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PROGRESS REPORT
SNAP REACTOR IMPROVEMENT PROGRAM
APRIL - JUNE 1966
(Title Unclassified)

AEC Research and Development Report

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SNAP REACTOR IMPROVEMENT PROGRAM
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L. M. MAKI

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I. PROGRAM OBJECTIVES

The primary objective of the SNAP Reactor Improvement Program is advancement of the performance capabilities of uranium-zirconium hydride reactors. Improved reactor performance may provide either higher powers and longer lifetimes or may be utilized to provide greater design margins and enhanced reliability at present reactor operating conditions. Analytical and developmental studies are in progress to evaluate methods of improving technology areas that most affect reactor performance. These studies will define developmental areas necessary to achieve these improvements.

In order to meet the overall objectives of the SNAP Reactor Improvement Program, analytical tasks are established to determine the reactor performance limits within the scope of existing technology. The results of these analyses will guide the fuel and component development tasks in the program. Data from the developmental program will provide continual improvement in the knowledge of reactor performance.

Engineering analyses are in progress to determine the limits of performance of the present SNAP reactors and the gains to be achieved by changes in configuration or extension of the limits of technology. Key problem areas limiting the performance of the present reactors are being identified and potential increases in performance will be evaluated in terms of increased reliability, increased lifetime, and reduced system weight. The results of these analytical studies will be used to guide the development effort carried on in the program.

The fuel development program is directed toward the optimization of the present ceramic barrier nonrefractory metal clad fuel element design. Improved barriers, closures, and clad materials are under study, in order to improve fuel element performance. Indications are that an operating life in excess of 20,000 hours at SNAP 8 conditions is attainable.

Irradiation experiments are being prepared to further determine behavior of the fuel and cladding materials.

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The component development effort is directed toward those items which will most improve SNAP reactor performance. The development of a high-torque control drum actuator is being initiated. Studies of advanced electronic logic circuits and high-temperature bearings are also underway.

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II. PROGRESS SUMMARY

A. REACTOR ENGINEERING

Trade studies were performed to determine the effect of reactor configuration, payload configuration, reactor power, and dose criteria on shield weight. Normalized shield weight curves were prepared for instrument-rated shadow shields, manrated shadow shields, and manrated 4π shields. This parametric information will be used in the evaluation of alternate reactor configurations.

Evaluation of fuel irradiation growth data continued during the quarter. Work was initiated to derive a correlation equation for the data.

An analysis has been made of the detailed heat transfer characteristics of fuel elements in the tightly packed (P/D - 1.018) SNAP 8 reactor core. The increased cladding circumferential temperature variation due to the small (3.2-mil) radial gas gap between a fuel rod proper and the cladding was found to be as significant thermally as the close packing of the elements. Calculated average Nusselt numbers and convective heat transfer coefficients are approximately one-fourth those for flow in a circular tube of equivalent hydraulic diameter. The effects of element spacing, gap conductance, cladding thickness, and fuel rod asymmetry within the cladding were also investigated. Although the results are strictly valid only for the specific geometry of the SNAP 8 reactor, the analytic techniques and general conclusions are applicable to all liquid-metal-cooled reactors with tightly packed bundles of elements.

B. FUEL DEVELOPMENT

The fabrication of the NAA-120 fuel material irradiation experiment is continuing. All 17 fuel assemblies were completed and the required cladding tube assemblies have been machined, welded, and coated with hydrogen barrier material.

The effect of process variables on the adherence of SCB-1 coating to 304 stainless steel was studied in detail. Good adherence was obtained using the following process: preoxidation of the stainless steel at 1950°F in air followed by firing of the ceramic at 2100°F in an air-argon mixture. These studies were performed using six-inch long, open-end tubes and will have to be extended to full-length, closed-end cladding tubes.

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Experimental studies have continued on the investigation of the fuel-barrier reaction. Fuel elements were fabricated with a stainless-steel sleeve separating the fuel from the barrier and placed on endurance testing. Additional experiments are being performed to determine the temperature dependence of the reaction. In general, the reaction occurs in about 100 hours at 1400°F but no reaction was observed after 550 hours at 1300°F.

C. COMPONENT DEVELOPMENT

A transistor power switch circuit was designed, fabricated, and tested to determine the feasibility of replacing the relays presently used for switching with a potentially more reliable device. The circuit performed successfully under irradiation and over the temperature range from -100°F to +275°F. As a result of these tests, it may be concluded that:

- 1) A power switch utilizing only transistors (no electromechanical relays) is capable of operating to integrated neutron and gamma doses expected in the vicinity of SNAP systems.
- 2) The transistor power switch circuitry is compatible with the output from core-transistor logic presently used in the SNAP 8 controller.
- 3) An asymmetrical oscillator capable of delivering the desired ratio of switch "on" time to overlap for use with the expected type of actuator is feasible.

Design layouts for the improved high-torque actuator were completed and the preparation of detail drawings was initiated.

Tests were performed to determine the effect of tooth size and rotor-to-stator air gap on the output torque of an actuator. It was found that the basic equations used in the design of reluctance devices of this type can yield grossly inaccurate results when applied to small tooth geometry of the high-torque actuator design. Techniques are being developed to correct these inaccuracies.

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III. REACTOR ENGINEERING

A. DESIGN INTEGRATION

1. Shield Weight Trade Studies

The optimum reactor configuration for a specific mission application can be strongly influenced by the shielding requirements. The evaluation of the operating capabilities and growth potential of SNAP reactors must therefore include an assessment of the effects of various shield configurations.

Parametric shield weight studies were made for three basic shield configurations: instrument-rated shadow shield, Figure 1; manrated shadow shield, Figure 2; and manrated 4π shield, Figure 3. The calculated shield weights for the base cases are given in Table 1, while normalized weight scaling factors are shown in Figures 4 through 12. Each figure shows the effect on shield weight of changes in operating or design parameters. Each parameter is considered to be essentially independent so that the weight of any proposed shield may be obtained by multiplying the base case weight by the appropriate scaling factors from the figures.

TABLE 1
CALCULATED BASE WEIGHTS
AND PARAMETER VALUES

<u>Instrument-Rated Shadow Shield</u>	
Base case weight (lb)	844
Parameter values	
A - Reactor power (kw)	600
B - Shadow cone diameter at reactor midplane (in.)	20
C - Fuel element length (in.)	20
D - Separation distance (ft)	50
E - Dose plane diameter (ft)	25
F - Core diameter (in.)	10
G - Dose rate at dose plane (nvt equiv)	2.8×10^{12}
<u>Manrated Shadow Shield</u>	
Base case weight (lb)	4,588
Parameter values	
A - Reactor power (kw)	600
B - Shadow cone diameter at reactor midplane (in.)	20
C - Fuel element length (in.)	20
D - Separation distance (in.)	150
E - Dose plane diameter (ft)	60
F - Core diameter (in.)	10
G - Dose rate at dose plane (mrem/hr)	1.5
H - Gallery height (in.)	24
<u>Manrated 4π Shield</u>	
Base case weight (lb)	15,383
Parameter values	
A - Reactor power (kw)	600
B - Shadow cone diameter at reactor midplane (in.)	20
C - Fuel element length (in.)	20
D - Separation distance (ft)	150
E - Dose plane diameter (ft)	60
F - Core diameter (in.)	10
G - Dose rate at dose plane (mrem/hr)	1.5
H - Gallery height (in.)	12
I - Dose rate at reactor midplane (r/hr at 100 ft)	100

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Figure 1. Instrument-Rated Shadow Shield

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Figure 2. Manrated Shadow Shield

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Figure 3. Manrated 4π Shield

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Figure 4. Weight Scaling Factors, Instrument-Rated Shadow Shields - Dose Plane Diameter at Given Separation Distance

Figure 5. Weight Scaling Factors, Instrument-Rated Shadow Shields - Dose Rate at Dose Plane, Reactor Power, and Core Diameter

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Figure 6. Weight Scaling Factors, Instrument-Rated Shadow Shields – Separation Distance, Shadow Cone Diameter at Reactor Midplane, and Fuel Length

Figure 7. Weight Scaling Factors, Manrated Shadow Shields – Dose Plane Diameter at Given Separation Distance

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Figure 8. Weight Scaling Factors, Manrated Shadow Shields —
Reactor Power, Reactor Core Diameter, and
Separation Distance

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Figure 9. Weight Scaling Factors, Manrated Shadow
Shields — Shadow Cone Diameter at Reactor Mid-
plane, Dose Rate at Dose Plane, Gallery
Height, and Fuel Length

Figure 10. Weight Scaling Factors, Manrated 47 Shields — Dose Rate at Dose Plane and Dose Plane Diameter at Given Separation Distance

Figure 11. Weight Scaling Factors, Manrated 47 Shields — Gallery Height, Reactor Power, and Fuel Length

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Figure 12. Weight Scaling Factors, Manrated 4 π Shields—
Shield ID at Reactor Midplane, Separation Distance,
and Rendezvous Zone Dose Rate

2. Studies of Neutron Relative Biological Effectiveness (RBE)

A major consideration in the design of SNAP reactor shields is the fast neutron component of the reactor radiation. For certain biological effects fast neutrons are more efficient than x- or gamma-radiation. Consequently, in manned systems a fast neutron weighting factor can be quite significant. Additionally, it is important to know, under conditions where more than one type of ionizing radiation may be encountered, whether all types add to produce injury. Answers to these questions are necessary for the appropriate optimization of shield designs in manrated SNAP reactor configurations.

The relation between the incident radiation and the biological response significantly involves the units employed. At the present time, the most widely accepted physical unit of ionizing radiation dose is the rad. The rad is based upon the energy deposition by the radiation in the material of concern. In the exposure of biological materials it is found that, even with equal energy depositions, the effects vary with the linear energy transfer (LET), the biological effect under consideration, the physiological state, the dose rate, etc. Additional complicating

factors are introduced when considering larger animals due to variation in dose distribution within the animal's body and the involvement of the animal's specific radiation sensitivity. Basically an exposure or dose unit from which biological effects could be accurately predicted would be highly desirable. The achievement of this aim has been one of the most significant factors in the historical evolution of radiation units. The roentgen, for example, gained its original acceptance primarily because it could physically be measured and it was directly relatable to a then (1928) common biological effect unit (Erythema Dose).

In the early 1940's, H. M. Parker introduced the Relative Biological Effectiveness (RBE) concept to account for the fact that the absorption of quantities of different ionizing radiations which deposit the same amount of energy may not have the same biological effect in the human body. Since its inception, the RBE has been used as a weighting factor to relate radiation effects from the various radiations to the effects from 250-kvp x-radiation. The RBE was defined as:

$$\text{RBE of a particular radiation} = \frac{\text{250 kvp x-ray dose (rad) required to produce an effect}}{\text{dose (rad) of that particular radiation required to produce the same effect}}$$

The physical unit of absorbed dose, the rad, has the magnitude of 100 ergs/gram. The unit of biological dose to man, the rem, was defined as the rad dose times the RBE. The technological advances since the introduction of the RBE concept have severely limited the usefulness of this weighting factor. Research has demonstrated that biological efficiency is influenced by many factors so that combining them together in the RBE is not adequate for many applications.

It has been found that for many biological effects biological efficiency increases with LET reaching a maximum, and then decreases with very high values of LET (Figure 13).

Another example of the variability of the RBE may be found by review of some of the accidental exposure of humans. The fast neutron RBE for blood injury, when comparing dose at a tissue depth of 7.5 cm, varies from 0.80 to 5.45. In addition to the problem of varying organ sensitivity, these experiments point out some of the effects of the dose distribution within the animal's body. Dose

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Figure 13. RBE for Different Radiations as a Function of LET

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Figure 14. Quality Factors as a Function of LET

distribution effects are quite apparent in lethality experiments with mice, in that the hematopoietic syndrome is difficult to produce with fast neutrons alone.

In industrial radiation protection work, for exposures in the range of the Maximum Permissible Dose (MPD), the problem of weighting factors for high-LET radiation is currently resolved by the legislatively specified RBE or QF (Figure 14).

There are several significant differences between SNAP applications and conventional radiation control situations which justify and indeed necessitate special weighting factors:

- 1) NASA has established special radiation control guidelines,
- 2) Exposure rates can be known and this permits direct use of data rather than conservative extrapolations,
- 3) Exposure duration will be known for specific missions and the potential exposure over a lifetime need not be considered,
- 4) The neutron spectrum can be known; therefore, appropriate linear energy transfer factors can be obtained,
- 5) Dose distribution can be known, consequently, specific dose distribution factors can be utilized.

Current knowledge is sufficient to permit the choice of neutron dose weighting factors for a specific mission which are more realistic than the conventional quality factor.

Based upon the quality factor-linear energy transfer relationship (Figure 14), from the well known Snyder-Neufield calculations, an average quality factor for the fission neutron spectrum can be obtained. This factor is about six for a typical fission spectrum. However, as noted by the RBE committee, "The detailed relationship between RBE and LET, as it appears in the Recommendations, therefore cannot be regarded as having other than a very slight experimental basis."

Taking the NASA-recommended Apollo dose limits and using the literature-reported RBE's as a basis, a somewhat more accurate weighting factor can be obtained. Typical literature values for 1-Mev fast neutron RBE's of the critical organs in NASA recommendations are:

<u>Organ</u>	<u>RBE</u>
Skin	2.5
Blood forming organs	5
Eyes	5

Using the dose distributions within the body, a weighting factor of 2.5 could be used for most normal situations where the eyes may be protected by spacecraft design and orientation. A weighting factor of 5 would apply for accident conditions. Firm specifications of general weighting factors for space applications require additional experimental data.

The question of additivity of doses from various radiations is not definitively answered. More research is needed on this question. Until needed data is obtained, it is considered prudent to base shield design on the conservative assumption of complete additivity.

B. STRUCTURAL ANALYSIS

1. Dynamic Analysis

The vibration characteristics of SNAP reactor systems and reactor fuel elements have been investigated. The previous analyses of reactor/shield structural response to launch vibrations were reviewed and evaluated. Data from fuel element vibration tests were reviewed to determine the basic characteristics of the dynamic response of the elements, to initiate analytical models of the vibrating fuel element, and to investigate potential design modifications.

Accelerometer traces of the response of individual fuel elements in a mockup core assembly were obtained. In these tests the axial clearance between the fuel element and grid plate was varied from zero to about 0.120 in. This data review resulted in several general conclusions.

1) With zero axial gap between the fuel element and grid plate there was no significant vibration amplification and the resonant frequency of the fuel element/grid plate combination is in the range of 1500 to 2000 cps.

2) Clearance between the grid plate and fuel elements resulted in a significant increase in the dynamic response of the fuel elements. The indicated response is on the order of a factor of 5 to 10 times the input.

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3) The fuel element impacts the grid plate several times during each grid plate displacement cycle.

4) The greatest fuel element response occurs at lower frequencies; as a general rule, the lower the frequency the greater the amplification.

5) Increased clearance tends to increase the dynamic response.

These tests were run in a water-filled core mockup and accelerometers were mounted directly on several fuel elements. Acceleration only was recorded. It was not possible to estimate impact velocity from these data because (1) the fuel element impact frequency could not be established accurately, and (2) the displacement cycle for impact is discontinuous, not sinusoidal.

Initially, an analytical model was developed to estimate impact velocity with a gap between the fuel element cladding and the grid plates only. For a vibration input corresponding to NASA 417-2 the maximum estimated impact velocity was about 16 in./sec. Since the latest SNAP 8 fuel element designs consider gaps between the fuel rod and cladding on the order of 0.200 in., an analytical model was set up which considered both the axial gap between the fuel rod and cladding and the gap between the cladding and grid plate.

Preliminary results from the digital code set up to solve this problem indicate impact velocities as high as 70 to 80 in./sec. However, in this model the fluid damping of the surrounding NaK was not taken into consideration. This analytical approach will be refined and an attempt will be made to correlate the results of the analysis with measured degradation, if any, in permeation resistance of vibrated fuel elements.

C. NUCLEAR ANALYSIS

One objective of the SNAP reactor improvement program is to provide continually upgraded methods of analysis. The proper interpretation and application of data from developmental tests and the literature is a major activity in the analytical upgrading process. A review of progress in nuclear analytical methods and fuel swelling data correlations follows.

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1. LOAFER Code Modifications

The LOAFER code is used to determine the power-temperature-lifetime capabilities for SNAP reactor cores. The code was modified to allow the use of flow factors, time-dependent hydrogen loss coefficient, time-dependent radial power shape, and a temperature- and/or H/Zr-dependent fuel thermal conductivity.

The addition of flow factors gives a more realistic calculation of the temperatures in the various fuel rings. The previous method of calculation was made with constant values for core temperature rise in each ring, radial power factors, hydrogen loss coefficient, and fuel thermal conductivity. Use of a time-dependent hydrogen loss coefficient gives an improved calculation of the integrated hydrogen loss, assuming that the time dependence of the coefficient can be determined. The movement of control drums during the operating lifetime results in changes in the radial power profile. This effect may now be included in the calculations. Also, the thermal conductivity of zirconium hydride is dependent on the temperature and hydrogen content of the fuel. The data for thermal conductivity of the fuel is subject to interpretation and the change to a variable conductivity was made primarily to make the code more versatile.

2. Fuel Irradiation Damage

a. Fuel Growth

Irradiation-induced fuel growth is a primary consideration in the design of a SNAP reactor core. Several irradiation tests of uranium zirconium hydride fuel have been performed in the SNAP program;* the results are summarized in Figure 15. Upper and lower limit lines have been superimposed which are based on the first-order assumption that fuel growth ($\% \Delta V/V$) is linearly dependent on the burnup of fuel and independent of temperature, H/Zr, and rate of loss of H/Zr. It is apparent that not all the S8ER data is enclosed in the band. An expanded version of the same plot is shown in Figure 16. The data points which fall outside and above the band have been noted and are discussed below. The non-zero intercept is for plotting convenience since very low burnup data are lacking and, therefore, definition of the dependence of swelling in this domain is not known.

*L. M. Maki, "Progress Report SNAP Reactor Improvement Program, January-March 1966," NAA-SR-11882, June 1, 1966 (CRD)

Figure 15. Fuel Growth vs Burnup

Figure 16. Fuel Growth vs Burnup in S8ER Range

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The data shown in Figure 16 are primarily for δ -domain fuel growth. The NAA-53-3L Irradiation Test data are representative of β -domain fuel growth. Since the band for δ -domain fuel growth was determined from capsule irradiation data, not all the δ -domain data points from the S8ER fall within the band. The S8ER data includes samples which are representative of β -domain growth, samples which were near cracks in the fuel, and samples representative of δ -domain fuel growth.

Another method of presenting the data is shown in Figure 17, which shows the rod-averaged $\% \Delta V/V$ vs rod-averaged burnup. Essentially the same elements show up with large growth. The same data is shown in Figure 18 plotted against the minimum H/Zr for each rod. The general trend is increased fuel growth with decreasing H/Zr. Elements 378, 432, and 474 appear to be exceptions to this rule. These elements have hydrogen content which corresponds to the boundary of the (δ - ϵ)-domain. Also in this hydrogen content domain are Elements 111, 293, and 206, which show much less fuel growth. The difference in fuel growth may be attributable to carbon content (for this hydrogen domain), temperature fluctuations, or axial stress differences. The carbon contents for the six elements in question are shown in Table 2. From these data, Element 206 would be

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Figure 17. As-Measured Percent $\Delta V/V$ vs Burnup

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Figure 18. S8ER Fuel Rod Profiles (High H/Zr)

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TABLE 2
CARBON CONTENT OF SELECTED S8ER
FUEL ELEMENTS

Element Number	Carbon Content	H/Zr _{eff}	
		Initial	Final
378	0.34	1.69	1.64
432	0.54	1.74	1.66
474	0.49	1.80	1.73
111	0.01	1.75	1.69
293	0.0	1.74	1.66
206	0.48	1.75	1.68

expected to show growth similar to 378, 432, and 474. The values of H/Zr associated with the transformation in a radiation field from the tetragonal, ϵ , to the face-centered cubic, δ , material are important to the interpretation of these data.

The elements (declad) from the S8ER may be separated into two groups representing high end-of-life hydrogen content and low and middle end-of-life hydrogen content. The two groups are shown in Figures 18 and 19. It would be difficult to make a quantitative statement about growth as a function of H/Zr from these figures. The same data were separated into two groups based on temperature and the results are shown in Figures 20 and 21. The conclusion drawn is that greater-than-average fuel growth is associated with low (<1.45 H/Zr) and with 1.6 H/Zr (for $T < 1410^\circ\text{F}$). For the higher temperature region the growth observed increases with decreasing H/Zr. It appears that the 1.6 to 1.7 H/Zr region is a region in which large fuel growth can occur under certain conditions. One possible method of causing this greater-than-expected growth would be temperature cycling (which may cause the material to oscillate between the δ and ϵ domains) or axial compression loads.

b. Fuel Growth Correlation

The prediction of fuel swelling has in the past been based on results from the NAA-53 irradiation capsules. This limitation on experimental data to predict

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Figure 19. S8ER Fuel Rod Profiles (Low and Middle H/Zr)

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Figure 20. S8ER Diametral Growth Correlation with Local H/Zr - Zones Less Than 1410°F, 600 kwt, 1300°F Outlet

Figure 21. S8ER Diametral Growth Correlation with Local H/Zr - Zones Greater Than 1410°F, 600 kwt, 1300°F Outlet

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fuel swelling has resulted in large uncertainties in SNAP reactor operating limits. With the completion of the S8ER and the NAA-115 tests, a reevaluation of the analytical equation for fuel swelling was made based upon the additional experimental data. To assist in the evaluation of an analytical equation to approximate this data, a computer program was written. The equation for fuel swelling was assumed to be of the following form:

$$\% \Delta V / V = AB^2 e^{-C/T} + EB + F$$

where

$\% \Delta V / V$ = volumetric fuel growth

B = burnup (met. at. %)

T = temperature ($^{\circ}$ R)

and

A, C, E, and F are constants.

The code evaluated $1/T$ and $[B^2/(\% \Delta V / V - EB - F)]$ and presents the results on CRT plots. The constants E and F can be varied to determine a linear semi-log plot. If the fuel swelling data show temperature dependence, then the correct value of constants can be easily evaluated.

The results from this program are shown in Figures 22, 23, and 24 for a fixed set of values for the parameters E and F. The fuel swelling data in each figure are derived from the following tests:

NAA-53, NAA-43, NAA-116, and NAA-75 (Figure 22);

NAA-53, NAA-43, NAA-116, NAA-75, and NAA-115 (Figure 23);

NAA-53, NAA-43, NAA-116, NAA-75, NAA-115, and S8ER (Figure 24).

The scattered results in Figures 22, 23, and 24 indicate that the values $E = 1.0$ (the value used in the early Miller Correlation) and $F = 0.5$ are not particularly well chosen, or, alternatively, that no strong temperature dependence exists or a temperature dependence is masked by other effects, such as H/Zr or carbon content.

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Figure 22. Fuel Swelling Data from NAA-53, NAA-43, NAA-116, and NAA-75

Figure 23. Fuel Swelling Data from NAA-53, NAA-43, NAA-116, NAA-75, and NAA-115

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Figure 24. Fuel Swelling Data from NAA-53,
NAA-43, NAA-116, NAA-75, NAA-115,
and S8ER

D. THERMAL ANALYSIS - FUEL ELEMENTS

An analysis has been made of the detailed heat transfer characteristics of fuel elements in the tightly packed ($P/D = 1.018$) SNAP 8 reactor core. The increased cladding circumferential temperature variation due to the small (3.2-mil) radial gas gap between a fuel rod proper and the cladding was found to be as significant thermally as the close packing of the elements. Calculated average Nusselt numbers and convective heat transfer coefficients are approximately one-fourth those for flow in a circular tube of equivalent hydraulic diameter. The effects of element spacing, gap conductance, cladding thickness, and fuel rod asymmetry within the cladding were also investigated. Although the results are strictly valid only for the specific geometry of the SNAP 8 reactor, the analytic techniques and general conclusions are applicable to all liquid-metal-cooled reactors with tightly packed bundles of elements.

An important aspect of the analysis is the insulating effect of the gas gap which partially isolates the fuel rod from the cladding. A Fourier series representation of circumferential temperatures was used for the fuel rod, the surface heat flux being specified as a boundary condition and the corresponding fuel temperature

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distribution then calculated. The gas gap, barrier, clad, and coolant associated with one twelfth of an element were simulated in a digital program nodal model. The fuel surface temperature was assumed as a boundary condition. Slug flow of the coolant was assumed and eddy diffusivity was neglected corresponding to liquid-metal flows below the critical Peclet number of 400 to 600. The analysis was iterated between the series solution and the digital nodal solution until the fuel surface temperature and the heat flux distributions were matched. This technique of solution is particularly suited to the analysis of asymmetric configurations.

The reference case is the nominal 0.560-in.-diameter S8DS fuel element with the fuel rod centered within the cladding and with a 10-mil clearance between adjacent elements at closest approach. Nominal beginning-of-life dimensions (3.2-mil radial gas gap) and conductivities are assumed. Reactor operating conditions are 600 kwt, 1100°F core inlet temperature, and 1300°F outlet temperature. The SNAP 8 design flow of 48,800 lb/hr corresponds to a Peclet number of 70, well below the critical value.

Circumferential and radial temperature profiles within the central element at the axial midpoint of the core are shown in Figure 25. This core location exhibits the largest differences between element and bulk coolant temperatures. (Temperature differences at any other location can be calculated from the ratio of local powers.) All circumferential temperature variations repeat every 60° and can be accurately approximated by simple cosine curves. The maximum circumferential variation in fuel surface temperature is 14°F; the corresponding cladding variation is 75°F. The average Nusselt number is 1.57 and the average convective coefficient (h) is 3500 Btu/hr-ft²-°F.

Figure 26 shows heat flux around the periphery of the element. Because of the insulating gas gap, the variation of the heat flux at the fuel surface is considerably less than that in the cladding, 40% as compared to 70%. The minimum cladding heat flux occurs at the cusps between channels and is 0.45 times the average. As anticipated, this minimum heat flux was found to be extremely dependent upon the spacing between elements.

The general effect of decreasing element spacing (i.e., pitch-to-diameter ratio) is to increase circumferential temperature variations and the difference

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Figure 25. Temperature Distributions for S8DS Symmetric Fuel Rod

Figure 26. Heat Flux Variations for S8DS Symmetric Fuel Rod

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between the average cladding temperature and the bulk coolant. This is shown in Figure 27. With touching elements ($P/D = 1.0$), the average Nusselt number is only 0.70. At 20-mils spacing ($P/D = 1.036$), the Nusselt number has increased four-fold to 3.02. Corresponding changes in heat flux are shown in Figures 28 and 29.

A similar effect is seen with gap conductance. The greater the conductance, the more closely bound is the fuel to the cladding, the smaller the cladding temperature variation, and the greater the average convective coefficient.

Increasing the cladding thickness from 10 mils to 20 mils has little thermal effect, as shown in Figures 30 and 31. The maximum cladding temperature variation is decreased to 60°F and the average Nusselt number is increased to 1.98. The difference between the fuel surface and the cladding heat flux distributions is exaggerated with the thicker cladding.

Two worst cases of unsymmetrical geometry were also considered:

- 1) Fuel touching barrier at a cusp between coolant channels (i.e., at the point of closest approach of adjacent elements);
- 2) Fuel touching barrier at center of coolant channel (30° rotation from first case).

Because of the resulting gas gap variation, asymmetry of a fuel rod greatly increases the fuel surface temperature variation. However, the cladding ripple is increased only slightly and the maximum fuel temperature is actually reduced from the symmetric case.

Circumferential temperature distributions for the case of fuel-barrier contact at a cusp are shown in Figure 32, radial profiles through the fuel in Figure 33, and the circumferential variation of the fuel surface heat flux in Figure 34. Figures 35, 36, and 37 are similar plots for the case of fuel-barrier contact at midchannel. In the former asymmetric case, the maximum fuel temperature is 207°F greater than the surrounding bulk coolant. In the latter case, the difference is 210°F . For comparison, the maximum temperature difference for the symmetric rod is 229°F . The large variation in heat flux between 30° sections in both asymmetric cases should also be noted.

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Figure 27. Effect of Element Spacing on Temperature Distributions

Figure 28. Effect of Element Spacing on Fuel Surface Heat Flux

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Figure 29. Effect of Element Spacing on Cladding Heat Flux

Figure 30. Effect of Cladding Thickness on Temperature Distributions

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Figure 31. Effect of Cladding Thickness on Circumferential Heat Flux Distributions

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Figure 32. Temperature Distributions - S8DS Asymmetric Fuel Rod

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Figure 33. Radial Temperature Distributions—
S8DS Asymmetric Fuel Rod

Figure 34. Fuel Surface Heat
Flux Variation for S8DS
Asymmetric
Fuel Rod

Figure 35. Temperature Distributions —
S8DS Asymmetric Fuel Rod (Fuel-
barrier contact at midchannel)

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Figure 36. Radial Temperature Distributions - S8DS Asymmetric Fuel Rod

Figure 37. Heat Flux Variation for S8DS Asymmetric Fuel Rod

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IV. FUEL DEVELOPMENT

A. NAA-82-1 IRRADIATION EXPERIMENT

1. Summary

The hot cell examination of the NAA-82-1 experiment has been essentially completed. This experiment evaluated full-scale-production, SNAP 2-type fuel element performance at 125-kwt reactor conditions of temperature and burnup. The fuel element, both cladding and fuel rod, survived the irradiation intact and in excellent condition.

2. Status

The active hot cell examination of this experiment has been completed except for chemical burnup determinations and for a series of supplementary cladding tensile tests which will be performed under the SNAP 8 program in fiscal year 1967. The experiment has been removed from the hot cell to a storage area and the hot cells have been cleaned. The details of the NAA-82-1 hot cell examination were given in the last quarterly progress report.*

The results of this irradiation experiment are being correlated with those obtained from both the NAA-115-1 experiment and the post-irradiation examination of the S8ER reactor core.

B. NAA-120-4

1. Summary

The NAA-120 irradiation experiments are statistically designed to evaluate SNAP fuel performance as functions of temperature, burnup, and fuel composition. The detailed design features have been reported in previous quarterly progress reports.†

*L. M. Maki, "Progress Report, SNAP Reactor Improvement Program, January-March 1966," NAA-SR-11882 (CRD) May 23, 1966

†Principal design features of the NAA-120 irradiation experiments were reported in "SNAP Reactor Improvement Program Quarterly Progress Report, July-September 1965," NAA-SR-11690 (CRD), November 22, 1965

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FUEL

430 F SS

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7130-1-1 250X

(a)

7647-5165

FUEL

430 SS FIBER METAL

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7130-1-2 300X

(b)

7647-5166

Figure 38. 430 Stainless Steel Fuel Couples After 1800°F
for 200 Hours

The construction of this experiment is proceeding satisfactorily, and certain problem areas have been resolved including:

- 1) Evaluation of fuel-fiber metal compatibility,
- 2) Fuel and fuel specimen assembly techniques,
- 3) Fuel and cladding inspection techniques.

2. Status

The original procedure for assembling the fuel material was to shrink-fit or press-fit the enriched center and guard sleeves over the support rod. However, stress calculations and experimental tests indicated cracking of these sleeves to be a potential problem. Hence, the decision was made to adopt a slip-fit type of assembly, and subsequent experimental tests showed this type of assembly to be superior. However, adoption of the slip-fit design change posed an additional problem area. This problem involved maintaining the position of the enriched center and guard sleeves in the proper position on the support rod during pre-irradiation handling and, at the same time, allowing radiation-induced fuel swelling to occur during irradiation testing. The material chosen to perform this task is a deformable, 10% dense, fiber metal body. Since stresses less than 1000 psi are needed to deform this material, lateral movement of the sleeves during handling is prevented; however, irradiation-induced fuel swelling is not.

Since fiber metal is available in only a limited number of materials, it was necessary to evaluate several types of materials in terms of compatibility with the fuel material. Two types of fiber metal were chosen for evaluation: 302 stainless steel and 430 stainless steel. Three separate compatibility tests were performed with each material — two with fiber metal fuel couples and one with solid material (100% dense, 304 and 430 F SS) fuel contact. The tests were performed at an accelerated test temperature of 1800°F under a hydrogen pressure sufficient to maintain a minimum of 1.60 H/Zr. Sample contact was maintained by use of a molybdenum bolt. The tests were run for 200 and 400 hours for fiber metal fuel tests and 200 hours for solid material fuel tests. Metallographic examination of these samples after test completion indicated the following:

- 1) No apparent reaction of 430 SS with fuel in any of the three tests described (Figure 38);

FUEL
FUEL -
304 SS INTERACTION

304 SS

7-15-66

7130-2-1

250X (a)

7647-5167

FUEL

302 SS FIBER METAL

7-15-66

7130-5-2

250X (b)

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Figure 39. 304 and 302 Stainless-Steel Fuel Couples
After 1800°F and 200 Hours

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2) Reaction of 304 SS (compositionally similar to 302 SS) with fuel for the solid material fuel test (Figure 39), and no apparent reaction with fuel - 302 SS fiber metal couples for 200 or 400 hours.

The reason for no apparent reaction of 302 SS fiber metal with the fuel may lie in the relative contact area when compared with solid material (304 SS) fuel couples. Since the 200 hours at 1800°F is the equivalent of ~2000 hours at the peak operating temperature of the experiment, the tests were conclusive in that 430 SS fiber metal was chosen for use in the NAA-120-4 irradiation experiment.

Sufficient cladding tube assemblies, including a minimal backup, have been machined, welded, and coated with hydrogen barrier material. All 17 fuel assemblies have been successfully assembled. Figure 40 is a representative photograph of a fuel assembly (120-4E-1). Of the 17 fuel assemblies, 5 have been encapsulated in Hastelloy X tubing assemblies and welded. These fuel specimens are presently being inspected for weld quality. The remaining 12 fuel specimens are in the process of being dimensionally inspected prior to being encapsulated and welded in Hastelloy X. Following final closure welding of the fuel assemblies, the fuel specimens need to be (1) inspected, (2) blended, (3) permeation tested, (4) final inspected, and (5) assembled into subcapsules. At this point, the subcapsules will be integrated into the experiment assembly and subsequently shipped to the reactor for irradiation.

An evaluation of the dimensional data on the first five fuel assemblies (120-4E subassembly) and their corresponding cladding tubes shows that the maximum possible off-center fuel condition due to buildup of tolerances is 0.0014 inch. This maximum condition will exist only if the component parts are aligned such that the tolerances are additive. In this maximum condition, a 40°F circumferential ΔT will exist between the top and bottom of the fuel assembly. This compares very favorably with the 100°F circumferential ΔT present in the NAA-115 fuel specimens, which were operated at a specific power of only 40% of the rate for NAA-120. In addition, a thermal stress analysis has shown that any off-center condition will create thermal expansion effects which will tend to straighten the fuel assembly and to restore an isothermal condition.

Several process changes in fuel and fuel specimen assembly techniques have been initiated. The initial design for fitting of fuel supports called for 0.2- to 0.4-mil interference shrink fit. The high interference-shrink fit combination in

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Figure 40. Typical 120-4 Fuel Assembly

some cases resulted in "cocking" of the fuel supports on the support rod during assembly, thereby resulting in high TIR values of the fuel assembly. This problem was alleviated by reducing the interference to 0.1 to 0.2 mils and using a "push fit" technique. In addition, fuel supports are being "tailor ground" to fit specific tubing assemblies which are hand picked for each fuel specimen assembly. This was necessary since the present tolerance allows for only 0 to 0.2-mil fuel support to cladding gap, and even though dimensional inspection indicated an acceptable gap, TIR on the fuel and tubing assemblies prevented loading.

Dimensional inspection techniques for the various fuels and claddings are now uniformly established. Techniques for evaluating concentricities of fuel supports, fuel assemblies, and cladding assemblies have been examined, and the best technique was chosen in each case. In addition, a complete set of manual dimensions is now required for each fuel assembly, as well as profilometer measurements.

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These dimensions were felt to be necessary in order to provide backup data for the profilometer, since the possibility exists that profilometer equipment breakdown during post-irradiation examination could occur.

C. DESIGN AND EVALUATION

1. Summary

The design and evaluation task serves three basic needs: (1) provision of direct analytical support for the other fuel development tasks; (2) provision of technical liaison to assure that experimental data and the results of analytical studies are incorporated into the fuel development effort; and (3) performance of inexpensive experimental tasks which are not funded under another fuel development subaccount, or are needed to evaluate the feasibility of a particular concept. During the last quarter, the tasks briefly summarized in the following two paragraphs were performed.

Modifications have been initiated to simplify the digital computing data reduction code for the NAA-120 series irradiation assemblies. These simplifications are warranted because of schedule revisions for the irradiation of this assembly series. A considerable savings in computer time for both routine data reduction and ultimate correlation and evaluation will be effected.

The application of an yttrium coating to the exterior of SNAP cladding tubes progressed on a limited basis. A special fixture was fabricated to evenly coat tubular sections and several tubes were coated with moderate success. Because of program redirection, this effort will not be continued.

2. Status

a. NAA-120 Data Reduction Code

The requirements for the digital computing code for data reduction for the NAA-120 series irradiation assemblies were revised to take into account the new proposed schedule for irradiation of the first two assemblies of the series. Inasmuch as the schedule no longer necessitates that two assemblies be on the instrumentation console simultaneously,* the 100 total data input channels now

*"Project Proposal Fiscal Years 1967 and 1968 Reactor Development Programs, SNAP 8, Reactor Improvement Program and General Supporting Technology," NAA-SR-MEMO-11868, Volume 4 (CRD), May 16, 1966

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permit recording of all thermocouples, flux monitors, and pressure instrumentation for a single assembly. Additionally, individual data channels are available for each experiment of an assembly to record on an experiment basis the time periods of 100% helium operation.

Significant progress had been made and reported in the previous quarterly progress report* on a sophisticated NAA-120 data reduction code which permitted one or two NAA-120 series assemblies to be on the instrumentation system simultaneously. Moreover, a scheme had been devised and successfully demonstrated permitting any instrument of either assembly to be on any data channel at any time. Since such features are no longer required, they are being eliminated from the NAA-120 digital code in order to minimize the running time of the code.

The use of data channels to indicate 100% helium operation will greatly facilitate the means by which the code will continually recalibrate the associated flux monitors. Additionally, using data channels for the various in-pile hydrogen dissociation pressure measurements will also facilitate computations on hydrogen loss.

The above-mentioned changes will greatly enhance future efforts in correlation and evaluation of the fuel behavior of the NAA-120 series irradiation assemblies.

b. Yttrium Coating

A small development effort was initiated last quarter to apply an 8-mil coating of metallic yttrium to the exterior surfaces of SNAP fuel elements. The objective of this work is to study the feasibility of an in-core hydrogen getter concept. The application and processing work has been subcontracted to Temescal Metallurgical Corporation, Berkeley, California. Temescal uses large electron-beam guns to melt and vaporize metallic materials within large high-vacuum chambers. The vaporized material streams in a direct line-of-sight path and deposits on the component to be coated. By bending the electron beam, they are able to mount the electron gun to one side of the crucible, thereby permitting an

*L. M. Maki, "Progress Report SNAP Reactor Improvement Program, January-March 1966," NAA-SR-11882 (CRD), May 23, 1966

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unobstructed path for the vaporized particles. This feature, together with large working areas within the vacuum chamber, permits considerable versatility in fixturing devices for handling complicated substrate geometries.

Based on the evaluation of Phase I, where small flat coupons were successfully coated with yttrium,* Phase II of the program was initiated. A special fixture was built to rotate the tubular specimens and provide a uniform coating on the outside surfaces. Three tubes were coated in this manner before problems were encountered in the process. In subsequent runs, satisfactory coating could not be obtained. Time has not permitted an evaluation to determine the cause of the problem. All samples are being returned to AI for examination. Further work will not be initiated due to program redirection.

D. IMPROVED BARRIER DEVELOPMENT

1. Summary

During this report period, the main effort was directed toward the development of good adherence of the SCB-1 coating to an SS 304 substrate. With the help of preoxidation in pure air and application of the two coatings in an atmosphere consisting of 50% air and 50% argon by volume, an adherence capable of withstanding an average of 20 reverse flexes was achieved. Exposure of the substrate to temperatures in excess of 1950°F was reduced to 3 minutes during the firing cycle, which should result in a noticeable reduction in the grain size of the coated substrate.

2. Status

Stainless steel 304 is oxidation-resistant to approximately 1700°F. Since the application of the SCB-1 coating requires a temperature of about 2100°F, it is therefore necessary to protect the substrate from excessive oxidation during coating, while allowing for sufficient oxidation to achieve good adherence. An additional consideration in this development work was to minimize exposure of the stainless steel to temperatures over 1700°F in order to reduce grain growth.

*Pertinent details were reported in the Quarterly Progress Report, SNAP Reactor Improvement Program, January-March 1966, L. M. Maki, NAA-SR-11882 (CRD), May 23, 1966

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TABLE 3
STAINLESS STEEL 304 AND SCB-1 COATING DEVELOPMENT DATA

Sample Number	Preoxidation			First-Coat Firing			Second-Coat Firing			Adherence Second Coat Number of Flexes*
	Temperature (°F)	Time (min)	Atmosphere	Temperature (°F)	Time (min)	Atmosphere	Temperature (°F)	Time (min)	Atmosphere	
1			No Preoxidation	1900	15	Flowing Air 1100 cc(STP)/min	2100	6-1/2	Flowing Air 1100 cc(STP)/min	1
2				1900	12		2100	6-1/2		1
3				1900	9		2100	6-1/2		1
4				1900	6		2100	6-1/2		1
5				1900	3		2100	6-1/2		1
6				1700	15		2100	3	50 Air and 50 Argon 1000 cc(STP)/min	7
7				1700	30		2100	3		5
8	1900	3	Flowing Air 1000 cc(STP)/min	1950	9	50 Air and 50 Argon 1000 cc(STP)/min	2100	3		18
9	1900	6		1950	6		2100	3		20
10	1900	9		1950	3		2100	3		19
11			No Preoxidation	1950	3		2100	3		2-1/2
12				1950	6		2100	3		4
13				1950	9		2100	3		8-1/2
14				1950	3		2100	3		1
15				1950	3	Flowing Air 1100 cc(STP)/min	2100	3	Flowing Air 1100 cc(STP)/min 50 Air and 50 Argon 1000 cc(STP)/min	5
16				1950	6	50 Air and 50 Argon 1000 cc(STP)/min	2100	3	Flowing Air 1100 cc(STP)/min 50 Air and 50 Argon 1000 cc(STP)/min	2
17				1900	15	Flowing Air 1100 cc(STP)/min	2100	3		2-1/2
18				1900	30	50 Air and 50 Argon 1000 cc(STP)/min	2100	3		1
19				1950	3		2100	3		4
20				1950	6		2100	3		2
21				1950	9		2100	3		2
22				1950	12		2100	3		1
23				1950	9		2100	3		4
24				1950	12		2100	3		4
25				1950	15		2100	3		4
26	1700	3	Flowing Air 1000 cc(STP)/min	1950	3		2100	3		0
27	1700	6		1950	6		2100	3		14
28	1700	9		1950	9		2100	3		13
29	1700	12		1950	12		2100	3		10
30	1700	15		1950	15		2100	3		13
31	1800	3		1950	3		2100	3		0
32	1800	6		1950	6		2100	3		0
33	1800	9		1950	9		2100	3		18
34	1800	12		1950	12		2100	3		8
35	1800	15		1950	15		2100	3		12
36	1900	3		1950	3		2100	3		16
37	1900	6		1950	6		2100	3		20
38	1900	9		1950	9		2100	3		18
39	1900	12		1950	12		2100	3		20
40	1900	15		1950	15		2100	3		21

NOTE: Samples 11 through 13 and 23 through 25 were fired without "Protector-Tube"; Samples 26 through 30 had "Protector-Tube" on first- and second-coat firing only.

*The number of flexes are averages of from 1 to 6 samples.

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The original objective of promoting adherence by controlled oxidation during the application of the first coat could not be realized, because the coating seemed to interfere with the formation of a uniform oxide layer and caused spalling. A separate preoxidation step proved necessary. The outsides of the stainless steel tubes were protected from excessive scaling by a tight-fitting outer tube made of Hastelloy X.

The preoxidation and the two coating steps were applied on 6-in.-long, open-end tubes in a vertical furnace, in a flowing atmosphere. The time, temperature and atmosphere of the preoxidation and first coating step were varied, while the parameters of the second coating were held constant, with few exceptions. Altogether 40 experiments were conducted, as listed in Table 3. The table shows best adherence for Experiments 8 to 10, and for the repetition and expansion experiments 36 to 40. These experiments show also the advantage of preoxidation. Good adherence of the SCB-1 coating, applied as two 1-mil thick layers, was obtained after firing for only three minutes at a temperature of 2100°F.

A further optimization of the coating procedure for 304 stainless steel is in progress, with the aim of an increase in adherence and reduction in heat exposure. Resistance to deterioration of adherence due to aging will also be investigated. Experiments with SCB-1 derivatives (e.g., SCB with Al_2O_3 additions) will be conducted to investigate the performance of this coating* as a function of time at temperature and its ability to withstand temperature transients.

E. IMPROVED PROCESS DEVELOPMENT

1. Summary

During the last quarter the tasks briefly described in the following paragraphs were performed.

Major efforts during the reporting period were directed toward evaluating the reverse-drain coating method and the quality of the plug seal from the viewpoint of hydrogen leakage.

*The effort conducted to date on the SCB-1 plus Al_2O_3 ceramic barrier was reported in the Quarterly Progress Report, SNAP Reactor Improvement Program, January-March 1966, "L.M. Maki, NAA-SR-11882 (CRD), May 23, 1966

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The 12 tubes which were processed using this technique* were permeation-tested at 1400°F at 2 atmospheres of hydrogen. Those tubes which were chromized prior to coating were found to exhibit leak rates of about 1.65 cc(STP)/hr-cm², whereas those which were coated over preoxidized X-285 were found to exhibit leak rates of about 0.72 cc(STP)/hr-cm². Based on an average rate of 1.60 cc(STP)/hr-cm², which is considered to be an acceptable value for good membranes, it appears that this method of coating and sealing has merit and should be considered for possible use to eliminate coating taper, providing such leak rates can be maintained after ramp heat and vibrational shock.

The application technique of glass transfer tapes to ceramic coatings, as developed by an outside supplier, was investigated last quarter. The potential value of such a process would be the ability to apply Sm₂O₃ poison in a uniform, or deliberately spaced nonuniform, distribution down the length of a tube. A number of closed-end and halved SNAP 10A tubes, coated and uncoated, were forwarded to the supplier for test purposes. Test results showed that the coated tubes which were halved could be successfully recoated using this technique with tapes containing Sm₂O₃, whereas the closed-end tubes could not, due to the lack of proper tooling.

2. Status

Twelve full-length, open-end tubes which have been coated using the reverse-drain principle and assembled with plug-type seals were submitted for hydrogen leak testing with the closure portion of the assembly at 1400°F and hydrogen pressure of 2 atmospheres. Six of the tubes were chromized Hastelloy N and six were preoxidized Hastelloy X-285. Half of each group were plugged after the first application of enamel and half after the second.

Thermocouples were placed at four positions along each tube during permeation testing to indicate temperature gradients. From these data and established relationships between hydrogen flow at various temperatures the leak rates determined in terms of cc/hr were converted to cc(STP)/hr-cm². The leak rates

*Details of this process and program objectives are given in the Progress Report, SNAP Reactor Improvement Program, October-December 1965, L.M.Maki, NAA-SR-11754 (CRD), February 15, 1965

for the chromized Hastelloy N tubes were found to stabilize at values averaging 1.80 and 1.51 cc(STP)/hr-cm² depending on whether they were plugged after the first coat or after the second followed by a refire. The preoxidized Hastelloy X-285 membranes were found to exhibit much lower leak rates, averaging 0.85 and 0.59 cc(STP)/hr-cm². See Table 4.

TABLE 4
LEAK RATES FOR 12 TUBES COATED USING REVERSE-
DRAIN PRINCIPLE AND PLUG-TYPE SEAL

Membrane		Series	Leak Rate at 1400°F and 2 atmospheres Hydrogen Pressure [cc(STP)/hr-cm ²]	
Number	Type		Specific	Average
62-1 62-2 62-3	Chromized Hastelloy N	Plugged after first coat	1.59 1.82 1.98	1.80
X3 X4 X6	Preoxidized Hastelloy X-285		0.88 0.97 0.69	0.85
55 153 157	Chromized Hastelloy N	Plugged after second coat	1.40 1.61 4.45	1.51*
X1 X2 X5	Preoxidized Hastelloy X-285		0.74 0.33 0.69	0.59

*Discounting the value for tube 157 which appears to be unreasonably high.
This tube being retested to ascertain its degree of stabilization.

Based on an established value of 1.6 cc(STP)/hr-cm² for acceptable membrane performance it appears that this method of coating and sealing has merit and should be considered for possible use to eliminate coating taper providing they can be made to withstand subsequent tests involving ramp heat and vibrational shock.

Those membranes which were plugged after the first coat appeared to be the best on the basis of reproducibility of results. Detailed dimensions obtained during processing showed the reverse-drain method was capable of reducing the

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coating taper by a factor of approximately three times that produced by the conventional coating procedures. Since the leak rates appear to be commensurate with standard practices it is recommended that further work be done in this area.

A representative from a supplier who had developed various techniques of transferring metalized, conductive or glass tapes to metals, or ceramics, met with various AI personnel last quarter to discuss possibilities of application to S8DS hardware. Since a continuous coating can not be applied by tape to the interior surfaces of a tube without being overlapped or without leaving a gap, its potential to AI was believed to lie in its application as a second coat containing Sm_2O_3 .

Four 13-1/2 by 1-1/4-in.-diameter closed-end tubes, and eight 13-1/2 by 1-1/4-in.-halved sections, some coated and some uncoated, were sent to the supplier along with 4 pounds of AI 203D milled frit containing Sm_2O_3 powder and technical data regarding firing parameters for test purposes. According to the supplier, the tapes were prepared in various thicknesses from the powder supplied by AI. Initial experiments revealed that the shrinkage of the tapes upon firing was approximately 50%, as expected.

No problems were encountered in applying the tapes to the halved sections. Attempts to coat the closed end tubes were unsuccessful. It was most difficult, with the tooling at hand, to apply a uniform pressure to the tape inside the tube, making it impossible to remove the carrier film from the glass layer without tearing and cracking.

While it was beyond the scope of this initial study, it would appear possible to coat the closed end tubes if a uniform pressure could be obtained. A fixture which would seal off the open end of the tube and use compressed air or nitrogen to apply pressure might prove very satisfactory for this purpose.

F. IMPROVED CLADDING DEVELOPMENT

1. Summary

The objective of this subaccount is the development of an improved cladding material for SNAP reactor fuel elements. The cladding materials being studied are Hastelloy N, Hastelloy X, Type 304 stainless steel, and Type 316 stainless steel. The literature search has provided a basis for rejection of Hastelloy X

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as a cladding alloy candidate because of metallurgical instability. Two additional nickel base alloys (Inconel 600 and Inconel 625), and an iron base alloy (Incoloy 800), appear to be attractive cladding materials. Therefore, sufficient material of both tubing and bar stock has been ordered to perform a process evaluation study. The literature search also has shown that trace impurities, particularly sulfur, appear to exert a powerful influence on the ductility of the nickel-base alloys. Mechanical properties data on materials with known composition are being collected in a form that is readily adaptable to computer analysis of the effects of composition on ductility.

The major effort during the quarter was expended on the fabrication of fourteen Type 304 stainless-steel test membranes enameled with AI-8763D hydrogen barrier material. These membranes were placed on long-time aging and permeation tests at the end of the quarter.

2. Status

Work has been concentrated during this quarter on preparing Type 304 stainless steel samples for the creep rupture and hydrogen aging experiments. Fourteen samples (18-in.-long, half-in.-diameter tubes with 0.010-in. wall) were coated with AI-8763D hydrogen barrier material. Open-end tubes were enameled with two coats. Each coat was drained from a different end, utilizing the reverse drain principle. This procedure resulted in a maximum taper of 0.0005 in. in the ID of the coated tubes. Adherence samples taken from the ends of the tubes each withstood 23 bends over a half-inch mandrel without loss of enamel. End caps were interference-fitted at room temperature into the coated tubes and electron-beam welded in place.

The experiment consists of creep rupture testing samples that have been exposed to the following conditions: (a) "as-received," (b) "as-enameled," and (c) "as-aged" for 500, 1,000, 5,000, 10,000, and 20,000 hours at 1,400°F and two atmospheres internal hydrogen pressure. The creep rupture test furnace has been modified for use in these tests in order to permit removal of a single sample without disturbing the remaining samples. Sufficient internal pressure will be applied to the above-mentioned tube-type samples to impose hoop stresses expected to cause rupture in 200 to 500 hours. The "as-received" samples are being used to determine a "reference" stress level necessary for these samples.

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The literature search continues to yield information. All data are being collected in a form that is readily adaptable to computer analysis. A computer program has been used in a related project to evaluate the effect trace impurities have on mechanical properties of Haynes 25. This computer program is being studied to determine its usefulness for SNAP cladding materials evaluation. The literature search has shown sulfur to exert an influence on ductility of nickel-base alloys. A correlation has been shown for a decrease in ductility of Hastelloy N with increasing sulfur content in the range of 20 to 120 ppm. This effect will be further investigated. The literature search also yielded information on the thermal instability of Hastelloy X above the SNAP cladding temperature range. Samples aged for 5000 hours at 1850°F have shown major phase segregation of Mo into the grain boundaries. At 10,000 hours, essentially all Mo has segregated to form a two-phase alloy. Review of photomicrographs of samples aged at 1500°F for long times show what is believed to be the initial stages of this same two-phase segregation. This information, together with the microcracking problem during welding, has led to the rejection of Hastelloy X as a serious cladding alloy candidate. Two other nickel-base alloys (Inconel 600 and Inconel 625) and an iron-base alloy (Incoloy 800) are attractive as cladding candidate materials. Therefore, sufficient tubing and bar stock have been ordered for a process evaluation study.

G. FUEL-BARRIER INTERACTION STUDIES

1. Summary

The objective of this subaccount is to develop a method for suppression of the phenomenon that results in an increased hydrogen loss rate from fuel elements as compared with that from unfueled membranes.

Results obtained in the second quarter of GFY 66 provided experimental basis for an oxygen transport theory as the cause for the fuel-barrier interaction. According to this theory, zirconium in the fuel rod is a sink for oxygen. The dry hydrogen gas from the fuel rod reacts with the silicate glass in the hydrogen barrier material to form water vapor. The water vapor migrates, in the vapor phase, to the fuel rod where it is decomposed and the oxygen is absorbed by the zirconium. The presence of the oxygen sink prevents an equilibrium water vapor concentration from being established and leads to the gradual reduction of the

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hydrogen barrier material. It is believed that the removal of oxygen from the glass barrier material is responsible for the frequently observed reduction in its hydrogen retention properties.

The approach being studied is the isolation of the fuel from the barrier to prevent the oxygen transport phenomenon from occurring. Two experiments are in progress to evaluate the advantage obtained and a technique for preventing the fuel barrier interaction. In the first experiment, five fuel elements are being fabricated utilizing Hastelloy-N membranes coated with SCB-1 and with the fuel rod "canned" in 0.010-in.-wall type-304 stainless steel. The second experiment is designed to determine the kinetics of the fuel-barrier reaction by insertion and withdrawal of the fuel rod from the test membrane without disassembly of the test sample; this has been designated the sliding-fuel experiment.

2. Status

As mentioned, two experiments are in progress to evaluate the advantage of preventing the fuel-barrier interaction, and to develop techniques for its prevention.

In the first of these experiments, five fuel elements are being fabricated with the fuel canned in 0.010 -in.-wall type-304 stainless steel. This was demonstrated to be effective in preventing the fuel-barrier reaction in previously conducted experiments, as reported in the last SRIP Quarterly Progress Report. Two of the stainless-steel cladding assemblies have a single coat of AI-8763D hydrogen barrier material while the other three assemblies were left uncoated. The AI-8763D coating was applied to prevent possible reaction between the fuel rod and the stainless steel cladding. The canned fuel rods are being clad in SCB-1-coated S8DS chromized Hastelloy N cladding assemblies. These elements will be endurance tested at 1400°F for up to 20,000 hours.

The second experiment is being used to study the fuel barrier reaction by insertion and withdrawal of the fuel rod from the test membrane without disassembly of the test sample. Four membranes have been (or are being) tested at 1400°F and 2 atm hydrogen pressure. Three membranes were made of chromized Hastelloy N coated with SCB-1 according to the latest S8DS process. The fourth test membrane was fabricated using SCB-1 hydrogen barrier material applied to an oxidized Hastelloy X-285 tube. In each case, an increase in permeation rate was obtained after the fuel was inserted. The results are shown in Table 5.

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TABLE 5

HYDROGEN PERMEATION RATE INCREASE AFTER FUEL INSERTION
(Test temperature, 1400°F; hydrogen pressure, 2 atm)

Experiment No.	Coating	No Fuel [cc(STP)/hr]	With Fuel [cc(STP)/hr] After			
			24 hr	100 hr	200 hr	500 hr
Experiment 3	Hastelloy X-285 and SCB-1	0.1	2.0	4.2	5.5	—
Experiment 4	Hastelloy N and SCB-1	0.35	1.0	1.75	2.7	3.5
Experiment 6 Cladding 4275	Hastelloy N and SCB-1	0.31	0.55	0.82	1.0	—
Experiment 7 Cladding 4128	Hastelloy N and SCB-1	0.23	0.55	0.76	—	—

Further investigation is underway to determine the reason for the difference in magnitude of the change in permeation rate caused by the presence of the fuel and the fact that the sliding fuel experimental approach appears to enhance the reaction compared to that seen for endurance-tested fuel elements.

A Hastelloy X-285 membrane, coated with SCB-1, is under test at 1300°F and has accumulated 550 hours at 1 atm hydrogen pressure. The hydrogen permeation rate has remained between 0.15 to 0.17 cc(STP)/hr throughout the test. The experiment suggests that the fuel barrier interaction is strongly temperature-dependent. This experiment will be repeated.

V. COMPONENT DEVELOPMENT

A. POWER SWITCH

1. Introduction

SNAP reactor systems require electrical switching devices capable of switching dc currents up to 4 amperes. Applications include squib firing circuits and the output stages of the controller which operates the control-drum actuators. The controllers for the SNAP 10A and SNAP 8 reactors utilized relays to perform this switching function. Previous development tests have demonstrated that these relays are the fundamental components determining the reliability and lifetime of the controller. Relays are also strong sources of electromagnetic interference, while the driver stage circuits for the relays are very susceptible to noise. Therefore, one objective of the advanced electronics development program has been the development of solid-state output stages to increase the reliability of the actuator controller.

2. Selection of Approach

A number of approaches to the power switch problem were studied during the early stages of the investigation. The various devices considered fell into three categories: (1) systems utilizing nonlinear magnetic devices, i. e., saturable reactors or magnetic amplifiers; (2) solid-state switches belonging to the thyristor generic family; and (3) power transistors.

The magnetic amplifier approach was eliminated since the control system would be dependent on a source of unregulated ac power at all times, including reactor startup, which could constitute a serious handicap. Available test data, including previous AI work on the SNAP program, indicated that the thyristor family of devices, such as the SCR and similar types, are susceptible to radiation damage. Thus, this method would be acceptable only for manned applications.

Fortunately, development of radiation-resistant power transistors has achieved significant success recently. At least two companies have marketed power transistors capable of switching currents higher than those required for the SNAP controller, with test data showing gains of 10 to 15 after integrated neutron doses of 10^{14} . These power transistors were evaluated with regard to

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their electrical properties and irradiation test data. Based on this comparative evaluation, the Bendix BR100C was selected for use in the SRIP power switch development program. The characteristics of the BR100C given by Bendix are listed in Table 6.

TABLE 6
CHARACTERISTICS OF BENDIX BR100C POWER TRANSISTOR
Case type TO-59

Operating and storage temperature -65°C to $+200^{\circ}\text{C}$

Maximum ratings

Power dissipation 35 watts

Collector current 10 amperes

Electrical characteristics at $T_J = 25^{\circ}\text{C}$

Symbol	Test Conditions	Pre-Radiation Limits			Post-Radiation Limits $1 \times 10^{14} \text{ n/cm}^2$			Unit
		Mini- mum	Typi- cal	Maxi- mum	Mini- mum	Typi- cal	Maxi- mum	
V_{CBO}	$I_{CBO} = 1 \text{ ma}$	60	75	-	60	-	-	v
V_{CEO}	$I_{CEO} = 50 \text{ ma}$	40	50	-	40	-	-	v
V_{EBO}	$I_{EBO} = 1 \text{ ma}$	3	5	-	3	-	-	v
I_{CBO}	$V_{CBO} = 30 \text{ v}$	-	-	0.1	-	-	2	ma
h_{FE}	$I_C = 0.5 \text{ a}, V_{CE} = 5 \text{ v}$	40	-	-	-	-	-	-
h_{FE}	$I_C = 3 \text{ a}, V_{CE} = 5 \text{ v}$	40	100	200	15	25	-	-
h_{FE}	$I_C = 5 \text{ a}, V_{CE} = 5 \text{ v}$	25	-	-	-	-	-	-
$V_{CE(s)}$	$I_C = 3 \text{ a}, I_B = 0.3 \text{ a}$	-	0.5	1.0	-	1.5	2.0	v
$V_{BE(s)}$	$I_C = 3 \text{ a}, I_B = 0.3 \text{ a}$	-	-	1.5	-	-	-	v
C_{ob}	$V_{CBO} = 30 \text{ v}, f = 1 \text{ mc}$	-	-	75	-	-	-	pf
f_t	$V_{CE} = 28 \text{ v}, I_C = 0.5 \text{ a}$	-	500	-	-	-	-	Mc

3. Switch Circuit Functions & Requirements

The logic circuit of the SNAP 8 flight type controller is based on the use of core-transistor logic modules, which have been shown to possess a number of advantages over the more conventional all-transistor digital logic. These advantages include:

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1) A small number of active elements (transistors) required — Each logic element contains only one transistor, whereas ordinary digital logic requires two transistors and two diodes.

2) Lower power consumption — Information is stored in a core-transistor logic system in the magnetic state of the cores in each module. Once the core is pulsed to the desired state, no further power is required, whereas information is stored in the conduction state of one leg of a conventional digital stage.

3) Passive memory — Since information is stored in the magnetic state of the cores, loss of power does not cause loss of memory as it does in all transistor systems.

4) Greater resistance to radiation, due to the fact that operation is maintained with more degradation of the transistors than is possible with all-transistor systems. Irradiation of core-transistor modules has shown them to be capable of operation to beyond integrated fast neutron doses of 10^{14} .

Therefore, the primary requirement of the power switch circuit is that it be compatible with, and operable by, core-transistor logic stages.

The output from core-transistor logic modules, or CTL's, as they are called, is in the form of very narrow current pulses, of the order of 10 microseconds wide. On the other hand, the current pulses to the phases of the SNAP actuator typically must be several hundreds of milliseconds in width, as indicated by the chart of Figure 41. Therefore, the first requirement of the power switch, or output stage, is to use these narrow current pulses to generate the wide ones needed for operation of the actuator.

The second function of the output stage is amplification. The load current ordinarily delivered by a CTL module is approximately 150 ma, whereas the current required by the SNAP actuator can be as high as 4 amperes when the actuator is cold, such as at startup of the system. When the system is at its operating point, actuator phase current requirements are about 2 amperes.

Current must be maintained in at least one phase of the actuator at all times during a stepping sequence, to prevent loss of holding torque between steps. This need for "overlap" is shown in the timing chart of Figure 41, and constitutes

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Figure 41. SNAP 8 Actuator Phase Current Sequencing Requirements

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Figure 42. Transistor Power Switch Circuit Schematic

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the third requirement of the controller output stages. In the SNAP 8 controller, where the switching device was a latching relay, the difference between the "on" and "off" transfer times of the relay (typically less than a millisecond) was utilized to obtain overlap. However, in an all-solid-state system, this must be achieved electronically.

4. Circuit Design

A circuit to achieve the functions and meet the performance requirements described above is shown schematically in Figure 42. It consists of a flip-flop, two intermediate amplifier stages, and the output stage, or power switch. The flip-flop may be considered an electronic equivalent of the previously used latching relay, since it is bistable; i.e., it is triggered by an incoming pulse from a CTL, which causes one of the two transistors to conduct. The circuit will then maintain this condition until another pulse is received, at which time the conducting transistor will be turned off and the transistor in the other leg will be turned on. The flip-flop, therefore, provides the memory function required to convert the microsecond-long pulses from the CTL's to the long pulses required by the actuator. The circuit was designed so that the load current delivered by the "driving" leg of the flip-flop into the first amplifying stage is only approximately two milliamperes.

A digital circuit, of which the flip-flop, amplifying, and output stages are examples, is essentially an electronic switch. Thus, operation is not sensitive to the gain of the active element, provided it remains above some minimum value selected during the design phase. Since considerable degradation of transistors occurs in a radiation environment, this minimum value should be designed for less than 1/10 of the maximum initial gain. Hence, through most of the system operating time, each stage is being heavily overdriven; i.e., sufficient base current is supplied to each transistor to drive it well past saturation.

For example, the gain of a 2N2219 or 2N2222, as is used in the intermediate amplifying stages, is typically in the range from 40 to 100. However, in the power switch circuit design, the "forced", or "apparent" gain of the transistors is in the range of 10 to 13. The obvious penalty of this approach is that more stages are required. As shown in Figure 42, three stages (including the output switch) were used to amplify the 2-ma signal from the flip-flop to the 4 amp required by the actuator.

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In an actual controller, five circuits of this type are used, one for each phase of the actuator, and one for the actuator brake. The flip-flop for each switch receives inputs from two CTL's, one to turn the switch on and one to turn it off. All these CTL's are connected in a "ring counter", such as is shown in Figure 43(a). Outputs from the CTL's are obtained in the proper sequence as a "one" is shifted along the ring counter, in either direction depending on which direction of motion of the actuator is desired. [As drawn, Figure 43(a) would allow one direction only.] Whether an output is obtained from a CTL when it receives a shift pulse is determined by the magnetic state of its core at the time. Magnetization in one direction is referred to as the "zero" state, and in the other the "one" state.

For the solid-state power switch concept, the overlap previously mentioned (maintaining current in both phases concurrently for a short time during the transition from one phase to another) is obtained as follows: An asymmetrical oscillator is used, the output of which is a wave of the shape shown in Figure 43(b). The width of the narrow portion of the wave is selected for the amount of time overlap needed. The output of this oscillator is then fed into two CTI's (shift drivers), one of which is triggered by a rising wave and the other by a falling wave, as shown in Figure 43(b). A CTI is essentially a differentiator

Figure 43. Controller Power Switch Circuits

and pulse generator. It responds to either a rising or falling voltage level and generates current pulses of sufficient magnitude to change ("shift") the magnetic state of a number of core transistor logic stages (CTL's) simultaneously. The outputs from these two CTI's are two pulse chains, one leading the other by the overlap time. The outputs of these CTI's are delivered to the on and off CTL's as indicated in Figure 43(a) [delayed inputs marked "D"].

The lengths of time each power switch is on can be made to be any multiple of the oscillator period by simply running the undelayed pulses through a count-down chain of CTL's.

5. Test Circuit Design

The circuit shown in Figure 42 was first designed using a 2N2219 transistor as the final intermediate amplifier stage, or switch "driver." However, since it was being used close to its design operating level in this application, a second version of the switch circuit was designed using BR-100C transistors as both the switch and its driver. The two versions of the switch are shown in Figure 44, and the flip-flop design is shown in Figure 45.

The circuit built for testing is shown in block diagram in Figure 46. It includes the two versions of the switch previously described, the asymmetrical oscillator, two CTI's, and five CTL's. The period of the oscillator used is 250 milliseconds. Therefore, CTL No. 1 generates an off pulse to both switches every 250 milliseconds. CTL No. 2 delivers pulses lagging those of CTL No. 1 by 50 milliseconds (or leading by 200). These pulses are then counted down in a scale-of-eight counter (CTL's 3, 4, and 5). Thus, the switches receive on pulses every two seconds, and off pulses every 250 milliseconds, with the time between an on and the next off being 200 milliseconds. Therefore, the duty cycle of the switches was 10%, with a 200-millisecond pulse every two seconds.

6. Test

The circuit of Figure 46 is shown assembled in Figures 47 and 48. Due to scheduling problems with the Shield Test and Irradiation Facility, it was not possible to run an extensive series of pre-irradiation tests on the circuit. A temperature cycling test was performed in which the circuit was cooled to a -100°F and heated to +280°F. Although data on individual stages were not taken, operation of the entire circuit was monitored throughout this temperature range.

(a) CIRCUIT 1 USING BR100C AS BOTH SWITCH AND DRIVER

(b) CIRCUIT 2 USING 2N2219 AS SWITCH DRIVER

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Figure 44. Two Versions of Transistor Power Switch

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Figure 45. Flip-Flop Design

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Figure 46. Logic Diagram - Irradiation Test Circuit

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Figure 47. Transistor Power Switch and Associated Core-Transistor Circuitry Assembled for Irradiation Test (Front View)

Figure 48. Transistor Power Switch and Associated Core-Transistor Circuitry Assembled for Irradiation Test (Back View)

No apparent change in operating characteristics was observed from the low end of the range to the high end up to 275°F. The amplifier stages ceased to operate properly, however, at 280°F, probably due to high leakage currents.

The test circuit was then installed in the STIR in-pool capsule, and located in the pool at a distance from the reactor which receives fast neutron and gamma radiation in the approximate ratio specified in the SNAP 8 environment specification. Based on previous dosimetry for this capsule location, the fluxes at the reactor centerline at a reactor power of 1 Mw are:

$$\text{Fast neutrons (1.1 Mev)} = 3.15 \times 10^8 \text{ nv}$$

$$\text{Gammas} = 6.4 \times 10^5 \text{ r/hr}$$

The irradiation was performed in two runs, the first being 8 hours, and the second 16 hours in duration. The total integrated flux at the end of the test was calculated to be 2.4×10^{13} nvt and 1.54×10^7 r. The circuit was operated continuously during irradiation, except for periods in which voltages were measured to allow determination of the gain of each transistor. This data was taken every two hours for the first 17 hours and every hour for the last seven hours.

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Figure 49. V_{CE} vs Integrated Flux for BR100C Power Transistor
 Q_6 - Circuit 2 ($I_L = 4$ amp)

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Figure 50. V_{CE} vs Integrated Flux for BR100C Power Transistor
 Q_3 - Circuit 1 ($I_L = 4$ amp)

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The test was considered completely successful in every respect. No difficulties in circuit operation were observed at any time. Changes in the apparent gain of the individual stages were insignificant. One of the parameters which was considered an important measure of the operation of the solid-state switch is the V_{CE} of the output switching transistor with the required load current flowing through it. Due to the displacements induced in the semiconductor lattice by radiation, the conductivity of the material decreases with increasing integrated dose. This results in increased voltage drop across the transistor, increased transistor power dissipation, and decreased load current. Considerations of transistor power dissipation in the degraded condition and minimum acceptable actuator input voltages resulted in the selection of 5 volts V_{CE} as the "failure point" of this parameter.

V_{CE} data on both switching transistors for $I_L = 4$ amp) vs calculated integrated fast neutron and gamma dose is shown in Figures 49 and 50. Extrapolation of these curves indicates that successful operation of this type of switch should be possible to integrated doses of 8×10^{13} nvt and 5×10^7 r. If the actuator design were such that only three amperes were required, the switch would be expected to operate to integrated neutron doses of approximately 10^{14} nvt.

7. Conclusions

Successful completion of the design, fabrication, and test of the transistor power switch, as described above, has demonstrated that:

- 1) A power switch utilizing only transistors (no electromechanical relays) is capable of operating to integrated neutron and gamma doses expected in the vicinity of SNAP systems.
- 2) The transistor power switch circuitry is compatible with the output from core-transistor logic presently used in the SNAP 8 controller.
- 3) An asymmetrical oscillator capable of delivering the desired ratio of switch "on" time to overlap for use with the expected type of actuator is feasible.

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Figure 51. One-Inch-Diameter Shaft of Tantalum 10% Tungsten Coated with Al_2O_3 , Prior to Thermal Cycle Test

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B. HIGH-TEMPERATURE BEARINGS

The high-temperature bearing program is scoped to develop bearings for operation at 70 to 1500°F in deep space for as long as five years, the most demanding applications being those for self-aligning journal bearings which operate at slow speeds and endure long dwell periods. Design guidance for the bearing needs of future generations of SNAP reactor control systems will be gained from this development program.

The detail design of the bearing test fixture, described previously,^{*} has been completed. This test fixture represents a considerable advancement to the state of the art in environmental testing of bearing assemblies. It combines ultrahigh vacuum with very high temperature and is capable of testing both sliding and rolling element bearings under a variety of loads and speeds. The vacuum system has been demonstrated to be capable of maintaining 10^{-10} torr or lower pressure with simulated SRIP bearing outgassing loads. The test facility will be instrumented to provide immediate, reliable readings, and record various information such as bearing temperature, friction or torque, shaft speed, and loading.

Thermal cycling was conducted on two prototype bearing shafts to determine if the detonation-gun-applied Al_2O_3 coating on the tantalum-10% tungsten would withstand repeated thermal shock. The coated surfaces on each shaft were finish-ground to establish several different thicknesses of Al_2O_3 coating. The coating thickness ranged from almost zero, with bare metal exposed, to 0.005 inches. Each thickness area was separated from the next by a plunge cut through the coating into the base metal. Figure 51 illustrates the typical pre-test condition of the coated surfaces. Note the chipped areas of Al_2O_3 along the circular grooves. This chipping occurred during the plunge-cut operation and was left in this condition to see if propagation of the chipping would occur during thermal cycling.

^{*}L. M. Maki, "Progress Report, SNAP Reactor Improvement Program," January through March 1966, NAA-SR-11882 (CRD), May 23, 1966

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Figure 52. Thermal Cycle Test – Tantalum 10% Tungsten Shaft with Detonation-Gun-Applied Al_2O_3

Each shaft assembly completed 13 thermal cycles from approximately 170°F to 1580°F while in a 10^{-5} torr vacuum. Figure 52 shows the temperature-time curve for a typical temperature cycle. The average increasing and decreasing ramp rates were 14°F/minute.

Both shafts were visually and dimensionally examined after completion of the thermal cycle test. No spalling, cracking, or loss of the Al_2O_3 coating was evident over any of the coated area. The chipped areas, previously noted along the circular grooves, showed no evidence of increasing in size or lifting off the shaft. The shafts were slightly discolored after test due to a thin film deposit of chromium that sublimed from the stainless-steel chamber walls during the high-temperature cycling. The typical post-test condition is illustrated in Figure 53.

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Figure 53. One-Inch-Diameter Shaft of Tantalum 10% Tungsten Coated with Al_2O_3 , After Thermal Test

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Figure 54. Homopolar Actuator Assembly

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C. HIGH-TORQUE ACTUATOR

The control of SNAP reactor systems is accomplished by varying the neutrons that are allowed to escape through the external reflector. In the present SNAP 2, 10 or 8 systems this is effected by rotating drums, each coupled to an electro-mechanical drive system.

The actuator — the prime mover of this drive system — takes the electrical signal from the controller and transforms it into mechanical motion to rotate the reactor control element. Previous actuators have been developed to meet the needs of individual systems. A high-torque (advanced actuator) program, now underway, is scoped to develop a standardized unit to meet the needs of existing SNAP systems, as well as those projected in the future.

Present systems require drum motions of $1/4^\circ$ -arc increments, with torques from 10 to 17.5 lb-in. at the drums, and temperature capabilities to 1025°F. The dynamic performance parameters are obtained by an intermediate transformation device (simple gears) through which the actuator supplies torque and angular motion. Excellent performance has been demonstrated by the actuators throughout the stringent environments imposed.

1. Advanced Actuator

Future reactor systems may require performance values far exceeding the capabilities of existing actuators, and impose environments even more severe. The needed prime movers cannot be provided solely from extension of past work; new configurations and approaches are necessary to meet the requirements. The criteria for these advanced actuators have been consolidated into a performance specification, summarized in an earlier progress report.*

An envelope limit study defining the space allowable for an actuator was presented previously, as well.† An actuator configuration study within this space envelope concluded that, of the various concepts studied,* the homopolar actuator appeared the most practical.

Design layouts for the homopolar actuator were completed and the preparation of detail drawings has been initiated. The major design features of the actuator and the brake are shown in Figures 54, 55, and 56.

*L. M. Maki, "Progress Report, SNAP Reactor Improvement Program, October-December 1965," NAA-SR-11754 (CRD), February 15, 1966

†L. M. Maki, "Progress Report, SNAP Reactor Improvement Program, January-March 1966," NAA-SR-11882 (CRD), May 23, 1966

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Figure 56. Homopolar Actuator Brake Design

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A major difference between the high-torque actuator and the previous SNAP actuator designs is the segmented stator of the high-torque device. The Hiperco-27 stator shell segments are joined by fusion welding. The use of a nonmagnetic weld rod provides magnetic separation between the segments. The stator assembly is then split axially into two halves to facilitate assembly. The difference in thermal expansion between the Hiperco phase rings and the nickel alloy spacers produces relatively large stresses in the nickel alloy weld material during temperature changes. The dimensional stability of this stator design will be demonstrated by thermal tests prior to fabrication of the actuator.

2. Actuator Technology - Air Gap Geometry Test

The air gap geometry (AGG) test is a technique to gather experimental data necessary to verify analytical calculation of actuators. The AGG test actuator consists of a rotor with two groups of slots and teeth, one group with 0.020-in.-wide slots and teeth and the other group 0.040-in.-wide. Details of the AGG actuator and test methods were described previously.*

It should be noted that the actuator tested is a modified S8DS (heteropolar) actuator, but the data obtained should be applicable to the homopolar high-torque actuator.

Experimental testing to determine the variation of torque with variation in air gap geometry has been initiated. Test 1 was a verification test of the S8DS actuator performance. The air gap geometry parameters are shown in Table 7.

TABLE 7
AGG PARAMETERS, S8DS ACTUATOR PERFORMANCE
(TEST 1)

Component	Tooth Width (in.)	Slot Width (in.)	Air Gap (in.)	Number of Active Teeth
Rotor	0.060	0.060	} 0.0075	12
Stator	0.060	0.060		12

*NAA-SR-11882, Ibid.

The results of the test are shown in Figure 57. The torque curve essentially repeats those previously established for the S8DS actuator. This repeatability is significant since the AGG actuator stator for this test was composed of the shell and only two active poles. All previous tests for the S8DS actuator were performed with the normal eight poles in the stator, six poles being dormant and two poles energized. The test proved significant in that the torque was found to be the same for both configurations, demonstrating that the magnetic leakage flux through the dormant poles is negligible.

Tests 2 and 3 used the newly manufactured parts with an 0.0045-in. air gap; the parameters are shown in Table 8.

The results of these tests are shown in Figure 58. The curves show that the the 0.020-in. tooth configuration produces less torque than the 0.040-in. configuration. The output torque calculated by the classically derived equations would be greater for the 0.020-in. tooth configuration than that for the 0.040-in. tooth configuration.

Tests 4 and 5 were the same as Tests 2 and 3 except that one stator pole was removed and a smooth pole without teeth was inserted in its place. The

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Figure 57. Output Torque for S8DS Rotor Tooth Geometry - Air Gap Geometry Test 1

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Figure 58. Output Characteristics - Air Gap Geometry Tests 2 and 3

TABLE 8
AGG PARAMETERS, TESTS 2 AND 3

Test No.	Component	Tooth Width (in.)	Slot Width (in.)	Air Gap (in.)	Number of Active Teeth
2	Rotor	0.020	0.020	} 0.0045	32
	Stator	0.020	0.020		32
3	Rotor	0.040	0.040	} 0.0045	16
	Stator	0.040	0.040		16

purpose of this was to see if one-half the value of Tests 2 and 3 would result, and also to verify the lineup of the stator poles. The parameters for these tests are shown in Table 9.

TABLE 9
AGG PARAMETERS, TESTS 4 AND 5

Test No.	Component	Tooth Width (in.)	Slot Width (in.)	Air Gap (in.)	Number of Active Teeth
4	Rotor	0.020	0.020	} 0.0045	16
	Stator	0.020	0.020		16
5	Rotor	0.040	0.040	} 0.0045	8
	Stator	0.040	0.040		8

Figures 59 and 60 illustrate the results of Tests 4 and 5. The broken curve is one-half the value of that for Tests 2 and 3, taken from Figure 58. It is noted that the torque for the smooth pole tests is higher than the expected half-value torque. This is no doubt due, in large part, to the lower reluctance to magnetic flux of the smooth pole. Since more flux is conducted through both air gaps, the remaining toothed pole produces slightly more torque for a given magnetomotive force. Some small fraction of the increased torque must be ascribed to the absence of teeth misalignment at the smooth pole. This probably accounts for the more pronounced variation of the 0.020-in. tooth torque curves where teeth misalignments would have greater effect.

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Figure 59. Output Torque Characteristics -
Air Gap Geometry Test 4

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Figure 60. Output Torque Characteristics -
Air Gap Geometry Test 5

The next phase of the program was evaluation of the effect of larger air gap on the same tooth geometry. The larger gap size was obtained by grinding the rotor to a slightly smaller diameter, the resultant air gap being 0.0085 inch.

Tests 6 and 7 are similar to tests 2 and 3 except for the larger gap; parameters are given in Table 10.

TABLE 10
AGG PARAMETERS, TESTS 6 AND 7

Test No.	Component	Tooth Width (in.)	Slot Width (in.)	Air Gap (in.)	Number of Active Teeth
6	Rotor	0.020	0.020	0.0085	32
	Stator	0.020	0.020		32
7	Rotor	0.040	0.040	0.0085	16
	Stator	0.040	0.040		16

The test results, Figure 61, again show the loss of torque with the decrease in tooth width. Variation of torque with the tooth width for the two air gap conditions is plotted on Figure 62.

Additional information on the relationship of torque variation to air gap was provided by these tests (see Figure 63). With low magnetomotive force and negligible saturations, the torque varies directly with air gap size. This is as expected, and conforms to general theory. As the mmf is increased, the effect of air gap is decreased due to saturation. This effect, previously observed in the S8DS program, was again confirmed by the results of the AGG tests.

Some uncertainty has existed as to the effect of post-heat-treatment machining on Hiperco-27 material. Specifically, it was not known if machining a part after the basic stock had been heat-treated affected its magnetic properties, necessitating a post-machining heat treatment. This question was answered in the next phase of the AGG test. For this phase the stator poles (both 0.020-in. slots and 0.040-in. slots) were heat treated in accordance with the required specification. (No heat treating after final machining had been done for the previous tests). The stock (Hiperco 27) from which the parts were manufactured was heat treated in the basic stock form. This was in accordance with the manufacturer's recommendations to obtain optimum magnetic performance.

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Figure 61. Output Torque Characteristics - Air Gap Geometry Tests 6 and 7

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Figure 62. Output Torque vs. Tooth Width (at 600 ampere turns)

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Figure 63.
Output Torque
vs
Air Gap

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After re-heat treating the machined parts, they were assembled into the actuator and Tests 6 and 7 were repeated. The test parameters were the same as those listed in Table 10. The output torques were essentially the same as the pre-heat treated curves, as indicated in Figure 64. The small difference between the post-heat-treat curve and the pre-heat-treat points is within testing accuracy limits.

Another area investigated was the effect of slot depth on torque. To test this effect, all slots (0.040-in.) on both the stator and the rotor were filled to slightly more than one-half the slot depth with magnetic material. Figure 65 shows one pole with the magnetic material inserted in the slots, the other pole being shown in its normal configuration for comparison.

For the test, both poles had magnetic material in the slots. The unit was assembled and tested in the same manner as in Test 3. The solid line shown on Figure 66 is the torque curve of the shallow slot and is compared with the torque curve (broken line) at normal slot depth. The relatively small loss in torque indicates that most of the torque is obtained near the tooth tip.

A summary of the significant AGG test findings to this stage indicates the following:

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Figure 64. Output Torque Characteristics -
Air Gap Geometry Tests 8 and 9

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Figure 65. Experimental Procedure for
Determination of Slot Depth Effect
on Torque

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Figure 66. Output Torque vs Slot Depth - Air Gap Geometry Test 10

1) A heteropolar actuator of the configurations tested has negligible adjacent pole leakage effect.

2) The torque per tooth decreases as the tooth width decreases.

3) Torque is proportional to the number of teeth.

4) Torque varies almost directly with air gap size for a given configuration in an unsaturated condition.

5) Machining of teeth has little apparent effect on permeability of properly heat-treated Hiperco 27 material.

6) For the configuration tested the slot depth beyond one-half the slot width is relatively unimportant for torque generation.

7) Equations found in most reference books relating reluctance torque to air gap geometry and based upon energy considerations for a single tooth pair, yield grossly inaccurate results when applied to a multitooth system with tooth spacing < 0.060 inch.

The air gap geometry tests are not completed; further testing with a larger air gap and wider teeth will be performed to extend present knowledge and to assist in reconciling theory with measured results.

3. Analytical Technology

There are three techniques available for calculating the output torque of reluctance-type actuators. One involves equations derived from the change of stored magnetic energy in an air gap. The second technique is a static method involving the attraction of ferromagnetic surfaces in a magnetic flux field. The third is a method of ratio where the results of a tested machine are extrapolated to find the performance of a new actuator.

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For the change-of-energy technique the fundamental equation is:

$$T = K \frac{E}{X} r \quad \text{lb-in. per tooth} \quad \dots(1)$$

where

E = energy stored in the air gap,

X = tooth displacement (linear),

r = radius to the mean air gap,

T = torque, and

K is a constant which includes rotor diameter.

This equation, however, is not useful in this form. With certain assumptions, the equation may be used to derive somewhat more useful equations. These are:

$$T = 70.9 D (NI)^2 \frac{dP}{dx} 10^{-9} \quad \text{lb-in. per tooth}^* \quad \dots(2)$$

$$T = 1/2 (NI)^2 \frac{dP}{d\theta} \quad \text{joules per radian}^\dagger \quad \dots(3)$$

where

D = diameter to the mean air gap

P = permeance

NI = ampere turns (mmf)

θ = tooth displacement (radians)

Equations 2 and 3 are the same, in slightly different form. The difficulty of using these equations is explained subsequently.

Equations easier to use may be further derived by assuming certain expressions for permeance. One of these is:

*Suhr, F. W., "Torque of Reluctance Type Magnetic Coupling," AIEE Transactions, Paper No. 52-167, pp 581-584, August 1952

†Roters, H. C., Electro-Magnetic Devices, John Wiley & Sons, pp 204-205

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$$T = \frac{70.9}{10^9} \left(\frac{rl}{g} \right) (NI)^2 \quad \text{lb-in. per pair of teeth}^* \quad \dots (4)$$

where

l = length of active magnetic iron (in.)

g = air gap length (in.).

Another form of this equation is

$$T = 0.222 B_g^2 g l n \quad \text{oz-in.} \quad \dots (5)$$

where

B = air gap flux density

n = number of active teeth.

These equations are relatively easy to use, but lead to gross errors in some cases, as is explained in a subsequent discussion.

The method of static analysis involves the equation

$$T = K B^2 A \quad \dots (6)$$

where A is the active iron area. The same comments apply here as those for Equations 2 or 3.

The ratio method essentially involves using Equations 4 or 5 and, as stated, may lead to large errors, depending on which factor is constant and what is being ratioed.

A summary of the foregoing results in the conclusion that the basic equation (1) is not directly usable. Equations 2, 3, and 6 are difficult because of the methods used to evaluate permeance and effective magnetomotive force. Equations 4 and 5 are easy to use but can lead to gross inaccuracies. The following discussion will serve to illustrate some of the problems involved in the use of the equations and the methods under investigation for their solution.

*Roters, op. cit.

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Figure 67. Typical Flux Plot

Figure 68. Calculated vs Actual Performance

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Referring to Equations 2 and 3, the author* states, "This all seems very straightforward and simple until we try to evaluate P and dP/dx ." One method of evaluating this has been to flux-map the air gap geometry. In this technique the permeance (P) of the configuration is estimated by making a scale drawing of the geometry to be analyzed. Equipotential lines are drawn and at right angles to these the magnetomotive force (mmf) or flux flow tubes are drawn. This would be a suitable method if an accurate flux map could be drawn. Usually, flux mapping is a tedious operation, the results of which rarely justify the time and cost. The general rules for flux plotting are difficult to follow in regions of nonuniform geometry such as in the slot portions of the actuator. Figure 67 is a typical flux plot performed manually; this has since been shown experimentally to be inaccurate.

Equation 6 also involves the use of a flux plot to evaluate the flux density, and the same criticism applies.

The use of Equations 4 and 5 has also yielded erroneous results. The torque curve shown in Figure 68 was derived by these equations and is compared to experimental data. Not only is there a large discrepancy between the calculated and test curves, but the calculated curves show increase in torque as the teeth are made smaller and more numerous. This is the reverse of the test data conclusions. Saturation, of course, plays a part, causing the curve to bend at high mmf's, but even at lower mmf's where all the curves are concave upward, the errors are significant. The ratio method is another way of using the same equations; hence the same faults are evident in this method.

For the S8DS actuator, and similar actuators such as the S8DRM-1 and SNAP 10A, these equations were used successfully, resulting in reasonably accurate predictions for the following reasons. In the case of the S8DRM-1 and SNAP 10A, the torque requirements were so low (12 to 20 oz.-in.) that straight application of the formula gave fairly accurate results. Inspection of Figure 68 shows that at very low torques the basic equation curve is almost identical with the tested 0.060-in. teeth up to about 70 ounce-inches.

To obtain the higher torque output for the S8DS actuator, various corrections to the basic equation were applied, providing a pessimistic calculation. These

*Suhr, op.cit.

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Figure 69. Preliminary Equipotential Plot

Figure 70. Detailed Flux Plot

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corrections, involving the use of the Carter coefficient and other "allowances" were used under "designer's license" to establish reasonable margin. As proved by the end results, the calculated and the tested curves matched very closely, as indicated in Figure 68 for the 0.60-in. teeth.

For comparison, the 0.040-in. tooth AGG test configuration was calculated using the same empirical factors as for the 0.060-in. teeth S8DS configuration. The large deviation shown by this curve (broken line) from that of the test data (see Figure 68) indicates that the "empirical factor" must be tailored for each configuration. Data shown in this figure illustrate the need for a more consistent method for performance calculations.

The foregoing has described the inadequacy of present methods. These methods, however, should prove satisfactory if a good flux map could be produced. An analog technique for flux plotting was investigated. This is accomplished by use of electrically conductive paper cut to the desired geometry and to a suitable scale. A potential was generated across the gap and an electric probe was used to find lines of equipotential. Figure 69 shows the S8DS configuration cut to scale of 125 to 1; the lines of equipotential mmf's are shown. Since it was necessary to draw in the flux flow lines at right angles to these lines, it was decided to map the flow lines directly, these being the torque-producing factors. Figure 70 shows the flux flow lines obtained in the same manner except that the potential was generated horizontally rather than vertically as in Figure 69.

Although no detailed calculation of output torque has been made, a preliminary estimate indicates the results to be very close to the tested values in the region where saturation is not an important factor. Further reevaluation of this technique will be made in conjunction with experimental data from air gap geometry tests.

4. Coil Materials Technology

The coil materials technology program includes both the development and evaluation of the individual constituents of a high-temperature coil. The main constituents are the encapsulant, conductor, and the bobbin. Test programs have been established to define the limitations of service operation variables such as temperature, voltage, and thermal cycling on these subcomponents.

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Tests have been conducted at temperatures to 1200°F for periods up to 2000 hours. A summary of test results to date is given below.

a. Encapsulant

In electromagnetic devices, insulation is required to physically separate and electrically isolate adjoining turns. A technique of wet-winding has been developed at AI in which an inorganic insulation is applied to the conductor as the coil is being formed. The encapsulants being evaluated in this program are AI-developed AI-1 cement and PBX* cement. A test was conducted on these materials at 1200°F for 2000 hours in vacuum, with the results given below:

Insulation Resistance (ohms)

<u>Material</u>	<u>Initial</u>	<u>Final</u>
AI-1	480 K	480 K
PBX	480 K	150 K

Further tests on these materials will be conducted at 1300°F and 1400°F. AI-1 cement is considered the reference design choice for the high-torque actuator, with PBX as the backup material.

b. Conductor

Conductors currently being evaluated in this program include copper, nickel-clad silver, and stainless steel-clad copper. Tests are being performed at 1100 and 1200°F; results are given below:

<u>Material</u>	<u>Test Temperature (°F)</u>	<u>Time (hr)</u>	<u>Change in Resistance</u>
Copper	1100	1000	0 %
Copper	1200	1000	+1 %
SS Clad Copper	1100	1000	0 %
SS Clad Copper	1200	1000	+7 %
Ni-Clad-Ag	1200	840	+1 %

*Proprietary Material of Robert G. Allen Co.

REF ID: A66011

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Metal bobbin materials are presently being evaluated with inorganic insulation to ground. Stainless steel bobbins, lined with AI-1 cement, PBX cement, and mica sheet have been tested at 1200°F for 1000 hours. All of these materials appear satisfactory for 1200°F service. All samples were subjected to 50 thermal cycles between 500 and 1200°F in addition to the 1000-hr dwell at temperature. The readings of insulation resistance were made at 50 and 500 volts. The 500-volt readings are given below (in megohms):

<u>Material</u>	<u>Initial</u>	<u>@ 1000 Hours</u>
Mica	20	4.8
PBX	0.3	9.0
AI-1	1.5	9.0

Further tests are planned at 1300 and 1400°F. These same insulation materials will be tested on Inconel bobbins at 1300 and 1400°F.

d. Configuration Study

Coil materials are being evaluated in a coil configuration similar to the geometry which will be employed in the high-torque actuator program. The coils were wound on stainless-steel bobbins, using AI-1 cement and 24-gauge stainless-steel clad copper wire.* The coils are being evaluated at 1200 and 1300°F, with thermal cycles performed at the rate of 5 per week. The 1200°F test has accumulated a total elapsed time of 1593 hours, with 51 thermal cycles being performed during this period. The 1300°F test has a total elapsed time of 1260 hours, with 45 thermal cycles. The conductor resistance has increased approximately 6.5% at 1200°F and 20% at 1300°F.

*L. M. Maki, "Progress Report, SNAP Reactor Improvement Program, January-March 1966, NAA-SR-11882 (CRD), May 23, 1966

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