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SNAP REACTOR PROGRAMS
PROGRESS REPORT
MAY-JULY 1971
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AEC Research and Development Report

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SNAP PROGRAM
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I. INTRODUCTION

A. PROGRAM SCOPE AND OBJECTIVES

The SNAP Program activities covered in this report include the following major tasks:

- 1) SNAP 8 Developmental Reactor (S8DR) Test
- 2) SPF Zirconium Hydride Reactor Engineering
- 3) Thermoelectric Converter Development
- 4) Reactor Development
- 5) Fuel Development.

The S8DR Reactor Test activity included all activities necessary for the installation, checkout, operation, test support, disassembly, and examination of the S8DR. The GFY 1971 objective consisted primarily of completion of the post-test analysis of the S8DR.

The SPF Zirconium Hydride Reactor Engineering effort provides for the engineering and design of a reactor to be tested in the NASA Space Power Facility at Plum Brook. The design power level will be 300 kw, with an outlet coolant temperature of 1200°F.

The Compact Thermoelectric Converter Technology Program is being carried out by the Astronuclear Laboratory of Westinghouse Electric Corporation, under subcontract to Atomics International. This program is intended to provide the thermoelectric power-conversion subsystem for use with a ZrH reactor - thermoelectric space-power system.

The Reactor Development tasks include heat transfer and hydraulic tests in support of the SPF reactor, as well as the development of the control drive components for the reactor. Also included in this report are the concluding activities of the 4 π reflector and shield development tasks.

The Fuel Development tasks provide for basic understanding, substantiation, and improvements in the technology of ZrH-type fuel-moderator elements, in support of the ZrH reactor programs.

B. SUMMARY OF PROGRESS

The post-test examination and analysis of the S8DR were completed, and were reported in AI-AEC-13003, "Findings of the SNAP 8 Developmental Reactor (S8DR) Post-Test Examination."

The conceptual design studies of the SPF reactor continued during the quarter. The current configuration uses 199 fuel elements on a 0.74 in. pitch with the right-left-neutral fin arrangement and a two-zone core. The analyses indicated relatively low operating temperatures and positive stress margins, with either Incoloy 800 or Hastelloy X fuel element cladding.

Three TEM-X series thermoelectric power modules, incorporating duplex Ta - 10 W inner claddings, were tested during the quarter. No degradation in performance has been observed. A study was conducted to determine the potential performance improvement available through the incorporation of the various design modifications which have been identified. An overall improvement of 59.8% was calculated, assuming the incorporation and success of all design improvements considered. Two prototype pump modules continued on endurance test, with over 18,000 hr of testing.

NaK heat transfer testing was performed on a 19-element cluster of smooth heaters simulating fuel elements. The experimental results very closely matched the analytical predictions of cladding temperatures and heat transfer coefficients for both a close-packed ($P/D = 1.0$) and a 30-mil spaced ($P/D = 1.05$) array.

Long-term (>20,000 hr) endurance tests of the high-temperature bearing and the S8DR actuator were terminated, and a final examination completed.

Fuel element process studies were performed to evaluate hobbing techniques, establish welding parameters, and establish final closure parameters for both Hastelloy X and Incoloy 800. A number of Hastelloy X clad fuel elements were fabricated, and cladding property studies were performed on both alloys. Evaluation of the vapor deposition of burnable poison materials on the fuel rods continued.

II. SNAP 8 DEVELOPMENTAL REACTOR

A. SUMMARY OF DETAILED EXAMINATION

The detailed postirradiation examination of the S8DR core was completed. Major tasks finished included hydrogen permeation testing and permeation zone scanning of selected elements and fuel density, dimensional, and hydrogen content measurements on other selected elements. Eight batches of fuel (174 elements) were canned and shipped to the radioactive material disposal facility for storage. The balance of the core remains in storage in the Atomics International hot laboratory.

B. CONCLUSIONS FROM POST-TEST EVALUATION

Conclusions drawn from the results of the post-test examination of S8DR (as presented and discussed in the final report — AI-AEC-13003, "Findings of the SNAP 8 Developmental Reactor (S8DR) Post-Test Examination") were as follows:

- The fuel element cladding ruptures were the result of excessive cladding strain caused by fuel swelling due to temperatures in excess of the design values (Figure 1).
- The fuel growth exceeded design limits due to overtemperature fuel conditions in the core resulting from hydraulic maldistributions attributed to fuel element bowing and clustering patterns, greater than anticipated coolant bypass flow near the core periphery and the detrimental effects of surge tank gas (which was entrained in the NaK coolant) on the fuel element-to-coolant heat transfer characteristics.
- The Hastelloy N cladding material strain-to-rupture characteristics observed in S8DR were in good agreement with design values.
- Excluding elements which sustained major damage (e. g., cracked elements), it appears that some measure of hydrogen barrier damage was incurred relatively early in life, particularly in the tube section region of the fuel elements; the damage seems to have occurred primarily as the result of stresses caused by element-to-element loadings resulting from bowing and clustering of the elements.

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Figure 1. Comparison of Design Temperatures
with Deduced Operating Temperatures
(Figure Unclassified)

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III. SPF ZIRCONIUM HYDRIDE REACTOR ENGINEERING

A. INTRODUCTION AND SUMMARY

The design effort on the 295-element, 600-kwt reactor was redirected toward the design of a 199-element reactor to be tested in the NASA Space Power Facility at Plumbrook. The reactor design power level was established as 300 kwt. The mechanical design of the 199-element core was little changed from that reported last quarter.

Thermal analytical emphasis was placed on predicting nominal and worst case core temperatures, particularly the thermal gradients which occur across individual fuel elements at the core exit. Analyses were also performed on the effect of integral fuel element fins on local cladding temperatures and NaK film coefficient values in core edge channels. Hydraulic analyses were performed on the inlet plenum in order to predict flow pattern tendencies.

Structural analyses of the fuel element were conducted for the current geometry and latest thermal predictions. Both Incoloy 800 and Hastelloy X cladding candidates had positive margins of safety under the worst case assumptions of the analysis. The safety margins for Hastelloy X being considerably greater, however, than the Incoloy margins.

Initial creep relaxation analyses were also performed in order to scope the load and creep strain tendencies of the elements.

Nuclear parameter studies were performed to investigate the effects of element diameter and length, and core ΔT on the availability and requirements of core excess reactivity and control. An objective was set to limit the fuel element hydrogen content to that which places the operating fuel totally in the delta-phase domain. The objective was met with a nuclear design having a 0.74-in. pitch, active core length of 17.0 in. and a reflector (control drum) thickness of 3.34 in.

A special study was performed to determine methods of extrapolating the design technology of the 300-kwt SPF core to higher power level applications. It was concluded that the risk would be minimal if this were accomplished by increasing the number of fuel elements in proportion to the power level requirement, accompanied by a reduced fuel element diameter and reduced stress

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Figure 2. SPF Nominal Characteristics
(Figure Unclassified)

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levels. Under the conservative ground rule of a fixed fuel element linear power density, the nuclear requirements of the reactor could not be met unless the fuel element diameter were reduced for higher power level cores.

B. CORE THERMAL AND HYDRAULIC ANALYSIS

1. Core Temperature Mapping Studies

The detailed thermal performance of the 300-kwt SPF core was analyzed for the basic configuration of a perfectly symmetric geometry plus several cases involving clustering of elements, asymmetric position of fuel slugs within the cladding tubes, and different levels of coolant bypass at the core edge. The studies were done using a computer model capable of treating a full 60° core sector including the reactor vessel wall and internal reflector regions. Another important feature of the model is that it accounts for the thermal convection provided by the interchannel coolant mixing in the RLN (right, left, neutral) finned fuel element array.

The computed results for the basic configuration are shown in Figure 2. The equivalent cross element ΔT is defined as the extreme of the equivalent cladding temperature difference that produced pure bending in the cladding tube. Six θ temperature variations (not shown) are those local variations which depart from the bending thermal gradient and respond to the proximity of the six elements surrounding each element. Maximum values of equivalent cross element ΔT for each individual element in the 60° sector are shown. The relatively high equivalent cross element ΔT at the outer edge of the core is due in part to the assumed internal reflector design. This can be reduced by decreasing the outlet coolant channel flow areas.

A summary of the thermal and hydraulic characteristics and performance of the basic core is shown in Table 1.

The cluster patterns assumed in the studies to date have been reasonably pessimistic and have included stackup of interelement clearances to allow accumulation of coolant flow area at the edge of the core. Also, a pattern of allowing rows of elements to bow in different directions to accumulate clearance in an adverse manner at the zone boundary interface was examined. The largest increase in cross element ΔT due to the cluster geometries considered to date

TABLE 1
SYMMETRIC CORE, 1200°F OUTLET

Core pressure drop (psia)	1.28
Peak fuel temperature, BOL (°F)	1366
Bypass temperature rise (°F)	59
Maximum channel outlet temperature (°F)	1211
Minimum channel outlet temperature (°F)	1181
Maximum cross element ΔT (°F)	17
Average Reynolds Number	15,360
Peak mixing factor (%/inch)	39

was only a few degrees larger than the basic 17°F. This presumes that the clustering is not accompanied by reduced performance of the lip seals which restrict bypass flow at the core edge.

Since in early core life, the present vibration suppressor concept (collar button) will not exert an axial load into the fuel slug column, the slugs will be free to move randomly in a zero gravity state. The energy to sustain slug motion may arise from slight hydraulic induced vibrations or other sources. Hence, it must be considered credible that the fuel slugs can align in adverse geometries such as to aggravate the cross element ΔT variations. Worst case studies of asymmetric fuel slugs were superimposed on the cluster patterns described above. The maximum effect of the combined condition was 35°F (cross element) compared to 17°F for the symmetric core. The 35°F value will likely reduce when the edge channel flow is more closely matched to the local power generation.

2. Lower Plenum Hydraulics

A nodal model of the reactor lower plenum was set up as a potential flow problem for numerical solution on the digital computer. The results show the streamlines of the ideal flow pattern for different inlet line nozzle designs. Since the potential flow equations do not consider function effects it is not possible for this particular calculation to predict flow separation or vortexing. However, the ideal flow profile can be approached by placing vanes at the plenum inlet to force the flow along the potential streamlines. These calculations,

then, can be used to determine the starting configurations for an experimental hydraulic program to develop an acceptable flow profile in the lower plenum.

Typical results of the potential flow calculations are shown in Figures 3 and 4. The calculations are for a four(4)-inlet plenum with a total flow of 65,000 lb/hr (equivalent to 75°F core ΔT at 300 kw). The calculational model includes a quarter section of the plenum and follows the flow from one of the four inlet lines. Figure 3 shows the velocity potential lines and Figure 4 the velocity profile.

3. Heat Transfer at Element Fins and at Core Edge

Several temperature profile calculations have been made with the computer model of the 180° segment of a fuel element. The effect of a 0.015-in.-high by 0.069-in.-wide fin on the aximuthal temperature ripple of an element was found to be small. The cladding temperature under a fin increased 3° at the temperature minimum location of the ripple and 4° at the temperature maximum as compared to the no-fin case. Also, the effective NaK film drop increased about 10% with the fin at the cold or hot locations. These effects are separate from, and do not include, the hydraulic and thermal effects of coolant mixing which the fins promote.

Calculation of the effective NaK film coefficient, h , at the core edge, for a NaK channel between a fuel element and the edge reflector showed a 17% increase in h over the regular channel value of 3500 Btu/hr-ft²-°F.

A calculation of NaK film h for an element with the fuel slug off center inside the cladding gave essentially the same result as for a symmetrical element. Coolant mixing at 20%/in. increased the value about 50%, also in agreement with the symmetrical element value. It was noted also that, although coolant mixing always reduces the peak to minimum aximuthal temperature ripple within a coolant channel, the mixing effect on channel to channel and cross element ΔT depends on the orientation of the mixing pattern to the direction of fuel slug offset. The details of this last effect are handled by overall core thermal model.

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Figure 3. Reactor Lower Plenum Velocity Potential
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Figure 4. Reactor Lower Plenum Velocity Profile
(Figure Unclassified)

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C. STRESS ANALYSIS

Fuel Element Stress Analysis

The fuel element stresses were recalculated for the 300-kwt, 199-element, core configuration. The geometry and materials considered were consistent with a 0.74-in. fuel element pitch using 0.032 Incoloy or 0.020 Hastelloy X cladding (0.020 is considered a lower bound thickness). The SCB coating thicknesses were 0.0025-in. maximum in all cases.

The operational stresses are computed assuming that the fuel elements have an initial TIR of 0.012-in. and assembled into the core in the most adverse position adjacent to a similar element. The interference causes contact between the elements and this loading is superimposed on the loading induced by the operational environment. The element chosen for analysis is Number 28 in Ring 8 utilizing a random cluster pattern with asymmetrical fuel slugs. The maximum equivalent structural linear cross element temperature gradient is 35°F (case without matching core edge channel flow to power). This produces a bow in the element and, assuming the element is constrained within its original envelope, produces a contact force. The local stresses due to this force have been estimated using a Bijllard analysis technique. Because of the approximations required on contact width and area, a simulated element was internally strain-gaged and is currently being tested.

The local contact stresses were next combined with the six θ , bending, and other secondary stresses to determine the total stress profile. The maximum tensile stresses occur underneath the contact point on the inner surface of the cladding while the maximum compressive stresses occur at the same location only on the cladding outer surface. The criteria for failure is the onset of gross hydrogen permeation caused by the SCB coating failure.

The stresses are summarized for the two candidate cladding materials along with their respective margin of safety.

The relatively high stress in compression for Incoloy is considered acceptable at this time due to conservatism in the analysis including the mirror-image structural assumption and because the compressive component is not considered to be related to a significant damage mode.

TABLE 2
CLADDING MATERIAL STRESSES

Material	Incoloy 800	Hastelloy X
Maximum tensile stress (psi)	8,900	12,500
Allowable stress, onset of H ₂ permeation (p's)	16,500	32,500
Margin of safety (to H ₂ permeation)	0.85	1.60
Maximum compressive stress (psi)	15,100	20,000

The cladding was then analyzed to determine the amount of creep relaxation that occurs in the reactor lifetime. The load relaxation and bending components of stress are shown in Figures 5 and 6. Note that for the Incoloy element the local contact load relaxes from 16.6 to 9.2 lb in 50,000 hours while the load in the Hastelloy X element relaxes from 9.5 to 8.4 lb. When these steady-state operating stresses relax in the reactor environment, an additional stress source is introduced upon reactor shutdown.

The cold core fuel element stresses are initially caused by the thermal cooldown of the fuel element during manufacturing and the assembly into the core. The residual stresses, after shutdown, when added to these stresses have a relieving effect for early shutdowns. For a shutdown at 50,000 hours, the Incoloy 800 fuel element tensile stress (cold fabrication plus assembly component) increases approximately 20% while the Hastelloy X fuel element is essentially the same as at the beginning of life.

D. NUCLEAR ANALYSIS

Parametric studies were performed to determine an optimum nuclear design for a 300-kwt reactor with a 5-year operating life. Constraints placed on the studies were an average linear power rating near 1 kw/ft, fuel length between 16 to 18 in., and core ΔT between 50 to 100° F. The cladding was to be 32-mil Incoloy 800 with fins to provide 30-mil interelement spacing in the inner hydraulic zone and 15-mil spacing in the outer zone. The initial H/Zr of the fuel was to ensure operation only in the δ phase. Control drums were to be S8DR-type, void-back beryllium drums.

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Figure 5. Creep Relaxation of Incoloy, $\Delta T = 35^\circ F$
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Figure 6. Creep Relaxation of 0.20 Hastelloy X, $\Delta T = 35^{\circ}F$
(Figure Unclassified)

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TABLE 3
NUCLEAR PERFORMANCE* VS PITCH

Pitch (in.)	0.72	0.74	0.76
Core Vessel ID (in.)	11.34	11.65	11.96
U ²³⁵ (kg)	9.70	10.34	11.00
ρ Available (\$)	10.40	12.35	14.15
Short-Term ρ Loss (\$)	3.01	2.96	2.91
Long-Term ρ Loss (\$)	7.59	7.22	6.89
Total ρ Required (\$)	10.60	10.18	9.80
EOL ρ Margin (\$)	-0.20	2.17	4.35
Total Drum Worth (\$)	13.45	12.24	11.14
MOL Shutdown Margin, \$3 SM Loading (\$)	5.80	2.64	-0.26
Total Margin (\$)	5.60	4.81	4.09

*199-Element Core, 300 kwt, 5-yr Life, 1200° F Outlet Temperature, 32-mil Incoloy Cladding, 17-in. Length, T (Reflector) = 3.34 in.

TABLE 4
NUCLEAR PERFORMANCE VS LENGTH

Length (in.)	16	17	18
kwt/ft	1.13	1.06	1.01
U ²³⁵ (kg)	9.74	10.34	10.95
ρ Available (\$)	10.92	12.35	13.55
Short-Term ρ Loss (\$)	3.00	2.96	2.91
Long-Term ρ Loss (\$)	7.57	7.22	6.91
Total ρ Required (\$)	10.57	10.18	9.82
EOL ρ Margin (\$)	0.35	2.17	3.73
Total Drum Worth	12.57	12.24	11.97
MOL Shutdown Margin, \$3 SM Loading (\$)	4.40	2.64	1.17
Total Margin	4.75	4.81	4.90

*199-Element Core, 300 kwt, 5-yr Life, 1200° F Outlet Temperature, 5.81 N_H, 32-mil Incoloy Cladding, 0.74-in. Pitch, T (Reflector) = 3.34 in.

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The effect of changing fuel element pitch on nuclear performance is shown in Table 3. For this study, fuel length was held at 17 in. and the core ΔT at 75°F. It may be seen that a 0.72-in.-pitch core has insufficient excess reactivity for 5-year operation and that a 0.76-in.-pitch core has insufficient midlife shutdown margin. An initial 3 Sm^{149} in prepoison loading was assumed throughout.

The effect of varying fuel length is shown in Table 4. The 16-in. core has a small end-of-life (EOL) reactivity margin and the 18-in. core has a small midlife (MOL) shutdown margin.

Changing the core ΔT primarily affects the hydrogen loss and the initial H/Zr required for all δ -phase fuel operation. As the core ΔT increases, the initial H/Zr must be decreased to maintain all δ -phase fuel because the increased ΔT causes increased hydrogen redistribution. There is some benefit from reducing initial H/Zr in that the reactivity loss due to hydrogen loss is reduced. This benefit is more than offset, however, by the decrease in excess reactivity. The trades involved are shown in Table 5. It may be seen that a 75°F ΔT provides a reasonable compromise between EOL ρ margin and MOL shutdown margin.

The effect of varying the number of control drums on drum worth, reactivity, and total reactor envelope is illustrated in Figure 7. The end points of the curves occur at drum thickness equal to drum radius. As can be seen, no more than eight drums may be used to provide the minimum drum requirements. The use of eight drums minimizes reactor envelope.

Table 6 shows the effect of varying the number of drums on nuclear performance in a different way. In this study, pitch was varied in order to maintain EOL margin. It may be seen that eight drums and a 0.74-in. pitch provide a reasonable compromise between reactor envelope and MOL shutdown margin.

Table 7 summarizes the nuclear characteristics of a tentative 300-kwt reactor design concept. The design must necessarily be subjected to more precise calculation of the burnable poison loading and evaluation of the reactivity loss due to hydrogen permeation.

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TABLE 5
NUCLEAR PERFORMANCE* VS CORE ΔT

Core ΔT ($^{\circ}F$)	50	75	100
N_H for All- δ Fuel	5 91	5 81	5 74
ρ Available (\$)	13 19	12 35	11 76
Short-Term ρ Loss (\$)	2 98	2 96	2 93
Long-Term ρ Loss (\$)	7 63	7 22	6 96
Total ρ Required (\$)	10 61	10 18	9 89
ρH_2 Loss, Included in Long Term (\$)	1 68	1 27	1 01
EOL ρ Margin (\$)	2 58	2 17	1 87
Total Drum Worth (\$)	11 91	12 24	12 48
MOL Shutdown Margin, \$3 SM Loading (\$)	1 47	2 64	3 47
Total Margin (\$)	4 05	4 81	5 34

*199-Element Core, 300-kwt, 5-yr Life, 1200 $^{\circ}F$ Outlet Temperature, 17-in Length, 32-mil Incoloy Cladding, 0 74-in Pitch, T (Reflector) 3 34 in

Figure 7. Drum Worth vs Number of Drums and Drum Radius
(Figure Unclassified)

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TABLE 6
REFLECTOR THICKNESS VS FUEL ELEMENT PITCH

Pitch (in.)	0.72	0.74	0.77
Number of Control Drums	7	8	10
Drum Radius, Reflector Thickness (in.)	4.33	3.34	2.33
ρ Available (\$)	12.77	12.35	11.85
ρ Required (\$)	10.60	10.18	9.68
EOL ρ Margin (\$)	2.17	2.17	2.17
Total Drum Worth (\$)	14.56	12.24	9.23
MOL Shutdown Margin, \$3 SM Loading (\$)	4.54	2.64	0.13
Total Margin (\$)	6.72	4.81	2.30
Core Vessel ID (in.)	11.34	11.64	12.08
Reactor Envelope (in.)	29.0	25.3	21.7

*EOL Reactivity Margin Constant, 199-Element Core, 300-kwt, 17-in. Length, 32-mil Incoloy

TABLE 7
300-kwt REACTOR NUCLEAR CHARACTERISTICS

199 Elements	
5-Year Life	
1200°F Outlet Temperature	
75°F ΔT	
0.74-in. Pitch	
17-in. Fuel Length	
32 ⁺² ₋₀ mil Incoloy Cladding	
6-mil Gap	
30/15-mil Spacing	
All- δ -Phase Fuel ($N_H = 5.81$)	
8 Control Drums, 6.75-in. diam, 3.34-in. thick	
Cold Unpoisoned ρ Available	\$12.35
ρ Requirements	
Temperature Defect	2.18
Power Defect	0.10
Xe ¹³⁵	0.43
H Redistribution	0.24
Subtotal	\$ 2.95
U ²³⁵ Burnout	1.82
U ²³⁶ Burnin	0.74
U ²³⁸ Burnout	-0.06
Fission Products	2.40
Sm ¹⁴⁹ Burnin	1.05
H Loss	1.27
Subtotal	\$ 7.22
Total ρ Requirement	\$10.18
EOL ρ Margin	\$ 2.18
Total Drum Worth	\$12.24
MOL Shutdown Margin	\$ 2.64

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Figure 8. 300-kwt SPF Reactor
(Figure Unclassified)

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E. MECHANICAL DESIGN

The preconceptual design of the SPF reactor is illustrated in Figure 8. The reactor vessel is the basic structure for support of the core and the reflector and control components. Each of the eight control drums and its drive is an independent assembly which attaches to support flanges on the vessel. The stationary reflector consists of eight beryllium tricuspid fillers between drums.

The control-drum actuators are identical, except for details of mounting and electrical terminations, to those used on the S8DR reactor. The drive consists of a 6-to-1 spur gear reducer and a drum lockout mechanism which is released by the initial rotation of the actuator. The drums rotate in self-aligning ball journal bearings of materials used on the S8DR reactor bearings.

Four inlet and four outlet pipes, 2.0 in. OD by 0.065 in. w, feed NaK to and from the reactor vessel. The inlet plenum may include diffuser vanes and/or baffles, not shown, to ensure uniform hydraulic conditions at the inlet grid plate.

F. CORE POWER LEVEL EXTRAPOLATION STUDY

This study was made to determine the penalties involved in using a common fuel element in a 199-element configuration to produce 300 kwt and in a 295-element configuration to produce 450 kwt. With this ratio of power levels and number of elements, the linear power density in each core is nearly equal. The study was made assuming 32-mil Incoloy 800 cladding, 17-in. fuel length, 75°F ΔT , 1200°F outlet temperature, and 5-year operation.

A comparison of allowed design domains for the 300- and 450-kwt reactors with 4-in. reflectors is shown in Figure 9. The required excess reactivity and required control lines are only a function of the pitch and not of the number of elements. This results from the fact that both cores have the same linear power rating. The allowable domain of operation is defined by the area to the right of the point where the required reactivity is less than the available reactivity and to the left of the point where the required control is less than the available control. Note that the allowable design domains do not overlap when the reflector thickness is the same for both cores. Decreasing the reflector thickness increases the separation between allowable domains and increasing the reflector

Figure 9. Comparison of Allowed Design Domains
(Figure Unclassified)

thickness decreases the separation. Increasing the reflector thickness results in a significant penalty in reactor envelope for both cores. This is illustrated in Figure 10.

Figure 11 illustrates the situation in which cores containing the optimum size element for the 300- and 450-kwt cores, respectively, are operated at the other power level with a common size element (the number of elements is changed to maintain the linear power density constant). As indicated (Case I in Figure 10), that is, moving from the 300-kwt domain to the 450-kwt domain at constant pitch), the penalty in reactor envelope is seen to be quite extreme. In fact, the optimum 450-kwt element does not have sufficient reactivity for 5-year operation in a 199-element, 300-kwt reactor.

The major conclusion reached with respect to the nuclear design is that no common fuel element pitch (i. e., diameter) reasonably satisfies the requirements for both cores. The current selected design point for the 300-kwt SPF reactor cannot reasonably be used for a 450-kwt design or vice versa. A compromise element pitch results in large reactor envelope penalties for both designs. This would cause sizeable weight penalties in a shielded system.

Other key design parameters influenced by lattice pitch are shown in Figure 12. It is to be noted that the thermal and hydraulic performance is enhanced as the element pitch is decreased (equivalent to going to higher powers with constant linear power density). The trend toward increased core pressure drop is considered conservative within the limits shown since increased pressure drop reduces the sensitivity of the core to plenum and inlet conditions. The fuel element stress levels can also be shown to be appreciably reduced as the element diameter is reduced, since element flexibility and contact loads are key considerations.

It was concluded from the study that reducing the fuel element pitch leads to more benign conditions from the standpoints of hydraulic and structural performance. Fixing the fuel rod linear power density fixes the fuel element temperature levels and leads to nearly constant fuel growth. Thus, extrapolation of the 300-kwt SPF technology to cores designed for higher power level can very satisfactorily be accomplished by reducing the fuel element diameter and proportionately increasing the number of elements. The operating conditions of the fuel element in the higher power reactor would then be essentially equivalent to, and in some important areas more benign than, the lower power reactor.

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Figure 10. Allowable Operating Domains, 450-kwt and 300-kwt Reactors, EOL ρ Margin and Control Available for Mismatch (Figure Unclassified)

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Figure 11. Minimum Reactor Envelope vs Power —
Effect of Common Element
(Figure Unclassified)

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Figure 12. Influence of Pitch on Key Core Parameters
(Figure Unclassified)

IV. THERMOELECTRIC CONVERTER DEVELOPMENT

A. REACTOR POWER MODULES

Three TEM-X series reactor power modules incorporating duplex Ta - 10 W inner claddings were tested during the past quarter. TEM-X S/N-3C has completed 1958 hours of static testing at WANL including 52 thermal cycles. There has been no measurable degradation observed in this module which has been tested at $\bar{T}_H = 1085^\circ\text{F}$ and $\bar{T}_C = 507^\circ\text{F}$. TEM-X S/N-3D, the first refractory-clad module to be tested in the liquid metal loop system at Atomics International, was placed on test during the past quarter and has currently accrued 834 hours of stable steady-state performance. The beginning-of-life power output of this module was about 4% higher than predicted (304 watts compared to 291 watts). The third module, TEM-X S/N-3E, was recently placed on steady-state test and is producing performance data duplicating that of the other modules quite closely. A fourth module, TEM-X S/N-3F, was fabricated and shipped to Atomics International for loop testing.

One TEM-13G series reactor module remained on test in the liquid metal loop system at Atomics International with performance characteristics unchanged from previously reported trends. This module, TEM-13G S/N-2, has accrued over 15,000 hours of operation at $\bar{T}_H = 1025^\circ\text{F}$ and $\bar{T}_C = 538^\circ\text{F}$ test conditions.

During the past quarter, an analysis was completed to determine the amount of performance improvement available through the incorporation of various design modifications which have been identified. An overall improvement of 59.8% was calculated assuming the incorporation and success of all design improvements considered.

1. Refractory-Clad Modules

Testing of TEM-X S/N-3C, the first tubular module to incorporate hermetically sealed end cap assemblies, continued during the past quarter at $\bar{T}_H = 1085^\circ\text{F}$ and $\bar{T}_C = 507^\circ\text{F}$. Module end caps, backfilled with argon at a nominal 7 psig level, prevent trace amounts of oxygen from entering the end closures along the boron nitride power pin insulators. This temporary design modification was incorporated on this module to allow testing prior to qualifying a directly applied glass-to-metal seal over the boron nitride insulators.

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Figure 13. Overall Efficiency vs Elapsed Time for Refractory-Clad Reactor Power Modules (Figure Unclassified)

Figure 14. Electrical Power Output vs Elapsed Time for Refractory-Clad Reactor Power Modules (Figure Unclassified)

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Overall efficiency, electrical power output, internal resistance, and open circuit voltage data from TEM-X S/N-3C as well as all other TEM-X S/N-3 series modules tested are presented as functions of testing time in Figures 13 through 16. The data presented in these figures exhibits the high degree of reproducibility of performance, the degree of insensitivity to cyclic operation, and the high degree of performance stability achieved in these modules. A comparison of pre- and post-cyclic test data from TEM-X S/N-3C in Figures 13 through 16 indicates the lack of sensitivity of all performance parameters to cyclic operation.

A statistical analysis (linear curve fit) of all life test data from TEM-X S/N-3C is presented in Table 8 along with data from the other TEM-X modules. The excellent long-term stability of the TEM-X design is evident from this data.

TEM-X S/N-3D and -3E, fabricated with circuits identical to TEM-X S/N-3C, were placed on test during the past quarter. Effective Seebeck coefficient, internal resistance, and thermal impedance data recorded during the open circuit heatup of TEM-X S/N-3E are compared to calculated values as well as similar TEM-X S/N-3C data in Figures 17 through 19. The data correlate very well with predicted values over the entire temperature range. There is, however, some deviation between the calculated and measured thermal impedance data near design operating conditions. The calculated heat input is nearly 300 watts (approximately 5%) greater than the measured level. This deviation is felt to be caused by the use of higher than actual lead telluride thermal conductivity levels in the calculational code TEMOD.

TEM-X S/N-3D is being tested in a liquid metal loop system at Atomics International. Due to a thermocouple reading error during the initial heatup of the module, heatup data are not presented here. As a result of the thermocouple reading error, the module was operated for 25 hours after the initial heatup at an average hot cladding temperature of 1175°F, 95°F above standard operating conditions. During this time, the primary NaK inlet temperature was 1300°F. Upon identifying the over-temperature condition, the module was returned to the design $\bar{T}_H = 1085^\circ\text{F}$ and $\bar{T}_C = 507^\circ\text{F}$ operating conditions.

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Figure 15. Internal Resistance vs Elapsed Time for
Refractory-Clad Reactor Power Modules
(Figure Unclassified)

Figure 16. Open Circuit Voltage vs Elapsed Time for
Refractory-Clad Reactor Power Modules
(Figure Unclassified)

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TABLE 8
REFRACTORY-CLAD REACTOR MODULE PERFORMANCE STABILITY SUMMARY

Module	Testing Time (Hours)	Rate of Change: % Per 10,000 Hours				Thermal Cycles		
		R _g	V _{oc}	P _e	η	Uncontrolled Number	Controlled	
							No.	Rate (°F/Min)
TEM-X S/N-2*	216	**	**	**	**	0	50	30
TEM-X S/N-3	Not Tested	--	--	--	--	-	--	--
TEM-X S/N-3A*	1166	4.0	1.1	-1.9	-2.0	1	49	30
TEM-X S/N-3B*	2584	2.8	0.1	-2.5	3.0	2	0	--
TEM-X S/N-3C	1958	0.6	1.8	3.0	4.1	0	51	30
TEM-X S/N-3D	834	**	**	**	**	1	1	30
TEM-X S/N-3E	160	**	**	**	**	0	0	--
TEM-X S/N-3F	Not Tested	--	--	--	--	--	--	--

* Testing Terminated

** Insufficient Testing Time to Allow Meaningful
Performance Analysis

All modules tested with $\bar{T}_H = 1085^\circ\text{F}$, $\bar{T}_C = 538^\circ\text{F}$

All data included through July 16, 1971.

Figure 17. A Comparison of Calculated and Experimental Effective Seebeck Coefficient Data Recorded During Open Circuit Heatup of TEM-X Modules
(Figure Unclassified)

Figure 19. A Comparison of Calculated and Experimental Thermal Impedance Data Recorded During Open Circuit Heatup of TEM-X Modules
(Figure Unclassified)

Figure 18. A Comparison of Calculated and Experimental Internal Resistance Data Recorded During Open Circuit Heatup of TEM-X Modules
(Figure Unclassified)

The $\bar{T}_H = 1085^\circ\text{F}$ and $\bar{T}_C = 507^\circ\text{F}$ beginning-of-life performance data from all TEM-X S/N-3 series modules are compared in Table 9 to the corresponding calculated values. The voltage, power, and efficiency levels of S/N-3D are somewhat above the band of data formed by all other modules indicating that the initial over-temperature operation had no adverse effect on the initial design temperature performance levels. It will be noted, however, that the calculated heat input level for all modules is consistently about 300 w lower than the calculated level as discussed above.

In addition to the reactor modules discussed above, TEM-X S/N-3F was fabricated, processed, installed in a liquid metal test shroud assembly, and shipped to Atomics International for testing during the past quarter.

Additional design work was directed, during the past quarter, toward the development of a glass-to-metal seal which can be directly applied to the exposed boron nitride insulators to eliminate the need for end cap assemblies in future reactor power modules incorporating refractory claddings.

2. Prototype 5.25-Volt Reactor Power Modules

TEM-13G S/N-2, a first generation 5.25-v reactor power module fabricated with an Inconel inner cladding, remained on test in a two-loop liquid metal system at Atomics International during the past quarter. This module has accumulated over 15,000 hours of operation at $\bar{T}_H = 1025^\circ\text{F}$ and $\bar{T}_C = 538^\circ\text{F}$ operation conditions. Previously reported performance trends continued for this module during the past quarter. Power output and overall efficiency degradation rates of 18 and 16%, respectively, have been determined for this module which has been thermally cycled six times. These data are in close agreement with similar data from TEM-13G S/N-4 which was previously tested at the same temperatures in a static test stand at WANL.

It is significant to note that the rate of degradation increased after each thermal cycle. In this first 3255 hours of operation in the liquid metal loop system during which no thermal cycles were experienced, power output and overall efficiency degradation rates of 10 and 8%, respectively, were observed. The increased rate of degradation is caused by accelerated tellurium flow through mica cracks and tears produced by component distortions during transient operation. More recent reactor power modules, viz. the TEM-X series incorporating

TABLE 9

A COMPARISON OF CALCULATED AND EXPERIMENTAL BOL PERFORMANCE PARAMETERS
FROM TEM-X S/N-3 SERIES MODULES

Module	Room Temperature		B.O.L. $\bar{T}_H = 1085^\circ\text{F}$, $\bar{T}_C = 507^\circ\text{F}$					
	R_g (Milliohms) Circuit Module		R_g Circuit (Milliohms)	R_g Module (Milliohms)	V_{oc} (Volts)	P_e (Watts)	Q_{in} (Watts)	η_o (Pct)
TEM-X S/N-3	2.7	*	**	**	**	**	**	**
TEM-X S/N-3A	2.7	3.0	23.7	24.9	5.24	276	5847	4.73
TEM-X S/N-3B	2.7	*	*	25.0	5.29	280	5933	4.72
TEM-X S/N-3C	2.7	*	*	25.1	5.44	294	5995	4.90
TEM-X S/N-3D	2.7	3.2	*	25.0	5.52	304	5974	5.10
TEM-X S/N-3E	2.6	2.9	*	25.0	5.46	297	5978	4.98
TEM-X S/N-3F	2.6	2.9	**	**	**	**	**	**
CALCULATED	2.7	3.0	23.8	24.9	5.41	291	6272	4.64

* Pin Resistance Not Measured

** Not Tested

refractory inner claddings, have exhibited greatly improved performance stability in comparison to these TEM-13G data by virtue of the reduced mica deformation. In addition, test data from TEM-X S/N-3A and S/N-3C, which were thermally cycled 50 and 52 times, respectively, showed that the improved performance stability is not altered by cyclic operation.

3. Module Improvement Effects Study

During the past quarter, an analysis to determine the effects of various proposed module design improvements was completed. The objective of this work was to quantify the performance improvement associated with each individual modification considered in an "optimized" reactor module design.

Since the effect associated with each modification is somewhat dependent on the order in which the modifications are considered, two sets of calculations were generated. First, calculations were performed considering each individual design improvement, incorporated one at a time into the existing TEM-X S/N-3 design. The calculated overall efficiency for the existing TEM-X S/N-3 is 4.64%. The improvement effect, then, was defined by the expression

$$\Phi_i = 100 \left(\frac{\eta_i}{\eta_b} - 1 \right) ,$$

$$\eta_b = 4.64\% ,$$

where Φ_i is the percent improvement associated with each design modification; η_b (4.64%) and η_i are the calculated overall efficiencies before and after the incorporation of the improvement, respectively. Hence the improvement number represents the percent by which the TEM-X S/N-3 efficiency would be improved by the incorporation of each individual design improvement.

In the second phase of the work, each design improvement was investigated one at a time after the incorporation of all other improvements. The improvement effect in this case was defined by

$$\Phi_i = 100 \left(\frac{\eta_b}{\eta_i} - 1 \right) ,$$

TABLE 10
CALCULATED MODULE IMPROVEMENT EFFECTS

Design Modification	Improvement Effect (Pct.)	
	Added to TEM-X S/N-3	Last Addition to Optimized Module
1. Ternary materials	19.3	23.1
2. Reduced mica and EBVD barrier	13.9	14.6
3. Increased T/E radial thickness	5.1	1.7
4. Low heat loss end closure	2.4	3.6
5. Thin inner clad	0.6	0.5
6. Thin outer clad	3.5	1.8
7. Segmented T/E washers	5.0	5.0
Accumulative	59.8	59.8

NOTE:

1. Improvement No. 7 can not be added to TEM-X S/N-3 without incorporation of improvement No. 1.
2. The accumulative total is not the arithmetic sum of the individual effects since the effects compound one another.

$$\eta_b = 7.41\%$$

Here Φ_i is the percent improvement associated with each design modifications, η_b (7.41%) and η_i are the calculated overall efficiencies with all improvements and with all except the i th improvement, respectively.

Table 10 summarizes the calculated module improvement effects associated with each design improvement. All numbers were generated using TEMOD, the tubular thermoelectric module performal calculational code. The design improvements considered were:

- 1) Incorporation of ternary materials. For these calculations the ternary thermoelectric material properties quoted by RCA* were substituted directly for the standard lead telluride.
- 2) Reduced mica thickness and EBVD barrier. These two improvements were paired since it appears that the incorporation of reduced axial thickness must be accompanied by a thin electron beam vapor deposited diffusion barrier. The mica axial thickness considered here was 0.0015 in.
- 3) Increased radial thickness thermoelectric washers. Washer radial thickness increased to 0.200 in. from 0.145 in. was considered to improve efficiency.
- 4) Low heat loss end closure. By redesigning the existing TEM-X S/N-3 type end closure, it is considered feasible to reduce the end closure conductances by 50%.
- 5) Thin inner cladding. Using the swirl generator for internal radial support, it is considered feasible to reduce the inner cladding thickness by approximately 50%, thus reducing the temperature drop across the cladding. However, the calculated benefit of such a modification (less than 1%) makes the consideration of such an effort questionable.
- 6) Thin outer cladding. Using the NaK shroud swirl generator for external radial support, it is considered feasible to reduce the outer cladding thickness by approximately 50%. The calculated benefit of this

*RCA Quarterly Progress Report NYOO-3886-4

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Figure 20. Electrical Current Data from Three Prototype Pump Power Modules
(Figure Unclassified)

Figure 21. Power Output Data from Three Prototype Pump Power Modules
(Figure Unclassified)

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modification, especially in terms of the optimized module, is considered too low to be undertaken.

- 7) Segmented thermoelectric washers. By varying the dopant levels in the ternary materials, it is possible to shift the peak in the figure-of-merit curve to lower temperatures. Segmenting of the thermoelectric washers would allow the outer portion of the washer, which operates in a lower temperature range, to be doped such that it more efficiently uses the existing temperature gradient.

B. PUMP TUBULAR POWER MODULES

Performance Summary of Prototype Pump Modules

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 $\bar{T}_H = 1125^\circ\text{F}$ and $\bar{T}_C = 570^\circ\text{F}$.

Electrical current and electrical power output data from all TEM-14A series modules are presented in Figures 20 and 21, respectively. Data from TEM-14A S/N-3, which was removed from test for destructive examination after completing 50 thermal cycles with no measurable degradation, are included for comparison. Performance levels achieved in S/N-2 and S/N-3, 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 11 presents a summary of performance data from the electromagnetic pump modules. Degradation rates for four parameters were determined from a statistical analysis of experimental data from these modules. An electrical heater failure in TEM-14A S/N-1 after 17,847 hours resulted in a shutdown and subsequent re-instrumentation of the module. Prior to the shutdown, the module was being tested with a permanently fixed load (0.46 milliohms). Because the load was bolted to the module leads, it was impossible to make meaningful instantaneous open circuit measurements, hence there was no open circuit voltage or internal resistance data recorded.

TABLE 11
PUMP MODULE PERFORMANCE STABILITY SUMMARY

Module	Testing Time (Hours)	Rate of Change: % Per 10,000 Hours				Thermal Cycles		
		R _g	V _{oc}	P _e	η_o	Uncontrolled Number	Controlled	
							No.	Rate (°F/Min)
TEM-14A S/N-1	17847	*	*	-4.8	-6.2	9	0	-
	3712***	2.1	0.2	-2.1	-2.1	1	2	30
TEM-14A S/N-2	18374	6.5	2.0	-2.7	-2.7	8	1	30
TEM-14A S/N-3**	7727	4.5	3.0	1.3	-0.4	2	50	30

* No open circuit data taken for S/N-1 during first 17847 hrs of testing. All modules tested at $\bar{T}_H = 1125^\circ\text{F}$, $\bar{T}_C = 570^\circ\text{F}$

** Testing terminated.

All data included through July 16, 1971.

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

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After the re-instrumentation, however, the TEM-14A S/N-1 test assembly was equipped with the load resistance network previously used in the TEM-14A S/N-3 test, thus allowing open circuit measurements. This modification makes it possible to perform temperature adjustment calculations. Data from TEM-14A S/N-1, then are handled as two separate line segments; the first covering the initial 17,847 hours and the second covering all subsequent data. As can be seen in Table 11, the degradation rate data measured after the re-instrumentation and load modification are somewhat lower than the initial data.

The long-term stable performance capabilities of the tubular module incorporating lead telluride thermoelectric materials are clearly demonstrated by the performance data from the TEM-14 series pump modules. Successful performance following the fifty controlled thermal cycles on S/N-3 (see Figures 20 and 21) and the various uncontrolled shutdowns of all three modules indicate that the TEM-14 design is insensitive to thermal cycling.

C. SUPPORTING TECHNOLOGY DEVELOPMENT

1. Sublength Module

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 to 4.0 in. from 15.0 in. The purpose for fabricating and testing these modules is to evaluate the effects of design, processing, and fabrication modifications on module performance. All TEM-15 series modules have been tested at nominal $\bar{T}_H = 1125^\circ\text{F}$ and $\bar{T}_C = 570^\circ\text{F}$ cladding temperatures in static test facilities at WANL.

A list of all the process and design features of each TEM-15 series module fabricated to date is presented in Table 12.

a. TEM-15A Series Modules

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

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TABLE 12
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-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 15A-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 insulators prevented from extending beyond mica at O.D. and tungsten insert clearance eliminated.

TABLE 12
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 - 4000 Å Mo barriers deposited between two .0025 inch mica washers.
15C S/N-4A	Same as 15A-4F except each intercouple insulator consists of 500 - 1500 Å 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).
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.

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Figure 22. A Comparison of Power Output Data
from TEM-15A Series Modules
(Figure Unclassified)

Figure 23. A Comparison of Overall Efficiency Data
from TEM-15A Series Modules
(Figure Unclassified)

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Endurance testing of five TEM-15A series modules continued during the past quarter. Electrical power output and overall efficiency data from these modules are presented in Figures 22 and 23, respectively, as functions of elapsed time. As can be seen in the figures, excellent long-term performance reproducibility has been achieved in all TEM-15A series modules, indicating that none of the design, processing, or fabrication modifications has had a gross effect on module performance.

Degradation rate data from all TEM-15A series modules remained essentially unchanged during the past reporting period, i. e., no major changes in previously established performance stability trends were observed. These data indicate that degradation continues to follow a linear relationship with time for extended testing periods and that performance stability is relatively insensitive to the processing and dimensional variables under investigation in the TEM-15A series of modules. The performance stability observed in TEM-15A series modules is discussed relative to other TEM-15 modules in a later section of this report.

b. TEM-15B Series Modules

The TEM-15B designation applies to all sublength reactor technology modules incorporating tungsten foil diffusion barriers positioned between two mica electrical insulators. Two TEM-15B series modules, S/N-4A and S/N-4B, continued on test during this reporting period.

TEM-15B S/N-4A incorporates 0.0004 ± 0.0001 -in. tungsten foil extending radially outward from the module hot junction to a point midway between the hot and cold junction and sandwiched between two 0.005-in. mica washers. This is similar to the barrier design of TEM-X S/N-3 series modules discussed in Section A. The number of thermoelectric couples was reduced to 33 in TEM-15B S/N-4A to accommodate the increased mica thickness within the 4.0-in. circuit length. All other TEM-15 series modules with the exception of TEM-15D S/N-4, discussed below, were fabricated with 36 thermoelectric couples.

Electrical power output and overall efficiency data from TEM-15B S/N-4A are presented in Figures 24 and 25. Similar data from other modules incorporating design features incorporated to improve performance stability, which are

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Figure 24. A Comparison of Power Output Data from Improved Performance Stability TEM-15 Series Modules (Figure Unclassified)

Figure 25. A Comparison of Overall Efficiency Data from Improved Performance Stability TEM-15 Series Modules (Figure Unclassified)

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discussed below, are also presented. Although the power output and efficiency levels of TEM-15B S/N-4A are lower than that of nonbarrier TEM-15A series modules as a result of the tungsten foil shunt heat losses, performance stability is greatly improved. Statistical analysis of the power output and overall efficiency data from this module indicates degradation rates of 5 and 3% per 10,000 hours, respectively.

The most recently fabricated TEM-15B series module, S/N-4B, is identical in design to the initial TEM-15B series module, S/N-4, except that inner conductor rings were resized to ensure a 0.001 to 0.002-in. interference between the tungsten inserts and the mica insulators. This modification was incorporated to eliminate tellurium diffusion during purging and processing. In addition, the outer diameter of the tungsten foil diffusion barrier was reduced by 0.020 in. to prevent shorting of the foil to the outer conductor rings, as was observed in the initial module. Performance data from TEM-15B S/N-4B are presented in Figures 24 and 25. Performance stability of TEM-15B series modules is discussed in a later section of this report.

c. TEM-15C Series Modules

TEM-15C series modules are experiments in which thin films of molybdenum or tungsten formed by electron beam deposition are used to retard the transport of tellurium between adjacent thermoelectric washers. Two such modules, TEM-15C S/N-4, and S/N-8, have been fabricated and tested to date using a 0.0025-in.-thick mica sheet upon which 1000 to 2000Å of Mo was deposited. Two washers are used at each mica position (Mo-coated faces together) so that the total barrier thickness is 2000 to 4000Å at each position. Since the total electron beam deposited film is only approximately 3% as thick as the tungsten barrier used in the mica washers of TEM-15B S/N-4, shunt heat losses in TEM-15C series modules are negligible.

TEM-15C S/N-8 remained on test during the past quarter. Power output and overall efficiency data from this module are compared to similar data from other TEM-15 series barrier modules in Figures 24 and 25, respectively. Results of a statistical analysis of performance data from both TEM-15C series modules are presented in a later section of this report.

Fabrication of a third TEM-15C series module, S/N-4A, was completed during the past quarter. This module incorporates intercouple insulators identical to those of the previous TEM-15C series modules except that 500 to 1500Å tungsten barriers were vapor deposited onto mica. An improvement in performance stability is anticipated here since the molybdenum barrier in the previous modules was observed to have reacted with the tellurium during operation.

d. TEM-15D Series Modules

The TEM-15D designation applies to modules designed with 0.010-in. mica insulators containing no additional diffusion barrier mechanisms. Like TEM-15B S/N-4A, TEM-15D S/N-4 contains 33 thermoelectric couples as opposed to 36 used in all previously fabricated TEM-15 series modules. The reduction in the number of couples was required to accommodate the increased mica thickness within the 4.0-in. (nominal) circuit length.

Power output and overall efficiency data from TEM-15D S/N-4 are presented in Figures 24 and 25. A reduction in the BOL level of both parameters (compared to TEM-15A series module data) is the result of replacing a portion of the lead telluride with mica in this design.

e. TEM-15F Series Modules

TEM-15F S/N-4 is the first module to incorporate the recently developed lead telluride base ternary alloy Pb-Te-GeTe, n-type thermoelectric material (designated ANX-6). This module is identical to TEM-15A S/N-4F except for the n-type material incorporated.

Beginning-of-life test data from this module indicated that the reduction in resistivity anticipated was not achieved during hot autoclaving. The internal resistance of the module was approximately 10% higher than TEM-15A series modules which incorporate standard GE n-type lead telluride. Although insufficient time has been accrued to allow a meaningful performance analysis, the module appears to be degrading at a rate nearly the same as that of TEM-15A series modules.

f. TEM-15H Series Modules

The first TEM-15H series module, S/N-4, continued on static test at $\bar{T}_H = 1125^\circ\text{F}$ and $\bar{T}_C = 570^\circ\text{F}$ operating conditions during the past quarter. This

module is identical in design to TEM-15A S/N-4F, discussed above, except that a tungsten insert was incorporated under both the p- and n-type lead telluride washers. This modification was made to eliminate a possible surface reaction between molybdenum and n-type lead telluride at the hot junctions of the module. In addition the axial length of the n-type washer was reduced by 1 mil to ensure an interference fit between the tungsten inserts and adjacent mica washers.

Initial performance data from TEM-15H S/N-4 indicate that the elimination of the molybdenum-lead telluride interface has reduced the initial degradation rate by a small but measurable amount.

g. Performance Stability Analysis

Table 13 lists all TEM-15 series modules fabricated to date and summarizes results of the most recent degradation rate analysis of modules having steady-state testing times in excess of 1000 hours (a minimum of 1000 hours is required to establish performance trends). Degradation rates for four parameters (resistance, open circuit voltage, power output, and overall efficiency, all adjusted to account for minor load and temperature testing variations) were determined.

As can be seen in Table 13, all TEM-15A series modules exhibit resistance degradation rates falling in a very tight band ranging from 29 to 32% per 10,000 hours. The one exception to this is TEM-15A S/N-4E which was inadvertently operated for 190 hours at $\bar{T}_H = 1200^\circ\text{F}$. It is apparent from TEM-15A S/N-4E post-over-temperature data that short-term high temperature operation does cause increased degradation in a nonbarrier module. No similar data have been generated in modules incorporating diffusion barriers.

However, the internal resistance degradation rate data indicate that performance stability is relatively insensitive to the processing and dimensional variations under investigation in the TEM-15A series of modules. In addition, these data exhibit the extent to which performance stability data can be reproduced in similar modules.

Short-term voltage degradation rate data appear to be much more "noisy" than resistance data. TEM-15A series modules fall within the band 1.3 to 7.1% per 10,000 hours, also shown in Table 13. However, data from those TEM-15A series modules which exceed 4,000 hours of testing time, indicate voltage degradation rates ranging from 2.6 to 3.1% per 10,000 hours, also a very tight data band.

TABLE 13
TEM-15 PERFORMANCE ANALYSIS SUMMARY

Module	Testing Time (Hours)	Rate of Change: % Per 10,000 Hrs				Thermal Cycles		
		R _g	V _{oc}	P _e	η_o	Uncontrolled No.	Controlled	
							No.	Rate(°F/Min)
TEM-15A S/N-1	13,051	32	2.5	-19	-16	7	1	30
TEM-15A S/N-4*	2,349	30	3.9	-22	- 8	2	0	-
TEM-15A S/N-4A	Not Tested	--	---	---	---	-	0	-
TEM-15A S/N-4B*	2,174	30	6.4	-17	-20	0	50	2.5
TEM-15A S/N-4C*	875	**	**	**	**	2	50	30
TEM-15A S/N-4D	13,327	31	3.1	-17	-14	4	1	30
TEM-15A S/N-4E	3,093	29	7.1	-14	-10	1	0	-
	5,130***	41	3.4	-28	-25	2	1	30
TEM-15A S/N-4F	2,463	30	3.7	-22	-12	0	1	30
TEM-15A S/N-5	12,580	29	2.9	-17	-13	8	1	30
TEM-15A S/N-8*	2,442	29	1.3	-22	-13	2	50	60
TEM-15B S/N-4*	3,637	12	6.0	0	- 3	2	0	-
TEM-15B S/N-4A	7,556	14	4.1	- 5	- 3	2	1	30
TEM-15B S/N-4B	2,465	21	3.7	-12	-11	0	1	30
TEM-15C S/N-4*	2,753	29	6.9	-14	-14	2	0	-
TEM-15C S/N-8	10,745	25	2.6	-16	-12	5	1	30
TEM-15D S/N-4	8,792	18	2.9	-10	- 7	4	1	30
TEM-15H S/N-4	2,871	25	3.1	-18	- 9	0	1	30

*Testing Terminated

**Insufficient Testing Time to Allow Meaningful Performance Analysis.

***After 190 hours of $\bar{T}_H = 1200^\circ\text{F}$ operation.

All Data Included Through July 16, 1971
All Modules Tested with $\bar{T}_H = 1125^\circ\text{F}$,
 $\bar{T}_C = 570^\circ\text{F}$.

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Similarly, power output and overall efficiency degradation rate data observed for the three long-term steady state modules are $18 \pm 1\%$ and $14 \pm 2\%$, respectively.

It appears from the TEM-15A series module data that in evaluating performance stability of modules having limited testing times, such as some of the barrier modules, the rate of increase of internal resistance is the most meaningful quantitative parameter.

Destructive examination of modules has revealed that degradation is the result of n-leg washer contamination by excess tellurium given off by the p-leg washers. Clearly, there are two paths by which the tellurium can migrate between adjacent washers: through the intercouple insulators, and around the intercouple insulators. In the case of the TEM-15A series modules, the 30% per 10,000 hours increase in resistance can be considered as the result of the contribution of both migration paths.

TEM-15B S/N-4B is identical to TEM-15A series modules except that Path 1 (through the mica) has been eliminated through the incorporation of tungsten foil diffusion barriers. As shown in Table 13, the observed 21% per 10,000 hours resistance increase in TEM-15B S/N-4B indicates that 21% of the total 30% TEM-15A series modules resistance increase occurs via Path 2 (around the mica), leaving only 9% for Path 1 in the nondiffusion barrier TEM-15A series modules.

TEM-15B S/N-4A differs from TEM-15B S/N-4B primarily in that the mica axial thickness is 0.010 in. rather than 0.005 in. Once again, tungsten foil barriers eliminate diffusion of tellurium through the mica (Path 1) in TEM-15B S/N-4A. The increased mica thickness extends the diffusion path length around the mica, reducing the resistance increase rate to the observed 14% per 10,000 hours (see Table 13).

TEM-14D S/N-4 is identical to TEM-15B S/N-4A except that the diffusion barriers have been removed. On this basis, the Path 2 contribution for these two modules should be the same (14%). The total TEM-15D S/N-4 resistance increase rate shown in Table 13 is 18% per 10,000 hours. This indicates that the Path 1 contribution is 4%. Comparing TEM-15A and TEM-15D Path 1 data as is shown in Table 14, indicates that the amount of tellurium diffusion through the mica is nearly proportional to mica thickness as would be expected.

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TABLE 14
EFFECTS OF THERMOELECTRIC DIFFUSION PATH ON OBSERVED
RESISTANCE INCREASE RATES IN TEM-15 MODULES

Module	Mica Thickness	Diffusion Barrier	Resistance Increase Rate (percent per 10,000 hours)		
			Path 1 (through mica)	Path 2 (around mica)	Total
TEM-15A series	.005 inch	none	9	21	30
TEM-15B S/N-4B	.005 inch	.4 mil W foil	0	21	21
TEM-15B S/N-4A	.010 inch	.4 mil W foil	0	14	14
TEM-15D S/N-4	.010 inch	none	4	14	18
TEM-15C series	.005 inch	2000Å ^o moly	6	21	27

TEM-15C data also presented in Table 14 indicates that the EBVD deposited diffusion barrier reduces the Path 1 contribution by approximately one-third. This is a relatively small percent, however, of the total resistance rate increase in nonbarrier TEM-15A series modules.

The data shown in Table 14 indicate that the primary factor in module degradation is tellurium flow around the intercouple insulators. Creating an axial interference between the tungsten inserts and mica insulators, however, does not appear to have substantially affected the rate of degradation. This conclusion is reached by comparing TEM-15A S/N-4F data with data from the TEM-15A series modules not having a pinch (see Table 13). This suggests that the rate of tellurium flow around the intercouple insulators is controlled primarily by the isothermal leg at the base of the mica. This explains the substantial difference in observed degradation rate data between TEM-15B S/N-4A and 4B modules. The isothermal path is twice the length in the former module.

During the destructive examination of TEM-15B S/N-4, little or no trace of migrated tellurium was found in the n-leg washer adjacent to the small diameter mica washers. It appears most likely that when the tellurium migrates from the p-washer around the mica it was absorbed by the boron nitride. Indeed

a chemical analysis of the boron nitride sleeve in a module after a substantial amount of testing, showed a much higher tellurium concentration directly below the small mica insulator than below the inner conductor rings. It is quite possible that the incorporation of a boron nitride tellurium getter in the inner conductor ring assembly could substantially reduce the Path 2 component of tellurium diffusion.

During the past quarter, an additional tungsten diffusion barrier module, TEM-15B S/N-4C, was released for fabrication. This module is identical to TEM-15B S/N-4B except that the tungsten extends only half way into the lead telluride washer. If the above analysis of module degradation is correct, the resistance degradation rate of TEM-15B S/N-4C should be 21% per 10,000 hours, or identical to TEM-15B S/N-4B. Furthermore, the degradation rate of TEM-15C S/N-4A, which will be placed on test next month, will have as lower and upper limits 21 and 30% per 10,000 hours, respectively.

2. Improved Thermoelectric Materials

n-Type $\text{Pb}_{0.95}\text{Ge}_{0.05}\text{Te}(\text{PbI}_2)_{0.0007}$

The investigation of the effects of specimen source, configuration and processing temperature on the Seebeck coefficient and electrical resistivity of this optimum ternary composition was essentially completed during this period. The most significant points and subsequent actions are discussed below.

Literature data^{*} have been found which indicate that the vapor in equilibrium with solid PbTe-GeTe pseudobinary alloys consists almost entirely of the pure molecular species PbTe and GeTe at temperatures at least as high as the alloy melting point. Furthermore, the partial vapor pressure of GeTe is between one and two orders of magnitude higher than that of PbTe over the entire range of pseudobinary compositions, and the partial vapor pressures of both molecular species exhibit the usual exponential increase with increasing temperature. In view of this information, we might expect a priori to find that our $\text{Pb}_{0.95}\text{Ge}_{0.05}\text{Te}$ ternary alloy exhibits the following tendencies and characteristics.

*S. G. Karbanov et al., "Physico-Chemical Study of the System PbTe-GeTe," Izv. AN SSR Neorgan. Materialy 5, No. 2, pp 270-274 (1969)

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Figure 26.

Effect of Ge Content on the Seebeck Coefficient of PbTe-GeTe Alloys at the 0.0007 PbI₂ Doping Level

Figure 27.

Effect of Ge Content on the Seebeck Coefficient of PbTe-GeTe Alloys at the 0.001 PbI₂ Doping Level

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- 1) When heat treated in an equipment system having either a physical or thermal vapor sink (or both, which is the case with our heat treating furnaces; vapor is continuously removed from the constant temperature zone both by being swept away in the hydrogen stream and by simply condensing in the cooler regions of the furnace retort), the exposed surfaces of a $\text{Pb}_{0.95}\text{Ge}_{0.05}\text{Te}$ specimen would rapidly become depleted in Ge due to the relatively high partial vapor pressure of GeTe and hence selective sublimation of that molecular species. The "exposed surface" area in typical specimens is much higher than the nominal area of the geometric specimen shape because of the presence of an extensive network of surface-connected porosity in the powder-metallurgically-fabricated specimens, and the surface area of this network increases rapidly with decreasing specimen density.
- 2) The rate at which the bulk Ge content of the specimen is depleted is thus dependent upon the rate of diffusion of Ge from the interior to the specimen surface (i. e., essentially, upon the diffusion coefficient of Ge on the cation sublattice in this NaCl-type of crystal structure). This rate increases exponentially with increasing temperature.
- 3) For specimens processed in a given system for a given temperature-time process cycle, the depletion of bulk Ge content will increase with increasing surface-to-mass ratio of the specimen (noting that this ratio is affected by both the shape and the density of the specimen).
- 4) From previous experiments, we know that both the Seebeck coefficient and electrical resistivity decrease (i. e., presumably, carrier mobility increases) with decreasing Ge content. This is shown, for example, in Figures 26 (0.0007 PbI_2 level) and 27 (0.001 PbI_2 level), where the comparisons should be made between specimens processed at a given temperature (data are shown for both 650 and 750°C). Thus increasing the process temperature and holding time at temperature, increasing the surface area-to-volume ratio of the geometric specimen shape, and decreasing the specimen density, would all be expected to promote a decrease in bulk Ge content and hence a decrease in Seebeck and resistivity.

Figure 28.
Properties of Specimens Representing
 $\text{Pb}_{0.95}\text{Ge}_{0.05}\text{Te}(\text{PbI}_2)_{0.0007}$ Washers
Heat Treated at 750°C

Figure 29.
Properties of Specimens Representing
 $\text{Pb}_{0.95}\text{Ge}_{0.05}\text{Te}(\text{PbI}_2)_{0.0007}$ Washers
Heat Treated at 650°C

The results of the experimental investigation confirm all of the above expectations. The most impressive data were obtained in a series of experiments where 0.050-in.-long TEM-15 type washers ($\sim 91\%$ dense, high surface-to-mass ratio) were heat treated at 575, 650, or 750°C. These were then crushed to powder and repressed into electrical test bars (96 to 97% dense, low surface-to-mass ratio) which in turn were heat treated at 650°C if the parent washers had been treated at 650 or 750°C, or at 575°C if the washers had been treated at 575°C (all heat treatments for both washers and bars were 2-hour runs in flowing hydrogen). The Seebeck and resistivity of these bars were then measured as functions of temperature, and the results compared with measurements on primary control bars made from the same starting powder and heat treated in the same furnace run as the respective original washers. These data are shown in Figures 28 and 29.

In Figure 28, for the 750°C washer heat treatment case, a large and well defined difference is seen between the properties of primary and repressed test bars. For the 650°C case shown in Figure 29, much of this difference has disappeared because of the exponential effect of temperature on the Ge depletion mechanisms. In Figure 30, for the 575°C case, the difference is entirely absent in the Seebeck data although some effect is still indicated by the resistivity points (which in general tend to be somewhat more scattered and less reproducible specimen-to-specimen than the Seebeck points). Any hesitation in interpreting the information in Figures 28 through 30 is removed when the results of the other experiments are also taken into account.

Another striking indication of the effects of temperature and surface-to-mass ratio is given in Figure 31, where the relative weight loss during heat treating is plotted versus the nominal process temperature for three different specimen geometries. The indicated relationship is not quite exact because the data have not been corrected for such factors as abrasive weight loss in handling, small run-to-run temperature variations about the nominal values and losses during heating to and cooling from the nominal temperatures, but the exponential temperature dependence and strong surface-to-mass ratio effect are clearly demonstrated.

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Figure 30.
Properties of Specimens Representing
 $\text{Pb}_{0.95}\text{Ge}_{0.05}\text{Te}(\text{PbI}_2)_{0.0007}$ Washers
Heat Treated at 575 °C

Figure 31.
Relative Weight Loss of Various
 $\text{Pb}_{0.95}\text{Ge}_{0.05}\text{Te}(\text{PbI}_2)_{0.0007}$
Specimens Heat Treated
2 Hours in Flowing
Hydrogen of Indicated
Nominal Temperatures

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In view of the foregoing, it appears that the effect of increasing process temperature on the properties of PbTe-GeTe n-type ternary alloys, which was previously attributed to the precipitation of a Ge-rich second phase, is in fact caused by bulk Ge depletion resulting from the "selective sublimation" of GeTe from the alloy surface. It was thus decided to fabricate a group of $\text{Pb}_{0.95}\text{Ge}_{0.05}\text{Te}(\text{PbI}_2)_{0.0007}$ washers for the TEM-15F S/N-4 experimental module using a heat treating temperature of 575 to 595°C to limit Ge depletion and this was also completed during this period. The washer group exhibited acceptable dimensional and density characteristics and was released for module use.

3. Deposition of Thin-Film Tellurium Barriers

A set of EBVD tungsten-coated mica insulating washers was prepared for use in the TEM-15C S/N-4A experimental module. Tungsten films $\sim 500\text{\AA}$ thick were deposited on one surface of 2 by 2 by 0.0025-in. mica blanks using a 10-minute argon glow discharge cleaning process followed by two 1-minute deposition runs separated by a 30-minute cooling period. Washers were then punched from the coated blanks using standard tool-steel mica punching dies. Two washers were used (tungsten faces together) at each insulator position in the module circuit, giving a total barrier thickness of $\sim 1000\text{\AA}$. The coated blanks or washers were not annealed prior to module assembly since it has been shown that the module hot isostatic compaction temperature is sufficiently high to promote the required amorphous-to-crystalline structural transformation in the as-deposited tungsten films.

4. Process Development

A program is continuing for the development of a glass-to-metal seal to prevent air leakage around the conductor pins of TEM-X series modules. Various glasses were evaluated by reducing them to powder and melting the powder to effect a seal between the nickel pin and the stainless-steel plate using a one-pin mockup of the end retaining ring.

Although helium leak tight seals could be produced, specific difficulties occurred which made the technique undesirable. Specific problems were as follows:

- 1) The extremely low density of the powder (despite a cellulose nitrate binder) required melting, refilling, and remelting to fill the recessed annulus between the nickel pin and the stainless-steel wall.
- 2) There was an extreme amount of gas evolution which tended to splatter out the molten glass.
- 3) The uncompacted state of the components caused some glass leakage out the back of the mockup, a condition which would be avoided if an autoclaved end had been used.

In order to approach the problem from a more advantageous position, a new program was undertaken. First, it was decided to use glass preforms which would be extremely dense and prevent the first two problems listed above. Accordingly, a 5-lb lot of glass was formulated to the composition below:

BaO	66.83 wt %
B ₂ O ₃	24.28 wt %
Al ₂ O ₃	8.89 wt %

The characteristics of the glass were a mean coefficient of thermal expansion of $14 \times 10^{-6} \text{ }^{\circ}\text{C}^{-1}$ and a sag point of 478°C . The glass was reduced to powder and fabricated into two different size glass bead preforms. Each of the beads have been used to form a helium leak tight seal on a single pin mockup. The preliminary trials of melting and sealing proceeded in a more satisfactory way than when the powder was used. These and other mockups, which are to be made, will be thermally cycled.

Second, a pseudo-mockup of the end retaining ring was fabricated according to WANL Drawing No. 982D096 and autoclaved in a standard module processing cycle. Once sufficient experience has been acquired on the one pin mockups, the autoclaved ends will be used for experiments.

5. Die and Capsule Experiments

Preliminary compatibility tests between ANX material and TEM components were carried out. However, the results are obscured by effects which seem to have been caused by a contaminated test atmosphere.

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Ten ECX capsules for compatibility and stability experiments of ANX material were fabricated and autoclaved. Six of the capsules were placed on test at 650°C.

6. Reduced Te in P-Type Telluride

An experiment was conducted to investigate tellurium transport from p-type to n-type lead telluride washers. A p-type lead telluride composition of reduced excess Te:Na ratio is being evaluated as a possible improvement to the tellurium transport problem. Preliminary results from the experiment are currently being analyzed.

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V. REACTOR DEVELOPMENT

A. INTRODUCTION AND SUMMARY

The current reactor development efforts comprise the fuel element and reactor core heat transfer and hydraulic studies and the testing of the control drum system for the SPF reactor. The drum system to be tested includes the complete drum assembly with bearings, drive gear and the drum launch lock, drive actuator, and miscellaneous components such as the limit switch. The development efforts on the high temperature bearings, poison back control drum, and the high torque actuator, all applicable to the 4- π shieldable reactor, and on the design and fabrication technique of flight-type LiH shields, have been terminated.

Additional tests and analytical studies have been conducted to improve the correlation of the experimental results of the core mixing hydraulic test with the analytical predictions. In the interim, the fabrication of the components of the next hydraulic mixing test section with elements which are more representative of the SPF design is continuing.

The results of the basic heat transfer tests between NaK and smooth wall heaters which simulate the fuel elements have closely matched the analytically predicted values. These excellent results ensure that the test technique should provide reliable data for the next series of tests with finned elements similar to that of the SPF fuel elements. Attachment of the fins by brazing to the smooth heaters has been initiated.

The final inspection of the high temperature bearing after 21,170 hours of ultrahigh vacuum environment tests and of the S8DR design actuator after 25,931 hours of operation thermal cycle and dwell tests indicated both components to be in excellent condition with minimal wear. Results of the 16,000 hours of tests on nickel-clad silver and BeO-reinforced copper conductors at temperatures to 1300°F showed both to be superior to the S8DR design stainless-steel-clad copper wire only if numerous thermal cycles to temperatures above 1200°F are required.

A complete 4- π shieldable control drum system, including the drum launch lock to be incorporated in the SPF reactor, was subjected to the prelaunch

thermal cycles and launch-simulating vibration loads. The performance was satisfactory.

Results of the evaluation including the disassembly examination of the 86-in.-diameter by 13-in.-thick LiH shield demonstrated that shields of this configuration can be designed to be successfully cast with high density LiH.

B. FUEL ELEMENT HEAT TRANSFER STUDIES

Studies are in progress to generate experimental data pertaining to the heat transfer characteristics of NaK flowing through channels formed by closely spaced fuel elements, a configuration representative of the ZrH Reactor core. These data will provide the basis for more reliable prediction of the fuel and cladding temperatures. Initial tests with a tightly packed array of smooth heater elements conducted to generate basic data and to develop experimental technology have been concluded. Typical experimental 6θ ripple data are shown plotted with predicted values in Figure 32. The data shown are for test conditions of 1.7 kw of heater power per element applied and a total flow of 26 gpm. Note that with the application of an experimentally derived correction factor to compensate for an unavoidable temperature drop between the actual effective location of the sheath thermocouples and the inner sheath temperature, the experimental data are within approximately $\pm 6^\circ\text{F}$ of the predicted values. The experimental values of the film coefficient, in the dimensionless form of Nusselt vs Pecelt numbers, are in excellent agreement with the theoretical values as shown in Figure 33.

A second series of test in which the 19 elements were spaced 0.030 in. from each other (pitch to diameter = 1.05) have also been completed. The element spacing was achieved by use of 0.030-in.-thick by 0.125-in.-long rings located at 3 axial positions on each element. The data from these tests are presently being analyzed. Preliminary results indicate that the 6θ temperature ripple of the cladding was reduced significantly from that of the pitch-to-diameter (P/D) ratio = 1.00 case. This reduction is attributed to greater intrachannel mixing promoted by the presence of the spacer rings and not merely by the increased spacing of the elements. Data also indicate that only minor interchannel mixing had taken place.

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Figure 32. 6θ Ripple of Cladding Temperature

Figure 33. Comparison of Theory with Experiment for Nusselt Number vs Peclet Number for Center Heater

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The test section has been disassembled and preparations are being made to have 12 of the elements finned for the next series of tests. As reported previously,^{*} the feasibility of wrapping and brazing fins on the existing heater elements has been demonstrated with sample heaters, and the tooling is available.

C. FUEL ELEMENT AND CORE HYDRAULICS

Hydraulic studies are being conducted to evaluate the influences of the R-L-N fin configuration on the fuel element intrachannel coolant mixing and pressure drop of the ZrH Reactor core. Experimental and analytical efforts to improve the understanding of the mixing test data were continued. Two significant findings were concluded. First, the constant interchannel mixing efficiency assumed in the previous analytical model is not representative of the physical action taking place in the test section and probably the core. Better fit to the characteristics of the conductivity curves derived from the test data can be achieved by allowing for variable initial fluid slippage over the fins until constant mixing is eventually achieved. Second, some of the experimental data to date appeared to be attenuated to an undeterminable degree by the capacitive coupling which existed between the lead wires from the instrumented elements and the electronic instrumentation equipment. The overall characteristics of the conductivity curves determined from the attenuated data are still useful for generating the applicable analytical model.

The test instrumentation of the existing test section was revised by physically separating the lead wires to minimize the coupling effect and additional tests, designed to generate data useful for verifying the new modeling approach, were conducted. Checks during the tests indicated that the signal attenuation problem was minimized. The test data are being reduced. Analytical efforts to select the appropriate model are also being continued.

The fabrication of the components, except for the instrumented elements, for the next test section is proceeding. This test section will have elements whose configuration, including the fins, closely represents the conceptual fuel element design. Fabrication of the instrumented elements will proceed when the optimum location of the conductivity probes is determined from the analytical

*"SNAP Reactor Program Progress Report, February-April 1971," AI-AEC-12998, June 15, 1971

modeling efforts in progress. Complete review of the data instrumentation has been made and modifications to eliminate or minimize the effects of any error sources are being made.

D. CONTROL DRUM ACTUATOR

The effort in this program is directed toward the development of advanced actuators for ZrH reactors.

1. Component Testing

Tests conducted to evaluate potential high-temperature, high-reliability, conductor materials for actuator coils have been completed after 16,043 hours of testing of which 14,912 were at 1300°F. After completing endurance testing, resistance measurements were made and photomicrography, burnout, and pull-strength tests and examinations were made on selected coils. Coil resistance measurements showed that the change in resistance of the cube alloy was negligible but that in the long dwell and thermal cycle mode, the nickel-clad-silver conductor was susceptible to conductor elongation, referred to as "ratcheting," as evidenced by a 9% increase in resistance and visual growth on the bobbin. Burnout testing at temperature indicated a current density of approximately 7500 A/in.² for nickel-clad silver and 9500 A/in.² for cube alloy. The data on the nickel-clad silver represents a decrease of 42% over new wire. "Before test" data on cube alloy conductor were not obtained. Typical pull-test data indicated a maximum reduction in strength of 20% in nonburnout affected areas. Based on the above data and previous failure examinations, it was concluded that:

- 1) Nickel-clad silver and cube alloy conductors are generally inferior to Oxalloy 28* for high-temperature actuator coil application.
- 2) Cube alloy and nickel-clad-silver conductors appear to be only suitable for applications involving a large number of thermal cycles to high temperatures (above 1200°F) in a short period of time where demonstrated ratcheting of the Oxalloy would be a problem.
- 3) Cube alloy appears to have a slightly higher temperature limit than nickel-clad-silver conductors.

*Stainless-steel-clad copper

- 4) Nickel-clad silver, due to its greater ductility and consequently greater number of turns allowable in an irregular space, is more suitable than cube alloy for high performance coils.

2. Actuator Performance

After completion of the S8DR design verification testing, one actuator, S/N Dev 013, was continued in test to demonstrate extended-life actuator performance at temperature. After 25,931 hours of simulated operation testing, including thermal cycles to 1125°F and dwell at that temperature, this test was shut down and the actuator was disassembled and examined. The examination disclosed actuator S/N Dev 013 to be in excellent condition with no evidence of incipient failure. Only minor mechanical modifications are recommended for adapting the actuator for future flight applications. These recommendations relate to hardening the actuator for launch shock and vibration.

E. CONTROL DRUM BEARINGS

Development Bearing Test

This test was conducted to demonstrate operation of a control-drum bearing in an ultrahigh vacuum and high temperature environment. The bearing is a spherical self-aligning type with a carbon graphite ball and Al_2O_3 -coated socket and shaft of Ta - 10 W. The test history of the bearing assembly was 19,124 hours at 1500°F; 21,170 hours at 10^{-8} torr; 33 thermal cycles; 962 operational cycles (equivalent to 280 In-Out movements of a control drum), and dwells of 1,000, 1,000, and 500 hours. The bearing was loaded to 25 lb radially and 75 lb axially to simulate the reactions of the control drum. The maximum friction at various phases of the test are shown in Table 15. The condition of all parts after the test was excellent as seen in Figure 34. The test demonstrated the performance of the bearing to high vacuum, high temperature, life, endurance, and loading conditions within requirements for torque and wear.

F. 4- π SHIELDABLE REFLECTOR

The objective of this project is to develop a 4- π shieldable reflector control drum for the ZrH reactor. This program consisted of control drum development and material studies.

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Figure 34. Development Bearing Post-Test

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TABLE 15
STARTING FRICTION WITH 25-lb RADIAL AND 75-lb THRUST LOAD

	Maximum Starting Friction, 10 ⁻⁸ Torr					
	Ambient		800° F		1500° F	
	(in.-lb)	(μ)*	(in.-lb)	(μ)	(in.-lb)	(μ)
Endurance						
10,000 to 17,947 hr	21.4	0.65	14.2	0.43	7.1	0.22
5,000 to 10,000 hr	-	-	-	-	7.1	0.22
0 to 5,000 hr	-	-	-	-	7.1	0.22
Parametric Load Test, 100 hr	7.0	0.21	9.5	0.30	5.1	0.15
Thermal Cycle Test, 1050 hr	24.0	0.73	15.0	0.45	9.5	0.29

*μ = friction coefficient

1. Control Drum Development - Drum Rivet Studies

Two sets of test samples simulating the riveted attachment of the cladding to the absorber were assembled. One set was thermally cycled to 1800°F ten times and both sets were subjected to pull tests. The tests demonstrated a considerable margin of safety for the rivet design.

2. Material Studies

The previous emissivity measurements of some candidate drum coatings yielded values higher than the literature data (AI-AEC-12989). Remeasuring of the emissivity on the original coupons and some coupons (cleaned" by reduction in a hydrogen atmosphere, plus a single new sample, showed considerably lower values, Table 16. This apparent discrepancy was considered to have resulted from the difference in vacuum level between the original and rerun tests (10⁻³ torr vs 10⁻⁵ torr). The higher pressure during original measurements is postulated to have caused some oxidation (or contamination) at the high measurement temperature (>1000°F). The oxides (or contaminants) did not appear to form in the lower pressure environment and the data was in close agreement with the literature.

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TABLE 16
EMISSIVITY MEASUREMENTS OF CANDIDATE DRUM COATINGS

Specimen Identification	Emissivity at Temperature					
	In 10^{-3} Torr			In 10^{-5} Torr		
	1000°F	1500°F	2000°F	1000°F	1500°F	2000°F
1. CVD-W As Received	0.61	0.68	0.92			
2. CVD-W As Received				0.29	0.37	0.41
3. CVD-W 1-1/2 "Cleaned"				0.30	0.35	0.41
4. CVD-W + Plasma-Sprayed W						
Initially	0.69	0.74	0.91			
Cleaned 1-1/2				0.35	0.42	0.42
5. CVD-W						
Initially	0.61	0.70	0.93			
Remeasured				0.82	0.36	0.40
6. CVD-W + Plasma-Sprayed W						
Initially	0.67	0.75	0.76			
Remeasured				0.36	0.39	0.42

G. CONTROL DRUM DRIVE SYSTEM

The control drive system for the ZrH Reactor was evaluated under simulated launch loading and under simulated reactor thermal vacuum conditions. The test drive system consists of the rebuilt Control Drum No. 1 and supporting bearings, a mechanical mockup of the actuator with locking cam and drive gears, and the drive shaft with flexible couplings.* The thermal-vacuum tests were reported previously.† The drive system was removed from the vacuum chamber and assembled in the vibration fixture (Figure 35). One phase of the vibration was conducted with the coupling and actuator removed. The drum was positioned for maximum loading on the bands holding the BeO blocks. A sinusoidal sweep from 8 to 2000 Hz was conducted to determine resonant frequencies after which a random noise vibration was run to 1.5 times the Saturn V launch levels.

*SNAP Reactor Programs Progress Report May-July 1970, "September 15, 1970, AI-AEC-12975 (CRD)

†"SNAP Reactor Programs Progress Report, February-April 1971, "June 15, 1971, AI-AEC-12998 (CRD)

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Figure 35. Drum-Drive System Vibration Test
(Figure Unclassified)

A second phase of the test was run on the complete drive assembly with the drum positioned to induce maximum torsional vibration and thus demonstrate the integrity of the coupling and lock device. Again, a sinusoidal sweep and random noise vibration was conducted. A spurious signal during the second phase sinusoidal sweep imposed a 50 to 90 g shock on the assembly which deformed the bellows in the coupling but the coupling survived the subsequent testing without further damage. An increase in backlash after the vibration test was noted.

The vibration test program demonstrated that the drum and drive system are structurally sound and the function of the locking device was demonstrated. Also, the transmissibilities observed during the resonance scan indicate that the design loads can be substantially reduced.

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Figure 36. Lithium Hydride Removed from Between the Channel Structure
(Figure Unclassified)

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H. SHIELD FABRICATION DEVELOPMENT

All development activities on the design and fabrication of flight-type neutron shields were terminated at the end of Fiscal Year 1971.

The LiH Checkout Shield (Experimental Shield No. 4)

The final destructive examination of the LiH checkout shield was completed. The LiH was removed in a large area exposing one complete triangle of channels and the bottom of the shield (Figure 36). Figure 37 shows a close-up of the region looking toward the center of the shield. Visual examination of the LiH inside perforated channels did not reveal any pocketing to have occurred under the upper side of the channel, nor was any distortion of the channels evident.

Additional LiH was removed, completely exposing the inside of the channel structure and the joint between the channels and the outer skin of the shield. Again, no evidence of distortion or voids was found. Since the region examined was selected as representative of the entire shield, and since no irregularities had been observed, the destructive examination was terminated.

Samples of LiH were removed from three places in the shield — one at the region of the small void which was visually estimated to have a high percentage of impurities — the other two from the LiH in the main body of the shield, as indicated on Figure 36. Table 17 summarizes the results of the analyses.

These analyses confirmed the visual estimate of a high concentration of impurities at the small casting void, and also indicated that the LiH in the main body of the shield was of high purity (>98 wt% LiH). Since the freezing of the LiH from the bottom upward lifts the impurities to the top of the shield, preferably into the filler tube or the reservoir, a high concentration of impurities is expected at the region to freeze last or, in this case, the small casting void which tended to trap and hold the impurities.

The Large Shield Examination Room was cleaned of LiH dust and chips, which were sealed in plastic bags. The bags were transferred through the antechamber and stored in steel drums. As much of the room as possible was vacuumed to pick up the residual LiH and abrasive wheel dust. The top of the shield was covered with plastic sheeting and taped. The N₂ supply was turned

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Figure 37. Close-up of Channel Perforations
(Figure Unclassified)

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TABLE 17
CHEMICAL ANALYSIS OF LiH FROM EXPERIMENTAL
SHIELD NO. 4

	Sample No. 1 [*]	Sample No. 2 [†]	Sample No. 3 [†]
LiH (wt %)	87.1	99.2	98.4
LiOH (wt %)	5.36	0.61	0.71
CO ₃ (wt %)	0.065	0.026	0.034
Cl (wt %)	0.038	<0.001	0.005
Al (ppm)	>5000	200	130
Ca (ppm)	900	<5	35
Cr (ppm)	200	<25ND [§]	<25ND
Cu (ppm)	140	<25ND ^{**}	<25D
Fe (ppm)	100	20	50
K (ppm)	200	<25ND	<25ND
Mg (ppm)	5	10	15
Na (ppm)	200	<5D	<5D
Ni (ppm)	1400	<50ND	180
Si (ppm)	200	25	50
V (ppm)	10	<5ND	<5ND

*Sample No. 1 from near the casting void.

†Sample Nos. 2 and 3 from the main body of the shield.

§ND = not detected

**D = detected

off and air slowly introduced into the room, permitting the remainder of the LiH dust to react with atmospheric moisture and convert to lithium-hydroxide. The shield was removed from the room, and the room completely re-vacuumed. The room was placed in an inoperative condition.

The shield was placed in the casting reservoir, and the cover and all openings were welded closed to permit unattended storage of the shield.

It was concluded from this examination that a large, thin (pancake-shaped) LiH neutron shield could be cast successfully, to densities equal to or greater than the previous smaller shields. It also was concluded that the facility functioned as planned with only minor, easily correctable deficiencies.

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As a part of the program termination, the Large LiH Shield Casting Facility (Building 042, Santa Susana) was deactivated. A detailed operating procedure was prepared for the Large Shield Examination Room.

An annotated bibliography of all AI reports on shields and shielding materials generated by, or of interest to the Shield Development Program was prepared for future reference.

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VI. FUEL DEVELOPMENT

A. INTRODUCTION AND SUMMARY

The major goals of the fuel development effort are to characterize the cladding materials for each of the candidate fuel element claddings, establish the detail process parameters for fuel element fabrication, and to define the influence of the overall operating environment of the ZrH reactor fuel element in appropriate fuel element tests.

The primary effort in fuel element processing during the quarter was on evaluation of hobbing techniques and establishing weld parameters and final closure parameters for both Hastelloy X and Incoloy 800. The first 20-mil, smooth-wall Hastelloy X elements were assembled. Effort on cladding property evaluations was concentrated on completion of the Incoloy 800 heat comparison and initiation of the test program on Hastelloy X. Mechanical and thermal testing of fuel elements continued. The initial barrier creep tests on Incoloy 800 fuel elements were completed.

Evaluation of the vapor deposition for application of the burnable poison onto the fuel rods is continuing. Specimens from 3,000-hour compatibility tests were examined. Preliminary indications are that the vapor deposited coatings are stable at both 1400 and 1500°F.

B. FUEL ELEMENT MATERIALS EVALUATION

1. Fuel Element Assembly and Test

Four finned, 25-mil, Incoloy-800-clad developmental elements have now accrued over 4300 hours at 1400°F. These elements received 5 ramp thermal cycles at the 4300-hour mark. The average leak rate and hydrogen loss after the thermal cycles were 0.5 cc(STP)/hr and 1.7%, respectively. The average normalized leak rate and cumulative hydrogen loss for the endurance history are shown in Figures 38a and 38b.

The seven 15-mil, Incoloy-800-clad elements (4 double blind end, and 3 cup-plug-type) have now accrued over 20,000 hours at 1400°F and 25 ramp thermal cycles. Curves of the average normalized leak rate and cumulative hydrogen loss are shown in Figures 39a and 39b, respectively.

a. Average Normalized Leak Rate

b. Cumulative Average Hydrogen Loss

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Figure 38. Permeation History of Four 25-mil Incoloy-Clad Elements

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b. Cumulative Average Hydrogen Loss

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Figure 39. Endurance History for Seven 15-mil-wall Incoloy Elements, Four Double-Blind End and Three Cup-Plug Type

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a. Average Normalized Leak Rate

b. Cumulative Average Hydrogen Loss

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Figure 40. Endurance History for Four Enriched-Uranium 15-mil-wall Incoloy Elements, Double-Blind End Type

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The four double-blind-end elements containing enriched uranium fuel rods also have accrued over 20,000 hours at 1400°F and 29 ramp thermal cycles at intervals during the endurance exposure. The average normalized leak rate and cumulative hydrogen loss of this group are shown in Figures 40a and 40b, respectively. The relatively poor performance, when compared to the group of normal-U elements, is due to one high-leaking element. Excluding this element, the remaining three have an average normalized leak rate of 0.51 cc(STP)/hr and an average cumulative hydrogen loss of 8.42%.

Preparation techniques and welding parameters for the blind end butt weld of SCB-coated, 20-mil, Hastelloy X cladding tubing have been set. Preliminary welding parameters have also been set for the element final closure. Both welds remain to be qualified by process demonstration.

The first two smooth, 20-mil-wall, Hastelloy-X-clad, developmental elements were assembled and initial permeation testing completed. These elements contain segmented fuel with an N_H of 6.3, helium backfill, and an internal axial end gap of 0.25 in. in lieu of a vibration suppressor. The as-built permeation rates are given in Table 18.

TABLE 18
PERMEATION RATES OF THE INITIAL TWO 20-mil,
HASTELLOY-X-CLAD, DEVELOPMENTAL ELEMENTS

Element Number	Test Temperature (°F)	Measured Rate [cc(STP)/hr]	Normalized Rate [cc(STP)/hr]	
			at 1300°F, 2 atm	at 1400°F, 2 atm
X-3	1300	0.27	0.27	0.46
X-6	1300	0.20	0.20	0.35
X-3	1400	0.96	0.23	0.41
X-6	1400	0.63	0.12	0.22

Twelve additional Hastelloy X elements, essentially identical to the first two, are currently being assembled and an initial group of finned Hastelloy X cladding tubes are currently being hobbled.

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Figure 41. Strain and Leak Rate vs Time for Two Incoloy Element Barrier Creep Tests, 1100 and 1300°F

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Analysis of the dimensional data from 50 finned Incoloy 800 cladding tubes indicates that hobbing can meet the design tolerances. A number of actual dimensions did not meet drawing requirements but a statistical analysis of the data shows that the hobbing operation did control all critical dimensions within print tolerances. A high yield can be achieved by improving the machine set-up technique and performing first piece inspection before machining a quantity of parts.

A series of tests was initiated to measure the glass hydrogen barrier strain capability under creep conditions. The test consists of loading a fuel element uniaxially at temperature with leak rate and creep rate being monitored continuously. A temperature profile was used with the ends of the element 100°F cooler than the center so that maximum strain and failure of the barrier would occur in the center of the element. The test was terminated when a sharp increase in leak rate was noted, indicating barrier failure, or when 5% strain was attained. Gage marks on the cladding surface were measured after the test to determine maximum local strain areas. The first series of barrier creep tests were conducted on 25-mil-wall, Incoloy 800 (Heat HH7531A), fuel elements. A summary of test results is shown in Table 19. Figure 41 shows the strain vs time and leak rate vs time for the 1300 and 1100°F barrier creep tests.

TABLE 19
SUMMARY OF 25-mil-WALL INCOLOY 800 BARRIER CREEP TESTS

Test Temperature (°F)	Maximum Primary Creep Rate (in./in./hr)	Secondary Creep Rate (in./in./hr)	Tensile Load (ksi)	Average Creep Strain at Failure (%)	Maximum Indicated Local Strain (%)	Time to Failure (hr)
1300	3.5×10^{-4}	8.0×10^{-5}	15	3.2	7.4	300
1200	2.1×10^{-5}	3.6×10^{-6}	15	*	-	*
1100	1.7×10^{-5}	4.5×10^{-6}	18	0.22	0.4	620

*No failure after 1300 hours with average strain of 0.75%

2. Burnable Poisons

Four compatibility test elements were removed for examination after 3000 hours at temperature. Two each were soaked at 1400 and 1500°F. Specimen appearance was substantially the same as those of the corresponding 1500 to 2000-hr

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Figure 42. Ductility of 2 Irradiated SCB-Glass-Coated Heats of Incoloy 800 Tubing

test specimens, i.e., unchanged. X-ray diffraction, X-ray fluorescence, and electron microscope examinations are being performed for comparison to results obtained on the shorter term tests.

Status of continuing compatibility tests is given in Table 20.

TABLE 20
COMPATIBILITY TEST STATUS

Test Number	Specimen Type	Accumulated Time 8/24/71 (hr)	Target Time (hr)	Test Temperature (°F)
7	Disc Stack	6218	-	1430
8	Disc Stack	6194	-	1430
	Element Number			
D	9456	8362	-	1400
F	9193	5543	10,000	1300
G	9194	5543	10,000 ⁺	1300
J	F2	5541	10,000	1400
L	9567	4393	10,000	1300
O	9519	5541	10,000	1400

C. CLADDING PROPERTIES

Three special vacuum melted heats and two commercial heats of Incoloy 800 are being evaluated to optimize the chemistry for this alloy (heat chemistries listed in AI-AEC-12989). The two commercial heats 810535 and HH7531A, have been irradiated in the fully processed condition and are being stress rupture tested. Current diametral strain vs rupture life data are plotted in Figure 42. It can be noted in this figure that: (1) a large degree of scatter occurs for the 810535 heat at 1300°F and, to a lesser degree for the HH7531A heat at 1200°F—this is believed to be caused, in part, by the irregular local strain rates occurring when the glass cracks ($>4\% \Delta D/D$); (2) a single curve fits the HH7531A data for all temperatures with a minimum and then, improvement beyond ~1000 hours—this appears to be consistent with observations of other experiments reported in the literature; and (3) Heat 810535 generally has greater ductility than Heat HH7531A.

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Figure 43. Effect of Aging on the Tensile Properties of 3 Heats of Incoloy 800

Tensile tests were performed on the 3 vacuum melted heats in the STT* condition plus aging. The data obtained for the various aging treatments are shown in Figure 43. The 100-hour treatments at 1300 and 1400°F are possible pre-reactor insertion permeation test cycles. The low carbon heats, K89676 and K89339, have virtually the same tensile properties after 1000 hours of aging. Significant property changes occur in Heat K89630 (high carbon) after 100 hours at 1400°F but then a level occurs between 100 and 1000 hours of aging.

Property evaluation has begun on Hastelloy X (low Co) to assess its worth as a cladding material. The strength, low coefficient of thermal expansion, and coatability of this alloy are highly attractive. Moreover, a considerable body of literature data indicate an adequate ductility (>1%, irradiated) can be achieved. However, because of the unique heat treatment imposed by the coating firing operation, mechanical properties must be measured in the processed condition.

The tensile properties (20-mil tubing) of a Hastelloy X heat with 0.29% Co were measured in the simulated thermal treatment (STT) plus aged condition. These data are listed in Table 21 and compared to the literature data in Figures 44 and 45.

Initial results from uniaxial creep testing on the same 20-mil tubing are given in Figure 46.

*STT = Simulated Thermal Treatment — coating process time and temperature cycles in a neutral environment.

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Figure 44. Short-Term Tensile Strength of Hastelloy X

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Figure 45. Literature Comparison of Short-Term Ductility of Aged Hastelloy X

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TABLE 21
HASTELLOY X (0.29% Co) TENSILE PROPERTIES

Sample Number	Sample Condition	Test Temperature (°F)	0.2% Yield Strength (ksi)	Ultimate Tensile Strength (ksi)	Elongation (% in 2 in.)
9T0-1	STT + 100 hours at 1300°F	75	52.3	115.4	37
-2	↓	75	54.8	114.5	35
-3	↓	75	51.7	112.7	35
-4	↓	1200	36.3	84.8	44
-5	↓	1200	37.3	83.6	47.5
-6	↓	1200	34.8	83.3	47.5

Figure 46. Uniaxial Creep of Hastelloy X at 1200°F,
0.29% Co

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