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SNAP REACTOR PROGRAMS
PROGRESS REPORT
NOVEMBER 1971 - JANUARY 1972

AEC Research and Development Report

Atomics International
North American Rockwell

P.O. Box 309
Canoga Park, California 91304

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Printed in the United States of America
Available from
National Technical Information Service
U.S. Department of Commerce
Springfield, Virginia 22151
Price: Printed Copy \$3.00; Microfiche \$0.65

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

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CONTENTS

	Page
I. Introduction	9
A. Program Scope and Objectives	9
B. Summary of Progress	9
II. SPF Zirconium Hydride Reactor Engineering	15
A. Introduction	15
B. Internal Reflector Selection	17
C. Stress Analysis	17
1. Strain Gage Test	17
2. Operational Stresses	19
D. Nuclear Analysis	21
1. Reactivity	21
2. Control Drum Worth	27
3. Temperature Coefficients	27
4. Uncertainties and Shim Capability	29
5. Power Distribution	33
E. Control System Studies	33
F. Component Thermal Analyses	35
G. System Assembly Sequence	35
III. Reactor Development	46
A. Introduction and Summary	46
B. Fuel Element Heat Transfer Studies	47
C. Fuel Element and Core Hydraulic Studies	47
1. Core Mixing Experiments	47
2. Inlet Plenum Evaluation	53
D. Reflector Drive System Tests	57
IV. Fuel Development	62
A. Introduction and Summary	62
B. Fuel Element Materials Evaluation	62
1. Fuel Element Assembly and Test	62
2. Burnable Poison	67
C. Cladding Properties	69

CONTENTS

	Page
V. Thermoelectric Converter Development	73
A. Reactor Power Modules	73
1. Refractory-Clad Modules	73
2. High-Voltage Refractory-Clad Module	77
3. Prototype 5.25-v Reactor Power Modules	79
B. Pump Tubular Power Modules	79
C. Supporting Technology Development	85
1. Sublength Module	85
2. Glass Seal Development	100
References	103

TABLES

1. SPF Reactor — Design Summary	12
2. SPF Reactor — Performance Summary	13
3. Stresses on Inner Tube Surface Due to Fin Contact Load (psi/lb of applied load)	19
4. Short-Term Operational Stresses — Steady State	20
5. Short-Term Operational Stresses — PCS Transient	22
6. SPF Reactor Nuclear Performance Summary (300 kw, 5-year life)	23
7. Comparison of Calculated and Experimental Reactivities of SNAP Reactors	31
8. Hastelloy X Elements Permeation Data	63
9. Hastelloy X Elements Barrier Creep Data	64
10. Incoloy 800 Elements Endurance Data	65
11. Aging Effects on Tensile Properties of Incoloy 800 Heat K89630	69
12. Comparison of Secondary Creep Rates for Two Heats of Hastelloy X	71
13. Refractory-Clad Reactor Module Performance Stability Summary	78
14. BOI Performance Levels and Predicted Degradation Rates for Various Reactor Module Designs	80

TABLES

	Page
15. Pump Module Performance Stability Summary	84
16. Process and Design Features of TEM-15 Series Modules	87
17. TEM-15 Performance Analysis Summary	89
18. Summary of Seals Made With Lead-Base Glass Plus Feldspar	102

FIGURES

1. Reactor Vertical Section	8
2. Control Drum Drive	10
3. Core Boundary Concept	
a. Beam Type With Spline Seals	16
b. Ring Concept	16
4. Reactivity and Drum Worth of Internal Reflector Materials	18
5. 300-kwt SPF Reactor — Reactivity vs Operating Time:	24
6. 300-kwt SPF Reactor — Components of Reactivity Loss vs Operating Time	25
7. 300-kwt SPF Reactor — Percent Pre-poison Remaining vs Operating Time	26
8. Effect of 4π Shield on SPF Reactor Drum Worth [Based on 2-D analysis (DOT)]	28
9. SNAPTRAN Prompt Temperature Coefficient With Various Poison Loadings	
a. Comparison of Ni, Gd II, Gd III, and Eu V	30
b. Comparison of Sm and Gd IV + Eu V	30
10. 300-kwt SPF Reactor — Plan View	32
11. Effect of Shimming Control Drums on BOL and EOL Reactivity, and Shutdown Margin	34
12. 300-kwt SPF Reactor — Power Distribution	34
13. Assembly Sequence	36
14. Core Assembly	37
15. Thermal-Vacuum Checkout Test	38
16. Core Installation, Criticality Test, and Nuclear Calibrations	39
17. Outlet Plenum Assembly	40
18. Piping Installation	41

FIGURES

	Page
19. Packaging for Delivery to SPF	42
20. Reactor Assembly and Checkout at SPF	43
21. Reactor Installation	44
22. SPF Reactor-Shield Installation	45
23. Heat Transfer Test Assembly (Finned) (Top View — Test Section 3A)	48
24. NaK-to-Cladding Heat Transfer Correlation	49
25. R-L-N Finned 19-Element Hydraulic Test Section	50
26. Orientation of Instrumented Rod Sensors for Checkout Tests (As viewed from outlet end)	51
27. Pressure Loss Through R-L-N Finned 19-Element Test Section	52
28. Concentration Variation in Channel Next to Injected Channel	54
29. Five-Vane Diffuser in Straight Flare Inlet	55
30. Sliding Friction Test Fixture	56
31. Sodium-Silicate-Bonded Molybdenum Disulfide on Rene' 41 (0.188-in. radius rider, contact stress = 12,500 psi, total cycles = 600)	
a. Starting Coefficient of Friction	58
b. Dynamic Coefficient of Friction	58
32. Coefficient of Friction for Sodium-Silicate-Bonded Molybdenum Disulfide on Rene' 41 (0.188-in. radius rider, 500°F, 1×10^{-7} torr)	59
33. Coefficient of Friction for Sodium-Silicate-Bonded Molybdenum Disulfide on Rene' 41 (0.188-in. radius rider, 1000°F, 1×10^{-7} torr)	60
34. Hydrogen Permeabilities for SPF Cladding-Barrier Materials	66
35. Hastelloy X Stress-Rupture Data From Heat C9591 (0.89 wt % Co) at 1200°F in Helium	70
36. Overall Efficiency vs Elapsed Time for Refractory-Clad Reactor Power Modules	72
37. Power Output vs Elapsed Time for Refractory-Clad Reactor Power Modules	74
38. Open-Circuit Voltage vs Elapsed Time for Refractory- Clad Reactor Power Modules	75
39. Internal Resistance vs Elapsed Time for Refractory-Clad Reactor Power Modules	76

FIGURES

	Page
40. Predicted Long-Term Performance of 0.090-in. Washer TEM-X Modules With Various Barrier Configurations	81
41. Electrical Current Data From Three Prototype Pump Power Modules	82
42. Power Output Data From Three Prototype Pump Power Modules	83
43. Comparison of Power Output Data From TEM-15A Series Modules	90
44. Comparison of Overall Efficiency Data From TEM-15A Series Modules	91
45. Comparison of Calculated and Measured Effective Seebeck Coefficient Data Recorded During Heatup of Recent TEM-15 Series Modules	92
46. Comparison of Calculated and Measured Internal Resistance Data Recorded During Heatup of Recent TEM-15 Series Modules	94
47. Comparison of Power Output Data From TEM-15B, Tungsten-Foil Barrier Modules	96
48. Comparison of Efficiency Data From TEM-15B, Tungsten- Foil Barrier Modules	97
49. Comparison of Power Output Data From Improved Performance Stability TEM-15 Series Modules	98
50. Comparison of Overall Efficiency Data From Improved Performance Stability TEM-15 Series Modules	99

Figure 1. Reactor Vertical Section

I. INTRODUCTION

A. PROGRAM SCOPE AND OBJECTIVES

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

- 1) SPF Zirconium Hydride Reactor Engineering
- 2) Reactor Development
- 3) Fuel Development
- 4) Thermoelectric Converter Development

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 (SPF) at Plum Brook. The design power level will be 300 kw, with an outlet coolant temperature of 1200° F.

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.

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.

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.

B. SUMMARY OF PROGRESS

The conceptual design review of the 300-kw SPF reactor was held in December. At that time, the SPR reactor (Figure 1) consisted of 199 fuel elements, 18 in. long, with a lattice pitch of 0.737 in., contained in a stainless steel vessel. Four inlet and four outlet pipes were used to duct the NaK coolant to and from the reactor. In-core instrumentation penetrated the pressure vessel through double brazed seals at the outlet end of the reactor. The reactor

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Figure 2. Control Drum Drive

was supported through a flanged skirt at the outlet end. Relatively large plenums and nozzle diffuser vanes were used to assure uniform NaK distribution in the core.

Eight independent drive trains (Figure 2) with beryllium reflectors were employed to control the reactor. Each drive train consisted of an actuator-brake, gear reduction, flexible coupling, self-aligning bearings, and reflector drum. A cam and pin device was also incorporated into the design to positively lock out the drums during launch shock and vibration. Maximum use was made of S8DR-type components in the drive train. Thermal expansion mismatches were accommodated by the use of ball and socket bearings and bellows flex couplings. The "full drums out" envelope of the reactor was 26 in. in diameter.

The conceptual design and performance summaries for the SPF reactor are shown in Tables 1 and 2. A two-zone core, where the fuel element wall-to-wall spacing is decreased in the outer zone, was employed to enhance the cooling of the center zone. Hastelloy X was the reference fuel element cladding material, and Incoloy 800 was the backup material. Both alloys were readily coated with the SCB ceramic hydrogen barrier; however, the Hastelloy X had a significantly higher yield strength. The finned fuel elements consisted of three types: a right-hand pitched fin type, a left-hand pitched fin type, and a neutral or no-fin type (L-R-N). The various types of elements were arranged in a repeating array within the core to promote maximum NaK mixing.

The core temperature rise was limited to 75°F to maintain thermal stresses in an acceptable range, and to keep the fuel in the delta phase during operation. The maximum calculated cross element ΔT was 32°F, where the fuel elements were assumed clustered and the fuel rods asymmetrically oriented within the cladding. Pressure drop across the core was maintained at a sufficient level to assure that maldistribution did not occur across the core. Also, the flow rate was kept high, to avoid any transition flow regime. Burnable pre-poisons were selected to give the best balance between end-of-life (EOL) reactivity margin, shutdown margin, and prompt temperature coefficient. The control system was capable of inserting reactivity at the rate of $\sim 0.6\text{¢}/\text{sec}$; while in the scram mode, with all eight units working, it could remove reactivity at the rate of 27 $\text{¢}/\text{sec}$.

TABLE 1
SPF REACTOR - DESIGN SUMMARY

Core Design	
Number of Elements	199
Lattice Pitch (in.)	0.737
Core Vessel ID (in.)	11.6
Active Fuel Length (in.)	18
Number of Elements (inner/outer zones)	85/114
Cladding Material	Hastelloy X (Incoloy 800)
Cladding Thickness (average) (mils)	28 (33)
Hydrogen Barrier Material	SCB
Hydrogen Barrier Thickness (mils)	2
Fin Type	Right-Left-Neutral
Number of Fins Per Element	3
Spiral Pitch (in.)	5.5
Fin Height (inner/outer zones) (mils)	27/12
Fuel Element Spacing (inner/outer zones) (mils)	30/15
Element Backfill Gas	Helium
Backfill Pressure (Cold) (atm)	0.1
Internal Reflectors	NaK - Type 316 SS
Core Vessel Material	Type 316 SS
Coolant	NaK-78
Reflector Design	
Control Drum Type	Reflector-void
Drum Radius (in.)	3.52
Drum Material	Anodized Beryllium Metal
Number of Drums	8
Shim Capability (in.)	±1
Drum Envelope Diameter (in.)	26.0
Bearing Couple	Graphite vs Al ₂ O ₃
Control Drum Actuator	Redesigned S8DR

TABLE 2
SPF REACTOR - PERFORMANCE SUMMARY

Thermal Performance	
Thermal Power (kwt)	300
Outlet Temperature (°F)	1200
Core Temperature Rise (°F)	75
Peak Fuel Temperature (BOL Radial Average) (°F)	1332
Peak Cross Element ΔT (°F)	
Nominal Geometry	13
Cluster and Asymmetry	32
Core Pressure Drop (Fuel Bundle) (psi)	1.1
Reactor Design Lifetime (years)	5
Coolant Velocity (core average) (ft/sec)	4.6
Reynolds Number (core average)	15,700
Nuclear Performance	
Burnable Poison Mix	62.5% Natural Gd - 27.5% Eu-151
Total Poison Loading (\$)	4.00
Cold, Clean Excess Reactivity (\$)	14.28
Total Reactivity Requirements (\$)	11.24
EOL Reactivity Margin (\$)	2.70
Available Control Drum Worth (\$)	13.15
Minimum Shutdown Margin (\$)	3.21
Prompt Temperature Coefficient ($\$/^{\circ}\text{F}$)	-0.14
Control Performance	
Control Drum Step Size (°)	0.6
Reactivity Step Size, Maximum ($\$/\text{step}$)	1.5
Drum Rotation Rates (Maximum)	
Manual (step/sec)	1/2.5
Automatic (step/min)	1/2.5
Scram Performance	
Scram Mode	Motor Drive All Drums
Scram Rate ($\$/\text{sec}$)	27

The NaK heat transfer results indicate that the L-R-N finned fuel element approach will significantly increase the heat transfer coefficient over that of smooth tubes, and also promote interchannel NaK mixing. A radial pressure gradient of less than ± 0.01 psi appears achievable, based upon plenum hydraulic tests.

Fuel element processing work continued to be concentrated on hobbing techniques and welding of SCB-coated cladding. Thermal and mechanical testing of fuel elements, to evaluate barrier behavior, were also conducted. Short- and long-time property data on the reference alloy, Hastelloy X, and the backup material, Incoloy 800, were also generated.

Four refractory center tube modules (TEM-X Series) remained on test during the reporting period, and all continue to exhibit excellent performance stability. The longest test time on TEM-X S/N-3C is 6446 hr, and the output power remains near 300 w at a $\bar{T}_H$ of 1085°F and $\bar{T}_C$ of 507°F . Three new TEM-X modules were designed and fabricated with the recently developed glass-to-metal seals for the power pins.

II. SPF ZIRCONIUM HYDRIDE REACTOR ENGINEERING

A. INTRODUCTION

The conceptual design of the SPF reactor was completed during the quarter. The design, based on operation at 300 kw and 1200° F in the NASA Space Power Facility (SPF) at Plum Brook, consists of 199 fuel elements arranged in a cylindrical geometry. Reactivity control to achieve a reactor lifetime of 5 years is provided by 8 beryllium void-backed control drums.

The reactor design is based upon the experience gained in previous reactor tests conducted in the ZrH Reactor Development Program. The heat produced in the core is removed by circulating NaK coolant. Coolant temperature variations and fuel element cladding temperature gradients are minimized by utilizing fins on the fuel element cladding surfaces to enhance interchannel coolant mixing. Based on the results of hydraulic mixing tests, the optimum fin arrangement is a right-left-neutral configuration. In each cluster of three elements, one element has no fins, and is called the neutral element. The two-finned elements employ fins with either a right-handed or a left-handed twist. In this way, the fins always promote the movement of coolant from one channel to adjacent channels throughout the entire core while the coolant is traversing the core from the inlet end to the outlet. This fin configuration also provides an intermediate support point between fuel elements near the core midplane to reduce cladding stresses. The fuel elements are pinned to the inlet and outlet grid plates.

The fuel elements are contained in a cylindrical stainless steel reactor vessel, and the coolant circulation in the region between the boundary of the fuel element bundle and the vessel is controlled by flow control and core support rings. These rings are fabricated from stainless steel, and conform to the outer boundary of the fuel element bundle. The rings are stacked to form a continuous flow baffle between the inlet and outlet grid plates, and serve to limit the core bypass flow.

The engineering effort was directed to better define reactor thermal-hydraulic performance characteristics, assess the reactor nuclear performance characteristics, evaluate the structural margins using either Hastelloy X

6531-5205

a. Beam Type With Spline Seals

6531-5206

b. Ring Concept

Figure 3. Core Boundary Concept

or Incoloy 800 as cladding material, evaluate reactor transient performance characteristics, and establish a reactor assembly sequence.

B. INTERNAL REFLECTOR SELECTION

The internal reflectors are the filler pieces which occupy the spaces between the fuel elements and the core vessel. In previous designs, these spacers reflected neutrons back into the active core, and thereby contributed to core reactivity — hence the name, "internal reflector." In addition, they served to limit the flow which bypassed the fuel elements, and also formed the outer boundary of the outer fuel element channels, controlling the area of these channels. They also acted as a restraint to outward bowing of the fuel elements.

The internal reflector configurations were selected for detailed analysis. The first, shown in Figure 3a, is similar to previous internal reflector designs for SNAP reactors. It consists of 12 shaped pieces to fill the edge areas. In one version, the NaK-filled areas of the reflector pieces are filled with BeO. The second proposed configuration, Figure 3b, consists of 8 solid rings which are bolted tightly together. Tolerance requirements and methods of manufacturing have, at this time, ruled out making this second configuration in 1 piece.

Both configurations show large areas of the internal reflector filled with NaK. The NaK allows more neutrons to pass through to the control drums than BeO or steel; thus, its presence maximizes the control drum worths. BeO, on the other hand, reflects more neutrons back into the core, thereby increasing reactivity; but it reduces control drum worth, when compared to the NaK-filled internal reflectors. This effect is shown in Figure 4, where core reactivity and control drum worth are plotted as functions of material volume fraction. The SPF reactor design benefits from the higher drum worth, so the NaK-filled reflector pieces were preferred from a nuclear standpoint.

C. STRESS ANALYSIS

1. Strain Gage Test

The test to experimentally determine the strain on the inner surface of the fuel element cladding, due to the force of a fin on an adjacent wall, was completed. The strain on the inside of the cladding tube is important, because of the need to insure hydrogen barrier integrity.

6531-5207

Figure 4. Reactivity and Drum Worth of Internal Reflector Materials

The test objective was to determine the maximum strains (or principal stresses) on the inner surface of a cladding tube for a variety of fin geometries. Tube thicknesses of 0.020 and 0.032 in. were tested, so that the results could be interpolated. An interpolation procedure was developed, based on theory developed by Bijlaard on the induced stresses due to a pad load on a cylinder.

During this quarter, the 0.020-in. thick cylinder was tested, in a manner similar to the 0.032-in. thick cylinder test operated last quarter. A comparison of the peak stresses for the 0.20 in. and the 0.032 in. tubes is shown on Table 3.

TABLE 3
STRESSES ON INNER TUBE SURFACE DUE TO FIN CONTACT LOAD
(psi/lb of applied load)

Fin Configuration	0.032 in. Tube		0.020 in. Tube		
	Hoop (Max.)	Axial	Max.	Hoop	Axial
0.072 in. Fin	1030	812	3170	3000	1840
0.072 in. Fin, Ground Flat	855	710	2660	2370	1600
0.125 in. Fin	970	750	2640	2450	1330
0.125 in. Fin, Ground Flat	855	600	2250	2130	1440
0.160 in. Fin	950	875	2590	2550	1070
0.125 in. Fin with a 0.4-in. Radius Reverse Curvature	720	580	2000	1620	900
0.125 in. Fin with a 0.5-in. Radius Reverse Curvature	700	650	2250	2050	1350

The results of the 0.032 in. tube indicated that the principal stress directions were near the axial directions. Therefore, the maximum stress for the 0.032 in. tube is the same as the hoop stress. The principal stress direction, however, shifted with the thinner tube. The maximum stress for the 0.020 in. tube is also shown on Table 3.

2. Operational Stresses

The operational stresses include those due to the element thermal-gradient-induced bowing, in addition to the manufactured element bow. The element is restrained from bowing by its neighbors, assuming its neighbors act as a rigid

TABLE 4
SHORT-TERM OPERATIONAL STRESSES – STEADY STATE

	Inner Zone		Outer Zone	
	Environmental Condition			
	Nominal	Clustered	Nominal	Clustered
<u>0.032 in. Incoloy 800 Element</u>				
Contact Side of Element (outer surface)				
Bending stress	-2,131	-4,302	-2,393	-4,877
Six-theta stress	-1,316	-2,350	-1,319	-2,006
Contact stress				
Hoop	-6,000	-12,200	-7,200	-16,220
Axial	-3,710	-7,550	-4,400	-10,000
Equivalent stress	-6,975	-13,300	-7,695	-16,558
Contact Side of Element (inner surface)				
Bending stress	-2,131	-4,302	-2,393	-4,877
Six-theta stress	-1,816	-2,350	-1,319	-2,006
Contact stress				
Hoop	4,460	9,060	5,260	11,950
Axial	1,910	3,890	2,185	4,970
Equivalent stress	5,021	9,806	6,320	12,676
Side Opposite Contact				
Bending stress	2,066	4,251	2,752	5,610
Six-theta stress	1,058	531	763	75
Vibration suppression load	575	575	575	575
Equivalent stress	3,700	5,357	4,090	6,260
Margin of Safety				
Onset of gross hydrogen leakage	15,500	15,500	15,500	15,500
Margin of safety	2.09	0.58	1.45	0.22
<u>0.027 in. Hastelloy X Element</u>				
Contact Side of Element (outer surface)				
Bending stress	-2,061	-3,906	-2,243	-4,356
Six-theta stress	-1,545	-1,999	-1,122	-1,706
Contact stress				
Hoop	-6,660	-12,500	-8,250	-16,020
Axial	-3,880	-7,400	-4,880	-9,350
Equivalent stress	-7,486	-12,920	-8,246	-15,722
Contact Side of Element (inner surface)				
Bending stress	-2,061	-3,906	-2,243	-4,356
Six-theta stress	-1,545	-1,999	-1,122	-1,706
Contact stress				
Hoop	-4,910	9,390	6,120	11,900
Axial	2,280	3,850	2,425	4,710
Equivalent stress	5,376	9,976	6,455	12,394
Side Opposite Contact				
Bending stress	1,998	3,859	2,579	5,010
Six-theta stress	900	452	649	64
Vibration suppression load	680	680	680	680
Equivalent stress	3,578	4,991	3,908	5,754
Margin of Safety				
Onset of gross hydrogen leakage	38,500	38,500	38,500	38,500
Margin of safety	6.16	2.86	4.96	2.12

wall. The contact load will then cause a local stress and beam bending. The load vs stress data were generated in a test of a strain gaged cladding tube. Also present are circumferential six-theta stresses. These are axial stresses caused by hot line - cold line conditions, which, in turn, are caused by the proximity of the adjacent elements and the adjacent flow channels. Also, there is a stress induced by the vibration suppression device, which causes an axial internal load and a low-magnitude tensile stress. The allowable stress for the element is that which causes loss of hydrogen retention capability.

A summary of the steady-state operational stresses for Incoloy 800 and Hastelloy X claddings are shown in Table 4. These stresses correspond to the most severely loaded elements.

Table 5 presents the stresses associated with startup of the Brayton power conversion system. The transient data were supplied by NASA, and correspond to the milder of two proposed startup methods. The assumed startup involved introducing the working fluid to the Brayton loop in two steps in order to reduce the severity of the transient.

These results show the minimum margin of safety for the Hastelloy X clad element to be 1.16, compared to a negative margin of 0.16 for the Incoloy 800 cladding. For this reason, Hastelloy X was selected as the reference cladding material for the SPF application.

D. NUCLEAR ANALYSIS

1. Reactivity

The available reactivity at beginning-of-life (BOL) and the reactivity requirements for 5 years full-power operation were determined for the base reactor and three alternates. The base reactor has Hastelloy X cladding, and a pre-poison loading of \$2.5 gadolinium and \$1.5 Eu-151. The alternate cladding is Incoloy 800, and the alternate pre-poison is gadolinium alone. These data are shown on Table 6.

The reactivity variation with time is shown in Figure 5 for the base reactor, and for the alternate \$4 gadolinium pre-poison loading. The components of the reactivity loss excluding pre-poison burnout are shown in Figure 6. Pre-poison burnout is shown separately in Figure 7.

TABLE 5
SHORT-TERM OPERATIONAL STRESSES – PCS TRANSIENT

	Inner Zone		Outer Zone	
	Environmental Condition			
	Nominal	Clustered	Nominal	Clustered
<u>0.032 in. Incoloy Element</u>				
Contact Side of Element (outer surface)				
Bending stress	-2,652	-5,990	-3,352	-7,028
Six-theta stress	-2,198	-3,889	-1,856	-3,446
Contact stress				
Hoop	-7,300	-10,300	-10,000	-23,600
Axial	-4,510	-10,450	-6,130	-14,480
Equivalent stress	-8,517	-21,687	-10,730	-24,301
Contact Side of Element (inner surface)				
Bending stress	-2,652	-5,990	-3,352	-7,028
Six-theta stress	-2,198	-3,889	-1,856	-3,446
Contact stress				
Hoop	5,440	12,600	7,340	17,280
Axial	2,330	5,480	3,050	7,180
Equivalent stress	6,138	13,801	8,058	18,434
Side Opposite Contact				
Bending stress	2,378	5,874	3,273	8,084
Six-theta stress	1,629	686	2,404	29
Vibration suppression load	575	575	575	575
Equivalent stress	4,582	7,135	6,252	8,688
Margin of Safety				
Onset of gross hydrogen leakage	15,500	15,500	15,500	15,500
Margin of safety	1.52	0.12	0.92	-0.16
<u>0.027 in. Hastelloy Element</u>				
Contact Side of Element (outer surface)				
Bending stress	-2,509	-5,342	-3,084	-6,142
Six-theta stress	-1,870	-3,308	-1,578	-2,959
Contact stress				
Hoop	-7,840	-17,100	-10,100	-22,800
Axial	-4,640	-10,080	-5,970	-13,450
Equivalent stress	-8,489	-17,968	-10,375	-22,673
Contact Side of Element (inner surface)				
Bending stress	-2,509	-5,342	-3,084	-6,142
Six-theta stress	-1,870	-3,308	-1,578	-2,959
Contact stress				
Hoop	5,860	12,800	7,500	16,900
Axial	2,415	5,250	2,986	6,700
Equivalent stress	6,138	13,779	8,068	17,753
Side Opposite Contact				
Bending stress	2,250	5,239	3,011	7,115
Six-theta stress	1,385	584	2,045	25
Vibration suppression load	680	680	680	680
Equivalent stress	4,315	6,503	5,736	7,820
Margin of Safety				
Onset of gross hydrogen leakage	38,500	38,500	38,500	38,500
Margin of safety	5.27	1.79	3.77	1.16

TABLE 6
SPF REACTOR NUCLEAR PERFORMANCE SUMMARY
(300 kw_t, 5-year life)

	Base (Hastelloy X)	Alternate Cladding (Incoloy 800)	Alternate Poison (Hastelloy X)	Alternate Cladding Alternate Poison
Cold, Clean, Excess Reactivity	\$14.28	\$14.32	\$14.28	\$14.32
Hot Pre-poison Loading				
Gd	2.50	2.50	4.00	4.00
Eu-151	1.50	1.50	-	-
Cold, Poisoned, Excess Reactivity	9.94	9.98	9.60	9.64
Reactivity Requirements				
Temperature Defect	2.18	2.18	1.63	1.63
Power Defect	0.09	0.09	0.06	0.06
Xe-135	0.40	0.41	0.40	0.41
Hydrogen Redistribution	0.23	0.24	0.23	0.24
Total Recoverable Losses	2.90	2.92	2.32	2.34
U-235 Burnout	1.84	1.90	1.84	1.90
U-236 Burnin	0.67	0.69	0.67	0.69
U-238 Burnout	-0.05	-0.06	-0.05	-0.06
Fission Products	2.04	2.10	2.04	2.10
Sm-149 Buildup	1.02	1.02	1.02	1.02
Hydrogen Loss	1.77	1.54	1.77	1.54
Residual Pre-poison	1.05	1.05	0.18	0.18
Nonrecoverable Losses	8.34	8.24	7.47	7.37
Total Requirements	11.24	11.16	9.79	9.71
EOL Reactivity Margin	2.70	2.82	3.81	3.93
Total Drum Worth	13.15	13.57	13.15	13.57
BOL Shutdown Margin	3.21	3.59	3.55	3.93
BOL Prompt Temperature Coefficient ($\Delta/^\circ\text{F}$)	-0.14	-0.14	-0.09	-0.09

6531-5301

Figure 5. 300-kwt SPF Reactor - Reactivity vs Operating Time

6531-5302

Figure 6. 300-kwt SPF Reactor — Components of Reactivity Loss vs Operating Time

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6531-5303

Figure 7. 300-kwt SPF Reactor — Percent Pre-poison Remaining vs Operating Time

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2. Control Drum Worth

Reactivity control is provided by 8 void-backed beryllium drums. Rotation of the beryllium away from the core increases neutron leakage, and therefore reduces reactivity. The effectiveness of the drums is reduced somewhat by the 4π shield, which reflects some of the neutrons back into the core. Two-dimensional calculations in R,Z geometry were made to determine the maximum effect the shield might have on drum worth. The percent reduction in drum worth due to the shield, as a function of spacing between the shield and drum envelope, is shown in Figure 8. The assumed minimum clearance for the conceptual design is 0.25 in., which gives a 14% reduction in drum worth.

Due to the complex geometrical configurations which result as the drums are rotated, analytically determined drum worths were normalized to the measured drum worths of SCA-4A, S8ER, and S8DR.

Drum worth measurements indicate that the maximum worth occurs when the thickness of the drum is equal to the drum radius and when it is rotated 135° out; therefore, these conditions were used in the determination of the base SPF reactor drum worth of \$13.15. With the alternate Incoloy cladding, the drum worth increases to \$13.57 because the core is more dilute and the radial leakage is larger. The uncertainty in the base reactor drum worth is estimated to be $\pm \$2$.

3. Temperature Coefficients

One of the primary considerations in the selection of pre-poisons for the SPF reactor is the effect they have on the prompt temperature coefficient. Judged on their merits as burnable poisons alone, either samarium or gadolinium would be selected. They both burn out at an acceptable rate in a 300-kwt reactor, and leave a small residual poison at end-of-life. Unfortunately, both poisons have an adverse effect on the prompt temperature coefficient of the reactor. This is because the burnable isotopes of samarium and gadolinium all have cross sections which decrease in magnitude with increasing energy more rapidly than U-235 in the thermal range. As the system heats up, the flux shifts to higher energies; and fewer neutrons are absorbed in the poisons, relative to those in fuel, causing an increase in reactivity. To offset this, it is desirable to have a poison whose cross section increases above the peak of the thermal spectrum. The isotope which has been selected for this purpose is Eu-151.

6531-5304

Figure 8. Effect of 4π Shield on SPF Reactor Drum Worth
[Based on 2-D analysis (DOT)]

Experiments were made in the SNAPTRAN machine to determine the effect samarium, Gd-155, and Eu-151 have on the prompt temperature coefficient of a zirconium hydride reactor. Coefficients were measured for loadings of samarium, Eu-151, a mixture of Gd-155 and Eu-151, and various loadings of Gd-155. The results of these measurements are shown in Figures 9a and b. From these measurements, it is apparent that the increasingly heavier loadings of Gd-155 made the coefficient less negative. Transients on a Gd IV loading of \$7.29 could not be run, because it was feared that the coefficient might become positive. It was also determined, from the mixed Gd-155 + Eu-151 loading, that linear interpolation of the coefficient according to the dollar amount of each poison is permissible.

The SNAPTRAN measurements were used to normalize the calculations of the temperature coefficients of the SPF reactor. From this normalization, coefficients at operating temperature of $-0.14 \text{ } \epsilon / ^\circ \text{F}$ for the \$2.5 Gd + \$1.5 Eu-151 loading, and $-0.09 \text{ } \epsilon / ^\circ \text{F}$ for the \$4 Gd loading, were obtained.

4. Uncertainties and Shim Capability

The 28-group transport theory method used in the analysis of the SPF reactor was tested against several SNAP reactor experiments. The reactivities of a variety of core loadings and shim configurations were measured on SCA-4A, S8ER, and S8DR. Reactivities near critical and significantly supercritical reactivities were measured. Table 7 contains a comparison of calculation and experiment of ten of these configurations. In general, critical cores were calculated very accurately, and the large excess reactivities were underestimated. Part of the error in the supercritical reactivities may be due to the difficulty involved in their measurement. All of the supercritical reactivities were obtained from control drum calibrations, and large variations in these calibrations were obtained due to interaction with neighboring drums. When these individual drum calibrations are added to obtain large excess reactivities, considerable uncertainty may result. Most of the near-critical reactivities were obtained from inverse multiplication with all control drums full in.

Because of the uncertainty in reactivity calculations, the SPF reactor is provided with beryllium shims which may be added to or removed from the control drums (Figure 10). Two 0.5 in. and one 1.0 in. shims are provided for

a. Comparison of Ni, Gd II, Gd III, and Eu V

b. Comparison of Sm and Gd IV + Eu V

Figure 9. SNAPTRAN Prompt Temperature Coefficient With Various Poison Loadings

TABLE 7
COMPARISON OF CALCULATED AND EXPERIMENTAL
REACTIVITIES OF SNAP REACTORS

Assembly	Reactivity (\$)	
	Measured	Calculated
<u>Near-Critical Reactivities</u>		
S8DR, 165 Elements Full Shims	0.16	1.35
SCA-4A, 37 Elements 1.25 in. Reflector	-0.25	-0.73
S8ER, 211 Elements No Shims	-0.28	-0.88
S8ER, 186 Elements A Shims	0.14	0.40
S8ER, 173 Elements A + B Shims	0.10	0.60
<u>Supercritical Reactivities</u>		
S8DR, 169 Elements Full Shims	1.40	2.37
S8DR, 211 Elements Full Shims	13.62	12.14
SCA-4A, 37 Elements 2.25 in. Reflector	6.55	5.33
SCA-4A, 37 Elements 3.25 in. Reflector	10.45	8.68
S8ER, 211 Elements Full Shims	10.09	7.36

72-J4-2-6A

Figure 10. 300-kwt SPF Reactor – Plan View

each drum. This provides shim capability of ± 1 in. from nominal drum thickness, or a reactivity variation of $-\$3.35$ to $+\$2.15$. By selectively adding or removing shims, reactivities within this range are attainable to within approximately $\pm 20\%$. Figure 11 shows the reactivity variations which may be obtained by shimming the drums.

5. Power Distribution

The radial and axial power distributions for the SPF reactor are given in Figure 12. These are based on a reflector thickness which will result in a just-critical system at BOL.

E. CONTROL SYSTEM STUDIES

The SPF reactor and its interfacing components were modeled for transient simulation studies. A digital analog simulation program (Continuous System Modeling Program) was employed, which allowed detailed modeling of the control and safety systems, reactor kinetics, and system hydraulics. The simulation model was used to study system control and safety requirements during various transient situations.

Of those transients investigated, reactor scrams resulting from system malfunctions caused the worst temperature (and thus stress) conditions. Scrams investigated included: (1) manual scrams from full power, (2) step reactivity removal from full power, (3) peak rate reactivity insertion accident resulting in overpower alarm scram, and (4) loss of flow incident (pumping failure) resulting in low flow alarm scram. In each of Cases 1, 2, and 3, the temperatures and temperature gradients generated were not considered severe. A loss of flow incident, however, resulted in: (1) a core temperature rise in excess of 250°F , (2) a core outlet temperature in excess of 1350°F , and (3) an average core coolant temperature derivative in excess of $400^\circ\text{F}/\text{min}$.

In addition to the reactor plus system model, a reactor only model was formulated to simulate the control system during subcritical source power room-temperature reactivity insertion accidents. It was found that the nominal scram rate for the SPF control system (-27% /sec) would limit the fuel temperature rise to $<100^\circ\text{F}$ for all credible reactivity insertion rates.

Figure 11. Effect of Shimming Control Drums on BOL and EOL Reactivity, and Shutdown Margin

Figure 12. 300-kwt SPF Reactor - Power Distribution

F. COMPONENT THERMAL ANALYSES

Thermal analyses were performed on selected areas of the reactor-shield assembly. Temperatures in the region between the reactor inlet plenum and the control actuators were studied to determine the influence of thermal insulation between reactor support plate and lithium hydride plug shield. The lithium hydride temperatures averaged $\sim 980^{\circ}\text{F}$ without insulation, and 920°F with $1/8$ in. of insulation. The sensitivity of calculated temperatures to variation in nuclear heat generation rates was found to be small. The control actuator temperatures were found to be in the 500°F range; thus, the use of a cold plate to control temperatures in the actuator region was concluded to be unnecessary.

Analyses of control drum and stationary reflector temperatures as a function of the beryllium and cold wall emissivity values were performed. A shield cavity cold wall temperature of 500°F was assumed. If the beryllium emissivity were to degrade to as low as 0.6, a cavity cold wall emissivity of at least 0.4 would be required, in order to assure peak beryllium temperatures below the maximum allowable level of 1200°F . This indicates a probable need for a high emissivity treatment for the cold wall surface. In comparison, emissivity values of 0.8 for both cold wall and beryllium surfaces result in a predicted peak beryllium temperature of 1090°F .

G. SYSTEM ASSEMBLY SEQUENCE

The steps involved in reactor assembly, checkout, and installation were defined, to serve as a guide for preliminary design, and to establish the facility, equipment, and tooling requirements for the program.

Figure 13 shows the sequence of the basic steps. Figures 14 through 21 illustrate each of the numbered steps, and show the required unique fixtures and equipment. The final reactor-shield installation is shown in Figure 22.

COMPONENTS
FUEL ELEMENTS
INTERNAL REFLECTORS
GRID PLATES

COMPONENTS
CORE VESSEL AND HEAD
REFLECTOR COMPONENTS
CONTROL DRIVES
END SHIELD
ELECTRICAL CABLES

6531-1201

Figure 13. Assembly Sequence

6531-1202

Figure 14. Core Assembly

6531-1203

Figure 15. Thermal-Vacuum Checkout Test

AI-AEC-13023

6531-1204

Figure 16. Core Installation, Criticality Test,
and Nuclear Calibrations

AI-AEC-13023

4

6531-1205

Figure 17. Outlet Plenum Assembly

Figure 18. Piping Installation

6

6531-1207

Figure 19. Packaging for Delivery to SPF

ELECTRICAL SYSTEM CHECKOUT

REFLECTOR INSTALLATION

DRUM AND DRIVE CHECKOUT

7

6531-1208

Figure 20. Reactor Assembly and Checkout at SPF

NOTES: UNLESS OTHERWISE SPECIFIED BY THE DRAWING

Figure 21. Reactor Installation

6531-1209

6531 1210

Figure 22. SPF Reactor-Shield Installation

III. REACTOR DEVELOPMENT

A. INTRODUCTION AND SUMMARY

The current SPF reactor development efforts include the fuel element and reactor core heat transfer and hydraulic studies, the design and testing of components of the control drum drive system, and the verification of the complete control drum drive system performance. The control drive components include the modified S8DR actuator, bearings and limit switch, drum launch lock mechanism, and metallic-sheathed electrical cables.

The third series of the fuel element heat transfer tests with the R-L-N finned heater assemblies spaced at P/D of 1.05 are being conducted. Preliminary data indicate that the heat transfer between the NaK and the heater cladding is improved, over that measured for the smooth-wall heaters spaced at P/D of 1.05. Experiments to determine the cross-element temperature gradient, a measure of cross-channel coolant mixing, are in progress.

The core hydraulic tests to measure the pressure losses and the inter-channel mixing with R-L-N finned elements were started. Pressure losses at various flow rates were measured.

The results of the series of open-channel inlet plenum experiments have narrowed the selection of the inlet nozzle and flow diverter configuration applicable to the SPF core vessel to a five-vane diffuser.

To facilitate the assembly and the checkout of the actuator and drive gear assembly, the control drum drive system for the SPF was modified. Extensive friction and wear tests on surfaces coated with inorganic-binder-bonded molybdenum disulfide dry-film lubricant were conducted. Data were recorded at or above design temperature and load, for upward of 1200 operational cycles.

Samples of metallic-sheathed electrical cables with single and multiple conductors were procured, and are being evaluated. Methods for preparing the cable end, terminating the conductors, and attaching the cable sheath to the electrical components were studied; a model of a potential method was built for further study.

Reliability studies of several types of electronic control systems for driving the control drum actuators during reactor scram were made. A control concept which provides the highest reliability was tentatively selected for the SPF.

B. FUEL ELEMENT HEAT TRANSFER STUDIES

Testing is in progress on a simulated 19-element fuel bundle, made up of 12 elements having 0.030 in. fins with a 5.5 in. lead, and 7 smooth elements. The base diameter of all elements is 0.562 in., and the active length is 13 in. Figure 23 illustrates the arrangement of the elements in the bundle, and the location of the fins and thermocouples at the thermocouple plane. The finned elements are alternately right- and left-hand wrapped. Analysis of preliminary data indicates the presence of the fins improves the NaK-to-sheath heat transfer coefficient over the $P/D = 1.00$ case, and provides a moderate improvement over the ring spacer configuration, Figure 24. Significant reduction in the cladding circumferential temperature variation over the $P/D = 1.00$ case was also seen. Data are presently being taken on asymmetrical heating of the elements, to obtain qualitative and quantitative information on cross-channel mixing.

Fabrication development of prototype heaters of the size more closely approximating present fuel element design dimensions is in progress. The instrumented heater sheath assemblies with nine thermocouples embedded within the sheath wall have been fabricated, and shipped to the heater manufacturer.

C. FUEL ELEMENT AND CORE HYDRAULIC STUDIES

1. Core Mixing Experiments

The 19-element hydraulic test section was assembled with the R-L-N finned elements, with a spacing of 0.670 in., and 0.026 in. fins, as shown in Figure 25. The initial assembly was made with only three instrumented elements, other being not yet available. The location of the finned and neutral elements, fin orientation, location of the instrumentation probes, and the injection channels are shown in Figure 26.

Pressure losses through the test section were measured by pressure taps along the test section, with flow through the test section varied from a Reynolds

Figure 23. Heat Transfer Test Assembly (Finned) (Top View – Test Section 3A)

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Figure 24. NaK-to-Cladding Heat Transfer Correlation

Figure 25. R-L-N Finned 19-Element Hydraulic Test Section

AI-AEC-13023

6531-5309

Figure 27. Pressure Loss Through R-L-N Finned 19-Element Test Section

number of 1,000 to 20,000. The slight displacement of the two curves shown in Figure 27 is probably due to some entrance effects, influencing the data from the lower set of pressure taps.

Preliminary mixing tests indicated that the test section and the in-channel instrumentation were performing satisfactory, as seen by the repeatability of the data in Figure 28. However, the salt concentration, as determined by the probe located in the inlet shroud tube below the self-injected channels, varied considerably, although salt was injected into all three channels at an equal rate. From the evaluation of the data and from other experiments, it was determined that the 2-in. length of the shroud tube must be lengthened to ~ 10 in., to achieve mixing at the probe location. The test section is being modified to provide the longer shroud tubes.

During the preliminary mixing tests, it was noted that the achievable signal level from some probes decreased. Investigation revealed that the salt solution had penetrated through the instrumentation lead feedthrough seals on these elements, partially short circuiting the signal. All instrumented elements, included the unused finned elements, are being reworked to improve the lead feedthrough seals, and to seal the individual probe within the element.

2. Inlet Plenum Evaluation

The investigation into the effect of straight, constant area inlets on the plenum flow patterns and pressure gradients was extended to include flared inlet configurations. The pressure profiles in the plenum with 2 by 3 in. and 2 by 4 in. straight flared inlets, and 2 by 3 in. curved flared (trumpet shaped) inlets, were measured. Tests on these inlet configurations were conducted with and without four- and five-vane diffusers, specifically designed for the inlets.

The flattest pressure profiles were obtained with the five-vane diffusers installed in the 2 by 3 in. straight flared inlets (See Figure 29). Only 4 of the 199 pressure measurements exceeded the ± 0.01 psi design requirement, and all the data fell within ± 0.015 psi limits. Inasmuch as this configuration surpassed all the other configurations tested, it was recommended for incorporation into the plenum design.

Figure 28. Concentration Variation in Channel Next to Injected Channel

AI-AEC-13023

6531-5208

Figure 29. Five-Vane Diffuser in Straight Flare Inlet

AI-AEC-13023

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Figure 30. Sliding Friction Test Fixture

D. REFLECTOR DRIVE SYSTEM TESTS

Several tests were conducted to evaluate the performance capability of sodium-silicate-bonded molybdenum disulfide on a plate and cylinder system duplicating the reflector cam locking mechanism. The cylinder and plate were subjected to contact stresses, temperature, and vacuum conditions that could be expected during the SPF reactor testing.

All testing was conducted in an existing test fixture previously used for sliding friction measurements (See Figure 30). Test samples of Rene' 41 were dimensionally mapped, and the sliding surfaces were coated with sodium-silicate-bonded molybdenum disulfide, in accordance with NR specifications. The rider portions of the test samples were fabricated with a 0.188 in. radius, to duplicate the radius of the cam lock pin design.

The first sample couple was installed in the test rig, and cycled a total of 600 times, to evaluate the performance capability of the solid-film lubricant. The sample was loaded to an equivalent contact stress level of 12,500 psi, and cycled 100 times at room temperature in a vacuum of 1×10^{-5} torr. Each cycle approximated the distance of camming the drum out and in during the camming portion of the SPF control drum rotation. The temperature of the test section was raised to 500°F, and the sample cycled 100 times. The temperature was raised to 1100°F, 100 more cycles were completed, and the temperature was then reduced to room temperature. The entire test sequence was repeated once more. Figures 31a and b illustrate the range of the coefficients of starting (static) and running (dynamic) friction observed during the 600-cycle test. The upper points are the starting friction averages for each 100 cycles during the first test sequence at the indicated temperatures. The lower points are the observed averages during the second sequence. A visual post-test inspection of the samples indicated only a burnishing of the rubbing surfaces, with no apparent breakdown of the film adhesion.

A second sample couple was installed in the test rig to determine the endurance capability of the dry-film lubricant. The sample temperature was maintained at 500°F, at $<1 \times 10^{-5}$ torr pressure. Cycling of the test specimen was initiated with 100 cycles at a contact stress of 12,500 psi. The load was successively increased 25% after each 100 cycles of data acquisition. Figure 32

a. Starting Coefficient of Friction

b. Dynamic Coefficient of Friction

Figure 31. Sodium-Silicate-Bonded Molybdenum Disulfide on Rene' 41
(0.188-in. radius rider, contact stress = 12,500 psi,
total cycles = 600)

AI-AEC-13023

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Figure 32. Coefficient of Friction for Sodium-Silicate-Bonded Molybdenum Disulfide on Rene' 41
(0.188-in. radius rider, 500°F, 1×10^{-7} torr)

6531-4704

Figure 33. Coefficient of Friction for Sodium-Silicate-Bonded Molybdenum Disulfide on Rene' 41 (0.188-in. radius rider, 1000° F, 1×10^{-7} torr)

illustrates the behavior of the starting and running friction with each successive increase in sample loading.

After 1200 cycles and contact stress loads up to 25,000 psi, the test was terminated, due to the uncertainty of the test fixture to withstand higher loads. Visual inspection of the test samples indicated no breakdown of the film adhesion. To further evaluate the endurance capability of the dry-film lubricant, a third test was performed after the fixture was strengthened.

A sample couple, previously tested in the screening tests, was used. The sample had undergone a total of 600 cycles at contact stresses of 12,500 psi and temperatures up to 1100°F. The couple was installed in the test rig and cycled 100 times at room temperature, 29,600 psi contact stress, and 1×10^{-7} torr pressure. The coefficient of friction was found to average 0.116. The temperature of the couple was raised to 1000°F, and the sample loaded to the design contact stress of 29,600 psi and 1×10^{-7} torr pressure. An additional 100 cycles were completed. The average friction was found to be 0.131. The contact stress was then increased to 150% of design stress (44,000 psi) and cycled an additional 100 cycles. Friction values of 0.124 were observed. The contact stress was then increased to 175% of design stress (51,800 psi) and cycled 100 times. Values of friction observed averaged 0.117. At the conclusion of this test sequence, the couple was unloaded and returned to room temperature under vacuum (1×10^{-7} torr). An additional 20 cycles at the design stress were made, to determine whether any degradation occurred during the high temperature and load conditions. The average value of this friction was found to be 0.120. This compares with the initial 0.116 observed. Figure 33 illustrates the average values observed during the test cycle. At the completion of the test, the sample was removed for visual examination. Inspection of the sample couple indicated no film breakdown or abnormal conditions, as a result of the test.

IV. FUEL DEVELOPMENT

A. INTRODUCTION AND SUMMARY

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

Fuel element processing work continued to be concentrated on hobbing techniques and butt welding of SCB-coated cladding. Tests were conducted to establish permeability constants for Hastelloy X, and to confirm those being used for Incoloy 800. Thermal and mechanical testing of fuel elements continued. Initial barrier creep data were obtained on Hastelloy X fuel elements. Analyses of vapor-deposited Eu_2O_3 compatibility tests were completed, and a new test series was initiated. Cladding property testing continued, with emphasis on Hastelloy X. Some tensile data were obtained on 5000-hr thermally aged Incoloy 800 cladding.

B. FUEL ELEMENT MATERIALS EVALUATION

1. Fuel Element Assembly and Test

A group of eight Hastelloy X developmental elements continued undergoing thermal test inputs. These elements have 20-mil wall cladding, segmented fuel with an H/Zr of 1.81, and a 0.1-atm helium backfill. Permeation data for these elements are summarized in Table 8.

Developmental efforts continued on refinement of the hobbing process for fabrication of finned cladding tubes. A test program is underway to analyze the relationship between variations in the starting tube blank (ID, OD, concentricity, and TIR) and achievable tolerances in the hobbled tube.

Modification of welding parameters was begun to scale up the Hastelloy X coated butt weld process from 20- to 28-mil wall cladding. The 28-mil wall tubing is being machined from the same 50-mil wall tubing stock used for hobbing studies. Metallographic examination of the Hastelloy X butt welded specimens indicates that the oxide layer interface between the SCB and metal is relatively undisturbed by the welding operation; and, on the ID, the weld zone

TABLE 8
HASTELLOY X ELEMENTS PERMEATION DATA

Element No.	As-Measured Permeation [cc(STP)/hr at 1300°F]				
	Initial Test	Post-Scan	Post-Ramp	1000-hr Aging*	2000-hr Aging
X-7	0.13	-	0.16	0.25	
X-8	0.17	-	0.22	0.23	
X-11	0.13	-	0.18	0.24	
X-10	0.18	-	0.18	0.08	0.14
X-13	0.17	-	0.20	-	0.16
X-15	0.14	0.13	0.16	0.11	
X-17	0.20	0.19	0.24	0.20	
X-19	0.25	0.19	0.21	0.19	

*Elements X-10 and X-13 were aged at 1050°F; all other elements aged at 1300°F.

exhibits a rough appearance, comparable to the adjacent tubing. This is in contrast to the smooth, oxide-free interface observed in Incoloy 800 butt weld studies.

Two 20-mil wall Hastelloy X fuel elements were subjected to uniaxial creep tests to determine the strain capability of the hydrogen barrier. Element leak rates were monitored continuously during the tests. Barrier failure was defined as a significant increase (doubling) of the fuel element permeation rate. The tests were conducted with the ends of the elements ~100°F cooler than the isothermal central zone, to minimize end effects. Post-test analysis of incremental gauge marks and zone scan permeation data showed a good correlation between high local strain and high local leakage. The test data are summarized in Table 9.

Processing of Incoloy 800 cladding components continued to be delayed, due to inability to produce acceptable coated end cap - to - coated tube welds. An extensive series of parametric studies were performed during the reporting period. Without exception, the SCB ceramic spalled from the weld area of all

TABLE 9
HASTELLOY X ELEMENTS BARRIER CREEP DATA

Element No.	Test Temperature (° F)	Uniaxial Stress (psi)	Time to Failure (hr)	Secondary Strain Rate (%/hr)	Average Strain (%)
X-3	1200	25,000	48	6.7×10^{-3}	0.25
X-9	1200	21,200	80	2.25×10^{-3}	0.25

specimens which had 100% weld penetration in the base metal. Metallographic examination of test specimens revealed that the normal oxide interface layer between the ceramic and the cladding is missing over the weld bead. Previous studies have shown that the oxide layer is necessary to achieve a good bond of the ceramic to the cladding substrate. A final series of test specimens are currently in progress, to determine whether other process variables can compensate.

As reported previously,⁽¹⁾ the formation of nitrides on the OD of cladding tubes during air firing of the SCB ceramic degrades the ductility of the alloy. Considerable effort was directed toward modification of the firing system and cycle, so that the cladding OD could be protected by a flow of inert gas. The test results, using this approach, on Incoloy 800 cladding have not been satisfactory. In order to fire the ceramic to the normal high gloss appearance, it was necessary to appreciably increase the firing temperature over that utilized in conventional firing in static air. Based on coloration, the ceramic from the inert process was in a reduced state. X-ray diffraction patterns of the ceramic and underlying substrates from the inert and conventional processes revealed significant differences. The basic problem to date appears to be a lack of available oxygen when firing with the present inert gas flow apparatus. This observation is supported by a depletion of the interface oxide layer when the inert gas process is used. Prior work with various oxidation processes indicates that coating adherence is poorer when there is an inadequate oxide layer between the metal and the ceramic. As might be expected with the oxide layer depletion, the adherence of the inert-fired coatings is substandard, compared to the conventional process. Current developmental efforts are directed toward modifying the inert gas processing apparatus, to provide additional oxygen during firing.

In addition, alternate methods of protecting the OD during firing are under investigation.

Four 25-mil wall, finned Incoloy 800 fuel elements continued thermal endurance testing at 1400°F. Pertinent test data are summarized in Table 10.

TABLE 10
INCOLOY 800 ELEMENTS ENDURANCE DATA

Element No.	Time at 1400° F (hr)	Cumulative Hydrogen Loss (%)	Normalized* Leak Rate [cc(STP)/hr]
F-28	7776	1.30	0.64†
F-29	6408	3.41	0.28
F-30	7664	2.75	0.24
F-31	6320	2.77	0.22

*Normalized to 1400°F and 2 atm

†Element F-28 test data indicate that the hydrogen barrier was damaged by the third and fourth ramp heat cycles at 6500 and 7500 hr. The permeation rate increased sequentially from 0.15 to 0.37 to 0.64 cc(STP)/hr after these ramp inputs.

The hydrogen permeation model which has been established for the SPF reactor fuel element is:

$$L = A \left\{ \underbrace{A_D \frac{\sqrt{P}}{t} \left[C \exp \left(- \frac{E_{\text{metal}}}{RT_{\text{metal}}} \right) \right]}_{\text{permeation through defects in ceramic}} + (1 - A_D) P \underbrace{\left[K_A \exp \left(- \frac{E_{\text{SCB}}}{RT_{\text{SCB}}} \right) \right]}_{\text{permeation through SCB ceramic barrier}} \right\}, \dots (1)$$

where:

L = measured permeation [cc(STP)/hr]

A = internal surface area (cm²)

A_D = fractional ceramic defect area (defect area/A)

P = hydrogen dissociation pressure (atm)

t = cladding thickness (mm)

6531-4705

Figure 34. Hydrogen Permeabilities for SPF Cladding-Barrier Materials

$$C = 1500 \text{ cc-mm/hr-cm}^2\text{-atm}^{1/2}$$

$$E = 16,000 \text{ cal/mole (metal); } 22,950 \text{ cal/mole (SCB)}$$

$$T = \text{temperature } (^{\circ}\text{K})$$

$$K_A = 38 \text{ cc/hr-cm}^2\text{-atm}$$

$$R = \text{gas constant (cal/mol-}^{\circ}\text{K)}$$

Changes in ceramic quality in fuel elements result in changes in the calculated A_D value (i. e., the fractional ceramic defect area). In testing fuel elements, A_D values are calculated for evaluation of performance and for comparing the performance of different designs, since A_D is a dimensionless term. During this report period, tests were conducted to establish the hydrogen permeability of Hastelloy X, and to confirm values being used for Incoloy 800. These data are necessary to calculate A_D values, and to make a meaningful evaluation of element performance. The relative permeabilities determined for the claddings are shown in Figure 34, in comparison to that of SCB ceramic. The SCB curve shown was determined previously from permeation tests on coated 15-mil Incoloy 800 cladding membranes (no fuel) over a range of temperatures and pressures.

In conjunction with determining the cladding permeabilities, tests were conducted to determine the degree to which cladding permeability is reduced by the presence of oxide films on the cladding outside surface. Oxides form on the cladding surface during long-term thermal endurance or permeation tests, although a vacuum ($\sim 10^{-3}$ torr) is maintained. To evaluate oxide effects, selected fuel elements were permeation tested after removal of oxides by liquid honing or by a 100 hr soak in NaK at 1300° F. It was found that, after removal of surface oxides, the permeation rates increased by a factor of 2 to 3.

2. Burnable Poison

Analyses and evaluation of the 1500 and 3000 hr compatibility test specimens were completed. Additional data confirmed the results previously reported⁽²⁾ [i. e., Gd_2O_3 and Y_2O_3 vapor-deposited films were stable, while massive transport of Eu_2O_3 to the SCB barrier occurred in all cases]. The results consistently showed that: (1) the transfer of europium was limited to the adjacent barrier, (2) the europium was not on the surface of the barrier, but had diffused into

the barrier, and (3) there seemed to be no appreciable movement of the europium axially within the barrier.

Two duplicate fuel elements were assembled to investigate alternate methods of incorporating Eu_2O_3 . Each element contained:

- 1) SCB barrier doped with Eu_2O_3 , with and without exposure to U-ZrH fuel
- 2) SCB barrier adjacent to doped SCB barrier, with exposure to dry hydrogen but not exposed to U-ZrH fuel
- 3) Eu_2O_3 vapor deposited on stainless steel foil and encapsulated in stainless steel or Hastelloy X
- 4) Controls of the standard SCB - U-ZrH system.

The two test elements were placed on test in vacuum at 1430° F. The first element was removed from test after 112 hr, and was sectioned. The other element continues on test, and is scheduled for sectioning and examination after 1000 hr at temperature.

Visual examination of the decanned element revealed fuel-barrier reaction, with both the control and the Eu-doped SCB barriers. Evaluation of specimen interfaces by x-ray fluorescence (XRF) indicated the following:

- 1) There was no detectable europium movement from the doped barrier to adjacent fuel or exposed metal components (Hastelloy X and stainless steel), or to adjacent undoped barrier.
- 2) There was no detectable loss of europium from the foil encapsulation, as determined by XRF analyses of adjacent components.

No further analyses are planned until the 1000 hr test element is decanned. Additional work may be done at that time, as dictated by results of the 1000 hr element examination.

Five fuel elements, containing Gd_2O_3 - and Y_2O_3 -coated fuel rods, continue on long-term compatibility tests. The elements, three operating at 1300° F and two at 1400° F, have each accrued ~9000 hr thermal-aging time.

C. CLADDING PROPERTIES

Tensile testing was conducted at room temperature and 1200°F on specimens from three vacuum-melted heats of Incoloy 800 that had been aged for 5000 hr at 1200°F. There was very little change in properties for the K89676 (0.10 wt % Ti, 0.22 wt % Al, 0.006 wt % C) and K89339 (0.40 wt % Ti, 0.02 wt % Al, 0.04 wt % C) heats, by comparison to data obtained after 1000 hr of aging at 1200°F. Also, there was no discernible effect of 100-hr pre-aging at 1300°F vs 1400°F. Heat K89630 (0.50 wt % Ti, 0.24 wt % Al, 0.10 wt % C) was affected considerably by the 5000-hr aging treatment. The high carbon and titanium level in this alloy produced a large increase in yield strength, concurrent with some loss of ductility on aging. The test data for the 1000- and 5000-hr aged specimens from this heat are shown in Table 11. The data are averages of four specimens each.

TABLE 11
AGING EFFECTS ON TENSILE PROPERTIES OF
INCOLOY 800 HEAT K89630

Test Temperature (°F)	Aging Time at 1200°F (hr)	Yield Strength 0.2% Offset (ksi)	Ultimate Strength (ksi)	Elongation in 2 in. (%)
1200	1000	16.8	54.0	35.4
1200	5000	28.3	63.2	27.5

Eleven uniaxial creep tests have been completed on two heats of Hastelloy X. Samples from both heats have been tested in aged and unaged conditions. The high-cobalt heat is stronger for the three stress levels that were tested. A secondary creep rate comparison between the two heats is given in Table 12.

The two Hastelloy X heats show opposite effects on properties, as a result of thermal aging. The low-cobalt heat is stronger after a 1000-hr aging at 1200°F, which was also shown by tensile test data.⁽¹⁾ The high-cobalt heat apparently loses strength, and the creep rate is higher after aging 1000 hr at 1200°F. However, even after aging, the high-cobalt heat is still the stronger of the two heats. Additional aged creep specimens of both heats are now on test.

AI-AEC-13023
70

6531-4706

Figure 35. Hastelloy X Stress-Rupture Data From Heat C9591 (0.89 wt % Co)
at 1200° F in Helium

TABLE 12
COMPARISON OF SECONDARY CREEP RATES FOR TWO HEATS
OF HASTELLOY X

Temperature (°F)	Stress (psi)	Secondary Creep Rate (%/hr)	
		Heat C9591 (0.89 wt % Co)	Heat E9504 (0.10 wt % Co)
1200	25,000	6.37×10^{-3}	1.33×10^{-2}
1200	20,000	1.24×10^{-3}	2.54×10^{-3}
1200	15,000	2.51×10^{-4}	3.16×10^{-4}

Stress-rupture testing of Hastelloy X in a helium atmosphere was initiated, and the results to date are plotted in Figure 35. The data obtained to date are very close to the literature data.

Uniaxial creep tests have begun on archive Hastelloy X specimens from the NAA 120-4 Experiment. The specimens are electric-discharge machined from half-round cladding halves. The gauge length is 1.375 in., and the width in the gauge length is nominally 0.250 in. The wall thickness of the cladding is nominally 0.030 in. The SCB coating is removed from the specimens prior to test. Preparation of the irradiated specimens has begun at the hot cell. This material contains 1.46 wt % Co, and will not produce data for design use. Rather, the data will provide a basis for evaluating effects of thermal aging and irradiation on material which has been given all of the cladding processing steps involved in fuel element fabrication.

Figure 36. Overall Efficiency vs Elapsed Time for Refractory-Clad Reactor Power Modules

V. THERMOELECTRIC CONVERTER DEVELOPMENT

A. REACTOR POWER MODULES

1. Refractory-Clad Modules

During the past quarter, the major thermoelectric effort was directed toward the evaluation of endurance test data from four TEM-X series refractory-clad modules. Three new modules were designed and fabricated with end closure modifications required for application of the recently developed glass-to-metal seals at each power pin base. In addition, the design of a high-voltage reactor power module was identified, and drafting work for this module has been initiated.

Four refractory-clad modules remained on test during this reporting period. TEM-X S/N-3C and S/N-3E have accrued 6446 and 4577 hr of static testing at WANL, respectively, at $\bar{T}_H = 1085^\circ\text{F}$ and $\bar{T}_C = 507^\circ\text{F}$ test conditions, with the former being thermally cycled 55 times. Overall efficiency, electrical power output open-circuit voltage, and internal resistance life test data from these modules are presented in Figures 36 through 39, respectively, along with similar data from other TEM-X S/N-3 series modules currently on test. The excellent degree of performance stability of all refractory-inner-clad modules tested to date is quite evident from these data.

During the past reporting period, operating conditions on the two refractory-clad modules being tested in the liquid metal loop system at Atomics International, TEM-X S/N-3D and 3F, were modified. First, the primary and secondary axial temperature drops of both modules were reduced from 200 to 100°F, while maintaining the $\bar{T}_H = 1085^\circ\text{F}$ and $\bar{T}_C = 507^\circ\text{F}$ average cladding temperature levels. Data recorded after the test modification agree with the 200°F axial temperature drop data to within 2%, with slightly higher power and efficiency levels recorded at the final operating conditions being observed in both modules.

TEM-X S/N-3F test conditions were then adjusted to $\bar{T}_H = 1140^\circ\text{F}$ and $\bar{T}_C = 523^\circ\text{F}$ levels. Whereas data recorded at the initial operating conditions are indistinguishable from other TEM-X module data shown in Figures 36 through 39, the $\bar{T}_H = 1140^\circ\text{F}$ and $\bar{T}_C = 523^\circ\text{F}$ data are clearly distinguishable.

Figure 37. Power Output vs Elapsed Time for Refractory-Clad Reactor Power Modules

Figure 38. Open-Circuit Voltage vs Elapsed Time for Refractory-Clad Reactor Power Modules

Figure 39. Internal Resistance vs Elapsed Time for Refractory-Clad Reactor Power Modules

Module power output rose by 25 w, as a result of modifying the test conditions. A 22 w increase had been calculated. The high-temperature operation of TEM-X S/N-3F will allow a determination of the sensitivity of performance stability to module operating temperatures. The 100°F loop axial temperature drops are more consistent with projected operating conditions for reactor-thermo-electric systems.

A statistical analysis (linear curve fit) of all endurance test data from all TEM-X series modules is presented in Table 13. As can be seen, the previously reported degradation trends have not changed during the past reporting period. TEM-X S/N-3C, the refractory-clad module with the maximum testing time, has exhibited essentially no degradation of power output data in over 5000 hr of testing, including 55 thermal cycles. Insufficient data have been generated by TEM-X S/N-3F to allow a meaningful performance stability evaluation at the elevated operating temperatures.

Three additional TEM-X series refractory-clad modules were designed and fabricated during the past quarter. These modules, designated TEM-X S/N-3G, 3H, and 3J, include end closure modifications permitting the application of glass-to-metal seals at each power pin base. The design modifications consist of a radial increase in the power pin bolt circle of 0.042 in. to provide additional clearance to apply the glass-to-metal seal. In addition, the boron nitride insulators are recessed 0.050 in. (axially) to capture the glass during application. Details of the glass-to-metal seals are discussed in a later section of this report.

2. High-Voltage Refractory-Clad Module

The TEM-X S/N-4 reactor power module is directed at demonstrating that tubular converters can deliver power levels equivalent to that of the TEM-X S/N-3 series (300 w) at substantially higher voltage levels, while maintaining an acceptable power degradation rate. A reduction in thermoelectric washer axial thickness from 0.188 to 0.090 in. provides a factor of ~2 increase in voltage.

Because of the relationship between washer thickness, diffusion barrier configuration, and degradation rate, a study was performed during the past quarter to determine the final circuit configuration to be incorporated in the

TABLE 13
REFRACTORY-CLAD REACTOR MODULE PERFORMANCE STABILITY SUMMARY

Module	Test Conditions $\bar{T}_H/\bar{T}_C$ (°F)	Testing Time (Hours)	Rate of Change: % Per 10,000 Hrs.				Thermal Cycles	
			R_g	V_{oc}	P_e	η_o	Rapid	30°F/Min.
TEM-X S/N-3A*	1085/507-S	1166	4.0	1.1	-1.9	-2.0	1	49
TEM-X S/N-3B*	1085/507-S	2584	2.8	0.1	-2.5	+3.0	0	0
TEM-X S/N-3C	1085/507-S	6445	2.3	0.8	-0.7	+0.6	2	53
TEM-X S/N-3D	1085/507-L	2885	4.4	1.2	-1.9	+1.0	5	53
TEM-X S/N-3E	1085/507-S	4648	2.7	0.5	-1.7	-1.5	1	0
TEM-X S/N-3F	1085/507-L	1011	6.0	2.5	-1.0	+4.1	} 5	1
	1140/523-L	666	**	**	**	**		

* Testing Terminated
 L Indicates Loop Test
 S Indicates Static Test
 ** Insufficient testing time to allow meaningful performance analysis.

All static test data included through 1/24/72
 All loop test data included through 1/4/72

high-voltage module design. The results of the study are summarized in Table 14. Respective degradation rates were calculated, using TEM-X S/N-3 data as a base, and using empirical factors associated with each barrier configuration as determined from TEM-15 series module data.

Using the BOL power levels and associated degradation rates given in Table 14, a power output vs operation time plot was generated. The results are shown in Figure 40. Modules incorporating electron-beam vapor-deposited tungsten barriers, although they appear optimum, were eliminated from consideration for the first high-voltage module, because of the increased cost and manpower required to perform the vapor depositions. Comparing the curves for the other configurations, it was determined that the 0.010 in. mica module with no diffusion barrier represented the most reasonable performance trade-off of those modules which can be built at this time (i. e., excluding the EBVD barrier).

Drafting work on this high-voltage module has been initiated.

3. Prototype 5.25-v 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 19,400 hr of operation at $\bar{T}_H = 1025^\circ\text{F}$ and $\bar{T}_C = 538^\circ\text{F}$. Previously reported performance trends continued for this module during the past quarter.

B. PUMP TUBULAR POWER MODULES

One tubular module, designed to deliver power to an electromagnetic pump, continued on test at WANL throughout the past quarter. This module, designated as TEM-14A S/N-1, has accrued 25,860 hr of operation at nominal average cladding temperatures of $\bar{T}_H = 1125^\circ\text{F}$ and $\bar{T}_C = 570^\circ\text{F}$. A second module, TEM-14A S/N-2, was shut down as a result of an electrical heater failure. A new heater has been installed, to permit continued testing of the module.

Electrical current and electrical power output data from all TEM-14A series modules are presented in Figures 41 and 42, respectively. Data from TEM-14A S/N-3, which was removed from test for destructive examination

TABLE 14
BOL PERFORMANCE LEVELS AND PREDICTED DEGRADATION RATES FOR
VARIOUS REACTOR MODULE DESIGNS

Mica Thickness (In.)	Diffusion Barrier	T/E Washer Thickness = .090 in.				T/E Washer Thickness = .188 in.			
		B. O. L. Levels			P_e Deg. Rate (%/10,000)	B. O. L. Levels			P_e Deg. Rate (%/10,000)
		η (%)	P_e (Watts)	V_L (Volts)		η (%)	P_e (Watts)	V_L (Volts)	
.010	W foil (A)	4.28	283	5.55	2.1	4.64	291	2.70	1.0
.010	EBVD (B)	4.90	299	5.61	3.0	5.02	298	2.71	1.4
.010	None	4.90	299	5.61	4.2	5.02	298	2.71	2.0
.005	W foil (A)	4.62	300	5.56	5.0	4.83	297	2.70	2.4
.005	EBVD (B)	5.28	317	5.62	5.5	5.19	306	2.71	2.6
.005	None	5.28	317	5.62	6.7	5.19	306	2.71	3.2

- NOTES: (1) All calculations and predictions based on $\bar{T}_H = 1085^\circ\text{F}$, $\bar{T}_C = 507^\circ\text{F}$ operation.
(2) All modules incorporate 15.0 inch circuits.
(A) .004 in. tungsten foil barrier extending radially from hot junction midway through T/E washer.
(B) Electron beam vapor deposited tungsten.

Figure 40. Predicted Long-Term Performance of 0.090-in. Washer TEM-X Modules With Various Barrier Configurations

Figure 41. Electrical Current Data From Three Prototype Pump Power Modules

Figure 42. Power Output Data From Three Prototype Pump Power Modules

TABLE 15
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 -
	8013***	1.7	-0.3	-2.5	-2.5	1	2 30
TEM-14A S/N-2	18784	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.

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

All data included through January 15, 1972

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 from 6 to 12 to minimize electrical resistance, are in excellent agreement with the design levels (110 w at 500 amp).

Table 15 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 hr 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 m Ω). Because the load was bolted to the module leads, it was impossible to make meaningful instantaneous open-circuit measurements; hence, no open-circuit voltage or internal resistance data were recorded.

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 hr, and the second covering all subsequent data. As can be seen in Table 15, 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 50 controlled thermal cycles on S/N-3 (see Figures 41 and 42), 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 from 15.0 to 4.0 in. The purpose for fabricating and testing these modules is to evaluate the effects of design, processing, and fabrication modifications on module performance. All TEM-15 series modules tested to date have been operated in static test facilities at WANL.

A list of all the process and design features of each TEM-15 series module fabricated to date is presented in Table 16. Thirteen TEM-15 series modules were tested continuously, and one new module was placed on test during the past quarter.

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

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.

Endurance testing of five TEM-15A series modules continued throughout the past quarter. Electrical power output and overall efficiency data from these modules are presented in Figures 43 and 44 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.

TEM-15A S/N-5 has completed 2766 hr of steady-state operation since completion of 50 controlled thermal cycles. Analysis of post-cyclic data show the degradation rates continue to remain essentially unchanged from the pre-cyclic test levels. For example, the post-cyclic test resistance degradation

TABLE 16
PROCESS AND DESIGN FEATURES OF TEM-15 SERIES MODULES
(Sheet 1 of 2)

Module	Process and Design Innovation
15A S/N-1	Same as 15A-4 except 300 lb axial preload and EB welded.
15A S/N-4	Baseline design: 36 couples, .050 inch T/E washers, .005 inch mica, .001 inch mica radial clearance, 1100 lb axial preload, TIG welded, hydrogen purged, tungsten insert under p-type washer with axial clearance at assembly.
15A S/N-4A	Same as 15A-4. (Destructively examined after processing.)
15A S/N-4B	Same as 15A-4. (Thermal cycled at 2.5°F/min ramp rate.)
15A S/N-4C	Same as 15A-4. (Thermal cycled at 30°F/min ramp rate.)
15A S/N-4D	Same as 15A-4. (Steady state endurance tested.)
15A S/N-4E	Same as 15A-4 except not hydrogen purged.
15A S/N-4F	Same as 15A-4 except not hydrogen purged and tungsten insert clearance eliminated.
15A S/N-4G	Same as 15A-4F.
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 prevented from extending beyond mica at O. D. and tungsten insert clearance eliminated.
15B S/N-4C	Same as 15B-4B except W foil reduced from full to 1/2 radius.
15C S/N-4	Same as 15A-4 except each intercouple insulator consists of 2000 - 4000Å Mo barriers deposited between two .0025 inch mica washers.

TABLE 16
PROCESS AND DESIGN FEATURES OF TEM-15 SERIES MODULES
(Sheet 2 of 2)

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

TABLE 17
TEM-15 PERFORMANCE ANALYSIS SUMMARY

Module	Test Conditions $\bar{T}_H/\bar{T}_C$ (°F)	Testing Time (Hours)	Rate of Change: % Per 10,000 Hrs.				Thermal Cycles	
			R_g	V_{oc}	P_e	η_o	Rapid	30°F/Min.
TEM-15A S/N-1	1125/570	17,323	31.8	1.8	-18.3	-15.5	7	1
TEM-15A S/N-4D	1125/570	17,598	31.5	2.2	-17.4	-14.8	6	1
TEM-15A S/N-4E	1125/570	3,090	29.3	7.1	-13.8	-9.6	1	0
	1125/570*	9,406	49.4	2.3	-25.9	-23.3	3	1
TEM-15A S/N-4F	1125/570	6,735	26.8	3.4	-16.3	-13.4	1	1
TEM-15A S/N-4G	1025/480	1,626	7.0	3.1	-0.8	-1.1	1	0
TEM-15A S/N-5	1125/570	16,852	30.0	2.4	-16.7	-13.8	9	51
TEM-15B S/N-4A	1125/570	11,828	13.9	3.0	-6.8	-5.9	3	1
TEM-15B S/N-4B	1125/570	6,737	17.9	2.1	-12.3	-10.4	1	1
TEM-15B S/N-4C	1125/570	2,993	19.0	3.8	-10.8	-6.6	1	0
TEM-15C S/N-4A	1125/570	3,493	21.0	5.6	-9.2	-6.4	1	0
TEM-15C S/N-8	1125/570	15,017	24.5	2.3	-14.8	-11.9	6	1
TEM-15D S/N-4	1125/570	13,064	18.5	2.4	-11.0	-8.1	5	1
TEM-15F S/N-4	1125/570	4,865	50.9	1.2	-39.2	-36.5	1	0
TEM-15H S/N-4	1125/570	7,144	22.2	2.9	-14.4	-12.3	1	1

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

All data recorded through 1/15/72

** Insufficient testing time to allow meaningful performance analysis.

Figure 43. Comparison of Power Output Data From TEM-15A Series Modules

Figure 44. Comparison of Overall Efficiency Data From TEM-15A Series Modules

Figure 45. Comparison of Calculated and Measured Effective Seebeck Coefficient Data Recorded During Heatup of Recent TEM-15 Series Modules
AI-AEC-13023

rate is 30.0% per 10,000 hr, whereas, before cycling, the rate was 29.2% per 10,000 hr. Post-cyclic power output degradation rate is 15.9% per 10,000 hr, compared to 16.7% per 10,000 hr prior to cycling. TEM-15A S/N-5 performance stability data presented in Table 17 represents the results of a linear fit to all the data from this module. Within the accuracy with which the calculations can be made, however, it does not appear that the cycling has had any appreciable effect on subsequent performance stability.

An additional TEM-15A series module, designated S/N-4G, was fabricated and placed on test during this reporting period. This module, which is identical to TEM-15A S/N-4F, is being steady-state endurance tested at $\bar{T}_H = 1025^\circ\text{F}$ and $\bar{T}_C = 480^\circ\text{F}$ operating conditions, thus duplicating the inner-to-outer cladding thermal expansion mismatch existing in all other TEM-15 modules at the standard $\bar{T}_H = 1125^\circ\text{F}$ and $\bar{T}_C = 570^\circ\text{F}$ operating conditions. After establishing a degradation rate at these reduced operating temperatures, the operating temperatures will be increased to a higher level.

Data recorded during open-circuit heatup of TEM-15A S/N-4G are presented in Figures 45 and 46. Also shown for comparison are similar data from two earlier modules, TEM-15A S/N-4F and TEM-15C S/N-4A. The calculated parameters for open-circuit heatup of these three modules are identical (the EBVD diffusion barrier of C-4A, discussed later, is too thin to produce a measurable effect), and are also presented in the figures. The experimental data from the three modules show very nearly complete reproducibility. Effective Seebeck coefficient data (Figure 45) and internal resistance data (Figure 46) can be seen to correlate very well with calculations, over the entire temperature range.

Endurance test data recorded during the first 1410 hr of operation of TEM-15A S/N-4G are presented in a later section of this report. Degradation rates for this module observed thus far (see Table 17) are lower than expected.

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. Three TEM-15B series modules continued on test throughout this reporting period.

Figure 46. Comparison of Calculated and Measured Internal Resistance Data Recorded During Heatup of Recent TEM-15 Series Modules

Electrical power output and overall efficiency data from the tungsten foil barrier modules are presented in Figures 47 and 48, respectively. The individual design features of each of the modules are listed in Table 16. Although the power output and efficiency levels of the TEM-15B series modules are somewhat lower than those of the nonbarrier TEM-15A series modules, as a result of the tungsten foil shunt heat losses, greatly improved performance stability has been achieved, as can be seen in Table 17.

Both the data plots and the degradation analysis show that the performance stability of the 0.010-in. mica module (TEM-15B S/N-4A) is substantially better than that of either module incorporating 0.005 in. mica. In addition, a comparison between the two most recent TEM-15B series modules verifies that substantial performance improvements can be achieved, with no degradation penalty, by terminating the foil barriers midway in the thermoelectric washers. Temperatures beyond the midpoint are too low to sustain tellurium diffusion.

c. Additional Modules

In addition to the tungsten-foil barrier and nonbarrier modules, five one-of-a-kind modules were tested continuously during the past quarter. These are the last five modules listed in Table 17. TEM-15C S/N-8, TEM-15D S/N-4, and TEM-15H S/N-4, whose individual design features are listed in Table 16, all have accrued substantial testing times, and all continued to operate without change during the past quarter. These modules indicate that various degrees of performance stability improvements can be achieved through the use of electron-beam vapor-deposited barriers, tungsten n-washer contacts, and increased mica washer thicknesses. Power output and overall efficiency data from these modules are presented in Figures 49 and 50, respectively.

Also shown are data from TEM-15A S/N-4G, a low-temperature nonbarrier module, discussed previously. Notice that the power output and efficiency levels of this module are higher than all previously tested modules, despite its being operated at lower temperatures and at a reduced radial temperature drop. Although the Carnot efficiency for $\bar{T}_H = 1025^\circ\text{F}$ and $\bar{T}_C = 480^\circ\text{F}$ is 1% higher than for $\bar{T}_H = 1125^\circ\text{F}$ and $\bar{T}_C = 570^\circ\text{F}$ operation, a measured overall efficiency improvement of 5% was observed. This phenomenon is predicted by the performance code, and results from the fact that the lower operating temperatures

Figure 47. Comparison of Power Output Data From TEM-15B, Tungsten-Foil Barrier Modules

Figure 48. Comparison of Efficiency Data From TEM-15B, Tungsten-Foil Barrier Modules

Figure 49. Comparison of Power Output Data From Improved Performance Stability
TEM-15 Series Modules

Figure 50. Comparison of Overall Efficiency Data From Improved Performance Stability
TEM-15 Series Modules

span a more favorable band of the lead telluride figure-of-merit curves. Power and efficiency degradation rates recorded for this nonbarrier module can be seen, in Figures 49 and 50, as well as Table 17, to be nearly undetectable, thus far.

The degradation analysis presented in Table 17 indicates that the thin EBVD diffusion barrier incorporated in TEM-15C S/N-4A is as effective in reducing degradation as the 0.0004 in. tungsten foil used in TEM-15B S/N-4B. No other module with 0.005-in. mica insulators has exhibited as low a rate of power degradation at $\bar{T}_H = 1125^\circ\text{F}$ as has been measured for this module. These data indicate the desirability of using this barrier configuration, because the reduced shunt heat loss associated with the EBVD barrier results in substantially higher efficiency levels (TEM-15C S/N-4A has an efficiency level 18% higher than TEM-15B S/N-4C, and 37% higher than TEM-15B S/N-4B).

TEM-15F S/N-4 was tested continuously throughout the past quarter. This module is identical to TEM-15A S/N-4F, except for the ternary n-type material incorporated. Power output and overall efficiency data from this module indicate a rate of degradation substantially higher than that of other TEM-15 modules (see Table 17). The increased degradation is thought to be a result of contamination of the n-type material. Identification of the contamination mode will require a detailed destructive examination of the module.

2. Glass Seal Development

a. Leaded Glass

Eight-pin mockups, using a leaded glass, were able to withstand thermal cycling at a rate of $\sim 17^\circ\text{C}/\text{min}$ between 530 and 210°C . Three of these mockups survived 50 or more of the cycles. However, life testing of comparable mockups at 500°C revealed an inability of the seal to remain helium leaktight (HLT). The failure appeared to be produced by a devitrification of the glass (confirmed by x-ray studies) which, in conjunction with the lack of glass stiffness, produced shrinkage holes and helium leaks. Although the test temperature of 500°C is higher than that anticipated during actual use on a module, the seal was deemed not acceptable unless it could pass the accelerated test (i. e., higher temperature for a reasonable time period).

b. Modified Leaded Glass

A series of modified glasses was prepared by adding various amounts of feldspar to the leaded glass described previously. These additions produced increases in the melting point, stiffness, and crystallization rate and/or temperature of the glass. Concurrent investigations on the use of RF heating resulted in the elimination of producing seals with a hand torch. All seals made with the modified glass were formed using the more precise and controlled RF heating process. It should be pointed out that none of the more than 70 pin seals produced by this process has experienced a failure; all have remained helium leaktight. These data include the results obtained on the initial seals which were used to determine the parameters of the sealing process (i. e., power output, heating rate, and maximum temperature to produce finning). The resulting seals are clear and vitrified from inception, and through subsequent testing.

The results obtained on the present type seals are summarized in Table 18. Seals that have data for both cyclic and life test were cycled prior to the life test. Every seal has remained helium leaktight under the test conditions described previously. The cyclic tests have been completed; all others are continuing, with periodic checks being performed. In addition, the pin-to-plate resistance is being monitored at 500°C to assure that the glass remains non-conductive during test. No changes have been observed in the nominal $2 \times 10^6 \Omega$ value.

The module end mockups are dummy module ends which have been processed through the autoclave cycle to produce a representative sample for experiments. These ends also include power take-off rings on the pins. Number 3 was processed with power leads on the rings. Both of the ends were instrumented with thermocouples to determine the temperature distribution during the RF processing which forms the seals. The data resulted in a change of the RF coil, to permit concentration of the heat in a narrow region near the glass-metal interface. The new type of coil has been found to accomplish its intended purpose on two modules, TEM 13 and TEM-X types. As soon as the specified heating curves are established on these full-scale modules, the glass will be applied to the three TEM-X power modules which are currently being instrumented.

TABLE 18
SUMMARY OF SEALS MADE WITH LEAD-BASE GLASS
PLUS FELDSPAR

Specimen	Cycles		Life Test - 500°C (932°F)		
	Number Complete	Number Helium Leak Tight	Total Hours	Hours - Last Check HLT	
NM-S12	25	25	1260	1136	Still on test
NM-S13	5	5	1410	953	Still on test
8P-9LA			1410	953	Still on test
8P-10LA	50	50			Test complete
8P-11LA			1217	930	Still on test
8P-12LA			1217	930	Still on test
8P-18L			1260	1136	Still on test
8P-19L	54	54			Test complete
MEM-2			1120	1093	Still on test
MEM-3			480	458	Still on test (upside down)

NM-Single Pin Mockup

8P-Eight Pin Mockup

MEM-Module End Mockup

c. Glass Enamel

Another glass, AL-35, was evaluated during this reporting period. Although it produced excellent seals with extremely good adherence to both the nickel and stainless steel, its high surface tension, coupled with a low softening point, rendered it unsuitable for the intended application.

REFERENCES

1. "SNAP Reactor Program Progress Report - August-October 1971," AI-AEC-13017 (December 23, 1971)
2. W. A. Young, "Preliminary Evaluation of Thermal Endurance Tests of Eu_2O_3 - Sm_2O_3 Coated Fuel," TI-696-24-049 (December 22, 1969)