporated

b. Power Output Data

Figure 68. Prototype Pump Power Modules
(Sheet 2 of 2)

TABLE 31
PUMP MODULE PERFORMANCE STABILITY SUMMARY

Module	Testing Time (Hours)	Rate of Change: % Per 10,000 Hours			Thermal Cycles	
		R _g	P _e	η_o	Rapid	30°F/Min.
TEM-14A S/N-1	17847	*	-4.8	-6.2	9	0
	14356**	1.1	-3.1	-3.1	5	1
TEM-14A S/N-2	24783	5.5	-4.6	-4.3	13	0

*No open circuit data taken for S/N-1 during first 17847 hours 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 October 16, 1972.

Figure 69. Pump Module External Collector Ring

into an integrated module assembly - electromagnetic pump test to be conducted at Atomics International.

Concurrent with the conductor pin - external collector ring welding development effort, as discussed in Section III-C-3, considerable design effort was expended on the redesign of the external collector ring. The weld development and redesign were required to successfully interface the 3-pump module assembly with the flexible bus assembly of the electromagnetic pump. This interface affects the assembly sequence, as the three external collector rings will be brazed to the flexible bus assembly before the external collector rings are welded to the pins. Thus, the external collector ring design incorporated adequate tolerances to permit assembly to the 12 pins, proper orientation to assemble the 3 pump modules to the brazed flexible bus assembly - external collector rings, and incorporation of the weld preparation "pocket" as defined from the weld development. During the past quarter, the external collector ring was designed as shown in Figure 69. Sufficient quantities were fabricated and shipped to Atomics International for braze development to the flexible bus assembly and final braze of 3 assemblies, including a spare, which will be returned to Westinghouse for final assembly to the 3-pump modules.

The integrated module assembly - pump test involves the operation of the modules in a 2-loop liquid metal system. During the past quarter, hydraulic calculations were performed to determine helical spiral dimensions required to achieve primary (hot-side) and secondary (cold-side) pressure drops specified for this test. TEM-14B primary and secondary flow channel design calculations were submitted to Atomics International, and were approved. The results of these calculations are summarized in Table 32. Fabrication of shroud components has begun.

A thermal analysis, to determine end closure heat losses, has been initiated. This analysis will include the effects of thermal losses through the electrical busses connecting the modules to the pump assembly.

c. Pump Module Parametric Analysis

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

TABLE 32
TEM-14B FLOW CHANNEL DESIGN PARAMETERS

	<u>Primary Channel</u>	<u>Secondary Channel</u>
<u>Flow Parameters</u>		
Fluid	NaK	NaK
Flow Rate	0.146 lb/sec	0.093 lb/sec
Average Temperature	1080°F	516°F
Pressure Drop	0.40 psi	0.26 psi
<u>Geometry Parameters</u>		
Annulus I. D.	0.50 inch	2.14 inches
Annulus O. D.	0.75 inch	2.39 inches
Axial Length	5.6 inches	5.6 inches
Separator, height	0.125 inch	0.125 inch
Inlet Pipe I. D.	0.75 inch	0.522 inch

TABLE 33
PRELIMINARY EOL 5-kwe REACTOR PUMP MODULE REQUIREMENTS

	<u>Per Module</u>	<u>System</u>
Average Module Inner Clad Temperature (°F)	1140	---
Average Module Outer Clad Temperature (°F)	523	---
Pin-to-Pin Load Voltage (volts)	0.195	0.195
Current (amps)	520	1560
Total Power Degradation (pct.)	8.75	8.75

calculations, variations in inner and outer conductor ring radial thicknesses, lead telluride radial thickness, and numbers of thermoelectric couples were considered for modules meeting EOL performance requirements specified in Table 33. Module degradation was accounted for using the same techniques described in the reactor power module parametrics. In this case, however, a total 8.75% degradation rate was assumed.

The study showed that performance (i. e. , efficiency) increased continuously as conductor ring radial thicknesses were increased. Thus, inner and outer conductor ring thickness were selected at 0.225 and 0.075 in. , respectively, on the basis of being able to insure full compaction during processing of the module. (The TEM-14A Series modules employ 0.226 and 0.050 in. inner and outer conductors, respectively.)

The relationships between module weight (including end closures) and overall efficiency for 3-couple and 4-couple modules are presented in Figure 70. As is shown in the figure, the 3-couple modules become more efficient as the lead telluride radial thickness is increased beyond 0.120 in. , for the operating conditions and performance requirements specified.

Optimum dimensions can be selected from these parametric results, based on the trade-off between increasing module weight and decreasing radiator weight as a function of increasing lead telluride thickness.

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 Technology Program. These modules were designed with radial dimensions identical to those of reactor power modules, but with circuit lengths reduced from 15.0 to 4.0 in. The purpose of 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 34. Fifteen TEM-15 Series modules were tested continuously during the last quarter.

Figure 70. Calculated Efficiency and Weight, as Functions of Lead Telluride Radial Thickness, for TEM-14C Pump Modules

TABLE 34
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.

TABLE 34
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 35 lists the operating conditions of all TEM-15 Series modules tested during this reporting period, and summarizes the results of the most recent degradation analysis. Degradation rates were determined, using data recorded through October 16, 1972.

(1) TEM-15A Series Modules

Seven TEM-15A series modules, having processing and design features listed in Table 34, were endurance tested during the past quarter. Electrical power output and overall efficiency data from the first three of these modules are represented in Figures 71a and b, respectively, as a function of elapsed time. All three modules were tested at $\bar{T}_H = 1125^\circ\text{F}/\bar{T}_C = 570^\circ\text{F}$ operating conditions for testing times exceeding 22,000 hr. As can be seen in the figures, excellent long-term performance reproducibility has been achieved in these 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. The most recent data from these modules indicate a tendency to deviate from the characteristic linear power degradation. The rate of power reduction in these modules, having in excess of 20,000 hr testing, appears to be reducing.

TEM-15A S/N-5 has accrued 8457 hr of steady-state operation since completion of 50 controlled thermal cycles. The time at which the cycles were performed is indicated in Figures 71a and b. Analysis of the data shows that the degradation of rates of this module have remained essentially identical to those of noncycled modules.

Testing of two TEM-15A Series modules at modified temperatures continued during the past quarter. Modifications were made, in the previous quarter, to 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 $\bar{T}_H = 1125^\circ\text{F}/\bar{T}_C = 570^\circ\text{F}$, while TEM-15A S/N-4G operating temperatures 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 these modules recorded at each operating temperature level are shown in Figures 72a and b, respectively. Degradation rates associated with operation at the previous temperatures have been well established.

TABLE 35

TEM-15 PERFORMANCE ANALYSIS SUMMARY

Module	Test Conditions $\bar{T}_H / \bar{T}_C (^{\circ}\text{F})$	Testing Time (Hours)	Rate of Change: % Per 10,000 Hours			Thermal Cycles	
			R_g	P_e	η_o	Rapid	30 $^{\circ}\text{F}/\text{min.}$
TEM-15A S/N-1	1125/570	23,716	31.6	-16.9	-14.9	11	1
TEM-15A S/N-4D	1125/570	23,958	33.2	-17.4	-15.1	9	2
TEM-15A S/N-4F	1125/570	10,146	29.2	-20.2	-15.8	5	2
	1200/637	2,905	51.1	-27.8	-28.0		
TEM-15A S/N-4G	1025/480	5,051	10.1	-4.5	-2.5	5	0
	1075/525	2,689	16.0	-8.0	-8.4		
TEM-15A S/N-4H	1125/570	3,618	22.6	-14.5	-12.3	1	1
TEM-15A S/N-4J	1125/570	2,753	24.1	-7.8	-7.9	0	0
TEM-15A S/N-5	1125/570	22,653	31.3	-16.5	-14.3	13	51
TEM-15B S/N-4A	1125/570	18,207	14.6	-7.7	-6.8	6	1
TEM-15B S/N-4B	1125/570	13,117	18.4	-12.9	-10.7	5	1
TEM-15B S/N-4C	1125/570	9,379	23.2	-15.8	-12.3	5	0
TEM-15B S/N-4D	1125/570	2,632	*	*	*	0	0
TEM-15C S/N-4A	1125/570	4,433	20.2	-10.6	-7.5	5	1
	1125/570**	5,269	24.2	-17.1	-13.9		
TEM-15C S/N-8	1125/570	21,217	25.6	-14.3	-12.0	10	1
TEM-15D S/N-4	1125/570	19,427	20.5	-11.4	-8.9	9	1
TEM-15H S/N-4	1125/570	13,506	23.5	-14.4	-11.9	5	1

All data included through October 16, 1972.

*Module design prohibits meaningful stability analysis.

**Data recorded after overtemperature to 1360 $^{\circ}\text{F}$.

a. Comparison of Power Output Data

Figure 71. Long-Term TEM-15A Series Modules
(Sheet 1 of 2)

b. Comparison of Overall Efficiency Data
Figure 71. Long-Term TEM-15A Series Modules
(Sheet 2 of 2)

a. Comparison of Power Output Data
Figure 72. Recent TEM-15A Series Modules
(Sheet 1 of 2)

b. Comparison of Overall Efficiency Data
Figure 72. Recent TEM-15A Series Modules
(Sheet 2 of 2)

After establishing a $\bar{T}_H = 1075^\circ\text{F}/\bar{T}_C = 525^\circ\text{F}$ degradation rate, 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 whether 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.

Identical TEM-15A Series modules have accrued sufficient testing times at various operating temperatures to permit a determination of degradation rates as a function of operating temperature. Based on the hypothesis that the major degradation mechanism is either diffusion or chemical reaction rate controlled, the relationship between degradation rate and operating temperature for a particular module design and testing mode should follow an equation of the form

$$\text{Rate} = A \exp(-Q/RT) \quad \dots (6)$$

In this equation, known as the Arrhenius relationship, A is a "frequency factor," Q is an "activation energy," R is the ideal gas constant, and T is absolute temperature. The rate of degradation refers to a percent increase per 10,000 hr for internal resistance, or a percent decrease per 10,000 hr in the case of power output. For this analysis, the rate controlling temperature is considered to be the average hot cladding temperature.

An Arrhenius plot of TEM-15A resistance degradation rate data as a function of inverse absolute temperature is shown in Figure 73a. A similar plot of TEM-15A power degradation rate data is presented in Figure 73b. Using a least squares technique on the semi-log plots, straight lines were fit for both parameters. A linear relationship was obtained by modifying the Arrhenius equation to logarithmic form, yielding

$$\log(\text{Rate}) = \log A - (Q/R)(1/T) \quad \dots (7)$$

a. Power Degradation

Figure 73. TEM-15A Degradation Rates, as a Function
($10^{-3}/^{\circ}\text{R}$) of Inverse Absolute Temperature
(Sheet 1 of 2)

AI-AEC-13046

b. Resistance Degradation

Figure 73. TEM-15A Degradation Rates, as a Function ($10^{-3}/^{\circ}\text{R}$) of Inverse Absolute Temperature (Sheet 2 of 2)

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This is a linear relationship for the log of the rate as a function of inverse temperature having a slope of $(-Q/R)$ and a y-intercept of $(\log A)$.

Based on the least squares analysis of the degradation rates of TEM-15A static-tested modules, the frequency factors and activation energies for resistance and power degradation were calculated, and are presented in Table 36. The frequency factors have no real physical significance, but relate to the degradation rates as the operating temperatures approach infinity. The activation energies indicate the sensitivity of the degradation rates to changes in temperatures. The higher the activation energy, the greater the sensitivity. The "best fit" lines are shown in Figures 73a and b, along with the experimental data from which the lines were derived.

TABLE 36
TEM-15A ARRHENIUS RELATIONSHIP CONSTANTS

	Frequency Factor (A) (pct/10, 000 hrs)	Activation Energy (Q) (cal/gm mole)
R_g	1.2×10^8	27, 000
P_e	5.4×10^8	30, 000

NOTE: Analysis is based on data taken through October 16, 1972.

Another TEM-15A Series module, 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 whether the elimination of air leakage at the pin bases has a measurable effect on performance levels or stability. The module has accrued 3618 hr at $\bar{T}_H = 1125^\circ \text{F}$ / $\bar{T}_C = 570^\circ \text{F}$. No definite conclusion has yet been reached on the effectiveness of glass-to-metal seals in a TEM-15 module. Electrical power output and overall efficiency data from TEM-15A S/N-4H are presented in Figures 72a and b, respectively.

The final TEM-15A Series module, 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.

Recent data from TEM-15A S/N-4J indicate that processing at lower temperatures and higher pressures (relative to the standard autoclave cycle) may change the degradation characteristics of the module. Data presented in Table 35 indicate that, after 2753 hr of operation, TEM-15A S/N-4J has a power degradation rate nearly one-half as large as the rate displayed by identical modules tested at the same temperature — $\overline{T}_H = 1125^\circ\text{F} / \overline{T}_C = 570^\circ\text{F}$. A closer examination indicates that, although the resistance degradation rate of TEM-15A S/N-4J is identical to that of other nonbarrier TEM-15A series modules, the voltage appears to be increasing much faster, whereas the initial voltage increase rate of a TEM-15A processed under standard conditions is between 4 and 5%/10,000 hr, TEM-15A S/N-4J has an initial voltage increase rate nearly twice as high. However, it is impossible to speculate, at this time, whether this condition will continue, or whether it is a short-term phenomenon.

Electrical power output and overall efficiency data from TEM-15A S/N-4J are presented in Figures 72a and b, respectively.

(2) TEM-15B Series Modules

The TEM-15B designation applies to all sub-length reactor technology modules incorporating tungsten foil diffusion barriers positioned between two mica electrical insulators. Four TEM-15B Series modules continued on test throughout this reporting period. The individual design features of each module are listed in Table 34.

Electrical power output and overall efficiency data from the first three tungsten foil barrier modules are presented in Figures 74a and b, 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 35.

a. Comparison of Power Output Data

Figure 74. TEM-15B Tungsten Foil Barrier Modules
(Sheet 1 of 2)

b. Comparison of Efficiency Data

Figure 74. TEM-15B Tungsten Foil Barrier Modules
(Sheet 2 of 2)

a. Comparison of Power Output Data

Figure 75. Improved Performance Stability TEM-15 Series Modules
(Sheet 1 of 2)

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 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.

(3) Additional Modules

In addition to the tungsten foil barrier and nonbarrier modules, 4 one-of-a-kind modules were tested continuously during the past quarter. These are the last 4 modules listed in Table 35. 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 34, 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 EBVD barriers, tungsten n-washer contacts, and increased mica washer thicknesses. Power output and overall efficiency data from these modules are presented in Figures 75a and b, respectively.

Data from TEM-15C S/N-4A have been broken into two line segments, as shown in Table 35. After 4433 hr of operation at constant $\bar{T}_H = 1125^\circ\text{F}$ operating conditions, the module was inadvertently overtemperated to 1360°F , due to a power controller malfunction. Subsequent data, recorded under constant $\bar{T}_H = 1125^\circ\text{F}$ operating conditions, indicate that the degradation rates have increased as a result of the high temperatures encountered. This increase is attributed to the establishment of additional diffusion paths during high-temperature operation. Prior to the overtemperature, TEM-15C S/N-4A, an EBVD tungsten barrier module, exhibited the lowest power degradation rate yet recorded for a TEM-15 Series module using standard 0.005-in. thick mica insulators.

b. Inner Cladding Joint Qualification

A precision standard for ultrasonic evaluation of the joint bond region, which simulates both localized defects and large-area unbonding, was designed, procured, and evaluated. It was found that good reproducible indications could

b. Comparison of Overall Efficiency Data

Figure 75. Improved Performance Stability TEM-15 Series Modules
(Sheet 2 of 2)

be obtained for the localized defects, but that indications of unbonding are far more difficult to interpret; the reliability of the latter interpretation depends to a great degree on operator skill and experience.

Ultrahigh-vacuum furnaces and cycle-control equipment for performing isothermal annealing tests and thermal cycling tests were identified for this task. Setup and calibration of this equipment is currently in work.

Information and data from reference programs^(1,2) which have considered the growth rates of intermetallic bond regions in refractory metal - austenitic stainless steel transition joints were used to estimate the growth rates of our Ta-10W - Type 316 stainless steel joint in the temperature vicinity of 650°C. Based on these estimates, temperatures of 750, 800, and 850°C have been selected as the 2000-hr exposure temperatures for specimens on which the actual Ta-10W - Type 316 stainless steel joint growth rate will be measured. Growth measurements will be based almost exclusively on electron beam microprobe traces, as the estimated growth values are too small for obtaining accurate results with optical techniques. The use of higher exposure temperatures to produce larger growth values, and perhaps simplify the growth measurements, is precluded by an apparent sharp change in the parabolic growth rate constant - reciprocal temperature slope in the vicinity of 850°C. If this change truly exists for the Ta-10W - Type 316 stainless steel case (as must be assumed at present), then data obtained above 850°C cannot be back-extrapolated to give the growth rate of 650°C (which is the information sought by the experiment).

A study was conducted of available information from programs which have considered the effects of thermal cycling on refractory metal - stainless steel transition joints.^(3,4) In one case,⁽³⁾ a 3.0-in. ID by 0.095-in. wall Nb-1Zr - Type 316 stainless steel joint was subjected to 20 thermal cycles between 149 and 843°C at rates of 11.6 (heating) and 5.8 °C/min. (cooling), 500 more cycles between 788 and 871°C at 16.7 °C/min., and finally 58 cycles identical to the first 20. No structural defects were found in this specimen after cycling, but a 0.052 to 0.055-in. radial reduction had occurred in the joint region.

In a second, more extensive, program,⁽⁴⁾ 1.75-in. ID by 0.123-in. wall Ta - Type 316 stainless steel joints were subjected to 100 thermal cycles between 121 and 732°C at a rate of 5.1 °C/min. All four specimens remained

helium leaktight after cycling, but all four had developed fissures (cracks) in the Type 316 stainless steel ID adjacent to the intermetallic bond region, and one had also developed fissures in the Ta OD adjacent to the bond region. No unbonding of the intermetallic region itself was observed. All four specimens exhibited diametral reductions in the joint region, ranging from 0.005 in. (specimens internally helium pressurized to 250 psig at 732°C) to 0.017 to 0.065 in. (specimens tested with open bores).

In considering the applicability of these results to the tubular module inner cladding, we have, in the TEM-X case, a much smaller ID (0.750 in.), while maintaining a comparable wall thickness (0.091 in.). These, combined with the considerably reduced upper temperature limit for cycling (593°C), mean that the radial displacements due to thermal expansion will be very much smaller, and hence the extent of the yielded zone and amount of plastic flow in the TEM-X joint region will be far less than in the reference cases. Furthermore, the strength of the Ta-10W refractory metal component in TEM-X is substantially higher than that of the Nb-1 Zr or Ta reference specimens (67/37/35 kpsi, 0.2% YS at RT; 80/50/45 kpsi, UTS at RT; typical values for comparable specimens),⁽⁵⁾ which will further reduce the extent of the yielded zone, the amount of plastic flow, and the probability of fissuring in the refractory-metal side of the joint. In defense of the preceding argument is the fact that 8 TEM-X modules with Ta-10W - Type 316 stainless steel inner claddings are currently operating on test, with up to 13,000 hr of operating temperature exposure and up to 60 thermal cycles, without any indications of transition joint failure.

The possibility of developing diametral shrinkage in the TEM-X transition zone is not quite so easily dismissed; although such shrinkage would be far less than in the reference cases, it clearly could be non-zero. An immediate look into this situation was taken by making ID profile measurements on a TEM-X cladding which was subjected to 50 thermal cycles between 250 and 593°C at 16.7 °C/min. in a liquid metal loop system at Atomics International. The results indicated that no significant diametral changes (localized to the transition zone) had occurred. However, a more precise evaluation of this situation will be made during measurement of specimens from experiments conducted under this task.

A tensile specimen design for evaluating the transition joint was evolved, in conjunction with mechanical testing consultants at the Westinghouse Research and Development Center, and tensile grips for this specimen configuration are being procured.

Work was performed at Atomics International on determining the effect of hydrogen-containing NaK on the Ta-10W alloy itself. Samples of Ta-10W were exposed at 649° C to NaK containing 3, 30, and 300 ppm of hydrogen. Pre- and postexposure chemical analyses and mechanical tests indicated that, although detectable property changes were produced by exposure to the highest hydrogen concentration, no embrittlement or other deleterious consequences have occurred.

Six Ta-10W - Type 316 stainless steel single-joint specimens, ordered for this qualification program, were received near the end of this period. Dimensional inspection and nondestructive testing (including x-ray radiography, liquid penetrant, and ultrasonic examination) of the as-received specimens are currently in work.

c. Mica Processing

A problem concerning rejection of phlogopite mica module components processed in the standard 800° C, 1-hr vacuum heat treating cycle, due to wrinkling, blistering, and/or delamination, has been developing to intolerable proportions in the last year. In response to this condition, an experimental study was undertaken to seek an alternate process which would remove absorbed water and/or volatile impurities as efficiently as the standard vacuum cycle, but which would yield a larger percentage of acceptable components. Briefly, the following were found:

- 1) No improvement could be expected in the response of available mica to the standard vacuum process
- 2) Reducing the heating rate and/or holding temperature of the vacuum cycle did not reduce the amount of physical damage suffered by the mica components
- 3) An 800° C air heat treating - vacuum cooling cycle was developed which did not cause physical damage to the mica components.

Since the thermoelectric material is the module element most responsive to contamination, a series of high-temperature die tests were implemented, using lead telluride and mica. These tests were designed to evaluate the compatibility of mica, both heat treated in air and vacuum, with n- and p-type lead telluride. Both light and dark mica were used in the tests, whose results are under evaluation.

d. Welding Development

A series of welding experiments were conducted in connection with the conductor pin - external collector ring joint for the 3-module assembly of TEM-14B electromagnetic pump power supplies. In an attempt to minimize external I^2R losses in the high-current circuit, experimental TIG and EB welds were performed on nickel conductor pin - copper collector ring specimens (as opposed to the nickel pin - nickel ring combination used in previous pump modules). In the TIG welding case, heat inputs far in excess of those normally used or allowed for thermoelectric module welds were required to obtain fusion in the self-chilling copper ring, and it was felt that damage to either the module or the collector ring - bus assembly braze joint could too easily occur under such conditions. The EB welding experiments indicated a severe problem in obtaining simultaneous fusion of both pin and ring, when using the assembly clearances anticipated to be required for the 3-module array. At this point, it was decided that, although a satisfactory nickel-copper joining procedure could eventually be developed, the amount of development required was beyond the existing work scope, and the schedule could not tolerate the time required, so the nickel-copper approach was abandoned.

A series of EB welds was then performed on nickel pin - nickel ring specimens, anticipating that weld heat input to both module and braze joint would be minimal for the EB process. Although this indeed appeared to be the case, and adequate penetration was easily obtainable, two problems were encountered. First, burnoff of the thin web around the conductor pin hole at the collector ring ID invariably occurred, with the molten web material running down the inner surface of the ring (where it could drip onto the module end face and/or retaining ring stretch neck); second, the welding parameters could not be fixed for the total range of anticipated assembly clearances. It was thus decided, in the

interest of reliability and simplicity, to work with a nickel pin - nickel ring TIG welded joint. A design procedure was evolved in which a 0.1-in. wide by 0.1-in. deep by 240° "pocket," oriented opposite to the collector ring ID, was milled around each conductor hole; the pin was welded to the bottom of the pocket with a simple fusion pass, after which the pocket was filled in with a high-nickel-content alloy, to develop an electrical contact area ~ 1.5 times the cross-sectional area of a conductor pin. Several demonstration joints of this type were welded with satisfactory results, and this approach (with slight modifications to ease the assembly fitup problems of the 3-module array) was committed for the TEM-14B application.

A weld specimen of precisely the final joint design was then fabricated, and a series of qualification welds performed. During this work, it was determined that shrinkage cracks at the beginning and end of the initial fusion pass could be eliminated by adding a significant volume of ASTM B304 Class ER Ni-3 filler metal^{*} at these points, and this detail was incorporated into the final procedure. Completed qualification welds were sectioned, examined metallographically, and found to be satisfactory. An electrical resistance of 0.07 m Ω was measured from the pin to the collector ring. Accordingly, the joint design and welding procedure were designated as qualified for the TEM-14B assembly.

e. Diffusion Barriers

Operator orientation and training, and acceptance testing of the new EBVD system, were conducted at the vendor's plant during the last week of September. A number of malfunctions occurred, and some system deficiencies were discovered during this period; these were either corrected on the spot, or scheduled by the vendor to be corrected prior to shipment. The system was delivered to WANL in October, and moved into a new laboratory reserved for diffusion barrier work. Electrical and plumbing installation have been completed, and system setup, checkout, and debugging are currently in work. Evaporation experiments are scheduled to begin as soon as the installation of a large-capacity liquid nitrogen storage tank is completed.

*This alloy is also used to fill in the pocket above the end of the conductor pin.

f. Glass Seal Development

During this reporting period, the glass seal on an 8-pin flat-plate mockup failed to remain helium leakright (HLT) after 1468 hr of life test at 400°C. Subsequently, a second similar mockup was tested for 326 hr, and was also found to be non-HLT. A seal made using the original leaded glass (without added stiffener) was also found to be non-HLT after 129 hr at 400°C. Since the actual temperature experienced by a seal during module test lies in the 300 to 400°C range, a hot isostatic pressurized (HIP) mockup, MEM-4, was set up to check the response of the seal under conditions more comparable to those which the seal must serve in an actual module. To date, this seal has remained HLT after 2080 hr at 400°C. Obviously, the glass seal is subjected to a more stringent test, using a slip-fit mockup (i. e. , assembly clearances to be filled by the glass) than an HIP end which is compacted. The test is continuing at 400°C. In addition, MEM-3 was placed on test after 3634 hr at 500°C. This latter mockup has 1000 hr of life test, with the last HLT check at 983 hr.

The glass pre-forms which will be used to seal future modules have been received. The use of these 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. The pre-forms were made in two half shells, which permits assembly of the glass to the pin annulus after the collector ring is welded to the module. Two additional HIP mockups are in process. These mockups, using glass pre-forms, will be used for further life testing at 300 and 400°C.

g. Ternary Thermoelectric Material Development

(1) p-Type PbTeSe

Several melts of $\text{PbTe}_{0.965}\text{Se}_{0.05}\text{Na}_{0.01}$ were prepared in three different-size batches, early in the program. Some difficulty was encountered during the quenching step, when the quartz tube would explode. Those ingots that were not destroyed were reduced to powder, and qualification test bars were pressed for electrical property measurements. Preliminary analyses indicated that the electrical properties were affected by the amount of material charge and/or the thickness of carbon deposited on the quartz tube.

Because of the explosion of quartz tubes during quenching, the step-by-step preparational procedures were reviewed for p-type PbTeSe. The intent was to change procedures to prevent further explosions, and provide reproducible electrical properties from ingot to ingot. Two changes were instituted to improve material preparation and performance. Metallic sodium was used as the dopant in the formulation, instead of a sodium lead alloy, and the size of the melts was reduced.

Twelve consecutive ternary p-type liquid melts in quartz tubes at 1000°C were successfully quenched in cold water without exploding. Each of the ingots of ternary materials (APX 10-21 through APX 10-32) were crushed in an argon atmosphere (dry box), and separated into powder for hot pressing (-20 +35, U. S. Standard sieve size) and powder for cold pressing (-50 +270). Both powders were heat treated in hydrogen, prior to hot or cold pressing.

Four qualification bars were cold pressed and sintered from each lot of -50 +270 powder. Electrical characteristics and x-ray diffraction tests were performed with the qualification bars from lot numbers APX 10-21 through APX 10-29. Figure 76 presents a typical resistivity and absolute Seebeck coefficient vs temperature curves for APX 10 p-type ternary material. Comparison with RCA reference curves indicates the APX 10 material is more highly doped, for optimum operation at the maximum operating temperature of TEM modules, 640°C (1185°F). Since the electrical characteristics are similar to p-type lead telluride, increases in material efficiency would occur, as a result of the reduced phonon thermal conductivity which is produced by alloying the lead telluride.

(2) n-Type PbGeTe

As a result of the destructive examination and evaluation of the TEM-15F S/N-4 module, a new formulation of the ternary material was decided upon. The new alloy has a specific composition of $\text{Pb}_{0.951}\text{Ge}_{0.05}\text{Te}(\text{PbI}_2)_{0.001}$. Ten ingots of this formulation have been prepared, in various charge sizes up to ~1 lb of material. All ingots were successfully quenched, with the carbon coating intact on the quartz tube. These ingots have been reduced to powder, and test bars fabricated for electrical property measurements. All the electrical test bars were processed by pressing un-heat treated powder and heating

Figure 76. Electrical Properties vs Temperature for APX 10-23

the bars in hydrogen for 2 hr at 575°C. This treatment duplicates the standard n-type process procedure, and minimizes the temperature at which the material may lose germanium through sublimation. Electrical test bars from Ingots ANX 1-9 through ANX 1-13 have been tested, over the temperature range of 295 to 850°K. The results of these tests, except for ANX 1-11, are shown in Figure 77, along with the RCA n-type ternary reference curves (solid lines), for Seebeck coefficient and resistivity. ANX 1-11 produced very anomalous electrical resistivity results, although the Seebeck coefficient data obtained on three specimens were identical, but did not agree with the reference data. The data for the other 4 ingots show good agreement with the Seebeck coefficient reference curve. However, there is some scatter in the electrical resistivity measurements. Whether the resistivity differences arise because of density variations, cracks, processing changes, or measurement errors is not known at this time. The reason for the mixed results, along with the ANX 1-11 properties, will be looked at in some depth.

X-ray diffraction patterns were run on each of the ANX 1 ingots after the material was ground to -50 +270 mesh powder. The x-ray films produced very broad and diffuse diffraction lines in the back-reflection area, with no resolution between the α_1 and α_2 lines. The broad lines made accurate determination of the lattice parameters very difficult. Powder from one ingot, ANX 1-9, was sealed in a Vycor tube and homogenized for 100 hr at 700°C. The resultant x-ray film of this powder gave very sharp and good resolution between α_1 and α_2 diffraction lines. The lattice parameter was calculated to be 6.4401 A. U. The lattice parameter obtained from the standard n-type material was measured to be 6.4608 A. U. This decrease in lattice parameter is to be expected, because the atomic size of the germanium is smaller than the lead atomic size which the germanium replaces in the 5 mol % ternary PbGeTe alloy. A review of the literature will be conducted to check for additional lattice parameter determinations that may have been made in this system. In addition, an as-quenched ingot will be annealed in-situ, to establish an accurate lattice parameter measurement on a material that cannot lose GeTe during heat treating.

(3) Chemistry

Final chemical analyses of the major constituents of the ANX 6 alloy used in the TEM-15F S/N-4 module were received. The results of the analyses, and

Figure 77. Electrical Properties vs Temperature for ANX 1 Alloys

the composition of the material as formulated, are shown in Table 37. These analyses of germanium tend to confirm the x-ray diffraction results which indicated that germanium has been lost from the alloy during processing. The chemical results indicate approximately a 20 to 22% loss in germanium for the heat treated powder, and a 28% loss for the washers that were made from the powder, heat treated again, and used in the module. There appears to be no change in germanium content during operation.

TABLE 37
CHEMICAL ANALYSIS OF ANX 6

	Mol Fraction			
	Pb	Te	Ge	I
1. ANX 6 Formulation	0.950	1.000	0.050	0.0014
2. Powder from ANX 6 Heat treated 2 hours at 750°C	0.9602	1.001	0.039	0.0012
	0.9595	1.000	0.040	0.0012
3. Unused Washers ANX 6 2 + washers heat treated two hours at 585°C	0.9625	1.001	0.036	0.0012
	0.9644	1.003	0.033	0.0011
4. ANX 6 Washers from TEM-15F S/N-4 2 + 3 + 4865 hours test	0.9630	1.001	0.036	0.0011
	0.9630	1.002	0.035	0.0012

(4) Hot Pressing

Modifications to a vacuum furnace were completed, to provide a hydrogen atmosphere at elevated temperatures (700°C) for hot-pressing operations. Safety procedures and precautions were reviewed and judged by committee members, and the facility was sanctioned for future hot-pressing work in hydrogen.

Initial procedural checkout tests were performed with standard n-type PbTe. Quenched n-type material was reduced to powder and screened in an

a. Before Machining

b. After Machining

Figure 78. Hot-Pressed n-Type PbTe Cylinders

argon dry box to produce powder lots of -20 +35, and -50 +270 (U. S. Standard Sieve Size). Qualification bars were cold pressed, heat treated, and tested for Seebeck voltage and resistivity measurements to establish a baseline for cold-pressed n-type PbTe.

Three successful hot-pressing runs were made with n-type PbTe. In each experimental run, n-type hot-pressing powder was loaded into a 5/8-in. diameter die, and compacted under 6500 psi pressure for 1/2 hr at a temperature of 700° C. Temperature profiles were controlled and monitored on both the heatup and cooling cycles. Crack-free solid n-type cylinders (0.625 in. diameter by 0.7 in. length) were obtained from all three experimental runs. To verify the integrity and density of the hot-pressed cylinder, the diameter was turned down on a lathe from 0.625 to 0.273 in. Figure 78 illustrates the comparative sizes of cylinders before and after machining. The relative non-smooth surface of the larger cylinder is caused by the graphite surfaces of the punches and die. Weight and volume measurements indicated a theoretical density of 99.1% for the machined cylinder, compared to 98.95% for the total 5/8-in. diameter pellet.

After qualifying the hot-pressing facility and procedures with n-type PbTe powder, two changes were made for the APX hot-pressing runs. First, the size of the hot-pressing die was reduced from 5/8 to 1/2 in. diameter, to reduce the quantity of hot-pressing powder for each run. Second, the time at temperature was increased to 60 min, rather than 30 min, to increase the density of the hot-pressed APX 10 p-type pellet.

Some difficulty was encountered in hot pressing APX 10 p-type material under 6500 psi at 700° C. The pellet was partially hot pressed and nonhomogeneous, with one end containing considerable voids. Consequently, the pressure was increased, in small increments, for successive pressings until the APX 10 p-type powder was successfully hot pressed. Figure 79 illustrates the surface condition of the bottom end of a 1/2-in. diameter APX 10 p-type pellet after hot pressing for 1 hr under varying pressure, from 6500 to 7500 psi. Once the pressing conditions were established, the size of the die was increased to 5/8 in. diameter, so that hot-pressed pellets would be large enough to be machined to a size compatible with the thermal conductivity apparatus (0.500 in. diameter by

a. Pressed for 1 hr at 700°C
and 6500 psi

b. Pressed for 1 hr at 700°C
and 7000 psi

c. Pressed for 1 hr at 700°C
and 7250 psi

d. Pressed for 1 hr at 700°C
and 7500 psi

Figure 79. Bottom Views of 1/2-in. Diameter APX Pellet After
Hot Pressing Under Varying Load Conditions

0.625 in. minimum length). Two samples of APX 10-26 and APX 10-27 ternary materials are shown in Figure 80, after hot pressing and machining to 0.500 in. diameter.

Seebeck voltage characteristics were checked by heating hot-pressed specimens in the high- ΔT apparatus, and comparing the results produced with cold-pressed and sintered qualification bars. One such comparison is shown in Table 38, which illustrates the performance of APX 10-24 ternary material vs the standard qualification bar. From the resultant Seebeck voltage vs temperature, it appears that the hot-pressing procedure does not produce deleterious effects with regard to output voltage. Both ternary materials were from the same ingot, and are very close in performance characteristics. Seebeck coefficient and electrical resistivity vs temperature measurements will also be performed on the hot-pressed material, for comparison with data obtained on cold-pressed specimens.

(5) Compatibility Experiments

Graphite dies and punches were fabricated for the compatibility experiments with APX and ANX ternary materials. Twelve sets of matching components were used for the APX tests. Ternary p-type materials were obtained from APX 10-25, APX 10-26, and APX 10-27 ingots. Pellets (0.50 in. diameter by 0.30 in. length) were pressed from powder (-50 +270, U. S. Standard sieve size) and sintered in hydrogen prior to compatibility testing.

Short-term compatibility experiments were performed in a vacuum furnace with pure hydrogen as a cover gas. Samples of each material were loaded into a graphite die with an APX 10 p-type pellet. Graphite punches were used to maintain contact between the ternary p-type pellet and the respective compatibility sample. The sequence of operations started by evacuating the air from the furnace at room temperature and backfilling with pure helium. After a second evacuation and backfill with helium, the pure hydrogen was introduced and continuously flowed through the furnace throughout the compatibility test run. Electric heaters within the furnace controlled the graphite temperature during warmup and at the test temperature. An Instron compression test apparatus was used to apply 3000-psi pressure to the graphite punches, while both the ternary p-type pellet and compatibility sample were at 700°C.

Figure 80. Hot-Pressed Cylinders of APX 10-26
and APX 10-27 p-Type Ternary Material,
Machined to 0.500 in. Diameter

TABLE 38
COMPARATIVE TEST OF HOT-PRESSED APX 10-24 WITH COLD-
PRESSED QUALIFICATION BAR

	APX 10-24 Qualification Bar Q 1076	APX 10-24 Hot Pressed Cylinder 006
$T_{H1}(^{\circ}\text{C})$	319	319
$T_{C1}(^{\circ}\text{C})$	26	26
$\Delta T (^{\circ}\text{C})$	293	293
Voltage (mV)	39.2	38.6
$T_{H2}(^{\circ}\text{C})$	593	593
$T_{C2}(^{\circ}\text{C})$	32	32
$\Delta T_2(^{\circ}\text{C})$	561	561
Voltage (mV)	110.7	110.7
$T_{H3}(^{\circ}\text{C})$	651	651
$T_{C3}(^{\circ}\text{C})$	34	34
$\Delta T_3 (^{\circ}\text{C})$	617	617
Voltage (mV)	126.8	127.5

TABLE 39
ATOMIC ABSORPTION ANALYSIS FOR SODIUM

		Weight Percent No.
Qualification Specimens from	W308	0.061
Original APX 4 Powder Lot	Q757	0.066
Specimens Reclaimed from 15E-4 Module Washers	Q617	0.058
	Q618	0.055
Control Specimen Reclaimed from Unassembled Washers	Q621	0.061
Standard p-type lead telluride powder	No. 1	0.061
	No. 2	0.061
	No. 2R	0.061
APX 10-8 Powder	---	0.058
13B	---	0.060
17	---	0.069
19	---	0.053
22	---	0.062
23	---	0.061
25	---	0.057
27	---	0.058
APX 28	---	0.062
30	---	0.052
31	---	0.062
32	---	0.062

NOTE: APX 4 and APX 10 sodium content as formulated was 0.069 weight percent

Each compatibility material was tested with a ternary p-type pellet on three separate occasions for test runs of 2, 4, and 6 hr at 700°C in a hydrogen atmosphere. After cooldown, the graphite die was removed from the test chamber and potted in an epoxy compound. The assembly was later sectioned into two halves along the longitudinal axis, and polished. One section was submitted for electron beam probe, and the other half was tested for Seebeck voltage output.

As of the end of this period, all samples of tungsten, phlogopite mica, and standard n-type lead telluride have been compatibility tested with APX 10 ternary material. The analysis of the data is in process.

The indicated sodium dopant level of 0.03 wt%, which was found in the APX washers during destructive examination of TEM-15E S/N-4, was a potential problem, considering the fact that the amount of sodium added to the ternary composition, $\text{PbTe}_{0.96}\text{Se}_{0.05}\text{Na}_{0.01}$, was 0.069 wt%. A discussion was held with the chemistry group which does the sodium analysis. During this meeting, the accuracy of the spectrographic method used was estimated to be from 1 to 3 times the amount reported. This tolerance was unacceptable for determining an element as important as the dopant.

Another method, atomic absorption, with a reported accuracy of 5 to 10% of the amount present in the sample, was decided upon to analyze the previous samples, as well as a group of new ternary p-type (composition $\text{PbTe}_{0.965}\text{Se}_{0.05}\text{Na}_{0.01}$ and TEGS-2P) material. The results of this work are shown in Table 39. The results obtained using the atomic absorption method are higher, and compare well with the amount of sodium added to the melt.

(6) X-Ray Diffraction

During this report period, it was determined that the extra and unidentified lines reported for the ternary p-type material used in TEM-15E S/N-4 were due to iron K_{α} lines inherent in the x-ray unit, and uniquely diffracted by the lead telluride crystal structure.

The precision with which the lattice parameter of PbTeSe can be measured was determined, measuring the lattice parameter of four powder samples from the same melt. The data yielded a result of +0.0005 A. U. for the measurement

Figure 81. Lattice Parameter vs Mole Fraction PbSe in PbTe

precision of this technique. Powder from a group of 11 ingots with the APX 10 composition ($\text{PbTe}_{0.965}\text{Se}_{0.05}\text{Na}_{0.01}$) were submitted for lattice parameter determinations. One portion of powder from each ingot was not heat treated, while a second portion of powder from each ingot was heat treated in flowing hydrogen for 1 hr at 550° C. These lattice parameter values were averaged, and plotted on an expanded plot (Figure 81) of lattice parameter vs mole fraction PbSe in PbTe, as determined by Steininger.⁽⁶⁾ The lattice parameter, 6.4438 A. U., obtained from 11 un-heat treated APX 10 samples agrees very well with the value of 6.4435 A. U., which represents 5.2 mole % on the curve. These data indicate that the ingots are reproducible, and contain the as-formulated composition value of 5 mole % selenium.

The 11 samples that were heat treated, as mentioned previously, yielded diffraction lines that were broader than those obtained on the un-heat treated powder. The average of 11 lattice parameters determined from the middle of the line was calculated to be 6.449 A. U., which represents ~3.7 mole % PbSe. However, if the lattice parameter were calculated using the spacing of the leading and trailing edges of the broad line, values of 6.4545 and 6.4444 A. U. were obtained, which represent 1.7 and 4.7 mole % PbSe, respectively.

Powders from two different ingots were heat treated for 2 and 4 hr at 550° C in flowing hydrogen to check whether the lattice parameter increases, as should be the case if selenium were being lost through vaporization. The results of this study are shown in Table 40. The lattice parameter measurements indicate that the additional heat treatment time at a temperature of 550° C had little or no effect on the lattice parameter. Homogenization and higher-temperature heat treatments are planned to further study their effect on line broadening and apparent lattice parameter movement, which would indicate a composition change.

4. Hydrogen - Ta-10W - NaK Partition Experiment

The purpose of this experiment is to determine the distribution of hydrogen in Ta-10W and NaK, when cooled from reactor operating conditions to room temperature. It appears, analytically, that the most critical condition occurs when the system cools. At operating condition, 1200° F, the solubility of hydrogen is low in Ta-10W, and relatively high in NaK. When the system cools, as during a shutdown, the solubility of hydrogen decreases in NaK, and increases in Ta-10W.

TABLE 40
LATTICE PARAMETER MEASUREMENTS FOR APX 10 MATERIAL

<u>Ingot</u>	<u>Heat Treatment</u>	<u>Lattice Parameter</u>	<u>Line Breadth</u>
APX 10-21	None	6. 4427	None
	1 hr at 550°C in H ₂	6. 4486	6. 4517/6. 4452
	2 hrs at 550°C in H ₂	6. 4477	6. 4507/6. 4440
	4 hrs at 550°C in H ₂	6. 4472	6. 4503/6. 4441
APX 10-13 B	None	6. 4429	None
	1 hr at 550°C in H ₂	6. 4483	6. 4534/6. 4486
	2 hrs at 550°C in H ₂	6. 4487	6. 4527/6. 4447
	4 hrs at 550°C in H ₂	6. 4482	6. 4526/6. 4438

Five sealed stainless steel capsules, containing NaK and Ta-10W, were soaked for >300 hr at 1200°F in controlled-atmosphere retorts. Hydrogen was bled into each retort, and maintained at specific partial pressures for the duration of the thermal soak. As hydrogen diffused through the wall of the capsules and into solution with the NaK, the pressure in the retorts decreased. In time, the hydrogen in the gaseous state and in solution came to equilibrium, and the pressure decrease stopped. The pressure was adjusted continually to maintain the pressure at a predetermined level throughout the test. Tests were run at three pressures which, according to Webb,⁽⁷⁾ would be in equilibrium with 3, 30, and 300 ppm of hydrogen in solution in the NaK. The capsules were then cooled from 1200°F to room temperature at a rate in the same range expected during a reactor shutdown, -30°F/min. To determine whether there was a gross effect on reaction rate, one additional capsule was exposed to a partial pressure in equilibrium with 300 ppm hydrogen in NaK, and cooled to room temperature much more slowly (for >30 hr). A fifth capsule, identical to the others, was soaked at temperature, but in vacuum, to provide control samples for the experiment.

Following soaking, the capsules were cut open in a controlled-atmosphere glove box and examined. The NaK was very clean, and appeared to have wetted

and adhered to the metallic surfaces in the capsule. A sample from each test condition was free bent in the glove box, to determine whether there was a gross effect on the Ta-10W as a result of the exposure. Table 41 shows that the specimens exposed to hydrogen levels of 30 ppm and less (hydrogen in NaK) were quite ductile. The specimens exposed to the higher hydrogen levels were less ductile, although they would not be considered brittle. The specimens were then cleaned in mercury and brought out into the air. Test samples were boiled in water for a number of hours. Analysis of the water indicated that there was $<50 \mu\text{g}/\text{cm}^2$ of residual potassium removed from the specimens.

TABLE 41
Ta-10W FREE BEND TEST RESULTS

Identification	Bend Angle (°)	Remarks
As Received	150	No Cracking
Vacuum	150	No Cracking
0.03 torr H ₂ *	150	No Cracking
0.3 torr H ₂ *	150	Surface Cracking
3 torr H ₂ *	90	Specimen Broke
3 torr H ₂ * Slow Cool	70	Specimen Broke

*Utilizing $\log_{10} (c/p) = 7.765 - 2520/T(^{\circ}\text{K})$ [Webb⁽⁷⁾]

at 1200° F: 0.03 torr H₂ is in equilibrium with 3 ppm hydrogen in NaK

0.3 torr H₂ is in equilibrium with 30 ppm hydrogen in NaK

3 torr H₂ is in equilibrium with 300 ppm hydrogen in NaK

This suggests that the mercury cleaning was effective. The cleaned samples were tensile tested in air at room temperature to evaluate quantitatively the effect of the exposure on the mechanical properties of Ta-10W. Table 42 shows the results of the tensile test. This test confirms the results of the bend test — specimens exposed to 300 ppm (hydrogen in NaK) showed a loss in total elongation at rupture; the specimens exposed to lower hydrogen levels did not

TABLE 42
ROOM-TEMPERATURE TENSILE PROPERTIES OF Ta-10W

Identification	Ultimate Strength (psi)	0.2% Offset Strength (psi)	Elongation in 2 in. (%)
As Received	87,000	79,200	25
	84,200	75,400	31
Vacuum	80,300	60,600	22
	83,700	70,600	20
0.03 torr H ₂ *	86,300	74,000	20
	85,300	67,200	19
0.3 torr H ₂ *	84,200	66,200	23
	87,800	70,000	20
3 torr H ₂ *	82,900	66,300	8
	81,700	74,000	6
3 torr H ₂ * Slow Cool	77,300	74,000	4
	84,700	71,600	8

*Utilizing $\log_{10} (c/p) = 7.765 - 2520/T(^{\circ}\text{K})$ [Webb⁽⁷⁾]

at 1200° F: 3 torr H₂ is in equilibrium with 300 ppm hydrogen in NaK

0.3 torr H₂ is in equilibrium with 30 ppm hydrogen in NaK

0.03 torr H₂ is in equilibrium with 3 ppm hydrogen in NaK

appear to be significantly affected. The strength of the refractory metal was virtually unaffected by the exposure. The very sharp demarcation from elastic to plastic behavior was also not affected.

The results of the interstitial analysis are shown in Table 43. The overall low levels make for very difficult analysis and wide scatter. However, it is evident that the exposure did not increase the carbon, nitrogen, or oxygen levels. The low oxygen level confirms that the cleaning technique was also very effective. The exposure did increase the hydrogen content of the Ta-10W. It would be difficult to separate the vacuum control and the 0.03 torr H₂ groups from each other. There does, however, appear to be an increase (but very small,

TABLE 43
INTERSTITIAL ANALYSIS OF Ta-10W

Identification	Oxygen (wppm)	Nitrogen (wppm)	Carbon (wppm)	Hydrogen (wppm)
As received	1) 10, 40 [†] 2) 19, 23	10, 40 2, 6	24, 16	4, 5 1, 1
Vacuum	1) 28 2) 25, 25	2 10, 12	45, 28	4 10, 11
0.03 torr H ₂ [*]	1) 11, 67, 18 2) 45, 10	3, 5, 1 20, 14	33, 14	11, 14, 12 12, 4
0.3 torr H ₂ [*]	1) 13, 50, 62 2) 10, 7	33, 13, 20 5, 7	9, 24	71, 62, 44 4, 5
3 torr H ₂ [*]	1) 35, 38 2) 40, 35, 4	4, 5 18, 12, 10	36, 23	34, 41 37, 10, 16
3 torr H ₂ [*] Slow Cool	1) 2, 35 2) 40, 33	33 10, 9	44, 55	48, 49 35, 35

*Utilizing $\log_{10} (c/p) = 7.765 - 2520/T(^{\circ}K)$ [Webb⁽⁷⁾]

at 1200° F: 3 torr H₂ is in equilibrium with 300 ppm hydrogen in NaK

0.3 torr H₂ is in equilibrium with 30 ppm hydrogen in NaK

0.03 torr H₂ is in equilibrium with 3 ppm hydrogen in NaK

†1) First Analysis

2) Check

5 to 10, compared to 5) over the as-received level. This indicates that little hydrogen is adsorbed and dissolved at low hydrogen impurity levels (3 ppm hydrogen in NaK). There is a significant, though not large, increase in hydrogen content of the samples exposed to the high hydrogen levels. The cooling rate appeared to have little effect on the hydrogen levels of these specimens. Both were about the same (35 to 50 ppm). The scatter in the analysis of the specimens exposed to intermediate levels, and the close proximity of the overall results, makes it difficult to separate these samples from either of the two groups.

The results of the experiment are consistent with prior analytical considerations. Ta-10W in the presence of NaK will adsorb hydrogen when cooled

from 1200°F to room temperature. The amount of hydrogen dissolved in the Ta-10W increases with the hydrogen partial pressure over the NaK, and therefore, with the concentration of hydrogen in solution. This does not appear to affect the room-temperature mechanical properties of the Ta-10W until the hydrogen level in the Ta-10W increases to ~35 ppm. At this level, the strength is not affected, but the elongation at failure is. This does not appear to be catastrophic embrittlement, but a reduction from over 20% to ~5%.

The results of the experiment do not suggest any reason to doubt their validity. Care should be taken in their application to regions outside the realm tested. Note especially that the specimens were neither under stress nor in the presence of a thermal gradient. While, in other systems, these additions may pose only second-order effects, stress and thermal gradients do affect hydrogen mobility, and therefore could well have a significant effect on the experimental results.

The correlation developed by Webb is probably valid over the lower to intermediate pressures tested (0.03 to 0.3 torr, 3 and 30 ppm hydrogen, at 1200°F). However, loop data by Savage⁽⁸⁾ suggest that the correlation may not be valid at higher pressures and levels. For the sake of consistency, one correlation was used throughout this test.

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