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SNAP REACTOR PROGRAMS
PROGRESS REPORT
AUGUST-OCTOBER 1969

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AEC Research and Development Report

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"SNAP Reactor Programs Progress Report, May-July 1969."

I. INTRODUCTION

A. PROGRAM SCOPE AND OBJECTIVES

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

- 1) SNAP 8 developmental reactor (S8DR) test
- 2) Reference zirconium hydride reactor engineering
- 3) Reference reactor — thermoelectric system engineering
- 4) Compact thermoelectric converter development
- 5) Component development
- 6) Fuel development
- 7) Aerospace nuclear safety

The S8DR reactor test effort includes all activities necessary for the installation, checkout, operation, test support, and disassembly and examination of the S8DR. The GFY 1970 objective consists primarily of continued endurance testing at 600 kwt.

The reference ZrH reactor engineering effort provides preliminary engineering for a reference space reactor capable of a broad range of performance, including operation at 600 kwt 1300°F for a minimum of 20,000 hr. The design must also be suitable for use with thermoelectric (TE), mercury-Rankine, Brayton, or organic cycle power-conversion systems, and must be capable of operation in either a shadow-shielded or a 4π -shielded configuration.

The reference reactor-TE system engineering effort includes preliminary system definition and related application and integration studies. GFY 1970 objectives include conceptual designs of systems in support of NASA Phase-B Space-Station/Space-Base studies.

The compact thermoelectric converter technology program is being carried out by the Astronuclear Laboratory of Westinghouse Electric Corporation (WANL) under subcontract to Atomics International (AI). This program is intended to provide the TE power-conversion subsystem for use with a ZrH reactor TE space power system. GFY 1970 objectives are to establish sensitivity of the TE module

to temperature and thermal cycling and to examine fabrication parameters. A first-generation reference module will be tested to provide degradation and life-test data. A 4-pack assembly more representative of a flight-type component will be assembled. A 24-pack converter will be designed.

The component development tasks include the development and testing of reflector components, shielding, and liquid-metal components in support of the S8DR, the reference ZrH reactor, and the reactor - TE system development programs.

The fuel-development tasks provide for basic understanding, substantiation, and improvements in the technology of ZrH-type fuel-moderator elements in support of the S8DR and reference ZrH reactor programs.

The GFY 1970 aerospace nuclear safety program work includes development of basic information for intrinsic water subcriticality and incorporation of this safeguard into the SNAP system design. Analyses and tests to determine post-re-entry and post-impact configurations of reactor systems will also be made. Impact disassembly studies will provide analysis and tests to develop analytical models and scaling laws to predict post-impact configuration.

B. SUMMARY OF PROGRESS

Operation of the S8DR long-term endurance run at 600 kwt continued throughout the quarter. Fission-product activity level has not increased greatly in the coolant and surge-tank cover-gas during this period. A summary of operation through the end of the period is given below:

Reactor critical	6,363 hr
Reactor at power (>525 kwt)	5,891 hr
Energy release	3,788,629 kwhr

Consideration of the S8DR operations problems has resulted in a re-evaluation of the fuel temperature and growth characteristics of the reactor. Parameter studies have been started for different fuel-rod diameters to determine the trade-offs between fuel temperature, fuel growth, and reactivity for a maximum reactor lifetime. Initial trade studies of fuel-element spacers have

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been completed and detailed mechanical, thermal, and hydraulic effects of the favored spacers are being investigated.

A preliminary reactor-TE power system optimization was performed for the NASA Space Base involving electrical power, unshielded-power-system weight, radiator area, and design lifetime. Shielding studies have been completed with a spectrum of dose rate constraints, and 4π shield weights have been evolved.

Eight first-generation 5.25-volt prototype TE modules were tested to approximately 2200 hr in static test. A module has completed about 900 hr in the NaK test loop and shows no change in power output. Prototype pump modules have accumulated 7500 hr with little change in performance. Performance maps have been prepared to indicate response of the converter to changes in system operating conditions.

The prototype high-temperature control-drum bearing is performing satisfactorily after 6000 hr at 10^{-8} torr and 1500°F. Three S8DR actuators successfully completed 12,000-hr design-verification testing. The addition of carbon to the liquid-lead shield has little or no beneficial effect on decarburization of the shield structural material by the liquid lead.

Seven Incoloy-800-clad developmental fuel elements have accumulated 7000 hr of thermal-endurance testing. An increase in the average leakrate has been detected since occurrence of a temperature excursion at the 5000-hr point.

Post-irradiation examination of the NAA-121-1 experiment was begun. Three of the four fuel rods were discovered to be broken. Metallographical examination of these rods is continuing.

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II. SNAP 8 DEVELOPMENTAL REACTOR

A. INTRODUCTION

The long-term endurance run at 600 kwt was continued throughout the report period.

A summary of the S8DR operating statistics up to October 31, 1969 is as follows:

Reactor critical	6362.6 hr
Reactor at power, >525 kwt	
>1280°F	1670 hr
>1000°F	5890.8 hr
Energy release	3,788,629 kwh

B. OPERATIONS

Reactor operations were continued throughout the report period at a power level of 600 kwt. On August 1 the outlet temperature was reduced from 1200 to 1100°F to evaluate the change in hydrogen lossrate. As is shown by Figure II-1 the reactivity lossrate attributed to hydrogen loss was reduced from approximately 2¢/day at 1200°F to about 1¢/day at 1100°F. On September 9 the outlet temperature was returned to 1200°F, with the hydrogen lossrate returning to about 2¢/day at that temperature.

A reactor scram occurred on October 2 when a power supply for the period-scram bypass failed. The period scram is normally bypassed to eliminate scrams from spurious noise signals whenever the reactor is operating at significant power. Since the system is designed to be failsafe, the bypass function was not operative during the period when the power supply was inoperative. A spurious noise signal during this time resulted in the reactor scram.

C. FISSION-PRODUCT MONITORING

The extensive and close monitoring of fission products in the NaK coolant and the primary system surge-tank cover gas was continued throughout the report period. This monitoring program provides data on the integrity of the fuel-element cladding during reactor operation. Included in the monitoring program are:

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Figure II-1. S8DR Operating History

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Figure II-2. On-Line Cover-Gas Monitoring Results

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- 1) Radiochemical analysis of periodic samples of the primary system coolant for Cs^{137} ,
- 2) Radiochemical analysis of periodic samples of the surge-tank cover-gas for Xe^{133} and Xe^{135} , and
- 3) On-line monitoring of the surge-tank cover-gas xenon activity.

Figures II-1 and -2 give a summary of all of the fission-product monitoring data accumulated to date. As can be seen from both figures the fission-product level has not increased greatly since about the middle of July. This would indicate that very little further cladding degradation has been experienced since that time.

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III. REFERENCE ZIRCONIUM HYDRIDE REACTOR ENGINEERING

A. NUCLEAR ANALYSIS

A parametric study was performed to determine the optimum reactor design for a variety of reactor thermal powers, operating temperatures, and lifetimes. The study provided insight into the performance margin of current reference design and its ultimate capability as well as a better basis for performing system reliability studies. Characterization of the 295-element core design with nominal 16.825-in. long fuel as a function of operating power level and reactor-outlet temperature was completed during this reporting period. Lifetime characterization of other representative core sizes and fuel-element configurations is in progress.

Table III-1 shows the effect of increasing fuel-element length and core diameter while maintaining constant fuel and cladding volume fractions. While the vessel ID for the reference reactor design is slightly smaller than the previously quoted diameter, the incremental changes noted are valid. The 9.214-in. ID vessel corresponds to the ID of the S8DR reactor.

TABLE III-1
EFFECT OF CORE LENGTH AND DIAMETER ON EXCESS REACTIVITY
(BASE CASE: 11.215-in.-ID Vessel x 16.825-in.-Long Fuel
15-mil Hastelloy-N Cladding
5-in.-Thick Beryllium Reflector)

Core Length (in.)	Vessel ID (in.)	Reactivity Change (\$)
16.825	11.215	0.00
24.825	11.215	+5.40
16.825	10.376	-3.00
16.825	9.214	-5.90
20.825	9.214	-2.00
24.825	9.214	+0.20

Table III-2 shows the reactivity effect of using Incoloy-800 cladding instead of Hastelloy-N. The table also illustrates the effect of shaving the fuel-rod diameter, changing the cladding thickness, and changing the fuel hydrogen content for the reference reactor core vessel diameter and for an S8DR-size core vessel.

TABLE III-2
MATERIAL WORTH IN REFERENCE ZIRCONIUM HYDRIDE REACTOR

	Change in Excess Reactivity
Change 15-mil Hastelloy-N cladding to 15-mil Incoloy-800 cladding	+\$2.60
Hastelloy-N cladding worth for cladding - thickness increase	-30¢/mil
Incoloy-800 cladding worth for cladding - thickness increase	-12¢/mil
Fuel-meat worth for rod (11.215-in.-ID vessel)	22¢/mil
- diameter increase (9.214-in.-ID vessel)	25¢/mil

Table III-3 shows poison-back control-drum worth for various core sizes, and the increase in system excess reactivity and control when 5-1/8-in. thick void-backed control drums are used instead of the reference design Ta-10W poison-backed control drums.

TABLE III-3
COMPARISON OF REACTOR EXCESS REACTIVITY FOR VOID- AND
POISON-BACKED REFLECTOR SYSTEMS;
COLD, CLEAN, UNPOISONED

Core Vessel ID (in.)	Fuel Length (in.)	Cladding Material	Poison-Backed Drums Reactivity(\$)		Void-Backed Drums Reactivity(\$)	
			Excess	Control	Excess	Control
11.215	16.825	Hastelloy-N	16.00	8.75	21.15	~13.50
9.214	16.825	Hastelloy-N	10.10	13.55	16.85	21.00
11.215	16.825	Incoloy-800	18.60	8.75	23.75	~13.50
11.215	24.825	Incoloy-800	24.00	9.00	28.85	-
9.214	16.825	Incoloy-800	12.70	13.55	19.45	21.00
9.214	24.825	Incoloy-800	18.80	13.70	25.25	-

A comparison of the system excess reactivity for four fuel-element configurations is shown in Table III-4 for cores with Hastelloy-N and Incoloy-800 claddings. Figure III-1 provides a preliminary evaluation of the maximum attainable reactor power and lifetime. The reactor has ten poisoned-backed control drums. The unpoisoned excess reactivity of the reactor was \$16.00 (15-mil Hastelloy-N cladding), but the initial prepoison loading reduced the beginning-of-life (BOL) excess reactivity to \$7.00. Variable flow maintained a 200°F ΔT across the core at all power levels.

Fuel-element diametral gas gap was optimized for each power level and operating temperature. The fuel-growth limit was defined as the point when the hottest nominal fuel element (excluding any hot-channel effects) would be in full diametral contact with the cladding, rather than that associated with a computed probability of fuel-element cracking (failure). No credit was taken for cladding ductility.

TABLE III-4
EFFECT OF FUEL-ROD SIZE ON EXCESS REACTIVITY
(11.215-in.-ID Vessel x 16.825-in.-Long Poison-Backed Drums;
10-mil-Diam Gas Gap, 20-mil Interelement Spacing,
15-mil-Thick Cladding)

Fuel Meat OD (in.)	Number of Elements	Cladding Material	Excess Reactivity (\$)
0.5322	295	Hastelloy-N	16.00
0.4878	349		14.50
0.4411	421		12.70
0.4012	499		10.85
0.5322	295	Incoloy-800	18.60
0.4878	349		17.25
0.4411	421		15.65
0.4012	499		14.05

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Figure III-1. Reference Zirconium Hydride Reactor
Power vs Lifetime

Reactivity limit was defined as the point when it was no longer possible to maintain operation at rated power and temperature conditions. Although exceeding the fuel-growth limit will result in failure of fuel elements with corresponding increase in the reactivity lossrate, the lifetime is ultimately limited by the reactivity losses.

Hydrogen lossrate was calculated based on isothermal permeation tests of fuel elements for 10,000 to 16,000 hr. If the fuel elements fail (crack), however, the lossrate will greatly exceed that predicted on the isothermal testing. Fuel-element failure at low temperatures (800°F inlet) will cause a smaller percentage reduction in reactor lifetime than a similar failure at high temperature (1100°F inlet). While out-of-pile isothermal testing of H₂ permeation indicates little H₂-barrier degradation to 16,000 hr, endurance data are not available to provide leakrate data to longer times.

The H_2 loss and fuel-growth computations were made assuming a gaseous continuum in the fuel-to-cladding diametral gas gap. This assumption implies use of a second gas in the gap to overcome the very low H_2 pressures which occur at the lower operating temperatures of 1000 and 1100°F outlet. The use of helium in the annulus to provide low-temperature conductance during reactor startup is currently under consideration and would provide this function.

The dotted lines of Figure III-1, showing constant peak fuel burnup as a function of power, represent a potential area of uncertainty. Fuel-growth data for temperature domains of interest and fuel burnup of 1 met. at. % and greater do not exist. There are some theoretical reasons to expect a "breakaway" fuel growth to occur at burnups greater than about 1 met. at. %. Growth limits are suspect at burnups greater than 1 met. at. % and it is not known whether the error makes the results conservative or optimistic. Thus lifetime projections which encompass peak fuel burnups of greater than 1 met. at. % represent a "best estimate" resulting from a very long extrapolation.

B. FUEL-ELEMENT SPACER

1. Trade Study

Evaluation of various fuel-element spacer configurations was completed. The purpose of the spacer is to minimize fuel-element circumferential gradients, associated cladding stresses, and hot-channel factors.

A summary of the analytical results comparing spacer characteristics is given in Table III-5. Characteristics of nonspaced fuel elements (except for fixed ends at the gridplate) are shown for comparison. A large circumferential temperature gradient exists for bare fuel elements with pinned ends because of the lack of resistance to thermal bowing, with resultant pinching of certain coolant channels and opening of contiguous coolant channels. Bare rods with fixed ends offer increased resistance to thermal bowing and will therefore exhibit lower circumferential gradients. Both types of nonspaced cores, however, may be unstable because of lack of positive spacing.

The circumferential temperature gradients which can result in thermal bowing are at satisfactory levels for all of the spacer configurations. Local hot spots,

TABLE III-5
COMPARISON OF FUEL-ELEMENT SPACER CHARACTERISTICS

Spacer Type	P (psi)	Circumf.* ΔT (°F)	Local Hot Spot (°F)	Clad- ding Stresses (ksi)†	Vari- able Spac- ing	Develop- ment Required	Cool- ant Mix- ing
None; pinned ends	0.093	75	0	0	Yes	None	No
None; fixed end	0.093	45	0	0	Yes	None	No
Round-wire wrap	0.164	32	7	43	No	Moderate	Yes
Flat-wire wrap	0.152	32	7	19	No	Moderate	Yes
Integral fin	0.152	32	3	19	No	Moderate	Yes
Slotted tube	0.256	36	4	17	Yes	Slight	No
Twisted ribbon	0.226	27	5	25	Yes	Slight	Yes
Cookie cutter	0.145	45	19 to 135§	17	Yes	Extensive	No

*Add 38° F for worst case of rod touching clad.

†Assumes elements have 10-mil bow, spacer has 5-mil clearance.

§Assumes no NaK under band.

however, can be high for spacer designs that favor gas entrapment: these include the cookie cutter and the flat-wire wrap. Cladding stresses are high in the case of the round-wire wrap, which produces a point loading of the cladding. The other spacer designs result in line loads which produce minimum stresses. Spacer designs amenable to variable spacing of the core to allow matching of coolant distribution and the radial power-generation profile, and which provide interchannel coolant mixing, can significantly reduce core temperature gradients. The wire-wrap and integral-fin spacer designs provide mixing but can only provide variable spacing on a zone basis. Relatively large clearances will exist at zone interfaces but the allowable bowing and resultant circumferential gradients will remain small. An alternate version would use a variable element OD on a zone basis to graduate the flow channel areas. This would eliminate the matching problem at zonal interfaces.

The conclusions reached in the trade study are that two spacer configurations warrant continued evaluation: (1) integral fin, and (2) twisted ribbon. The bases for the conclusion are that both spacer types have acceptable pressure drops and low inherent circumferential-temperature gradients and hot spots, and are amenable to low clad-stress designs. These designs preclude gas or vapor

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Figure III-2. Comparative Evaluation of Local Bending Stresses in Cladding Wall

entrapment and therefore eliminate potential source of significant cladding hot spots. Also, both designs provide interchannel coolant mixing which will dampen both hot spots and circumferential gradients induced by thermal bowing to the degree allowed as a result of dimensional tolerances. The problems associated with integral fin fabrication, including extrusion and machining methods, require further investigation to determine the practicability of this spacer design. In the case of the twisted-ribbon coolant-channel inserts the problems associated with stable installation in the core assembly to preclude vibration and fretting corrosion require further study.

The wire-wrapped cladding (i.e., integral fin) and twisted-ribbon in tricuspid flow channel concepts were evaluated with respect to cladding stress. Local bending stresses are shown in Figure III-2 and -3. The gross bending stresses through the cladding tube due to the restraining force must be superimposed upon the local bending stresses. For a given deflection condition the gross bending stresses are independent of wall thickness; however, the local cladding stresses are approximately inversely proportionate to wall thickness.

2. Mechanical Design

Tentative design configurations for the integral fin cladding tube and the tri-form spacer were established. The configurations shown in Figures III-4 and -5 were selected based on a 0.25-psi pressure drop through the core with a 0.035-in. average spacing. Study showed that fin-on-fin contact at all close approaches requires an even number of fins, with fin pairs spaced 180° apart. The best configurations are two fins 180° apart, four fins 90° apart, six fins 60° apart, etc.

Three- and four-zone variations in element configuration were studied with lattice pitch and diameter encompassing the fins constant. A four-zone design gives decided improvement in flow control over that of a three-zone. Table III-6 shows options for the design of zoned, constant-pitch variable-spaced cores. The uniform-wall-thickness option is the most costly and the uniform-cladding-ID is the least.

C. THERMAL ANALYSIS

Parametric thermal studies for the basic ZrH reactor concept are being carried out. Some results of these studies are shown in Figures III-6, -7, and -8.

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Figure III-3. Comparative Evaluation of Longitudinal Local Bending Stresses in Cladding Wall

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Figure III-4. Tri-Flute Spacer

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Figure III-5. Finned Cladding Variable Pitch Spacing

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TABLE III-6
DESIGN OPTIONS FOR CONSTANT-PITCH
VARIABLE SPACING

Design	Fin Height (in.)	Wall (in.)	Number of Rods	Average Wall (in.)
A. Uniform Wall				
3-Zone	{ 0.027 0.021 0.014	0.015 0.015 0.015	37 90 168 <hr/> 295	<hr/> 0.015
4-Zone	{ 0.027 0.022 0.017 0.013	0.015 0.015 0.015 0.015	37 54 78 126 <hr/> 295	<hr/> 0.015
B. Uniform ID				
3-Zone	{ 0.027 0.021 0.014	0.015 0.021 0.028	37 90 168 <hr/> 295	<hr/> 0.0242
4-Zone	{ 0.027 0.022 0.017 0.013	0.015 0.020 0.025 0.029	37 54 78 126 <hr/> 295	<hr/> 0.0245
C. Stepped ID				
3-Zone	{ 0.027 0.021 0.014	0.015 0.021 0.015	37* 90* 168† <hr/> 295	<hr/> 0.016
4-Zone	{ 0.027 0.022 0.017 0.013	0.015 0.020 0.015 0.019	37* 54* 78§ 126§ <hr/> 295	<hr/> 0.0176

*0.540 ID

†0.566 ID

§0.560 ID

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Figure III-6. ZrH Reactor Nominal 600-kw Conditions, Maximum Radial Averaged Temperature for Nominal Fuel Element

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Figure III-7. ZrH Reactor Nominal 600-kw Conditions, Temperatures After 20,000 hr (Includes fuel-growth effect)

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Figure III-8. Sensitivity of Fuel Temperature to Radial Gas Gap

Fuel-rod diameters stated are for the fuel meat itself and do not include the cladding. Calculations were made for an average rod at 600-kw conditions. A 10%-flow bypass and 15-mil cladding of Incoloy-800 were assumed for all cases. The maximum temperature shown as the ordinate is the maximum radial-averaged temperature along the length of the fuel rod. This temperature is the significant temperature for fuel-growth correlations. Figure III-6 is for BOL and III-7 shows similar temperatures for the same parameters after 20,000 hr of operation and includes the effects of fuel growth. A line has been drawn on both Figures III-6 and -7 to indicate the fuel temperatures attained by reducing the gap in proportion to the rod diameter. The sensitivity of fuel temperature to gap width is shown explicitly in Figure III-8. Further calculations are being made for 1000-kw power conditions. Cladding hot spots caused by thermal resistance of fuel-element spacers have been determined for the spacer at the location of interest. Hot spots can vary from about 3 to 30°F depending on the type and location of spacer. Loose-fitting spacers which allow a NaK film between the element and the spacer result in the larger hot-spot temperature differentials.

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Figure III-9. Calculated Core Temperature vs Power for Natural Convection Air-Cooling of Bare Open Core

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D. SHIELD ANALYSIS

The total nuclear heating in the reference shield was determined from the ORNL DØT case to be about 4 kw. Since this significantly disagrees with a preliminary AI estimate of about 14 kw, the thermal and stress analyses were postponed pending resolution of the above difference. Review of the ORNL case indicated a possible inconsistency in the cross-sections used for the flux calculations and heating activities. The AI estimate came from the shield materials trade study and consequently tended to be on the conservative side (too high). ORNL was requested to review their cross-section library and to run additional DØT cases to investigate low-energy streaming past the poison-backed control drums. Meanwhile best-estimate of the heat generation is in process so that the thermal analysis can begin.

E. SYSTEM SAFETY ANALYSIS

Evaluation of reactor steady-state operation following re-entry and non-destructive impact was started during this period. The principal mechanism governing system performance under the assumed conditions is the heat-rejection capability of the assembly. The relationship of core temperature to core power was evaluated under the conditions of an air-filled critical core and a water-filled critical core at various steady-state power levels. Figure III-9 shows calculated system core temperature vs thermal power for the case of the reactor assembly being critical with an air-cooled core. Figure III-10 gives the same situation, including enthalpy rise for a water-flooded core.

F. CONTROL-DRUM TEMPERATURES

The steady-state thermal analysis of the reference control-drum configuration was extended to cover the effect of (1) the variation in emissivity values of the surfaces, and (2) the removal of the drum cladding. The study was made with heat-generation rates for the control drum corresponding to the 1200-kwt reactor-power level. For all configurations investigated the NaK temperature was assumed constant at 1110°F.

The results of peak temperature at the axial midplane for the BeO and the Ta-10W sections are summarized in Table III-7 for both the BeO-in and the poison-in drum orientations. As indicated in the tabulated results, appreciably higher

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Figure III-10. Enthalpy Rise for Natural-Convection
Water-Cooling of Bare Core

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TABLE III-7
CONTROL-DRUM PEAK TEMPERATURES IN °F
FOR VARIOUS CONFIGURATIONS

Drum Orientation	BeO-In		Poison-In	
Material	BeO	Ta-10W	BeO	Ta-10W
Reference configuration: (TI-696-26-017); all $\epsilon = 0.60$	1772	1614	1655	1788
Configuration 1: Same as reference but all $\epsilon = 0.30$	2186	1956	2012	2207
Configuration 2: Same as 1 but oxidized drywell, $\epsilon = 0.73$	2081	1804	1866	2038
Configuration 3: Same as 1 but no cladding	1922	1780	1773	2082
Configuration 4: Same as 3 but oxidized drywell, $\epsilon = 0.73$	1732	1590	1583	1892
Configuration 5: Same as 2 but inner cladding and inner Ta-10W surfaces coated, $\epsilon = 0.80$	1967	1776	1825	1985

material temperature than indicated in the reference configuration analysis can be expected if no emittance coatings are provided for the surfaces considered. These temperatures can be lowered by 100 to 150°F by providing a higher emissivity-oxidized drywell.

Configuration 5 is a realistic compromise between the ideal reference configuration and that with no surface coatings.

The effect of removing the drum cladding is to decrease significantly the peak temperatures of the materials by eliminating an additional radiation interchange. The removal of the cladding coupled with an oxidized-drywell surface, Configuration 4, gives the lowest temperature levels of the cases investigated at the reactor power of 1200 kw. However, the mechanical uncertainty of a design without cladding tends to offset the inherent advantages of this configuration.

IV. REACTOR-THERMOELECTRIC SYSTEM ENGINEERING

Applications of zirconium hydride reactor-thermoelectric power systems for manned space stations and space bases were given increased emphasis to support customer efforts in this field. A planned space station with up to 12 men will require a nominal 25 kwe while a space base with 50 men would need about 100 kwe. The latter requirement will probably be supplied by two complete nuclear power systems. The desired power system life is 10 yr with five as a minimum. The proposed operational date for the station is 1975 and for the base 1977 to 1980. Because the base is conceived as a buildup from the station, the power system will be selected considering requirements of the base. This approach has placed emphasis on higher power levels and longer lifetimes than those previously considered.

To provide tools for analyzing the higher power thermoelectric (TE) systems, computer programs are being prepared to permit rapid optimization of all the major components and subsystems. Included are those for the TE converter, NaK pumps, piping subsystem, radiator, and shield. The codes for the piping subsystems, radiator, and shield are in working form, but are not completely finalized.

A. SYSTEM OPTIMIZATION

A preliminary reactor-TE power system optimization was performed for the space base to relate major variables: (1) electrical power; (2) power system unshielded weight; (3) radiator area; and (4) design lifetime.

Figure IV-1 shows the expected lifetime of the Reference ZrH reactor as a function of thermal power and reactor-outlet temperature. For the higher power system a reactor was defined having a 50% longer core length. This reactor results in about a 50% increase in lifetime for any given thermal-power level.

Figure IV-2 was constructed from a knowledge of the weights of various components, in particular the variation of radiator weight and area for various cold-junction temperatures. The performance of a particular PbTe module (13G) was used to establish the converter weight, size, and thermal power.

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Figure IV-1. Reference ZrH Reactor Expected Lifetime

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Figure IV-2. Effect of Average Cold-Clad Temperature on Unshielded Weight and Area

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Figure IV-2 indicates the various combinations of weight and area that can be selected for a particular reactor-outlet temperature (1125°F) and electrical power level (66 kwe). Unshielded system weights for other electric power levels were also determined as shown in Figure IV-3. This figure is for a system life-time of 5 yr.

B. SHIELD PARAMETERS

Parametric shielding studies have been carried out using the TRIPLEX-2 code. This code is a shield weight of optimization code using three-dose constraints in three directions; bottom, side, and top. This version uses a 4 π geometry where the top and bottom sections are ellipsoids of revolution, and the sides are cylinders. Figure IV-4 gives the shield weight as a function of side and top doserates ranging from 10 to 10,000 rem/6 mo at 100 ft. The direct (or bottom) doserate was varied between 1 and 5 rem/6 mo. The thermal powers used were 300, 600, and 1200 kwt. The shield weights ranged from 14,000 to 80,000 lb. These weights do not include the shielding required for the power-conversion system.

Figure IV-3. Effect of Power and Lifetime on Unshielded Weight

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Figure IV-4. Parametric 4 π Shield Weights

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V. THERMOELECTRIC CONVERTER DEVELOPMENT

(Prepared by Westinghouse Astronuclear Laboratory)

A. REACTOR TUBULAR POWER MODULES

1. First-Generation Prototype Modules

Five first-generation reactor power prototype modules were tested in static test stands at WANL (Westinghouse Astronuclear Laboratory) during the past quarter. An additional module, TEM-13F SN-3, was tested in the liquid-metal-loop system at AI (Atomics International). All of these modules were endurance-tested at average clad temperatures simulating the nominal temperatures of the two-loop reactor system, $\bar{T}_H = 1125^\circ\text{F}$ and $\bar{T}_C = 570^\circ\text{F}$.

a. Solid Tungsten Conductor-Ring Modules

Two prototype reactor modules fabricated early in the Compact Converter System Technology Program were removed from test during the month of August. These modules, TEM-13A SN-1 and -13C SN-2, were fabricated before the development of the two-piece inner-conductor ring. Initial degradation rates observed in these modules are assumed to be primarily controlled by the contamination of N-type material by P-type as a result of lead telluride diffusion through and around the mica insulator/inner-conductor-ring butt-joint. A destructive-examination program has been planned for these as well as other first-generation prototype reactor modules.

b. Modules Incorporating Stepped Inner Conductor Rings

TEM-13D SN-2 incorporates TEGS-3P P-type thermoelectric (TE) washers, whereas all other reactor power modules tested during this quarter use TEGS-2P P-type material. Figure V-1 shows adjusted power-output data from this module, plotted as a function of time on test. For each data point the experimentally measured power-output data have been corrected to account for minor deviations in experimentally recorded operating temperatures from the design operating temperatures, $\bar{T}_H = 1125^\circ\text{F}$ and $\bar{T}_C = 570^\circ\text{F}$. This adjustment is made to remove the effects of minor temperature fluctuations on experimental data which, in an analysis of data from modules with short testing times, could mask the effects of module degradation.

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Figure V-1. Electrical Power Output Data from TEM-13D SN-2

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Figure V-2. Comparison of Power-Output Data from All TEM-13F Series Modules

Shown on the figure are the times at which all deviations from steady-state test conditions occurred. Categorized as "instrumentation modifications" are all shutdowns and subsequent restarts made to modify or replace instrumentation of any sort (such as cooling-fin modifications, fuse replacement, etc.). Power failures are caused by laboratory electrical interruptions. In addition to these unscheduled thermal cycles, a series of ten thermal cycles was performed after approximately 2200 hr of testing.

Despite the rather large number of thermal cycles, programmed and non-programmed, the module has exhibited somewhat continuous performance trends. That is, there has been no gross effect on module performance levels or degradation rates observed as a result of transient operation.

Testing of three first-generation (14-volt) reactor power modules incorporating TEGS-2P material continued during the month of September. Adjusted power-output data from these modules, the TEM-13F series, is shown in Figure V-2. Experimental power-output data have been adjusted to account for minor temperature deviations ($\pm 4^\circ\text{F}$) from the design levels of $\overline{T}_H = 1125^\circ\text{F}$ and $\overline{T}_C = 570^\circ\text{F}$. As above, the time at which all deviations from steady-state testing conditions occurred for each module is shown on the figure.

Evaluation of data from TEM-13F series modules in Figure V-2 indicated a potential design weakness. TEM-13F SN-3 was removed from static test at WANL after 500 hr of testing and subsequently shipped to AI for additional testing in the two-loop liquid-metal system. During the startup of this module a blockage in the primary loop made it necessary to transfer heat for a short period of time from the secondary loop, through the module, into the primary loop. This process was considered necessary to clear the blockage and was accomplished by raising the outer-clad temperature level above that of the inner clad.

An examination of the power output data from TEM-13F SN-3 in Figure V-2 reveals a discontinuity in the degradation rate before and after placement in the liquid-metal loop system.

TEM-13F SN-2, an identical module to SN-3, was tested for 1100 hr in a static test stand when a blower fan failure caused a cold-side (outer-clad) temperature excursion from the design level of 570 to 870°F. During this time

period the hot-side (inner-clad) temperature level was held constant at $\bar{T}_H = 1125^\circ\text{F}$ by an automatic temperature controller. An examination of power-output data from TEM-13F SN-2 reveals a discontinuity in the degradation rate immediately before and after this cold-side temperature excursion. The SN-2 discontinuity appears to be identical to that observed in SN-3 after its cold-side temperature excursion.

2. First-Generation 5.25-volt Prototype Modules

Static testing of eight TEM-13G series prototype reactor power modules continued at WANL during the last quarter. In addition TEM-13G SN-2 was placed on test at AI in the liquid-metal loop system. A complete summary of test data from all TEM-13G series modules is given in the Appendix.

a. Static-Test Instrumentation Improvements

An evaluation of beginning-of-life (BOL) data from the first four TEM-13G modules tested (SN-1 through -4), described in the most recent Quarterly Progress Report,⁽¹⁾ indicated that a substantial amount of module-to-module performance nonreproducibility was a result of inaccurate temperature determinations. A subsequent investigation led to the discovery of two important factors:

- 1) A systematic error in average hot-clad temperature determination had resulted from the placement of thermocouples used in the instrumentation of the first four TEM-13G series modules.
- 2) Nonuniformities in the thermal characteristics of the material (Thermon) used to bond the cooling fins to the module outer clads could result in azimuthal cold-side temperature variations.

The first factor is related to the relative positions of individual thermocouples and active segments of the electrical heater in the TEM-13G modules. The heaters used in these tests are fabricated with five active sections, each separated by a 0.2-in. gap as shown in Figure V-3. The figure shows the relationship between the individual heater sections and thermocouple (TC) locations used in the standard ten-thermocouple harnesses. This is the configuration used in the TEM-13G SN-1 through -4 initial test assemblies.

It is clear that, except for the third and eighth TC's all are positioned on or near the "dead" space between heater sections. This results in a systematic

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10 THERMOCOUPLE HARNESS

21 THERMOCOUPLE HARNESS

Figure V-3. Thermocouple Placements Relative to Heater Sections for TEM-13G Test Assemblies

low indicated average hot-clad temperature determination. To eliminate this condition a new 21-TC harness was designed, in which the individual TC's are evenly distributed along the active and dead spaces of the heater segments. The locations of the TC's relative to the heater sections in the new harness also are shown in Figure V-3. The new harness was used in all TEM-13G tests after SN-4.

In addition a new cooling-fin assembly was designed for use in future TEM-13G series tests. In previous test assemblies cold-side TC's were located along a single line on the outer clad of the module. The new fin allows for placement of TC's along four circumferential positions.

A test program was devised using TEM-13G SN-3. In this three-phase program the module was taken stepwise from its initial test configuration to a final one in which each of the changes could be evaluated separately. The results of this test program are discussed in detail below.

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TABLE V-1
RESULTS OF TEM-13G SN-3 INSTRUMENTATION MODIFICATIONS

		Phase I	Phase II	Phase III
	10TH/10TC Orig. Fin	10TH/21TC Orig. Fin	21TH/21TC Orig. Fin	21TH/21TC Slotted Fin
I. $Q = 5173$, $\bar{T}_{H10} = 1022^{\circ}\text{F}$				
A. Data Pt. No.	18	28	35	43
B. Q_{in} (watts)	5173	5173	5164	5166
C. $\bar{T}_{H10}$ ($^{\circ}\text{F}$)	1016	1021	1022	1020
D. $\bar{T}_{H21}$ ($^{\circ}\text{F}$)	-	-	1044	1047
E. $\bar{T}_{C10}$ ($^{\circ}\text{F}$)	521	521	529	541
F. $\bar{T}_{C21}$ ($^{\circ}\text{F}$)	-	518	527	531
G. V_{∞} (volts)	9.41	9.49	9.47	9.37
H. R_g ($m\Omega$)	95.5	98.2	96.7	96.5
I. P_E (watts)	231	229	232	227
II. $Q = 5940$, $\bar{T}_{H10} = 1129^{\circ}\text{F}$				
A. Data Pt. No.	20	30	38	45
B. Q_{in} (watts)	5940	5945	5955	5943
C. $\bar{T}_{H10}$ ($^{\circ}\text{F}$)	1122	1126	1129	1128
D. $\bar{T}_{H21}$ ($^{\circ}\text{F}$)	-	-	1153	1157
E. $\bar{T}_{C10}$ ($^{\circ}\text{F}$)	555	553	563	577
F. $\bar{T}_{C21}$ ($^{\circ}\text{F}$)	-	550	560	566
G. V_{∞} (volts)	11.08	11.08	11.11	10.93
H. R_g ($m\Omega$)	107.7	109.5	108.5	107.6
I. P_E (watts)	285	280	284	278

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b. TEM-13G SN-3 Instrumentation Modification Results

In the first phase of the TEM-13G SN-3 test program the module was removed from test; the fins, Thermon, and cold-side instrumentation were removed; and 21 equally spaced TC's were replaced along the outer clad. The fin was then replaced and the module installed in the static test stand.

The module was then restored to heat input, Q_{in} , and indicated average hot-clad temperature conditions, $\bar{T}_{H10}$ (hot-side temperature averaged based on ten TC's), duplicating as close as experimentally possible the conditions before the test change. Data points taken before and after this test change are given in the first two columns of Table V-1. The data show that the indicated module radial ΔT increased by 5 and 6°F at the $\bar{T}_H = 1025^\circ\text{F}$ (nominal) and $\bar{T}_H = 1125^\circ\text{F}$ (nominal) levels, an increase of approximately 1% in both cases. Since voltage is a function of the radial-temperature drop across the lead telluride only (provided, of course, that no intercouple shorting resulted from the thermal cycle necessary to make the instrumentation modification), the indications are that the radial-temperature drop across the lead telluride remained constant to within 1%.

The increase in internal resistance and subsequent decrease in power output measured after the instrumentation change is partially attributable to the increased average module temperature. During heatup of TEM-13G series modules an increase of 0.18 milliohm/°F in module temperature has been observed. This agrees very well with TEMØD calculations. The additional increase (approximately 2%) must either be associated with experimental uncertainty or a reduction in interface pressures as a result of thermal cycling.

Experimentally measured inner- and outer-clad temperatures during the two $\bar{T}_H = 1025^\circ\text{F}$ (nominal) data points before and after the instrumentation change (data points 18 and 28, respectively) are presented in Figure V-4. A rather marked difference between the two profiles can be observed at a position 6 in. from the negative end of the circuit. The hot- and cold-clad temperatures at this axial station rose approximately 100°F after the instrumentation modification. This phenomenon was most likely a result of the fact that the upper portion of the lower fin section was attached more tightly to the module during the initial testing. This indicates that the temperature drop across the Thermon

Figure V-4. TEM-13G SN-3 Axial-Temperature Profiles

can be as great as 100°F locally and can result in significantly different axial-temperature profiles from module to module, or from test to test on the same module.

Another important factor to be considered here is that if the temperature drop across the Thermon can be as high as 100°F, then certainly azimuthal cold-clad temperature variations will exist as the Thermon thickness varies. It is interesting to note, however, that the average cold-clad temperature calculated using all 21 TC's ($\bar{T}_{C21}$ in Table V-1) agreed quite closely with the average temperature ($\bar{T}_{C10}$ on the table) calculated using only the readings from the axial stations of the ten-TC harness. In both cases the 21-TC average was 3°F lower than the 10-TC average.

In the second phase of the TEM-13G SN-3 test program the module was again removed from test for re-instrumentation. For this experiment all instrumentation was removed from the module including the heater. A 21-hot-side-TC harness was installed, the outer clad was re-instrumented as in Phase I, the Thermon and fin replaced, and the module was returned to test conditions.

Once again the heat input and indicated average hot-side temperature, $\bar{T}_{H10}$ (using only the readings from the axial stations coinciding with positions in the ten-TC harness), were restored to their initial levels. Data sets were recorded at $\bar{T}_H = 1025^\circ\text{F}$ (nominal) and $\bar{T}_H = 1125^\circ\text{F}$ (nominal). These results are consistent with those obtained in previous modules instrumented with the 21-TC harnesses (TEM-13G SN-5 through -9). As in Phase I, $\bar{T}_{C21}$ was about 3°F lower than $\bar{T}_{C10}$.

Axial-clad-temperature profiles recorded for the $\bar{T}_H = 1025^\circ\text{F}$ (nominal) data point are given in Figure V-4 (data point 35). The locations of the five-heater segments are clearly defined by the hot-clad temperature profile. The systematic error resulting from the use of only those TC positions in the ten-TC harness is quite evident.

In the final phase (Phase III) of the TEM-13G instrumentation modification the module was again removed from test. The cold-side instrumentation and Thermon were replaced. A new fin, designed with axial slots for TC positioning, was used in this experiment. Thermocouples were positioned in this test

at the same axial locations as in the previous experiment. However, each couple was positioned at an azimuthal station 90° from each adjacent one. The purpose of this modification was to observe possible azimuthal temperature variations. Unlike all previous test assemblies, only the first and last TC's were exposed to the cooling airflow.

The module was then placed on test and restored to heat input and indicated hot-clad average temperature test conditions (using only the axial stations of the original ten-TC harness) of the previous tests. Results of these experiments are shown in the fourth column of Table V-1.

The indicated module radial-temperature drop using the ten-TC harness stations was about 2.8% lower than those recorded during Phase II. However, the indicated module temperature drop using all 21 TC locations were nearly identical in the final two phases. In Phase III a 10°F variation between $\bar{T}_{C21}$ and $\bar{T}_{C10}$ was observed. Figure V-4 gives the axial-temperature profiles recorded for the $\bar{T}_H = 1025^\circ\text{F}$ (nominal) data point of Phase III (data point No. 43 on the figure).

Although no gross azimuthal variations in cold-clad temperatures are apparent, two observations could be significant: (1) All of the TC's used in the calculation of $\bar{T}_{C10}$ with the exception of two were located in the same azimuthal position, i. e. in the same fin slot; (2) the temperature drop from the first and second and the last and next-to-the-last TC's in Phase III is considerably higher than in the previous phase. If subtle azimuthal temperature variations do exist in the Phase III test, then the calculation of $\bar{T}_{C10}$, which uses primarily TC's from only one azimuthal position, would be expected to deviate from $\bar{T}_{C21}$, calculated using TC's from all azimuthal positions. Observation 2 may be explained by the fact that only the first and last TC's of the Phase III test harness are exposed to cooling air. If a deterioration of Thermon is caused by a reaction with air, then these couples would give higher indications than those embedded in the Thermon and not exposed to air. If this is the case, then all TC's of the previous harnesses could read high, since all are positioned under Thermon which is exposed to airflow between the fin sections.

Voltages recorded for TEM-13G SN-3 during Phase III were 1.1 and 1.6% lower than those recorded at $\bar{T}_H = 1025^\circ\text{F}$ (nominal) and $\bar{T}_H = 1125^\circ\text{F}$ (nominal),

respectively, in the previous phase of the experiment. This indicates that the average radial-temperature drop across the lead telluride has reduced by the same percentage. Resistance data correlate well with that from previous phases, and power-output data are lower in direct proportion to the voltage reductions.

These tests have resulted in the following conclusions concerning module testing:

- 1) Temperature drops across the Thermon heat-transfer medium can be as great as 100°F.
- 2) Rather severe distortions in axial-temperature profiles result from nonuniformly installed fins.
- 3) Despite nonrepeatable axial-temperature profiles, electrical parameters can be closely reproduced by controlling heat-input rates and average clad temperatures.
- 4) The systematic error in the calculation of $\bar{T}_H$ using the initial ten TC's was greater than 20°F.

c. TEM-13G Arrhenius Analysis

Three of the TEM-13G series modules (SN-1, -4, and -5) are being tested at three different temperature levels to establish a temperature-dependence of the degradation rate associated with the TEM-13G-series modules. Figure V-5 gives electrical-power output-data from these three modules as well as a table indicating the nominal operating temperatures. In each case the data have been mathematically adjusted from the indicated design temperatures to the appropriate design levels.

Overall efficiency data from the same three modules are shown in Figure V-6. These data are presented as recorded; that is, no adjustments have been made to account for temperature fluctuations.

During the last quarter a preliminary degradation analysis was made using experimental data from these three modules. A least-mean-squares fit was applied to the overall efficiency data. Figure V-6 shows that the experimental data from all three modules can be well represented by a straight line. Table V-2 compiles the results of the preliminary degradation analysis.

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Figure V-5. Comparison of Power-Output Data from Three TEM-13G Series Modules Tested at Separate Temperature Levels

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Figure V-6. Comparison of Overall Efficiency Data from Three TEM-13G Series Modules Tested at Separate Temperature Levels

TABLE V-2
INDICATED POWER OUTPUT AND OVERALL EFFICIENCY
DEGRADATION RATES OF TEM-13G MODULE SERIES

Module TEM-13G	$\bar{T}_H / \bar{T}_C$ (°F)	Overall Efficiency Degradation Rate (%/10,000 hr)
SN-4	1048/538	4.1
SN-1	1150/570	34.4
SN-5	1225/601	66.2

An Arrhenius plot of efficiency degradation rates is shown in Figure V-7.

d. Static Testing of Converter Module SN-1 Modules

Static testing of the four TEM-13G series reactor modules to be incorporated into Converter Module SN-1 was completed during the past quarter. These four modules, SN-6 through -9, were tested in electrically heated static test stands at WANL. Each was tested at $\bar{T}_H = 1025^\circ\text{F}$ and $\bar{T}_C = 538^\circ\text{F}$ operating conditions and instrumented with the 21-TC scheme shown in Figure V-3. Although the modules were placed on test during the previous quarter, heatup data were not completely evaluated until after the completion of that report.

Figures V-8 through -10 present comparisons of effective Seebeck Coefficient, internal resistance, and thermal-impedance data recorded during heatup of these four modules with the corresponding calculated parameters. In each case excellent module-to-module reproducibility was observed and excellent correlation was achieved between the calculated results and experimental data.

Power-output data from TEM-13G SN-6 through -9 recorded during static testing are given in Figure V-11. As above, adjustments to the data have been made to account for minor deviations from the design operating temperatures, $\bar{T}_H = 1025^\circ\text{F}$ and $\bar{T}_C = 538^\circ\text{F}$. Since being placed on test, adjusted power output data from all of these modules have fallen within the band 214 ± 10 watts ($\pm 5\%$ deviation from the norm). In no case has there been a measurable degradation rate. All have been removed from static test and are now being installed into Converter Module SN-1.

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Figure V-7. Arrhenius Plot of TEM-13G Efficiency-Degradation Rates

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Figure V-8. Comparison of Calculated and Experimental Effective Seebeck Coefficient Data Measured During Heatup of TEM-13G SN-6 Through -9

Figure V-9. Comparison of Calculated and Experimental Internal-Resistance Data Measured During Heatup of TEM-13G SN-6 Through -9

Figure V-10. Comparison of Calculated and Experimental Thermal-Impedance Data Measured During Heatup of TEM-13G SN-6 Through -9

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Figure V-11. Comparison of Adjusted Power-Output Data from Reactor Modules Tested at $\bar{T}_H = 1025^\circ\text{F}$ and $\bar{T}_C = 538^\circ\text{F}$

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Figure V-12. Electrical-Current Data from Three Prototype Pump Power Modules

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e. TEM-13G SN-2 Loop Test Data

The final TEM-13G series reactor module, SN-2, was placed on test in the liquid-metal loop system at AI. This module is also being tested at $\bar{T}_H = 1025^\circ\text{F}$ and $\bar{T}_C = 538^\circ\text{F}$ temperature conditions. Power-output data from this module are shown in Figure V-11 along with similar data from reactor modules being tested in static test stands. The 222-watt level of TEM-13G SN-2 falls within the band formed by data from the modules on static test and agrees precisely with the power output level calculated by TEMØD, the mathematical performance model. No measurable degradation has been observed in this module.

3. Second-Generation 5.25-volt Prototype Modules

The design of second-generation prototype modules, designated as the TEM-13H series, has been started. The TEM-13H design will be very similar to that of the 13G modules. Basically the design improvements will be based on the results of the TEM-13G and TEM-15 sublength module tests, discussed in a separate section of this report.

B. PUMP TUBULAR POWER MODULES, PROTOTYPE PERFORMANCE SUMMARY

Three tubular modules designed to deliver power to an electromagnetic (EM) pump are on test. These prototype pump modules, designated TEM-14A SN-1, -2, and -3, are being tested at average clad temperatures simulating those of the zirconium-hydride two-loop reactor system, $\bar{T}_H = 1125^\circ\text{F}$ and $\bar{T}_C = 570^\circ\text{F}$.

Electrical current and electrical-power-output data recorded from the TEM-14A series modules as functions of time are shown in Figures V-12 and -13, respectively. The electrical-current data from SN-2 and -3 have been adjusted to account for minor deviations in the load resistor from the design load of 0.44 milliohm. The BOL data from TEM-14A SN-2 and -3, mathematically adjusted to the design load resistance, agree to within 1% of the design levels. SN-1 performance parameters are somewhat lower than the design levels because the electrical resistance of the nickel power pins is larger than calculated in the original design study, which assumed molybdenum pins. The power-pin electrical resistance of TEM-14A SN-2 and -3, however, was reduced by incorporating 12 rather than six power pins at each end.

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Figure V-13. Power-Output Data from Three Prototype Pump Power Modules

Excellent performance stability has been realized in the TEM-14 series pump modules. No measurable degradation of performance has been observed for TEM-14A SN-1 during the first 7340 hr of operation at design operating conditions. Similarly, no measurable performance degradation has been observed on either TEM-14A SN-2 or -3 after 3648 and 2455 hr of testing, respectively.

During the past quarter a series of thermal cycles was conducted on TEM-14A SN-3. Ramp rates of 30°F/min were achieved for both the cooldown and heatup portions of each thermal cycle. As can be seen in Figures V-12 and -13, the thermal cycles had no apparent effect on module performance. Two thermal cycles on TEM-14A SN-1 caused by laboratory power failures also produced no effects on module performance stability.

The long-term stable-performance capabilities of the tubular module incorporating lead telluride TE materials are clearly demonstrated by the performance data from the TEM-14 series pump modules.

C. CONVERTER MODULE

1. Hydraulic Test Program

The results of waterflow tests of tubular module primary and secondary flow passages have been examined to determine the relationship between pressure drop and swirl generator pitch. The curve in Figure V-14 shows the test results scaled to apply to NaK flow at the operating temperature of the module. The water tests were made with full-scale models of converter-module flow channels and flowrates consistent with Reynolds numbers that are developed with NaK flow.

The swirl generator pitch was selected based on these data. A pitch of 1.335 in. was selected for the primary swirl generator and 1.25 for the secondary. The primary is machined from a solid bar of stainless steel in a form similar to a coarse screw thread, and the secondary is formed by winding a 0.125-in.-diam wire on the outside surface of the TE module.

If changes in the pressure drop across the module are necessary these curves may be used to determine what will result from changes in the swirl generators. If any change decreases pressure drop across the swirl generator, the distribution of flow between the four flow passages of a converter module

	<u>Primary</u>	<u>Secondary</u>
NaK Flow Rate - lb/sec	0.13	0.13
NaK Avg. Temp. - °F	1125	570
Annulus Height - In.	0.125	0.125
Passage Length - In.	15.1	10.3
Annulus Outer Diameter - In.	0.75	1.90

Figure V-14. Swirl Generator Pressure Drop for Tubular Module Primary- and Secondary-Flow Passages

will not be as nearly equal. Conversely if the pressure drop is increased, the distribution will be more nearly equal. The flow-distribution characteristics of the converter module were investigated and the results presented in the previous Quarterly Progress Report.⁽¹⁾

2. Performance Maps of the TEM-13G Tubular Module Series

A set of multivariable performance maps has been prepared to indicate how a converter system utilizing the TEM-13G tubular module design will respond in a variety of situations. A coupled hydraulic, thermal, and TE model of the tubular module has been developed and cast in the form of a digital computer program called TEFLØW. The program provides systems-oriented performance predictions of tubular modules operating as shell-and-tube heat exchangers. Counterflow or parallel-flow operation may be examined and a variety of working fluids used.

a. Model Description

The model is so organized that the independent variables for any calculation consist of the following:

- 1) Module dimensions
- 2) Component material specifications
- 3) Bulk inlet primary- and secondary-loop NaK temperatures
- 4) Primary- and secondary-loop NaK flowrates
- 5) External circuit resistive load

Based on the overall module dimensions which are input the program automatically constructs a system of spatial nodes. Cylindrical symmetry is assumed so that an individual cell has the shape of a washer. An energy balance at each node is used to construct a thermal-energy matrix. The off-diagonal elements of the matrix represent net internodal conductances while the main diagonal elements are the self-conductances. The inhomogeneous portion of these simultaneous equations contains all internal sources of sinks of energy and certain fixed boundary conditions. Thus Joule heating, Peltier heating, or cooling and Thomson heating or cooling are accounted for explicitly in the matrix system. The NaK-flow channels are included in the nodal system

so that energy transfer within the moving medium and between the medium and the walls is accounted for by including transport terms in the energy conservation equation for the appropriate nodes.

The system of equations is first solved under zero-power or open-circuit conditions. The initial temperature distribution is then fed to a separate TE model which evaluates TE effects under loaded conditions. (Matched-load conditions are generally assumed; however, any given input load may be applied.) The TE heating and cooling effects are fed back to the thermal model for another pass. Three iterations of this sort are generally sufficient. The TE model evaluates performance on a couple-by-couple basis. The local TE material temperatures are obtained by two-dimensional interpolations within the temperature distribution obtained after the previous thermal pass.

At present the program evaluates axial and radial heat-transfer within the active length of the tubular module. For the purposes of constructing the performance maps to be presented here it was assumed that the primary- and secondary-NaK flowrates and temperatures can be independently varied. Actually, the primary and secondary variables are coupled by the radiator and pump characteristics and are not entirely independent. Thus the data to be presented predict the performance of the tubular module as a separate entity, without regard to system constraints.

b. TEMØD-TEFLOW Comparison

The first step in this analysis was to compare the TEFLOW results for the performance of a TEM-13G module with those obtained by TEMØD, the basic tubular module design code. The TEMØD calculations assumed no axial variation in clad temperature. To duplicate this condition the analogous TEFLOW cases were run with very large NaK flowrates which produced axial-temperature changes of fractions of a °F in the NaK. Table V-3 is a summary of the results of this comparison. Two sets of operating points, defined by clad-temperature combinations of 1025/538°F and 1125/570°F, were checked.

In terms of power output the discrepancy between the codes amounts to 1.5 and 2.5% for the two temperature conditions, respectively. This is excellent agreement for numerical work of this kind. The same tubular-module circuit dimensions were input to the codes which use basically the same property data.

TABLE V-3
TEMØD-TEFLØW COMPARISON

Case	Mean Clad Temperatures (°F)	Load Voltage (volts)	Load Current (amp)	Internal Resistance (milliohms)	Electrical Power Output (watts)	Heat Input (watts)	Circuit Efficiency (%)
TEMØD	1025/538	4.57	48.7	93.8	222.7	4602	4.84
TEFLØW (Fixed Clad Temperatures)	1025.3/537.7	4.55	48.1	94.6	218.9	4623	4.73
TEMØD	1125/570	5.24	49.4	106.0	259.0	5326	4.86
TEFLØW (Fixed Clad Temperatures)	1125.2/569.8	5.21	48.7	107.1	253.5	5408	4.69
TEFLØW (Nominal Design Point)	1124.5/570	5.18	48.7	106.4	252.1	5434	4.64
BOL Measured Performance	1025/538	4.65	47.9	97.2	223.1	4900	4.55 [†]

These data are for the TEM-13G SN-2 tubular module averaged over the first 100 hr of loop testing.

[†]Overall efficiency.

The primary difference in the two codes is the heat-transfer model used. TEMØD uses approximate analytical expressions and considers radial-heat transfer only.

An additional TEFLØW result is included in Table V-3, representing nominal design point operation. The inlet-NaK conditions were varied to yield mean clad temperatures of 1125 and 570°F on the primary and secondary sides, respectively. The difference in results between the fixed clad temperature case and the case with a realistic axial-temperature profile is essentially negligible. Table V-4 lists the conditions required to produce the reference clad temperatures.

Experimental BOL performance data for the TEM-13G SN-2 tubular module are also listed in Table V-3. This module was tested at the nominal mean clad temperatures of 1025 and 538°F on the hot and cold sides, respectively, in a two-loop liquid-metal system at AI. The numbers represent mean performance of the module over the first 100 hr of loop testing. Corrections were made to the raw measurements to account for deviations from matched load and the target clad temperatures.

TABLE V-4
TEM-13G REFERENCE MATCHED-LOAD
OPERATING POINT

Location or Material	Temperature (°F)	Flowrate (pps)
Inlet primary-NaK	1241	
Inlet secondary-NaK	463	
Primary-NaK		0.129
Secondary-NaK		0.116
Primary-NaK axial ΔT	198	
Secondary-NaK Axial ΔT	208	
Mean hot-clad	1124.5	
Mean cold-clad	570.0	

c. Loop-Test Data Comparison

TEFLOW results were also compared against raw TEM-13G SN-2 data obtained during liquid-metal loop tests at Atomics International. Measured NaK-inlet temperatures and flowrates and the reported load resistances were input to TEFLOW. A comparison of test and TEFLOW results is given in Table V-5 for data points 11 and 13. There is considerable uncertainty in the test data. If the reported flowrates and NaK-temperature changes across the test section are used to compute heat throughput, the heat-rejection rate is found to exceed the heat-input rate.

The actual difference between computed and measured parameters is quite small, however. TEFLOW does not completely account for end heat losses, so that it tends to predict higher electrical power outputs and efficiencies than are measured. The discrepancies are in the right direction and of the right order of magnitude.

d. Load-Curve Analysis

The aspect of performance considered here illustrates the response of a tubular module to external load settings. Two sets of boundary conditions were compared. The first approach involved fixing the inner- and outer-clad temperatures at 1125 and 570°F regardless of the applied load; the other fixed the

TABLE V-5
COMPARISON OF TEFLØW RESULTS WITH TEM-13G SN-2
MEASUREMENTS

	Primary NaK Axial ΔT (°F)	Secondary NaK Axial ΔT (°F)	Load Voltage (volts)	Internal Resistance (milliohms)	Electrical Power Output (watts)
Data Point 11					
Measured	204.8	202.9	4.59	97.33	222.5
Calculated	206.5	176.6	4.70	92.35	233.1
Data Point 13					
Measured	198.0	199.0	4.581	97.60	222.6
Calculated	197.3	185.9	4.610	93.22	225.3

NaK-inlet temperature and flowrates. The latter is more representative of the operation of the real system.

The conditions defined by Table V-4 yield the reference clad temperatures at matched load. Thus, matched-load operation of the tubular module using either set of boundary conditions produces the same performance results. The fixed clad condition was again imposed by specifying very high NaK flowrates.

The load curves were generated, therefore, by fixing the thermal-boundary conditions and by varying the load resistance from 10 to 1000 m Ω , which brackets most feasible operating conditions. Figure V-15 contains the fixed clad-temperature case load curve set, showing the variation of electrical power output, circuit efficiency, load current, and load voltage with external load resistance. Figure V-16 contains the load curve set for the fixed NaK-conditions case.

Classical TE analysis predicts that maximum power is obtained at the matched-load point; however, the assumption of fixed junction temperatures is implicit in this result. Figure V-15 shows that maximum power is obtained at the matched-load point. Figure V-16, however, shows the power peaking at 130 m Ω rather than at the matched-load point (107 m Ω). Performance (in terms of power output and efficiency) varies quite slowly with load resistance in

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Figure V-15. TEM-13G Load Curves with Clad Temperatures Fixed at $\bar{T}_H = 1125^\circ\text{F}$ and $\bar{T}_C = 570^\circ\text{F}$

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Figure V-16. TEM-13G Load Curves with Fixed NaK-Inlet Conditions

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the vicinity of the matched-load point. On a linear scale it is particularly apparent that the choice of load resistance is not critical near the peak of the power and efficiency curves. The load-voltage variation is considerably more pronounced. From Figure V-16 it can be seen that going from the matched-load point of $107\text{ m}\Omega$ to the peak-efficiency point, $145\text{ m}\Omega$, yields essentially no power change, a small efficiency change, but a 20% change in voltage (from 5.2 to 6.0 volts). Table V-6 lists the performance results obtained for the three significant operating points: matched load, maximum power, and maximum efficiency.

TABLE V-6
SUMMARY OF LOAD CURVE RESULTS

	Fixed Clad Temperatures	Fixed NaK-Inlet Conditions
Matched Load:		
Power output, watts	253	252
Resistance, $\text{m}\Omega$	107	106
Voltage, volts	5.21	5.18
Efficiency, %	4.69	4.64
Peak Power:		
Output, watts	254	254
Load, $\text{m}\Omega$	107	130
Voltage, volts	5.21	5.75
Efficiency, %	4.69	4.75
Peak Eta:		
Power output, watts	249	253
Load, $\text{m}\Omega$	145	145
Voltage, volts	6.00	6.00
Percent	4.78	4.78

Figure V-17 is the I-V characteristic curve for the tubular module. Results are shown for both modes of operation. To complete this aspect of performance analysis Figure V-18 was prepared to show how the clad temperatures and the NaK axial temperatures vary with load voltage, assuming the fixed NaK-inlet conditions mode of operation. As the load voltage changes in this case the

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Figure V-17. TEM-13G I-V Characteristic Curves

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Figure V-18. TEM-13G Temperature Response During Load Resistance Variations with Fixed NaK-Inlet Conditions

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circuit current changes, thus modifying Peltier-pumping at the junctions and the total heat throughput. This mode of operation, while it is more realistic than assuming fixed clad temperatures, still does not model the operation of the actual system properly. In the real system the NaK flowrates and temperatures cannot be independently controlled; these quantities depend on the pump and radiator characteristics and the reactor power level. Thus if the system is operating at some steady-state point and the load resistance is changed, the system as a whole will seek a new operating point. NaK flowrates and temperatures associated with the new operating point can only be determined with a more comprehensive system model.

e. Temperature/Flowrate Performance Maps

The following study maps the effects of varying the four working-fluid parameters. Five pairs of inlet-NaK temperatures represented different modes of system operation. Primary- and secondary-NaK flowrates were varied from 0.10 to 0.14 pps. Each point on each map represents matched-load operation; this is useful at this stage since the study is of a parametric nature. In actual operation the power-converter system would probably be isolated from the using system and thus would see a "fixed" load, which would be adjusted initially to yield the proper electrical power output and load voltage at a set of reference NaK-inlet conditions. Off-design performance maps of the system operating into a fixed load will be examined in a future study.

The alternate inlet-temperature conditions in °F were:

<u>Case</u>	<u>Primary</u>	<u>Secondary</u>
Reference	1245	463
Derated 100°F	1145	430
Derated 100°F, reduced ΔT	1120	455
Derated 150°F	1095	410
Up-rated	1345	485

The first five performance maps cover the reference inlet condition case. Figure V-19 is the predicted power/load-voltage/flowrate performance map for the TEM-13G tubular module for the stated inlet conditions. For each

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Figure V-19. TEM-13G Power/Load-Voltage/Flowrate Performance Map (Reference Case)

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primary/secondary flowrate coordinate there is a unique power/load voltage coordinate. Figure V-20 shows the power/flowrate efficiency performance map. Figure V-21 gives the mean clad surface temperatures on the primary and secondary sides as dependent variables, and Figure V-22 shows the axial-temperature drops in the NaK loops as a function of flowrate. Figure V-23 shows the estimated difference between the NaK bulk-temperature and the tubular module clad-surface temperatures as a function of flowrate. The film-drop curve is included for the reference case only to illustrate its behavior. Performance maps covering the other four inlet-temperature cases have also been prepared.

3. Converter Module SN-1

A mechanical and stress analysis of converter module SN-1, based on the use of conventional structural design codes and procedures, continued during the reporting period. The following summarizes the general approach used:

- 1) The STASYS three-dimensional frame computer program was used to make a flexibility analysis of the complete unit.
- 2) Stress intensities were determined using three formulae (9, 12, and 13) from the Nuclear Power Piping Code.*
- 3) The stress indices for branch connections and straight pipes were obtained from Appendix D of the same design code.
- 4) The allowable stresses (S_m values) for Formulas 9 and 12 were obtained, with one exception, directly from Section VIII, Division 1 of the ASME Boiler and Pressure Vessel Code. The 1270°F allowable-temperature limit obtained from the code was so restrictive that an investigation was made to determine its basis. This limit was found to be based on 1% creep in 100,000 hr. Since the system has only a 20,000-hr design life this was adjusted to 1% creep in 20,000 hr. The S_m values obtained are 16,300 psi at 410°F, 16,000 psi at 660°F, 13,300 psi at 1045°F, and 7,970 psi at 1270°F.
- 5) The allowable stresses for Formula 13 (alternating stress) were obtained from Code Case 1331-4 for 50 cycles of operation. It was

*Draft USA Standard Nuclear Power Piping Code, USAS B31.7, February 1968

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Figure V-20. TEM-13G Power/Efficiency/Flowrate Performance Map (Reference Case)

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Figure V-21. TEM-13G Clad-Temperature/Flowrate Performance Map (Reference Case)

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Figure V-22. TEM-13G NaK Axial-Temperature-Drop Performance Map (Reference Case)

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Figure V-23. TEM-13G Film-Drop Performance Map (Reference Case)

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Figure V-24. STASYS Nodal Model of Converter Module SN-1

found that this category of stress is never critical for this design. Even when the other stresses (primary or primary + secondary) were well over the limits, the alternating stress was less than 50% of the allowable.

The stress-analysis of the original converter module design indicated that certain local stress intensities exceeded code allowables. The worst violations of the code requirements occurred: (1) at the shroud to secondary-header joint where the primary-plus-secondary stress intensity is 70,000 psi and the allowable value is 48,000 psi; and (2) at the primary-inlet header joint where the primary-plus-secondary stress intensity is 67,400 psi and the limit is 23,900.

Certain design changes were then identified which would reduce the critical stress intensities. These design changes may be summarized as follows:

- 1) Extend the curved primary-header tubes so that the centerline spacing between a tubular module and the primary manifold is 6 in. (formerly was 2).
- 2) Increase the reinforcement section at the primary-header/primary-manifold joint to 0.10 in. (formerly was 0.065).
- 3) Increase the primary-manifold wall thickness to 0.075 in. (formerly was 0.065).
- 4) Taper the transition piece so that the thickness runs from 0.05 in. at the tubular-module end to 0.041 in. at the other end (formerly was 0.035 in. constant-section thickness).
- 5) Increase secondary-manifold header stub thicknesses to 0.084 in. (formerly was 0.055 in.).

Figure V-24 is a cathode-ray-tube plot of the STASYS digital computer nodal model of the converter module which illustrates the nomenclature used to identify critical sections. The assumed internal pressure of the system is 1.5 times the design value of 20 psi. In addition a temperature profile base on a 1270°F metal temperature at the primary inlet and 410°F at the secondary inlet was assumed.

TABLE V-7
CONVERTER MODULE SN-1 CRITICAL-STRESS SUMMARY*
(Stress Intensity in psi)

		Location								
		21 end 2	1 end 1	14 end 1	30 end 1	137 end 1	13 end 1	13 end 2	129 end 1	20 end 2
Primary	Pressure	143	143	250	548	648	139	152	226	176
	Pressure and mechanical†	5,697	6,386	10,815	19,008	12,463	11,221	11,592	13,185	9,769
	1.5 S _m	11,950	19,950	11,950	24,000	24,000	11,950	11,950	24,000	11,950
Secondary	Thermal	10,670	15,034	11,961	21,290	24,992	7,268	9,942	0	13,564
	Thermal and mechanical†	15,965	21,208	23,387	39,241	36,177	15,513	18,980	17,314	20,491
	3.0 S _m	23,900	39,900	23,900	48,000	48,000	23,900	23,900	48,000	23,900

*Revised design with elbow extensions and local reinforcement.

†Assumed loading pattern consists of 50-lb forces and 100 in.-lb moments applied to the piping-connection points.

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Two sets of external loading conditions were used. First the system was assumed to be a free body so that the effects of thermal stress and the mechanical stress could be isolated. The results are listed in Table V-7 for what have been shown to be the most critical sections. The free-body analysis of the converter module with the recommended design changes shows a positive design margin for all sections.

The next step in the analysis involved postulating a feasible external-loading pattern. Since the power-converter assembly design has not progressed sufficiently to identify the individual converter-module piping interactions there are no clear criteria to follow. Our approach was to consider a loading pattern which consisted of all combinations of forces, bending, and torque at each piping-connection point. The central support member was assumed to be fixed at one end and constrained at the other so as to permit motion only perpendicular to the tubular module and along the axis of the plate.

With loads applied according to the specified pattern it was found that acceptable magnitudes for the piping reactions (at the four connection points) are 50 lb of force (applied simultaneously along three mutually perpendicular axes) and 100 in.-lb torsion and bending moments. All forces and moments are assumed to occur simultaneously in the worst direction. Note that these reactions define an acceptable loading pattern, and are not necessarily optimum. Obviously if the degrees of freedom in the loading were restricted, higher acceptable magnitudes could be determined. A summary of the stress-analysis based on this loading pattern is summarized in Table V-7.

The recommended design changes associated with the primary manifold assembly will be implemented. However, because the heat-rejection manifold assembly has been completed we do not recommend removing the two existing header stubs (which have an 0.055-in. wall thickness). Further analysis has shown that reducing the stub length by 0.7 in. will accomplish a reduction to an acceptable stress-intensity level.

a. Allowable Stress at Maximum Design Temperature

The uprated or maximum temperature operating condition for the power-converter assembly is defined by a reactor-outlet temperature of 1350°F and a radiator-outlet temperature of 495°F. In order to extend the stress-analyses

to cover the uprated operating mode the allowable stress for SS-316 was evaluated at 1350°F.

The ASME Boiler and Pressure Vessel Code allowables are based on the lowest value of one of the following:

- 1) 0.25 of ultimate stress.
- 2) 0.625 of yield stress.
- 3) Stress to produce 1% creep in 100,000 hr.
- 4) 0.6 of stress to produce rupture in 100,000 hr.

The Alloy Digest lists the various properties required to determine the above allowable values in SS-316. It was found that the stress to produce 1% creep in 100,000 hr is the basis for the published code allowables.

The digest lists the stresses to produce 1% creep in both 10,000 and 100,000 hr for various temperatures. It was found that these data could be correlated on a time-temperature basis using the Larson-Miller equations with a parameter of 9.46. Using this parameter it is possible to find a temperature where the 100,000-hr allowable stress is the same as the 20,000-hr 1350°F allowable. This temperature was found to be 1261°F. Interpolating the data given in Section VIII, Division 1 of the ASME Boiler and Pressure Vessel Code yields an allowable S_m of 5015 psi for SS-316 at 1350°F.

b. Converter-Module Stress-Analysis at Maximum Temperature

The structural adequacy of the present converter module SN-1 configuration, and a design incorporating 180° bends in the primary headers was examined with a maximum instead of nominal temperature difference between the primary and secondary inlets. The worst case temperatures used were 1350 and 400°F at the primary and secondary inlets, a conservative estimate of temperatures after a fast startup with a thermal lag in the heat-rejection loop. Thermal stresses only were considered in this study to provide background information for future design efforts.

The U-tube configuration is a logical evolution of the existing design with 4-in. elbow extensions which can be used to construct an array of six converter modules which will fit within the PCA envelope. A stress summary of both

configurations subjected to the maximum temperature difference is given in Table V-8. It is evident that all stresses are within the allowables, with the U-tube design exhibiting considerably more design margin at the primary-inlet-manifold/primary-bend saddle joint where the improvement is important.

TABLE V-8
CONVERTER MODULE STRESS SUMMARY FOR MAXIMUM-
TEMPERATURE CONDITIONS (in psi)

Case	Primary-Header/ Tubular-Module Joint	Shroud-Exit Saddle Joint	Primary-Header/ Primary-Manifold Saddle Joint
SN-1 Configuration:			
Primary-stress intensity	200	548	143
Allowable ($1.5 S_m$)	7,523	24,000	7,523
Secondary-stress intensity	7,975	21,650	14,970
Allowable ($3.0 S_m$)	15,045	48,000	15,045
180° Bend Configuration:			
Primary-stress intensity	200	548	143
Allowable ($1.5 S_m$)	7,523	24,000	7,523
Secondary-stress intensity	11,324	33,487	11,705
Allowable ($3.0 S_m$)	15,045	48,000	15,045

Assumptions: Primary-inlet NaK temperature = 1350°F.
Secondary-inlet NaK temperature = 400°F.
No externally applied mechanical loads.

4. Converter Module SN-2

The preliminary design of Converter Module SN-2 was begun during the reporting period. It is felt that the heat-rejection manifold assembly of CM SN-2 will be virtually identical to the cold side of SN-1. This implies that the unit will be welded and local reinforcement of the joints provided, based on stress considerations. The design of the primary-manifold system depends to a great degree on the results of the power-converter assembly conceptual design effort, which is proceeding concurrently.

Figure V-25. Dummy Tubular Module Gas-Filled Gap Requirements

D. POWER-CONVERTER ASSEMBLY

1. Metal Mockup (PCA SN-1)

The power-converter assembly (PCA) metal mockup will be designed to provide hydraulic and thermal simulation of real PCA performance characteristics. It is planned to achieve this simulation through the use of dummy tubular modules. The flow channels can easily be sized to provide good hydraulic simulation. During the past month the problem of providing thermal simulation, without resorting to the use of real tubular modules, has been studied.

The conceptual design of a dummy tubular module has been established based on this analysis. Thermal simulation will be achieved through the use of gas-filled gaps between inner and outer clads which can be sized to provide an overall thermal-impedance equivalent to that of a power-producing tubular module at design operation conditions. The dummy module basically consists of a solid stainless-steel jacket surrounding an Inconel inner clad. This clad is a duplicate of that used on real modules. An annular gap of 0.011 in. exists between the inner clad and the jacket.

Heat-transfer calculations show that this arrangement will provide the required thermal-impedance characteristics. Figure V-25 shows the design parameters of interest. The radial thickness of the annular gas-filled gap is plotted vs the thermal conductivity of the fill medium. Air, helium, and hydrogen are shown on the curve. Helium gas is the best selection, as the gap thickness required is reasonable from the fabrication point of view. A three-dimensional analysis will be made to establish the importance of end losses.

2. Heat-Transfer Analysis

An analysis of the heat loss from a PCA has been completed during the reporting period. A TØSS (Transient or Steady-State digital computer heat-transfer program) model was made of a corner section of a representative converter-assembly design. This model simulates the heat transfer by conduction and radiation of the internal components of a converter assembly to the outside walls. It was assumed that all of the heat from the walls radiated to surrounding structural surfaces which had a constant temperature of 200°F. The low environment temperature was chosen to provide a conservative basis for these calculations.

Operating Temperatures:

Primary Inlet - 1685°R (1225°F)

Secondary Inlet - 930°R (470°F)

Reference Drawing 939J774

Figure V-26. Power-Converter Assembly Casing Isotherm Map

The results of the analysis show that a heat loss of 7800 Btu/hr (2.3 kw) will result at design temperatures. This represents approximately 2% of the heat transferred through the TE modules and is about the same as 0.1% loss in circuit efficiency. The calculated temperature levels on the outside surfaces of the converter assembly are as high as 994°F near the 1225°F inlet headers and as low as 300°F near the heat-rejection manifolds.

Figure V-26 shows isotherms for the outside surface of a quarter segment of the converter assembly container. The areas where high temperatures are shown are near the primary inlet and exit headers. This calculation was done for stainless-steel walls which average about 650°F at the design operating conditions.

E. SUPPORTING TECHNOLOGY DEVELOPMENT

1. Sublength Modules

The TEM-15 series modules were designed as a part of the supporting technology development phase of the Compact Thermoelectric Converter System Technology Program. These modules were designed with radial dimensions similar to those of the TEM-13G reactor power modules, but with circuit lengths reduced from 15.0 to 4.0 in. The purpose of fabricating and testing these modules is to evaluate the effects of varying processing techniques, assembly clearances, and intercouple insulating methods on module performance (see Figure 5-2, Reference 1).

All TEM-15A series modules use the conventional single-piece 0.005-in.-thick mica insulator between each adjacent set of TE washers. These modules have been designed to investigate the influence of (1) mica radial clearance on mica deformation during processing, (2) weld shrinkage on circuit alignment and mica deformation, and (3) axial preload on mica deformation and circuit alignment in a module incorporating standard 0.005-in.-thick mica insulators.

All TEM-15B series modules are designed with a 0.0005-in.-thick layer of tungsten sandwiched between two 0.00225-in.-thick mica washers. The purpose of these modules is to ensure an absolute diffusion barrier between adjacent lead telluride washers in order to completely eliminate intercouple diffusion, hence module degradation. Incorporation of this design scheme into the reactor

power module would not be a suitable solution since a high shunt-heat loss through the tungsten barriers reduces the module efficiency by nearly 40%.

However, success of this experiment will validate the theory that module degradation is caused by an interdiffusion of lead telluride through the mica washers.

TEM-15C series modules are designed with two 0.0025-in.-thick mica insulating washers, each with either a molybdenum or tungsten coating applied on one side. Shunt-heat losses through the coating will be minimized with this technique.

Table V-9 gives a comparison of calculated performance parameters for the TEM-15A and TEM-15B designs. The reduction in power output and subsequent increase in required power input for the TEM-15B design as a result of the incorporation of tungsten are shown in the table.

TABLE V-9
COMPARISON OF CALCULATED PERFORMANCE PARAMETERS
FOR THE TEM-15 SUBLLENGTH MODULE DESIGN

Parameter	TEM-15A	TEM-15B
1. $\bar{T}_H$, °F	1125*	1125*
2. $\bar{T}_C$, °F	570*	570*
3. Open-circuit voltage, volts	4.77	4.41
4. Internal resistance, milliohms	84.7	84.3
5. Circuit heat input, watts	1446	2078
6. Total heat input, watts	1748	2380
7. Power output, watts	67.0	57.6
8. Circuit Efficiency, %	4.64	2.77
9. Overall efficiency, %	3.83	2.42

*Temperature conditions used as input to the performance code.

During this reporting period eight modules were assembled and processed. Five TEM-15A SN-4 type modules were included in this group to establish a baseline design and provide information necessary for evaluation of TEM-15 degradation. The TEM-15A SN-4A series modules are identical to the TEM-13G design except for circuit length, and were fabricated for the following studies:

- SN-4: Long-term testing to establish performance degradation.
- SN-4A: Destructive examination following hot autoclaving to evaluate pretest circuit alignment.
- SN-4B: Destructive examination following a 1000-hr test at $\bar{T}_H = 1125^\circ\text{F}$, including 50 cycles at $2.5^\circ\text{F}/\text{min}$ ramp rate, to evaluate the combined effects of low-temperature ramp cycles and creep on circuit alignment.
- SN-4C: Destructive examination following a 1000-hr test at $\bar{T}_H = 1125^\circ\text{F}$, including 50 cycles at $30^\circ\text{F}/\text{min}$ ramp rate, to evaluate the combined effects of reactor-type-temperature ramp cycles and creep on circuit alignment.
- SN-4D: Destructive examination following a 1000-hr steady-state test at $\bar{T}_H = 1125^\circ\text{F}$, with a low ramp rate startup to evaluate the effect of creep on circuit alignment.

During fabrication of these modules, axial-compaction studies were made using SN-4A to determine the expected compacted length at maximum axial-compressive load during assembly.

Three tests were run on this module with measurements made of the axial distance between retainer ring faces for each incremental load of 50 psi. Figure V-27 gives the data recorded during axial loading of TEM-15 SN-4A. A modified Alsimag spacer with no step on its OD was provided on the negative end of the module (see schematic on Figure V-27). This modified component permitted uninterrupted relative movement between circuit and boron nitride sleeve.

The first test consisted of loading the module to that pressure at which no discernible change in length occurred with increased loading. For this phase of the test, loading was stopped at 450 psi as shown in Figure V-27.

Figure V-27. Results of the TEM-15A SN-4A Module Compaction Experiment

For the second test the outer clad was removed and the loading sequence repeated. Although the preloading dimensions differed slightly, continued loading yielded the same minimum measured length. Measurements of the OD showed no buckling or radial shift of the conductor rings, nor was any damage to component parts visible.

A third test was completed with the clad reassembled and measurements taken every 50 psi up to a maximum load of 1100 psi. Again the same minimum dimension of 4.920 in. was achieved, as is shown in Figure V-27. Since actual measurements of individual components were not recorded and the extent of mica compaction could not be established, the axial clearance between the BN sleeve and the original Alsimag spacer could not be determined with the compressed length of 4.920 between retaining rings.

After completion of these tests it was decided to machine 0.010 in. from the step on the original Alsimag spacer and use this modified part in the TEM-15 modules. The additional 0.010 in. of movement assures loading of the circuit before loading on the BN sleeve.

Figure V-28 shows the compaction curves for all TEM-15 modules built to date. The lower curves follow the test data for TEM-15 SN-4A very closely. The curves for TEM-15A SN-8 and -5 show a minimum stackup 0.025 in. greater than the previous modules. Although these two modules contain insulating mica with a smaller OD, the specified axial tolerance on the components was the same for all modules.

The circuit-length discrepancy was large enough to warrant examination of a sample lot of the circuit components. Measurements were made of a sample of 20 mica washers of each type washer used in TEM-15A. The results showed that the conductor-ring axial diameter fell within 0.00015 in. of the nominal length, but the type of mica used in the module represented by the lower curves deviates further from specification toward the lower tolerance limit than those used in TEM-15A SN-5 and -8. Such a difference in mica size could result in different compacted lengths as observed in the two groups.

The electron-beam (EB)-welded modules TEM-15A SN-1 and -5 were assembled with a minimum axial load of 236 lb (300 lb/in.² line pressure gauge reading). Based on the earlier generated curve it was concluded that this represented a minimum force consistent with a highly compacted circuit.

Figure V-28. TEM-15 Compaction Curve

TABLE V-10
TEM-15A SN-4 BEGINNING OF-LIFE PERFORMANCE DATA

Parameter	As Recorded	Adjusted to 1125°F/570°F
1. $\bar{T}_H$, °F	1127	1125
2. $\bar{T}_C$, °F	570	570
3. Q_{in} , watts	1813	1810
4. V_{oc} , volts	4.73	4.71
5. V_L , volts	2.41	2.36
6. I , amp	29.6	30.1
7. R_g , milliohms	78.5	78.3
8. P_e , watts	71.3	70.8
9. η_o , %	3.93	3.91

TEM-15A SN-4, the baseline module, was placed on test during the past quarter. Table V-10 gives BOL performance data from this module. The temperature-adjusted data include an adjustment for matched-load.

To date, TEM-15A SN-4 has accrued 310 hr of testing at $\bar{T}_H = 1125$, $\bar{T}_C = 570^\circ\text{F}$ operating conditions. As expected, performance has remained unchanged since BOL.

Two additional TEM-15 series modules, SN-4B and -8, were placed on test but data are not yet available.

2. Module Examination

a. TEM-15

The TEM-15 series modules are being fabricated to study the effects of various design, process, and component-material improvements on reactor module performance. The modules are similar to the TEM-13 series in radial dimensions but the axial circuit length is approximately one-quarter that of a TEM-13 module.

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Figure V-29. Photomacrograph of TEM-15A SN-4A

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TEM-15A SN-4A was one of the first modules of the series. The module contained 0.050-in. lead telluride washers separated by 0.005-in.-thick mica insulators. The conductor rings were similar to those used in the TEM-13G series. The module was processed using standard compaction processes and then destructively examined to evaluate the circuit configuration. Because of the smaller axial dimension of the TEM-15 design, it was possible to mount, cut, and polish the module in one piece.

Figure V-29 is a photomacrograph of the polished module section. It may be seen from Figure V-29 that the general appearance of the circuit is as expected, i.e. the circuit is well aligned, the TE washers are straight, and the mica insulator intact. Inboard bending of the molybdenum loading plate at its OD is observed at both ends of the module. The deformation at the positive end appears to be larger than at the negative. The bending effect is attributed to a mismatch between the inner- and outer-conductor-ring stackup. Size measurements on representative samples of the U-type mica used in this module indicate that the large mica washers deviate further from specification toward the lower tolerance limit than do the small ones. Such a difference in mica size could conceivably result in an outer-ring stackup with an overall dimensional length smaller than that of the inner-ring stackup. The loose stackup condition of the outer rings could allow axial movement during welding and/or application of high external loads. The ID movement of the plate would be restrained by the relatively solid length of inner-conductor rings. The mechanical behavior could be likened to a simply supported plate uniformly loaded over its entire area.

It is believed that the bending occurs during tungsten-inert-gas (TIG) welding of the end cap to the module. X-rays taken of TEM-15A SN-4A show the deformation of the plate after welding with no noticeable change occurring after processing. X-rays of an EB-welded module show no bending of the molybdenum load-plate after complete welding on either end of the module.

The inner and outer conductor rings were examined with the light microscope at 100X. Particular attention was paid to the distance between the rings. Along the outer-conductor-ring axis the gap between the rings appeared uniform. At the inner-conductor rings, however, microscopic examination showed that the axial gaps between the inner-conductor rings were not uniform. Figure V-30 is a schematic representation of the two cases of misorientation noted at the

Figure V-30. Schematic Representation of the Two Cases of Inner-Conductor-Ring Misorientation in TEM-15A SN-4A

inner-conductor rings. In both cases the mica appears to be expanded in the vicinity of the insert. The conclusion is that both cases could be explained by the step in the stepped ring being too long axially or the insert being too small axially. Either one of these cases would result in an expanded mica thickness as represented in Figure V-30.

To determine the validity of the above hypothesis, the gaps between the inner-conductor rings were measured under the microscope with the filar eye-piece at 100X. The distance between the large stepped rings was fairly uniform along the module axis, the average distance being 0.0048 in. The axial gap between the insert ring and the stepped conductor ring (Case 1, Figure V-30) was found to be larger than the average distance in 13 of the 35 gaps measured. Measurements made on the axial gaps between the insert ring and the step on the larger stepped ring revealed that in 22 of the 36 gaps measured the gaps were larger than 0.0048 in. (Case 2, Figure V-30). The net result of all the measurements taken was that the insert ring was approximately 0.001 in. too small axially or the step in the large ring was approximately 0.001 in. too large axially.

In a few of the cases examined a small amount of lead telluride was present in the gaps between the stepped inner ring and the small conductor insert or between the insert and the adjacent stepped ring. In no case was any lead telluride found to have penetrated radially into the gap far enough to reach the stepped conductor ring. Examination of the gaps in the outer-conductor rings revealed that no lead telluride had penetrated between the conductor rings.

b. TEM-9

Three TEM-9 type modules were also destructively examined to determine the effects of steady-state testing and thermal cycling on the module circuit. TEM-9C SN-5, containing 0.090-in. lead telluride washers and iron conductor rings, was cycled 22 times between $\bar{T}_H = 1132^\circ\text{F}$, $\bar{T}_C = 572^\circ\text{F}$, and $\bar{T}_H = 500^\circ\text{F}$, using a ramp rate of $20^\circ\text{F}/\text{minute}$ for both heatup and cooldown. TEM-9U SN-9 and -10, containing 0.090-in. lead telluride washers and solid tungsten inner-conductor rings, both experienced 21 of the cycles described above. The SN-10 testing and cycling were done with both heat input and heat rejection accomplished by a two-loop NaK system. None of these three modules showed any significant change in the operating parameters from the start to the end of the cycling periods. Continued testing showed no change in the performance trends of the modules.

Destructive examination of the three modules revealed that the TEM-9C SN-5 lead telluride washers were not displaced relative to the inner-conductor rings, and mica breakage at the inner-conductor-ring/lead-telluride interface had not occurred. TEM-9U SN-9 and -10, however, exhibited some axial displacement of the inner-conductor rings from the lead telluride washers. Additional measurements are in process.

3. Mica Evaluation

Experiments during this reporting period were continued to evaluate the effect of alternate barrier forms between pellets of N- and P-type lead tellurides. EXC-81, a sleeved capsule using a mica/tungsten-foil/mica sandwich as a barrier between N- and P-type pellets, was tested for 1000 hr at 1202°F and 1000 hr at 1382°F. The capsule was removed from testing and sectioned along one-half its axial length for Seebeck probe and microscopic examination. Seebeck-probe results from a similarly designed capsule, EXC-82, which was tested for 1000 hr at 1202°F, showed that the W was an effective barrier against interpellet contamination.⁽¹⁾ The Seebeck-probe traces performed along the axial length of EXC-81 showed that the N-type pellet remained unaffected even after the 1000 hr at the higher temperature. The results demonstrate that the foil was an effective barrier at the high temperature.

Four sleeved capsules, EXC-A1 through -A4, were fabricated with alternating N- and P-type pellets separated by 0.005-in. mica. The capsules were processed in the standard manner up to the scheduled initial compaction process. The cold-autoclave step was eliminated so that the capsules were compacted only by hot-isostatic compression in an attempt to evaluate whether hot compression would produce more ductile deformation of the mica. After processing, the capsules were tested for 1000 hr; EXC-A1 and -A2 at 1202°F and EXC-A3 and -A4 at 1382°F. After the annealing period the capsules were sectioned and prepared for metallographic examination and electrical probing. Seebeck probes were taken along the axial length of the capsules to determine to what extent interdiffusion had occurred. The resulting traces showed no evidence of contamination of the PbTe contained in the capsules tested at 1202°F. The N-type PbTe from the capsules tested at 1382°F was found to be contaminated to an axial depth of approximately 0.100 in. from each P-type/mica interface.

EXC-A1 and -A2 were then taken apart and the pellets removed. Seebeck-probe traces were taken on the N- and P-type pellet faces. Traces on pellets from EXC-A1 showed the N-type PbTe pellets were converted to P-type at the center of the faces that were previously adjacent to the 5-mil mica-insulator/P-type interface. The pellet edges, however, showed the normal N-type Seebeck value. Traces on the P-type pellet faces showed normal Seebeck values for the material.

Similar probe traces on EXC-A2 pellets showed that, although the faces of the N-type which were adjacent to the P-type PbTe were contaminated, the degree of contamination was not as bad as that noted in the material from EXC-A1. The N-type pellets showed high Seebeck values at their radial centers, but the conversion to P-type was not evident. Further, the radial width of the contamination was not as large. In capsule EXC-A1, the contamination was approximately 170-mils wide on a probe trace taken along the pellet diameter. Similar probe traces taken on pellets from EXC-A2 showed the width of the contamination to be less than 100 mils.

The above results were compared with those obtained from EXC-66, a capsule which had been processed in the standard manner and tested for 1000 hr at 1202°F. Seebeck probes along the circuit axis showed contamination of the N-type pellet at the N/P interfaces. Radial probes along pellet faces produced results similar to those obtained on pellets from EXC-A1 and -A2, i. e. maximum contamination concentrated at the center of the pellet. It is concluded that while compaction by the hot isostatic compression cycle may change the mica deformation mode, the change is not significant enough to completely eliminate intertype diffusion of the TE elements.

Four capsules, EXC-A5 through -A8, were removed from testing after 1000 hr at 1202°F. These were sleeved capsules containing alternate pellets of N- and P-type PbTe. The pellets were separated by 0.005-in. mica discs which were coated with a thin film of W or Mo. Two of the discs were assembled with the W or Mo-coatings facing and then positioned between N- and P-type PbTe, resulting in a total 0.010-in. mica barrier. Two of the four capsules, A5 and A6, contained three W-coated disc assemblies and one Mo-coated assembly. The other two capsules, A7 and A8, contained all W-coated disc assemblies. Figure V-31 is a plot of the circuit resistance vs time at 1202°F for the four

Figure V-31. Circuit Resistance vs Time at Test Conditions for EXC Capsules Containing Coated Mica Barriers

capsules. It can be seen from the figure that the capsules containing the single Mo-coated mica assembly exhibited a higher circuit resistance for a longer time than did the two capsules containing all W-coated discs. Also the Mo-containing capsule, which was hot-autoclaved only, maintained a higher circuit resistance for a longer period of time than did the Mo-containing capsule that was given the standard fabrication process of cold- followed by hot-autoclaving.

It was not possible to establish how the rate of resistance decrease was related to the effectiveness of the coated discs in inhibiting cross-contamination of the pellets. The foil barrier capsule also showed a decreasing circuit resistance with time and yet the barrier was completely effective. It appears that in the capsule at temperatures above 1200°F the BN insulating sleeve is contaminated by the direct contact with the PbTe. In power modules, however, the PbTe only contacts metal conductors, and as a result the BN remains an effective insulator and no low resistance paths are observed.

After testing, the capsules were sectioned axially and Seebeck-probe traces taken along the circuit length. The data for all four capsules showed no bulk contamination in the N-type PbTe. The capsules were then sectioned at their axial center, the pellets removed from one half of each capsule, and Seebeck-probe traces taken on the pellet faces. No contamination was evident in the pellets removed from EXC-A5. Probe traces taken on the pellets from EXC-A6, -7, and -8, however, showed a slight Seebeck-voltage rise but no conversion near the radial center of each N-type pellet. It is anticipated that when thicker crystalline coatings ($>1000\text{\AA}$) are deposited on the mica, the contamination will be reduced below the minimal amounts now present.

4. Lead Telluride Evaluation

N-type electrical measurement bars containing excess Te (0.25, 0.50, and 1.0 wt %) have been measured after 2000 hr at 1100°F in vycor tubes. Similar bars had previously been measured after 500 hr at 1100°F. Data from the Seebeck probes carried out on the bars heated for 2000 hr showed no significant changes had occurred due to the additional 1500 hr of annealing. The bars had all been converted to P-type at room temperature and demonstrated thermal conversion at elevated temperatures. Further, there were no additional indications that Te in excess of 0.25 wt % produced any further effects on the electrical properties over those caused by the minimum addition (0.25%).

Evaluation has been completed on an experiment designed to establish the effect of vapor transport of Te from P-type to N-type PbTe. A bar of each material was sealed in a single vycor vial and annealed for 2000 hr at 1000°F. Results obtained after 500 hr at 1100°F have been reported previously.⁽¹⁾ In the 500-hr test the P-type bar remained unaffected and the N-type was converted to P-type up to approximately 800°F where thermal conversion back to N-type occurred. In the case of the 2000-hr soak the N-type bar was converted to P-type over the entire temperature range investigated (80 to 1085°F). A change was also noted in the P-type bar, though it was relatively minor compared to the change in the N-type material. The peak value of the Seebeck coefficient had shifted to a lower temperature and the resistivity increased approximately 15% at the peak-temperature measurement of 1085°F. The results of this experiment indicate that the P-type material has a limited amount of excess Te which it may lose before the electrical properties are affected. However, this amount is more than enough to drastically affect the electrical properties of the N-type PbTe.

5. Material Studies

An experiment evaluating the oxidation behavior of nickel and iron pins heated at 975°F in air has accumulated more than 6000 hr at test conditions. The experimental details were covered in an earlier report.⁽¹⁾ After 6000 hr at 975°F the resistance of the Fe pins had risen 12% while the Ni pins exhibited no change (slope = 0) on the resistance-vs-time plot.

After 6100 hr the resistance of the Fe pins could not be measured, indicating that the contact between the stainless-steel wires and the Fe pins had been broken. The test was shut down and subsequently examined. The Fe pins showed oxidation had occurred to the point where approximately 2/3 of their original diameter remained. In addition the stainless-steel wires had broken away from the portion of the pins. The Ni pins, on the other hand, appeared similar to their original condition. The stainless-steel wires were rewelded to the Fe pins and the test was continued. At the present time the Ni pins have accumulated 7600 hr at test conditions without any change in resistance. The results obtained thus far indicate that Ni is completely acceptable as a conductor-pin material in terms of being resistant to oxidation in air at 975°F.

Another experiment was fabricated to evaluate the behavior of Ni pins in contact with Mo. In a module the Ni conductor pins are joined to Mo collector rings at both ends of the TE circuit. The experimental details of this study were given in a previous report.⁽¹⁾ The experimental configuration has been assembled and is being wired for testing at a temperature of 1100°F in vacuum.

6. Process Development

a. Coating of Mica Insulators

Work continued during this quarter on development of an EB evaporation process for applying thin (1000 to 50,000 Å) W or Mo films to mica insulating washers. Such coatings are expected to function as barriers to transport of Te from P-type to N-type PbTe washers in TEM module couples. This transport, either by diffusion through the mica lattice or by direct macroscopic flow through mechanical defects in the thin mica washers, has been identified as the principal remaining mechanism of steady-state degradation in tubular modules.

Twenty-seven experimental coating runs were made during this quarter. This phase of the program lacks equipment suitable for EB evaporation experiments. While a number of runs have been aborted due to equipment difficulties, some progress has been made and the concept appears feasible.

Films are being evaluated on the basis of five criteria:

- 1) Visual appearance: bright, shiny, metallic appearance free from cloudy or milky regions or stains is desired.
- 2) Mechanical integrity: freedom from cracks, blisters, peeling, flaking, and pinholes is desired.
- 3) Adherence: either uniform adherence to the mica substrate, or uniform and complete peeling as a foil sheet, is required. In either case the film must withstand gentle handling without experiencing mechanical damage.
- 4) Crystallinity: film must be crystalline (as opposed to amorphous).
- 5) Thickness: 1000 Å minimum required for initial evaluation, with an eventual objective of 10,000 to 50,000 Å.

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The first seven runs were investigative attempts to deposit W films. While some success was achieved in depositing films having good appearance and mechanical integrity, their adherence and thicknesses were poor. In addition the W films did not appear to be crystalline; no X-ray diffraction pattern could be obtained, either because the films were amorphous or because they were too thin to give a detectable diffraction pattern at X-ray wavelengths.

In view of the above problems it was decided to switch to Mo coatings, as previous work had hinted that Mo might be less difficult to deposit. The remaining twenty runs were attempts to deposit acceptable Mo films. With Mo, better results were obtained in terms of appearance and mechanical integrity, but, while some improvements were made in adhesion and film thickness, these latter remained unacceptable in terms of use as module components. Also, the crystallinity problem persisted; no X-ray diffraction pattern could be obtained on as-deposited Mo films.

At this point, it was discovered that after annealing an as-deposited W film at a fairly high temperature ($\sim 800^{\circ}\text{C}$), an X-ray diffraction pattern could be obtained and film adherence was noticeably improved. The pattern obtained happened to be that of hexagonal α - WC_2 ; however, it was deduced that this was due to a reaction between the W film and the graphite support rack in the heat-treating furnace, and that if the graphite had not been present, a pure W crystal pattern would have been obtained.

The above led to the concept of depositing a thin film, annealing to promote crystallinity and adherence, and then repeating the deposition-annealing process until an acceptable total film thickness was obtained. The last few experimental runs were used to establish the feasibility of this concept for Mo films. It was then decided to attempt to produce a quantity of multiple Mo-coated sheets sufficient for fabricating washers for one TEM-15 module. A "production" project will be undertaken and, based on experimental results, components produced in this manner should exhibit high-quality adherent Mo films having a final thickness in the 1000 to 2000 Å range.

The original objective of the experimental project, i. e. the direct application of 10,000 to 50,000 Å-thick high-quality W or Mo films by EB evaporation, cannot be achieved at the present time. Experimental work to date, together

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with extensive literature research, indicate that the following requirements are probably mandatory to meeting the objective:

Chamber pressure: 5×10^{-7} torr.
Substrate temperature: $\sim 500^{\circ}\text{C}$.
Substrate cleanliness: free of absorbed gases, particularly hydrocarbons.
Electron gun: reliable; filament-protected from source vapor.

This combination of requirements cannot be fulfilled in any equipment system to which we currently have access.

Accordingly, upon completion of the production Mo-coating program, experimental work will be resumed in the following areas:

- 1) Development of a multiple-coating/annealing process for application of W films.
- 2) Continued investigation of the effect of in situ glow-discharge cleaning of the mica substrate on the quality and adherence of as-deposited films. To this end a self-shielding hollow glow-discharge cathode has been designed and fabricated and a high-voltage power supply (separate from the electron-gun power supply) has been obtained.

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APPENDIX, SECTION V
TEST DATA FROM TEM-13G SERIES MODULES

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TEM-13 G S/N- 1

MEASURED

* TEMP AND LOAD
* ADJUSTED
* *****
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TIME HOURS *****	TH DEGF *****	TC DEGF *****	Q WTS ***	VOC VOLTS *****	RG M-OHM *****	PE WTS ***	ETA PCT ***	VOC VOLTS *****	RG M-OHM *****	PE WTS ***
1125/570F NOMINAL TEMPS										
.0	1153	575	5942	11.15	108.0	288	4.84	10.72	105.1	273
18.7	1150	569	5921	11.21	107.7	292	4.92	10.73	105.7	272
43.6	1152	576	5901	11.00	105.6	287	4.86	10.61	102.7	274
67.1	1147	567	5935	11.15	107.4	290	4.88	10.71	105.7	271
92.7	1147	573	5900	11.11	107.3	288	4.87	10.75	105.0	275
186.7	1145	576	5839	11.07	108.8	282	4.82	10.80	106.5	274
217.3	1150	574	5951	11.12	106.7	290	4.87	10.72	104.2	276
CHANGE TO 1025/570F										
218.1	1029	535	5069	9.34	96.5	226	4.45	9.22	96.4	220
235.5	1048	544	5153	9.50	97.3	232	4.51	9.19	94.8	223
341.3	1046	538	5272	9.69	97.5	241	4.57	9.31	95.7	226
CHANGE TO 1125/538F										
359.2	1148	573	5994	11.23	108.1	291	4.86	10.84	105.8	278
523.3	1145	578	5837	11.09	110.0	279	4.79	10.84	107.5	273
FIN MODIFIED (2 CYCLES)										
558.0	1152	572	5967	11.19	109.4	286	4.78	10.73	107.0	269
POWER FAILURE (1 CYCLE)										
582.9	1149	571	5862	11.13	110.2	281	4.79	10.70	108.0	265
627.8	1151	568	5876	11.21	110.1	285	4.86	10.70	108.1	265
916.3	1149	573	5708	11.05	111.7	273	4.78	10.65	109.4	259
960.5	1149	566	5810	11.11	109.9	281	4.83	10.59	108.2	259
POWER FAILURE (1 CYCLE)										
1014.5	1150	570	5815	11.02	110.5	275	4.73	10.57	108.4	258
1036.1	1151	568	5879	11.15	111.5	279	4.74	10.64	109.4	259
1153.8	1150	567	5767	11.13	112.5	276	4.78	10.63	110.6	255
1203.6	1148	567	5783	11.10	112.5	274	4.73	10.62	110.8	254
1347.5	1152	571	5762	11.14	114.4	271	4.70	10.65	112.0	253
1415.4	1149	569	5665	11.11	114.4	270	4.76	10.65	112.5	252
1566.0	1151	572	5795	11.00	113.7	266	4.59	10.57	111.3	251
1686.3	1147	562	5742	11.19	114.7	273	4.75	10.66	113.5	250
1757.7	1144	563	5774	11.09	114.2	269	4.66	10.61	113.1	249
1847.6	1144	565	5678	11.06	115.3	265	4.67	10.62	114.1	247
QIN&TH ADJ TO DPI8 LEVEL										
1848.5	1151	550	5952	11.33	111.6	288	4.83	10.53	111.1	250
21 COLD CPLS (2 CYCLES)										
1848.5	1151	569	5522	10.75	116.8	247	4.47	10.27	114.7	230
1365.4	1151	566	5525	10.77	116.9	248	4.49	10.24	115.0	223
POWER FAILURE (2 CYCLES)										
2059.1	1147	573	5534	10.46	116.2	235	4.25	10.12	114.0	224
2157.4	1153	568	5606	10.71	118.7	242	4.32	10.19	116.5	223

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MEASURED

* TEMP AND LOAD

* ADJUSTED

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TIME HOURS	TH DEGF	TC DEGF	Q WTS	VOC VOLTS	RG M-OHM	PE WTS	ETA PCT	VOC VOLTS	RG M-OHM	PE WTS
*****	*****	*****	***	*****	*****	***	***	*****	*****	***
0.0	1050	542	5151	9.52	95.3	238	4.62	9.15	94.7	226
14.4	1045	540	5276	9.32	92.7	234	4.61	9.00	90.3	223
166.5	1045	536	5273	9.56	93.7	244	4.63	9.17	92.1	228
POWER FAILURE (1 CYCLE)										
246.0	1052	542	5278	9.72	96.1	246	4.65	9.31	93.6	232
269.1	1044	537	5304	9.64	94.6	246	4.63	9.28	93.3	231
323.0	1050	543	5312	9.66	95.5	244	4.59	9.22	93.3	232
415.3	1047	543	5236	9.63	96.3	241	4.60	9.32	93.9	231
PLACED ON LOOP TEST										
504.2	1026	538	4918	9.30	97.1	223	4.52	9.28	97.3	222
519.4	1025	537	4942	9.31	97.3	223	4.51	9.30	97.4	222
543.0	1025	539	4912	9.32	97.6	223	4.53	9.34	97.5	224
557.3	1024	539	4901	9.28	97.3	221	4.51	9.33	97.3	224
591.2	1026	540	5012	9.24	96.2	222	4.43	9.25	96.3	223
LOOP SHUTDOWN & RESTART										
597.5	1027	540	5129	9.36	97.8	224	4.37	9.36	97.4	225
614.6	1025	539	5039	9.34	97.5	224	4.44	9.36	97.4	225
638.3	1026	540	5014	9.32	97.6	223	4.44	9.33	97.4	224
710.6	1026	539	5104	9.33	97.5	223	4.36	9.33	97.3	224
734.6	1026	538	4955	9.34	97.8	223	4.52	9.34	97.7	223
758.3	1027	539	5048	9.29	96.7	223	4.42	9.28	96.4	223
782.6	1026	539	5081	9.31	97.2	223	4.40	9.32	96.9	224
806.6	1026	540	5017	9.33	97.5	223	4.45	9.34	97.3	224
873.6	1027	540	5021	9.35	98.0	223	4.45	9.35	97.5	225
902.6	1025	539	4969	9.36	97.9	224	4.52	9.37	97.6	225
926.6	1026	540	4996	9.35	98.0	223	4.40	9.36	97.7	224
957.5	1026	539	5031	9.38	96.3	224	4.42	9.27	96.6	224

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MEASURED

* TEMP AND LOAD

* ADJUSTED

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TIME HOURS *****	TH DEGF *****	TC DEGF *****	Q WTS ***	VOC VOLTS *****	RG M-OHM *****	PE WTS ***	ETA PCT ***	VOC VOLTS *****	RG M-OHM *****	PE WTS ***
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1025/538F NOMINAL TEMPS

.0	1047	538	5037	9.22	95.8	222	4.41	8.84	93.9	208
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POWER FAILURE (1 CYCLE)

19.5	1037	536	5040	9.13	95.5	218	4.33	8.89	94.6	209
36.4	1050	538	5072	9.31	96.8	224	4.41	8.89	94.7	208
66.2	1048	538	5044	9.26	96.9	222	4.39	8.86	94.9	207
189.9	1040	542	4959	9.04	95.9	213	4.30	8.84	94.3	207
353.3	1042	536	5024	9.25	96.0	223	4.43	8.90	94.6	209
379.0	1042	537	4927	9.16	94.2	222	4.51	8.83	92.9	210

TEST CHANGE TO 1125/570F

381.7	1147	570	5743	10.74	107.3	269	4.68	10.35	105.5	254
407.9	1145	573	5698	10.73	108.4	265	4.66	10.41	106.4	254
569.8	1142	570	5686	10.69	107.7	265	4.67	10.39	106.2	254

POWER FAILURE (1 CYCLE)

658.4	1142	573	5658	10.57	107.4	260	4.60	10.32	105.7	252
736.7	1141	569	5688	10.70	108.9	263	4.62	10.39	107.5	251
749.3	1140	569	5672	10.70	109.6	261	4.61	10.42	108.3	250

13G1 TH & QIN AT 1025/538F

750.4	1039	521	5173	9.41	95.5	232	4.48	8.89	95.9	206
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13G-1 TC & QIN AT 1025/538F

752.3	1062	541	5164	9.59	100.0	230	4.45	8.98	96.6	209
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13G-1 TH & QIN AT 1125/538F

753.9	1147	555	5940	11.08	107.7	285	4.79	10.43	107.1	254
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13G-1 TC & QIN AT 1125/538F

754.8	1172	575	5942	11.18	111.6	280	4.72	10.43	107.0	254
773.1	1173	575	5988	11.20	111.0	283	4.72	10.42	106.4	255
801.4	1169	574	5931	11.18	111.7	280	4.71	10.45	107.5	254

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MEASURED

* TRIP AND LOAD

* ADJUSTED

* *****

TIME HOURS	TH DEAF	TC TEGF	Q WTS	VOC VOLTS	RG M-OHM	PI WTS	ETA PCT	VOC VOLTS	RG M-OHM	PI WTS
*****	*****	*****	***	*****	*****	***	***	*****	*****	***

OVERTEMP TRIP (1 CYCLE)

866.0	1168	574	5928	11.19	112.3	279	4.70	10.48	108.2	254
883.6	1171	574	5972	11.21	112.0	281	4.70	10.45	107.7	253

21 COLD TCS INSTALLED, 1025/538F

866.0	1044	541	4905	9.09	99.1	208	4.24	8.81	97.2	200
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13G-5 TC21 & QIN AT 1025/538F

883.4	1041	539	4900	8.97	98.1	205	4.18	8.70	96.6	196
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13G-3 QIN & TH to 750.4 HR LEVEL

884.6	1044	518	5173	9.49	98.2	230	4.44	8.83	98.3	198
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13G-5 TC21 & QIN AT 1125/570F

886.8	1139	571	5576	10.37	111.7	240	4.30	10.15	110.3	233
-------	------	-----	------	-------	-------	-----	------	-------	-------	-----

13G-3 QIN & TH TO 753.9 HR LEVEL

887.8	1151	550	5945	11.08	109.5	280	4.71	10.29	109.0	243
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21 HOT COUPLES, 1025/538F

887.8	1025	536	4901	9.02	96.5	211	4.30	8.98	96.7	209
903.8	1025	535	4881	8.99	96.5	210	4.29	8.96	96.8	207
928.7	1024	537	4812	8.90	96.1	206	4.28	8.90	96.3	206

13G-5 TC21 & QIN AT 1025/538F

929.7	1030	537	4915	9.03	96.9	211	4.29	8.93	96.5	207
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13G-3 D. P. 18 TH & QIN

932.1	1044	527	5164	9.47	96.7	232	4.49	8.93	96.1	208
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1125/570F NOMINAL TEMPS

934.9	1124	570	5519	10.39	107.5	251	4.55	10.41	107.6	252
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13G-5 TC21 & QIN AT 1125/570F

935.8	1126	560	5955	11.11	108.5	284	4.77	10.43	107.0	254
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TEM-13 G S/N- 3

MEASURED

* TEMP AND LOAD
* ADJUSTED
* *****

TIME HOURS	TH DEGF	TC DEGF	Q WTS	VOC VOLTS	RG M-OHM	PE WTS	ETA PCT	VOC VOLTS	RG M-OHM	PE WTS
*****	*****	*****	***	*****	*****	***	***	*****	*****	***

13G-3 QIN & TH TO 753.9 HR LEVEL

1001.4	1153	560	5955	11.11	108.5	284	4.77	10.43	107.0	254
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SLOTTED FIN INSTALLED, 1025/538F

1001.4	1025	534	4918	8.89	94.3	210	4.26	8.82	94.7	206
1014.3	1022	531	4910	8.89	94.2	210	4.27	8.83	95.1	205

13G-5 TC21 & QIN AT 1025/538F

1064.6	1022	534	4878	8.83	94.4	207	4.24	8.82	95.0	205
1113.0	1019	529	4913	8.84	93.4	209	4.26	8.80	94.8	204

13G-3 QIN & TH TO 750.4 HR LEVEL

1114.5	1047	531	5166	9.37	96.5	227	4.40	8.86	95.2	206
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1125/570F NOMINAL

1115.2	1127	566	5597	10.38	105.8	255	4.55	10.27	105.9	249
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13G-3 QIN & TH TO 753.9 HR LEVEL

1117.0	1157	566	5943	10.93	107.6	278	4.67	10.29	105.2	252
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1125/570F TEST CONDITIONS

1117.5	1126	564	5609	10.36	105.2	255	4.55	10.24	105.7	248
1502.8	1123	569	5536	10.24	108.7	241	4.58	10.26	108.9	242

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MEASURED

* TEMP AND LOAD

* AEC-11

TIME HOURS *****	TH DEGF *****	TC DEGF *****	W WTS ***	VOC VOLTS *****	RG M-OHM *****	PE WTS ***	ETA PCT ***	VOC VOLTS *****	RG M-OHM *****	W WTS ***
.6	1052	540	5338	9.72	94.3	250	4.69	9.14	91.0	229
20.8	1047	537	5337	9.68	94.1	249	4.66	9.25	91.2	232
42.6	1049	537	5355	9.58	93.6	250	4.67	9.22	91.6	232
63.4	1047	535	5363	9.74	94.9	250	4.66	9.30	93.3	232
96.4	1046	536	5336	9.71	94.8	248	4.66	9.29	93.1	231
113.8	1047	539	5313	9.66	95.2	246	4.63	9.27	93.0	231
138.2	1046	541	5253	9.56	94.6	242	4.60	9.22	92.4	230
POWER FAILURE (1 CYCLE)										
454.3	1047	537	5317	9.73	94.7	250	4.72	9.31	93.0	233
475.3	1046	534	5379	9.77	94.5	253	4.72	9.32	93.0	233
529.2	1052	536	5414	9.90	95.5	256	4.73	9.35	93.3	234
SLWR FAILURE (3 CYCLES)										
562.5	1052	539	5442	9.99	97.4	256	4.70	9.50	95.0	237
POWER FAILURE (1 CYCLE)										
706.5	1047	535	5428	9.91	96.7	254	4.62	9.45	94.9	235
743.7	1049	538	5304	9.72	96.0	246	4.64	9.28	94.0	229
744.6	1049	549	5150	9.55	97.8	233	4.52	9.31	94.7	229
QIN & ICAVG CONSTANT										
746.6	1041	543	5132	9.46	95.9	233	4.54	9.25	94.1	229
768.9	1040	542	5147	9.52	96.2	234	4.55	9.32	95.1	228
1068.9	1040	543	5164	9.44	95.9	232	4.50	9.25	94.2	227
1104.6	1042	544	5168	9.46	95.8	231	4.47	9.29	95.0	227
POWER FAILURE (1 CYCLE)										
1249.3	1039	544	5167	9.39	95.2	231	4.40	9.23	93.5	227
1309.3	1036	541	5172	9.38	94.3	233	4.50	9.44	93.1	229
1410.9	1034	542	5189	9.45	95.3	234	4.51	9.23	93.9	229
1504.9	1037	541	5163	9.41	95.1	233	4.52	9.25	93.8	229
1612.9	1033	542	5163	9.37	94.9	231	4.43	9.26	94.0	227
1632.1	1036	543	5163	9.41	95.5	232	4.49	9.31	94.1	232
QIN & THAVG CONSTANT										
1637.7	1047	554	5164	9.45	97.5	229	4.43	9.33	94.1	231
1711.5	1040	556	5116	9.43	99.1	224	4.38	9.36	95.7	229
1901.8	1045	554	5097	9.34	97.3	224	4.40	9.17	94.1	229
1971.5	1042	551	5108	9.37	97.5	225	4.40	9.29	94.8	229
2037.2	1043	558	5037	9.26	96.1	213	4.34	9.20	94.8	227
OVERTEMP TRIP (1 CYCLE)										
2140.4	1049	555	5164	9.49	96.7	222	4.42	9.32	95.3	225
2305.3	1042	551	5121	9.48	99.6	225	4.40	9.39	97.2	227
2732.2	1071	569	3759	7.31	113.7	117	3.12	9.15	96.1	219
2734.0	1041	551	5108	9.37	97.2	225	4.41	9.17	94.6	227

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MEASURED

* TEMP AND LOAD

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TIME HOURS	TH DEGF	TC DEGF	Q WTS	VOC VOLTS	RG M-OHM	PE WTS	ETA PCT	VOC VOLTS	RG M-OHM	PE WTS
*****	*****	*****	***	*****	*****	***	***	*****	*****	***
1025/538F NOMINAL TEMPS										
.0	1051	536	5160	9.72	96.0	246	4.76	9.22	93.9	226
168.1	1049	544	5304	9.95	98.8	251	4.72	9.59	96.2	239
21 CPL HARNESS INSTALLED										
355.3	1021	538	4860	9.16	95.1	221	4.54	9.23	95.4	223
434.5	1024	537	4898	9.26	95.9	224	4.57	9.26	96.1	223
435.7	1024	515	5150	9.59	93.3	246	4.78	9.21	95.4	222
TEST CHANGE TO 1225/601F										
438.0	1224	599	6328	12.23	116.3	321	5.08	12.20	116.6	319
452.5	1228	600	6355	12.25	115.5	325	5.11	12.18	115.4	322
477.0	1227	600	6315	12.13	115.8	318	5.03	12.09	115.7	316
597.8	1228	600	6272	12.11	117.1	313	4.99	12.05	116.9	311
649.3	1224	598	6330	12.15	116.3	318	5.02	12.13	116.7	315
OVERTEMP TRIP (1 CYCLE)										
699.6	1223	599	6309	12.10	116.7	314	4.97	12.10	117.1	313
715.2	1224	597	6344	12.09	115.7	316	4.98	12.04	116.2	312
836.8	1224	598	6303	12.10	117.5	311	4.94	12.07	117.9	309
889.3	1221	599	6272	11.98	117.2	306	4.88	12.02	117.8	307
931.2	1224	598	6339	12.09	118.0	310	4.88	12.05	118.4	307
1006.0	1222	600	6273	11.96	118.3	302	4.82	11.98	118.6	303
1077.2	1222	600	6225	11.95	118.4	302	4.85	12.00	118.7	303
1220.1	1229	600	6239	12.09	120.8	303	4.85	12.00	120.6	298
1368.8	1223	596	6125	11.91	121.1	293	4.78	11.84	121.7	288
1437.7	1224	600	6158	11.92	120.8	294	4.77	11.91	120.9	294
1509.9	1226	599	6139	11.85	121.7	289	4.70	11.82	121.8	287
1604.5	1224	596	6180	11.87	120.6	292	4.72	11.80	121.2	287
1680.9	1227	601	6127	11.79	121.1	287	4.68	11.75	120.9	285
2041.6	1202	588	6014	11.63	121.5	279	4.63	11.82	124.6	283
2042.3	1224	602	6120	11.78	123.3	281	4.59	11.81	123.3	283

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MEASURED

* TEMP AND LOAD

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TIME HOURS	TH DEGF	TC DEGF	Q WTS	VOC VOLTS	RG M-OHM	PE WTS	ETA PCT	VOC VOLTS	RG M-OHM	PE WTS
*****	*****	*****	***	*****	*****	***	***	*****	*****	***
.0	1030	540	4934	9.18	95.6	220	4.47	9.12	95.6	217
147.8	1029	543	4914	9.03	93.3	218	4.44	9.04	93.3	219
190.2	1023	534	4920	9.11	94.1	221	4.48	9.07	94.1	218
POWER FAILURE (1 CYCLE)										
246.0	1027	539	4971	9.27	95.7	224	4.51	9.24	95.7	223
265.9	1023	537	4930	9.03	92.9	222	4.51	9.11	93.1	223
338.0	1023	539	4862	9.08	95.0	217	4.47	9.14	95.1	223
411.8	1023	533	4883	9.08	94.8	218	4.45	9.03	95.5	214
554.4	1023	537	4935	9.09	94.6	219	4.45	9.11	94.8	219
728.1	1021	539	4864	9.11	96.6	215	4.41	9.20	96.8	219
771.9	1023	539	4842	9.08	96.6	214	4.41	9.13	96.7	218
844.0	1024	544	4809	9.02	96.6	211	4.38	9.15	96.1	218
939.3	1021	539	4860	9.03	95.8	215	4.45	9.17	95.8	219
1033.2	1019	542	4824	8.99	95.1	212	4.40	9.13	95.1	219
1374.2	1016	545	4746	8.87	96.2	204	4.32	9.15	96.4	217
1418.7	1014	541	4777	8.00	95.8	207	4.33	9.15	96.5	217
1419.4	1025	539	4931	9.20	96.6	219	4.45	9.21	96.5	220

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MEASURED

* TEMP AND LOAD

* ADJUSTED

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TIME HOURS	TH DEGF	TC DEGF	Q WTS	VOC VOLTS	RG M-OHM	PE WTS	ETA PCT	VOC VOLTS	RG M-OHM	PE WTS
*****	****	****	***	*****	*****	***	***	*****	*****	***
.0	1029	538	4791	8.90	94.7	209	4.36	8.82	94.4	206
18.0	1026	539	4806	8.91	94.8	209	4.36	8.92	94.6	210
90.7	1027	536	4889	9.08	94.7	218	4.46	9.01	94.7	214
140.4	1023	535	4860	9.02	94.5	215	4.43	9.01	94.9	214
182.5	1023	537	4852	9.04	95.3	214	4.41	9.06	95.6	214
213.9	1026	536	4899	9.05	93.7	219	4.46	9.00	93.8	216
POWER FAILURE (1 CYCLE)										
234.2	1024	536	4864	9.10	95.6	217	4.45	9.08	95.8	215
306.0	1022	541	4813	9.06	96.7	212	4.40	9.17	96.7	218
381.3	1025	538	4873	9.14	96.1	217	4.46	9.14	96.1	217
448.5	1031	541	4825	9.03	95.2	214	4.44	8.98	94.4	213
670.9	1031	539	4843	9.11	95.9	216	4.46	9.03	95.3	214
739.8	1030	539	4859	9.02	93.7	217	4.46	8.94	93.2	215
813.7	1033	547	4777	9.04	97.0	211	4.41	9.05	95.6	214
907.4	1034	541	4848	9.08	94.7	213	4.49	8.97	94.7	212
1250.5	1023	535	4810	9.01	94.5	215	4.46	8.99	94.8	213
1342.3	1023	537	4713	8.86	92.2	213	4.52	8.89	92.5	214
1387.9	1025	537	4806	8.95	93.2	215	4.47	8.94	93.3	214

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MEASURED

* TEMP AND LOAD

* ADJUSTED

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TIME HOURS	TH DEGF	TC DEGF	Q WTS	VOC VOLTS	RG M-OHM	PE WTS	ETA PCT	VOC VOLTS	RG M-OHM	PE WTS
*****	*****	*****	***	*****	*****	***	***	*****	*****	***
.0	1025	536	4928	9.03	91.9	225	4.56	9.05	92.0	223
68.1	1025	538	4896	9.36	92.2	223	4.55	9.06	92.1	223
121.8	1028	538	4778	8.91	92.5	214	4.49	8.85	92.2	213
163.5	1028	539	4765	8.92	92.8	214	4.50	8.88	92.5	213
POWER FAILURE (1 CYCLE)										
194.5	1028	537	4838	8.98	92.3	219	4.52	8.91	92.1	216
215.6	1027	536	4845	8.97	92.4	215	4.50	8.91	92.4	215
287.0	1028	537	4805	8.90	91.6	216	4.50	8.93	91.9	217
362.3	1024	540	4805	8.93	92.4	216	4.49	8.99	92.3	219
531.0	1021	542	4741	8.82	92.3	211	4.44	8.97	92.2	218
651.7	1020	536	4808	8.94	92.3	217	4.51	9.00	92.9	213
722.1	1019	534	4813	8.89	91.0	217	4.51	8.94	91.9	218
795.1	1018	539	4757	8.83	92.2	212	4.45	8.93	92.2	219
858.5	1020	534	4802	8.88	91.1	216	4.51	8.91	91.1	218
963.3	1017	531	4810	8.94	91.6	218	4.54	8.96	91.6	219
1324.9	1009	535	4771	8.73	83.4	216	4.52	8.96	92.1	223
1325.2	1025	537	4939	9.20	93.3	227	4.59	9.19	93.4	226
1394.1	1024	534	4870	9.03	92.4	221	4.53	8.98	92.8	217

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MEASURED

* TEMP AND LOAD

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TIME HOURS *****	TH DEGF *****	TC DEGF *****	Q WTS ***	VOC VOLTS *****	RG M-OHM *****	PE WTS ***	ETA PCT ***	VOC VOLTS *****	RG M-OHM *****	PE WTS **
.0	1027	537	4891	9.09	96.2	215	4.39	9.04	96.2	212
29.4	1027	537	4874	9.07	96.4	214	4.38	9.01	96.3	211
71.2	1028	536	4891	8.95	93.2	215	4.40	8.87	93.1	211
190.2	1026	537	4901	8.96	93.4	215	4.38	8.92	93.3	213
312.2	1028	541	4838	9.03	96.8	211	4.36	9.02	96.2	212
364.9	1022	537	4875	8.98	93.7	215	4.41	9.00	94.1	215
410.8	1024	535	4764	8.88	96.9	204	4.27	8.85	97.2	201
480.2	1022	538	4760	8.82	94.4	206	4.32	8.88	94.7	208
554.9	1023	536	4771	8.91	95.7	208	4.35	8.92	96.0	207
724.4	1029	540	4818	9.01	97.0	209	4.34	8.97	96.4	209
844.4	1028	537	4840	8.95	94.2	212	4.39	8.86	94.1	209
914.9	1027	535	4841	9.05	96.2	213	4.40	8.96	96.4	208
987.9	1027	541	4792	8.84	94.7	206	4.31	8.85	94.2	208
1083.0	1026	532	4886	8.99	93.1	217	4.43	8.85	93.1	210
1156.9	1027	536	4857	9.02	95.8	212	4.37	8.94	95.8	209
1516.1	1035	541	4916	9.08	95.6	216	4.39	8.95	94.5	212
1588.3	1034	540	4912	9.05	94.9	216	4.40	8.93	94.0	212

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VI. COMPONENT DEVELOPMENT

A. INTRODUCTION AND SUMMARY

The current Component Development effort comprises the design verification testing of the S8DR components and the technology development applicable to reliable high-performance components for the Reference Zirconium Hydride Reactor. The latter components include the bearings, actuator, 4 π shieldable reflector, shielding, and NaK pump.

The design verification testing of the three S8DR actuators and four sets of the control-drum bearings completed the scheduled 12,000 hr. These performed satisfactorily during the test.

One of the actuators which completed the S8DR design verification test is now being tested to demonstrate 20,000-hr life; it has accumulated over 16,000 hr of test. Both the nickel-clad silver and BeO-hardened copper conductor materials being evaluated for advanced actuator application continue to show stable electrical characteristics after 11,000 hr at 1400°F. A mockup actuator, incorporating most of the expected modifications to the S8DR design for the ZrH reactor application, was designed to be tested with the control-drum drive system.

The prototype high-temperature bearing is performing satisfactorily after 6000 hr at 1500°F and 10^{-8} torr. A MoS₂-Ta compact which is being evaluated as possible backup to the carbon graphite for bearings exhibits stable low frictional characteristics against both Al₂O₃ and Ta-10W at temperatures up to 1300°F.

The test fixtures to conduct thermal-cycle and shock and vibration tests on the previously completed 4 π shieldable control-drum assembly were designed and are being fabricated. Preliminary design of a control drum with a composite absorber (B₄C inserts in Ta-10W structure) is being prepared. The drum drive train components which connect the drum to the actuator were designed and the fabrication started.

Process problems which had delayed the fabrication of the developmental NaK pumps were successfully solved, and the pumps are being completed.

TABLE VI-1
S8DR ACTUATOR DESIGN VERIFICATION TESTING

Test Item	Actuator SN No.		
	DEV-010	DEV-011	DEV-013
Initial 55 thermal cycle	x	x	x
1000-hr endurance at 1125°F	x	x	x
1000-hr dwell at 1125°F	x	x	x
3000-hr endurance at 1125°F	x	x	x
Second 1000-hr dwell at 1125°F	x	x	x
4000-hr endurance at 1125°F	x	x	x
2000-hr dwell at 1125°F	x	x	x
Final 15 thermal cycles	x	x	x
Test hours	13,711	13,956	13,776
Thermal cycles	201	200	201
Startup cycles	100	98	100
Hours at 1125°F	11,867	11,625	11,738

TABLE VI-2
ACTUATOR PERFORMANCE AT 1125°F

Test Item	SN No.	Brake Current		Stepping Current		Insulation Resistance	
		Pull-In Amps (Minimum)	Release Amps (Maximum)	CW Amps (Minimum)	CWW Amps (Minimum)	Ohms Brake	Ohms Minimum Phase
Start of test	DEV-011	1.55	0.35	2.9	2.9	7M	300k
After 1st 1000-hr dwell		1.80					
After 2nd 1000-hr dwell		1.62					
After 2000-hr dwell		1.44					
Latest data		1.5	0.34	2.4	2.5	0.04M	0.075M
Start of test	DEV-013	1.44	0.68	3.9	2.8	7.5M	5M
After 1st 1000-hr dwell		1.56					
After 2nd 1000-hr dwell		0.94 [†]					
After 2000-hr dwell		1.68					
Latest data		1.58	0.54	2.8	2.8	0.37M	2.25M
Start of test	DEV-010	1.28	0.34	3.0	3.2	10M	200k
After 1st 1000-hr dwell		1.6					
After 2nd 1000-hr dwell		1.62					
After 2000-hr dwell		1.62					
Latest data		1.46	0.32	2.6	2.7	0.7M	1.2M

To step 40 oz-in. lead
[†]Brake not fully seated during dwell

The 5000-hr test results on the liquid-lead shield studies confirm that the addition of carbon to the lead has limited or no beneficial effect on the decarburization of the shield structural material by the liquid lead. The design of the liquid-lead test shield was completed and released for fabrication. An annular LiH shield was successfully cast.

A large-diameter LiH shield is being designed to check out the new AEC shield-casting facility and to provide experimental input for the design of similarly shaped shields such as the flight shield below the gallery section.

B. CONTROL-DRUM ACTUATOR

1. S8DR Actuator Design Verification Test

Three actuators of the modified S8DR design have now completed their 12,000-hr design verification tests. The test schedule consisted of an initial phase of 55 thermal cycles (400 to 1125°F) including 27 simulated startups, followed by periods of actuator operation and static dwells at 1125°F. During the actuator operational sequences, the actuator is stepped at 1125 and 400°F, between which it is periodically thermally cycled. The test sequence and accumulated test times are shown in Table VI-1. The insulation and coil resistances, listed in Table VI-2, are within design requirements.

Actuator SN DEV-011, which was placed back on test after the repair of the previously reported failure^(2,3) suffered another failure, similar to the previous failure, on the 98th of the scheduled 100 startup cycles. Performance data taken during testing and measurements taken after testing are being evaluated to determine if the rubbing of the rotor against the stator was caused solely by increased bearing clearance or distortion of rotor or stator parts.

2. Zirconium Hydride Actuator

The efforts in this program are directed toward the development of advanced actuators for ZrH reactors.

a. Component Testing

Tests are being carried out to evaluate potential high-temperature high-reliability conductor materials for actuator coils. Three different materials have been tested, or are under test: nickel-clad silver, Cube alloy, and Oxalloy.

The nickel-clad silver consists of a fine silver core swaged in "A" nickel cladding. Cube alloy is a copper base or matrix, dispersion-hardened by addition of fine particles of beryllia. Oxalloy consists of an OFHC copper core swaged in a chrome-iron alloy (430 stainless-steel) cladding.

The first test run was designed to produce failure in the conductor materials by thermal-cycling to 1300°F. Failure did not occur after 400 cycles so this was converted into a steady-temperature test at 1400°F. The remaining four coils of the test have shown no significant circuit-resistance variation after a total of 406 thermal cycles to 1300°F, ten thermal cycles to 1400°F, 339 hours at 1300°F, and 10,780 hr at 1400°F.

The second test is a performance test of prototype actuator coils. Six coils were wound with Cube alloy, six with nickel-clad silver. After 40 thermal cycles and 2276 test hours of which 2063 were at the test temperature of 1300°F, two Cube alloy coils failed due to brittle fracture of the conductor near the termination to the lead wire. This region of the conductor is subject to mechanical flexing during the coil-fabrication process. The conductor's inherent stiffness probably results in localized high stresses. This area of high stress is more subject to corrosion by the acidic solvent of encapsulant during the winding and curing cycles. Testing is continuing and the remaining ten coils have accumulated 64 thermal cycles and 5380 hr at 1300°F in a total test time of 5729 of a scheduled 20,000 hr.

b. Actuator Performance

After completion of the S8DR design verification testing, actuator SN DEV-013 was continued in test to demonstrate actuator performance to 20,000 hr. The total test time on this actuator is now 15,670 hr of which 13,392 have been at 1125°F. A total of 223 thermal cycles has been accumulated of which 113 have been startup cycles. The objective is 300 thermal cycles and 150 startup cycles. Performance of this actuator is normal.

c. Actuator Design

The design effort, targeted toward completion and review of the design layout near the end of this fiscal year, is continuing at a minimal level. A mockup actuator has been designed to provide an interface for the control-drum drive train test on the 4π shieldable reflector. This mockup actuator is described in Section VI-F-2-c.

C. CONTROL-DRUM BEARINGS

1. S8DR Design Verification Tests

The 12,000-hr design verification tests of the S8DR control-drum bearings have been completed. Four tests on the reference Al_2O_3 (on Inconel-750) against carbon-graphite were run, three at ground test conditions of 10^{-5} torr (two at a nominal temperature of 1150°F , one at 1250°) and the fourth at simulated space conditions of 10^{-8} torr at a nominal temperature of 1150°F . A fifth test at a nominal temperature of 1150°F and 10^{-8} torr was run on the backup TiC (on Inconel-750) against carbon-graphite. This one was terminated after completing 6000 hr at 1150°F .

Each of the reference material tests completed 12,000 hr and included a 1000-hr static dwell test at temperature and 85 thermal cycles to 450°F . Figures VI-1 through -5 show the test histories, and Table VI-3 summarizes the data.

TABLE VI-3
DESIGN VERIFICATION TESTS, MAXIMUM
TORQUE (in. -lb)

Temperature or Event	Test No. [†]				
	1	2	3	4	5
1150°F	3.8	3.7	-	4.5	5.5
1250°F	2.3	2.5	3.8	4.6	3.8
450°F	4.5	4.6	6.5	4.0	6.5
Ambient	6.8	5.2	6.5	4.3	3.8
Predwell*	2.0	2.4	2.4	1.5	-
Postdwell*	2.7	3.1	3.1	1.8	-
Vacuum	10^{-5}	10^{-5}	10^{-5}	10^{-8}	10^{-8}
Total hours	12,031	12,045	12,140	12,089	6019

*All 1000-hr dwell tests at 1150°F , except No. 3 at 1250°F

†Tests 1 through 4: Al_2O_3 vs carbon-graphite

Test 5: TiC vs carbon-graphite

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Figure VI-1. S8DR Bearing Friction
Test No. 1

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Figure VI-2. S8DR Bearing Friction
Test No. 2

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REFERENCE MATERIAL: Al_2O_3 vs P5 CARBON-GRAPHITE
 SNAP 8 CONTROL-DRUM BEARINGS DESIGN VERIFICATION TEST No. 3
 FRICTION AS A FUNCTION OF TIME AND TEMPERATURE
 1250°F AND 10^{-5} TORR
 31-lb AXIAL, 8.5-lb RADIAL LOADS
 12,140 hr at 1250°F
 86 THERMAL CYCLES

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Figure VI-3. S8DR Bearing Friction Test No. 3

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REFERENCE MATERIAL: Al_2O_3 vs P5 CARBON-GRAPHITE
 SNAP 8 CONTROL-DRUM BEARINGS DESIGN VERIFICATION TEST No. 4
 FRICTION AS A FUNCTION OF TIME AND TEMPERATURE
 1150°F AND 10^{-8} TORR
 31-lb AXIAL, 8.5-lb RADIAL LOADS
 11,443 hr at 1150°F
 643 hr at 1250°F
 86 THERMAL CYCLES

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Figure VI-4. S8DR Bearing Friction
 Test No. 4

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Figure VI-5. S8DR Bearing Friction Test No. 5

Tests No. 1 and 2 are identical and the data follow the same basic trend of friction. Test No. 3 also followed the same trend, while No. 4 and 5 at a higher vacuum resulted in greater friction initially. A possible explanation is that during the initial 2000 hr, gases were trapped in the carbon-graphite of Tests No. 1, 2, and 3, thus enhancing lubrication. As the gases were dissipated the friction increased until a carbon-graphite transfer film was formed on the mating Al_2O_3 surfaces to increase the lubricating properties of the couples.

During the last 5000 hr of Tests No. 1 and 2, friction remained nearly constant while that of No. 3 increased slightly. As the pressure of No. 4 and 5 was three decades lower, the carbon-graphite may have retained less trapped gas than the bearings in Tests 1 and 2, resulting in poorer lubrication and greater friction.

After 7000 hr at test a vacuum failure occurred on Test No. 4 and the bearings were removed and inspected. Following reassembly, friction torque was considerably lower indicating the possibility that the internal linkage was binding during prior testing.

As a general conclusion, the friction characteristics of the TiC bearing set are considerably poorer than those of the Al_2O_3 bearing set.

Examination of the Al_2O_3 -carbon-graphite bearings revealed no evidence of cracking or failure of the Al_2O_3 coatings or wear of the carbon-graphite ball. Examination of the TiC-carbon-graphite bearing showed noticeable lines of contact on the carbon-graphite insert of the ball. There is, however, no evidence of heavy wear of the carbon-graphite nor damage to the TiC coating.

Design verification test results demonstrate that the S8DR control-drum bearing performance is well within design requirements. The backup material combination of TiC vs P5N carbon-graphite also appears to provide acceptable bearing performance.

2. Advanced ZrH Reactor Bearings

The ZrH bearing program is designed to advance bearing technology from the SNAP 8 requirements to 20,000 hr life at 1500°F. A prototype bearing with Al_2O_3 -plated Ta-10W shaft and socket and a solid carbon-graphite (Pure Carbon, grade P5) is undergoing life test.

a. Ultrahigh-Vacuum Bearing Test

The prototype bearing set continues under endurance testing at 10^{-8} torr and 1500°F . The test has completed 5000 hr at 1500°F . Figure VI-6 shows the friction data as a function of time. Testing is planned to continue to 20,000 hr.

b. Sliding Friction

A MoS_2 -Ta compact has been studied as a possible replacement for the carbon-graphite bearing material. Two specimens of the compact material have completed 1800 hr of testing at 1300°F and 10^{-8} torr. One specimen was tested against bare Ta-10W and one against Al_2O_3 . Figure VI-7 shows the friction coefficients for the two couples as a function of time. The results of these limited tests show that this material is probably acceptable for bearing use up to 1300°F in vacuum. Its application at higher temperatures is limited by the increased friction (i. e. $\mu = 0.7$ at 1600°F against Al_2O_3) as reported previously. ()

D. SHIELD DEVELOPMENT

1. Materials Studies

a. Liquid-Lead Studies

The purpose of this program is to study the effectiveness of saturating lead with carbon to minimize the decarburization of the structural alloys Croloy 2-1/4 steel and 1020 carbon steel by molten Pb.

The 5000-hr exposure test and capsule examination were completed. Tables VI-4 and -5 summarize the depth of decarburization of these test capsules. Also included for comparison are interpolated values for 5000-hr test data from earlier tests made at 625, 2500, and 10,000 hr only, as well as data from the 2500-hr test capsules. The benefit of the C addition to the Pb in the 1020 carbon steel capsules tested at 1300 and 1500°F for 5000 hr is readily seen. However, it is seen that the change in the depth of decarburization between the 2500- and 5000-hr test is negligible. In both cases the addition of C to the Pb-containing Zr and Mg increased the depth of decarburization.

The test data on the Croloy 2-1/4 steel is not as well defined. At 1300°F the depth of decarburization is quite low. At the 1500°F test condition the data are erratic, although it is obvious that the addition of C is less beneficial than

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Figure VI-6. Al_2O_3 vs Carbon-Graphite UHV Bearing Test, Friction Torque at 1500°F, 10^{-8} torr

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Figure VI-7. Friction Coefficient at 1300°F, MoS₂ Compact (65 wt % MoS₂, 10 wt % Mo, 25 wt % Ta) vs Bare Ta-10W and vs Al₂O₃ on Ta-10W

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TABLE VI-4
DEPTH OF DECARBURIZATION OF 1020 CARBON STEEL
AT VARIOUS TEST CONDITIONS AFTER
2500 AND 5000 hr OF EXPOSURE
(Values in 10^{-3} in., mils)

Additives	1300°F				1500°F			
	Lead		Vapor		Lead		Vapor	
	2500 hr	5000 hr	2500 hr	5000 hr	2500 hr	5000 hr	2500 hr	5000 hr
None	4.0	4.5	4.0	4.5	5.9	6.0	5.3	5.3
Carbon half-saturated	2.5	2.5	2.7	3.0	4.0	4.0	4.0	5.0
Carbon saturated	2.0	2.0	2.6	2.5	3.3	3.0	4.0	4.5
Zirconium, magnesium	~1	1.0	~1	2.0	<1	1.5	4.2	4.5
Zirconium, magnesium + carbon	1.3	1.5	1.8	1.5	2.7	3.0	3.3	4.0

Interpolated from earlier test data

TABLE VI-5
DEPTH OF DECARBURIZATION OF CROLOY 2-1/4 STEEL
AT VARIOUS TEST CONDITIONS AFTER
2500 AND 5000 hr OF EXPOSURE
(Values in 10^{-3} in., mils)

Additives	1300°F				1500°F			
	Lead		Vapor		Lead		Vapor	
	2500 hr	5000 hr	2500 hr	5000 hr	2500 hr	5000 hr	2500 hr	5000 hr
None	~1	~1	1.6	~1	4.5	3.0	3.0	3.3
Carbon half-saturated	~1	<1	~1	~1	3.6	2.4	3.3	3.3
Carbon saturated	1.1	2.0	~1	1	3.6	5.3	3.0	4.0
Zirconium, magnesium	<1	1.5	<1	1.4	1.1	1.1	3.5	3.6
Zirconium, magnesium + carbon	<1	~1	<1	<1	5.5	5.6	2.2	1.5

Interpolated from earlier test data

the addition of Zr and Mg. The addition of C to the Pb-containing Zr and Mg, as in the 1300°F case, actually increases the depth of decarburization of the Croloy 2-1/4 steel.

Chemical analysis of the Pb from both the 2500 and 5000-hr tests has failed to find C at any concentration greater than in the original untested lead. It is believed that during the slow cooling of the lead in the furnace at the end of the

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Figure VI-8. Close-up of LiH Spill at the Small Vent Tube

test period, the C precipitates out on the surface of the Pb and is lost during the cutting of the capsule for metallographic examination. At the end of the 10,000-hr test, selected capsules will be opened using a tubing cutter which neither produces dust nor adds any foreign material.

The conclusion of the 5000-hr test confirms that, like the 2500-hr test, the benefit of the C addition is minimal in that the reduction in the depth of decarburization is small. In addition, in those cases where the Zr and Mg corrosion inhibitors are present in the lead it is concluded that the addition of C is actually detrimental.

b. Liquid-Lead Shield Test

The redesign of the liquid-lead shield, to decrease the lead thickness to 4.5 in. and to reverse the curvature of the top to place the shield nearer the reactor core, was completed. Fabrication of the test shield was started. Design of the heater to simulate the reactor heatup cycle was completed.

c. Annular Lithium Hydride Shield (Experimental Shield No. 3)

The objective of the annular LiH shield test is to investigate the problems associated with fabricating and casting a unique annular configuration. A similar shape will surround the reactor in a 4π shielded arrangement.

Fabrication of the annular shield vessel was completed. The vessel was loaded with 142 lb of granular LiH and placed on the retort base which was evacuated and backfilled with H_2 gas. Heat was applied and required 38 hr to complete the melting and freezing cycle.

Several irregularities noted during the cycle were not considered serious enough to alter the process. They were:

- 1) Two heaters, on different filler tubes, developed open circuits, but the redundant heaters remained operative to continue the processing.
- 2) The H_2 burnoff flame was noted to be a vivid reddish-purple, indicative of lithium compounds in the flame. In addition the outflow of H_2 from the retort appeared to be slightly higher than normal.

When the system had cooled it was observed that LiH had spilled out of the two ventlines (shown in Figure VI-8) and run onto and reacted with the Fiberfrax insulation. It was concluded that H_2 gas escaping from the vessel during the

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Figure VI-9. Top of Cast LiH Block Showing the Two Casting Voids

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melting had blown a small amount (perhaps a quart) of molten LiH out the vent tubes. Normally all escaping H₂ gas leaves the vessel via the filler tube which is connected to the large reservoir.

Void volume of the vessel was measured to be 0.178 ft³; the volume of the shield vessel was 2.57 ft³. Full-can density (percent of the vessel filled with LiH) was calculated to be 93.1%, slightly lower than the 94.0% average obtained from previous castings.

Preliminary X-ray examination indicated the casting to be sound. More extensive X-ray revealed the presence of two voids near the top of the vessel which were not noted during preliminary X-ray examination. One void was estimated to be about 7 in. long by 2-1/2 in. deep; the other void was about 2 in. long by 1-1/2 in. deep. The crack pattern throughout the shield appeared to be normal.

Only one major dimensional change caused by the casting cycle was found to have occurred. The bottom of the shield was found to be convex and the top concave. These changes had been observed in previous test specimens having flat tops and bottoms, and are caused by the thermal stresses produced during the cooling cycle.

The upper portion of the stainless-steel vessel was removed from the LiH casting using an abrasive cutting wheel. The casting voids are shown in Figures VI-9 and -10. Removal of the remainder of the vessel revealed no other irregularities in the LiH block. Figure VI-11 shows the exposed LiH block. The areas where the LiH had stuck to the steel and broke off when the steel was removed appear as holes in the cast block. (NOTE: The irregularities observed along the edge of the block were caused by the chipping off of LiH during cutting of the steel.)

Evaluation of the shield and the casting data led to the conclusion that the LiH spill and the casting voids were entirely unrelated. It is believed that the casting voids were caused by differences in cooling rate of the inside and the outside of the annulus. The insulation which had been placed inside the center cavity to prevent rapid cooling had been too efficient and caused the LiH near the cavity to cool too slowly. The freezing front of the LiH progressed upward and inward towards the center while the LiH near the center remained liquid. As the freezing front passed inward across the thermocouple located at the

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Figure VI-10. Top View of the Large Casting Void in
Experimental Shield No. 3

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Figure VI-11. View of Cast LiH Block with Stainless-Steel Vessel Removed

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bottom of the filler tube, the LiH could no longer flow into the vessel. When the pocket of trapped LiH did freeze a void was left. It is recommended that additional thermocouples be placed in the region near the center cavity, and that an auxiliary heater instead of insulation be installed inside the cavity to provide a method of controlling the heat loss rate.

2. Design and Fabrication Development

a. The Checkout Shield

A new program was begun this fiscal year to provide the development effort needed to fabricate the large LiH shields for the 4π shadow shield system. The initial activity in this program is to provide a test shield to check out the operating characteristics of the new AEC Casting Facility (Bldg. 042, Santa Susana) equipment, and to provide preliminary information on the casting of thin large-diameter shields.

The design philosophy of this checkout shield is that the shield will simulate in geometry and size, but not in complexity, the lower axial shield of the 4π shield system.

General characteristics defined for the shield are:

- 1) Diameter, about 86 in.
- 2) Thickness, 13-in. maximum
- 3) LiH weight, about 2000 lb.
- 4) Structural material, a Type-300 series stainless steel.
- 5) Matrix, 1-in.² cell honeycomb, 1-mil stainless-steel foil, perforated 1/2-in. -diam holes on 3/4-in. centers.

E. NaK-PUMP DEVELOPMENT

The previously described problem⁽²⁾ in forming pump throats with a rectangular throat section and circular ends was solved by addition of MoS₂ powder to the forming lubricant and careful refinishing of the forming tools after each pass. Several acceptable throats were fabricated.

Problems of cracks and lack of complete penetration during the process qualification of the TIG welding of the support splitter plate in the throat section

were resolved by closer control of the process parameters. Acceptable weld rework methods were also developed and qualified.

The initial attempt to qualify the brazing process for attaching the flexible bus assembly and the two solid buses to the two throats failed. The joint between the throats and the diffusion-bonded ends^() of the flexible bus appeared to be starved of the braze material, resulting in excessive voids. Metallurgical examination of the braze joint indicated that possible outgassing from the unbonded space of the bus ends might have caused some of the voids.

Machining one side of the bus end, previously unmachined, approximately 0.25 in. from the braze interface datum and preparing the flexible bus assembly by heat-cleaning in a vacuum environment at 1825°F for 30 min. provided an excellent braze between the ends of the flexible bus and the throats. Metallurgical examination showed essentially 100% bond between the braze zone and both the bus and throat.

With these fabrication difficulties resolved, completion of the two developmental pumps is in progress.

F. 4π SHIELDABLE REFLECTOR

The objective of this project is to develop a control reflector system for a 4π -shielded reactor. This program currently consists of three main tasks: (1) drum development, (2) drum drive system development, and (3) material studies.

1. Drum Development

The drum development effort consists of four subtasks: (1) development drum No. 1, (2) development drum No. 2, (3) drum critical test, and (4) drum thermal-gradient test. Subtasks 1 and 2 are in progress and the remaining subtasks are scheduled to be started later in the fiscal year.

a. Development Drum No. 1

Design of a thermal-cycle test heater and a shock and vibration fixture was completed. Fabrication of the detail parts of the test heater was 75% completed. Materials for the shock and vibration fixture were ordered.

b. Development Drum No. 2

Object of this subtask is to develop a drum of lighter weight and greater nuclear worth than the present reference design. The weight of the present design imposes large launch loads on the drum lockout device and couplings. As a result of preliminary studies of alternate absorbers made of hafnium, Ta-10W with B_4C , and hafnium with B_4C , an absorber made of Ta-10W with B_4C was selected. The absorber designs studied use small B_4C rods imbedded in holes drilled in the Ta-10W absorber in the longitudinal direction. Stress and thermal analyses on this configuration have been started.

2. Drum Drive System Development

a. Drive Train Coupling

Design studies of various couplings were made, with three couplings studied in detail: (1) universal; (2) torque-transmitting arm; and (3) bellows coupling.

The bellows-coupling concept has been selected as the reference and the design has been completed and fabrication started. The material selected for the bellows coupling is Inconel-718.

b. Gear and Drum Lockout

Detail design and fabrication drawings have been completed from the drum lockout mechanism described in a previous progress report.⁽²⁾ Rene-41 alloy has been selected for these components.

c. Actuator Assembly

The original goal had been to use most of the S8DR actuator assembly design. High launch loads and the end load transmitted to the shaft by the lockout mechanism necessitated redesign of the actuator shaft. A composite rotor shaft has been designed. Hiperc-27 magnetic material has been retained for the rotor and Hastelloy-B material is used for the shaft.

The composite rotor and other associated components will be assembled as shown in Figure VI-12. This assembly, along with the couplings shown in Figure VI-13, will be evaluated for functional performance and ability to withstand expected launch loads.

Figure VI-12. Actuator Assembly,
ZrH Reactor Drum Dummy

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Figure VI-13. Drive Train Assembly,
Shock and Vibration Test Mockup

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3. Material Studies

The material study effort consists of five subtasks: (1) material selection; (2) material compatibility tests; (3) emittance tests; (4) drywell coating application development; and (5) coating evaluation tests. Subtasks 1, 2, and 3 are in progress, and the remaining ones are scheduled to start later in the fiscal year.

a. Material Selection

Material selection activities are directed toward the best choice of materials for the band, cladding, and coating or coatings required on the drum.⁽³⁾ It is tentatively planned to coat the drum surfaces with a high-emittance coating over a tungsten diffusion-barrier to limit the drum temperatures to acceptable levels.

Iron titanate has been tentatively chosen as the emittance coating; however, if the coating were to flake off the drum into the drywell area and become trapped in the bearing clearances, the drums could bind or lock to where they could not be turned. Two possible solutions to this problem are to eliminate drum coating or to coat only the internal surfaces of the drum. To evaluate the effects of removing the emittance coating from various surfaces of the drum, a thermal analysis was started on several alternately coated drum designs. A preliminary summary of these temperature effects is given in Table VI-6.

Studies are being continued for the best choice of material for the drum band and cladding. To date, no single band material has been identified which would meet all requirements with any degree of confidence. The best solution appears to be a refractory type material with a coating system to prevent or retard oxygen embrittlement.

b. Material Compatibility Tests

The bare sample material compatibility test was continued at 1800°F in a vacuum in the 10^{-7} -torr range with 2300 hr accumulated at elevated temperature. The coated material compatibility test efforts are being held up by selection of the materials and coating for the band and the cladding described previously. Materials for potential base materials are being procured.

TABLE VI-6
MAXIMUM DRUM TEMPERATURES AS A FUNCTION
OF ALTERNATE DESIGNS, °F

Design No.	Configuration	Beryllium Oxide in				Poison in			
		BeO	Absorber	Band	Cladding	BeO	Absorber	Band	Cladding
1	Reference design.* All surfaces with an emittance (ϵ) = 0.60	1772	1614	1625	1504	1655	1788	1521	1521
2	Same as 1 except band and cladding made of Cb-1Zr. All surfaces uncoated. ϵ = 0.30.	2186	1956	1988	1813	2012	2207	1833	1833
3	Same as 2 except drywell oxidized with ϵ = 0.73 for drywell only.	1996	1766	1798	1623	1822	2017	1643	1643
4	Same as 3 except inside of cladding and absorber at BeO-absorber interface coated with ϵ = 0.8.	1967	1776	1798	1653	1825	1985	1670	1670
5	Same as 2 except no cladding	1922	1780	Not calculated	-	1773	2082	Not calculated	-
6	Same as 5 except drywell oxidized with ϵ = 0.73 for drywell only.	1732	1590	Not calculated	-	1583	1892	Not calculated	-

*Ta-10W absorber and BeO reflector held by Ta-10W bands to the absorber.

Reactor power level = 1200 kw.

Drywell temperature = 1110°F.

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c. Emittance Tests

Literature surveys have shown large variations in the emittance of BeO. This variation in reported data and the fact that the reference design BeO material (UOX + 0.5% MgO) may not be available in the future prompted measurement of the emittance of both the reference BeO and the alternate BeO (UOX grade called Thermolox 995). Measurements are currently being made on the latter material and test samples of the reference BeO and of B₄C are being prepared.

VII. FUEL DEVELOPMENT

First of two primary objectives of this program is to develop fuel elements to meet the requirements for the 12,000-hr 600-kwt SNAP 8 reactor.

The second is to understand and, as necessary, upgrade the present ceramic-barrier metal-clad fuel-element technology to satisfy the requirements of the 20,000-hr 600-kwt ZrH reactor system. Improved assembly processes and new clad materials are under study to improve fuel-element performance. Irradiation experiments to further explore the behavior of the fuel and cladding materials and to confirm fuel-element improvements are part of this program.

A. FUEL-ELEMENT MATERIALS EVALUATION

1. Cladding Barrier

a. SCB-Coating Hastelloy-N Performance

The six 15-mil Hastelloy-N-clad developmental elements (three S8DR type as controls and three double-clad) completed the scheduled 10,000 hr of thermal endurance at 1400°F with no appreciable change in leakrate performance. Overall performance of these elements is summarized in Figures VII-1, -2, and -3. The double-clad elements experienced significantly less hydrogen loss than the standard clad elements. These elements are now scheduled for post mortem examinations.

Destructive examinations were performed on two Phase-I S8DR performance-test elements, No. 27 and 53, which had completed 12,000 hr of endurance at 1400°F. Results are summarized in Table VII-1 along with data from previous Phase-I, -II, and -III elements. (Phases I, II, and III denote representation of start-of-core, mid-core and end-of-core production, respectively.) Some flaking of the fuel-barrier reaction film was found when both of the elements were decanned. As with previous elements, nothing which could be considered abnormal was seen. A summary report of the examination of 14 of the S8DR performance-test elements is in preparation.

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Figure VII-1. Leakrate History of the Six 15-mil Hastelloy-N-Clad Developmental Elements

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Figure VII-2. Cumulative Hydrogen Loss for the Six 15-mil Hastelloy-N-Clad Developmental Elements

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Figure VII-3. K As a Function of Time for the Six 15-mil Hastelloy-N-Clad Developmental Elements

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TABLE VII-1
PERMEATION SCAN AND FLEX SUMMARY, S8DR
PERFORMANCE-TEST ELEMENTS

Test Time (hr)	Phase No.	Element No.	Percent of Total Measured Leakrate						Coating Adherence Test Number of Flexes				Percent Hydrogen Loss†
			1-1/4 in. of Closure End		12-1/2 in. Center		1-1/4 in. of Blind End		Closure End		Center of Tube	Blind End	
			Before	After	Before	After	Before	After	Before	After	After	After	
4000	I	32	-	-	-	-	-	-	9	5	-	23	1.83
		35	38	46	36	14	26	40	3	5	5	23	3.37
	II	94	32	43	50	18	18	39	7	1	5	23	5.42
		100	-	-	-	-	-	-	19	7	-	23	2.56
	III	170	-	47	-	14	-	39	17	3	-	23	2.38
		360	22	47	50	6	28	47	23	23	23	23	1.20
8000	I	65	-	51	-	9	-	40	5	3	-	13	5.71
		77	57	51	29	12	14	37	5	5	9	15	7.33
	II	96	23	45	59	13	18	42	9	5	5	25	7.38
		344	-	51	-	4	-	44	9	11	-	15	2.25
	III	359	-	54	-	15	-	31	23	23	-	23	2.27
		399	38	47	24	11	38	42	23	23	23	23	4.34
12,000	I	27	41	47	12	10	47	43	9	5	5	23	5.45
		53	-	50	-	4	-	46	3	7	-	23	6.89

Before and after testing.

†Based upon integrated leakrate.

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Figure VII-4. Specified Random Noise Input That SNAP Fuel Elements Will Be Subjected To

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b. Suppression Device

A significant change has been made in specified dynamic loads which fuel elements must withstand. Previously both random and sinusoidal dynamic inputs were required. The current specification (SS696N17001) requires only random vibration. The specified input is shown in Figure VII-4. Previous tests have shown that random vibration inputs are probably not damaging to the glass coating even without the load-suppressing column buckling device (CBD).

However, two changes to the fuel-element design which affect the design of the CBD are planned. The first of these requires backfilling the fuel element with helium. This will reduce the reactor startup ΔT between fuel and cladding. As a consequence the CBD will probably survive startup and will collapse due to axial fuel growth at reactor operating temperatures. The second design change involves segmenting the fuel rod. Even though previous tests have shown that the random vibration inputs may not damage the coating, it is felt that the CBD should be used to prevent initial movement and/or damage to the segmented fuel.

Type-405 SS was selected for use as the column material in the CBD. This alloy undergoes an even larger dropoff in strength than the 502-SS previously specified. The 405-SS gives an added margin of safety in that the strain on the ceramic barrier will be greatly reduced.

c. Alternate Cladding

The seven remaining 15-mil Incoloy-800-clad developmental elements continued on test. Since being inadvertently subjected to temperature excursions (possibly $\geq 1600^\circ\text{F}$) during the post-5000-hr ramp heat inputs, the elements have accrued an additional 2000 hr of endurance. Figure VII-5 shows a slight increase in the average leakrate after the ramp inputs and excursions. This change is also reflected in the cumulative hydrogen-loss curve of Figure VII-6. These elements are now being subjected to another series of ramp heat inputs.

The four double-blind-end Incoloy elements containing enriched uranium rods completed the initial 5000 hr of 1400°F endurance, and were subjected to the post-5000-hr ramp inputs; since then they accrued nearly 1000 of the second 5000 hr of endurance. Figure VII-7 shows an unexplainable step decrease in the average permeation rate after the last ramp inputs. The apparent poor

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Figure VII-5. Average Normalized Leakrate of the Seven Incoloy-800-Clad Developmental Elements (Four double-blind-end and three cup-plug type)

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Figure VII-6. Cumulative Average Hydrogen Loss for the Seven Incoloy-800-Clad Developmental Elements

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Figure VII-7. Average Normalized Leakrate of the Four Enriched Uranium Double-Blind-End Incoloy-800-Clad Elements

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Figure VII-8. Cumulative Hydrogen Loss for Four Enriched Uranium Double-Blind-End Incoloy-800-Clad Developmental Elements

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performance of this group of elements, as evidenced by the hydrogen loss shown in Figure VII-8, is a result of the influence of one high-leaking [1.17 cc(STP)/hr] element in the group. The other three elements are performing normally with an average rate of about 0.5 cc(STP)/hr (normalized).

During this quarter a requirement was made to include a partial pressure of helium within the fuel element (approximately 0.1 atm). It was felt that with a change in assembly procedures, cup-plug-type elements can be assembled to entrap the required helium with little or no element design change. Accordingly, four trial elements were assembled containing segmented fuel, a collarbutton vibration suppressor in the blind end, and a partial pressure of helium to check out the new assembly sequence. The welds were metallographically examined and no abnormalities were found. However, the elements were high leakers. This was attributed to difficulties encountered with the induction-heating unit during the blend cycle. The unit is worn out; a new unit to replace it is being purchased.

Stress-rupture data obtained from Incoloy-800 specimens tested in flowing NaK and in helium (NaK controls) at 1400°F are shown in Figures VII-9 and -10. For comparison, curves representing International Nickel Company data (Bulletin No. T-40) on solution-annealed hot-rolled Incoloy-800 plate are also shown. It should be noted, however, that the Inco stress-rupture tests were carried out uniaxially in air. Under these conditions the material should exhibit higher ductility than when tested biaxially, and higher strength than when tested in helium.

Two heats of Incoloy-800 are being examined, No. 810535 and HH7531A. Glass-coated specimens of both heats and uncoated as-received specimens of Heat No. HH7531A have been creep-rupture tested. The principal property-determining constituents of these two heats are shown in Table VII-2.

TABLE VII-2
COMPARISON OF ALLOY CHEMISTRY
(wt %)

Heat No.	Ti	Al	C
810535	0.38	0.37	0.04 maximum
HH7531A	0.20	0.10	0.03 to 0.05

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Figure VII-9. Biaxial Creep-Rupture Tests on Coated Incoloy-800 Tubing in 1400°F Flowing NaK

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Figure VII-10. Strain vs Strain-Rate Data Obtained from Biaxial Creep-Rupture Tests on Incoloy-800 Tubing

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Preliminary NaK-vs-helium data of coated tubing of Heat No. 810535 indicate a probable NaK effect. However, it is not possible to clearly state that a NaK effect has occurred until additional helium specimens have been tested. Some decrease in the ductility of samples tested in NaK would not be surprising in that Fe-Cr-Ni alloys have been observed to lose a portion or all of the tertiary stage of creep when exposed to liquid sodium.⁽⁴⁾

Three new heats of Incoloy-800 material were purchased, each involving a particular combination of carbon and titanium.

d. Special Fuel Tests

Twelve S8DR-type fuel elements were tested to determine how much short-term strain the SCB cladding can sustain before a significant change in hydrogen leakrate occurs. Six elements were tested in the biaxial condition and six in the uniaxial. Three of each group were clad in 10-mil Hastelloy-N (Heat 281-4-0143), the remainder in 15-mil Incoloy-800 (Heat HH7531A). The tests were conducted at temperatures of 1300, 1400, and 1500°F. No test was more than 2 hr. The results of the uniaxial and biaxial tests are given in Tables VII-3 and -4, respectively.

The data generally show that in the uniaxial condition no significant difference seems to exist between the two cladding materials in the amount of strain required to cause a significant change in the element leakrate. The required stresses are different due to the difference in material strength. In the biaxial condition no significant difference seems to exist in the strain values at 1300°F. At 1400 and 1500°F the difference is significant with the Incoloy enduring more strain for essentially equivalent changes in leakrate.

During the helium pressurization of the elements in the biaxial tests the leakrates unexpectedly dropped to significantly lower levels than their original rates (see Table VII-3). During the 1500°F Hastelloy-N test run, it was determined that the effect was reversible. Theories for this anomalous behavior include:

- 1) oxidation of the metal tube-wall where exposed by small coating defects, with the helium as the oxygen source;
- 2) the helium having some diluent effect upon the hydrogen-permeation mechanism; and

TABLE VII-3
SUMMARY OF FUEL-ELEMENT BIAXIAL-STRAIN TESTS

Cladding Material	Test Temperature (°F)	Hoop Stress at Start of Change in Leakrate (psi)	Initial Leakrate (cc/hr)	Stabilized H ₂ Leakrate Under Helium Pressure (cc/hr)	Final Leakrate (cc/hr)	Maximum* Residual Diametral Strain (%)
Hastelloy-N	1300	30,300	0.17	0.07	1.04	0.1
	1400	33,000	3.89	0.4	5.80	0.6
	1500	36,750	5.94	0.8	0.92 [†]	7.0
Incoloy-800	1300	13,500	1.27	0.43	3.78	0.4
	1400	18,600	4.93	3.7	5.9	7.0
	1500	18,000	8.03	0.96	1.20 [†]	11.9

*Determined from pre- and post-profilometer traces

[†]Leakrate just before cladding rupture and loss of pressure

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TABLE VII-4
SUMMARY OF FUEL-ELEMENT UNIAXIAL-STRAIN TESTS

Cladding Material	Test Temperature (°F)	Tensile Stress at Start of Change in Leakrate (psi)	Initial Leakrate (cc/hr)	Final Leakrate (cc/hr)	Residual Axial Strain (%)	
					Maximum*	Average*
Hastelloy-N	1300	30,300	0.20	0.51	1.1	0.3
	1400	29,300	0.61	1.88	2.1	1.0
	1500	29,300	1.78	3.58	6.6	1.7
Incoloy-800	1300	14,900	0.46	1.13	0.6	0.5
	1400	12,500	4.0	4.92	1.2	0.8
	1500 [†]	-	-	-	-	-

*Obtained from gage marks at 1-in. intervals.

[†]Data voided due to faulty meter on mass spectrometer.

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- 3) a stress effect on the permeation mechanism, although the anomaly was not seen in the uniaxial tests. Additional tests to provide further insight into the anomaly are not planned at the present.

2. Fuel

a. Burnable Poison Studies

Preceding quarterly reports included the results of eight of ten thermal-endurance tests of fuel elements which included both nickel-foil wrapped fuel and fuel with vapor-deposited gadolinium oxide films. The two remaining elements have completed 5000-hr endurance testing at 1250 and 1400°F; final hydrogen contents correspond to 1.73 and 1.68 H/Zr, respectively. Both elements contained three fuel specimens with Gd_2O_3 films and one foil-wrapped element. The results are similar to those reported for the 2000-hr tests. Film adherence was excellent on the specimens with the normal surface finish (≤ 32 in.); adherence on the rough-finished (> 100 -in.) specimens was also excellent but some of the film was abraded from the peaks of the machining marks during insertion and/or removal from the cladding. In both elements the Ni foil had diffused into the fuel specimen as in previous tests involving foil-wrapped fuel. The ceramic barrier in the 1400°F element was dull green in areas adjacent to the coated fuel; that in the 1250°F element was dark and somewhat glossy. In both elements the ceramic barrier had narrow dark porous-appearing rings opposite the uncoated areas of the fuel rods, as in the 2000-hr tests. The x-ray diffraction patterns showed a higher concentration of the SiO_2 -depleted phase than was found in the 2000-hr tests.⁽²⁾

Four Incoloy-800-clad fuel elements were vibration-tested to the Saturn-V launch conditions. Each element contained a vibration suppressor and a full-length fuel rod with a vapor-deposited gadolinium oxide film. The film was applied in two depositions; the resulting distribution is shown in Figure VII-11. All elements were subjected to S8DR acceptance tests before vibration testing and were radiographed and carefully disassembled after vibration testing. All suppressors were straight and intact. No loose powder was found and the coating was intact on all fuel rods except for several very narrow circumferential scratches and some longitudinal scratches. The circumferential scratches appear to be the result of spinning of the rod during vibration testing. The

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Figure VII-11. Film Thickness vs Length for Full-Length Fuel

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Figure VII-12. Thermal-Endurance Testing of Eu_2O_3 - Sm_2O_3 Films on ZrH_x Fuel, Arrangement of Fuel Specimens in Elements

longitudinal scratches are attributed to abrasion during insertion and/or removal of the fuel from the cladding. The scratches comprise much less than 1% of the area of each fuel rod. Deposits of light-gray material were found on the barriers at points adjacent to the circumferential scratches. This material is presumed to be Gd_2O_3 and fuel but has not yet been analyzed.

Twenty sublength (3.365-in.) fuel specimens were coated with vapor-deposited films of europium and samarium oxides for adherence evaluation of these alternate poisons. These specimens were assembled into four Incoloy-800-clad elements as shown in Figure VII-12. The elements were subjected to S8DR thermal-acceptance testing before thermal-endurance testing at 1400 and 1500°F. The thermal-endurance testing is in progress with an accumulated time at temperature of 1392 hr. The average film thickness is 0.04 mil on each specimen with the amounts of Eu and Sm oxides corresponding to 0.033 and 0.007 mil, respectively.

b. Large Critical Fuel Fabrication

During the quarter a new task was started with the overall objective of providing fuel elements for the large critical assembly for the Reference Zirconium Hydride Reactor. The short-term objectives were to reactivate the arc-melting equipment, conduct a process and equipment shakedown, and qualify a new supplier for extruding fuel rods.

During the reporting period the melting equipment was reactivated and final checkout was done by processing zirconium billets through all operations. Fabrication of six normal uranium-zirconium billets was begun to provide material for qualification of the new extrusion source. The material was processed through double melting and the primary extrusion operation. No problems were encountered.

B. IRRADIATION TESTS

1. NAA-120-4

The NAA-120-4 irradiation experiment was designed to evaluate SNAP fuel performance as a function of temperature, and fuel composition at 0.9 met. -at. %) burnup. The three fuel-test temperatures, 1300, 1500, and 1600°F, were maintained whenever possible.

The in-cell portion of the post-irradiation examination has been completed. Evaluation of the data has not been completed due to present uncertainties in burnup and temperature.

Results of the examination thus far show that eight of the 17 central fuel sleeves suffered severe damage as a result of the test. Only the central fuel sleeves are to be used to evaluate the data. These measurements are shown in Table VII-5; density measurements are shown in Table VII-6.

The diameter and density measurements indicate to some degree a correlation with the design temperatures, and indicate isotropic growth for the most part with the exception of M-1. The cracked and broken condition of M-1 appeared to have influenced the density measurements. Fuel-volume vs fuel-diameter changes are shown in Figure VII-13. Where three data points are shown for a specimen they represent top, middle, and bottom of the fuel sleeve.

The hydrogen analysis showed that most of the fuel specimens changed phases to some degree during operation. Steep H_2 gradients were observed in some cases, mostly in the 1600° specimens, M-1, M-2, and M-3. The hydrogen measurements are shown in Table VII-7.

Neutron radiography and general appearance of the 10 and 20 wt % end sleeves indicated for the most part that they were dehydrided, resulting from the high operating temperatures in those areas. This temperature profile would change when the capsule control gas was changed from helium to argon. It is possible that the center fuel sleeves changed phases several times during the irradiation due to hydrogen moving between hot and cold regions.

Evaluation of the metallography is not complete due to the unusual condition of some of the fuel, which will require metallography on the archive specimens. Table VII-8 shows the post-irradiation room-temperature phases determined from metallography.

2. NAA-121-1

The primary objective of this experiment is to evaluate performance of three S8DR-type fuel elements under simulated S8DR reactor conditions. The elements were designed to approximate power densities of 100, 110, and 120% of S8DR, Ring No. 1. A fourth element was included to evaluate high-temperature

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TABLE VII-5
FUEL DIMENSIONAL MEASUREMENTS

Specimen No.	Average Sleeve Diameter (in.)			Average Sleeve Length (in.)		
	Pre-Irradiation	Post-Irradiation	ΔD	Pre-Irradiation	Post-Irradiation	ΔL
E-1 H ₂ Corrected	0.4092	0.4171 0.4175	0.0079 0.0083	2.251 -	2.302 NC	0.051 -
E-2 H ₂ Corrected	0.4092 -	0.4162 NC	0.0069 -	2.250 -	2.281 NC	0.031 -
E-3 H ₂ Corrected	0.4091	0.4154 0.4155	0.0064 0.0065	2.250 -	2.275 NC	0.025 -
E-4 H ₂ Corrected	0.4093	0.4188 [†] 0.4190	0.0095 0.0097	2.249 -	2.250 [§] NC	0.001 -
E-5 H ₂ Corrected	0.4091 -	0.4164 NC	0.0073 -	2.250 -	2.303 NC	0.053 -
H-1 H ₂ Corrected	0.4011	0.4130 0.4132	0.0119 0.0121	2.250 -	2.305 NC	0.055 -
H-2 H ₂ Corrected	0.4007	0.4172 0.4185	0.0165 0.0178	2.250 -	2.310 [§] NC	0.060 -
H-3 H ₂ Corrected	0.4011 -	0.4146 NC	0.0135 -	2.249 -	2.313 NC	0.064 -
H-4 H ₂ Corrected	0.4011	0.4116 0.4120	0.0105 0.0109	2.248 -	2.273 NC	0.025 -
H-5 H ₂ Corrected	0.4011	0.4139 0.4142	0.0128 0.0131	2.254 -	2.255 NC	0.001 -
M-1 H ₂ Corrected	0.3921	0.4181 0.4205	0.0259 0.0284	2.251	2.350 [§] 2.351	0.099
M-2 H ₂ Corrected	0.3921	0.4178 0.4214	0.0257 0.0293	2.249	2.365 [§] 2.367	0.116
M-3 H ₂ Corrected	0.3921	0.4191 [†] 0.4204	0.0270 0.0283	2.254	2.369 [§] 2.373	0.115
M-4 H ₂ Corrected	0.3921	0.4122 0.4128	0.0201 0.0206	2.251 -	2.334 NC	0.083 -
M-5 H ₂ Corrected	0.3923	0.4126 0.4134	0.0203 0.0211	2.249 -	2.349 NC	0.100 -
V-1 H ₂ Corrected	0.4012	0.4091 0.4129	0.0087 0.0117	2.249	2.300 [§] 2.301	0.051
V-2 H ₂ Corrected	0.4011	0.4181 0.4186	0.0170 0.0175	2.251 -	2.335 [§] NC	0.084 -

Intact fuel sleeves

[†]Deduced from peak cladding measurements[§]Deduced from neutron radiographs

NC = no change.

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TABLE VII-6
FUEL-DENSITY MEASUREMENTS

Specimen No.	Center Fuel Sleeve				
	Pre- ρ_1	Post- ρ_2	$\Delta\rho$	% Change $\Delta\rho/\rho_1$	% Volume Change $\Delta\rho/\rho_2$
E-1 H ₂ Corrected	6.024	5.618 5.607	0.406 0.417	6.73 6.92	7.22 7.44
E-2 H ₂ Corrected	6.048	5.777 NC	0.271 -	4.48 -	4.69 -
E-3 H ₂ Corrected	6.046	5.801 NC	0.245 -	4.05 -	4.22 -
E-4 H ₂ Corrected	6.028	5.725 5.720	0.303 0.308	5.02 5.11	5.29 5.38
E-5 H ₂ Corrected	6.052	5.677 5.674	0.375 0.378	6.18 6.25	6.59 6.66
H-1 H ₂ Corrected	6.047	5.525 5.519	0.522 0.528	8.71 8.73	9.53 9.57
H-2 H ₂ Corrected	6.069	5.376 5.359	0.693 0.710	11.42 11.70	12.89 13.25
H-3 H ₂ Corrected	6.071	5.516 NC	0.555 -	9.14 -	10.06 -
H-4 H ₂ Corrected	6.050	5.592 5.581	0.458 0.469	7.57 7.75	8.19 8.40
H-5 H ₂ Corrected	6.052	5.581 5.577	0.471 0.475	7.77 7.85	8.43 8.52
M-1 H ₂ Corrected	6.070	4.812 4.751	1.258 1.319	20.72 21.73	26.14 27.76
M-2 H ₂ Corrected	6.045	4.953 4.902	1.092 1.143	18.06 18.91	22.04 23.32
M-3 H ₂ Corrected	6.044	4.985 4.812	1.059 1.232	17.52 20.38	21.24 25.60
M-4 H ₂ Corrected	6.025	5.315 5.300	0.710 0.725	11.78 12.03	13.35 13.68
M-5 H ₂ Corrected	6.022	5.192 5.178	0.830 0.844	13.77 14.02	15.97 16.30
V-1 H ₂ Corrected	6.060	5.743 5.682	0.317 0.378	5.23 6.24	5.51 6.65
V-2 H ₂ Corrected	6.047	5.181 5.170	0.866 0.877	14.32 14.50	16.71 16.96

Corrected for hydrogen loss
NC = no change.

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Figure VII-13. NAA-120 Experiment, Fuel-Volume vs Fuel-Diameter Changes

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TABLE VII-7
NAA-120-4 EXPERIMENT HYDROGEN ANALYSIS*

Specimen No.	Center Sleeve				
	Pre-H/Zr	Top No. 1 Post-H/Zr	Center No. 2 Post-H/Zr	Bottom No. 3 Post-H/Zr	Average H/Zr
E-1	1.85	1.77 [†]	1.75 [†]	1.71 [†]	1.74
E-2	1.72	1.74	1.74	1.72	1.73
E-3	1.72	1.72	1.68	1.72	1.71
E-4	1.86	Sample lost	1.82	1.81	1.81
E-5	1.86	1.83	1.86	1.86	1.85
H-1	1.71	1.66	1.66	1.67	1.66
H-2	1.71	1.74	1.58	1.44	1.59
H-3	1.70	1.69	1.70	1.70	1.70
H-4	1.85	1.76	1.74	1.72	1.74
H-5	1.85	1.70	1.76	1.91 [†]	1.79
M-1	1.71	1.58 [†]	1.30 [†]	1.60	1.49
M-2	1.72	1.62	1.59	1.12 [†]	1.44
M-3	1.70	1.24	1.63 [†]	0.90 [†]	1.26
M-4	1.85	1.69	1.69	1.69	1.69
M-5	1.84	1.71	1.72	1.70	1.71
V-1	1.71	1.51 [†]	1.49	1.47 [†]	1.49
V-2	1.71	1.63 [†]	1.61 [†]	1.61 [†]	1.62

*Accuracy ± 0.02 H/Zr

[†]Analyzed together with the support rod.

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TABLE VII-8
ROOM TEMPERATURE METALLOGRAPHIC FUEL EXAMINATION

Specimen No.	Phases			Comments on Center Specimens		
	Top	Center	Bottom	Porosity	Voids	Cracks
E-1 Support	$\epsilon^\dagger$	$\epsilon(\delta + \epsilon)$ ϵ		Yes No	No No	Surface micro only No
E-2 Support		$\delta(\delta + \epsilon)$ $\delta(\delta + \epsilon)$		No No	Yes No	No No
E-3 Support		$\delta + \epsilon$ $\delta + \epsilon$		No No	No No	Surface micro only Surface micro only
E-4 Support		$\epsilon(\delta + \epsilon)$ $\delta + \epsilon$		Yes No	Yes Yes	Yes Micro
E-5 Support		$\epsilon(\delta + \epsilon)$ ϵ		Yes No	No No	Micro No
H-1 Support	$\delta + \epsilon$	$\delta(\delta + \epsilon)^\dagger$ $\delta + \gamma'$		Yes Yes	No No	Surface micro only Surface micro only
H-2 Support		$\delta + \gamma'$ $\delta + \gamma'$	$\delta + \gamma'$ $\delta + \gamma'$	Yes Yes	No No	Yes Surface micro only
H-3 Support		$\delta + \epsilon$ δ		Yes Yes	Yes Yes	Surface micro only Surface micro only
H-4 Support		$\delta + \epsilon$ $\delta + \epsilon$		No No	No No	Micro No
H-5 Support		$\delta + \epsilon$ δ	$\delta + \epsilon$	Yes Yes	No No	Yes Micro
M-1 Support		$\delta/\delta + \alpha$, eutectoid $\delta + \gamma'$		Yes Yes	Yes Yes	Yes Micro
M-2 Support		δ $\delta + \gamma'$	$\delta + \gamma'$ $\delta + \gamma'$	Yes Yes	Yes Yes	Yes Micro
M-3 Support		δ $\delta + \gamma'$		Yes Yes	Yes Yes	Yes Micro
M-4 Support		$\delta + \epsilon$ δ		Yes Yes	No No	Surface micro only Surface micro only
M-5 Support		$\delta + \epsilon$ δ		No No	No No	No No
V-1 Support		$\delta + \gamma'$ $\delta + \gamma'$		Yes Yes	Yes Yes	Yes Micro
V-2 Support		δ δ		Yes Yes	Yes Yes	Yes Yes

*Epsilon + delta one side only (180°)

†Support rod broken off in Hastelloy-X fuel support

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δ -to- β phase change after accumulation of significant fission products at low (<700°F) temperature.

Visual examination of Fuel Elements No. 1, 2, and 3 indicated them to be in excellent condition. Examination of Element No. 4 revealed numerous lumps and bumps over the length. About six hairline cracks were noted at the hot end; a 1-in. longitudinal fracture, oozing NaK, was found at about midplane. Photographs of the elements are shown in Figure VII-14.

Post-irradiation measurements showed no significant change in cladding diameters and lengths in Elements No. 1, 2, and 3. Measurements on No. 4 showed local diameter increases up to a maximum of 0.030 in. and several areas of diameter decrease to a maximum of 0.008 in. The length of Element No. 4 increased 0.053 in.

Gamma-activity scans correlated very closely to the predicted axial power distribution. The scans of Element No. 3 also indicated that the fuel rod had broken. Neutron radiography indicated that Fuel Rods No. 1 and No. 2 were intact. Examination of Element No. 3 confirmed that the fuel rod was broken into two pieces. The break was $\sim 1/4$ in. from midplane of the rod. Neutron radiographic examination of Fuel Element No. 4 showed that the fuel rod was shattered. The worst breakage was noted in the area of high operating temperature. Neutron radiographs further showed that Fuel Rod No. 4 was not in contact with the bulged cladding in some areas. Dimensional measurements are being taken from the neutron radiographs of Element No. 4.

Elements No. 1, 2, and 3 have been disassembled. The fuel rods from all three had a light orange-peel surface condition at the colder operating temperature end, which is probably a result of δ/ϵ phase-boundary operation. The fuel rod from No. 1 was intact and in good condition. Fuel Rod No. 2 was broken into two pieces about 1-1/2 in. from the stepped end (cold region). The break appeared clean and smooth. In all other respects the fuel rod appeared in good condition. The break in Fuel Rod No. 3 was examined in some detail. Composite photographs of these three fuel rods are shown in Figure VII-15. The 0° orientation view of Rod No. 3 is shown in the figure. There was some light longitudinal cracking at the 180° orientation at the break but not nearly as severe as that noted at the 0° orientation. A 3-in. longitudinal hairline crack was observed

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FUEL ELEMENT NO.: 4

3

2

1

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Figure VII-14. NAA-121-1 Elements 1 Through 4, Post-Irradiation

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FUEL ELEMENT NO.: 3

2

1

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Figure VII-15. NAA-121-1 Elements 1 Through 3, Disassembled

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TABLE VII-9

NAA-83 EXPERIMENT ORR-209 IN-PILE CREEP-RUPTURE RESULTS

Specimen No.	Material	Hoop Stress (psi)	Time to Rupture (hr)	Strain-at-Failure (%)	Average Strain Rate (in./in.-hr $\times 10^{-5}$)
A-3	Incoloy-800	10,680	300.0	3.9	13
A-9		10,680	385.9	4.3	11
A-26		9,020	1200		
A-47		9,920	2336		
B-1	Hastelloy-N	7,950	1012.8*	1.1*	1.1
B-3		7,950	842.3	2.0	2.4
C-6		8,750	386.5	0.4	1.1
C-11		8,750	508.7	0.9	1.7

*Did not rupture.

TABLE VII-10

NAA-83 EXPERIMENT EX-PILE CREEP-RUPTURE
TEST STATUS AND RESULTS

Alloy	Number of Samples	Irradiated (I) or Control (C)	Temperature (°F)	Nominal Hoop Stress (psi)	Time to Rupture (hr)	Hours on Test
Incoloy-800	2	I	1300	6,930		2150
	4	I	1300	5,710		2200
	4	I	1300	4,930		1630
Hastelloy-N	2	I	1400	5,480		2200
	2	I	1400	4,670		2200
	3	C	1400	10,500	938	-
					1207	-
					1640	-
	3	C	1400	9,000	1916	1950
	3	C	1400	7,800		1950

at the 180° orientation at the high-temperature end. The pre-diameter measurements on the fuel rods show a machining step diameter change in the same region as the major break on Rod No. 3. This may have caused stress risers which were responsible for the break during irradiation. Fuel Rods No. 1 and No. 2 also had the same precondition in nearly the same region but did not break at that point. This may have been a result of the different power levels and/or number of scrams. Examination showed the ceramic barrier had a rough appearance but was intact and adhered to the cladding throughout the length on all three elements.

The diameter increases in the three fuel rods ranged from 0.0018 to 0.0057 in., except at the break in Fuel Rod No. 3. This area apparently has opened up since disassembly because vernier caliper measurements taken right after disassembly indicated about 0.006 in. where profilometry now shows 0.0075. The cold gap in this region was 0.006 mils; since no cladding distortion was seen, the fuel could not have been any larger than this before disassembly. Length measurements on Fuel Rod No. 1 showed a length increase of 0.065 in. With this measurement and neutron radiography, fuel-rod length increases were deduced as follows: No. 2, 0.066-in.; No. 3, 0.105-in.; and No. 4, 0.007 in.

Sample cutting for metallography, hydrogen analysis, and burnup determination are in progress. Density measurements are being made on the sectioned fuel rods from Elements No. 1, 2, and 3. Disassembly of Element No. 4 was started.

3. NAA-83

The primary objective of the NAA-83 irradiation experiment is to measure effect of neutron irradiation on the high-temperature stress-rupture ductility hydrogen-barrier-coated Hastelloy-N and Incoloy-800 cladding materials.

The status and results obtained to date are given in Tables VII-9 and -10. As planned, four in-pile ruptures of Incoloy-800 specimens occurred during the ORR-209, -211, and -212 irradiations. (The ORR-212 cycle was completed in August and had a total of 2422 hr.) The last two Incoloy specimens (Table VII-9) were in ORR-212 and have not been dimensioned yet. A log-stress vs log-time-to-rupture relation for the in-pile ruptures is shown in Figure VII-16.

Figure VII-16. NAA-83-1 Coated Incoloy-800 Specimens
Ruptured Biaxially In-Pile at 1300°F

It would appear, in comparing these data with the literature data, that the improved adherence of the SCB coating adds to the strength of the cladding composite. Assuming the ductility at lower strain rates remains the same as that for uncoated Incoloy-800, a fuel-element design-margin bonus has been identified. Further testing will provide more definitive data on this point.

C. FUEL-ELEMENT TESTING

1. Preproduction-Element Performance Tests

The endurance testing at 1400°F was terminated early in this quarter for the six preproduction elements. These six fuel elements were clustered in groups of three in two retorts. The fuel-element permeation rates were computed as a ratio of the total H_2 flow in the retort based on individual permeation rates as measured in the prior 10,000-hr endurance test. These six fuel elements completed more than 5000 hr in this extended endurance cycle and accumulated more than 16,000 total hours at 1400°F (see Figure VII-17).

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Figure VII-17. S8DR Preproduction Fuel-Element Performance Data

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Figure VII-18. S8DR Production-Fuel-Element Hydrogen Permeation Compared with "Special Seven" and Preproduction Fuel Elements

2. S8DR Element Performance Tests

The testing of all S8DR normal fuel elements was terminated early in the quarter due to program redirection. The status of the elements at the time of termination was as follows: Phase-I fuel elements, representative of start-of-core production elements, completed more than 12,600 of the scheduled 20,000 hr of endurance testing at 1400°F; Phase-II elements, midcore production, 11,600 hr; and Phase-III, end-of-core production, 9800 hr. Phase-V elements, which are being endurance-tested at 1300°F, completed 10,000 hr.

Figures VII-18, -19, and -20 compare the performance parameters (i.e. hydrogen-permeation rate, hydrogen-barrier parameter, and hydrogen loss) of the S8DR production elements with the S8DR preproduction and the pre-S8DR "Special Seven" elements. These time-increment graphs show that the overall hydrogen-retention performance of the S8DR elements is in general comparable to that of the preproduction elements and far superior to that of the Special Seven.

The hydrogen-retention parameters for the ten Phase-V elements being endurance-tested at 1300°F are plotted in Figure VII-21. The permeation-rate data are normalized to 1400°F, 2 atm pressure for direct comparison with other S8DR tests. Rates to date indicate that the performance during 1300°F endurance is somewhat lower than for 1400°F tests, as shown by comparison of permeation rates and the hydrogen-barrier performance parameter.

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Figure VII-19. S8DR Production-Fuel-Element Hydrogen Barrier Performance Compared with "Special Seven" and Preproduction Fuel Elements

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Figure VII-20. S8DR Production-Fuel-Element Total Hydrogen Loss Compared with "Special Seven" and Preproduction Fuel Elements

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Figure VII-21. S8DR Production-Fuel-Element Performance Parameters of Phase-V Endurance Tests at 1300°F Compared with 1400°F Tests

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VIII. AEROSPACE SAFETY

The revised analysis of the SCA-4B experiments was completed. The agreement between calculation and measurement was found to be good. However, the agreement was primarily the result of a review of the measurements after completion of the program rather than forcing the calculation to fit the measured data. The boron splines, which were used to determine the clean excess of the experimental configuration, were found by weighing to have a variation in boron content. This was not unlikely as the splines were designed to suppress reactivity and not account for it. The gadolinium sleeves which were developed for the program were fabricated to fairly rigid tolerance. In addition, the method of fabrication assured reasonable uniformity of the poison content in the sleeves. The worth of these sleeves was measured and the clean worth of the SCA-4B configuration was determined from the sleeves in a manner similar to that using the boron splines.

The revised calculations were made using the 28-group library recently developed for the Zirconium Hydride Reactor Program. These cross-sections were obtained from recent basic data contained in the AIENDF, KEDAK, and ENDF/B compilations. The absolute normalization of the calculations to experiment was found to be of the order of +\$1.00.

The calculations were performed relative to nickel sleeves about the fuel while the Gd sleeve measurements were relative to water about the fuel. The correction to accommodate the Ni sleeves was \$0.25 reactivity. The resultant match between calculation and experiment is shown in Figure VIII-1.

The analysis of the Gd-poisoned SNAPTRAN temperature coefficient transient experiments is underway.

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Figure VIII-1. Experimental and Calculated
Gd-155 Foil Worth

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