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PROGRESS REPORT
FEBRUARY-APRIL 1970
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AEC Research and Development Report

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The previous report in this series was AI-AEC-12923, "SNAP Reactor Programs Progress Report, November 1969-January 1970."

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I. INTRODUCTION

A. PROGRAM SCOPE AND OBJECTIVES

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

- 1) SNAP 8 developmental reactor (S8DR) test
- 2) 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 endurance testing at 600-kwt, shutdown, and preliminary examination of the fuel.

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 4 π -shielded configuration.

The reference reactor-TE system engineering effort includes preliminary system definition and related application and integration 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

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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 analyses and tests to determine post-reentry and post-impact configurations of reactor systems. Impact disassembly studies will provide analysis and tests to develop analytical models and scaling laws to predict post-impact configuration.

B. SUMMARY OF PROGRESS

At the beginning of this report period the S8DR was in a shutdown condition with the primary-coolant loop drained. On February 6 the reflector was removed from the vacuum vessel and showed no abnormalities. On February 17 the initial cuts into the primary-coolant system were made. On March 17 the core was removed from the Bldg 059 test vault and transported to the Atomics International Hot Laboratory (AIHL), Bldg 020. Visual examination of the core vessel showed that no changes had occurred and it was in excellent condition. There was evidence, however, of a crack or two in the thermocouple-to-bulkhead braze joints. No anomalies were observed during visual examination of the internal reflectors.

Fuel-element removal operations began on March 31, 1970. During this quarter all Ring 8 and 9 elements were removed, plus 24 of the 36 elements of Ring 7. Kollmorgen and stereo examination of these elements was started along with element-length measurements. In general the elements are in much better condition than those in S8ER as was expected.

The major effort undertaken as part of reference ZrH reactor engineering was the study of methods to minimize fuel-element bowing. Primary emphasis

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was placed upon various mechanical stabilization methods, with those designs using fixed inlet ends with intermediate supports being favored. A key factor in these studies is the allowable short-term strain capability of the hydrogen-permeation barrier. Other areas of effort included: (1) core excess reactivity for several element spacings and methods of flow area variation, and (2) development of a statistical sampling technique for establishing required clearance between fuel rod and cladding.

The space station reactor-TE power-system design was updated to incorporate the latest component technology and program requirements. The system uses the reference zirconium hydride reactor coupled to tubular PbTe TE converters, both of which are under active development for the AEC.

As part of the TE-converter development effort the first-generation 5.25-volt (TEM-13G Series) prototype reactor modules were tested and three pump modules continued on test. The first converter module (SN-1) consisting of four tubular modules went on test during the first week of January. Supporting technology efforts were directed toward: (1) test and evaluation of 4.0-in.-length TEM-15 modules, (2) TEM-15 and -13 module examination, (3) mica evaluation, (4) inner-cladding material studies, (5) investigation of improved TE materials, and (6) process development.

A topical report covering the 12,000-hr design-verification testing of S8DR actuators was published. One of these actuators has achieved 20,000 hr of operation. Prototype high-temperature bearing tests are proceeding satisfactorily. Additional component development efforts were directed toward: (1) fabrication of a mockup actuator for the reference ZrH reactor, (2) application of coatings to Nb-1Zr test samples, and (3) completion of the 10,000-hr lead-shield capsule tests.

Process development of integral finned tubes proceeded and a new induction-heating unit for the fuel-element assembly fixture was purchased. New fuel-rod extrusion dimensions and utilization of the copper-clad extrusion technique were verified. Additional materials-evaluation efforts included completion of burnable poison studies and Incoloy-800 investigation. The operation and post-irradiation-examination topical report for the NAA-121-1 tests was published and the NAA-120-4 report was completed and is being reviewed. The NAA-83 and NAR-83 tests are proceeding.

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Impact-disassembly efforts included development of a modeling technique and impact testing of LiH pads, stainless-steel cylindrical shells, and composite models of LiH and stainless steel. Re-entry analyses were directed toward orbital decay and atmospheric re-entry. These studies were concerned with LiH ablation and re-entry body dynamics.

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II. SNAP 8 DEVELOPMENTAL REACTOR

A. SYSTEM DISASSEMBLY

At the beginning of this report period the SNAP 8 Developmental Reactor was in a shutdown condition with the primary coolant drained. The test vault had been entered and disassembly preparations begun. The primary system had then been refilled with clean NaK, which was circulated to flush the system, and drained. Chemical analysis of NaK samples taken both before the initial drain and after the flushing operation indicate that a decontamination factor of 0.3 for the highly soluble fission products may be attributed to the flush. A summary of all NaK-sample analyses is given in Tables II-1A and -1B.

On February 6 the reflectors were removed from the vacuum vessel and stored in the shielded area prepared for them. Electrical resistance and continuity measurements made on reflector components before removal showed there was no significant change from measurements made before operation. Visual examination of the reflectors by television monitors revealed no obvious abnormal conditions.

Shield blocks were removed from the area adjacent to the vacuum vessel to expose the primary piping immediately outside the vessel. On February 17 the initial cuts into the main primary-coolant system were made. These were made at vault floor level in preparation for removal of the NaK piping from the shielded "pipe-chase" area. On February 20 the remote cutting of the primary piping at the bottom of the pipe chase was completed and the pipe sections removed and stored. The primary piping leading to the core was sealed with temporary plugs and an argon purge maintained on the core. Figures II-1 through -5 show the location of these pipe cuts.

The remainder of the month of February was devoted to staging required equipment into the vault for subsequent remote operations, and in performing practice and setup. On March 2 the required dry run for the interfacility transfer of the core was completed. On March 9 the expandable plugs to seal the primary lines inside the vacuum vessel were installed and remote core-removal operations started.

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NaK Sample No.	Date (1969)	Clock Time	Cumulative Time (days)	Power Level (kw)	Total Energy (Mwh)	I ¹³¹				BaLa ¹⁴⁰				Cs ¹³⁷				
						Sample (μci/gm)	Total Coolant (ci)	Calculated Core (ci)	Fraction of Core Inventory	Sample (μci/gm)	Total Coolant (ci)	Calculated Core (ci)	Fraction of Core Inventory	Chemical Separation Sample (μci/gm)	Gamma Spectrum Sample (μci/gm)	Average Total Coolant (ci)	Calculated Core (ci)	Fraction of Core Inventory
4N	Jan 19	13:30	19.56	0	24	-				$8. \times 10^{-4}$	2.59×10^{-4}	2.02×10^3	1.28×10^{-7}	$6. \times 10^{-5}$	-	1.94×10^{-5}	3.42	5.67×10^{-6}
5N	Feb 14	11:30	45.48	600	330	2.5×10^{-2}	8.10×10^{-3}	1.16×10^4	6.98×10^{-7}	4.0×10^{-3}	1.30×10^{-3}	2.05×10^4	6.34×10^{-8}	3.7×10^{-4}	-	1.20×10^{-4}	4.70×10^1	2.55×10^{-6}
6N	Apr 2	22:00	92.92	600	617	6.4×10^{-1}	2.07×10^{-1}	1.13×10^4	1.83×10^{-1}	8.6×10^{-1}	2.79×10^{-1}	2.04×10^4	1.37×10^{-5}	2.1×10^{-3}	1.9×10^{-3}	6.48×10^{-4}	8.80×10^1	7.36×10^{-6}
7N	21	13:00	111.54	990	1015	6.4×10^{-1}	2.07×10^{-1}	2.20×10^4	9.41×10^{-6}	1.4×10^{-1}	4.54×10^{-2}	4.03×10^4	1.13×10^{-6}	3.6×10^{-3}	3.7×10^{-3}	1.18×10^{-3}	1.45×10^2	8.14×10^{-6}
8N	May 7	11:50	127.49	600	1306	1.1×10^0	3.56×10^{-1}	1.95×10^4	1.83×10^{-5}	3.8×10^{-1}	1.23×10^{-1}	4.07×10^4	3.02×10^{-6}	6.2×10^{-3}	5.9×10^{-3}	1.96×10^{-3}	1.86×10^2	1.05×10^{-5}
9N	13	9:00	133.38	600	1392	1.2×10^{-2}	3.89×10^{-3}	1.80×10^4	2.16×10^{-7}	5.3×10^{-2}	1.72×10^{-2}	3.88×10^4	4.43×10^{-7}	9.1×10^{-3}	6.6×10^{-3}	2.54×10^{-3}	1.99×10^2	1.28×10^{-5}
10N	21	17:10	141.71	600	1505	2.39×10^{-1}	7.75×10^{-2}	1.69×10^4	4.58×10^{-6}	2.64×10^{-1}	8.55×10^{-2}	3.70×10^4	2.31×10^{-6}	5.5×10^{-3}	7.4×10^{-3}	2.09×10^{-3}	2.15×10^2	9.73×10^{-6}
11N	28	20:00	148.83	600	1630	2.0×10^{-1}	6.48×10^{-2}	1.64×10^4	3.95×10^{-6}	5.0×10^{-2}	1.62×10^{-2}	3.60×10^4	4.50×10^{-7}	1.1×10^{-2}	1.68×10^{-2}	4.51×10^{-3}	2.32×10^2	1.95×10^{-5}
12N	June 4	17:40	155.74	0	1674	7.3×10^{-2}	2.36×10^{-2}	1.27×10^4	1.86×10^{-6}	1.1×10^{-2}	3.56×10^{-3}	3.04×10^4	1.17×10^{-7}	1.92×10^{-2}	1.85×10^{-2}	6.09×10^{-3}	2.39×10^2	2.54×10^{-5}
13N	12	18:00	163.75	600	1781	8.3×10^{-1}	2.69×10^{-1}	1.39×10^4	1.93×10^{-5}	1.6×10^0	5.18×10^{-1}	3.10×10^4	1.67×10^{-5}	1.8×10^{-2}	2.8×10^{-2}	7.45×10^{-3}	2.54×10^2	2.93×10^{-5}
14N	18	14:30	169.60	600	1863	6.6×10^{-3}	2.14×10^{-3}	1.46×10^4	1.47×10^{-1}	1.6×10^{-3}	5.18×10^{-4}	3.17×10^4	1.63×10^{-8}	-	3.6×10^{-2}	1.17×10^{-2}	2.66×10^2	4.40×10^{-5}
15N	25	10:15	176.43	600	1958	1.3×10^{-3}	4.21×10^{-4}	1.46×10^4	2.88×10^{-8}	6.1×10^{-4}	1.98×10^{-4}	3.16×10^4	6.27×10^{-9}	-	7.8×10^{-2}	2.53×10^{-2}	2.79×10^2	9.07×10^{-5}
16N	July 4	14:50	185.62	600	2088	6.6×10^{-2}	2.14×10^{-2}	1.53×10^4	1.40×10^{-6}	2.3×10^{-2}	7.45×10^{-3}	3.25×10^4	2.29×10^{-7}	-	1.46×10^{-1}	4.73×10^{-2}	2.98×10^2	1.59×10^{-4}
17N	16	12:22	197.51	600	2242	1.6×10^{-1}	5.19×10^{-2}	1.48×10^4	3.50×10^{-6}	6.8×10^{-3}	2.20×10^{-3}	3.18×10^4	6.93×10^{-8}	-	2.2×10^{-1}	7.13×10^{-2}	3.20×10^2	2.23×10^{-4}
18N	24	17:45	205.74	600	2354	-				-				1.7×10^{-1}	1.6×10^{-1}	5.35×10^{-2}	3.36×10^2	1.59×10^{-4}
19N	31	14:48	212.62	600	2460	1.54×10^{-1}	5.00×10^{-2}	1.56×10^4	3.20×10^{-6}	5.4×10^{-2}	1.75×10^{-2}	3.29×10^4	5.32×10^{-7}	2.2×10^{-1}	1.32×10^{-1}	5.70×10^{-2}	3.51×10^2	1.62×10^{-4}
20N	Aug 7	15:13	219.64	600	2563	8.8×10^{-1}	2.85×10^{-1}	1.57×10^4	1.82×10^{-5}	1.23×10^{-1}	3.98×10^{-2}	3.32×10^4	1.20×10^{-6}	2.0×10^{-1}	1.68×10^{-1}	5.95×10^{-2}	3.66×10^2	1.63×10^{-4}
21N	21	11:03	233.46	600	2758	2.7×10^{-2}	8.75×10^{-3}	1.58×10^4	5.54×10^{-7}	5.7×10^{-3}	1.85×10^{-3}	3.35×10^4	5.52×10^{-8}	2.0×10^{-1}	2.04×10^{-1}	6.54×10^{-2}	3.93×10^2	1.66×10^{-4}
22N	Sept 4	11:09	247.46	600	2965	2.16×10^{-1}	7.00×10^{-2}	1.58×10^4	4.42×10^{-6}	1.63×10^{-2}	5.28×10^{-3}	3.36×10^4	1.57×10^{-7}	2.13×10^{-1}	2.16×10^{-1}	6.95×10^{-2}	4.22×10^2	1.65×10^{-4}
23N	11	10:35	254.44	600	3066	1.8×10^{-2}	5.83×10^{-3}	1.58×10^4	3.69×10^{-7}	8.5×10^{-4}	2.75×10^{-4}	3.37×10^4	8.16×10^{-9}	2.54×10^{-1}	2.65×10^{-1}	8.41×10^{-2}	4.37×10^2	1.92×10^{-4}
24N	25	10:53	268.45	600	3267	3.5×10^{-2}	1.13×10^{-2}	1.58×10^4	7.18×10^{-7}	2.9×10^{-3}	9.40×10^{-3}	3.37×10^4	2.79×10^{-7}	2.48×10^{-1}	2.58×10^{-1}	8.20×10^{-2}	4.66×10^2	1.76×10^{-4}
25N	Oct 9	10:40	282.44	600	3461	1.5×10^{-2}	4.86×10^{-3}	1.55×10^4	3.14×10^{-7}	9.7×10^{-3}	3.14×10^{-3}	3.31×10^4	9.50×10^{-8}	2.47×10^{-1}	2.88×10^{-1}	8.67×10^{-2}	4.94×10^2	1.76×10^{-4}
26N	23	9:00	296.37	600	3664	3.7×10^{-2}	1.20×10^{-2}	1.57×10^4	7.64×10^{-7}	5.4×10^{-3}	1.75×10^{-3}	3.35×10^4	5.23×10^{-8}	2.57×10^{-1}	3.32×10^{-1}	9.54×10^{-2}	5.23×10^2	1.82×10^{-4}
27N	Nov 7	9:45	311.41	600	3881	6.6×10^{-1}	2.14×10^{-1}	1.58×10^4	1.35×10^{-5}	1.5×10^{-1}	4.86×10^{-2}	3.36×10^4	1.45×10^{-6}	3.09×10^{-1}	3.56×10^{-1}	1.08×10^{-1}	5.54×10^2	1.95×10^{-4}
28N	25	10:37	329.44	300	4097	4.21×10^0	1.36×10^0	1.29×10^4	1.06×10^{-4}	7.6×10^{-1}	2.46×10^{-1}	2.94×10^4	8.37×10^{-6}	5.05×10^{-1}	4.74×10^{-1}	1.58×10^{-1}	5.84×10^2	2.71×10^{-4}
29N	Dec 5	10:25	339.43	600	4199	2.09×10^0	6.76×10^{-1}	1.23×10^4	5.50×10^{-5}	5.8×10^{-1}	1.88×10^{-1}	2.74×10^4	6.85×10^{-6}	3.57×10^{-1}	4.19×10^{-1}	1.26×10^{-1}	5.98×10^2	2.11×10^{-4}
30N	11	10:54	345.45	600	4284	1.53×10^{-1}	4.96×10^{-2}	1.37×10^4	3.62×10^{-6}	2.94×10^{-2}	9.52×10^{-2}	2.92×10^4	3.26×10^{-6}	4.76×10^{-1}	4.91×10^{-1}	1.57×10^{-1}	6.11×10^2	2.57×10^{-4}
31N	16	12:54	*	0	4345	8.92×10^{-1}	2.89×10^{-1}	1.44×10^4	2.00×10^{-6}	2.22×10^{-2}	7.20×10^{-2}	3.01×10^4	2.39×10^{-6}	4.56×10^{-1}	5.14×10^{-1}	1.57×10^{-1}	6.19×10^2	2.54×10^{-4}
32N	18	13:02	*	0	4345	6.44×10^{-1}	2.08×10^{-1}	1.44×10^4	1.45×10^{-6}	1.40×10^{-1}	4.54×10^{-2}	3.01×10^4	1.51×10^{-6}	5.5×10^{-1}	4.03×10^{-1}	1.54×10^{-1}	6.19×10^2	2.50×10^{-4}
Coolant drained and system refilled																		
33N	Jan 13 1970	10:30	*	0	4345	3.42×10^{-1}	1.11×10^{-1}	1.44×10^4	7.70×10^{-7}	4.4×10^{-2}	1.43×10^{-2}	3.01×10^4	4.7×10^{-7}	-	1.45×10^{-1}	4.70×10^{-2}	6.19×10^2	7.60×10^{-5}

*Used date of shutdown

Dec 15 15:00 349.63

Table II-1A. SNAP 8 Developmental Reactor NaK-Sample Analysis

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NaK Sample No.	Date (1969)	Clock Time	Cumulative Time (days)	Power Level (kwt)	Total Energy (Mwh)	Fission-Product Activity (μ ci/gm)				Corrosion-Product Activity (μ ci/gm)					NaK and Impurities Activity (μ ci/gm)				
						Sb ¹²⁵	TeI ¹³²	Zr ⁹⁵	Nb ⁹⁵	Co ⁶⁰	Cr ⁵¹	Fe ⁵⁹	Mn ⁵⁴	Sb ¹²⁴	Cs ¹³⁴	K ⁴²	Na ²² (n, 2n)	Na ²⁴	Rb ⁸⁶
4N	Jan 19	13:30	19.56	0	24	-	M	D	D	3.0×10^{-4}	3.0×10^{-3}	D	8.0×10^{-4}	-	D	M	9.0×10^{-5}	M	9.0×10^{-2}
5N	Feb 14	11:30	45.58	600	330	-	9.0×10^{-2}	D	D	1.6×10^{-4}	6.5×10^{-3}	D	4.1×10^{-4}	M	D	M	8.4×10^{-4}	M	5.0×10^{-1}
6N	Apr 2	22:00	92.92	600	617	D	M	D	D	9.7×10^{-5}	8.4×10^{-3}	D	4.5×10^{-3}	4.0×10^{-4}	D	-	1.7×10^{-3}	$2.1 \times 10^{+3}$	M
7N	Apr 21	13:00	111.54	990	1015	-	-	-	-	-	-	-	-	-	-	-	-	-	-
8N	May 7	11:50	127.49	600	1306	D	M	D	D	D	1.1×10^{-2}	D	1.8×10^{-3}	1.2×10^{-3}	D	M	3.6×10^{-3}	M	M
9N	13	9:00	133.38	600	1392	-	-	-	-	-	-	-	-	-	-	-	-	-	-
10N	21	17:10	141.71	600	1505	-	-	-	-	-	-	-	-	-	-	-	-	-	-
11N	28	20:00	148.83	600	1630	-	-	-	-	-	-	-	-	-	-	-	-	-	-
12N	June 4	17:40	155.74	0	1674	D	M	D	D	2.6×10^{-4}	$<1.0 \times 10^{-2}$	D	9.0×10^{-4}	3.5×10^{-4}	9.0×10^{-4}	M	5.8×10^{-3}	M	7.8×10^{-1}
13N	12	18:00	163.75	600	1781	-	-	-	-	-	-	-	-	-	-	-	-	-	-
14N	18	14:30	169.60	600	1863	-	-	-	-	-	-	-	-	-	-	-	-	-	-
15N	25	10:15	176.43	600	1958	-	-	-	-	-	-	-	-	-	-	-	-	-	-
16N	July 4	14:50	185.62	600	2088	D	M	D	5.2×10^{-2}	5.0×10^{-5}	D	D	6.0×10^{-4}	1.0×10^{-4}	4.7×10^{-3}	M	7.6×10^{-3}	M	1.06×10^0
17N	16	12:22	197.51	600	2242	-	-	-	-	-	-	-	-	-	-	-	-	-	-
18N	24	17:45	205.74	600	2354	-	-	-	-	-	-	-	-	-	-	-	-	-	-
19N	31	14:48	212.62	600	2460	-	-	-	-	-	-	-	-	-	-	-	-	-	-
20N	Aug 7	15:13	219.64	600	2563	D	M	D	2.2×10^{-1}	$<3.5 \times 10^{-4}$	D	D	$<4.0 \times 10^{-4}$	$<3.3 \times 10^{-4}$	7.5×10^{-3}	M	9.0×10^{-3}	M	1.17×10^0
21N	21	11:03	233.46	600	2758	-	-	-	-	-	-	-	-	-	-	-	-	-	-
22N	Sept 4	11:09	247.46	600	2965	D	M	D	8.5×10^{-2}	5.0×10^{-5}	D	D	2.0×10^{-4}	9.1×10^{-3}	9.1×10^{-3}	M	9.5×10^{-3}	M	1.24×10^0
23N	11	10:35	254.44	600	3066	-	-	-	-	-	-	-	-	-	-	-	-	-	-
24N	25	10:53	268.45	600	3267	-	-	-	-	-	-	-	-	-	-	-	-	-	-
25N	Oct 9	10:40	282.44	600	3461	D	M	D	1.72×10^{-1}	$<1.0 \times 10^{-4}$	D	D	$<2.0 \times 10^{-4}$	$<3.0 \times 10^{-4}$	1.17×10^{-2}	M	1.26×10^{-2}	M	1.17×10^0
26N	23	9:00	296.37	600	3664	-	-	-	-	-	-	-	-	-	-	-	-	-	-
27N	Nov 7	9:45	311.41	600	3881	$<3.1 \times 10^{-3}$	M	D	7.54×10^{-1}	$<5.0 \times 10^{-5}$	D	D	$<5.0 \times 10^{-4}$	$<6.0 \times 10^{-4}$	1.71×10^{-2}	M	1.51×10^{-2}	$1.55 \times 10^{+3}$	1.26×10^0
28N	25	10:37	329.44	300	4097	-	-	-	-	-	-	-	-	-	-	-	-	-	-
29N	Dec 5	10:25	339.43	600	4199	-	-	-	-	-	-	-	-	-	-	-	-	-	-
30N	11	10:54	345.45	600	4284	$<4.0 \times 10^{-4}$	M	D	1.00×10^{-1}	$<4.0 \times 10^{-5}$	D	D	$<4.0 \times 10^{-5}$	$<4.0 \times 10^{-5}$	2.62×10^{-2}	M	1.47×10^{-2}	M	1.31×10^0
31N	16	12:54	*	0	4345	-	-	-	-	-	-	-	-	-	-	-	-	-	-
32N	Dec 18	13:02	*	0	4345	$<3.0 \times 10^{-3}$	M	D	7.68×10^{-1}	$<1.0 \times 10^{-4}$	D	D	$<2.8 \times 10^{-4}$	$<2.5 \times 10^{-4}$	2.16×10^{-2}	M	1.43×10^{-2}	M	1.20×10^0
Coolant drained and system refilled																			
33N	Jan 13, '70	10:30	*	0	4345	$<1.0 \times 10^{-3}$	M	D	2.01×10^{-1}	$<1.0 \times 10^{-4}$	D	D	$<1.7 \times 10^{-4}$	$<1.5 \times 10^{-4}$	8.7×10^{-3}	M	3.9×10^{-3}	M	3.5×10^{-1}

*Used date of shutdown

Dec 15 15:00 349.63

D = not detected

M = detected but not measured

Table II-1B. SNAP 8 Developmental Reactor NaK-Sample Analysis

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Figure II-1. Reactor-Inlet NaK Pipes No. 105 and 106

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Figure II-3. Primary-System NaK Drain Pipe No. 471

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Figure II-4. Primary-System NaK Drain Pipe No. 472

Figure II-5. Primary-System NaK Drain Pipe No. 473

On March 13 the core and core-support structure were removed from the vacuum vessel and placed in a drip-pan on the vault floor. The reactor-vessel inlet and outlet lines were severed and the core vessel placed in the core-transfer cask. On March 17 the core was removed from the Bldg 059 test vault, as shown in Figure II-6, and delivered to the AIHL (Bldg 020). Figure 7 shows the core-transfer cask en route to the AIHL. Radiation measurements were made of the cask containing the core during this transfer. The results are shown in Table II-2.

TABLE II-2
CORE-CASK RADIATION MEASUREMENTS

Location	Gamma Reading (mrem/hr)	Neutron Reading (mrem/hr)	Total Doserate (mrem/hr)
Top			
(maximum)	20	15	35
(average)	15	12	27
Side			
(maximum)	80	48	128
(average)	40	30	70
Bottom*			
(maximum)	450	-	450

*Doserate measurements on cask bottom limited to gamma only, since the neutron detector could not be placed in position under the cask.

These readings compare well with the calculations made in evaluating the shield design. These design calculations, which bracket the actual operating time of 6688 hr, are shown in Table II-3 for comparison.

Throughout the disassembly period, from the time the core was drained until the plenum thermocouples (TC) were severed, the upper-plenum temperatures were monitored as an indication of core temperature. After draining both systems the average plenum temperature fell from $\sim 190^{\circ}\text{F}$ on January 23 to $\sim 183^{\circ}\text{F}$ on February 2. On this later date the helium backfill gas of the vacuum vessel was exchanged to nitrogen. The plenum temperatures rose to $\sim 230^{\circ}\text{F}$

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Figure II-6. Core Transfer-Cask Removal

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Figure II-7. Core Transfer Cask en Route to the AI Hot Laboratory

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TABLE II-3
CALCULATED CORE-CASK RADIATION

Location	Gamma Reading (mrem/hr)	Neutron Reading (mrem/hr)	Total Doserate (mrem/hr)
Top			
(6000-hr operation)	37	46	83
(8000-hr operation)	44	56	100
Side			
(6000-hr operation)	30	27	57
(8000-hr operation)	36	33	69
Bottom			
(6000-hr operation)	94	45	139
(8000-hr operation)	110	54	164

after the exchange and fell to $\sim 201^{\circ}\text{F}$ by March 10. On this date the TC's were severed.

Subsequent operations have resulted in the disassembly and packaging of the remainder of the highly radioactive piping and components removed from the vacuum vessel. By the end of this report period all components removed from the vacuum vessel (with the exception of the reflectors and some piping samples) had been shipped out of the facility for disposal, and decontamination of the vault and equipment was started.

B. S8DR EXAMINATION

1. Preparation Activities

The design, fabrication, and checkout of the equipment/fixtures required for the screening-examination phase of the S8DR post-test examination were completed during the reporting period.

A program was set up to develop a neutron-radiography technique for measuring (non-destructively) (1) the hydrogen-content/axial-distribution in the S8DR fuel elements and (2) the post-test fuel-rod dimensions (both diameters and lengths). The program met limited success; fuel-rod dimensional measurements appear obtainable using the n-ray technique, but attempts to measure hydrogen-content/axial-distribution were unsuccessful.

A computer-oriented data-handling/processing system was devised for treating the data obtained during the S8DR screening examination. As of the close of the report period the data-reduction code was being checked out.

2. S8DR Screening Examination

a. Core-Disassembly Operations and Observations

The S8DR core was received at the Atomics International Hot Laboratory (AIHL) on March 17, 1970. The core was removed from the shipping cask (after a nitrogen atmosphere had been established in the cell) and placed on a stainless-steel tray for visual examination of the inlet-line/lower-plenum region. The exam revealed NaK-oxide deposits and some small quantity of liquid NaK dripping from the core region into the lower plenum. The core was moved to the core-disassembly fixture and mounted to the fixture turntable (see Figure II-8). Visual examination of the core vessel exterior revealed no anomalies, with the surface of the vessel generally exhibiting a bright lustrous appearance (see Figure II-9). Core barrel diameters were measured at 4-in. increments along the length of the vessel with a pi tape. No significant changes were found.

The top-cover/bulkhead assembly was removed from the vessel assembly, using the pipe-cutter rig mounted on the core-disassembly fixture. A visual examination of the interior of the top-cover/bulkhead assembly revealed the

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Figure II-8. Disassembly Fixture Used in S8DR
Screening Examination

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Figure II-9. Core Vessel
Appearance Upon Receipt
at Atomics International
Hot Laboratory

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bulkhead to be in excellent condition. All surfaces appeared to be clean, with the exception of some NaK oxide observed in the region where the thermocouples are bundled together as they pass from the core-outlet plenum upward toward the bulkhead (see Figure II-10). Evidence of a possible crack in one or two (adjacent) thermocouple-to-bulkhead braze joints was seen after cleaning, when NaK-oxide deposits were observed on the underside of the lower bulkhead (see Figure II-11). The deposits may have wept out of such crack(s) after the assembly had been cleaned and left to sit over the weekend. After wiping away these deposits no further weeping occurred. After removal of the top-cover/bulkhead assembly the downstream side of the upper gridplate was visually examined (see Figure II-12). The plate was very clean except for some NaK-oxide balls resting on the plate in three spots. These probably fell onto the plate from the top-cover/bulkhead assembly when it was removed. Coolant-exit ports and fuel-element centering holes appeared clean and free of deposits/obstructions.

Visual examination of the internal reflectors exposed to view by removal of the core barrel revealed the reflector elements to be clean and free of deposits (see Figure II-13). No anomalies were evident. The outside surfaces of several of the Ring-9 fuel elements were covered in some locations with a thin silvery film or coating, appearing principally at the element midplane to outlet region. The coating was flaked and peeled off in several places, and was easily crinkled and removed by light scraping with a knife. A sample of the film was taken for analysis from Element No. 205, core position 9-34 (see Figure II-14).

Generally the top pins on the fuel elements (appearing upon removal of the gridplate) appeared quite clean with only minimal NaK/NaK-oxide deposits being visible. Figure II-15 compares the conditions of S8DR and S8ER before cleaning. The element identification numbers stamped on the flats of the upper end-cap locating pin were clearly visible and easily readable through the Kollmorgen on many of the elements. The internal reflectors generally appeared clean, free of deposits, and in excellent condition upon removal from the core. As shown in Figure II-16, the upstream face of the gridplate was found to be clean, and only minimal residual NaK-oxide deposits could be seen in the coolant flow passages and fuel-element positioning ports.

BEFORE CLEANING

DR-16

BEFORE CLEANING

DR-13

DR-8

AFTER CLEANING

DR-10

BEFORE CLEANING

DR-15

AFTER CLEANING

DR-17

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Figure II-10. S8DR Top-Cover/Bulkhead Assembly After Removal

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DR-19

OXIDE DEPOSITS OBSERVED TO HAVE
WEPTED OUT OF POSSIBLE CRACK(S)
OVER THE WEEKEND OF MARCH 21
AND 22, 1970

DR-20

SAME AREA AFTER CLEANUP AND
WAITING PERIOD OF ABOUT 20 HOURS
(ON MORNING OF MARCH 24, 1970).
NO RECURRENCE OF DEPOSITS WAS
OBSERVED.

70-A1-034-5

Figure II-11. Evidence of Possible Crack on Plenum Side of S8DR Brazed Bulkhead

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S8DR

DR-9

S8ER

S8C14

70-A1-034-2

Figure II-12. Downstream Face of Upper Gridplate Before Removal

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S8DR CORE

DR21

S8ER CORE

S8C5

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Figure II-13. Appearance of Core in As-Received Condition at
Atoms International Hot Laboratory

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Sample Peeled From Circled
Region Before Removal of
Ring-9 Elements

UNCL

DR-31
70-A14-034-10

Figure II-14. Fuel Element From Which Film Sample was Taken

S8DR

UNCL

DR-29

S8ER

S8C24

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Figure II-15. Top of Fuel Elements After Removal of Gridplate
and Before Cleaning

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S8DR

DR-30

S8ER

S8C29

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Figure II-16. Upstream Face of Upper Gridplate After Removal and Before Cleaning

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b. Fuel-Element Removal Operations and Observations

Fuel-element removal operations began on March 31, 1970. Before removal, each fuel element is scribe-marked on the blind-end cap at the outlet end to permanently identify orientation to the core center. The elements are cleaned immediately after removal by soaking in a Dowanol bath followed by brush cleaning and a final rinse in isopropyl alcohol. The cleaned elements are then visually examined at the Kollmorgen station, with selected elements being further examined (visually) in the stereo station. Upon completion of visual examination the elements are then subjected to a series of measurements (e.g. length, diameter, bow, gamma scan on selected elements, etc.) in accordance with the established AIHL examination plan.

During this quarter, all elements in Core Rings 8 and 9 were removed, plus 24 of the 36 elements in Ring 7. The locations of the elements removed are shown in Figure II-17. Completion of Kollmorgen visual examination characteristically lags about 24 elements (2 batches) behind the element-removal process. Stereo visual examination (on selected elements) lags somewhat behind the Kollmorgen examination progress. As of the close of the report period, Kollmorgen visual examination had been completed on all Ring-8 and -9 elements, with stereo examination being completed on all Ring-9 elements selected and about one-half of the elements selected from Ring 8. Figure II-18 shows the cracked-intact element status based on the available results from Kollmorgen and stereo examination as of the close of the report period. Element-length measurements had been completed on Ring-9 elements from core positions 9-02 through 9-42 (36 elements in all). The overall status of the S8DR screening examination as of the close of the report period is summarized in Table II-4.

c. Data and Evaluation

Generally the S8DR core components examined to date appear to be much cleaner than the corresponding units in S8ER. This may be seen in Figures II-12, -13, -15, and -16.

The film observed on the Ring-9 elements seems to be most extensive on elements near the "corners" of the hexagonal-core array (e.g. elements in core

9 AUI 006 34B

Figure II-17. Fuel-Element Removal Progress Through April 10, 1970

9-AUI-006-348

- FUEL ELEMENT REMOVED; VISUAL EXAM REVEALED NO CRACKS IN CLADDING
- FUEL ELEMENT REMOVED; CRACK(S) OBSERVED IN CLADDING

Figure II-18. Intact and Cracked Cladding Elements (Kollmorgen Examination) as of Close of Report Period

TABLE II-4
S8DR SCREENING-EXAMINATION STATUS SUMMARY
THROUGH APRIL 10, 1970

Number of Elements Which Have Been Processed Through and on Which Data Is Available	Ring No. and Total Number of Elements in Ring									Totals
	9 (42)	8 (42)	7 (36)	6 (30)	5 (24)	4 (18)	3 (12)	2 (6)	1 (1)	9 Rings 211 Elements
Visual Exam (Kollmorgen)	42*	42	← None As Yet →							84
	42†	42								84
Profilometer Scan	← None As Yet →									
Gamma Scan	← None As Yet →									
Neutron Radiography	← None As Yet →									

*Number of elements processed during reporting period.

†Total number of elements processed to date

Cracked-Element Data Summary to Date (See Figure II-18)

Fuel-Element Condition	Ring Number									Totals
	9	8	7	6	5	4	3	2	1	
Cracked	1	5								6
Intact	41	37	No data see above							78
Possibly Cracked	0	0								0

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positions 9-24, 9-26, 9-10, etc.) and less prevalent on those located on the "flats" of the core array (e. g. elements in core positions such as 9-4, 9-5, 9-6, 9-7, etc). The film-deposition pattern on the Ring-9 elements appears to favor the elements' outlet region although the film has been observed on the inlet region surfaces of some elements. The film deposits on the Ring-8 elements (qualitatively) appear to be less extensive (both in total amount deposited and in the area of elements covered) than those on the Ring 9. The deposition pattern on Ring 8 continues to favor, but by no means is restricted to, the core midplane-to-outlet region of the elements.

The results of the analyses on the film sample taken from Fuel Element 205 (in core position 9-34) were as follows:

1) Elemental constituents (based on X-ray-fluorescence analysis)

Iron	~50%
Nickel	~35%
Chromium	~10%
Potassium	~1%

Other constituents present in small amounts were molybdenum, manganese, sulfur, phosphorus, silicon, and possibly aluminum.

- 2) The sample was strongly magnetic, with a polycrystalline, randomly oriented, face-centered cubic structure consistent with a solid solution of iron and nickel.
- 3) The thickness of the film was estimated by computing the thickness based on the sample dimensions, weight, and an assumed (sample) density of 7 gm/cc. The resulting thickness was calculated to be 0.2 mil.
- 4) Pinholes were observed in the film sample when viewed with a light source behind the sample.

The archive cladding-tube sample from Element 205 was analyzed and the Fe content of the OD surface found to be less than 1%.

The thin metallic film observed on several of the fuel elements seems to be the result of a mass-transfer deposition phenomenon judging by the high (~50%) Fe content of the film. Possible sources of the Fe are the stainless-

steel components of the S8DR primary-coolant loop, the stainless-steel-clad internal reflector elements, and the core-vessel assembly.

The data available on observed cladding cracks in the Ring-8 elements are shown in Figures II-19 through -22 inclusive, with the currently observed core cracked-intact element pattern shown in Figure II-18. The data, which are preliminary, indicate that:

- 1) No strong correlation exists (as yet) between crack location and the orientation of the element to core center (Figure II-19);
- 2) The cracks observed occurred in the Z/L region between 0.5 and 0.8 (Figure II-20);
- 3) The cracks are relatively short, all being less than 3 in. in length (Figure II-21); and
- 4) The cracks appear to be favoring the region closer to the "hot-line" location (i. e. the circumferential position on the element at the closest approach to adjacent fuel elements) rather than the coolant subchannel cusp region (Figure II-22).

The crack in Element 334 located in core position 9-34 appears to be somewhat different than the cracks observed in the Ring-8 elements to date. Data on this crack are not included in Figures II-19 through -22). The Element-334 crack occurred within an inch of the inlet end of the element, was short (1.0-in. long), and was located (circumferentially) toward core center.

All cracks observed to date have been hairline longitudinal cracks. The cracks are very thin and do not appear to have opened up (see Figure II-23).

Of the 36 Ring-9 elements measured for length during the reporting period, none was observed to have increased in length. The post-test length was shorter than the pre-test, with the maximum-length change being about 0.09% (~15 mils), and the bulk of the data falling in a range about 1/2 of this maximum.

Figure II-19. Orientation of Cladding Cracks in Ring-8 Elements

Figure II-20. Location of Cladding Cracks in Ring-8 Elements

Figure II-21. Length of Cladding Cracks in Ring-8 Elements

Figure II-22. Element-to-Element Orientation of Ring-8 Cladding Cracks

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Figure II-23. Typical Cladding Cracks Observed in Ring-8 Elements

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III. REFERENCE ZIRCONIUM HYDRIDE REACTOR ENGINEERING

A. INTRODUCTION AND SUMMARY

The major emphasis during the period was the study of methods to eliminate the fuel-element bowing problem which characterized S8DR performance. Alternate support methods to achieve the desired mechanical stabilization were studied in detail. A significant consideration in assessing the adequacy of the structural margins was found to be the allowable short-term strain capability of the hydrogen-permeation barrier. A limited amount of data was available from which interim allowable values were established. A test program to provide more-refined glass-property data was begun. The support designs which currently are favored involve fixing the inlet ends of the fuel elements and providing intermediate support near the outlet end. The outlet end of the element beyond the intermediate support is allowed to bow to compensate for channel-to-channel temperature variation. In this scheme the flowrate in the channel tends to be compensated by the element deflections so as to minimize the channel-to-channel temperature variations.

Other areas studied included core excess reactivity as a function of element spacing and the method used to attain the several zones of channel area variation. Hydraulic and mechanical-design studies had shown a core spacing of uniform element pitch with element outside-diameters varying in several zones to be a desirable means of flow-matching to the core power. The nuclear studies showed that if the cladding thickness was varied to achieve the variation in fuel-element diameter, a significant reduction in excess reactivity would be incurred in comparison to varying the fuel-material diameter.

A statistical sampling technique was developed for establishing the required radial clearance between the fuel rod and cladding tube. The uncertainties due to manufacturing tolerances, flowrates, and the fuel-growth correlation were identified. An investigation of the sensitivity of the required gap dimension to the fuel-element diameter showed that the indicated reduction in gap for a reduction in element diameter was essentially insignificant.

B. STRESS ANALYSIS

1. Fuel-Element Material Properties

Guidelines were established to define the material property limits to be used in conjunction with the stress analysis. Consideration was given to the intent of the ASME boiler code and to hydrogen permeation through the SCB coating and Incoloy-800 cladding.

The induced stresses are almost completely thermally induced. These are classed as secondary stresses by the boiler code and are limited by twice the material yield strength or the ultimate strength when based on a linear analysis. Creep relaxation and/or collapse must also be considered as well as fatigue.

The stress limitation on the SCB coating is that which causes excessive hydrogen permeation. The SCB-coating failure mode is assumed to be caused by the maximum principal strain. Limited test data were used to establish a preliminary estimate of allowable strain before gross leakage occurs. These data were obtained at 1300, 1400, and 1500°F for both the uniaxial- and biaxial-stress state. The minimum mechanically induced strain where leakage occurred was in the biaxial test of a pressurized tube which failed at 13500-psi hoop stress at 1300°F. The principal mechanical strain for this condition is

$$E = \frac{\sigma_H}{E} - \mu \frac{\sigma_A}{E} = \left(1 - \frac{\mu}{2}\right) \frac{\sigma_H}{E} = 0.000515 \text{ in./in.}$$

An extrapolation of this SCB data combined with the boiler-code criteria for the cladding provides design envelope (Figure III-1). This design envelope defines the allowable stress based on a linear/straight-line stress-strain curve for use in a linear analysis.

2. Fuel-Element Stresses

The stress-analysis effort was to determine the core configuration which minimized stresses. Several methods and locations of support were considered. A partial summary of the concepts is shown in Figure III-2. Each of these methods has advantages and disadvantages when exposed to the fuel-element total environment. For example the element pinned at each end has excessive deflections while the element fixed at the hot-end has excessive stresses.

Figure III-1. Preliminary Envelope of Allowable Design Stress, Incoloy-800

Figure III-2. Core Fuel-Element Support Schemes

Although no decision has been reached on the support method the comparative results are discussed in following paragraphs.

Computer programs were written to determine the stresses for the various concepts. The major environment influencing stress is the initial temperature gradient across the elements causing them to cluster and contact each other. The temperature gradient is assumed to be a cosine function of the length from the inlet to outlet, or

$$\Delta T = \frac{\Delta T_o}{2} \left(1 + \cos \frac{\pi X}{L} \right),$$

where

ΔT_o = the maximum ΔT at the outlet,

X = the distance from the hot-end, and

L = the element length.

The basic assumption is that the two elements are equal and opposite in their actions and that the assumed initial manufacturing bow and induced thermal bow cause the elements to contact at a point equal to half the spacing. A point-contact is assumed to constrain the element to this envelope. The computer codes are iterative in that they compute the deflection and place the constraining load at the highest deflection point. The computer then determines the deflections and compares the new restricted (or expanded) flow-channel area with the original area to determine the change in temperature of that channel, i. e. hot-channel effect. This new self-induced temperature gradient is added to the original gradient and the problem resolved.

Also included in the analysis are the thermal stresses due to the symmetric temperature ripple around the element (6 θ stresses). The six-theta temperature distribution is a function of the element-pitch-to-diameter ratio and radial and axial power profiles. These temperatures transposed into stresses are shown in Figure III-3. The curve assumes a 60% reduction in thermal gradients due to interchannel NaK mixing. The vibration-suppression button after it buckles still has a 30-lb load capability which creates a tensile stress of approximately 1100 psi.

Figure III-3. Six- θ Stress (Assumes equal hot and cold areas, 60% mixing factor: 100% = no mixing)

A summary of the axial stresses for the various concepts in Figure III-2 are shown in Figure III-4. Note that the minimum-stress configuration of those studied is the one with the hot-end free and the cold-end fixed. This is primarily due to the fact that with the hot-end free the element tends to deflect into the cold-channel and produce a self-correcting effect. A comparison of the different hot-channel effects is shown in Figure III-5. These all include the effect of a 0.010-in. manufacturing bow. Without the initial bow effect the additional temperature gradients become more temperature-dependent. Also the hot-end fixed element has a slightly negative effect.

A study was made to determine the effect of cladding wall thickness on stresses. Thicknesses between 0.012 and 0.020 were considered for the fuel element pinned at both ends, and pinned at the hot-end and fixed at the cold-end. It was found that only a 6 or 7% change in stress occurs over the thicknesses of concern. This is because the mode shape is set within the restraints; thus bending stresses are relatively constant for a given spacing.

Figure III-4. Comparison of Stresses

Figure III-5. Hot-Channel Effects (0.010 initial bow)

C. CORE THERMAL AND HYDRAULIC ANALYSIS

1. Coolant-Channel Thermal Analysis

Two codes have been developed for core thermal analysis. The first, TEMPRØ2, considers an average element in each ring and is suitable for detailed analysis of fuel swelling, hydrogen loss, gap thermal conductivity, etc. The second, GEØM2, considers a 30° segment of the core and is suitable for computing spacing to match flows, channel-to-channel mixing effects, thermal shorting to a cooled reflector, asymmetric fuel effects, clustering effects, and—of great importance—coolant gradients across elements for use in stress analysis. GEØM2 also considers the effect of channel-to-channel temperature-difference effects on element power distribution.

The two programs have been designed to complement each other in that GEØM2 develops ring flow factors, film coefficients, etc, for use in detailed TEMPRØ2 studies. Results of the latter, in turn, provide gap-conductivity and swelling data for use in GEØM2.

2. Core Hydraulic Design

Optimally all elements in a core should experience the same temperature-power history so that at any given time during the reactor operating life no single element or group of elements has deteriorated more than the main body of elements to prematurely end reactor life. While this performance is not wholly attainable it may be approached by matching channel flows to the local power generation. Two basic methods have been analyzed to accomplish this, using the channel flow area as an orificing device:

- 1) Variable pitch with fixed clad OD and variable fin height.
- 2) Constant pitch and fin height with variable-clad OD.

To keep manufacturing and assembly problems to a realistic level, a zoned approach was considered for both methods. Four zones were selected as representing a sufficiently close power match. The constant-pitch method is favored in that there is no spacing mismatch between zones. The clad thicknesses in the four zones of the 295-element reference reactor are approximately 0.015, 0.020, 0.025 and 0.030 in. resulting in a channel-area variation of 52.5%. An alternative

is to use a constant-thickness cladding wall and use four different element diameters. Both methods are receiving further study.

3. Fuel-Element Gap Studies

The required fuel-to-cladding gas gap for fuel-swelling allowance was determined for a ZrH reactor with a minimum lifetime of 20,000 hr at 600 kw and 1300°F coolant-outlet temperature. It was required that after 20,000 hr the probability of having a fuel-element failure (defined as fuel-cladding interference) be less than 50%. When applied on a per-element basis to the 295-element reference reactor the maximum allowable failure probability for each fuel element is 0.235%. In determining interference a 0.005-in. margin in radial gap was allowed for fuel-segment bowing.

The variables contributing to the uncertainty in final gas-gap were fuel-swelling correlation, fuel-segment initial diameter, coated-cladding ID, and the flow-channel geometric variables which determine the channel flow and thus fuel temperature. These channel variables included fin-envelope radius, tube outside radius (at base of fins), and tube bow (magnitude and direction).

The calculations were made with a computer program which sampled each variable from its specified distribution and then calculated the resulting flow, fuel temperature, and gap size. This operation was repeated 10,000 times for each configuration of interest, and a distribution of results was plotted. The gap width at the desired probability of occurrence was then picked from the distribution. Typical distributions for radial gas gap and channel-flow factor are shown in Figures III-6 and -7. The flow factors were normalized to 1.0 as the ideal flow factor for a channel. The mean flow factor in Figure III-7 is less than 1.0 because of penalties taken in the calculations for loss of a 0.002-in. element assembly clearance from clustering and from flow-area mismatch to ideal. Approximately a 4% flow-channel-area mismatch is encountered when dividing the core into four zones of element spacings.

Calculations were made for three fuel-slug diameters with larger numbers of the smaller-diameter rods being required to fill out the reactor core. The three diameters with corresponding number of elements are:

<u>Fuel-Slug Diameter (in.)</u>	<u>Number of Elements</u>
0.530	295
0.493	349
0.453	421

Figure III-6. Gas-Gap Widths after 20,000-hr Operation

Figure III-7. Channel-Flow-Factor Distribution

Three-element spacings of 0.020, 0.035, and 0.050 in. were investigated. The mean gap and the minimum gap (at minimum allowable probability of occurrence) are shown in Figures III-8, -9, and -10 for the three rod diameters. Figure III-11 shows the required initial gap, according to the requirements listed above, as a function of number of elements. Most calculations assumed a fuel burnup for the center rod at axial location $Y/L = 0.8$, the temperature maximum. Figure III-11 shows the effect of burnup with results for a low-burnup rod toward the core edge.

From Figures III-8, -9, and -10, or -11, we see that the required initial gas-gap (radial width) is 0.0065 in. for the 295-element core and 0.0058 in. for the 421-element core. Thus we find little advantage in the smaller-diameter rods so far as fuel-swelling effects are concerned.

Values of fuel temperature, mean and 2σ , are shown in Figures III-12 and -13 for beginning-of-life and after 3000-hr operation. The 3000-hr values are those for after the initial rapid component of fuel-swelling has occurred.

D. NUCLEAR ANALYSIS

Three distinct core configurations were evaluated with respect to their initial cold unpoisoned excess reactivity, control-drum worth, and water-flooding worth. These cores correspond to the progression of the ZrH design as it is currently evolving. The last design considered is not acceptable from all considerations although it illustrates the direction in which the final design is going. It also serves as a good base point from which the various viable options may be investigated and the final design identified.

The first core considered, Core A of Table III-1, was the one used as the basis of the power-driftdown^{(1)*} and prepoison-optimization studies⁽²⁾ previously reported. The core design used constant fuel diameter of 0.517 in., a fuel-cladding radial gap of 0.005 in., constant Incoloy-800 cladding thickness of 0.015 in., and an average interelement spacing of 0.035 in. with fuel-element pitch of 0.596 in. Later studies indicated the need for increased fuel-cladding radial gap to assure nonfailure of the fuel elements from δ -phase fuel growth, increased flow area to reduce the effects of manufacturing tolerances on fuel-element hot-channel factors, and positive positioning of the elements in the core to reduce hot-channel factors and fuel-element movement (bowing) during operation.

*Superscript numbers in parentheses are for References at end of this report.

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CONFIDENTIAL RESTRICTED DATA (GP-1)

Figure III-8. Gas-Gaps for 295-Element Core, Zirconium Hydride Reactor Fuel

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~~CONFIDENTIAL~~ RESTRICTED DATA (GP-1)

CONFIDENTIAL RESTRICTED DATA (GP-1)

Figure III-9. Gas-Gap for 349-Element Core, Zirconium Hydride Reactor Fuel

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~~CONFIDENTIAL RESTRICTED DATA (GP-1)~~

Figure III-10. Gas-Gap for 421-Element Core, Zirconium Hydride Reactor Fuel

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Figure III-11. Minimum Required Gas-Gap
for 20-mil Spacing

Figure III-12. Maximum Center-Rod Beginning-
of-Life Fuel Temperatures

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Figure III-13. Maximum Center-Rod Fuel Temperatures After Offset Swelling (3000 hr)

TABLE III-1
CORE MODELS FOR ZIRCONIUM HYDRIDE REACTORS

	Core A	Core B	Core C
	Constant Fuel/ Clad/Pitch	Constant Fuel/ Pitch, Variable Clad	Constant Clad/ Pitch, Variable Fuel
Number of Fuel Rods	295	295/349/421	295
Vessel Inner Diameter, in.	11.215	11.82	11.82
Pitch, in.	0.596	0.630	0.630
Cladding Material	Incoloy-800	Incoloy-800	Incoloy-800
Finned Elements	No	Yes	Yes
Radial Gas-Gap, mils	5	7	7
Glass Thickness, mils	2	2	2
Mean Fuel-Volume Fraction	0.68	0.64	0.69
Fuel Hydrogen Content, $N_H \times 10^{22}$	6.3	6.3	6.3
Zoned Cladding Thickness, mils	15	15/20/25/30	15
Fuel Outer Diameter, in.	0.517	0.530	0.53/0.54/0.55/0.56
Core Length, in.	16.825	16.825	16.825
Fuel Composition	← Hydrided-Zr/10 wt % U →		
Calculated Cold Unpoisoned Excess, \$ ($\beta_e = 0.0075$)	14.25	11.60	16.27
Normalized Cold Unpoisoned Excess, \$	15.25	12.60	17.27
Number of Control Drums	10	10	10
Drum Diameter, in.	4.5	4.5	4.5
Drum Composition	← BeO/Ta-10W →		
Poison Thickness, in.	1.5	1.5	1.5
Drum Worth, \$ (one-dimensional calculation)	8.75	9.00	8.75

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The Core B design thus evolved which featured variable-flow area, increased fuel-element pitch, and integral finned-fuel elements. The variable-flow area provided an effective means of coolant-flow profile shaping. The increased fuel-element pitch reduced the sensitivity of the flow hot-channel factors to such things as manufacturing tolerances and operational fuel-element bowing. The integral finned elements provided a means of positive placement of the fuel elements, and coolant-flow mixing reduced the individual hot-channel factors. This design used constant fuel-rod diameter of 0.530 in., constant Incoloy-800-cladding ID of 0.548 in., variable cladding thickness ranging from 0.015 to 0.030 in., and variable fin height, so that the element-to-element pitch was maintained constant at 0.630 in. As may be seen in Table III-1 this design approach resulted in a relatively low normalized cold unpoisoned excess reactivity.

The Core-C design evolved as an interim compromise to provide a design with increased initial excess reactivity while maintaining the desirable aspects of the Core-B thermal and hydraulic design. Thus Core C maintained the same fuel-element external dimensions and pitch but proposed the use of variable-diameter fuel rod and variable-ID cladding. Both Cores B and C have approximated the ideal flow profile by using four distinct zones of fuel-element sizes rather than having each element individually sized.

The results of the comparison are shown in Table III-1. The table also shows that the control-drum worth for the three cores is nearly the same. The \$8.75 to 9.00 control worth shown is for the reference drum configuration with 4.5 in.-diam control drums, 3-in.-thick BeO reflector, and 1.5-in.-thick Ta-10W poison backing. Enriched B_4C inserts in the Ta-10W could be used to enhance the drum worth.

The computed worth of water-flooding for the Core-B and -C designs is \$13.65. This is considerably more than the \$10.00 water-flooding worth estimated for the Core-A design. On the basis of the \$10.00 flooding-worth, a tritium loading equivalent to an ultimate value of \$18.00 of He^3 had been specified to provide intrinsic water subcriticality following re-entry into water after a 50-yr orbit. On the basis of the newly computed water-flooding worth, and allowing for 2-yr elapsed time, between tritiding the elements and reactor startup, the ultimate He^3 requirement has increased to \$24.00. The use of the

tritium end-of-life (EOL) shutdown and inherent subcriticality device significantly increases both the control requirement and initial-excess-reactivity requirement.

The effect of the \$24.00 ultimate loading of helium-3 on required excess reactivity is illustrated in Table III-2 as a function of He^3 shelf-life. The table also indicates the effect of the He^3 on the control-drum-worth requirement. The case shown is typical and used an initial prepoison loading of \$2.00 Sm^{149} and \$6.00 Eu^{151} 600-kwt operation for 20,000 hr at 1300°F outlet-coolant temperature.

TABLE III-2
EFFECT OF HELIUM-3 SHELF-LIFE ON EXCESS-REACTIVITY
AND CONTROL-DRUM-WORTH REQUIREMENTS

Ultimate Helium-3 Worth (\$)	Helium-3 Shelf Life (yr)	Required Cold Unpoisoned Excess Reactivity (\$)	Minimum Required Control-Drum Worth (\$)	Control Worth Required for Variable * Shelf-Life (\$)
0	-	14.30	8.75	-
24	0.5	16.95	10.20	11.80
24	1.0	17.30	9.90	11.15
24	2.0	17.95	9.35	9.95
24	3.0	18.55	8.90	8.90

*Startup at indicated shelf-life with \$18.55 initial cold unpoisoned excess reactivity.

The last column of Table III-2 illustrates the effect of variable He^3 shelf-life on the control requirement. If the reactor were to be capable of starting at any time between 0.5 and 3.0 yr after tritiding, the initial excess reactivity must be increased by this same amount. The minimum required control-drum worth values shown in Table III-2 allow startup and full control only at the particular shelf-life indicated. Even under the conditions of reactor startup following a precisely specified shelf-life, the penalties in control and initial unpoisoned excess reactivity are substantial.

The effects of various core configuration changes which could be made to enhance the initial cold unpoisoned excess reactivity are shown in Table III-3. It will be necessary to modify the current Core-C design by using techniques such as these to meet the design objective of 600 kw_t for 20,000 hr at 1300°F outlet within the current design restraints of providing water subcriticality following 50-yr orbit, minimum cold-shutdown margin of one drum stuck (poison-out) plus \$1.00, and EOL hot reactivity margin of \$2.00. The EOL reactivity margin provides for one drum stuck (poison-in) plus \$0.50, or for a large uncertainty in hydrogen loss and prepoison burnout. These design constraints reduce the previously quoted lifetimes with zero EOL reactivity contingency⁽³⁾ by as much as 50%, depending on the He³ shelf-life and initial unpoisoned excess reactivity.

TABLE III-3
REACTIVITY EFFECTS OF REACTOR
CONFIGURATION CHANGES

Configuration Change	Reactivity (\$)
Increase fuel length (16.825 to 24.825 in.)	+ 0.70/inch
Increase effective reflector thickness	~ + 2.50/inch *
Decrease element-to-element pitch	+ 0.14/mil
Decrease cladding thickness	+ 0.12/mil
Increase fuel diameter (maintain- ing element-to-element pitch and cladding thickness)	+ 0.22/mil

*Since the average density of the BeO reflector is only ~78%, an "equivalent inch" corresponds to ~1.3 in. in the actual reflector thickness.

IV. REACTOR-THERMOELECTRIC SYSTEM ENGINEERING

A reactor-thermoelectric power-system design was finalized to meet the space-station (SS) power requirements. The system uses the reference zirconium hydride reactor coupled to tubular lead-telluride thermoelectric (TE) converters, both of which are under active development for the AEC. The system design is similar to that evolved in an earlier joint-AEC-NASA study, but incorporates refinements and modifications consistent with the latest technology and current space-station program requirements.

A. SYSTEM DESIGN AND PERFORMANCE

The reactor TE system is designed to produce 33 kwe of electrical power at the nominal reactor-outlet temperature of 1200°F. The system's performance parameters are listed in Table IV-1. At the reference 1200°F operating point the net conditioned power available to the space-station loads is 27.6 kwe or 10.4% over the required 25.0 kwe. The excess is an allowance for system degradation. The required power can be supplied initially at a reactor-outlet temperature of 1120°F and degradation can then be overcome by gradually increasing the operating temperature.

The power system package is designed to fit within the cargo hold of the Advanced Logistics Shuttle (ALS) and to be handled in space from a standard docking adaptor on a fixture that bridges over the shield to the upper end of the radiator structure.

With the split shield the entire package including boom can be launched integrally with the station at a weight of 31,900 lb. Alternatively, with the bulk of the lower shield supported separately as shown in Figure IV-1, 15,950 lb can be launched integrally and 16,500 lb can be transported on a subsequent ALS flight. An alternate approach is to keep the entire shield on the replaceable power system package. The boom package would then weight 6,690 lb and the power system package 25,250.

The maximum power system that can be designed within the 2100-ft² radiator limit imposed by the ALS would be 56.4 kwe (47 kwe net). The replaceable package weight for that case would be about 24,000 lb and the retained shield/boom package would weight 16,000 lb.

TABLE IV-1
SYSTEM PERFORMANCE DATA

System:	
Gross electrical power, kwe	33.0
Reactor lifetime, yr	7.6
Efficiency, %	7.37
Length, ft	40
Effective radiator area, ft ²	1240
Specific area, ft ² /kwe	38
Thermal Balance, kwt:	
Reactor power	448
Main converters	430
Pump converters	18
Radiator reject	415
Temperatures, °F:	
Reactor outlet	1200
ΔT (both loops)	200
Average thermoelectric hot-clad	1080
Average thermoelectric cold-clad	515
Average radiator NaK	500
Electrical power, kwe:	
Net power to loads	27.6
Power to source bus (base of boom)	31.4
Diode loss	0.3
Transmission loss from patch board (150 ft)	0.3
Transmission loss to patch board (50 ft)	1.0
Power at thermoelectric converter	33.0
Matched-Load Electrical Characteristics:	
Number of thermoelectric modules	80
Module voltage, volts	6.32
Number of circuits to patch board	16
Patch-board circuit voltage, volts	30.6
Number of circuits to source bus	4
Source-bus voltage, volts	120

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Figure IV-1. 33-kwe Reactor
Thermoelectric Power
System

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B. OPERATIONAL CHARACTERISTICS

1. Off-Design Performance

Figure IV-2 shows the off-design performance of the system. By adjusting the system operating point, the power output can be varied anywhere up to a maximum value of 35 kwe (29.5 kwe net) at the output temperature limit of 1300°F.

Adjustment in operating power is made simply by changing the outlet-temperature setting. All plant parameters are inherently related to this temperature. Figure IV-2 also shows the variation in converter electrical characteristics at off-design conditions.

2. Maintenance and Repair Provisions

The maintenance and repair philosophy for the reactor-thermoelectric system is established considering the fact that the system has no moving parts and no identifiable wearout modes. All testing of this type system shows that degradation may occur in the system performance, such as in the pumps or power-conversion equipment; however, catastrophic failures are not expected. The amount of maintenance planned is therefore minimal. The system design is modular in nature so that failures have only limited effect and the system has high reliability for at least 90% power. Overdesign provides up to an 18% allow for degradation and partial failures.

Some areas are specifically designed for maintenance and repair, however. The electrical power is transmitted to a terminal board near the aft end of the radiator in a region accessible during operation. Should a TE-module failure occur, the affected circuit can be isolated at this board, limiting the power loss to 6%.

3. Radiation Environment

The radiation environment created by the reactor is shown in Figure IV-3. The dose received by three types of crewmen in this environment has been evaluated and compared to the maximum allowable doses (Table IV-2).

The SS crewmen were assumed to be in the station for six months, residing on the average at the SS mid-height. The tug crewmen were considered in a worst-case condition, assuming six months residence in the compartment closest to the reactor and involvement in 48 rendezvous and departures. Two cases were

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Figure IV-2. Off-Design Performance

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Figure IV-3. Isodose Plot, Current Space Station Concept

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TABLE IV-2
SHIELDING CRITERIA AND DOSE RECEIVED

	Space Station Crew	Tug Crew	Advanced Logistics Shuttle	
Maximum Dose Allowable	27.4 rem/6 mo (0.15 rem/day)	27.4 rem/6 mo (0.15 rem/day)	25 rem/yr	
Assumptions:			8 trips/year	
1. In SS	6 mo, at station mid-height	6 mo, in forward compartment	4.5 days/trip	
2. Rendezvous and departures	1	48	1/trip	
3. In ALS cockpit docked to SS	-	-	12 hr/trip	
4. Loitering at 1 mile from SS	-	-	-	5 days/trip
Dose Received, rem:				
1. Reactor Source				
Direct dose in SS	19.7	23.9	4.71	- -
Direct dose in ALS cockpit	-	-	2.88	25.0
ALS scattered dose in SS*	0.36	0.36	0.43	- -
Rendezvous dose	0.02	0.96	0.16	- -
2. Radioisotope Source				
Direct dose in SS	2.0	2.0	0.40	-
Total	22.1 rem/6 mo	27.2 rem/6 mo	8.6 rem/yr	25.0 rem/yr

*ALS docked 5 days/mo.

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analyzed for the ALS crewmen. In both cases it was assumed they made 8 trips/year, spending 5 days/trip in orbit. In one case the ALS loitered one mile from the SS, in the second the ALS rendezvoused and docked with it. (see Figure IV-3).

As seen in Table IV-2, the shield meets all criteria, with consideration given to scattering from the docked ALS and reduction of the allowable dose from the reactor due to other radioactive sources on the station. Three conditions actually set the shield design:

- 1) The ALS crew dose limit when loitering 5 days/trip at one mile.
- 2) The requirement to shield docked 60-ft modules to 0.15 rem/day (6.25 mrem/hr) over their entire length.
- 3) The worst-case requirement of shielding a tug pilot residing in the forward compartment to an average of 0.15 rem/day.

C. TWO vs THREE NaK LOOPS

The reference system uses two NaK loops in series in each quadrant. With this arrangement the TE converter assemblies must be in the shield gallery since the primary-loop NaK is radioactive. An alternate system arrangement has three NaK loops in series. This arrangement uses intermediate heat exchangers in the gallery to transfer the thermal power to an intermediate loop. This second loop and the third loop are conceptually identical to the two loops of the reference system.

Table IV-3 shows some comparisons between 2- and 3-loop systems. Although the unshielded system weight is greater due to the extra components, this difference is essentially offset by the reduction in shield weight resulting from a gallery height reduction to 12 in.

TABLE IV-3
COMPARISON OF KEY SYSTEM PARAMETERS,
TWO vs THREE LOOPS

Parameter	Two Loops	Three Loops
Unshielded System Weight, lb	7,350	9,050
Shielded System Weight, lb	24,650	24,800
Radiator Area, ft ²	1,240	1,360
System Efficiency, %	7.37	7.24
Reactor Lifetime, yr	7.60	7.57

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Several advantages do result from going to a three-loop system, however, including:

- 1) Tight packaging constraints on the TE converter are eliminated.
- 2) The converters can be located near the terminal board at the aft end of the radiator; accordingly, fewer modules are needed in series to obtain a voltage for practical transmission, thus increasing the modularity of the system.
- 3) The primary-loop reliability is increased due to simplification and elimination of welds.
- 4) The overall system design is less sensitive to the type power-conversion equipment involved; thus future power growth can be accomplished by changing to Brayton equipment with little change in system configuration.

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V. THERMOELECTRIC CONVERTER DEVELOPMENT

(Prepared by Westinghouse Astronuclear Laboratory)

A. REACTOR TUBULAR POWER MODULES

During the past quarter ten first-generation 5.25-volt (TEM-13G series) prototype reactor modules were tested. Seven are being tested in liquid-metal loops at Atomics International, three individually and four in Converter Module SN-1; four modules are being tested in test stands at WANL.

1. Steady-State Test-Performance Data

TEM-13G SN-2 continued on test in the two-loop liquid-metal system at AI throughout the past quarter and has accrued a total of 4911 hr of steady-state, matched-load testing at $\bar{T}_H = 1025^\circ\text{F}$ and $\bar{T}_C = 538^\circ\text{F}$. The first 500 hr was done in a static test stand at WANL.

Performance parameters (i. e. open-circuit voltage, internal resistance, power output, and overall efficiency) are shown as functions of testing time in Figures V-1 through -4. Similar data from 13G SN-4, on static test at WANL, are also presented. Note that the data in the first three (voltage, resistance, and power) have been adjusted to compensate for minor fluctuations about the $\bar{T}_H = 1025^\circ\text{F}$ and $\bar{T}_C = 538^\circ\text{F}$ matched-load design operating point. These adjustments are required to allow meaningful evaluation of performance stability in these modules in which performance degradation is barely discernible.

A statistical analysis of the rates of changes of all experimental parameters indicates a degradation rate of 10%/10,000 hr in power output for TEM-13G SN-2. This change has been produced entirely by a 10% increase per 10,000 hr in internal resistance. Open-circuit voltage has remained essentially constant and efficiency is reducing at a rate of 8%/10,000 hr. These TEM-13G SN-2 degradation rates agree very well with similar data from TEM-13G SN-4.

TEM-13G SN-1 and -5 were placed on loop test at AI during the past quarter. These modules had previously accrued 2810 and 2592 hours respectively in static-test facilities at WANL. Initial loop-test data from these two modules agree well with static-test data. These modules will be endurance-tested at $\bar{T}_H = 1125$ and 1225°F respectively.

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Figure V-1. Comparison of Open Circuit Voltage Data from Two Reactor Power Modules Tested at $\bar{T}_H = 1025^\circ\text{F}$ and $\bar{T}_C = 538^\circ\text{F}$

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Figure V-2. Comparison of Internal-Resistance Data from Two Reactor Power Modules Tested at $\bar{T}_H = 1025^\circ\text{F}$ and $\bar{T}_C = 538^\circ\text{F}$

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Figure V-3. Comparison of Power-Output Data from Two Reactor Power Modules Tested at $\bar{T}_H = 1025^\circ\text{F}$ and $\bar{T}_C = 538^\circ\text{F}$

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Figure V-4. Comparison of Overall Efficiency Data from Two Reactor Power Modules Tested at $\bar{T}_H = 1025^\circ\text{F}$ and $\bar{T}_C = 538^\circ\text{F}$

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2. TEM-13G Cyclic-Test Modules

During the past quarter three additional TEM-13G series modules SN-10 through -12) were fabricated and processed. These are identical in design to TEM-13G SN-6 through -9, four modules incorporated into the power-converter module SN-1. The purpose of these was to establish the capability of the present TEM-13G module design to withstand thermal cycling.

The first of the new modules, SN-10, was placed on static test at $\bar{T}_H = 1125^\circ\text{F}$ and $\bar{T}_C = 570^\circ\text{F}$. Effective Seebeck coefficient, internal resistance, and thermal impedance recorded during heatup of TEM-13G SN-10 are plotted as functions of average module temperature in Figures V-5, -6, and -7. Similar data from four previously static-tested modules are given for comparison along with a line representing the calculated dependency of each of these parameters on average module temperature for the TEM-13G design. Excellent reproducibility of operation has been achieved in all of the modules over the entire temperature range, and all experimental data agree well with the corresponding calculated curves.

TEM-13G SN-10 was tested for a total of 260.7 hr at $\bar{T}_H = 1125^\circ\text{F}$ and $\bar{T}_C = 570^\circ\text{F}$ nominal operating temperatures under matched-load conditions. During this time period it was subjected to a total of 50 thermal cycles, each consisting of a cooldown to an intermediate $\bar{T}_H$ operating point of 500°F , a 1-hr soak period, and a return to operating conditions of $\bar{T}_H = 1125^\circ\text{F}$. All transient operations were done with hot-side temperature ramp rates at $30^\circ\text{F}/\text{min}$ in accordance with the design specifications for the tubular reactor power module.

Table V-1 shows all experimental data recorded for TEM-13G SN-10. Cycling periods are indicated in the table. The data indicate that, through the completion of 28 cycles, performance of the module was not measurably affected by the transient operation. That is, the adjusted power output (adjusted only to account for minor deviations from the design matched-load operating conditions of $\bar{T}_H = 1125^\circ\text{F}$ and $\bar{T}_C = 570^\circ\text{F}$) remained essentially unchanged from the BOL level. However, continued thermal cycling resulted in gradual reductions in power output. After 50 thermal cycles a total reduction of 8.6% in power output (relative to BOL) was observed.

Figure V-5. Comparison of Calculated and Experimental Seebeck Coefficient Data Recorded During Heatup of TEM-13G Series Modules

Figure V-6. Comparison of Calculated and Experimental Internal-Resistance Data Recorded During Heatup of TEM-13G Series Modules

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Figure V-7. Comparison of Calculated and Experimental Thermal-Impedance Data Recorded During Heatup of TEM-13G Series Modules

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TABLE V-1
SUMMARY OF TEM-13G SN-10 TEST DATA*

DP No.	Measured								Temperature- and Load-Adjusted		
	Time (hr)	$\bar{T}_H$ (°F)	$\bar{T}_C$ (°F)	Q (watts)	V _{oc} (volts)	R _g (mΩ)	P _e (watts)	η (%)	V _{oc} (volts)	R _g (mΩ)	P _e (watts)
1	0.0	1125	571	5457	10.31	100.7	264	4.84	10.32	100.6	265
2	21.5	1121	567	5439	10.28	100.6	263	4.83	10.31	101.2	262
3	24.2	1122	575	5312	10.19	101.4	256	4.82	10.33	101.2	264
4	26.8	1125	580	5392	10.18	102.0	254	4.71	10.36	101.1	265
	8th Cycle Completed										
5	45.4	1118	578	5349	10.10	101.5	251	4.70	10.35	101.4	264
	10th Cycle Completed										
6	52.0	1117	575	5327	10.12	101.0	253	4.76	10.35	101.3	264
	16th Cycle Completed										
7	69.1	1128	577	5465	10.30	102.5	259	4.73	10.37	101.5	265
	28th Cycle Completed										
8	94.2	1128	577	5474	10.32	102.1	261	4.76	10.39	101.3	266
	33rd Cycle Completed										
9	121.3	1124	574	5463	10.20	101.4	257	4.70	10.30	101.1	262
	43rd Cycle Completed										
10	151.3	1121	573	5449	9.90	99.1	247	4.54	10.02	99.2	253
11	151.7	1121	573	5445	9.90	98.7	248	4.56	10.02	98.7	254
	47th Cycle Completed										
12	167.8	1122	569	5444	9.83	98.1	246	4.52	9.85	98.4	247
	50th Cycle Completed										
13	188.6	1121	566	5446	9.79	97.7	245	4.50	9.81	98.4	244
14	213.1	1120	564	5497	9.81	97.5	247	4.49	9.81	98.5	244
15	237.5	1123	575	5373	9.70	99.0	238	4.42	9.82	98.7	244
16	260.7	1122	566	5495	9.83	97.8	247	4.49	9.82	98.5	245

*Data taken through 4/3/70

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After completion of thermal-cyclic testing, TEM-13G SN-10 was tested under steady-state matched-load operating conditions for 100 hr. Data accrued during this time period were very stable and no further reduction in power output was observed. The module was then removed from test for a destructive examination. Results of this examination are discussed in Section V-D. It is thought that the observed circuit distortion and resulting decrease in power output are due to relative motion of the inner and outer cladding during transient operation. These cladding relative motions result from different rates of thermal expansion of the inner and outer cladding during transient operation. This situation is being corrected in the design of future modules through the incorporation of a low-expansion inner cladding, resulting in a condition in which the axial expansion of the inner (at 1125°F) will be approximately the same as that of the outer cladding (at 570°F). Status of the improved module designs is discussed in V-D.

TEM-13G SN-11, identical in design to the previous module, has been placed on test at $\bar{T}_H = 1125^\circ\text{F}$ and $\bar{T}_C = 570^\circ\text{F}$ matched-load operating conditions. Initial data from this module were not available for inclusion in this report. Current plans are to test this module under steady-state operating conditions for 260 hr, which corresponds to the TEM-13G SN-10 test time. Following testing the module will be destructively examined. Evaluation of SN-11 will establish the degree of axial distortion due to initial heatup. Comparison of SN-11 will enable the determination of cyclic distortion due to cyclic operation.

3. Module Design Improvements

A detailed stress analysis has been made to examine the mechanical behavior of the tubular module during transient operation. The analysis predicted circuit-distortion patterns similar to those observed in a number of reactor power modules which were sectioned after thermal-cyclic operation. It was found in the analysis that the degree of distortion was strongly dependent on the relative movement of the inner and outer cladding. It was also shown in the analytical investigation that by matching the thermal expansion of the inner and outer cladding the distortion could be virtually eliminated.

Three different concepts providing axially matched thermal growth for both claddings were investigated. The first concept used a rigid end-closure, stiff

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enough to structurally "lock" the two claddings. A second used a prestressed Inconel-718 inner cladding, stretched to the calculated differential expansion between inner and outer. Increasing temperature merely relaxes the applied tensile load and theoretically decreases the amount of thermal expansion of the cladding. The final concept incorporates the substitution of a low coefficient of expansion material for Inconel-718 in the inner cladding.

As a result of design studies and experiments the first two concepts were deferred in favor of use of the low-expansion inner cladding. Refractory metals with coefficients of expansion less than one-half that of stainless steel used in the outer cladding appear to fulfill this requirement. This relationship between inner- and outer-cladding coefficients of thermal expansion results in nearly equal total thermal expansion from room temperature to operating conditions. In addition, the refractory metals have much higher thermal conductivities than Inconel, providing a slight performance improvement to the module.

Several candidate refractory metals were evaluated to determine their suitability. From the available data it appears that either TA-10W or T-111 (Ta-8W-2Hf) would provide adequate strength to replace the Inconel-718 inner cladding. Although T-111 is more attractive because of its long-term NaK compatibility, the availability of Ta-10W made it first choice for a short-term proof-of-principle test.

One of the major concerns associated with the use of a low-expansion inner cladding is the method of joining to the stainless-steel end-closure components. This problem is eliminated through the use of a bi-metal clad design consisting of a 15-in. section of the refractory metal joined to stainless-steel segments on either end. The stainless-steel-clad ends are then welded to end-closure components as in the current design.

Previous experience with a co-expansion method, discussed below, indicates that a bi-metal cladding can be successfully made using tantalum and stainless steel Type 316. However, tantalum does not have the necessary strength requirements, so Ta-10W was selected.

Procurement of a co-extruded Ta-10W/316 clad was started on a best-effort basis. Ta-10W and stainless-steel Type 316 were co-extruded successfully.

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Following the successful fabrication of the Ta-10W/316 cladding, TEM-X SN-2 was defined. This module will be subjected to 50 rapid thermal cycles and destructively examined as a proof of principle that the matching of relative-cladding motion will eliminate module distortion.

B. PUMP TUBULAR POWER MODULES

1. Performance Summary of Prototype Pump Modules

Three tubular modules designed to deliver power to an EM pump continued on test at WANL during the past quarter. These prototype pump modules, designated TEM-14A SN-1, -2, and -3, are being endurance-tested at nominal average clad temperatures, $\bar{T}_H = 1125^\circ\text{F}$ and $\bar{T}_C = 570^\circ\text{F}$.

Electrical current and electrical-power output data recorded from the TEM-14A series modules are presented in Figures V-8 and -9 respectively. 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. These adjustments cannot be made for SN-1 which has no provision for measuring the instantaneous open-circuit voltage, hence determining R_g . The BOL data from TEM-14A SN-2 and -3 agree to within 1% of the design levels. Excellent performance stability has been realized in the TEM-14 series pump modules. Performance degradation rates for each of these modules, determined using a statistical analysis of power output data, range from 0 to 3%/10,000 hr.

TEM-14A SN-3 has been subjected to four sets of ten thermal cycles. All were performed with $30^\circ\text{F}/\text{min}$ average hot-clad temperature ramp rates. None of the sets of thermal cycles has produced any deleterious effects on module performance. Ten additional cycles are scheduled for this module, after which it will be destructively examined.

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. The forty controlled thermal cycles on SN-3 and the three uncontrolled shutdowns of SN-1 indicate that the TEM-14 design is insensitive to thermal cycling.

Figure V-8. Electrical-Current Data from Three Prototype Pump Power Modules

Figure V-9. Power-Output Data from Three Prototype Pump Power Modules

C. CONVERTER MODULE

1. Converter Module SN-1 Loop-Test Performance

The first multiple tubular module conversion device, Converter Module SN-1, went on test in an Atomics International liquid metal loop during the first week of January 1970. While the four tubular modules (TEM-13G SN-6, -7, -8, and -9) are connected hydraulically within the unit, they are individually connected to external load resistors so that the performance of each tubular module may be evaluated.

The design objective of mechanical integrity under operational thermal- and mechanical-stress conditions has apparently been met. The net electrical-power output of the converter module, however, was lower than expected at the target test conditions. The test parameters were formulated with the objective of operating all four of the tubular modules with mean cladding temperatures of 1025 and 538°F on the primary and secondary sides and with axial ΔT 's of 200°F on both sides. Based on performance predictions and TEM-13G SN-2 loop-test performance it was expected that the net electrical-power output of Converter Module SN-1 would be 888 watts at the start of loop testing. The average power measured, however, over the first 140 hr of loop testing (covering the first five data points) was 831 watts.

The experimental data indicated that though the flow conditions for the converter module as a whole were close to nominal none of the tubular modules was actually operating at the prescribed temperature conditions. It is believed that this situation may be primarily attributed to cross-flow heat transfer in the secondary manifold. A series of flow experiments was made during the reporting period which confirmed the analysis of the temperature maldistribution. Furthermore, it was shown that if the cross-flow heat transfer was eliminated, the BOL power output of the converter module would have been close to 888 watts as predicted.

The design of Converter Module SN-1 permits the upper and lower 2.5 in. of each tubular module to be subjected directly to a cross-flow NaK stream. Thus both the secondary inlet and exit NaK streams extract heat from the exposed tubular module ends and show an increase in temperature as each end is traversed. The greater portion of the heat removed is due to radial conduction

through the tubular module. An additional quantity of heat is transferred through the end-closure to the outer cladding.

A thermal model has been constructed which indicates that with perfect hydraulic distribution, sufficient heat can be transferred to the cross-flow NaK to account for the observed temperature patterns.

Figure V-10 illustrates the pattern of temperatures observed during testing of Converter Module SN-1. The arrows point in the direction of NaK flow and give the magnitude of the inlet and exit NaK temperatures. The circles represent the output of thermocouples (TC's) mounted on the surface of the manifold. Two measurements are actually made at each location (on opposite sides of the piping). In most cases there was little variation in output among data pairs; however, the secondary-exit TC's were quite noisy and pairs of outputs disagree by as much as 20° F.

The possibility of hydraulic maldistribution cannot be completely ruled out, but such a condition by itself cannot account for the observed data. During the reporting period a set of waterflow tests were made with a geometry simulating Converter Module SN-1, i. e. with countercurrent flow in the heat-rejection manifold assembly. Previous waterflow tests made and reported last year^{*} utilized a test section with a cocurrent manifold arrangement. The latest tests verified the earlier results and do not indicate the presence of a flow maldistribution in either configuration. The test results are plotted in Figure V-11. The countercurrent arrangement is shown to yield a slightly better flow distribution.

The water tests were made on a mean Reynolds number basis, that is, the Reynolds numbers at the axial midpoint of the spiral flow passages were matched. The viscosity of NaK varies about 30% over the secondary inlet-exit temperature range so that a corresponding variation in Reynolds number will exist. This temperature variation was not simulated in the waterflow tests. It is also suspected that the flow in various sections of the secondary manifold may be on the transition-flow regime threshold. The noise observed on the secondary-flow TC's may be indicative of an unstable flow condition in that manifold. The presence of such an unstable flow condition, however, is not felt to be significant from the point of view of the performance of the unit.

* WANL-PR(EEE)-013, "Compact Converter System Technology," Quarterly Progress Report, May-July 1969.

Figure V-10. Typical Converter Module SN-1 Temperature Distribution Demonstrated During NaK-Loop Testing

$$\text{DISTRIBUTION FACTOR} = \left[\frac{\text{MAX. MODULE FLOW RATE}}{\text{MIN. MODULE FLOW RATE}} - 1.0 \right] \times 100$$

Figure V-11. Flow Distribution Between the Four Secondary Flow Passages of the Converter Module

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In future designs the flow distribution can be improved if necessary by slightly decreasing the pitch of the primary and secondary swirl generators. The pitches currently used produce pressure drops which are well below the limits set by the current system specifications.

Following the waterflow tests a series of flow-mapping experiments on Converter Module SN-1 were done at the AI liquid-metal-loop facility on March 31 and April 1, 1970. The primary objectives of the tests were to verify that cross-flow heat transfer was responsible for the less-than-predicted power output at target test conditions, and to demonstrate that if the cross-flow-induced temperature maldistribution were reduced, the net power output of the converter would be increased and module-to-module performance variations decreased. Up to the time of the flow tests 1640 hr of loop testing had been accumulated with four facility-related startup/shutdown cycles experienced.

Two test series were done. In the first the primary-side mean NaK temperature was fixed at 1038°F with a 200°F axial ΔT while the secondary axial ΔT was varied in discrete steps. During this process the mean secondary-NaK temperature was held at 533°F. Data were taken at four secondary ΔT points: 150, 100, 50 and 39°F. The last point represents the lower ΔT limit set by the EM pump capacity.

The second test series was done over the same range of secondary axial ΔT with the primary axial ΔT fixed at 100°F. At the conclusion of the tests the loop was brought back to the original test conditions.

A summary tabulation of the test results is given in Table V-2, which lists primary- and secondary-side axial ΔT 's, flowrates, power output, net heat input, and overall efficiency.

The variation of power output with secondary axial ΔT is shown in Figures V-12 and -13. Figure V-12 is associated with the first test series where the primary axial ΔT was fixed at 200°F. As expected the power output of the individual tubular modules converged as the axial ΔT was reduced. With the exception of the minimum ΔT point the trend was quite linear; dashed lines illustrate the linear relationship.

Figure V-13 shows the variation of power output with secondary axial ΔT , with the primary axial ΔT fixed at 100°F. Higher power outputs (by about 10 to

TABLE V-2
SUMMARY OF CONVERTER MODULE SN-1 FLOW-MAPPING TESTS
(3/31/70 - 4/7/70)

Data Point	Primary ΔT ($^{\circ}F$)	Secondary ΔT ($^{\circ}F$)	Primary Flowrate (lb/hr)	Secondary Flowrate (lb/hr)	Power Per Module (watts)				Net Power (watts)	Net Heat Input (watts)	Overall Efficiency (%)
					SN-6	SN-7	SN-8	SN-9			
106	200	200	1547	1470	176	193	209	217	795	18959	4.19
107	200	200	1531	1441	173	191	207	216	787	18733	4.20
108	200	150	1563	1936	185	194	208	214	801	19150	4.18
109	200	102	1576	2874	191	198	208	212	809	19320	4.19
110	201	50	1592	5815	201	202	208	208	820	19567	4.19
111	200	39	1618	7505	205	205	210	209	829	19860	4.17
112	98	198	3134	1457	173	191	209	223	797	18869	4.22
113	100	150	3168	1943	184	196	212	220	812	19263	4.22
114	100	100	3198	2951	197	201	213	219	824	19738	4.18
115	100	100	3184	2920	194	201	214	217	825	19585	4.21
116	101	50	3228	5906	205	206	213	215	838	19977	4.20
117	101	39	3224	7523	209	210	216	218	853	20107	4.24
118	200	201	1550	1441	176	189	206	215	785	18915	4.15

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Figure V-12. Variation of Converter Module SN-1 Power Output With Secondary-Loop Axial ΔT (primary ΔT fixed at 200°F)

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Figure V-13. Variation of Converter Module SN-1 Power Output With Secondary-Loop Axial ΔT (primary ΔT fixed at 100°F)

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20 watts) were achieved at each data point taken during this series compared with those taken with the 200°F primary ΔT . This effect is primarily due to the difference in the mean primary-side film coefficients between the two experiments. For operational simplicity the test conditions for each ΔT increment were set by striving to hold fixed-mean NaK temperatures of 1038 and 533°F in each loop. Thus if the primary flowrate is doubled the primary film-drop will change by 1 to 2°F. A change of radial (clad-to-clad) ΔT by 1°F will change the net power output of the converter by about 4 watts. It is therefore concluded that within the context of the precision of the experiments the converter module and individual tubular modules are insensitive to primary axial ΔT over the range examined (at least up to $\Delta T = 200^\circ\text{F}$).

As the secondary-loop flowrate was increased to lower the axial ΔT across the test section the variation between individual inlet and outlet temperatures decreased. Before the tests the primary-outlet temperatures indicated a rise of 30°F in the bulk-stream temperature across the primary collector manifold. A temperature drop of 38°F was similarly noted across the secondary collector manifold. At the minimum ΔT point (corresponding to data point 117 in Table V-2) the peak-to-peak outlet-temperature spread was reduced to 8°F in both primary and secondary manifolds. The correlation of a more uniform temperature distribution and power output between tubular modules with NaK flowrate, directly indicates that cross-flow heat transfer is responsible for the temperature maldistribution.

Due to problems associated with the operation of the EM pumps at very high currents (hence high flowrates) the time spent at the two minimum ΔT points was brief, on the order of 15 min. The NaK conditions were not adjusted as accurately as they were on the data points taken at lower flowrates. For data point 117, for instance, the mean NaK temperatures were 1038 and 530°F. The film-drops consistent with the flowrates and heat fluxes at that point are about 12 and 5°F. The radial-cladding ΔT was therefore about 491°F rather than the reference value of 487. The differential of 4°F will produce a power differential of about 14 watts. Figure V-13 shows that the power associated with data point 117 (853 watts) is about 14 watts above the linear relationship associated with the other measurements.

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The overall improvement in power output (start-of-test vs minimum ΔT point) obtained as a result of minimizing cross-flow heat-transfer effects was 66 watts. When the data point is corrected by 14 watts by the argument given above, the actual improvement is about 52 watts. If this amount is added to the average power obtained from the first five loop-test points, a "corrected" BOL power of 883 watts is obtained.

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D. SUPPORTING TECHNOLOGY DEVELOPMENT

1. Sublength Modules

The TEM-15 series modules were designed as a part of the support technology development phase of the Compact Thermoelectric Converter System Technology Program. These 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 for fabricating and testing these modules is to evaluate the effects of varying processing techniques, assembly clearances, and intercouple insulating methods on module performance.

The lead telluride washer axial thickness for the sublength modules was selected as 0.050 in. rather than 0.090 as in the TEM-13G reactor power modules. Experimental data from previous prototype reactor power modules (e. g. TEM-13A, -3B, and -3C series) have shown that the degradation rate is a strong function of lead telluride washer thickness. That is, as the TE washer thicknesses are reduced, the effects of the intercouple contamination become more pronounced. After a given testing-time period, a greater percentage of the thin washers is affected by the contamination process. This phenomenon is felt to be a primary reason for the very stable performance of the TEM-14 pump modules which have 0.700-in. -thick lead telluride washers.

With the relationship between TE washer axial thickness and degradation rate in mind, the TEM-15 circuit was designed with relatively thin washers. The thinner washers allow a more rapid evaluation of the effects of the TEM-15 design and processing modification incorporated to improve module performance stability.

a. TEM-15A Series Modules. All TEM-15A series modules use the conventional single-piece 0.005-in. -thick mica insulator between adjacent TE washers. These modules were designed to investigate the influence of mica radial clearance on mica deformation during processing, the influence of weld-shrinkage on circuit alignment and mica deformation, and the influence of axial preload on mica deformation and circuit alignment in a module incorporating standard 0.005-in. -thick mica insulators.

Six TEM-15A series modules were tested during the past quarter, all at

nominal $\bar{T}_H = 1125^\circ\text{F}$ and $\bar{T}_C = 570^\circ\text{F}$ cladding temperatures in static test facilities at WANL. Four were tested under steady-state conditions and two were thermal-cycled.

Electrical-power output data (adjusted to account for minor temperature and load-resistance fluctuations) as a function of elapsed testing time are shown in Figure V-14 for all steady-state-tested TEM-15A series modules. Excellent performance reproducibility has been achieved in all these modules. The results of the examination of SN-4 are given in another section of this report.

Also shown in Figure V-14 is a line indicating the performance recorded for TEM-13B SN-2 during the first 400 hr of steady-state testing. This module was selected for comparison with the TEM-15 module data because it has identical TE washer dimensions. Power output for TEM-13B has been adjusted to account for the decreased number of TE couples in the TEM-15 design. The improvement in performance stability observed in the TEM-15 series modules, shown in Figure V-14, is attributable to the incorporation of the stepped inner-conductor-ring concept (TEM-13B had solid tungsten inner conductors).

Figure V-15 gives overall efficiency data from all TEM-15A series modules tested under steady-state conditions during the past quarter. Very stable and reproducible performance data have been recorded for them. A degradation analysis in which the performance stability of these and all other TEM-15 series modules is endurance-tested under steady-state operating conditions is discussed below.

In addition to the steady-state-tested modules, thermal-cycle testing of TEM-15A SN-8 was begun and completed during the past quarter. A total of 50 thermal cycles were run. Each consisted of a cooldown to an intermediate average hot-clad temperature of 500°F , a 1-hr soak period, and a return to design operating conditions of $\bar{T}_H = 1125$. All transient operations were carried out at ramp rates of $60^\circ\text{F}/\text{min}$. This transient condition imposed during the heatup portion of each thermal cycle exceed the requirements in the design specifications for reactor modules. No ramp-rate margin is required for the cooldown portion of each cycle since $60^\circ\text{F}/\text{min}$ is the most rapid transient condition experienced in a system.

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Figure V-14. Comparison of Steady-State Power-Output Data from TEM-15A-Series Modules

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Figure V-15. Comparison of Steady-State Overall Efficiency Data from TEM-15A-Series Modules

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The purpose for running these thermal cycles was to compare the results of a post-testing destructive examination of this module with results from similar modules cycled at less-severe ramp rates. TEM-15A SN-4B and -4C were thermal-cycled 50 times each at 2.5 and 30°F/min ramp rates, respectively, during the previous quarter. Results of these destructive examinations are discussed in the Quarterly Progress Report, WANL-PR(EEE)-019.

Figure V-16 gives overall efficiency data from TEM-15A SN-8. These data indicate that in this module, as in all previous TEM-15 series modules which have been cycled, there has been no noticeable effect on module performance as a result of thermal cycling.

TEM-15A SN-4 was subjected to two rapid thermal transients, done by instantaneously terminating input power to the module while testing at design temperatures, $\bar{T}_H = 1125^\circ\text{F}$ and $\bar{T}_C = 570^\circ\text{F}$. This type of cycle produces hot-clad-temperature ramp rates in excess of $-450^\circ\text{F}/\text{min}$ and is typical of conditions existing immediately after a laboratory power failure during static testing. The module was then removed from the test stand and submitted for destructive examination, results of which are discussed below. In summary the TEM-15 destructive examination programs have revealed slight distortion which increases in extent as the severity of the thermal cycle is increased; however, in none of these modules has the circuit integrity been compromised.

b. TEM-15B Series Modules. The TEM-15B series module is designed with a 0.0005-in. -thick layer of tungsten sandwiched between two 0.00225-in. -thick mica washers. Purpose of the tungsten is to ensure an absolute diffusion barrier between adjacent lead telluride washers to completely eliminate inter-couple diffusion. Incorporation of this design scheme into the reactor power module, however, would not be attractive since the high shunt-heat loss through the tungsten barriers reduces the module efficiency by nearly 40%. However, success of this experiment will validate theory that module degradation is caused by an intercouple diffusion of tellurium through the mica washers. Data from this experiment are being used as a baseline to measure the effectiveness of various design fixes in reducing degradation rates.

TEM-15B SN-4, identical to the reference -15A module (SN-4) except for the tungsten barrier, has accrued 3292 hr of testing time at $\bar{T}_H = 1125^\circ\text{F}$ and $\bar{T}_C = 570^\circ\text{F}$.

Figure V-16. Adjusted Overall Efficiency Data from TEM-15A SN-8 Cyclic Test

Power-output data from TEM-15B SN-4 are shown in Figure V-17 along with similar data from two -15C series modules incorporating a different diffusion-barrier scheme. These modules are discussed below.

Incorporation of the 0.0005-in. tungsten-diffusion barrier in TEM-15B results in reduction of power output as well as an increase in heat input. The former effect can be seen by comparing power-output data of TEM-15B SN-4 in Figure V-17 with similar -15A data in Figure V-14.

The improved stability of performance observed in TEM-15B SN-4 can be readily seen in Figure V-17. Statistical analysis of these data indicates that there has been no degradation of power output or efficiency during testing. Details of the TEM-15 module-degradation analysis are given later in this report.

c. TEM-15C Series Modules. TEM-15C series modules are designed with two-piece mica washers coated with thin films of Mo deposited by EB evaporation. The washers used in TEM-15C SN-4 were made from 0.0025-in. -thick mica sheets upon which 1000 to 2000 Å of Mo was deposited. Two washers are used at each mica position (Mo-coated faces together), so that the total barrier thickness is 2000 to 4000 Å at each position. The design of TEM-15C SN-4, with the exception of the Mo-coated mica, is identical to that of the reference -15A series module (SN-4).

The purpose of the molybdenum barrier is to eliminate the intercouple diffusion of tellurium between adjacent lead telluride washers. Since the total EB-deposited film is approximately 3% as thick as the W barrier used in the mica washers of TEM-15B SN-4, shunt-heat losses in -15C-series modules are negligible. Power-output data from TEM-15C SN-4 are given in Figure V-17 along with similar -15B SN-4 and -15C SN-8 data.

TEM-15C SN-8, identical to -15C SN-4 except for an increased clearance between the large mica insulator OD and outer boron nitride ID, was placed on test near the end of this reporting period. Insufficient data have been accrued to evaluate performance stability at this time. However, power-output data from this module are shown in Figure V-17. BOL power-output data from TEM-15C SN-8 are approximately 7% lower than the calculated level of 67 watts. Primary cause of this deviation is a 4%-lower-than-predicted voltage. The cause of this anomaly is being investigated.

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Figure V-17. Electrical-Power Output vs Elapsed Time at Test Conditions for TEM-15 Modules Fabricated with Diffusion Barriers in the Mica Insulators

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d. TEM-15 Performance Stability Analysis. Table V-3 summarizes results of the most recent degradation-rate analysis of all TEM-15 series modules having steady-state testing times in excess of 1000 hr, this being the minimum time required to establish performance trends. Degradation rates for four parameters (resistance, open-circuit voltage, power output, and overall efficiency, all adjusted to account for minor testing-temperature variations) were determined by fitting each parameter as a linear function of testing time and dividing the first derivative, i. e. slope of the straight line, by the initial value. Recent data from two modules, TEM-15A SN-8 and -15B SN-4, however, indicate the possibility of a discontinuity in the data. Further testing is required to substantiate and evaluate the effects of these apparent discontinuities.

Also shown in the table are the total number of hours represented by the data used to fit the straight-line function and the number of thermal cycles accrued during this time period for each module.

Several interesting observations can be made concerning these degradation-rate data:

- 1) The rate of increase of R_g in all seven TEM-15A series modules is nearly equal ($31 \pm 3\%/10,000$ hr). This is true of cyclic- as well as steady-state-tested modules.
- 2) The rate of increase of R_g for TEM-15B SN-4 (W- foil barrier module) is less than one-half of all other modules, thus indicating the effect of the W barrier.
- 3) The rate of increase of R_g for TEM-15C SN-4 (Mo vapor-deposited barrier module) falls only slightly below the band formed by -15A-series modules with no intercouple diffusion barrier. On a resistance basis the Mo deposition appears to have an insignificant effect on the degradation mechanism.
- 4) Rates of change in open-circuit voltage cover a somewhat wider range than the resistance data. All modules, barrier and nonbarrier, fall within the range 2 to 15%/increase per 10,000 hr. This increase of voltage tends to offset a portion of the resistance increase.

TABLE V-3
 TEM-15 PERFORMANCE-ANALYSIS SUMMARY
 (RATES SHOWN AS PERCENT INCREASES
 PER 10,000 hr)

Module No.	Time (hr)	Increase/10,000 hr (%)				Number of Cycles
		R_g	V_{oc}	P_e	η	
TEM-15A SN-1	2699	31	+2	-25	-18	1
-15A SN-4*	2349	29	+4	-19	-6	2
-15A SN-4B*	2174	34	+14	-3	-4	50
-15A SN-4D	3235	32	+4	-21	-13	0
-15A SN-5	2421	32	+6	-19	0	2
-15A SN-8*	2442	29	+2	-23	-13	52
-15B SN-4	3291	12	+7	+1	-1	0
-15C SN-4	1752	28	+15	+3	-11	0

*Testing terminated

- 5) Both barrier modules increase in power, primarily due to increasing voltage overcoming the increasing resistance.
- 6) Degradation rates of the nonbarrier TEM-15 modules, with the thin washers (0.050 in.) are less than would have been expected based on -13G data.
- 7) TEM-15A SN-4B, which has been thermally cycled 50 times with 2.5°F/min ramp rates, has the lowest power-output degradation of all -15A series modules, indicating the insensitivity of the sublength-module design to thermal cycling.

The design variations in the TEM-15A-series modules appear to have had little or no effect on module degradation. However, the effectiveness of the tungsten barrier in -15B SN-4 is quite clear from a comparison of resistance-degradation-rate data. It is also quite clear that there is still a degradation mechanism in -15B SN-4 resulting in a measurable resistance increase. This increase in resistance is completely offset by a simultaneous increase in voltage, resulting in very stable power-output and efficiency data.

e. Modified TEM-15 Modules. Two TEM-15 series modules with structural modifications were designed, fabricated, processed, and placed on test during the past quarter. The modifications were identified as techniques for increasing thermal-cycling capabilities of the 15-in. reactor modules. It was decided to first incorporate the modifications into the sublength modules for evaluation before fabricating reactor-length modules.

Both of the new modules, designated as TEM-15 SN-1 and -2, were fabricated and processed as -15A SN-4 modules with the exception that the outer-cladding thickness was increased to 0.155 in. from 0.100. After processing, a conically shaped stiffened retainer was welded to the inner and outer claddings. A schematic of the conical retainer is shown in Figure V-18. Note that the axial loads resulting from differential thermal expansion of the inner cladding are partially carried through the bearing area of the 0.030-in. radial step machined on the inner cladding. This scheme minimizes structural dependency on the retaining ring to inner-cladding weldment.

The second modified sublength module, SN-2, was further modified after processing. Inconel cladding 0.100-in. -thick (radially) was placed over the

Figure V-18. Modified TEM-15 End-Closure Design

stainless-steel cladding which had been machined to a radial thickness of 0.050 in. after processing. An interference fit between the stainless and Inconel claddings was obtained by heating the Inconel sleeve before installation on the module. The Inconel is designed to prevent plastic deformation of the stainless-steel cladding produced by differential thermal expansion.

Data taken during the heatup of the two modified TEM-15 modules are shown as a function of average module temperature in Figures V-19 through -21, along with the predicted curve, for internal resistance, effective Seebeck coefficient, and thermal impedance. Figure V-19 shows the excellent agreement between measured and predicted module internal resistance. Measured and predicted thermal impedance values are also in good agreement, as shown in Figure V-20. Measured effective-Seebeck-coefficient values, however, are somewhat lower than the predicted ones (5% at operating conditions).

The effective-Seebeck coefficient (i. e. open-circuit voltage) of these two modules was predicted to be lower than for the TEM-15A reference modules due to the thicker claddings of the modified design. A larger portion of the total module radial-temperature drop is accounted for by the outer -15 cladding, hence the temperature drop across the TE material decreases. The lower than anticipated effective Seebeck coefficient is not understood at this time and the problem is being investigated.

After 117 hr of matched-load static testing at $\bar{T}_H = 1125^\circ\text{F}$ and $\bar{T}_C = 570^\circ\text{F}$, TEM-15 SN-1 was subjected to 50 thermal cycles in which $\bar{T}_H$ was reduced to 500°F at a ramp rate of $-60^\circ\text{F}/\text{min}$. Following a 34-min. (nominal) soak period for each cycle, $\bar{T}_H$ was returned to design temperatures at a ramp rate of $60^\circ\text{F}/\text{min}$. A 34-min (nominal) soak period at design temperatures was also adhered to before the start of subsequent cycles. The life-test data from TEM-15 SN-1 clearly show these cycles had no effect on the level of the performance parameters. This module has been removed from test for destructive examination.

2. Module Examination

a. TEM-15. Five TEM-15 sublength modules were examined destructively to determine the effect of thermal cycling on their respective circuit configurations. Table V-4 shows the cyclic history as well as the total testing time of each module.

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Figure V-19. Internal Resistance of TEM-15 SN-1 and -2 Measured During Heatup

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Figure V-20. Thermal Impedance of TEM-15 SN-1 and -2 Measured During Heatup

Figure V-21. Effective Seebeck Coefficient of TEM-15 SN-1 and -2 Measured During Heatup

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TABLE V-4
TEM-15 CYCLIC HISTORY

Module No.	Hours of Test Conditions	Cyclic History
TEM-15A SN-4A	0	As fabricated
-15A SN-4	2200	Two cycles Rapid shutdown, 2.5°F/min heatup
-15A SN-4B	2000	50 cycles 2.5°F/min ramp rate
-15A SN-4C	1100	50 cycles 30°F/min ramp rate Two cycles Rapid shutdown, 2.5°F/min heatup
-15A SN-8	2500	50 cycles 60°F/min ramp rate

Each module was cast in epoxy and sectioned axially, exposing the entire axial center-plane of the module and the complete TE circuit. Visual examination of the circuit revealed that the four modules which had been tested had a similar appearance, i. e. slight deformation of the washers along the inner-conductor rings was noted. This deformation was in the familiar chevron pattern, but the degree of chevrons was very slight. Examination by light microscopy at 100X revealed no shearing of the mica washers. TEM-15A SN-4A, which was examined in the as-fabricated condition, showed no evidence of this circuit deformation.

A series of measurements was made on each of the four modules to determine whether or not there was any minute deformation of the circuit that could possibly be related to the cyclic histories of the modules. Figure V-22 is a plot of the axial movement of the inner-conductor rings vs the position of the rings along the module axis. The rings which had shifted toward the negative end of the module are shown with a negative displacement and those with a shift toward the positive end are shown with a positive displacement.

The conclusions drawn from the plots in Figure V-22 could easily be interpreted to be that increasing ramp rates cause increasing deformation.

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INNER CLAD RING DISPLACEMENT - MILS

Figure V-22. Displacement of Inner-Conductor Rings Relative to Outer-Conductor Rings vs Conductor-Ring Axial Position for TEM-15-Series Modules

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At the positive ends of the five modules under consideration the maximum deformation occurred in the module which had the 60°F/min cycles (TEM-15A SN-4). The negative ends of all the modules do not appear to be affected by the various testing conditions beyond initial heatup. There was no clear pattern evident on the negative end relating number and/or ramp rates of cycles to relative movement of the inner conductor.

b. TEM-13. One reactor module was destructively examined during this reporting period. TEM-13G SN-10 was tested for approximately 250 hr at reactor conditions ($\overline{T}_H = 1125^\circ\text{F}$, $\overline{T}_C = 570^\circ\text{F}$). During the life test the module was given 50 thermal cycles with a ramp rate of 30°F/min. This module was the first TEM-13-type module ever examined which had experienced controlled thermal cycles and no rapid thermal heatup or cooldown. The destructive examination revealed the circuit was deformed in the familiar chevroned pattern noted in previously examined TEM-13 modules. The relative movement of the inner-conductor rings was greater than that noted in -15A SN-4C, which had a similar cyclic history.

The results from the destructive examination of TEM-13G SN-10 demonstrated that circuit deformation is not only ramp-rate dependent, as was demonstrated by the TEM-15 type modules, but is also length-dependent as evidenced by the greater deformation in 13G SN-10. The cyclic and length dependency of the circuit deformation of TEM-15 and -13 modules should be corrected by the substitution of an inner cladding designed to eliminate the relative motion of the inner with respect to the outer cladding. An inner cladding which possesses the required properties to alleviate the differential thermal expansion between the inner and outer is discussed in Section D-4.

3. Mica Evaluation

A new series of EXC capsules has been fabricated to evaluate the effects of alternate barrier forms between N- and P-type lead telluride. The ten capsules are of the sleeved type and contain mica discs (coated with a thin molybdenum coating) between alternating N- and P-type PbTe pellets. The Mo coating was applied by EB vapor deposition. The mica

discs, approximately 0.0025-in. thick, are placed between the pellets with the Mo coating, faces together to provide a maximum Mo layer which can act as a barrier. Two of the capsules were fabricated with mica discs that had no Mo coating.

The capsules were placed on test at 1202°F and the circuit resistance monitored as a function of time. Figure V-23 is a plot of the circuit resistance vs time at test conditions for three of the capsules tested at 1202°F for 1000 hr. The plot shows that the two capsules which contained EBVD molybdenum on the mica insulators exhibited a slightly higher circuit resistance than the capsule which contained uncoated mica. The capsules were removed from testing and sectioned transversely. One-half of each was sectioned axially and polished for Seebeck probe and microscopic examination. Figure V-24 is a photomicrograph of the mica barrier between two of the PbTe pellets from EXC A-17. This sample will be examined via the EB microprobe to determine if the barrier between the mica is pure Mo or if it has acted as a sink for excess Te from the P-type pellet. In addition the three capsules tested for 1000 hr at 1202°F are being examined via the Seebeck probe to ascertain whether or not any bulk-property changes have occurred.

A second set of EXC sleeved capsules has accumulated approximately 1900 hr at 1202°F. Figure V-25 is a plot of the circuit resistance vs time at test conditions for this set of capsules. Included in this set is one with 0.010-in. Mo-coated mica between the PbTe pellets. This capsule (EXC A-10) has maintained a high circuit resistance (10^5 ohms) over its life test thus far, which is a marked improvement over capsules with 0.005-in. mica. It may be significant to note that the TEM-14A modules, which contain 0.010-in. mica washers, have accumulated test times over 10,000 hr at similar temperature ($\bar{T}_H = 1125^\circ\text{F}$) with no noticeable degradation. Investigation of this phenomenon will continue. This second set of capsules will be removed from testing at 2000 hr and examined via the Seebeck probe and metallography to determine the effectiveness of the coated mica barriers in halting cross-contamination of the PbTe pellets.

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Figure V-23. Circuit Resistance vs Time at Test Conditions for EXC Capsules Tested for 1000 hr at 1202°F

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Figure V-24. Photomicrograph of Mica Barrier Between
N- and P-Type Lead Telluride Pellets
of EXC A-17 (400X)

Figure V-25. Circuit Resistance vs Time at Test Conditions for EXC Capsules Containing EBVD Molybdenum-Coated Mica Between Pellets

4. Material Studies

An investigation has been started on obtaining material for use as an inner cladding to replace Inconel-718. Stress-analysis of a TE module indicates that the mismatch in the relative thermal expansions between the inner and outer claddings could produce washer-chevroning when the module experiences thermal cycling. During module operation ($\bar{T}_H = 1125^\circ\text{F}$, $\bar{T}_C = 570^\circ\text{F}$) the inner cladding of a TEM-13 module expands approximately 0.132 in. in the axial direction while the outer expands axially only 0.073 in.

The requirements for a material to serve as a replacement for Inconel-718 are as follows:

- 1) The thermal-expansion coefficient for Inconel-718 is 8.4×10^{-6} in./in./ $^\circ\text{F}$. The replacement material should have a CTE of approximately one-half that of 718 ($3-5 \times 10^{-6}$ in./in./ $^\circ\text{F}$).
- 2) The new material must also have excellent corrosion resistance to liquid metals (NaK) at least to 1300°F for long times.
- 3) The new material must be weldable in order to join it to the rest of the module at the stretch-neck.
- 4) The new material must be compatible with other materials which it will contact in the module (boron nitride, Alsimag, etc.).

Among the materials which possess the required low expansion coefficients are the refractory metals molybdenum, niobium, tantalum, and their alloys. These materials also possess excellent corrosion resistance to NaK up to 1650°F . A Ta alloy (Ta - 10W) was selected as the material for an inner cladding to demonstrate proof-of-principle of a low-expansion inner cladding. This material has strength and rupture properties similar to Inconel-718. The design selected joins the refractory alloy to 316 stainless steel at each end of the inner cladding. Such a configuration forms a bi-metal cladding with the refractory alloy under the TE circuit. The stainless-steel ends make up the remaining lengths at each end.

An experiment is underway to determine the long-time compatibility of P-type PbTe with Mo at temperatures corresponding to cold-side temperatures at the PbTe/Mo outer-conductor-ring junction. Two EXC-S-type capsules containing one P-type pellet with Mo contacts at both ends have been placed on test at 660°F, and their resistance monitored as a function of time. It is planned to test these capsules for more than 10,000 hr provided that no marked changes in the resistance occur before that time.

The experiment evaluating the compatibility of nickel and molybdenum continues on test at 1100°F in a vacuum of 10^{-5} torr. After 3800 hr at test conditions, the four pins exhibited no changes (slope = 0) on the resistance-vs-time plot.

5. Improved Thermoelectric Materials

A task was started for investigating various TE materials which have been reported to exhibit properties superior to those of the materials currently used in tubular modules (3M Company TEGS-2P PbTe and General Electric Company GE-NL PbTe). At the present time this investigation is being directed toward two PbTe-base compositions developed by RCA^{*}; these are P-type $\text{PbTe}_{0.95}\text{Se}_{0.05}\text{Na}_{0.007}$, designated APXI, and N-type $\text{Pb}_{0.951}\text{Ge}_{0.05}\text{Te}(\text{PbI}_2)_{0.001}$, designated ANXI.

A laboratory area was set up and raw materials procured for preparation of material lots and fabrication of specimens for electrical testing.

One ingot of each composition (APXI and ANXI) was successfully prepared during this period. Fabrication and testing of specimens are currently in progress.

6. Process Development

a. Mica Evaluation. As a result of the heat treatment of the mica a number of the insulator washers were in a partially delaminated, i. e. "puckered" state. To avoid using mica in this condition the tests described below were run to set up an acceptance criterion.

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* "Improved Compatible Materials For Thermoelectric Power Generation", NYO-3886-1 (Feb. 1968), -2 (Nov. 1968), -3 (April 1969), and -4 (August 1969).

Sample runs of 50 washers each were vacuum heat-treated at 600, 700, and 800°C in order to determine the effect of temperature on the delamination of the mica. On these small sample batches the yield varied from 50 to 33%, decreasing with increasing temperature. Marked samples were included to determine the weight loss of the mica during heat treatment. The losses were from 0.4 to 0.5 milligram whether there was any delamination or not. One further run was conducted on 296 2-in. mica squares heat-treated at 800°C. The yield was higher, the acceptable pieces being greater than 60%. The samples from this run were subjected to some selectivity before being heat-treated. Since then the mica sheets have been examined and selected in the as-received condition, and delamination of the resultant washers during heat-treatment has been virtually eliminated.

b. Barrier Layers. Work during this quarter has been concentrated on the development of a satisfactory barrier to the intercouple diffusion of tellurium through the mica insulators. The general approaches have been to introduce a suitably thin metallic layer between the mica insulators, and to evaluate a glass layer.

c. Sputtering. Direct-current sputtering was investigated at the Westinghouse Aerospace facility to evaluate this process for depositing a tungsten coating on the mica substrate. The W was sputtered directly onto the mica and on an intermediate chromium layer in an attempt to improve the adhesion. In both cases the W layer flaked from the mica. In the latter case the W adhered to the Cr but the two coatings came off the mica as tightly curled flakes. It appears that crystalline coatings of as-deposited W are highly internally stressed and as a result flake off.

To improve the adhesion to the mica an intermediate layer of Al_2O_3 was introduced, a RF sputtering apparatus at Westinghouse R&D being used to deposit the alumina. The following conditions were used for both the Al_2O_3 and W:

Chamber pressure:	5×10^{-6} torr
Argon pressure:	20 microns
Target distance:	2 - 1/2 in.

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The alumina was deposited using both argon and a 50/50 mixture of argon and oxygen as the ionizable medium. The O_2 was introduced as a precaution to maintain the Al_2O_3 composition. The W was sputtered using only the Ar. The thickness of the films was measured at 1500 to 2000Å of alumina and 800 to 1000Å of tungsten.

The resultant W coating sputtered onto the Al_2O_3 was shiny and continuous; it was not, however, crystalline and could be wiped off. This sample was heat-treated at 800°C in H_2 in order to crystallize the W film. After heat-treatment the film was indeed crystalline and continuous. The results were obtained by x-ray analysis.

d. EBVD. During this quarter approximately 200 molybdenum-coated washers were made by electron-beam vapor deposit (EBVD). The washers were used in the fabrication of TEM-15C SN-8. These washers were coated in three stages with intermittent heat-treatments to achieve the desired thickness. Since then, Mo coatings with thicknesses estimated at 1000 to 1500Å can be achieved in one step, necessitating only one heat-treatment. At greater thicknesses some localized flaking of the coatings has been observed.

Further work has also been done to achieve a suitable W coating. These applied to date have been shiny and adherent. Heat-treatment at 800°C in H_2 , the same as used for Mo, has been found to be sufficient to insure that these coatings are continuous and crystalline. The results have been verified by x-ray analysis.

At present the W-deposition procedure is restricted to thin coatings with intermittent heat-treatments. Efforts to deposit thick W coatings over long time periods have resulted in "dusting" of the coatings with increasing times. Initially it was believed to be due to the outgassing of the system caused by heating at the higher power level. This, however, did not appear to be the answer to the entire problem and further studies are being made.

In both the sputtering and EBVD-coating methods the results to date indicate that W coatings have not been deposited in the crystalline state. X-ray analysis has verified that metal deposits consist of amorphous films which require heat-treatment to render them continuous. When deposited on

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a heated mica substrate, either by external heating or as a result of the deposition process, the resultant coatings are so highly stressed that they flake off. This was evident for EBVD, sputtering, and also for coatings previously done by chemical-vapor deposition. Apparently the flaking is due primarily to the brittle nature of these metals and also to the mismatch in expansion coefficients with the mica. Consequently, successful mica coating in large quantities will require additional development. The EBVD system appears to be the most favorable approach to this end.

e. Glass-Barrier Layers. Some cracking of mica insulators has been observed upon destructive examination of modules. It is highly probable that this crack network can be responsible, at least in part, for tellurium migration and subsequent module degradation. On this basis an evaluation has been undertaken.

During the autoclaving operation sufficient stresses are placed on the module to compress the various components and to fracture the mica. A proposed solution is to introduce a thin layer of glass between the mica, similar to the metallic barrier layers now being evaluated. During the final autoclave step at temperature the glass will flow and any cracking that takes place should be sealed by the glass. The process conditions now used are sufficient to crystallize the glass before cooldown of the module. The resulting barrier layer would be a glass-mica insulator under compression, similar to a hermetic glass/metal seal where the glass is under compression.

Historically there is sufficient evidence to justify this approach. As early as 1960, PbTe pellets were being encapsulated in glass to prevent the vaporization and loss of Te from the alloy to the atmosphere. Also, compatibility tests have been made with glass-bonded mica insulators which have shown that no reactions occur between them and the PbTe pellets. On this basis the glass seems to be worth investigation as a possible solution.

At present, reaction tests in graphite dies are being made on several glass compounds and PbTe alloys to determine compatibility. The tests are run at 700°C and 1000 psi for durations of 7 hr. The first tests were conducted on a glass obtained from Westinghouse R&D Center to determine

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compatibility. The results for this glass were encouraging. No visible reaction was apparent under microscopic examination. Checking the samples with a Seebeck probe showed that a reaction did occur with the P-type material but not with the N-type. An explanation is that since the glass contained approximately 30% PbO a reaction was possible between the PbO and the sodium dopant in the P-type alloy. The Na could reduce the PbO to form an oxide of sodium and deplete the alloy of carriers; hence the increase in Seebeck coefficient.

At present several compositions are being formulated with more-stable oxides, and testing will be continued on these. These formulations will include those previously used for pellet encapsulation. Future tests on these materials will also be made in EXC capsules when suitable formulations have been determined.

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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 components for the reference zirconium hydride reactor. The latter components include bearings, actuator, 4 π -shieldable reflector, shielding, and NaK pump.

A topical report covering the 12,000-hr design-verification testing of S8DR actuators was published. A similar one on SNAP 8 bearing development is being published.

One of the actuators which completed the S8DR design-verification test has almost achieved its goal of demonstrated 20,000-hr minimum life. Prototype-configuration actuator coils wound with Ni-clad Ag and BeO-hardened Cu conductor materials are operating satisfactorily. A mockup actuator incorporating most of the expected modifications to the S8DR design for ZrH-reactor application is being fabricated to be tested with the control-drum-drive system.

The prototype high-temperature bearing is performing satisfactorily after 10,000 hr at 1500°F and 10^{-8} torr. This bearing exhibited no self-welding tendency after the second 1000-hr static dwell at 1500°F.

The first development 4 π shieldable control drum completed ten prelaunch simulated thermal cycles to 1300°F and the launch-simulation vibration tests without any external evidence of distress. Removal of the cladding, however, revealed that one of the six BeO reflector blocks had cracked. Design modifications are being incorporated in the second drum, now being fabricated, to prevent repetition of this failure.

The Nb-1Zr test samples were successfully coated with chemical-vapor-deposited (CVD) tungsten and molybdenum over tungsten. The application of plasma-gun iron-titanate coating over these coatings was also successful. Tests are under way to evaluate the effectiveness of these CVD coatings in preventing oxygen absorption and resulting low-temperature embrittlement of the band and cladding, and high-temperature emittance characteristics of the iron titanate.

The 10,000-hr test to determine the effect of adding graphite to the liquid lead on the decarburization of the Croloy-2 1/4 alloy steel and 1020 carbon-steel

containers was completed. Analysis of lead and metallurgical examination of the capsule materials is in progress. The fabrication of a liquid-lead test-shield assembly is nearing completion. A large-diameter, thin LiH shield assembly representative of the axial shield for a 4π -shielded reactor power system is being fabricated. The purpose is to develop the casting technology required to fabricate shields of this type.

B. CONTROL-DRUM ACTUATOR

1. S8DR Actuator Design-Verification Test

A topical report (AI-AEC-12925) covering testing of the S8DR actuator (modified design) was published.

2. Zirconium Hydride Reactor Actuator

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

a. Component Testing

Tests are being run to evaluate potential high-temperature, high-reliability conductor materials for actuator coils. These tests are being made on coils wet-wound with AI-1 cement on rectangular-shaped alumina bobbins. Three different conductor materials have been or are being tested as candidates for the coils: nickel-clad silver, Cube alloy^{*}, and Oxalloy[†]. The Ni-clad Ag conductor consists of a fine Ag core swaged in "A" Ni cladding. The composite wire is 80% Ag by cross-sectional area. Cube alloy conductor is a Cu base or matrix, dispersion-hardened by addition of fine particles of beryllia. Oxalloy conductor consists of an oxygen-free high-conductivity (OFHC) Cu core swaged in a chrome-iron alloy (430 stainless-steel) cladding. The composite wire is 72% Cu by cross-sectional area.

A performance test of prototype actuator coils, built to the configuration, bobbin shape and material, conductor size and number of turns, lead-wire termination and fabrication process, representative of the advanced actuator design is underway. The test items originally consisted of six coils wound with Cube alloy, and six coils wound with Ni-clad Ag. After 40 thermal cycles and 2276

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*Handy & Harmon trade name

†Sylvania trade name

test hours of which 2063 hr were at the test temperature of 1300°F, two Cube alloy coils failed. The failures were due to brittle fracture of the conductor near the termination to the lead wire. This part of the conductor is subject to mechanical flexing during the coil-fabrication process. The inherent stiffness of this conductor probably results in localized high stresses and is therefore 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 87 thermal cycles and 9262 hr at 1300°F in a total test time of 10,047 hr of a scheduled 20,000.

b. Actuator Performance

After completion of the S8DR design-verification testing, one actuator, SN Dev-013, was continued in test to demonstrate actuator performance at temperature for 20,000 hr or more. Performance of this actuator is normal after a test time of 19,938 hr of which 17,415 have been at 1125°F. These hours do not include the 5,340 of test at 1025°F on mechanical parts before rebuild of the latest S8DR design. A total of 251 thermal cycles has been accumulated of which 127 have been startup. A startup cycle, made in conjunction with an alternate thermal cycle, consists of operating the actuator at rate, speed, and current continuously for 25 min before thermal ramp and after maximum temperature is attained.

The extended test run consists of the following:

- 1) 3000-hr endurance run at 1125°F (completed).
- 2) 2000-hr dwell at 1125°F (completed).
- 3) 3000-hr endurance run at 1125°F (1080 hr completed).
- 4) 2000-hr dwell at 1125°F.
- 5) 30 thermal cycles at 1125°F.

This testing will result in a total of 300 thermal and 150 startup cycles as well as approximately 21,500 hr of testing at 1125°F.

c. Actuator Design

The design effort, targeted towards completion and review of the design layout near the end of this fiscal year, is continuing at a minimal level. The

previously described⁽³⁾ mockup actuator, designed to provide an interface for the control-drum drive-train test discussed in Section VI-D, is being fabricated.

C. CONTROL-DRUM BEARINGS

1. S8DR Design Verification

A topical report covering the development and history of the SNAP 8 control-drum bearings is being prepared for publication.

2. Zirconium Hydride Reactor Bearings

The 10,000-hr milestone has been reached on the ultrahigh vacuum (UHV) high-temperature bearing simulated life test. The bearing set with Al_2O_3 -plated Ta-10W vs carbon-graphite friction surfaces has been subjected to continuous operation at 1500°F and 10^{-8} torr for 10,024 hr. Test sequences include two 1000-hr dwell periods and 8000 hr with periodic bearing rotation through the equivalent of two full-in to full-out drum cycles every 80 hr. Figure VI-1 summarizes bearing friction-torque through the 10,000 hr. Maximum friction-torque of 7.1 in.-lb, occurring initially and three times preceding the first 1000-hr dwell, has not been approached in the last 4000 hr. The test is being continued to 15,000 hr total with bi-weekly bearing rotation.

A material-evaluation test to study friction and wear of Al_2O_3 vs Al_2O_3 bearing couples has been begun. As shown in Figure VI-2, 435 hr at 1500°F and 10^{-8} torr have been accumulated on two identical test couples. Initial data scatter decreased as the test progressed and both test couples stabilized with friction coefficients of about 0.2. While a considerable difference in the ambient-friction coefficients occurred during the thermal cycle, both values decreased and agreed closely after temperature was returned to 1500°F . Following the thermal cycle (at 435 hr) a two-week dwell at 1500°F was started and is currently in progress.

D. 4π -Shieldable Reflector

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

Figure VI-1. Advanced Zirconium Hydride Bearing Simulated Life Test Friction Torque at 1500°F 10^{-8} torr Al_2O_3 vs Carbon-Graphite

Figure VI-2. Advanced Zirconium Hydride Bearing Material Evaluation Test Friction Coefficient at 1500°F 10^{-8} torr Al_2O_3 Against Al_2O_3

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Figure VI-3. Loose Cladding Rivets

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Figure VI-4. Drum Assembled in Vibration Fixture

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1. Drum Development

Work was continued on Development Drums No. 1 and 2, and activities were begun on the drum critical and thermal-gradient tests.

a. Development Drum No. 1

As reported previously⁽²⁾, the drum was subjected to ten thermal cycles from 1300 to 400°F or less in a vacuum of approximately 10^{-5} torr. Post-thermal cycle visual examination of the drum showed no signs of damage to the drum; however, it was slightly discolored as shown in Figure VI-3. The light-colored rectangular areas on the drum absorber are where thin thermocouple holding strips were removed from the drum after thermal cycling. There was no audible sign of loose parts in the drum when shaken manually. Measurements of drum-shaft alignment indicated no warping of the drum as a result of thermal cycling. Upon completion of the drum-alignment measurements it was noticed that one of the cladding rivets had loosened and was protruding from the drum. Further examination showed that four of 28 rivets had loosened. Figure VI-3 shows two loose rivets protruding from the drum before all four were removed.

Design specifications for this drum required special cladding rivets made of T-222 alloy. However because of the delayed delivery, standard stainless-steel rivets were substituted to complete the drum fabrication as scheduled. The thermal expansion of the stainless-steel rivets at 1300°F is three times that of the Ta-10W absorber in which they were installed. It is postulated that this greater expansion resulted in slight yielding of the rivet at higher temperatures, and greater contraction on cooldown caused the rivets to loosen. T-222 alloy has similar properties to Ta-10W, and its use for the reference design rivets should eliminate this problem. The drum was assembled and instrumented in a vibration fixture as shown in Figure VI-4. A bellows was used in the assembly to simulate the bellows-type coupling in the drive train. Nylon cord was tied around the drum in four places to prevent the cladding from being damaged in case additional cladding rivets became loose.

The drum was subjected to a 1-g sinusoidal sweep from 10 to 2000 hz in each of three directions as shown in Figure VI-4. The 1-g input was applied at the control accelerometers on the bearing housing shown in the figure. Natural frequencies of the drum and its transmissibilities as determined by this test are given in Table VI-1.

TABLE VI-1

DRUM NATURAL FREQUENCIES AND TRANSMISSIBILITIES

Axis	Natural Frequency (Hertz)	Transmissibility
Vertical	85	4
Lateral	48	8
Longitudinal	60	3.3

These data are significant in that the natural frequencies and transmissibilities determine the design loads on the drum.

Next the drum was subjected to a 3-min random-vibration test from 20 to 2000 Hz in each of the three axes. The acceleration spectral density for the input to the control-bearing housing is shown in Figures VI-5, -6, and -7 as a function of forcing frequency for the different axes. Table VI-2 shows the calculated peak acceleration g-response of the drum at the drum peak-response frequencies.

TABLE VI-2

CALCULATED LOADS ON DRUM DURING RANDOM VIBRATION

Location of Accelerometer	Vertical Axis		Lateral Axis		Logitudinal Axis	
	Frequency at Peak Responses, (hz)	Peak Acceleration, (g)	Frequency at Peak Responses, (hz)	Peak Acceleration, (g)	Frequency at Peak Responses, (hz)	Peak Acceleration, (g)
End of drum near control accelerometer	73	1.79	56	2.66	75	0.80
	400	3.83	280	2.7	No second peak	
Middle of drum	90	3.6	52	2.33	70	0.81
	300	2.5	No second Peak		No second peak	
End of drum opposite control accelerometer	90	3.42	56	2.25	70	0.81
	300	5.13	260	2.33	120	1.84

Figure VI-5. Input to Drum Housing During Longitudinal Random Vibration

Figure VI-6. Input to Drum Housing During Lateral Random Vibration

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Figure VI-7. Input to Drum Housing During Random Vibration in the Vertical Direction

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The maximum load on the drum during random vibration was therefore 5.13-g along the vertical axis.

Post-vibration examination of the drum showed no visual damage or loosening of additional cladding rivets. Measurements of shaft-alignment indicated no significant warping or bowing of the drum. The drum bearings appeared undamaged. The drum weighed 80 lb and the center of gravity of the drum was located 0.706-in. from the shaft center line corresponding closely with the calculated value of 0.704-in. Manual shaking of the drum gave no audible sign of any loose parts inside the drum.

The drum was next used in some critical experiments during which the drum cladding was removed. Upon removal of the cladding it was found that one of the BeO blocks had cracked as shown in Figures VI-8 and -9. The small cracked pieces of the BeO block were wedged tightly in place and the critical experiments were continued.

After completion of the critical tests the drum was examined to determine the cause of the BeO fracture. Figure VI-10 shows the details of the BeO block fracture after disassembly. The main cause of the fracture is attributed to the BeO-block locating-dowel pin's (Figure VI-10) being frozen in the absorber and protruding 0.066-in. higher above the absorber than normal. The pin was designed to be a free fit in the absorber as well as in the BeO. As shown in Figure VI-11, there was a possibility of interference between the slight step in the bottom of the hole in the BeO and the bevel on the top of the pin. It was also found that the band rivets had not completely filled the holes in the band thus permitting possible loss in band tension. A sufficient loss in tension could have permitted impacting between the bottom of the BeO hole and the pin during vibration, fracturing the block. The other five blocks appeared undamaged. The dowel pins and band rivets are being redesigned to prevent a recurrence of BeO fracture.

b. Development Drum No. 2

The design of this drum was completed and procurement begun in the last reporting period⁽²⁾. Fabrication of all drum components is expected to be completed by June 1, 1970. Processing of the Nb-1Zr claddings at Oak Ridge National Laboratory with oxygen-inhibiting coatings is in progress.

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Figure VI-8. Drum with Cracked BeO Block

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Figure VI-9. Close-Up of Cracked BeO Block

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Figure VI-10. View of Fractured BeO Block

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Figure VI-11. Cross-Section Through BeO Hole and Pin

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The following design changes are being made to prevent recurrence of BeO fracture:

- 1) Based on the relatively low equivalent g-force on Drum No. 1 during the random-vibration test, the band-tension requirement was reduced from 70 to 46 lb.
- 2) The bandwidth was decreased and its thickness increased from 0.010 to 0.040-in. to balance the design margins for tensile stress and bearing stress at the rivets.
- 3) The diameter of the band rivets was increased from 5/32 - to 1/4-in. diam and the rivet changed to a type designed to completely fill the hole. This will prevent loss of band tension due to slippage at the rivet. To preclude drilling into the B₄C slugs in the absorber, only one rivet is used at each end of the band instead of the two used on Development Drum No. 1.
- 4) The locating pins for the BeO blocks were spaced farther apart and the BeO blocks shortened slightly to reduce block rotation around the pins and to prevent contact between adjacent blocks.
- 5) The top of the locating pins was designed to make it impossible for the step in the bottom of the BeO hole to contact the pin.
- 6) A requirement was added on the assembly to positively check the proper insertion of the dowel pins in the absorber.
- 7) The dry film lubricant was added to the dowel pins to prevent them from freezing in the absorber.

c. Drum Thermal-Gradient Test

A study was started to determine the feasibility of conducting a thermal-gradient test of the control-drum assembly in the STIR (Shield Test Irradiation Reactor) facility. The objective is to simulate the operational gradients within the drum, using the STIR core as the nuclear heat source. Preliminary analyses show the heat-generation rates of the STIR should be similar enough to that of the ZrH Reactor for test purposes. A more rigorous nuclear-heat generation-rate analysis will be conducted when the preliminary design of the test fixture is completed.

2. Materials Studies

The materials-study effort consists of six subtasks: (a) Material Selection, (b) Material-Compatibility Tests, (c) Emittance Tests, (d) Coating Proof tests, (e) Coating-Evaluation Tests, and (f) Drywell Coating Application Development.

a. Material Selection

Material selection has remained unchanged from the last report⁽²⁾; however, there is some uncertainty as to what coating the oxygen barrier will be as explained under the section on coating proof tests.

b. Material-Compatibility Tests

The bare-sample material-compatibility test on the one remaining stack was continued at 1800°F in the low 10^{-6} -torr range pressure with 6424 hr completed at elevated temperature.

Preparation for the second series of material-compatibility tests is continuing. The samples on the tests will be coated with the reference oxygen-inhibiting and emittance coatings where applicable. Tensile-test and emittance samples are to be included in the test stack. Test-sample fabrication is nearly completed but the proper coatings remain to be applied.

c. Emittance Tests

Emittance tests on cold-pressed and sintered Thermalox-995 grade BeO were completed. Surface finish of the samples varied from 25 to 40 μ in. rms. The tests were made at room temperature to 2000°F at a pressure of 10^{-3} torr or less. The measured emittance values, shown in Table VI-3, indicated a significant reduction between 1500 and 2000°F.

Emittance tests on boron carbide and UOX + 0.5% MgO grade BeO were started.

d. Coating Proof test

Fabrication of all of the proof test samples and the CVD application of molybdenum and tungsten coatings by Oak Ridge National Laboratory (ORNL) were completed. The CVD coating processed with a molybdenum hexafluoride gas failed to produce a satisfactory coating of Mo directly on the substrate. (The preferred molybdenum pentachloride gas was not available in time.) By

TABLE VI-3
TOTAL NORMAL EMITTANCE OF THERMALOX-995 BeO

Sample No. and Location	Emittance Units at Temperature (°F)				
	100	500	1000	1500	2000
1, light side	0.68	0.60	0.74	0.68	0.48
2, ↓	0.67	0.61	0.72	0.70	0.47
3, ↓	0.67	0.60	0.75	0.65	0.48
4, ↓	0.67	0.63	0.77	0.64	0.47
5, gray side	0.68	0.63	0.77	0.63	0.47
6, ↓	0.67	0.66	0.72	0.64	0.47
7, ↓	0.67	0.65	0.74	0.67	0.48
8, ↓	0.68	0.66	0.72	0.68	0.47

applying a flash CVD coating of W on the substrate first, the Mo could then be applied in a satisfactory manner. It was found a satisfactory coating of W could be applied directly on the substrate provided the substrated surfaces were grit-blasted first. Tungsten was considerably easier to apply than Mo and for this reason W is preferred, provided it is an adequate O₂ barrier.

Process development for plasma-spraying the iron-titanate emittance coating over the CVD Mo and W was started. It was found necessary to lightly grit-blast the CVD coatings to obtain good iron-titanate coating adhesion. Coating of the test samples with iron titanate is in progress and start of the 200-hr coating proof tests at 1800°F and 50-micron air pressure is scheduled before May 1970.

e. Coating-Evaluation Test

A preliminary test procedure was completed. The procedure will be finalized after the coating proof tests above are completed and the O₂ barrier coating is selected. Fabrication of the uncoated test samples was completed.

f. Drywell-Coating Application Development

This will be started later.

3. Drum-Drive-System Development

The drum-drive-system components are still being fabricated. A thermal analysis of the thermal-test fixture was started and the mechanical design of the fixture is nearing completion.

E. SHIELD

1. Materials Studies

The 10,000-hr test 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 was completed. Weighed amounts of C had been added to the capsules at the start of the test in amounts sufficient to saturate (or in some cases half-saturate) the Pb at the test temperatures of 1300 and 1500°F.

The evacuated quartz tubes were broken open and the steel capsules removed. One end of each capsule was removed for metallographic examination of the Pb-steel interface to determine the extent of decarburization. The other end was removed for metallographic examination of the Pb-steel weld-joint interface. A tubing cutter was used to remove the ends of the 1020-carbon-steel 0.065-in. - wall capsules; however, it was necessary to use a lathe to cut the ends from the heavier-wall (0.109-in.) Croloy-2-1/4 steel capsules. To minimize the introduction of impurities into the capsules no cutting fluids were used.

Analyses of Pb samples removed from the body of the Pb specimen indicated 26-ppm carbon, similar to the earlier analyses. However, analysis of scrapings from the surface of the Pb revealed the C content to be about 340 ppm and analysis of a small amount of loose material, consisting of a fine powder and small droplets of Pb, from one capsule showed a C content of about 3200 ppm.

Earlier analyses for C in the Pb removed from the 2500- and 5000-hr capsules had failed to find concentrations of C greater than those found in untested Pb. These samples had been removed from capsules cut open by either a dry-radiac or wet-metallographic saw. Both cutting methods introduced impurities into the capsule which necessitated that the Pb samples be cleaned with an acid rinse before analysis. It had been concluded that on cooling to room temperature the C had precipitated out in a thin layer on the surface of the Pb and was lost during the cleaning operation.

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The presence of C in these 10,000-hr samples confirms that the C was present in the capsule, imbedded in or lying on the surface of the Pb as a solid material, and not present in the capsule as a gas, such as carbon monoxide or carbon dioxide produced by the reaction of residual amounts of lead oxide in the Pb and the C.

Additional analyses of the Pb for the Fe leached from the capsule, and the Zr and Mg corrosion-inhibitors are in progress.

Analyses of the metallic deposits (Figure VI-12) produced by vaporization of metal from the steel capsule and subsequent deposition onto the quartz for total weight of Fe and Cr agreed very well with the earlier results from the 2500- and 5000-hr tests at 1500°F as shown in Table VI-4. From these data it may be concluded that significant amounts of Fe and Cr will be vaporized from steels during exposure at 1500°F and above in the high vacuum of space. For example the evaporation rate of the 1020 carbon-steel capsule after 10,000 hr at 1500°F (total loss 1900 mg from a 14-in.² surface area) is calculated to be about 1.3×10^{-2} mg/in.²/hr. The Croloy-2-1/4 steel exhibits a lower evaporation rate at the same condition. Both materials have significantly lower rates of evaporation at 1300°F. Figure VI-13 graphically shows the effect of heating these two alloys in a vacuum at 1500°F for long periods of time.

2. Design and Fabrication Development

a. Liquid-Lead Shield

Fabrication of the previously discussed⁽²⁾ liquid-Pb shield vessel is in progress. The Pb-casting apparatus, the shield-support structure, the heater-reflector assembly, and all other test hardware have been completed.

b. The Lithium Hydride Checkout Shield

Fabrication of the LiH checkout shield and the shield-casting hardware, as described in previous reports⁽²⁾, is in progress.

The Large-Shield Casting Facility, Bldg 042 at Santa Susana, was accepted after a series of successful proof tests, and is now operational. The checkout shield will be the first large pancake-shaped shield cast in this facility.

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Figure VI-12. Metallic Deposits From Evacuated Quartz
Tube Containing Steel Capsule Heated for
10,000 hr at 1500°F

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TABLE VI-4

WEIGHTS OF METALLIC DEPOSITS IN QUARTZ CAPSULES AND EVAPORATION
RATES OF 1020 CARBON STEEL AND CROLOY-2-1/4 STEEL

Alloy	1020 Carbon Steel						Croloy-2-1/4 Steel					
	1300			1500			1300			1500		
Temperature, °F												
Time, hr	2,500	5,000	10,000	2,500	5,000	10,000	2,500	5,000	10,000	2,500	5,000	10,000
Weight Deposits, mg	30.2	82.1	120	1358.1	1723.1	1780	18.5	78.6	68.0	446.6	733	950
Evaporation Rate, mg/in. ² /hr	8.8×10^{-4}	12×10^{-4}	8.6×10^{-4}	4.0×10^{-2}	2.5×10^{-2}	1.3×10^{-2}	5.4×10^{-4}	11×10^{-4}	4.9×10^{-4}	1.3×10^{-2}	1.1×10^{-2}	0.7×10^{-2}

Figure VI-13. Weight Loss of Test Capsules at 1500°F

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 Processing

Integral finned tubes fabricated by chem-milling the OD of 50-mil wall tubing have been received from two vendors. They are being inspected to determine the process capability of this method of fabrication. Hobbing is being considered as a second approach to machining other than conventional milling, and a hobbing cutter head has been ordered. (A hob-cutter is a fluted worm type with form-relieved teeth which is used in a process in which both the cutting tool and the workpiece revolve in a constant relation (angle) to each other.) Hobbing cutters being custom-designed and ground, the best obtainable delivery schedule was two months. In the interim more tubes are being fabricated by conventional milling in an effort to improve the results of the wall-uniformity process and provide some finned tubing for developmental efforts.

Ingot chemistries of two of the three heats of Incoloy-800 currently on order^{*} have been obtained and are given in Table VII-1. Both fall within the imposed titanium and carbon limits. The third heat, actually the first melted, was damaged due to a malfunction in the rolling mill and is therefore being replaced. Initial delivery of the three heats will be in the form of 4-in. -diam bar. The bar will then be extruded to draw shell size (2-in. OD x 1-1/2-in. ID) and subsequently

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^{*}Reported in "SNAP Reactor Programs Progress Report, August-October 1969," AI-AEC-12894

TABLE VII-1

INGOT CHEMISTRY, THREE NEW HEATS INCOLOY-800,
DOUBLE-VACUUM-MELTED BY CARPENTER TECHNOLOGY

Element	High Ductility (Heat K89338)	Nominal (Heat K89339)	High Strength Heat	ST0160NB0038
Carbon	0.004 (0.03 max)*	0.029 (0.02 to 0.06)*	(0.08 to 0.12)*	0.08 to 0.12
Titanium	0.09 (0.05 to 0.15)*	0.25 (0.2 to 0.4)*	(0.4 to 0.6)*	0.4 to 0.6
Aluminum	0.03	0.036		0.4 maximum
Chromium	21.64	21.44		19 to 23
Nickel	33.57	33.54		30 to 35
Manganese	0.72	0.61		1.0 maximum
Silicon	0.29	0.29		0.50 maximum
Phosphorus	0.010	0.010		0.015 maximum
Sulfur	0.006	0.005		0.015
Molybdenum	0.07	0.06		0.15 maximum
Copper	0.04	0.04		0.35 maximum
Cobalt	0.04	0.04		0.20 maximum
Boron	0.001	0.001		0.0005 maximum
Nitrogen	0.017	0.016		0.05 maximum
Iron	Balance	Balance		Balance

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drawn into 50-mil wall tubing. The 50-mil wall tubing will be the starting material for the selected finned-tubing fabrication process.

A new induction-heating unit to be used as the heat source for the fuel-element assembly fixture has been purchased and is being installed. All element-assembly efforts have been curtailed for the past few months because the unit previously used would no longer operate reliably.

2. Fuel Processing

a. Fuel Fabrication

Six normal uranium-alloy billets were final-extruded and rough-cut to length; samples were prepared and submitted for chemical analysis. A limited series of hydriding runs were made to confirm that the modified billet and extrusion dimensions would result in adequate material for cleanup during final machining. In addition it was desirable to assure that no adverse effects resulted from the change to a copper-clad extrusion technique. The hydriding proceeded normally and cleanup to the desired rod diameter during final machining was verified. Based on the data obtained the modified processing dimensions will yield a 50% increase in usable fuel segments from the same quantity of starting raw materials.

b. Burnable Poison

The x-ray fluorescence and diffraction studies of the loosely adhering film found on the fuel rods (initially coated with Sm_2O_3 and Eu_2O_3) and barriers of elements after endurance testing at 1400 and 1500°F were completed.

X-ray fluorescence showed that a smaller amount of Eu was present on the fuel after endurance testing than was initially applied, the ratios of Eu to Sm being reduced from an average of 4.3 to 0.33. Appreciable amounts of Eu along with small amounts of Sm were found on the barrier after endurance testing, indicating that the Eu had been preferentially removed from the coated fuel and been deposited on the barrier. The ratios of Eu to Sm on the barrier varied from 7.0 to 10.4, depending on the temperature of the test.

X-ray diffraction studies revealed appreciable amounts of Eu in the form of Eu_4O_5 along with lesser amounts of Eu in the form of EuAlO_3 on the barrier after endurance testing at 1400°F. The peaks for the 002 face of $\text{BaAl}_2\text{Si}_2\text{O}_8$

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normally found in the barrier were not present. Lesser amounts of Eu along with copious amounts of BaAl_2O_4 were found in the barrier after exposures to temperatures of 1500°F . The sums of the peaks heights for Eu, in the form of Eu_8O_{11} , averaged 58 against 98 for those exposed to 1400°F . This may be explained by either diffusion beyond the depth of detection or by spalling of the loose film.

Based on free energy of formation and glass technology, the Eu_2O_3 is believed to have been reduced by the uranium and/or zirconium in the fuel to EuO. The EuO is believed to have reacted with the $\text{BaAl}_2\text{Si}_2\text{O}_8$ in the enamel forming Eu_4O_5 and EuAlO_3 at 1400°F and with the $\text{BaAl}_2\text{Si}_2\text{O}_8$ and SiO_2 matrix at 1500°F to form Eu_8O_{11} , EuAlO_3 , and BaAlO_4 as the attack became more severe.

Thermal-endurance testing of Element 9174 was terminated after 5100 hr at 1400°F . This is the last of the elements containing vapor-deposited Eu_2O_3 - Sm_2O_3 films. The fuel and SCB-barrier were visually examined after decanning. The adherence of the films was poor but slightly better than that observed in the shorter tests reported in the preceding quarterly. The barrier appearance was similar to that observed in the shorter tests, but except for the areas adjacent to film-free fuel there was no iridescence. The absence of iridescence suggests that the reaction of the film and barrier had proceeded to a greater depth than in the previous tests. However, since the normalized leak-rates of this element exhibited the very slow increase normally observed in the absence of vapor-deposited films, the presence of the film probably did not result in excessive degradation of the barrier. In view of the unsatisfactory results from these films no further study of this element is planned.

A new approach has been undertaken to substantially reduce, if not eliminate, the reactions of europium oxides with the fuel and barrier. The two rare-earth oxides and a third oxide will be vapor-deposited as a compound or solid solution. This may be applied directly to the fuel or sandwiched between layers of the stable third oxide. The compound (or solution) will be formed in situ by simultaneous controlled-vaporization of the three oxides from separate crucibles. Zirconium, titanium, and yttrium oxides are being investigated as the "third" oxide. The first two were selected because the trivalent rare-earth ions are more stable in zirconates and titanates than in the pure rare-earth oxides.

Yttrium oxide is considered promising because it is the most stable of the oxides and therefore should form a good reaction barrier between the rare-earth oxides and fuel on one side of the sandwich, and between these oxides and the SCB barrier on the other side. In addition, the linear coefficients of expansions of Y_2O_3 , Eu_2O_3 , Sm_2O_3 , and fuel are all within the range $9-11 \times 10^{-6}/^{\circ}C$.

Discs of fuel, currently being coated with vapor-deposited "sandwich" films of different compositions, will be formed into diffusion couples with SCB-coated discs of Incoloy-800 and Hastelloy-N for compatibility testing.

3. Fuel-Element Tests

Two Incoloy-800-clad elements were subjected to the random-vibration input specified for the Saturn V Zone IA application in SS696N17001. The elements contained column-buckling devices (CBD's) made with 51-mil 405 stainless-steel wire, segmented fuel, and 0.1-atm helium. The final closures were made with the standard cup-plug. The post-test evaluation of the elements showed that the CBD's buckled during the vibration test, which resulted in significant increases in the hydrogen-permeation rates, as given in Table VII-2.

TABLE VII-2

LEAKRATE HISTORY OF TWO VIBRATION-TEST ELEMENTS

Element No.	As-Measured Leakrate [cc(STP)/hr at 1400°F]		
	As-Fabricated	Post-"A" Ramps	Post-Vibration Test
9323	0.34	0.30	0.89
9327	0.73	0.86	1.63

Upon decanning the elements, internal measurements indicated that the as-assembled end-gap in the elements was about 25 mils, as opposed to the design value of 7 ± 2 mils. This excessive gap is believed to explain the collapse of the CBD's, as the impact loads increase dramatically with increasing axial clearances. Radiographs taken before testing of the elements did not reveal the excessive end-gaps. This could be due to the gaps being distributed between the fuel-segment interfaces within the element, rather than being localized at the fuel-CBD interface.

An effort was subsequently undertaken which: 1) resulted in the design and fabrication of a tool which produces a direct measurement of the required tube fitup length (cup-plug + assembly gap), in contrast to two indirect measurements previously required; 2) demonstrated that our x-ray technique could with proper alignment between x-ray tube and the interface gap be used to measure the 7 ± 2 -mils gap if it is localized to one interface; and 3) demonstrated that the gap can be localized to the CBD-fuel interface.

The seven 15-mil Incoloy-800-clad developmental elements (four double-blind-end and three cup-plug type) continued on test and have now accrued 12,000 hr at 1400°F with an average hydrogen loss of approximately 5%. Since the ramp-heating excursions at 6,000 hr, this group of elements has been subjected to the ramp-heat inputs at 8,000 and 10,000 hr with no effect on the hydrogen leakrates. Curves of the average leakrate and cumulative hydrogen loss are shown in Figures VII-1 and -2, respectively.

The four double-blind-end Incoloy-clad elements containing enriched-uranium rods have now accrued more than 10,000 hr at 1400°F. The average normalized leakrate and cumulative hydrogen loss of this group of elements are shown in Figures VII-3 and -4, respectively. The comparatively poor performance when compared to the group of seven normal U elements is primarily due to one high-leaking element. Excluding this element, the remaining three have a current average normalized leakrate of 0.61 cc(STP)/hr and an average hydrogen loss of 5.3%. This group is currently being subjected to the "A"-ramp thermal cycles.

B. IRRADIATION TESTS

1. NAA-120-4

The draft of the topical report on operation and post-irradiation examination was completed and is being reviewed.

2. NAA-121-1

Reports on the operations and the post-irradiation examination were issued. No further work is planned until next fiscal year, when burnup analyses, some fuel analyses, and a topical report are to be completed.

Figure VII-1. Average Normalized Leakrate of the Seven Incoloy-800-Clad Developmental Fuel Elements (four double-blind-end type and three cup-plug)

Figure VII-2. Cumulative Average Hydrogen Loss for the Seven Incoloy-800-Clad Developmental Fuel Elements

Figure VII-3. Average Normalized Leakrate of the Four Enriched Uranium Double-Blind-End Incoloy-800-Clad Developmental Elements

Figure VII-4. Cumulative Average Hydrogen Loss for Four Enriched Uranium Double-Blind-End Incoloy-800-Clad Developmental Elements

3. NAR-141-1

Work on this experiment was cancelled due to program redirection.

4. NAA-83 and NAR-83a. Incoloy-800

The Incoloy-800 program is being carried out to evaluate the potential of this material as fuel cladding for ZrH reactors. This is being accomplished by measuring strength and ductility characteristics in simulated reactor environments. The program encompasses: stress-rupture tests in helium of specimens irradiated at different temperatures; in-pile stress-rupture tests both in He and NaK; out-of-pile stress-rupture tests of controls in both He and flowing NaK; bend-fatigue tests on irradiated and unirradiated specimens; and tensile tests of unirradiated specimens. A summary of the tests to be made was given in the preceding quarterly.⁽²⁾

Stress-rupture data from irradiated samples indicate far greater rupture lives than was predicted from a compilation of literature data.⁽⁴⁾ The test data are shown in Table VII-3. A least-squares curve fit of these values yielded the following expression:

$$\text{Log } t_r = 34.62 - 7.97 \text{ Log } \sigma$$

where t_r = rupture life and σ = hoop stress. The highest stress used on the remaining 16 specimens was 6900 psi. Originally estimated to cause rupture in 2000 hr, two specimens tested at this stress have already accrued 6009. Applying the above expression, these two specimens (A-41 and A-29) would now be expected to rupture at 10,000 hr. In view of this analysis the decision was made to modify the stresses on 14 of the remaining 16 specimens.

TABLE VII-3
NAA-83-1 EXPERIMENT STRESS-RUPTURE-LIFE DATA USED AS A BASIS
FOR ALTERING TEST MATRIX ON REMAINING SAMPLES

Sample No.	Environment	Hoop Stress (psi)	Rupture Life (hr)
A-3	In-pile stress rupture	10,680	300
A-9	↓	10,680	386
A-26	↓	9,020	1200
A-39	↓	↓	2336
A-11	1 reactor cycle	↓	938
A-46	↓	↓	1109

Specimens A-41 and A-29 were maintained at the original stress; the stresses on the remainder were increased to produce rupture in 300, 1000, 5000, and 10,000 hr. The status of the remaining specimens is shown in Table VII-4.

The rupture-life data will be evaluated according to the method proposed by C. R. Kennedy.⁽⁵⁾ This method involves a summation of rupture-life fractions, where

$$\frac{\Delta t_1}{t_{r1}} + \frac{\Delta t_2}{t_{r2}} + \frac{\Delta t_3}{t_{r3}} + \dots + \frac{\Delta t_k}{t_{rk}} = 1$$

Δt_1 , Δt_2 , and Δt_3 = the times when σ_1 , σ_2 , and σ_3 were acting on the sample, and t_{r1} , t_{r2} , and t_{r3} = expected rupture times at σ_1 , σ_2 , and σ_3 , respectively. Referring to Table VII-4, the rupture life fractions ($\Delta t_1/t_{r1}$) for these samples ranged from ~ 1.5 to $\sim 10\%$. Since these fractions are so low, the strain data will be evaluated as if all the strain had occurred at the higher stress.

The data accrued to date on irradiated and control (helium) specimens are shown in Figure VII-5. Data from NaK stress-rupture tests and controls are shown in Figure VII-6. Strain data from all tests are shown as a function of strain rate in Figure VII-7.

Figure VII-5. Incoloy-800 Biaxial Stress-Rupture Tests at 1300°F in Helium

TABLE VII-4
STATUS OF REMAINING NAA-83-1 IRRADIATED STRESS-RUPTURE TESTS
(INDICATES PRIOR AND REVISED TEST CONDITIONS)

Sample Number	Number of Reactor Cycles	Initial Stress, (σ_1 , psi)	Time on Test at σ_1 , Δt_1 (hr)	Adjusted Stress, (σ_2 , psi)	Predicted Rupture Life (hr)	Effective* Rupture Life (hr)	Measured $\Delta D/D$ (%)	Average Strain Rate $\frac{\% \Delta D/D}{t_2}$
A-5	1	5710	5032	10,680	300	472	6.3	1.46×10^{-2}
A-22	1	5710	5032	10,680	300	531	3.34	0.69×10^{-2}
A-53	1	4930	4000	9,020	1,000	1322 [†]		
A-4	1	4930	4000	9,020	↓	900	5.52	6.26×10^{-3}
A-31	2	5710	2160	9,020	↓	437	1.40	3.34×10^{-3}
A-36	2	5710	2160	9,020	↓	642	2.01	3.26×10^{-3}
A-6	1	5710	5032	7,600	5,000	1322 [†]		
A-30	1	5710	5032	7,600	↓	↓		
A-12	1	4930	4000	7,600	↓	↓		
A-18	1	4930	4000	7,600	↓	↓		
A-24	2	4930	2132	7,600	↓	↓		
A-34	2	4930	2132	7,600	↓	↓		
A-41	1	6930	6009	No change required	10,000	6009 [†]		
A-29	1	6930	6009	No change required	↓	6009 [†]		
A-17	2	4930	2132	6,930	↓	1322 [†]		
A-21	2	4930	2132	6,930	↓	1322 [†]		

*Actual rupture life at σ_2 + correction for Δt_1 at σ_1

[†]Intact specimen still operating

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Figure VII-6. Incoloy-800 Biaxial Stress-Rupture Tests in Flowing NaK

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Figure VII-7. Incoloy-800 Strain vs Strain Rate

The NAR-83-2 (1400°F) completed one cycle in the Oak Ridge Reactor and was discharged. This experiment consists of 10 stress-rupture samples to be tested in He at 1400°F at the Atomics International Hot Laboratory. The NAR-83-3 (1200°F) and -4 (1100°F) experiments were inserted in the ORR and each accrued about 200 hr. These two experiments consist of 10 stress-rupture samples each and 40 fatigue-bend strip specimens.

Tensile testing on 72 fully processed, simulated-processed, and as-received tubing samples from Heat No. HH7531A has been completed. Evaluation of these data has begun.

b. Hastelloy-N

Stress-rupture data obtained on SCB-coated Hastelloy-N are given in Table VII-5 and Figure VII-8. Data on both specimens irradiated in the NAA-83 experiment and the unirradiated controls are included. The rupture-life data continue to agree well with previous data.

Figure VII-8. Hoop Stress vs Rupture Life for Hastelloy-N at 1400°F, Comparison of NAA-83 Data with ORR-S-2 and ORR-S-3

TABLE VII-5
HASTELLOY-N 1400°F BIAxIAL STRESS-RUPTURE DATA
(NAA-83 Experiment)

Sample No.	Heat No.	Environment	Nominal Hoop Stress (psi)	Rupture Life (hr)	$\Delta D/D$ (%)	Average Strain Rate (in./in. ⁵ hr x 10 ⁻⁵)
B-1	281-4-0143	In-Pile Test	7,950	1013*	1.1	1.1
B-3	281-4-0143	↓	7,950	842	2.0	2.4
C-6	5911	↓	8,750	387	0.4	1.1
C-11	↓	↓	8,750	509	0.9	1.7
C-3	↓	Ex-Pile Test	5,500	Unruptured after 6239		
C-21	↓	↓	5,500	↓		
C-1	↓	↓	4,700	↓		
C-10	↓	↓	4,700	↓		
C-7	↓	Thermal Control	7,800	4061	0.9	0.2
C-12	↓	↓	↓	5867	0.8	0.1
C-15	↓	↓	↓	4375	0.4	0.1
C-4	↓	↓	9,000	2082	2.81	1.4
C-5	↓	↓	↓	2216	2.2	1.0
C-8	↓	↓	↓	1916	1.3	0.7
C-2	↓	↓	10,500	983	1.8	1.8
C-14	↓	↓	↓	1627	4.7	1.6
C-17	↓	↓	↓	1207	4.0	3.4

*This specimen was unruptured at the end of the irradiation.

VIII. AEROSPACE SAFETY

A. IMPACT-DISASSEMBLY STUDIES

1. Analysis

This program is directed toward predicting the post-impact configuration of the reactor system after impact with various earth media. As a part of this task a study was made to determine if the post-impact configuration of the reactor system could be predicted with confidence from the results of model impact tests and, if so, to develop the laws governing the design and testing of these models. The results of the analytical investigation showed that it is indeed possible to predict the post-impact configuration of the reactor system based on those of reactor-system scale models. The percentage of damage done to the model would be identical to that for the prototype if the modeling laws are adhered to in the design and testing of the model. The laws governing the design and testing of the models are that the model must be geometrically similar and constructed of the same materials as the prototype, and that the impact velocity and target material must be the same for the model as the prototype.

The dimensionless analysis was based on the Buckingham π -theorem. The application of this theorem to model-testing consists of first, and most important, determining all the pertinent variables that affect the test results. These are then grouped into dimensionless and independent terms called π terms. The number of these required is equal to the number of pertinent variables involved minus the number of basic dimensions that comprise the pertinent variables. The basic dimensions are force (F), distance (L), and time (T).

The pertinent variables in determining the post-impact configuration of the reactor system are deformation (D), radius of cylinder (R), cylinder-wall thickness (t), length of cylinder (ℓ), material density (ρ), modulus of elasticity (E), ultimate impact stress (S), concentrated mass (M), location of concentrated mass (ℓ'), and impact velocity (V). These variables were arranged into the following seven dimensionless π terms. D/ℓ , t/ℓ , R/ℓ , ℓ'/ℓ , S/E , $\rho V^2/E$, and $MV^2/S\ell^3$.

DIAMETER (in.)	0.5	1.0	2.0
LiH LENGTH (in.)	2.0	4.0	8.0
TUBE THICKNESS (in.)	0.012	0.025	0.049
ROD DIAMETER (in.)	0.125	0.25	0.50
STEEL CAP THICKNESS (in.)	0.75	1.5	3.0

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Figure VIII-1. Composite Impact-Test Specimens

The model must be designed and tested so that the π terms of the model equal the corresponding ones of the prototype. Model-testing for impact loadings requires that the model materials and impact velocities be identical to those of the prototype. The model is then a true model and will give accurate information concerning the behavior of the prototype. The target material into which the model and the prototype impact must also be the same. The impact stresses in the prototype and the model will be equal, which can be verified by equating the impact energies of the model and the prototype to their respective strain energies. The analysis was based on the model and the prototype being homogeneous bodies; however, the modeling laws developed apply to composite structures as well.

2. Impact-Test Program

A series of impact tests was undertaken on LiH pads, stainless-steel cylindrical shells, and composite models of stainless steel and LiH. The objectives of these tests were to (1) determine the energy-absorption capabilities of thin-walled cylindrical shells and the energy required to pulverize LiH pads at various impact velocities, and (2) verify the modeling laws developed for predicting prototype post-impact deformation from scale-model testing. The specimens tested were similar in construction and geometry to reactor components. The energy-absorption characteristics obtained from these tests will be used to update the analytical model developed for predicting the reactor post-impact configuration described in the last report.

To verify the modeling laws each type of specimen to be tested was fabricated in at least two sizes, each a scale model of the other. Also, the two composite specimens described in Figures VIII-1 and -2 were fabricated. The specimens in Figure VIII-1 are not meant to simulate any of the reactor components but are designed strictly to verify the modeling laws which apply to a composite structure. The specimens described in Figure VIII-2 are approximate scale models of the reactor tungsten shield, reactor vessel, and control drums fabricated from 321 SS. By comparing the post-impact deformations of the scale models of the various specimens tested, the accuracy of the modeling laws will be established.

During this reporting period tantalum-10W specimens were impact-tested at impact velocities of 350, 600, 700, and 850 ft/sec. At each velocity the specimen exhibited plastic deformation with no evidence of fracture indicating the

SPECIMEN NUMBER	D (in.)	ℓ (in.)	L (in.)	t_1 (in.)	t_2 (in.)	t_3 (in.)	H (in.)	P (in.)	R (in.)	C (in.)	O (in.)
1	2.0	3.94	2.5	0.049	0.032	0.020	1.78	0.25	0.375	0.650	1.75
2	1.0	1.97	1.25	0.028	0.016	0.010	0.875	0.125	0.188	0.325	0.875

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Figure VIII-2. Impact-Test Specimens

material is ductile and capable of absorbing considerable energy. The K values (i. e. the ratio of the energy absorbed dynamically to the energy under the stress-strain curve) varied from 1.75 at 600 ft/sec to 3.2 at 800 ft/sec. Figure VIII-3 shows the post-impact configuration of the Ta-10W specimens.

B. RE-ENTRY STUDIES

1. Orbital-Decay Analysis

A study was made of the possibility of prolonging the orbital lifetime of the ZrH reactor power system by removal of the radiator soon after the system is separated from the space station. This would be an alternative to the use of a booster package, involving rocket motors and guidance equipment. Aerodynamic drag and moment coefficients in the free-molecule flow regime were calculated for the various re-entry body configurations. Ballistic coefficients, $W/C_D A$, were calculated as functions of the angle of attack of each configuration, and the corresponding orbital lifetimes were then determined.

Figure VIII-4 shows the free-molecule ballistic coefficients for the configurations with and without the radiator and without the power-conversion equipment gallery and aft shield. At the gravity-gradient stable attitude of 90° the ballistic coefficient of the configuration without the radiator is approximately 200 lb/ft^2 , or about ten times that of the configuration with radiator (Configuration A). It follows from this that the orbital lifetime can be significantly increased by dropping the radiator.

The relationship between ballistic coefficient and orbital lifetime, starting from circular orbits 270 n. mi above the earth, is shown in Figure VIII-5. The spread between the maximum and the minimum lifetime at any one of the ballistic coefficient is due to the cyclic variation in the solar activity, as discussed in the previous progress report.⁽²⁾

On the basis of the above the natural-decay orbital lifetime of the complete system with radiator would be from 1.2 to 8 yr, depending upon the year in the solar-activity cycle when separation from the station occurs, and the orbital lifetime of the system without radiator would be from 37 to 44 yr. By way of comparison if the complete system were boosted to an orbital altitude of

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Figure VIII-3. Ta-10W Post-Impact Specimens

Figure VIII-4. Ballistic Coefficients and Orbital Lifetimes for the Zirconium Hydride Nuclear Power System

500 n. mi — which would require a double firing of the rocket cluster, aligned to produce a velocity increment of 375 ft/sec at each firing — the lifetime would be extended to about 800 yr.

Figure VIII-5. Orbital Lifetime vs Ballistic Coefficient for Circular Orbits at 270 n.mi

2. Atmospheric Re-entry Analysis

a. Re-entry-Body Moments and Oscillations

The centers of mass and moments of inertia in pitch of the various re-entry-body configurations were recomputed, incorporating up-dated values of the weight estimates. The results of these computations are shown in Table VIII-1. Aerodynamic pitching-moment coefficients in continuum flow were also recomputed, including the effects of the control-drum drive motors ahead of the shield. In order to make the moment data more easily compared for the different configurations, the coefficients for each have been multiplied by the respective reference areas and diameters and divided by the respective moments of inertia. Figure VIII-6 shows the resulting parameters as functions of the angle of attack. The arrowheads on each curve illustrate the direction in which the aerodynamic

TABLE VIII-1
 DIMENSIONAL AND INERTIAL CHARACTERISTICS OF
 THE RE-ENTRY-BODY CONFIGURATIONS

Configuration	A ₀	A ₁	A ₂	B	C
Description	No-boost package	Before booster firing	After booster firing	After radiator separates	After gallery separates
Mass, W, (lb)	22,222	26,024	23,776	18,629	7,183
Center of mass, ft [*]	-7.90	-14.10	-10.55	-3.75	-1.06
Moment of inertia in pitch, I, (slug-ft ²)	78,664	271,555	165,671	5,084	608
Reference diameter, D, (ft)	11.2	11.2	11.2	5.96	4.68
Reference area, A, (ft ²)	98.52	98.52	98.52	27.90	17.20
Mass/area, W/A, (lb/ft ²)	225.6	264.1	241.3	667.7	417.6
A · D/I, ft/slug [†]	0.01403	0.00406	0.00666	0.0327	0.1323

*Measured from center of the reactor core.

†This parameter is used in the analysis of oscillation frequencies.

Figure VIII-6. Pitching Moment, Parameter for Zirconium Hydride Nuclear Power System "Re-entry" Body Configurations

moment tends to drive the body. For example, Configuration A₂ is strongly stable at an angle of 0° and unstable at 180°. Configuration B is weakly stable at 180°; C moderately stable at 0° and weakly stable at 180°.

Oscillation amplitudes and frequencies of Configuration A₂ were calculated as functions of time along the natural-decay re-entry trajectory, assuming arbitrary maximum amplitudes of oscillation at high altitude and terminating the calculations at an altitude of 215,000 ft where the radiator is expected to separate from the system. The results of these calculations, shown in Figure VIII-7, indicate that the oscillations at the time of radiator separation will be less than about 20° either side of the nose-first attitude.

Calculations of aerodynamic and inertial loads on the structure indicate that failure of the transition cone between the radiator and the power-conversion system will occur at the peak of one of the oscillations, releasing Configuration B with an unpredictable initial-rotation rate. The outer skin over the power-conversion equipment gallery will fail nearly simultaneously with failure of the transition cone, but some of the internal plumbing may hold the system together for a longer time. Escape of coolants from the system may perturb its oscillations. Therefore, the flight attitude of the final configuration at impact is difficult to predict. However, these torques provided by structural failures and coolant release in conjunction with the stability of the remaining reactor-shield assembly indicate that the most probable attitude is tumbling.

Calculated impact velocities at sea level for various assumed flight attitudes are shown in Table VIII-2.

TABLE VIII-2
CALCULATED IMPACT VELOCITIES AT SEA LEVEL

Flight Attitude	Final $W/C_D A$ (lb/ft ²)	Impact Velocity (ft/sec)
Nose-first	720	820
Tail-first	650	780
Side-on	450	630
Tumbling	500	660

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Figure VIII-7. Oscillatory Amplitudes and Periods
During Re-entry of Configuration A₂

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Figure VIII-8. Calculated Depths of Ablation of Lithium Hydride Shield During Re-entry

b. Lithium Hydride Ablation Analysis

Total depths of ablation were calculated for the natural-decay and deboost trajectories, assuming that the shield is initially hot. This simulates the low-probability accident case of the reactor operating at the time of re-entry. The results are shown in Figure VIII-8. As expected the depth of ablation is considerably greater than for the case of a cold shield, because the heat-absorption capacity of the hydride is significantly reduced by preheating it.

The heat-absorption capacity of lithium hydride used in the ablation calculations included a sensible-heat capacity of 1700 Btu/lb between room temperature and the melting point, and latent heat of fusion of 1250 Btu/lb, making a total heat capacity of 2950 Btu/lb. An examination of the computed ablation rates and net heat input rates along the trajectory showed that the effective heat of ablation over the period of significant heating was 2580 Btu/lb. The difference between this figure and the value of 2950 Btu/lb given above is apparently due to the effects of conduction within the material and to the preheating which occurs before peak heating.

The NASA-Ames Research Center made available to us data from a series of arc-jet tests of LiH specimens. These data showed that the hydride ablated smoothly, without cracking or spalling, and that its effective heat of ablation varied from 2100 to 3500 Btu/lb, depending upon whether the material was pure hydride or whether it had an internal matrix of steel wool or steel honeycomb. Plans have been made to use our computation methods with NASA's test conditions to try to establish a correlation.

The possible effects of oxidation of the LiH were evaluated by considering that all of the O_2 which diffuses through the boundary layer reacts with the melting surface. If all of the heat of the reaction were added to the aerodynamic heating, the total heat input to the surface would be 29% greater than without oxidation. Balanced against this increased heating due to oxidation are other effects which have not yet been analyzed. For example, the presence of a moving liquid layer on the surface changes the boundary-layer profiles, which tends to reduce the heat-input rate. The presence of reacted gases in the boundary layer has a similar effect. Further the moving layer carries molten LiH and much of the oxidation reaction away from the re-entry body.

All studies to date indicate intact re-entry survival of the reactor, surrounding gamma shield and a significant portion of the LiH shield. While detailed studies remain to be completed on oxidation, spalling, etc. the design of the shield and its physical properties make it effective re-entry protection.

C. INTRINSIC SUBCRITICALITY

The checkout of the methodology for comparing the experimentally calculated prompt-temperature coefficient of reactivity with theoretical calculations was completed. Preliminary checks indicate that the two results may agree within experimental error for some configurations. We expect to report the completed results in the next report period.

The method used is based on the APC and APCTC codes. First, the value of k_{eff} (before the transient) is calculated using APC. This is an R-Z consistent-buckling transport code based on ANISN. AI's 28-group SNAP cross-section library is used, with 15 fast and 13 thermal-velocity groups. The APC code iterates between R- and Z- direction calculations until a consistent set of fluxes, bucklings, and k 's are obtained.

The APCTC code calculates k_{eff} after the transient. The energy deposited during the transient is distributed spatially according to an input power profile, and spatial temperatures, temperature-modified cross-sections, and densities are calculated. The cross-sections take into account both Doppler and thermal-spectral effects. The density changes are used to modify both cross-sections and Z-direction core dimensions.

The resulting Eigenvalue problem, with cross-sections varying spatially, cannot be treated satisfactorily by consistent buckling. A straightforward variant of this method, consistent leakage, suffices, however. In this process the codes do not interpret the leakage into buckling but iterate on leakage directly. This is only slightly more complicated to program and converges just as satisfactorily. The spatially varying cross-sections used in each direction are obtained by averaging the variation in the other direction using flux-adjoint averaging. The averaging is repeated at each iteration. This process allows virtually exact consistency of leakages between R and Z problems. Consistency of Eigenvalues was typically within $0.0002 \Delta k/k$.

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The temperature coefficient is, of course, the difference in reactivity caused by the temperature change divided by the amount of the change. Individual components such as expansion and Doppler effects can be examined by supplying appropriate data. This method is similar, in principle, to the method used successfully in analyzing the SNAPTRAN-1 and -2 experimental data.

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