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**REACTOR ENGINEERING BRANCH ANNUAL REPORT
FISCAL YEAR 1968**

IDAHO NUCLEAR CORPORATION
NATIONAL REACTOR TESTING STATION
IDAHO FALLS, IDAHO

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REACTOR ENGINEERING BRANCH ANNUAL REPORT FISCAL YEAR 1968

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INTRODUCTION

The Reactor Engineering Branch represents one of the technical organizations of the Nuclear and Chemical Technology Division of the Idaho Nuclear Corporation. Branch personnel perform development work in the areas of materials science, nuclear fuels, and engineering. It operates two low power critical facilities in support of the test reactors, two reactivity measurement facilities for physics research, and conducts a school for training engineering and science graduates in test reactor operation.

The activities of the Branch's five sections are highlighted in the following pages covering the FY-1968 period. This report represents the first such compilation on an annual basis. Previous work has been reported in Idaho Nuclear Technical Branches Quarterly Progress Reports, in Reactor Fuels Development Annual Reports, and in appropriate Topical Reports. Many references to these earlier reports are listed throughout this report. More complete listings may be obtained from the authors of this edition.

The Branch organization as it existed at the end of FY-1968 is shown at the end of the report.

SUMMARY

MATERIALS RESEARCH

In hydrogen embrittlement studies, irradiation of specimens increased the rate of hydrogen occlusion and the apparent solubility after cathodic charging. Increased hydrogen content from 0.2 to approximately 7 ppm resulted in a loss in ductility (reduction in area) of the unirradiated steels but without marked increase in the as-received material of susceptibility to hydrogen-induced cracking. Modification of the microstructure by heat treatment of A542 carbon steel resulted in a tendency toward hydrogen-induced cracking.

Surveillance examinations of beryllium in the ETR revealed cracks in the reflector which had received a fluence of 1.7 to 2.3×10^{22} n/cm² $E > 1$ MeV. Examinations of other beryllium has given an essentially constant growth rate as a function of fluence of 0.02% per 10^{21} n/cm² $E > 1$ MeV, and electron transmission microscopy has shown that not all of the produced helium can be ascribed to the bubbles resolved (25-130A).

Specimens of cladding and structural materials for pressure vessels and high temperature usage are being irradiated in EBR-II for post-irradiation fatigue tests principally in support of the fast reactor program, although carbon steel specimens are also contained in the first two subassemblies.

METALLURGICAL DEVELOPMENT

The use of boron-aluminum foils, fabricated by powder metallurgy procedures, and incorporated in the fuel plate between two halves of a wrought uranium-aluminum alloy is being investigated as a possible fuel fabrication technique for the ETR. Irradiation results indicate some performance improvement in B₄C-Al foils over B-Al foils. Blister-anneal tests produced failure at 400°F for burnups up to 10^{21} fission/cc.

An ETR fuel element containing powdered UAl_x which had been irradiated to 6000 MWd at 2.56% of core power was evaluated and found to have had highly acceptable performance.

Electron beam microprobe studies made on samples taken from a partially melted ETR fuel element fueled with powdered uranium-aluminide indicated that the fuel plate cores had reached temperatures of 1340 to 2460°F, and had not released appreciable amounts of fission products.

Failure analysis indicated that large cold reductions (60%) used in the manufacture of aluminum-clad sample plates do not provide sufficient bond strength between the fuel plate core and cladding to resist swelling. A non-destructive test for boron assaying was developed consisting of tagging the boron with Co-60 in order to permit detection by gamma scanning. The tagged boron was incorporated as a glass in dispersion-type fuel plate cores using powder metallurgy procedures.

An investigation of silicon additions in uranium-aluminum alloys disclosed that when the silicon-to-uranium atom ratios are below one the silicon is concentrated in the intermetallic particles of the alloys. When this atom ratio exceeds unity the particles become saturated and the excess silicon appears in the matrix of the alloy.

Corrosion tests on a potential aluminum cladding alloy, 2219, followed by microprobe examination disclosed that aluminum in the base alloy preferentially migrates into the oxide corrosion layer leaving an enriched layer of copper at the metal-oxide interface and a decreased copper concentration in the corrosion layer. No copper was detected in the water of the corrosion loop.

Bulk UAl_3 pellets irradiation-tested at heat generations of 23 kW/linear foot have shown complete fission gas retention.

ENGINEERING EXPERIMENTS

Experimental and analytical fuel element investigations have been directed toward better defining the ATR operating conditions and limits. Local coolant flow rates were measured in the ATR core and factored into the computer code used in fuel element thermal hydraulic analysis.

REACTOR TRAINING

An ATR simulator consisting of duplicate console and analog computer has been assembled for training reactor operators.

CRITICAL FACILITIES

The first critical fuel loading in the ATR was attained July 2, 1967. The subsequent zero-power experiment program, which had also been performed previously in the ATRC, confirmed the nuclear correspondence between the two reactors. Additional low power experiments in the ATR have strengthened reactor physics correlations with the ATRC and provided data on temperature-induced reactivity effects which could not be readily obtained from the ATRC. Increasing reactor coolant temperature had only a small effect on shim and safety rod worths; however, the outer shim worths increased significantly when the experiment loop temperatures were raised. Measurements in typical ATR experiment irradiation positions show that fast (ie, $E > 1$ MeV) neutron flux and gamma heating can be predicted from low power measurements in the ATRC within 5 and 20%, respectively. The power spectral density, from zero-power noise measurements, shows prominent resonances attributed to flow-induced vibrations of the in-pile experiment tubes. Power distribution measurements in the ATRC show that the core hot-spot power generation can be reduced 18% by using radially zone-loaded fuel elements, without affecting the fission rate in adjacent loop experiments. The effect on other experiments in the core of changing or removing a loop experiment train can be minimized to the point

of insignificance if the reactivity effect of the change or removal is compensated by the adjacent outer shims.

A computer program, GRIPE II, was written for the analysis of data from pulsed-neutron experiments in either critical or subcritical reactors.

Detailed fission rate measurements in the same fuel loading in both ETR and ETRC have provided a comprehensive comparison of the two reactors. Some relatively large (eg, 20%) differences were noted in the power distribution, which have been tentatively attributed to inadequate experiment mock-ups in the ETRC. Marked improvements have been made in intercycle and intracycle flux stabilization in the ETR. Improvements in the former have been effected through a more comprehensive program of measurements in the ETRC on the mock-up of the fuel and experiment loading preceding each ETR cycle. Reduction of intracycle flux changes has been realized by increasing the U-235 and boron contents of the fuel elements which reduced the reactivity excursion throughout a cycle of operation. This in turn reduced control rod motion and hence flux changes, which are primarily influenced by rod motion.

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I. MATERIALS RESEARCH

1. EFFECTS OF IRRADIATION ON THE SUSCEPTIBILITY TO HYDROGEN EMBRITTLEMENT OF PRESSURE VESSEL STEELS (C. R. Brinkman, J. M. Beeston)

Hydrogen-induced cracking of a reactor pressure vessel in service is considered most unlikely for the following reasons:

- (1) Hydrogen-induced fracture generally occurs only in high strength steels, ie, hardnesses greater than $R_C 25$ and then at high stress levels.
- (2) Pressure vessels when fabricated in accordance with the ASME Boiler and Pressure Vessel Code, Section III, Nuclear Vessels, have a relatively low yield strength and a high degree of toughness.
- (3) Hydrogen-induced cracking in high strength steels at design maximum stress levels requires a threshold hydrogen concentration of the order of 2 to 4 ppm (by wt). This is considered to be in excess of the concentration level possible as a result of metal-water corrosion reactions which are thought to be the major source of hydrogen in a nuclear reactor.

However it is possible to envision a series of events which would render a vessel susceptible if a source for generation of sufficient quantities of monatomic hydrogen were also present. Irradiation damage as a result of exposing the material to peak fluences, ie, $3 \times 10^{19} \text{ n/cm}^2 E > 1 \text{ MeV}$, coupled by aging embrittlement involving nickel and/or aluminum [1], eg, ASTM A533, HY-80, A508, and A540 steels, might well render the material sufficiently brittle. An undetected flaw in an improperly heat-treated region (HAZ) of a weld as a result of failure to follow Code practice coupled with an unusually high stress concentration or some complex combination of any of these could well render the material susceptible to hydrogen-induced cracking.

It was not the objective of this research to investigate complex processes by which a reactor pressure vessel might pick up several ppm of hydrogen, but rather to determine whether several of the medium strength pressure vessel steels in the irradiated condition were inherently susceptible to hydrogen-induced cracking. Toward this end, plain and notched specimens were fabricated from three steels, ie, ASTM A302B, HY-80, and A542 in their normal quenched and tempered condition. In addition, specimens of A542 were heat-treated to different strength levels and microstructures to investigate the susceptibility to hydrogen-induced cracking as a function of strength level and microstructure. There is some evidence that A542 steel has a very sensitive relationship between the tempering temperature and notch toughness that leads to a disconcerting scatter in the measured values [2].

Specimens of these steels have been and are presently being irradiated to a fluence ranging from 1×10^{18} to approximately 10^{21} n/cm² E > 1 MeV. Two tests are being conducted to evaluate the behavior of hydrogen in these steels. Plain tensile specimens were cathodically charged with hydrogen to different hydrogen concentrations and then tensile-tested to determine the magnitude of hydrogen-induced decrease in ductility. Typical of the ductility data taken thus far on unirradiated steels is that shown in Figure I-1 for HY-80 steel. Photographs of representative specimens showing the transition in fracture mode from ductile to quasi brittle are shown in Figure I-2. Similarly, notched specimens were cathodically charged to different hydrogen concentrations and then statically loaded at different stress levels to failure to determine at what stress level, if any, hydrogen-induced cracking begins. Typical data taken are shown in Figure I-3 for unirradiated HY-80 steel. The other two steels, A542 and A302B in their normal quenched, tempered, and unirradiated condition, show a similar lack of hydrogen-induced cracking and subsequent failure at stress levels other than near the ultimate strength. Data taken from statically loaded specimens of A542 with a modified heat treatment show a marked tendency toward hydrogen-induced cracking (Figure I-4).

It is thus concluded that:

- (1) Increasing the hydrogen content from 0.2 to approximately 7 ppm resulted in a loss in ductility of the unirradiated steels tested in the normal quenched and tempered condition, but that this did not lead to a marked increase in susceptibility to hydrogen-induced cracking.
- (2) It is possible to modify unirradiated A542 steel, one of the prime candidates for the nuclear pressure vessel, by heat treatment to produce a higher strength such that it will show a tendency toward hydrogen-induced cracking.

Fig. I-1 Effect of hydrogen on the ductility (reduction of area) of HY-80 steel.

(A) SPECIMEN 31-24 PRECHARGED

(B) SPECIMEN 31-46 1.1PPM H_2

Fig. 1-2 Fracture surfaces of hydrogen-charged plain tensile specimen of A302B steel.

(C) SPECIMEN 31-42 3.2PPM H_2

(D) SPECIMEN 31-3 5.8PPM H_2

Fig. I-2 Fracture surfaces of hydrogen-charged plain tensile specimen of A302B steel.

Fig. I-3 Time-dependent fracture studies compared for preirradiated HY-80 steel.

Fig. I-4 Time-dependent fracture study for preirradiated A542 steel that had been austenitized, water quenched, and tempered to R_c 34-37.

2. EFFECTS OF IRRADIATION ON THE SURFACE

ACTIVITY AND DIFFUSIVITY OF HYDROGEN IN STEELS

(C. R. Brinkman, J. M. Beeston)

Cathodic hydrogenation studies were carried out in order to determine the specifics of the technique required to obtain reproducible hydrogen concentrations, in the range from 1 to 7 ppm (wt) in specimens of several pressure vessel steels, ie, ASTM A542, A302B, and HY-80. These steels are currently being tested in the non- and post-irradiated condition in order to determine the degree to which they are susceptible to hydrogen embrittlement as indicated by loss of ductility and a tendency toward time-dependent failure.

The variables examined and found to affect the concentration of hydrogen produced in these steels were cathodic charging current density, concentration of electrolyte poison in a 4% sulfuric acid solution, irradiation, and time. Specimens used were irradiated to a fluence of 4×10^{17} n/cm² E > 1 MeV and allowed to cool to 2 to 3 R/hr at contact prior to testing. After cathodic charging the specimens were analyzed in a Balzers' EA-1 Exhalograph, shown in Figure I-5. The analyzer is capable of quantitatively determining oxygen, nitrogen, and hydrogen concentrations down to 0.05 ppm. The analyzer has been attached as shown in Figure I-5 to a small mass spectrometer (foreground) to permit quantitative determination of other gases found in irradiated specimens.

Typical data obtained during this investigation are shown in Figures I-6 and I-7. When as-analyzed hydrogen concentration is plotted as a function of square-root of time (Figure I-6), a linear relationship exists up to a saturation concentration. Increasing the charging current density increases the rate of occlusion of hydrogen without greatly changing the saturation level; but irradiation increases the saturation level as well as the rate of occlusion for a given current density. Figure I-7 shows that increasing the poison concentration of arsenic similarly increases the rate of occlusion but does not appreciably change the specimen saturation level.

These effects were explained as follows: Increasing the current density increases the amount of monatomic hydrogen in the solution electrical double layer and, therefore, the amount of absorbed monatomic hydrogen which is available for diffusion into the steel. Arsenic seems to impede the recombination reaction,

which occurs at the metal surface; therefore, the amount of monatomic hydrogen available for diffusion at the solution-metal interface is increased. Hence, the increase in the rate of occlusion induced by an increase in current density or arsenic concentration is due to an increase in the amount of monatomic hydrogen available at the surface. The effect of irradiation on the occluding characteristics seems to be more complex, and it is thought that irradiation either alters the specific rate constants of the absorption-desorption reactions involved or changes the number of unique surface sites that are participating in these reactions. Irradiation-induced changes in solubility may be due to an increase in hydrogen trapping.

Fig. I-5 Gas Analyzer.

Fig. I-6 Cathodic hydrogenation of pre- and post-irradiated HY-80 steel at 0.01 amp/in.² and 0.1 amp/in.².

Additional hydrogen diffusivity and surface activity experiments using irradiated specimens are planned as work to be accomplished in the near future as a part and in support of the hydrogen embrittlement program. Specific details of information reported in this section may be found in a recent topical report^[2].

Fig. 1-7 Cathodic hydrogenation of pre- and post-irradiated HY-80 steel.

3. SURVEILLANCE OF TEST REACTOR BERYLLIUM

(J. M. Beeston, W. F. Zelezney)

The beryllium in the test reactors (ATR, MTR, and ETR) was monitored during the year through the ATR Beryllium Surveillance Program and through observations in the ETR and MTR including the photographs of the MTR reflector face. Encapsulated beryllium samples for the ATR Beryllium Program were irradiated in the ETR K-6 filler piece throughout the year. Because of the delay of the ATR in reaching full power, transfer of the capsules from the ETR to the ATR was delayed. The samples had received fluences ranging from 2 to 7×10^{21} n/cm² E > 1 MeV at the end of ETR Cycle 94.

Observation of the ETR beryllium Cycle 94-96 shutdowns revealed cracks across the top of the beryllium adjacent to C12 and G14 positions. The beryllium adjacent to C12 has received a fluence of 1.68×10^{22} n/cm² E > MeV at the core side edge and 1.68×10^{21} n/cm² at the outer edge. The beryllium adjacent to G14 has received a fluence of 2.29×10^{22} n/cm² at the core side edge and 2.74×10^{21} at the outer edge.

Photographs of the MTR reflector face revealed some general deterioration of the beryllium reflector primarily as additional spalling and cracking with two pieces from around the HB-2 beam tube being found in the reactor. These pieces were analyzed for helium and annealed to determine the threshold temperature for swelling. It can be seen in Figure I-8 that the threshold swelling temperature is > 550°C and decreases as the fluence increases. Although the mobility of the helium in the MTR reflector is low at reactor temperatures (< 100°C), significant gas release evidently occurs since conversion of the helium content on the basis of the analysis to a fluence value gave the low side of the range (1.3×10^{22} n/cm²) indicated in Figure I-8, and the calculated integrated flux gave the high side of 4.0×10^{22} n/cm². With a threshold temperature in excess of 550°C, catastrophic swelling is not anticipated for the MTR reflector unless plugged coolant channels allow a considerable increase in beryllium temperature. Hence, the deterioration should be limited to additional spalling and cracking of the general nature seen during the year.

It has been concluded that the growth of beryllium at temperatures less than 150°C bears an essentially constant relation to the fluence and is apparently independent of the strength of the beryllium. As indicated in Table I-1, the percent change in length increases with fluence, the rates remaining nearly constant at 0.02% per 10^{21} n/cm² E > 1 MeV. The effect appears to persist up to fluences of 1.45×10^{22} n/cm² which corresponds to atom fractions of 0.007 atom helium per atom beryllium. Any movement of helium would appear to occur by diffusion under growth conditions.

In order to get information on the location of the helium during growth, Kossel patterns were taken on zone-refined single crystal beryllium and indexed as a preliminary step in determining the lattice parameters of the beryllium with great precision. After irradiation the lattice parameters of this beryllium will again be determined by the same method. Precision density measurements will also be made. This information is expected to lead in turn to a knowledge of the defects (eg, vacancies, interstitials, and helium gas either within the lattice or in trapping sites) produced in beryllium by the irradiation.

Fig. I-8 Length changes in irradiated beryllium after annealing one hour at each temperature.

After irradiation at temperatures less than 150°C the helium is distributed mostly in small trapping centers. The nature of these trapping centers has not been fully defined. They were not resolved in electron transmission micrographs of beryllium irradiated to 10^{22} n/cm² as reported by Barnes[3], nor do gas bubbles show in transmission micrographs as do defect clusters (dislocation loops, etc) observed in other metals. In samples heated at 600°C for one hour the bubbles were resolved at a size range of 25-130Å.

Swelling of the irradiated beryllium occurs at temperatures $> 500^\circ\text{C}$ as a function of fluence and hence concentration of helium. The kind (strength) of the beryllium appears to have an effect on the swelling, Figure I-8. Mechanical property changes at reactor temperatures are such that at 5×10^{21} n/cm² the yield strength becomes practically identical to the tensile strength. After 600°C irradiation the compressive yield strength is about 1/3 the compressive ultimate strength which has decreased from the value of the controls, and the specimens evidence considerable (20%) plastic strain.

TABLE I-1

GROWTH OF BERYLLIUM IRRADIATED AT TEMPERATURES
LESS THAN 150°C

Material	Fluence $n/cm^2 E > 1 \text{ MeV}$	$\Delta L/L(\%)$	$\Delta L/L(\%)$ Per $10^{21} n/cm^2$
Hot Pressed 1.2% BeO (4 samples)	1.5×10^{21}	0.056 ± 0.012	0.038
Hot Pressed 2.0% BeO (4 samples)	2.3×10^{21}	0.068 ± 0.015	0.029
Extruded 1.2% BeO (5 samples)	2.1×10^{21}	0.036 ± 0.007	0.017
Hot Pressed 2.0% BeO (5 samples)	4.4×10^{21}	0.089 ± 0.011	0.024
Extruded 1.2% BeO (4 samples)	4.9×10^{21}	0.118 ± 0.016	0.020
Hot Pressed 1.2% BeO ^[a] (8 pieces)	$1.45 \times 10^{22[b]}$	---	0.020

[a] MTR lattice pieces on which the bowing strain was computed as percent change in length.

[b] Maximum value.

4. FATIGUE OF CLADDING AND STRUCTURAL MATERIALS (J. M. Beeston, C. R. Brinkman)

The program to determine the fatigue properties of fuel cladding materials for fast reactors (SS 304, 304 Ti modified, 316, 316 Ti modified, and Incoloy 800) and of structural materials for water power reactors (pressure vessel steels: ASTM A302B, HY-80, and A542) and liquid metal fast breeder reactors was approved in April 1967. The behavior of the fatigue properties, plastic strain and stress ranges, fatigue life, and fatigue ductility and strain hardening exponents will be determined as a function of temperature and fluence. The test program is planned to provide correlations between non- and post-irradiated conditions and between microstructures modified to tolerate greater or less gas retention (titanium modified).

The fatigue specimens are hourglass shaped to reduce buckling problems from elevated temperatures and from the large strains which produce low cycle fatigue. The specimen, Figure I-9, is loaded in the axial push-pull mode; the diametral strain is measured with an extensometer, Figure I-10. From relationships between the axial strain and the diametral strain, the axial strain is

calculated. These values are useful for design purposes, as in ASME Boiler and Pressure Vessel Code, Section III, for comparison with calculated pressure stress, thermal stress, and stress concentrations. In strain-controlled axial fatigue, a stress amplitude, $S = E \frac{\Delta \epsilon}{2}$, is calculated from the total strain range,

$\Delta \epsilon$, and Young's modulus, E . The axial strain is computed from the diametral strain since by definition the strain consists of an elastic and plastic component through the following relationships:

$$\frac{\Delta \epsilon}{2} = \frac{\Delta \epsilon_p}{2} + \frac{\Delta \epsilon_e}{2} = \epsilon_p + \epsilon_e = \epsilon_p + \frac{\sigma}{E} \quad (1)$$

$$\frac{\Delta \epsilon_d}{2} = \frac{\Delta \epsilon_{dp}}{2} + \frac{\Delta \epsilon_{de}}{2} = -\nu_p \epsilon_p - \nu_e \epsilon_e \quad (2)$$

where

$\Delta \epsilon$, $\Delta \epsilon_p$, and $\Delta \epsilon_e$ = total, plastic, and elastic strain gage, respectively

ϵ_p , ϵ_e = plastic and elastic strain amplitude

ν_p , ν_e = Poisson's ratios for plastic and elastic stress-strain

and the suffix d indicates diametral directions. Solving the ϵ_p from Equation (2) and substitution into Equation (1) with the use of the absolute value of ϵ_d gives

$$\epsilon = \frac{\sigma}{E} \left(1 - \frac{\nu_e}{\nu_p} \right) + \frac{\epsilon_d}{\nu_p} \quad (3)$$

which allows the axial strain amplitude, ϵ , to be determined from the stress amplitude, σ , from measurements of the diametral strain amplitude, ϵ_d , Young's modulus, E , and Poisson's ratios for plastic, ν_p , and elastic, ν_e , stress-strain. Since ν_p is about 1/2 and ν_e about 1/3, Equation (3) approximates to $\epsilon = \frac{\sigma}{3E} + 2\epsilon_d$.

Specimens of the cladding and structural materials were encapsulated in inserts. Three inserts comprise a capsule containing 15 specimens. Specimens in the insert at the center location (core elevation) are thermally bonded in argon gas and are irradiated at high temperature (600 to 800°C). Specimens in the two outer capsule locations (blanket elevations) are bonded in sodium and are irradiated at 400°C. Seven capsules comprise a hexagonal shaped EBR-II subassembly, Figure I-11. The first two subassemblies, X037 and X038, were loaded into the EBR-II in positions 7C3 and 7C5, respectively, on May 8, 1968. On June 15 at the end of Run 28 they had received 1,126 MWd of irradiation. Plans have been made to remove for testing X037 after 3,000 MWd and X038 after 18,000 MWd (maximum fluences of 10^{21} and 10^{22} n/cm² $E > 1$ MeV).

Flux monitoring wires (Fe, Ni, Ti) with Al-Co in the outer inserts were placed alongside the specimens. A few ampoules containing boron were placed

in the hollow grip ends of the specimens in the outer inserts. Temperature monitors were placed in the majority of the hollow grip ends. The temperature monitors consisted of either fusible wires or small cylinders of porous self-bonded β -phase silicon carbide ("Crusilite") which has been shown[4,5] to be a reliable temperature monitor for fast flux environments.

The sodium for bonding specimens in the outer capsule inserts was purified by cold trap and filtered at 180°C, and zirconium-gettered at 650°C. The atmosphere in which the purified sodium was loaded into the inserts contained less than 1 ppm oxygen and less than 15 ppm moisture. The oxygen content of the argon gas was less than 1 ppm.

A test enclosure has been nearly completed in which the irradiated samples can be fatigue-tested at temperatures up to 1000°C in an inert atmosphere. The pressure vessel steels are to be tested at 250 to 400°C. The austenitic stainless steel specimens for fast reactor pressure vessels are to be tested at 320 to 650°C with emphasis in the 480 to 650°C range. For cladding service the specimens are to be tested at 600 to 800°C. The tests are to be conducted at a strain-producing failure in the range of 10^3 to 10^5 cycles. The slope of the curve (stress or plastic strain versus number-of-cycles to failure) will be obtained from a limited number of tests at each set of conditions (material, fluence, and temperature) by taking three points each at each end of the strain range. The significance of this result can be shown by statistical analysis since the variance of the slope of a line is minimized when the variances of the independent variables are maximized, ie, when $(X_i - \bar{X})$ is large. One thus obtains the most precise measure of the exponent, the plastic strain range, and the fatigue life with the minimum number of samples which are sensitive indicators of the fatigue damage[6] as well as the design parameters as previously discussed.

Fig. I-9 Photograph of structural insert before assembly.

Fig. I-10 Photograph of diametral extensometer and heating coil arrangement.

Fig. I-11 Fatigue specimen EBR-II MARK B-7 subassembly.

5. REFERENCES

1. G. D. Whitman, G. C. Robinson, Jr., A. W. Savolainen (eds.), Technology of Steel Pressure Vessels for Water-Cooled Nuclear Reactors, ORNL-NSIC-21 (December 1967) p 17.
2. C. R. Brinkman and J. M. Beeston, Effects of Irradiation on the Surface Activity and Diffusivity of Hydrogen in Steels, IN-1200 (June 1968).
3. R. S. Barnes, "The Effect of Neutron Irradiation on Beryllium", Nucl. Eng., 6 (April 1961) pp 149-150.
4. R. P. Thorne, V. C. Howard, B. Hope, Radiation-Induced Changes in Porous Cubic Silicon Carbide, TRG Report 1024 (C) (1965).
5. W. H. Martin and A. M. Price, Determination of Irradiation Dose and Temperature in Graphite Irradiation Experiments Performed in the Dounreay Fast Reactor, TRG Report 1117 (C) (1966).
6. C. R. Brinkman and J. M. Beeston, Axial Fatigue of Pre- and Post-Irradiated A 302 B and A 212 B Steels, IN-1052 (January 1967).

II. REACTOR FUELS AND MATERIALS DEVELOPMENT

1. PROTOTYPE ELEMENTS

During fiscal year 1968 a number of fuel system concepts were tested as prototype fuel elements. These are discussed in the following sections.

1.1 The Development of Boron-Aluminum Foils as Controllable Neutron Absorbers in Aluminum-Clad Nuclear Reactor Fuel Elements

(G. W. Gibson, M. J. Graber, M. F. Marchbanks)

A continuing problem in the fabrication of the aluminum-clad plates for test reactor fuel elements has been to obtain a uniform dispersion of boron in the fuel plates. One solution to this problem is through the use of powder metallurgy procedures in the fabrication of the fuel plate cores^[1]. However, some of the potential fuel element fabricators lack the equipment required for the use of these procedures or feel that it is more economical to fabricate the cores by wrought metallurgical procedures. In the wrought metallurgical procedures, boron is added to the molten uranium-aluminum prior to casting. Considerable trouble is encountered in controlling the amount of boron present in alloy casting and ensuring that it is distributed uniformly throughout the finished fuel plates.

A means of controlling the boron addition while still using wrought metallurgical procedures to fabricate fuel plates would be to make the fuel cores of the plates from wrought uranium-aluminum alloy and use B₄C + Al foil made by powder metallurgy for the boron addition.

Another means of incorporating the boron would be to use B-Al alloy foils instead of the B₄C + Al foils. Sample fuel plates were irradiated which contained 0.0006 gram each of elemental boron incorporated in an aluminum foil located in the center of the core. The core was a uranium-aluminum alloy containing fully enriched uranium.

The plates were irradiated in various cycles in the L-51 loop of the MTR during Cycles 226 through 239 to the burnups given in Table II-1. Post-irradiation examination disclosed no appreciable change from the as-fabricated condition.

Eddy current instrument measurements showed no excessive oxide build-up. A blister anneal test yielded blisters at 400°C for all plates. A metallographic study of the plates was made on both as-irradiated and blistered plates.

Microstructures of the as-irradiated plates are shown in Figure II-1. The boron particles can be seen in the aluminum foil sandwiched between the core halves. Figure II-2 shows microstructures typical of the plates after blistering. The blistering

TABLE II-1

BURNUPS OF FOIL-TYPE
PLATES IRRADIATED IN THE MTR

<u>Plate Number</u>	<u>Burnup fiss/cc x 10⁻²⁰</u>
135-1	6.8
135-2	10.9
135-3	9.7
135-4	6.7

Fig. II-1 Typical area of as-irradiated plate, Composition 135.

E8-59

AS-POLISHED

(A)

50X

AS-POLISHED

(B)

200X

Fig. II-2 Typical area of blistered plate, Composition 135.

occurred within the foil itself, apparently resulting from the high concentration of helium existing there as a result of the boron-neutron reaction. No blistering in the "meat" was found.

In the development of foil-type elements it is desirable to incorporate a foil in which the helium gas retention is at least as good as the fission gas retention of the fuel or "meat". Otherwise, blister formation in the foil would cause premature core failure, as was the case here.

A comparison of the performance of these elemental boron-foil plates can be made by referring to an earlier study^[2] of prototype fuel elements containing B_4C . The blister temperature for the prototype plates was higher than that of the sample plates containing elemental boron for the same burnup. This indicates better helium gas retention for B_4C as compared to elemental boron. In addition, metallographic studies of the prototype plates showed that blistering generally occurred simultaneously within the core and foil. Thus, the use of B_4C in the place of elemental boron would be expected to prevent premature core failure.

The inspection results for fuel and poison contents on six elements are shown in Table II-2. The specified boron content of these elements is 3.8 g. Since the boron contents as determined by reactivity measurements are low, it is believed that some self-shielding has occurred and will be easily corrected by the addition of more B_4C to the foil or by the use of smaller particles of B_4C .

TABLE II-2
FUEL AND POISON LOADING OF EXPERIMENTAL ETR
FUEL ELEMENTS

<u>Fuel Element</u>	<u>Boron [a] (g)</u>	<u>U-235 [b] (g)</u>
XE-1-G	3.39	504.9
XE-2-6	3.47	507.5
XE-3-G	3.43	507.2
XE-4-G	3.44	507.7
XE-5-G	3.63	515.2
XE-6-G	3.44	516.9

[a] As determined by reactivity measurement

[b] As determined by gamma scanning

1.2 Irradiation Performance of a 500 g (U-235) ETR Fuel Element Containing Powdered UAl_x
(M. F. Marchbanks)

Fuel element E186E was from the first batch of 500 g ETR fuel elements fabricated by Atomics International to be irradiated to 6000 MWd. The fuel was in the form of powdered UAl_x . The element was operated in a high flux position in Cycle 92 of the ETR. The data and results are given in Table II-3.

TABLE II-3

BURNUP DATA FOR FUEL ELEMENT E186E FROM ETR
CYCLE 92

Initial fuel content	499.9 g
Core position	J9
Percent of total core power	2.56
Total core MWd	5949
Total fuel burnup in element	190.4 g
Total burnup in fissions/cc	7.1×10^{20}
Ratio of average power at 2 inches below midplane (hot spot plane) to overall average	1.35
Ratio of peak midplane flux to average midplane flux	1.46
Fractional fuel burnup, overall element average	0.381
hotspot in element	0.61

Post-irradiation channel measurements made in the ETR canal indicated no buckling of the plates. No channel blockage or plate warpage was found in hot cell examination of the elements. A thickness survey taken on selected plates produced readings of 0.0495 to 0.0515 inch on nominal 0.050-inch plates. A visual examination revealed no blisters or other surface flaws. An eddy current instrument survey was made to determine the oxide film buildup. The average of the measurements for each plate varied from 0.00033 to 0.00079 inch. The minimum and maximum readings were 0.00015 and 0.00146 inch. This is well below the oxide buildup reported in ex-reactor tests^[2] and is considered to be acceptable corrosion resistance performance. Gamma scans made on selected plates showed no unusual distribution of the burnup pattern.

Blister test results based on calculated burnups are plotted in Figure II-3 to compare the results with the established failure-no failure lines for sample fuel plates fueled with UAl_3 and B_4C as poison. Although this line was plotted from plates containing relatively large concentrations of fuel (40 to 50% UAl_3 as compared to 38% UAl_3 for E186E), the plot does show good blister resistance performance for fuel element E186E.

Fig. II-3 Blister temperatures of samples from fuel element E186E.

1.3 Microprobe Studies on ETR Fuel Element E018D Meltdown (W. F. Zelezny, M. J. Graber, M. F. Marchbanks)

In February 1967 several plates of ETR fuel element E018D underwent partial melting caused by the blockage of coolant channels by a piece of adhesive tape which inadvertently had been left in the reactor. An unusual and interesting feature observed in some of the melted portions of the fuel element was the tendency of the melted core to retain its shape while the molten cladding was completely removed by the coolant stream. At first glance such behavior is surprising since the core would reach a higher temperature than the cladding. Samples from the damaged fuel element were examined by microprobe during FY-68 in an attempt to explain this behavior and estimate the maximum temperature reached during the meltdown incident. Semiquantitative comparisons of particles and phases present were made with UAl_3 and UAl_4 standards. The findings and tentative conclusions regarding the unaffected and melted regions are presented.

One specimen (illustrated in Figure II-4) exhibited a globule of melted, resolidified core clinging to a portion of the parent plate which had not melted. In the unmelted core numerous large fuel particles of UAl_4 (somewhat substoichiometric in U) plus a few small particles of UAl_3 with sharp, well-defined edges were observed. It is possible that the starting fuel material for the manufacture of this fuel was UAl_3 powder which subsequently reacted with the aluminum matrix during manufacture and/or service to form UAl_4 , although no partially reacted UAl_3 particles were observed and the UAl_3 particles which were found were small and still retained sharp boundaries. It appears probable, therefore, that the original fuel was high in UAl_4 content.

Three melted samples were examined which gave indications of similar temperature but differing time at temperature histories.

E-77

A. Lower portion of photomicrograph shows apparently unaffected region of fuel plate with core and cladding intact. Large, rounded fuel particles are UAl_4 . Smaller, angular particles are UAl_3 .

Upper portion of photomicrograph shows portion of core still clinging to parent fuel plate after melting, flowing, and resolidifying. The molten cladding has been stripped away by the stream of coolant water (50X)

B. Enlarged view of melted core. Dark circles are voids, probably due to fission gas bubbles. Small, most distinct angular particles are UAl_3 (200X).

Fig. II-4 Melted and unmelted regions from the same fuel plate.

In the specimen illustrated in Figure II-5 the cladding was lost, but the core remained relatively intact. The core showed globules of pure aluminum surrounded by a matrix of fuel-containing material much lower than UAl_4 in uranium, indicating that both core and cladding had melted. The fluid wrought alloy cladding was easily swept away by the coolant stream. The core, on the other hand, was a powder metallurgy product made from UAl_x and Al powders. The oxide coating which is invariably present on the aluminum powder used as starting material has been found by other work to produce compacts which do not flow readily on melting. This is taken to be the explanation for the retention of shape by the core in the molten state. The fact that there were still globular-shaped particles of pure aluminum present indicates that the time in the molten state was so short as to permit only a limited interdiffusion between the aluminum matrix and the fuel particles. The Al_2O_3 coating on the aluminum powder undoubtedly acted as a diffusion barrier to retard this process. Some voids (probably due to fission gases) were present.

In the globule of core clinging to the parent plate in Figure II-4 a portion of the core had lost its cladding and become more fluid than in the case just described. A few distinct and relatively small UAl_3 particles were present in a uranium-containing matrix which was not characterized because its spatial variations in concentration were below the limit of resolution of the microprobe. This was interpreted as representing a melt of U-Al which had reached a temperature in the 730 to 1350°C region, followed by cooling at a sufficiently low rate to allow some crystals of primary UAl_3 to form. (If the temperature had been above 1350°C, primary crystals of UAl_2 would have been formed in addition, while if the maximum temperature had been between 640 and 730°C only primary crystals of UAl_4 would have formed.) Fission gas bubbles were present.

Another sample of core material which had undergone melting (Figure II-6) was similar to the one just described except that the crystals of UAl_3 were larger and more numerous. The conclusions are similar. This portion of the core likewise reached a temperature in the range of 730 to 1350°C, but was cooled more slowly, resulting in the formation of more and larger crystals of primary UAl_3 . Fission gas bubbles were present.

From the above it is tentatively concluded that:

- (1) The sections of fuel core examined reached a maximum temperature in the range of 730 to 1350°C.
- (2) The core produced by powder metallurgy of UAl_x and Al powders was rendered viscous in the molten state by the presence of Al_2O_3 around the Al particles.
- (3) The high viscosity of the molten core did not allow the loss of appreciable quantities of fission products and kept the core relatively intact while the more fluid molten cladding material was swept away by the coolant flow.

E-71

A

B

A. Melted, resolidified core (50X).

B. Same region at higher magnification. Light, rounded particles are aluminum, free of uranium. Gray intergranular material contains about 20 wt% uranium (200X).

Fig. II-5 Remains of melted core which has undergone some alteration.

E-76

A

B

A. Detached globule of melted fuel core. Molten cladding has been swept away by water stream (50X).

B. Enlarged view of above, showing approximately square particles of UAl_3 (200X).

Fig. II-6 Remains of melted core which has been markedly altered.

2. IRRADIATION TESTING

2.1 Sample Irradiation

2.11 PAED G-12 Irradiation Facility and Program

(V. A. Walker, E. H. Porter, G. W. Gibson, M. Hatch)

During FY 1968, the PAED-59 G-12 irradiation facility in the Engineering Test Reactor (ETR) was operated for six ETR cycles (Cycles 90, 91, 92, 93, 94, and 95) by the Reactor Engineering Branch for the irradiation testing of reactor fuels and materials. The PAED G-12 loop is a high flux pressurized water loop located in the SW corner (G-12 position) of the ETR core. The loop is presently being operated under a "feed-and-bleed" mode of operation, which permits higher temperature operation. The bulk coolant, entering from the ETR primary coolant stream at the top of the loop, is pressurized and pumped through the sample assembly, the coolant being heated by the sample fuel plates. After passing through the sample train, the coolant flow is split, and the major fraction is recirculated through the loop. By varying the amount of bypass flow recirculated higher bulk coolant temperatures can be achieved. During ETR Cycles 92, 93, 94, and 95, the G-12 loop was operated under this "feed-and-bleed" system. During ETR Cycles 90 and 91, problems with one of the pressurizing pumps necessitated reverting to a "once-through" mode of operation. Under this system, a bypass stream of ETR coolant is pressurized, pumped through the sample train, and returned to the ETR primary coolant stream below the reactor core. Bulk coolant inlet temperature under this mode is limited to that of the ETR primary coolant -- 44°C. Loop operating parameters for Cycles 90, 91, 92, 93, 94, and 96 are shown in Table II-4.

TABLE II-4

G-12 LOOP OPERATING PARAMETERS

<u>ETR Cycle</u>	<u>MWd</u>	<u>Mode of Operation</u>	<u>Flow (gpm)</u>	<u>Inlet Pressure (psi)</u>	<u>Inlet Temperature (°C)</u>
90	3626	Once-through	90	440	44
91	4870	Once-through	90	440	44
92	5949	Feed-and-Bleed	90	760	116
93	5966	Feed-and-Bleed	96	760	77
94	5310	Feed-and-Bleed	97	760	77
96	3297 ^[a]	Feed-and-Bleed	97	760	71

[a] To June 30, 1968

The G-12 sample assembly contains space for 24 sample plates 1.25 by 5.75 inches, situated in six tiers of four plates each. Each tier provides for a 0.150-inch-thick fueled instrumented thermocouple plate and three 0.050-inch-thick sample fuel plates, separated by 0.100-inch-wide flow channels.

The facility was being utilized by Idaho Nuclear Corporation for the Reactor Fuel Materials Program and the Advanced Test Reactor (ATR) Fuels Program and by the Oak Ridge National Laboratory for irradiation testing in support of the High Flux Isotope Reactor (HFIR). Table II-5 gives the data on the samples which were irradiated in the loop during the year.

2.12 Capsule Irradiations

(M. J. Graber, F. O. Haroldsen, V. A. Walker, R. J. Pollard)

As the development of improved reactor fuel systems has proceeded, it has become increasingly important to know the irradiation behavior of the fuel compounds being used in the fuel systems. To extend this technology, a program of fabrication and testing of fuel in bulk form was begun^[2]. Physical, mechanical, and irradiation testing was planned for various compositions, including the intermetallic compounds in the uranium-aluminum system.

One of the irradiation effects to be studied is the mechanism of sintering and resulting effects on fuel-particle stability. The effect of fission gases is also being studied. Since the fission gases are trapped at low temperatures, growth is expected to be produced by the solid fission products. Gas release in capsules will be determined as a function of total fluence and temperature.

Twenty-one capsules were fabricated and inserted into the MTR and ETR during the year. Irradiations have been completed on eight, five of which have undergone partial post-irradiation examination. The remainder of these examinations, which included rapid gas release temperature thresholds, were suspended in January 1968 for lack of funds.

Bulk UAl_3 pellets which were irradiation-tested at a heat generation of 23 kW/linear foot proved to have excellent fission gas retention. Powdered samples which had poorer thermal conductivity and, therefore, elevated center-line temperatures showed a 30% gas release at the same heat generation. UAl_4 pellets reached temperatures resulting in incipient fusion in the center.

The effects of heat on UAl_3 are being studied to show possible conditions of grain growth and recrystallization.

TABLE II-5

ETR G-12 LOOP IRRADIATION SCHEDULE FOR FY 1968

Experiment Number	Description	Sample Composition Numbers	ETR Cycles Tested	Number of Samples Tested
68-1	This test is an investigation into the effect of the use of 100% UAl_x fines (-325 mesh) on fuel plate stability. The use of fine UAl_3 powder in fuel plates is a potential source of economic savings.	1,6,7,11, 12	90	10

TABLE II-5 (Contd.)

ETR G-12 LOOP IRRADIATION SCHEDULE FOR FY 1968

<u>Experiment Number</u>	<u>Description</u>	<u>Sample Compositions Numbers</u>	<u>ETR Cycles Tested</u>	<u>Number of Samples Tested</u>
-2	The purpose of this test was to determine the effect on fuel stability of plate core porosity introduced by cold working. Controlled porosity in the cores of fuel plates is a means of reducing the swelling rates and of extending the fuel element life. This parameter should be included in the specifications of dispersion type fuel elements.	1,16,32, 46,52	90,91A, 92,93A, 93B,93E	24
-3	This test is the evaluation of Kerr-McGee U ₃ O ₈ . This material is being used as a fuel in 200 ATR elements. Testing is being done in order to establish its irradiation stability so that a comparison can be made with other types of oxide. This experiment is a continuation of work started in previous years.	2	90,91A, 93B	6
-4	This test is to evaluate the effect on fuel plate irradiation stability of the use of 55 wt% UAl alloy powder in making fuel plate cores. Some fuel fabricators have indicated that the cost of the fuel powder would be lower if the uranium content was lowered.	47	93A,93B, 93E,94, 96	6
-5	These samples contain the maximum fuel and boron loadings required by the ATR "7F" type elements.	23	96	2

TABLE II-5 (Contd.)

ETR G-12 LOOP IRRADIATION SCHEDULE FOR FY 1968

Experiment Number	Description	Sample Compositions Numbers	ETR Cycles Tested	Number of Samples Tested
	This test was started in FY 67 and is being run to ensure that the "7F" elements will be stable under the ATR operating conditions.			
-6	U-Si-Al alloys appear to offer economic advantages over powder metallurgy type fuel plate cores. This test is being run in order to provide information on the stability of these alloys under high flux conditions to long burnups.	48	92,93A, 5 93E,94, 96	
-7	A proposed method of controlling the boron addition is to tag it with Co-60. The B-Co would be added to the fuel plate cores in the form of a glass. This test is to investigate the irradiation stability of the glass.	51	96B ^[c]	5
-8 ^[b]	This test is being run to investigate the effect of void volume on the irradiation stability of dispersion type fuelplates. plates.	3,24,56, 57	93A,94, 11 96	
-9 ^[b]	This test is an investigation into the possible use of lower cost U ₃ O ₈ in HFIR fuel elements.	3,56	93A	7
-10 ^[b]	This test is being run as a comparison between UAl _x powders made by arc casting and solid state reaction.	24,57	93A	4

TABLE II-5 (Contd.)

ETR G-12 LOOP IRRADIATION SCHEDULE FOR FY 1968

Experiment Number	Description	Sample Compositions Numbers	ETR Cycles Tested	Number of Samples Tested
-11	The irradiation stability of UAl_x in stainless steel, zircaloy-2, Zr + 2% Cr alloy cladding materials is being studied under high flux conditions as these materials have long-range potential use in high flux thermal reaction.	30,45, 58	91A,92, 93A,93B, 93E,94	12
-12	The corrosion resistance of ferrous alloys is being studied under the operating conditions of thermal test reactors as these materials have attractive mechanical properties.	16,30,32 46,52	90,91A 92,93A, 93B,93E, 94	12
-13	The corrosion resistance of zirconium alloys is being studied under the operating conditions of thermal test reactors as these materials have attractive nuclear and mechanical properties.	45,50	92,93B, 93E,94	8
-14	The corrosion resistance of 2219 Al alloy is being studied under the operating conditions of high flux test reactors as this material has excellent elevated strength properties.	19	90,91A, 92	6
-15	The corrosion resistance of 1100 Al alloy is being studied under ATR conditions since this material is used in the ETR and MTR.	37	91A,92, 93B	5

TABLE II-5 (Contd.)

ETR G-12 LOOP IRRADIATION SCHEDULE FOR FY 1968

Experiment Number	Description	Sample Compositions Numbers	ETR Cycles Tested	Number of Samples Tested
-16	The use of fiber reinforced aluminum alloys as fuel plate cladding materials is being studied since these materials have attractive nuclear and mechanical properties.	53,60	96	5

[a] Some samples are used for more than one experiment.

[b] ORNL experiment.

[c] Tentative only.

2.2 Irradiation Effects on Fuels

As a result of the irradiation testing and post-irradiations which have been conducted in the past, it was possible during the year to make an assessment of the effects on a number of fuel concepts.

2.12 Voids for Fission Product Accommodation (M. J. Graber)

At selected irradiation conditions, the incorporation of voids in thermal reactor fuels results in reduced growth or swelling rates. When dense fuel materials containing U-235 are irradiated at low temperatures, their growth rates agree with the calculated rate based on the fission product yields considered in the solid form. This is true for the oxide, uranium metal, and uranium-aluminum alloy which show a growth rate of 1.7, 3, and 0.09%, respectively^[3,4,5], per atom percent burnup. When these values are normalized by putting them in terms of fissions/cc, they agree with the calculated value which amounts to a $\frac{\Delta V}{V}$ of 3.04 for the U-235 fissioned.

These normalized values for dense fuels have been compared to values obtained for porous fuels irradiated at selected conditions^[5]. Porous bulk UO₂, if irradiated at temperatures sufficiently elevated to allow plastic behavior but below the rapid gas release temperature, 300 to 700°C, exhibits a low initial growth rate about 0.16% for each 1×10^{20} fission/cc^[3]. At some point when the porosity is filled, the growth rate expected for dense fuel is resumed. Likewise, powder metallurgy UAl₃ dispersed in aluminum which retains a certain amount of porosity in the finished plate exhibits this delayed swelling effect when irradiated at temperatures between 150 to 280°C.

Some of the parameters to be considered in the utilization of incorporated voids are (a) the crush strength of the fuel particles, (b) the plastic behavior of the fuel as influenced by pressure, temperature, irradiation, and sintering aids, (c) particle size, and (d) quantity of fission gas released.

As shown in Figure II-7, the growth rate of 93 to 96% dense, 50 wt% UAl_3 dispersed in aluminum decreases with an increase in irradiation temperature. At the higher test temperatures, the finer particle size dispersions actually densify during irradiation. This is attributed to a sintering effect resulting from the diffusion of the aluminum matrix into the fragmented fuel to react partially with the fission products and partially to satisfy the $UAl_3 + Al = UAl_4$ reaction. The stability of this fuel is controlled by void content, irradiation temperature, and particle size.

Fig. II-7 The effect of irradiation temperature and fuel particle size on the growth rate of 93 to 96% dense, 50 wt% UAl_3 -Al powder metallurgy dispersion fuel cores.

2.22 Sample Plates with Graded Fuel (M. F. Marchbanks)

One possible means of reducing the flux peaking in ATR fuel elements would be to vary the fuel loadings in the plates azimuthally. This could be accomplished by the use of strips of core materials with variable fuel and poison loadings or by use of lens-shaped fuel cores with reduced thickness at the core edges. A number of sample plates using these fabrication techniques were irradiated in the G-12 loop of the ETR. These plates have been examined in the hot cells.

No sign of warping was found in the visual inspection of the plates with the lens-shaped cores. Dimensioning of the plates showed no appreciable change due to irradiation. Eddy current instrument readings taken across the width of the plates showed that oxide buildup over the thicker areas of the lens-shaped cores was appreciably greater than at the edges. The heavier oxide buildup over the central portion of the core is attributed to the higher heat flux in this area. The oxide thickness was found by metallography to vary from 0.00034 inch over the edge of the core to 0.00096 inch over the center area. Photomicrographs of the lens-shaped fuel cores are reproduced in Figures II-8 through -11. The irradiation swelling behavior of one of the plates is plotted in Figure II-12 as a comparison with the swelling of conventional cores fueled with UAl_x and clad with aluminum. Slightly greater swelling was found in this sample than has been previously exhibited in the conventional cores.

Blister test results are plotted in Figure II-13 to show the relationship to the previously established failure-no-failure line for UAl_x and B_4C in an aluminum matrix. The blister temperature was slightly below the established line, but well above the presently planned operating temperature of ATR fuel elements. As Figure II-14 shows, blisters first formed over the central area of the plate rather than around the periphery of the core as is common in conventional core shapes. Blister metallography (Figure II-15) showed that blisters formed both within the core and at the core-cladding interface.

Plates containing cores with variable fuel loadings were made with a high U-235 content (45 wt%) and low B_4C content (0.3 wt%) on one-half of the core and a low U-235 content (35 wt%) and high B_4C content (0.58 wt%) in the other half. Three of these plates were examined in the Hot Cells.

No sign of warping was found in the visual inspection, and dimensioning of the plates showed no appreciable change due to irradiation. The dermitron measurements showed the oxide buildup to be far below what was expected. The side containing the high amount of U-235 showed a corrosion film about 25% thicker than the low U-235 side. This was attributed to the higher surface temperature that would be produced on the high U-235 side of the plate.

Photomicrographs of typical areas of the as-irradiated fuel plates showed no microstructured anomalies resulting from the split-core construction. Figure II-16 shows a typical separation of the high and low concentrations of U-235 at the center line of the plates. A typical area of the core is shown in Figure II-17 at high magnification.

The irradiation swelling behavior of the plates is summarized in Table II-6. Although the swelling in each half of the plates would be expected to be different because of the variation in loading, present experimental techniques can only determine the average swelling of the whole core. These average swelling values, plotted in Figure II-12, are greater than that of conventional cores irradiated to equivalent burnups.

Two of the split-core samples were blister-tested. The results are listed in Table II-6. Special attention was given to determine if the side with the largest fuel loading would blister first. This was the case in sample P-31-1217. In sample P-31-1215, both sides blistered at the same temperature, but as Figure II-18 shows, the largest blisters formed on the high burnup side.

AS-POLISHED

50X

Fig. II-8 Picture-frame area of lens-shaped core.

AS-POLISHED

50X

Fig. II-9 Portion of lens-shaped core.

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Fig. II-10 Central area of lens-shaped core.

50X

Fig. II-11 Photomicrograph showing a typical core area from a lens-shaped core.

Fig. II-12 Core swelling of lens-shaped and split core compared to previously established swelling behavior of conventional cores.

Fig. II-13 A comparison of blister temperatures of Compositions 31 and 33 with failure - no failure line for $UAl_3 + B_4C$ dispersed in aluminum.

Fig. II-14 Photomicrograph of blistered plate showing initial blister formation over the central portion of the lens-shaped core.

AS-POLISHED

50X

Fig. II-15 Photomicrograph of typical blister in lens-shaped core.

AS-POLISHED

50X

Fig. II-16 Separation of high and low concentrations of U-235 in a split core sample fuel plate.

AS-POLISHED

500X

Fig. II-17 Typical area of fuel in a split core sample fuel plate.

TABLE II-6

POST-IRRADIATION DATA FOR SPLIT-CORE PLATES

Plate Number	Side	Burnup (%) ^[a]			Burnup ^[a] (fiss/cc x 10 ⁻²⁰)			Total Average BU of Both Sides (fiss/cc x 10 ⁻²⁰)	Irradiation Core Swelling ^[b] (% ΔV/V)	Blister Temperature (°C)
		High	Medium	Low	High	Medium	Low			
P-31-1214	1	43.0	--	20.3	10.3	--	4.9	6.3	2.1	---
P-31-1214	2	43.4	32.4	20.7	7.4	5.5	3.5			
P-31-1215	1		43.5			10.3		9.0	2.6	454
P-31-1215	2		45.7			7.8				454
P-31-1217	1	43.2	37.0	18.4	10.2	8.8	4.4	6.8		454
P-31-1217	2	43.2	36.6	19.0	7.5	6.4	3.3			482

[a] The high, medium, and low values of burnup are given for plates P-31-1218 and P-31-1217 because they were irradiated in tiers which have a high flux gradient from one end of the plate to the other. Plate P-31-1215 was in a tier with relatively uniform flux distribution; hence, the average for the plate is given.

[b] Average for the whole plate.

Fig. II-18 Macrograph showing initial blister formation on split-core plate number P-31-1215.

The blisters first appeared on the high burnup end of plate P-31-1217, which was exposed to a high neutron gradient. This effect is shown in Figure II-19. The results of the blister tests for these plates are plotted in Figure II-13 to show performance in comparison with conventional plates. The temperature of initial blister formation and the high burnup value was used for each side of plate P-31-1217. For plate P-31-1215, the average burnup value of the whole plate was used. Although the blister temperatures were all slightly below that established for conventional cores, they were well above the planned operating temperature for ATR fuel plate surfaces.

Blister metallography revealed the usual appearance of blistering both within the core and at the core-clad interface. A typical blister area is shown in Figure II-20.

Fig. II-19 Macrograph showing initial blister formation on split-core plate number P-31-1217.

Fig. II-20 Microstructure of typical blister area from a split-core plate which was blister annealed.

2.23 Failure Analysis of Sample Fuel Plate Manufactured Using Large Cold Rolling Reductions
(M. F. Marchbanks)

This plate is from a series of plates of various compositions being tested to determine the effect of plate core porosity on fuel stability. Controlled porosity in the cores of fuel plates is a means of reducing the swelling rates, thereby extending the fuel element life. Studies are being made with a view to including a porosity requirement in the specifications of dispersion-type fuel elements.

Sample fuel plate I-1-1421 was one of five plates of a typical ATR fuel plate composition manufactured by a special rolling procedure to introduce increased core porosity. The increased core porosity was evident in the as-irradiated microstructure as shown in Figure II-21. These plates were hot rolled using 20% reductions per pass to a plate thickness of 0.100 inch. They were then cold rolled in successive passes with reductions as follows: 15, 15, 20, 10, and one pass at approximately 5% to core length. This operation yielded a cladding with the physical properties listed in Table II-7. The hardness value is somewhat higher than that of plates fabricated by the usual procedure of hot rolling. The elongation of 3.5% is appreciably below the approximately 15% produced by hot rolling procedure. The materials used in this plate were as follows:

Core: 48.597 wt% UAl_3 (72.50 wt% U, 93.13% enriched), -100 + 325 mesh; 0.19 wt% B_4C ; X8001 aluminum. Cladding and picture frame: 6061 aluminum, alclad with 1100 aluminum.

Fig. II-21 As-irradiated microstructure of sample fuel plate I-1-1421.

TABLE II-7

AS-FABRICATED PHYSICAL PROPERTIES OF THE CLADDING
OF PLATE I-1-1421

<u>Yield Strength (psi)</u>	<u>Ultimate Tensile Strength (psi)</u>	<u>Percent Elongation</u>	<u>Hardness Superficial 15T</u>
30,200	32,700	3.5	68.4 (64 BHN)

This plate was irradiated in ETR Cycle 93A in the G-12 loop. It was placed in tier 4, the tier with the highest neutron density. After 567 MWd of operation the reactor was scrammed, and examination revealed a blister in the plate. A fission break in the form of a split in the top of the plate was also found. The maximum plate surface temperature during the cycle was estimated to be 175°C.

Failure analysis macrophotographs (Figures II-22 and -23) show that the blister caused separation at the end of the plate. Thickness measurements indicated that the plate had not undergone overall swelling, but that the swelling was limited to the blister area.

Eddy current instrument measurements on the as-irradiated plates indicated that the amount of oxide buildup was within the acceptable limits for the time and temperature of operation. The average oxide buildup was approximately 0.0003 inch. A gamma scan showed the usual uniform burnup for tier 4.

A composite photomicrograph (Figure II-24) shows that the blister propagated along the clad-core interface, indicating a poor bond in this area.

A blister anneal test produced a blister (Figure II-25) at 454°C on the end of the plate opposite the fission break. A metallographic composite reproduced in Figure II-26 shows that this blister also formed at the clad-core interface.

The cold rolling steps in the manufacture of the plate apparently were the cause of the poor bonding between the core and cladding.

Fig. II-22 Photomicrograph of plate I-1-1421 that blistered in the ETR.

Fig. II-23 Photomicrograph of the top end of plate I-1-1421 showing the fission break.

Fig. II-24 Photomicrograph of the blister of sample fuel plate I-1-1421.

Fig. II-25 Blister in annealed plate I-1-1421.

AS-POLISHED

50X

Fig. II-26 Metallographic composite of anneal blister in plate I-1-1421.

3. METALLURGICAL DEVELOPMENT

During the year a number of development projects of a metallurgical nature were undertaken by the branch. Some of these are discussed in this section of the report.

3.1 Assay of Boron in Nuclear Reactor Fuel Elements

(B. G. Carlson, G. W. Gibson)

Beginning with the first fuel elements fabricated for the ETR, considerable difficulty has been encountered by all fabricators in controlling the amount of boron included in the elements. Consequently, it has been necessary to assay the elements for boron in order to maintain reactivity control of ETR core loadings. All boron assaying has been done in the ETR Critical Facility (ETRC), a low power, swimming-pool-type reactor, which is a full-scale nuclear duplicate of the core and reflector of the ETR.

Assuming that the U-235 content is the same in all elements, the boron content of an element can be determined from its reactivity worth relative to elements with known boron contents. The relative reactivity worths are determined from the position of a shim rod which is manually adjusted to make the reactor critical. The calibration is accomplished by plotting a curve of shim position versus fuel element boron content from data obtained in measurements on fuel element standards containing known amounts of boron. These standards consist of unborated elements with boron-impregnated polyethylene tape distributed uniformly in their coolant channels. Because the time required to make a measurement is long and the cost relatively high, other means of assaying for boron are being investigated.

A feasibility study was made by members of the Nuclear Technology Branch for the scanning of ETR or ATR fuel elements containing Co-60-tagged boron. Co-60 would seem to be an "ideal" tagging radionuclide because it emits a pair of gamma rays whose energies are well above the energies of gamma rays emitted from U-235, because it has a long half-life, and because it can be obtained at reasonable cost.

A Co-60-tagged string containing 30 μ Ci (about 0.5 mg) was run down the middle of an ETR fuel assembly. A 3 x 3 inch NaI(Tl) detector was installed along a vertical axis in a fuel scanning system at the MTR, and the Co-60 was scanned simultaneously with the U-235 scan so that the interrelationship could be studied. This Co-60 resulted in a total count of 130,000 in a standard three-minute scan using an aperture which scanned a 3 x 1 inch area of the fuel assembly. Approximately 8000 of these counts were from background (this could be reduced with better shielding) and 600 were from the U-235 (a negligible amount). This would yield a statistical uncertainty in the determination of about 0.3%. The 30 μ Ci of Co-60 contributed 15% to the counting rate in the U-235 scanning counters (total count in three-minute scan: $\approx$ 2,000,000 counts). Thus, a correction to this number would have to be made. The correction would be based upon the number resulting from the Co-60 scan. Such a correction would be quite reliable^[6].

Compounds that would most likely be useful for "tagging" would be borides, carbides, nitrides, or oxides. Previous testing has shown that oxides and carbides would probably be stable under irradiation. More testing would be required on the borides and nitrides than on the other two. The oxides are most easily produced and the first effort was devoted to these. Sixteen different glass

compositions ranging from 13 to 77% B_2O_3 were tried. The balance of the melts consisted of Na_2O , Al_2O_3 , MgO , and SiO_2 in varying proportions to adjust the melting point so that it would be molten at 1000 to 1050°C and yet be solid at the rolling temperature of the fuel plate, 500°C. A glass which appeared to best meet these requirements, and yet had a high B_2O_3 content, was the following composition:

SiO_2	32%
B_2O_3	52%
Al_2O_3	6.8%
Na_2O	9.1%
Co_3O_4	0.0006% - 2%

Reducing the poured glass to a usable powder was readily accomplished with a Spex Mixer Mill. This utilizes a steel vessel and ball which is given a sharp twist and stop action to pulverize the material contained in the vessel. Screening followed this operation with -100 +200 mesh material selected for use in the experimental plate.

To ensure against a change in rolling properties due to the glass in the core, a much larger than normal loading was used (4%). The remainder of the core and cladding was X8001 aluminum. Hot rolling the assembly at 500°C produced a well-bonded, sound plate. A section was polished to check dispersion and compatibility of the glass in aluminum. The glass was uniformly distributed within the core and had not reacted with the aluminum. The remainder of the plate was returned to the furnace and heated 16 hours at 500°C to see if additional time would cause reaction of the glass with the aluminum. A section from this sample was polished and compared with the first. No change was visible (Figure II-27) so it is assumed the two are compatible at this temperature.

Sample fuel plates were next made for gamma scanning, reactivity measurements, and irradiation testing. These were of the type used in the L-51 position of the MTR core and the G-12 pressurized water loop of the ETR. The geometry and fabrication procedures of these plates have been previously described^[1].

The L-51 type plates were made with four compositions in the cores with fuel constant at a level comparable to the 500 gram U-235 loading in an ETR element. Three levels of boron content were present in these plates with the highest equivalent to 5 gram/ETR element and one free of poison for control purposes.

The G-12 type plates were made with ATR-equivalent fuel and boron loadings. The boron compound in these contained nonradioactive cobalt. These samples are scheduled for irradiation testing in ETR Cycle 96B.

Ultrasonic scanning and radiography did not reveal any fuel or glass clumping, but the density of the glass is too close to that of aluminum to make either of these techniques very effective.

AS-POLISHED

(A)

500X

AS-POLISHED

(B)

500X

Fig. II-27 Glass particles in fuel plate observed by dark field illumination.

Addition of a radioactive glass to the fuelplate core mixture did not present any complications in fabrication nor was there a large increase in activity from the finished plates which contained the cobalt versus the control plates without cobalt. It is believed that the small increase in activity detected in the most heavily loaded plates was not enough to interfere with the operation of a well run fuel fabrication shop.

The gamma scans and reactivity measurements on the samples are scheduled to be made in the first quarter of FY-69. The irradiation tests will be made during the first and second quarters of the year. Additional efforts will be devoted to the development of the information required for the preparation of detailed product specifications on the glass.

3.2 Metallurgical Effects of Silicon Additions to Uranium-Aluminum Alloys (W. F. Zelezny, G. W. Gibson, A. A. Angilella)

Wrought Al-Si-U alloys with from 22 to 32 wt% uranium containing varying percentages of silicon were examined by microprobe to determine the distribution of uranium and silicon. The results of this examination are shown in Table II-8. When the silicon-to-uranium atom ratio is below one, the silicon is found associated with the uranium in the intermetallic particles. When the atom ratio is greater than one, the fuel particles are saturated with silicon and the excess silicon appears as uranium-free segregations throughout the matrix of the alloy. Figure II-28 shows two-dimensional electron beam microprobe scans of two uranium-silicon-aluminum alloys. In the top of the figure are shown the scans for uranium and silicon with a silicon-to-uranium atom

TABLE II-8

RESULTS OF ELECTRON BEAM MICROPROBE ANALYSIS

Heat Number	% U	% Si	Si/U atom ratio	Results
4	31.3	0	0	Silicon not detected, either in fuel or matrix
7	31.5	1.38	0.37	Silicon associated only with fuel particles; no silicon detected in matrix
9e	31.0	2.84	0.85	Silicon associated only with fuel particles; no silicon detected in matrix
2	23.1	3.24	1.19	Only a slight tendency for the silicon to be found independent of fuel particles
3	22.4	4.89	1.85	Silicon associated with all fuel particles; also numerous segregations of silicon (free from uranium) throughout matrix

Fig. II-28 Two-dimensional microprobe scans of U-Al alloys with silicon additions.

ratio of 0.85 (heat 9e). The scans were made from the same area of a metallographic specimen. All of the silicon is associated with the uranium-bearing intermetallic compound particles at this atom ratio. At the bottom of the figure are scans from a specimen taken with a silicon-to-uranium atom ratio of 1.85. In this sample, silicon is likewise found in the fuel particles, and, in addition, there are particles of silicon free of uranium and distributed throughout the matrix.

In order to study the effect of silicon additions on the mechanical properties of the alloys, one-inch gage length tensile specimens, heated in air for one hour at 500°C and air-cooled, were prepared from the rolled alloy sheets. Tensile testing was then performed on an Instron-type TT-D testing machine at room temperature, 150°C (the estimated operating temperature of test reactor fuel plates) and 550°C (the fuel-plate hot-rolling temperature).

The results of the testing on the uranium-containing alloys are plotted in Figure II-29 and -30. Examination of the plots reveals that, as expected,

Fig. II-29 Effect of silicon additions on the mechanical properties of 32 wt% U-Al alloys.

Fig. II-30 Effect of silicon additions on the mechanical properties of 22 wt% U-Al alloys.

the addition of silicon lowers the tensile strength of both 22 and 32 wt% uranium-aluminum alloys at all three test temperatures with the minimum strength being achieved with a 3 wt% addition of silicon. Work at ORNL[7] also indicated that a 3 wt% silicon addition was the desirable amount for a 48 wt% uranium-aluminum alloy. Inasmuch as the effect of the silicon is to stabilize the UAl_3 phase in the alloy, it would be expected that the optimum amount of silicon required should be directly related to the uranium content.

In order to investigate the relationship of the silicon-uranium ratio in the alloy to the amount of softening produced in the alloy, the data in Figure II-31 were developed. Here the silicon-to-uranium atom ratios in the alloys are plotted against the following ratios:

$$\frac{UTS_o - UTS_{si}}{UTS_o}$$

$$\frac{YS_o - YS_{si}}{YS_o}$$

where

UTS_o = ultimate tensile strength without silicon

UTS_{si} = ultimate tensile strength with silicon

YS_o = yield strength without silicon

YS_{si} = yield strength with silicon.

As seen here, the maximum amount of softening is produced when the silicon-to-uranium atom ratio is in the range of 0.6 to 1.1. From the electron microprobe studies, this would be the expected result. These studies are capable

Fig. II-31 The softening effect of silicon addition on the room temperature strength of 22 to 32 wt% U-Al alloys.

of producing much more definitive results than tensile testing and indicate that the uranium-aluminide particles become saturated with silicon when the silicon-to-uranium atom ratios are in the range of 0.8 to 1.0. Up to the point of saturation of the intermetallic particles, it would be expected that increasing the amount of silicon would decrease the strength of the uranium-aluminum alloy since this would result in a smaller volume percent of uranium aluminide particles. However, once the fuel particles are saturated with silicon, the free element appears in the matrix of the alloy where it tends to increase the strength.

The use of high silicon-to-uranium atom ratios (1.85 and 1.16) did have an appreciable effect in improving the ductility (as measured by percent elongation) of the alloys at 550°C. This improved ductility was not completely reflected in the results obtained in hot rolling the alloy slab from heat 3 with a silicon-to-uranium atom ratio of 1.85. This rolling was done at lower temperature than the tensile testing. Not only was the preheating temperature 50°C lower for rolling than the tensile temperature, there is also a temperature drop from the time the alloy slab leaves the furnace until it enters the rolls.

3.3 Filament-Reinforced Aluminum Claddings (G. W. Gibson, J. K. Crandall)

An investigation was begun on the use of filament reinforced cladding materials. Shipments were received from the Harvey Aluminum Company in which 6061 and 2219 aluminum alloys were reinforced with NS 355 stainless steel wire of two diameters (0.004 and 0.006 inch). These wires have minimum tensile strengths of 400,000 psi. The hot pressing procedures used by Harvey have previously been described^[8]. The combination of stainless steel filaments in aluminum matrix is, of course, not the most ideal for use in test reactor fuel plates since stainless steel has a relatively low modulus of elasticity and a high nuclear cross section. Silicon carbide, graphite, or beryllium filaments would be better than stainless steel, but at present these filaments are considerably more expensive.

Sample fuel plates were fabricated by means of standard picture frame techniques in which these special alloys were used as the claddings. In order to prevent the breaking of the steel filaments, it is necessary that these run perpendicular to the rolling direction. Thus, the mechanical properties of the plate are improved only in the transverse direction.

In Figure II-32 is shown a longitudinal cross section of a sample fuel plate made with stainless steel filament reinforced 2219 aluminum alloy. An eight-to-one reduction ratio was used in the production of this plate with the result that the volume percent of wire is low. This aspect, however, can be improved by the use of lower rolling reduction ratios, additional layers of wire, and possibly larger diameter wires.

It is interesting to note that the wire has had an influence on the fuel plate core shape. Instead of the usual core thickening at the end of the core, a small notch has appeared in the core.

Corrosion testing in a stirred autoclave at 240°C and 475 psi for three weeks using deionized water gave very low weight gains (3 mdd). The 2219 aluminum alloy samples were found to have slightly better corrosion resistance than the 6061 aluminum alloy. No change of sample dimensions was noted. No pitting of the samples was noted. However, the interface between the wire and the 6061

NaOH ETCH

50X

Fig. II-32 Cross section of fuel plate clad with stainless steel wire reinforced 6061 aluminum alloy.

aluminum was slightly accentuated. These results would be expected since there are many stainless steel to aluminum alloy interfaces in a test reactor core.

The wires did not interfere with the ultrasonic or radiographic inspection of the sample fuel plates.

3.4 Use of 2219 Aluminum Alloy as a Cladding Material (W. F. Zelezny, G. W. Gibson)

In high flux aqueous reactor systems, the use of aluminum cladding materials is limited by the buildup of an insulating oxide corrosion layer^[9]. This oxide increases the fuel-plate cladding material temperatures to such an extent as to seriously weaken the plates. One approach to alleviating this problem would be to use an aluminum alloy with better elevated temperature mechanical properties than the alloys normally used as cladding materials on fuel elements in these reactors. Alloy 2219 is an aluminum-copper alloy which was originally developed for elevated temperature applications. In the fully heat-treated condition the typical mechanical properties of the alloy in the temperature range of 250 to 300°C are superior to those of any other commercially available aluminum alloy. In order to assess the possibility of using the alloy as cladding material on the fuel plates of high-power density reactors, such as the ETR and ATR, a program was instigated to study the corrosion behavior under dynamic conditions.

Preliminary microprobe studies have been made of the corrosion films produced on 2219 aluminum alloy cladding which has been exposed to high velocity (45 to 50 ft/sec) flowing pure water at 190°C for approximately 1000 hours. Comparisons of the distributions of the alloying elements revealed an interesting behavior of the principal alloying metal (copper) in the 2219 alloy. In this alloy, copper is distributed in two ways: (a) uniformly throughout the metal matrix as a relatively low concentration solid solution and (b) in small discrete particles having markedly higher copper content. In the corrosion film there still are indications (relatively weak) of the particles of high copper content, together with an extensive distribution of copper (of lower concentration than in the metal solid solution) which decreases in concentration as the outer surface of the corrosion film is approached. In addition (and this seems to be the most striking aspect of the copper distribution) there is a narrow band of high copper concentration in the corrosion film immediately adjacent to the underlying metal surface in the majority of the regions examined. These features are shown in Figure II-33, which consists of a photomicrograph of two 2219 aluminum alloy plates placed side by side and a chart recording of the $\text{CuK}\alpha$ X-ray intensity produced during a microprobe scan across the corrosion films.

It has been shown^[10] that in the corrosion of aluminum by water the metal ions move outward from the metal through the oxide film, rather than the oxide ions penetrating inward through the oxide film. Apparently aluminum ions have a greater tendency than copper to migrate outward and the copper is left behind as indicated by the copper buildup at the base of the oxide layer and by the tendency for the copper concentration to decrease progressively toward the outer surface of the oxide. On the basis of the evidence obtained so far, it appears possible to conclude tentatively that much of the copper in the corroding metal remains next to the metal surface, resulting in a corrosion film of reduced copper content, and that corrosion of this alloy will not introduce

Fig. II-33 Photomicrograph of corrosion films produced on 2219 aluminum alloy.

as much copper in the reactor coolant as might be expected from the analysis of the metal and the volume of metal corroded.

During the corrosion testing of the samples no copper had been detected in the water of the loops. This was somewhat of a mystery since some of the alloy, which contains 6 wt% copper, had corroded away. It is gratifying to have an explanation of the lack of copper in the water.

During the year, four ETR fuel elements made using fuel plates clad with 2219 aluminum alloy were obtained from a commercial fuel supplier. Except for the cladding alloy, these elements are standard ETR elements containing 500 grams of U-235. Irradiation testing on these elements is scheduled to start in Cycle 97 in July 1968.

3.5 Etching of Fuel Plates (B. G. Carlson)

One of the requirements of recent fuel test reactor contracts is that two plates out of every 100 manufactured be destructively examined. Metallographically, this includes four full transverse sections and four longitudinal sections through core runout. These are checked for lamination thickness, fuel distribution and size, and bonding. These are all easily handled except the bonding examination. Specifications call for 50% of the clad-to-core interface to show grain growth across its surface. Chemical etching will reveal grains and show bonding in the picture frame to clad area, but it tends to over-etch the matrix, masking grain boundaries in the core. Figure II-34 is typical of an NaOH etched section. A number of grains are shown crossing the aluminum clad boundary but the heavy etch obscures growth in the core.

Heating the polished microsection in a vacuum (10^{-4} to 10^{-5} torr) to 500°C rapidly and holding at that temperature for 20 minutes produces a well defined grain boundary (Figure II-35). Longer times at that temperature cause the fuel to partially react and the cladding to darken without improving the etch. Figures II-35 through -37 show the same area after progressively longer times at 500°C.

The high temperature of this technique may raise a question as to how much of the grain growth is due to the etching operation. Close examination of the grains in Figures II-35 through -37 will reveal little change in size.

This procedure was developed on the hot stage microscope; for the volume of samples generated in fuel fabrication, a simple resistance heated vacuum furnace would be more desirable.

With a suitable number of mounts and a vacuum furnace, the routine examination for bonding could be accomplished with little more difficulty than a chemical etchant. Results are far superior. Grains can actually be seen going into the matrix at low magnification, which is not possible with NaOH or HBF_4 etch.

NaOH

80X

Fig. II-34 Chemically etched fuel plate showing grain boundaries except in the matrix.

HEAT ETCH

120X

Fig. II-35 Grain boundaries after 20 minutes at 500 degrees in vacuum.

HEAT ETCH

120X

Fig. II-36 Same area as Figure II-34, after 35 minutes.

HEAT ETCH

120X

Fig. II-37 Same area as Figure II-34, after 90 minutes.

4. SERVICE WORK

4.1 Shaft Failure from ATR Regulating Rod Motor (M. J. Graber, B. G. Carlson)

The material of a failed ATR regulating rod motor (SN-ZXC 5253) shaft was examined to determine the mode of failure, type of material, and if it met specifications.

The failure occurred during a simulated reactor startup. When the regulating rods switched from manual control to automatic control, a procedure which is not expected in an actual startup, the rod travel made a sudden reversal and ran at full speed into the stop. The west rod and east rod were both subjected to the same operating conditions but only the west rod motor shaft failed.

It was concluded that:

- (1) Failure of the shaft resulted from loading beyond the yield strength of the material.
- (2) The failed shaft material did not meet the requirements called out on the drawing or in the original design. The material hardness was $R_c 27$ rather than the specified $R_c 32$.
- (3) The shaft material was a carbon manganese steel C1144. The microstructure of the shaft material, a mixture of fine pearlite and ferrite (Figure II-38), is an indication of its

Fig. II-38 Microstructure of failed shaft material in fine pearlite and ferrite.

metallurgical history. Pearlite can be formed by (a) slow cooling (longer than a minute) from above the eutectoid temperature (1330°F) through the 1025 to 1325°F temperature range or (b) by rapid cooling (less than a second) from the all austenite range (above the 1330°F) to some "intermediate" temperature (say 1100°F) and isothermally transforming (hold at temperature) the austenite to ferrite and pearlite. The finer the pearlite the harder it is and the lower its isothermal transformation temperature was.

The fact that the central part of the shaft was hardened by this "slow cooling" process rather than being rapidly quenched and tempered results in a lower strength for a given hardness. The yield strength of this material based on its metallurgical history and hardness (R_{c27}) is 90,000 psi, while the yield if water quenched and tempered to the same hardness would be 114,000 psi.

The fracture halves of the failure can be seen in Figure II-39. The twisted appearance of the shaft at the splines is evidence of good ductility and indicates

Fig. II-39 Fractured halves of shaft failure.

the mode of failure was loading beyond the yield strength. The lack of twisting of the splines in the part removed is attributed to the rigidity imparted by the spur gear which housed this section.

5. NEW EQUIPMENT ADDITIONS

A number of new equipment items of major importance were added to the Metallurgy Laboratory of the Reactor Engineering Branch during the year. These are discussed in this portion of the report.

5.1 Thermal Conductivity Measuring Apparatus

Very little data have been published on the thermal properties of new materials being developed for reactor application. The newly acquired thermal conductivity measuring apparatus will enable the Metallurgy Laboratory to determine the coefficient of thermal conductivity of such materials as UAl_2 , UAl_3 , UAl_4 , U_3Si , and others as they are developed. This information will assist the heat transfer and stress analysts to determine the reactor behavior of such materials when incorporated in fuel elements.

A cut-away sketch of the thermal conductivity test stand designed and manufactured by Idaho Nuclear is shown in Figure II-40. Installation will be completed early in FY-69. The first experiment is planned to determine the effect of Si additions on the thermal conductivity of U-Al alloys.

Fig. II-40 Cutaway sketch of Thermal Conductivity Test Stand.

5.2 Thermal Expansion Measuring Apparatus

There is a lack of published information on the thermal expansion characteristics of many of the materials being developed in the Metallurgy Laboratory. The dilatometer will not only measure the thermal expansion of basic raw materials, but will also be equipped to measure the thermal expansion of fabricated composite fuel plates.

This instrument is Model TE-3000-LA obtained from the Thermo-Physics Corporation of Cambridge, Massachusetts. It will plot dilation versus temperature directly on an X-Y recorder through an LVDT-thermocouple arrangement. The maximum temperature range is 0 to 1500°C. The instrument was installed and tested in FY-68 and operation will begin early in FY-69.

5.3 High-Temperature Controlled-Atmosphere Metallography

In order to more thoroughly predict the performance of new reactor materials, it is necessary to determine the compatibility of fuel core-cladding materials. Once the parameters of a given system have been defined, the optimum particle size, particle matrix combination, and void content can be prescribed for a given set of exposure conditions to provide maximum fuel stability.

The high-temperature controlled-atmosphere metallograph (hot stage metallograph) will be used for reaction rate (compatibility) studies between various fuel and matrix combinations and cladding and corrosion protecting substances. This new equipment obtained from William J. Hacker and Co. will enable the Metallurgy Laboratory to make such studies more accurately, more quickly, and with much greater convenience than could be done before. The instrument will reach a maximum temperature of 1500°C.

5.4 Electron Beam Furnace and Welding Unit

The conventional methods for production of nuclear fuels require that vacuum casting of alloys or hot pressing of powdered materials be employed. Inherent to these processes is contamination pickup from the crucibles, molds, or dies, and contamination by gas entrapment in the case of the hot pressing of powders.

The electron beam furnace and welding combination unit, obtained from Brad Thompson Industries, Inc., will provide a versatile and reliable means for the manufacture of uranium bearing alloys, intermetallic compounds, and potential cladding materials. Contamination of the melt will be reduced over that experienced with conventional techniques because of the high vacuum inherent to the use of an electron beam. In addition, metals with very high melting points can be consolidated because of the high energy density and excellent thermal efficiency. The production of high purity materials through the use of the electron beam melting furnace will give a better insight into the behavior and understanding of nuclear reactor fuels and cladding materials.

The second function of this unit is to join metals via the electron beam technique. This will enable the Metallurgy Laboratory to join dissimilar metals and refractory metals for closures of such items as test fuel plates, capsules, and pins.

This instrument delivers an output of 30 kV at 6 kW. It is capable of producing high-quality welds with very narrow heat-affected zones. The beam can be focused down to 1/8 inch. The vacuum chamber capacity is about eight

cubic feet and can be evacuated to a pressure of 10^{-4} torr. Warm-up and pump-down time for the "cold" instrument is about one hour. Pump-down time after changeouts is about three minutes.

5.5 Fluid-to-Fluid Extrusion Unit

The fluid-to-fluid apparatus was obtained from the Pressure Technology Corp. of America. This unit will be used to fabricate experimental and conventional uranium compounds into suitable geometric configurations for out-of-pile and in-pile testing. It will provide a versatile means of fabricating: (a) pure uranium compounds into solid pieces suitable for mechanical and physical properties testing, (b) test fuel pins for in-pile investigation of built fuels, (c) dispersion-type test fuel rods and plates for in-pile evaluation, and (d) special materials where a temperature limitation precludes conventional techniques.

The instrument has a maximum chamber capacity of 250,000 psi, thus can extrude materials with yield strengths below this. The present dies range from 0.1 to 0.5 inch. The instrument has the capability of extruding fluid-to-pressureurized-fluid, fluid-to-fluid-at-atmospheric-pressure, and fluid-to-atmosphere.

6. REFERENCES

1. G. W. Gibson, The Development of Powdered Uranium-Aluminide Compounds for Use as Nuclear Reactor Fuels, IN-1133 (December 1967).
2. W. C. Francis and R. A. Moen (eds.), Annual Progress Report on Reactor Fuels and Materials Development for FY 1966, IDO-17218 (November 1966).
3. R. C. Daniel et al, Effects of High Burnup of Zircaloy-Clad, Bulk UO_2 , Plate Fuel Element Samples, WAPD-263 (September 1962).
4. S. H. Paine and J. H. Kittel, "Irradiation Effects in Uranium and Its Alloy", in Proceedings of the International Conference on the Peaceful Uses of Atomic Energy Held in Geneva 8 August-20 August 1955. Volume 7. Nuclear Chemistry and Effects of Irradiation, New York: United Nations, 1956. pp 445-454. (A/CONF.8/P/745).
5. G. W. Gibson and M. J. Graber (eds.), Annual Progress Report on Reactor Fuels and Materials Development for FY 1967, IN-1131 (February 1968).
6. J. E. Cline, Idaho Nuclear Corporation, private communication.
7. W. C. Thurber and R. J. Beaver, Development of Silicon-Modified 48 wt% U-Al Alloys for Aluminum Plate-Type Fuel Elements, ORNL-2602 (March 1959).
8. E. V. Sumner and D. Q. Cole, Second Annual Report, Development of Ultra-high Strength, Low Density Aluminum Plate Composites, Harvey Engineering Laboratories, Report HA-2166, July 1965.

9. V. A. Walker, M. J. Graber, G.W.Gibson, ATR Fuel Materials Development Irradiation Results -- Part II, IDO-17157 (June 1966).
10. U. R. Evans, The Corrosion and Oxidation of Metals: Scientific Principles and Practical Applications, London: Edward Arnold (Publishers) Ltd., 1960, p 243.

III. ENGINEERING EXPERIMENTS

1. ATR THERMAL-HYDRAULICS

(M. L. Griebenow, G. N. Fillmore, J. A. Hendricks)

1.1 ATR Reactor Systems Hydraulic Testing

A total of 13 dummy (depleted fuel) fuel elements were instrumented to measure the coolant flow rates and both the axial and the lateral pressure gradients within the ATR reactor. The principal objectives were as follows:

- (1) The evaluation of local coolant flow rates and the dependence of any observed irregularities on quadrant flow imbalance, intraquadrant flow restrictions, and intraelement flow restrictions
- (2) The direct measurement of lateral pressures acting across the fuel plates and the change in the thickness of Channels 19 and 20 caused by these pressures
- (3) The measurement of entrance, exit, and frictional losses within the fuel assemblies.

Extensive reactor testing, utilizing the instrumented fuel assemblies, demonstrated that:

- (1) The coolant flow rates are not a function of either the quadrant or the position within the quadrant.
- (2) The coolant flow rates in Channels 1 through 18 are within 2% of the computed values used for thermal-hydraulics and safety analysis^[1,2]. The velocities in Channels 19 and 20 are 5 and 20% low, respectively. Two standard deviations about the measured mean velocities are 1.17, 1.09, 1.06, 1.06, 1.07, 1.06, 1.09, and 1.36 for Channels 1, 2, 3, 9, 10, 18, 19, and 20, respectively.
- (3) The lateral pressures, acting across the fuel plates, are small and should not adversely affect any fuel assembly that is properly constructed.
- (4) The fuel entrance and exit conditions differ from those anticipated, resulting in approximately a 6 psi increase in pressure at any point within the fuel assembly (at the design core pressure drop).

The measured data and analysis are reported in detail in Reference 2.

1.2 Calculational Techniques

The MACABRE^[3] computer code, used for the ATR fuel element thermal-hydraulic analysis^[1,2], has been expanded to:

- (1) Allow for channel dependent bias and standard deviation in the coolant flow rate
- (2) Allow for time dependent (quasi steady state) variation in the heat generation
- (3) Compute the corrosion of the aluminum-clad heat transfer surfaces (when present)
- (4) Compute the thermal conditions in the fuel assemblies containing the split core fuel plates used for direct temperature measurement within the reactor core.

The code and its capabilities are reported in Reference 3.

1.3 ATR Core 1 -- Approach to Power

An extensive, thermal-hydraulic test program has been developed for the ATR startup. The test program will define thermal conditions in the various heat transfer modes that will be encountered during normal and off-normal operating conditions. The program and the associated hazards analysis are presented in Reference 6. The experimental fuel elements, required by the program, have been fabricated, dimensionally inspected, hydraulic proof-tested, reinspected, and instrumented; the instrumented elements have been subjected to a final hydraulic proof-test and dimensional inspection.[7]

1.4 ATR Core 2

Thermal-hydraulic analysis of the proposed Core 2 operating conditions has been based on the computed fission rates and the measured coolant flow rates[3] using the MACABRE II[4] computer code. The limiting conditions have been set by the blister temperature[8] and the thermal stress-yield strength[9] relationship. Analyses performed to date show:

- (1) The maximum temperatures and stresses will be developed after approximately 17 days of operation, and continued operation to maximum fuel depletion will not aggravate the thermal conditions.
- (2) The fuel elements, operating in the hottest core positions, must be selected to stay within the acceptable blister and stress limitations. Selection will emphasize the thickness of Channels 19 and 20.

Analysis is continuing and recommended operating conditions should be released in the immediate future.

1.5 ATR Core 3

The experiments, scheduled for irradiation during Core 13, are not yet established. However, to maintain adequate lead time, the effect of the channel-dependent reduction in coolant flow[3] on the thermal hydraulics and stress during 250 MW (40-50-60 MW lobe unbalance) operation has been analyzed with reference fuel experiments. Core 3 is scheduled to operate with radially zone-loaded fuel (7F prescription) elements in the 12 hottest positions. The

7F prescription was developed to reduce the thermal and stress load developed in Plates 18 and 19 and therefore minimizes the effect of the reduced coolant flows in Channels 19 and 20.

The large bias (-20%) and standard deviation (18%) measured in Channel 20^[3] together with its minimum measured size (0.060 inch) have shifted the hot spot from Plates 5 and 15 to Plate 19. The thermal analysis indicates that none of the 7F elements, received to date, will exceed the 700°F, blister-stress limitation imposed on the UAl_x fuel assemblies.

Final preoperational Core 3 analysis will be performed after the experiment loadings have been specified.

1.6 ATR Hydraulic Development Testing

1.61 Coolant Flow Redistribution. The lower coolant flow rates in Channels 19 and 20^[3] of the ATR elements cause an undesirable increase in the temperatures and stresses. The low coolant velocities in Channel 20 are a result of its small minimum size (0.053 inch in MARK IV elements and 0.063 inch in MARK VI elements) and an additional bias that, as yet, remains unexplained. An ex-reactor, hydraulic test program is in progress to investigate the cause and possible methods of compensating for the reduced flow rates. (One method -- redistribution of the thermal load -- has been discussed in the Section III-1.5 of this report.)

The flow bias in Channel 20 appears to be either a flow resistance term not accounted for in the calculations or a change in the channel thickness along the length of the fuel element. The curved outer portion of the upper end casting presents the only obvious physical flow resistance that would selectively affect Channels 19 and 20. To investigate the effect of this portion of the end casting on the flow through Channels 19 and 20, two dummy (depleted) fuel elements were selected. One (DA11D) was selected because the curved outer portion of its upper end casting extended the furthest from the center of curvature, thus blocking more of the direct coolant supply to Channel 20. The second (DA18B) was selected because it had low coolant flow rates during the in-core hydraulic experiments^[3] (Section III-1.1). These fuel elements were instrumented for coolant velocity measurements, similar to the instrumentation described in Reference 2, and tested in the ATR hydraulic test facility. The obstructing portion of the upper end castings (with the exception of the control-drum drive-housing support pads) was then removed and the coolant velocities remeasured. The end box modification, in both cases, decreased the coolant velocity through the hot region of Channel 20.

The thickness of Channel 20 has been measured, at various axial locations, at full flow conditions. The measurements have been made with proximity gages that sensed the relative location of Plate 19 without applying any physical load to the plate and with mechanical gages that, although they applied a small load to Plate 19, had the advantage of simplicity and provided a positive measurement. There has been no indication of the plate or element motion required to produce the measured change in flow rate.

To investigate the possibility of physically restraining the fuel element so that the minimum Channel 20 thickness is larger, the side plate rails were built up on dummy assembly DA12D by adding weld material at 2, 16, 30, and 46-1/2 inches from the top of the fuel plates. The weld material was then

dressed down to form pads one inch long extending 0.015 inch beyond the current maximum location of the side plate rails, ie, 0.088 inch beyond the outer surface of Plate 19. The weld material was laid down with a miniature heliarc, and careful pre-post-inspection revealed no change in the physical tolerance of the assembly and no detectable weld-affected zone in the side plate. The fuel assembly was instrumented for coolant velocity measurements and tested in the ATR hydraulic test facility. The coolant flow through Channel 20 increased to an acceptable value (46-1/2 ft/sec at the axial hot spot location when operating @ 100 psid and 350°F) but the disparity between measured and computed flow rates was not significantly affected.

The coolant velocities measured in the various number 20 channels (at 100 psid) are shown in Figure III-1. Also shown for comparison are the velocities computed by the MACABRE II^[4] computer code.

1.62 Vent Size Optimization. The two-inch side plate vents reduce the fuel element resistance to axial bending thereby requiring a smaller hydraulic force to produce a given change in the external coolant channel thicknesses. The location and size of the side plate vents (two inches) was originally set, somewhat arbitrarily, from limited data. A hydraulic test program is currently in progress to determine the optimum location and the minimum vent size that will allow for adequate interchannel hydraulic communication. Dummy (depleted) fuel element DA5D was instrumented with a probe that measures the coolant velocities and pressures while axially traversing the coolant channels. DA5D was initially tested without vents. One-eighth-inch-long vents were then milled into the side plates at the three axial locations calculated, by use of the MACABRE II^[5] computer code, to produce the maximum reduction in lateral hydraulic forces. The vents are located 5, 14, and 26 inches from the top of the fuel plates. One-eighth-inch incremental increases, in vent length, have been made between each hydraulic test. Tests have been completed with no vent and with 1/8-, 1/4-, 3/8-, and 1/2-inch-long vents. Further tests are planned.

1.63 End Casting Modification. Handling experience, by Operations personnel, has revealed that the strength and shock resistance of the MARK IV ATR fuel element end castings was inadequate. The improved MARK V and MARK VI castings were designed to circumvent this handling problem, and stainless steel support plates were designed to be riveted to the end castings on the MARK IV assemblies in inventory. The stainless support plates were riveted to the end castings on two dummy fuel elements, and the instrumented elements were tested in the ATR hydraulic test facility. The hydraulic tests revealed no significant change in coolant flow rate or distribution. The support plates have been installed on all Core 1 fuel elements in preparation for the ATR startup.

1.7 ATR Hydraulic Proof Testing

All enriched ATR fuel elements fabricated by Atomics International are undergoing hydraulic proof testing at 120% of nominal flow at 350°F. One hundred percent proof testing, rather than a statistical sampling plan, will continue until the cause of recent failures has been determined. Two fuel elements have failed, by gross plate buckling, during the current test program. In both cases the failure was in internal fuel plates where high lateral pressures are not normally produced.

Fig. III-1 ATR Channel 20 coolant velocities.

A less serious problem has been encountered during the current proof-test program. Three elements were found to have failed plug welds in the junction between the side plates and the upper end casting. Tack welding the thin, leading edge of the side plates (in addition to the existing plug welds) appears to have eliminated the problem.

1.8 ATR Fuel Fabrication Inspection

The thickness of the ATR fuel element coolant channels is one of the prime factors in establishing reactor operating limits. Each fuel element is inspected as part of the Idaho Nuclear Corporation receiving procedure. Each coolant channel is measured at three azimuthal locations ($3/8$ inch from each side plate -- Planes A and C -- and within $3/8$ inch of the center line -- Plane B). The distribution of the average channel thickness and the distribution about the average (the factors of principal concern in thermal hydraulic analysis of core performance) are shown in Figures III-2 through -5 for the fuel element received under the current contract with Atomics International.

INC-C-12101

Fig. III-2 ATR fuel assemblies fabricated by Atomics International.

Fig. III-3 ATR fuel assemblies fabricated by Atomics International.

Channel Spacing Distribution About the Average
at Planes A & C

Fig. III-4 ATR fuel assemblies fabricated by Atomics International.

Fig. III-5 ATR fuel assemblies fabricated by Atomics International.

2. ETR THERMAL-HYDRAULICS

Cycle 83-84 (August 1966) was operated for 6,000 MWd. Because of the increased operating time, the hot fuel elements from this cycle permitted an accuracy check of the extended corrosion rate calculations^[5] and a measurement of the effect of the increased thermal stresses produced by the corrosion product retained on the heat transfer surfaces. Cycle 83-84 fuel elements E155D and E175D, the two hottest, were retained for physical and chemical analysis. The thickness of the coolant channels was measured using an underwater ultrasonic device developed for this purpose by the Instrument Development Branch of Phillips Petroleum Company. The coolant channels showed considerable

distortion (Table III-1). Because of the anticipated desire to increase the operating cycle, a surveillance program was established, during Cycle 90, to routinely measure the post-operation channel spacing for evidence of excessive thermal stresses and to destructively examine selected elements for retained corrosion product, blistering, burnup, etc. The initial channel thicknesses are known from the receiving inspections, and the fuel elements operating in the hot positions are selected for their superior channel spacing. Therefore, any significant variation in channel thickness (in excess of 0.008 to 0.010 inch), shown in Table III-1, can be regarded as a change due to stresses produced by fission product retention, hydraulic forces, or thermal gradients. The ETR operating cycle was extended to 6,000 MWd, starting with Cycle 92.

TABLE III-1

POST-OPERATION CHANNEL THICKNESS MEASUREMENTS

Core Life (MWd)	Cycle	Fuel Element [a]	Core Position	Average Burnup (wt%)	Maximum Difference in Channel Spacing (in.)
6000	83-84	E155D	I-10	39	0.035
		E175D	H-9	36	>0.040
3630	90	E102C	I-10	26	0.014
		E423D	I-8	27	0.019
4870	91	E166D	J-9	32	>0.060
		E84D	H-12	30	0.011
		E79E	I-8	29	0.005
5950	92	E186E	J-9	38	0.006
		E192E	H-12	37	0.006
		E202E	J-8	37	0.004
5966	93	E247E	H-11	37	0.007
5966		E270E	H-12	37	0.009
3178		E118D	H-8	17	0.009
3178		E122D	I-8	20	0.010
7658		E91E	H-8	34 (0.19) [b]	0.019
7658		E129E	I-8	38 (0.21) [b]	0.003

[a] C -- United Nuclear element with rolled swaged plates.
D -- Babcock and Wilcox element with pinned fuel plates.
E -- Atomics International element with roll-swaged fuel plates.

[b] Fractional fuel burnup (overall element average) after 4870 MWd of time prior to insertion into Cycle 93.

Data gathered during the surveillance program indicated that only those elements in which the fuel plates were physically prevented from differential axial expansion (pinned construction) at the fuel plate to side plate joint produce potentially deleterious plate distortion in 6,000 MWd of operation. During the Cycle 93 midcycle shutdown, two pinned fuel elements (E118D and E122D) were removed from the hot positions and replaced with roll-swaged elements (E91E and E129E) that had been operated to the same depletion (as the pinned elements) in a previous cycle. Since Cycle 93, pinned elements have not been operated in the hot ETR positions.

Fuel element E186E (5950 MWd of operation) was destructively examined in the TRA hot cell, and the measured corrosion product will be correlated with the computed value.

3. MTR THERMAL-HYDRAULICS

Thermal-hydraulic analysis of the proposed MTR plutonium fuel loading (Phoenix) has tentatively set the operating limit at 32 MW for a 3,000 MWd cycle. The analysis was based on PRCF measured power densities and distributions, and the actual operating power limit will be based on a comparison with critical measurements in the MTR. The analysis is described in detail in Reference 7.

4. ATR HEAT TRANSFER FACILITY (F. O. Haroldsen)

A heat transfer loop to be operated in conjunction with the existing ATR hydraulic loop is being designed and built to test and further develop ATR-type fuel elements in an effort to increase the capacity of the elements and/or reduce the cost of the elements by finding ways of relaxing the tolerance limitations.

Four surplus DC rectifier units were obtained from the AEC CANEL site to be used as the power source for the loop. These rectifiers were then modified by Rapid Electric Company of New York City so that a range of power requirements could be obtained. This modification, in effect, made each rectifier into two smaller units, which makes it possible to obtain a wide range of voltage-amperage combinations, depending on what parallel-series connection is used. There is also a transformer associated with each rectifier which can be either bypassed or used. If the transformer is used, the supply voltage to the rectifier is 690 volts; and if the transformer is bypassed, the supply voltage is 2300 volts. This further increases the range of voltage-amperage combinations.

A phase shifter has also been obtained and installed so that the power phase to two of the rectifiers is shifted 15 degrees in an effort to decrease the DC ripple. The rectifiers and associated switch gear are located in a building adjacent to the hydraulic loop building, and the control panel for the heat transfer loop is located in the hydraulic loop building adjacent to the hydraulic loop control panel.

By varying the supply voltage and the rectifier parallel-series combinations, the following DC voltage-amperage combinations can be obtained.

<u>Supply Voltage</u>	<u>DC Voltage</u>	<u>Current Amperes</u>
690	12	80,000
690	24	40,000
690	48	20,000
690	96	10,000
2300	50	40,000
2300	100	20,000
2300	200	10,000
2300	400	5,000

In order to simulate reactor transients, it was desirable that the rectifiers be able to closely approximate a large step increase in power. Several transients were run with the power output being monitored on a Visicorder. In general, it was found that the higher the initial power setting, the faster the response would be, and also that the response would be faster with all four rectifiers on the line than with just part of them. It also appeared that a series-parallel combination would respond slightly faster than when all units were in parallel. In summary, the data indicate that with all four rectifiers operating in parallel, a change in power level from 30% of full power to 100% can be accomplished in 0.3 to 0.5 second.

In order to obtain as near as possible a true DC output, the transformer of one-half of each rectifier is wired in delta, and the other half is wired in wye. Also, the secondary side of these wye and delta transformers is wired so that the output is six phase. This, coupled with the phase shifter, results in a reasonably smooth DC output. (There is also an inductive choke filter connected in the output of each rectifier to further reduce the ripple.) During the checkout of the system, an RMS meter was placed across the test section. With 10.5 volts DC and 74,000 amps, the RMS voltage was 0.105 volts resulting in a DC ripple of 1%. At low power levels, the DC ripple increased to about 5%.

An 80,000 ampere DC shunt is used for current measurements. This shunt has an output voltage of 100 millivolts DC at rated current, has an accuracy of about 0.5% at rated load, and is essentially independent of temperature over the operating range.

5. HEAT TRANSFER ANALYSIS OF ETR ELEMENT E018D (F. O. Haroldsen)

The following analysis attempts to determine, from a thermal-hydraulic point of view, the sequence of events that took place after tape obstructed flow through approximately 75% of the channels in fuel element E018D in the ETR. This element contained 400 grams of U-235 in the form of UAl_3 , operated in Position M12, generated 1.25% of core power at xenon equilibrium, and had been in the reactor at full power for 13-1/2 hours when it failed.

Figure III-6 is a top view of the element after the bail was removed. Part of the tape had been removed when this picture was taken; the dotted line indicates the area originally covered.

Plates 5 through 13 in the element had melted regions of different amounts, the longest being from 10 to 12 inches of the top to within 5 to 7 inches of the bottom and to within 1/2 to 3/4 inch of the side plate. Figure III-7 shows the melted zones of Plate 7.

The vertical and horizontal flux profiles used to determine heat generation rates for the analysis of this element were taken from ETRC data. The horizontal profile used was measured in Channel 9 of the element.

6.1 Analysis and Discussion

The tape that obstructed the flow through the element was tape that had inadvertently been left on a structural member above the core during a reactor shutdown. It seems reasonable to assume that the tape was washed from the structural member and landed on the fuel element in one lot. This resulted in a sudden reduction in flow rate or possibly a complete flow stoppage. In either event, several plates experienced a thermal burnout and melted a portion of the plates.

In an effort to determine the actual processes that led to failure, several cases were run with the PDQ and the MACABRE heat transfer programs to bracket the conditions that existed. The cases considered are as follows: (a) zero flow, no melting, and all heat conducted to the side plates, (b) zero flow, center portion of plate melted, and remaining heat conducted to the side plates, (c) center portion of plate melted, convective cooling over all of the remaining fuel plate, and (d) center portion of plate melted, adiabatic wall conditions over part of the remaining plate, and convective cooling over the balance of the plate.

Fig. III-6 Upper end of element showing comb, channels, plates, and tape.

Fig. III-7 Fuel plate No. 7.

Figure III-8 shows the plate surface temperatures that would have existed if the plates had not melted, and if the flow had been completely blocked off, and all heat generated had been conducted to the side plates. Different power levels or heat generation rates corresponding to different vertical locations in the element are shown. Also shown in the same figure is the case where the center part of the plate has melted and the remaining heat generated has been transferred to the side plate.

Figure III-9 shows the cases that would have existed after part of the plate had melted away but with a small amount of flow (a velocity of 4 ft/sec) still flowing past either all or part of the remaining plate.

As can be noted from Figure III-8, if there had been complete blockage there would have been a melted region over the full length of the plate and probably closer to the side plates. This indicates that only a small portion of the heat generated in the fuel plates could have been transferred to the side plates, and that there had to have been some cooling available at the edges of the plates after the center portion had melted away.

Figure III-9 indicates that a local boiling burnout condition developed in the central part of the plate and extended across the plate to a location slightly past the melted boundary (Figure III-7 shows a feathering effect at the melted boundary) and that the balance of the plate next to the side plate was in a convective heat transfer situation.

Griegenow^[10] states that a burnout heat flux equal to 85% of the "Bernath burnout heat flux" is a reasonable value for ETR-type elements. The Bernath burnout equation states that:

$$(Q/A)_{B.O.} = (h_{B.O.}) (T_{w_{B.O.}} - T_B)$$

where

$$h_{B.O.} = 10,980 \left[\frac{D_e}{D_e + D_i} \right] + \frac{48V}{D_e^{-6}}$$

and

$$T_{w_{B.O.}} = 57 \ln p - \frac{54p}{p + 15} - \frac{V}{4} .$$

With a velocity of from 1 to 5 ft/sec, the flow blockage has to be in the order of 90 to 98%, assuming the pressure drop across the tape would respond about the same as an orifice. With a blockage of this magnitude, about 98 to 99% of the available pressure drop is taken across the tape. Under these conditions, the pressure is almost a constant through the length of the element, so that the "Bernath Burnout Equation" at any point along these plates is almost independent of pressure and the bulk temperature becomes the predominant influence with a small effect from the velocity.

Fig. III-8 Calculated fuel plate surface temperature assuming fuel plate does not melt and all heat has to be transferred to the side plate channel.

Fig. III-9 Calculated fuel plate temperature with a portion of the plates melted away but with a small amount of flow in the channels.

Figure III-10 is a family of curves of 85% of the "Bernath burnout heat flux" as a function of bulk temperature for different velocities for this element. Figure III-11 shows the bulk temperature as a function of velocity at different locations in the element, and Figure III-12 shows the axial heat flux of the element.

The heat flux at 10 inches, 27 inches (hot spot), and 30 inches from the top of the plate, as Figure III-12 indicates, was 3.5×10^5 , 4.4×10^5 , and 3.6×10^5 Btu/hr-ft², respectively. Comparing these values with Figures III-10 and -11, it can be noted that these fluxes would have been equal to the burnout heat flux when the coolant (a) has a velocity of 1.25 ft/sec with a temperature of 260°F at the 10 inch level, (b) has a velocity of 4 ft/sec with a temperature of 328°F at the 27 inch level, and (c) has a velocity of 3 ft/sec with a temperature of 330°F at the 30 inch level. At the outset, these numbers appear to be incompatible. However, a closer examination shows that they could have been possible. Photographs of the top of the element, similar to Figure III-6 before the tape was removed, indicate that any major openings in the tape probably occurred along one or possibly both side plates. This would have undoubtedly resulted in coolant streaming along the sides with a relatively stagnant region toward the upper middle portion of the plate. As the coolant moved down the channel, it would have redistributed itself more uniformly across the channel.

It therefore appears that the most logical sequence of events that fits the conditions is as follows:

- (1) The tape landed on the element in one lot causing a flow blockage which reduced the velocity to a value slightly larger than 4 ft/sec. This resulted in the exit temperature being at saturation. This caused some additional resistance which caused a further reduction in velocity to about 4 ft/sec.
- (2) With this velocity coupled with the streaming in the upper region mentioned above, the center portion of the plate from just below the hot spot up to about the 10 inch level went into a local boiling burnout condition and a vapor blanket was formed over an area slightly wider than that which melted, causing the plate to melt. The hot spot was actually at the edge of the fuel plate, but because of the large mass of the side plate and because of the unfueled portion of the fuel plate, the plate was cooled sufficiently to maintain a forced convection situation for a small width of the plate next to the side plate.
- (3) The vapor plus the molten metal caused more resistance to flow, causing the velocity to drop to about 3 ft/sec, at least in the lower central portion of the plate, allowing the burnout region to propagate down to the 30 inch level (six inches from the bottom).
- (4) With a large portion of the source gone, the steam blanket disappeared, reducing the resistance to flow and allowing

Fig. III-10 0.85 Bernath critical heat flux for ETR element covered with tape.

Fig. III-11 Bulk water temperature in fuel plate channel.

it to increase back up to a velocity slightly larger than 4 ft/sec. This resulted in a forced convection situation over the remaining fuel plate, with temperatures somewhat as shown on the lower plot of Figure III-9.

The two upper curves of Figure III-9 are based on the assumptions that (a) the center part of the plate had melted, (b) a vapor blanket covered part of the remaining plate (adiabatic plate), and (c) there was forced convection (4 ft/sec) over the balance of the plate next to the side plate. This could not

Fig. III-12 Axial heat flux ETR position M12.

have been the steady state condition after the initial melting phase because, as the plot shows, the metal temperature in the region of forced convection would have been from 550 to 800°F. The Bernath wall burnout temperature is about 475 to 500°F, which indicates that that portion of the plate shown to be in forced convection would actually have been in a burnout condition. This would result in the temperatures becoming as shown on the lower plot of Figure III-7 (assuming the plate did not melt). Since this cannot be, it appears that all of the unmelted part of the plate would have had to be under forced convection heat transfer which justifies statement four above.

A velocity of 4 ft/sec would have required an area blockage of about 90% or there could have been a hole the thickness of the channel and about 1/16 inch wide. An examination of Figure III-6 (allowing for the fact that about half of the tape has been removed in this photograph) seems to indicate that the amount of blockage, at least in the middle channels, was of this order of magnitude.

Figure III-11 shows that the melted zone was closer to one side of the plate than the other side. The horizontal flux profile indicates that the cold side of the plate had a heat flux of 30 to 40% less than the hot side. This was undoubtedly the main reason for the unsymmetrical melting.

The possibility that the burnout or meltdown may have occurred during the steam-filled portion of a "chugging" cycle should be considered.

Calculations which consider buoyancy forces, viscous drag forces, and pressure differential forces indicate that steam bubbles up to 0.105 inch (the channel thickness) and about 0.75 inch in diameter would have been carried down the channel. Bubbles between that size and 0.105 inch by about 1.2 inches by about 5 inches would have risen up the channel, and bubbles larger than this would have been carried down the channel.

Therefore, for a steam blanket to form over approximately that part of the plate that melted, the following sequence of events would have had to take place: (a) the formation of net vapor near the exit, the location where saturation conditions were first reached, (b) the coalescence of the bubbles into a larger bubble 0.105 inch thick (full channel thickness) by about 1.2 inches wide and 3 to 5 inches long so that the vapor would rise rather than be carried down and out the channel, and (c) the bubble would then have had to rise and grow until it was about 0.105 inch by 1.75 inches by 5 or 6 inches by the time it had reached a level of about 10 inches from the top of the element (the highest level where melting took place). At this point the bubble would have had to have been swept out the bottom of the element.

Two points make it appear unlikely that this mechanism could have occurred: (a) The steam bubbles which would have started to form near the exit would have had to form and coalesce at a fast enough rate so that they would have risen rather than being carried out the bottom of the channel and at the same time not blanket the lower part of the plate. Otherwise, the lower part of the plate would have also melted; (b) The predicted local boiling burn-out conditions would have been reached either right at or slightly before saturation conditions were reached at the exit which would have in effect eliminated the triggering mechanism for chugging.

It would therefore appear that forced convection and local boiling burn-out as described earlier were the processes that took place.

It should be noted, however, that this analysis is quantitative and would only apply to the channels and plates with melted regions as specified. The sequence of events taking place in the channels where lesser amounts of melting took place would undoubtedly be the same, but the final flow rates would be slightly different. Also, no allowance has been made for inter-channel mixing that may have taken place after melting started.

6. THERMAL BOWING OF THE ATR PRESSURE TUBE AND INSULATION JACKET (N. R. Byers)

To ensure proper operation of the safety rod in routine and emergency shutdowns of the Advanced Test Reactor, the amount of thermal bowing of the insulation jacket must have a predictable maximum.

This section presents a method by which the amount of thermal bowing may be conservatively predicted and includes a computer program which may be applicable for the solution of other problems of this type.

The solution plan is as follows: (a) Assume a value of thermal bowing, Δ_a (at midcore) for the insulation jacket. (b) Perform the fluid flow and heat transfer analysis (including the assumption of an adiabatic surface in the pressure tube) resulting in a temperature drop across the P.T. (pressure tube) and across the I.J. (insulation jacket). Note: this is not the temperature drop across the tube walls but the temperature drop across the outside diameters of the P.T. and I.J. (c) The Δ_t 's (temperature drops) from step b are entered into the deflection analysis for the P.T. and I.J. and a calculated value of Δ_c (deflection) found. (d) The Δ_c from step c is compared to the Δ_a of step a. If the difference $|\Delta_c - \Delta_a|_{\max} > 0.0001$, a new Δ_a is assumed; a converging iteration process results. This process is continued until $|\Delta_c - \Delta_a|_{\max} < 0.0001$ inch. (e) With $|\Delta_c - \Delta_a|_{\max} < 0.0001$ inch, the value of the thermal bowing of the I.J. at midcore (this is the location of the maximum value of the I.J. thermal bowing) is considered to be sufficiently accurate for an engineering-type solution and is compared to the value of the radial clearance, between the S.R. (safety rod) and I.J., to confirm or deny adequate operational clearance.

When the internal jacket thermally bows, relative to the safety rod, it forms, with the safety rod, an eccentric annulus; and in order for the fluid flow analysis to proceed, a mathematical model composed of two different semi-circular annuli is formed as shown in Figure III-13. From this model is established the fluid flow necessary for the heat transfer portion of the solution.

The resistance to incompressible flow of any fluid in any pipe may be computed from

$$\Delta P = \frac{4 f L}{D_H} \frac{V^2}{2g} \quad (1)$$

where

f = friction factor

D_H = hydraulic diameter

L = length

V = fluid velocity

g = gravitational constant

ΔP = pressure drop.

Assume friction factors f_e and f_H for sections C and H in parallel flow, then

$$\Delta P_C = \Delta P_H$$

and

$$V_H = \frac{W}{\rho_c \left[\Delta_H + \Delta_C \sqrt{\frac{f_H}{f_c} \times \frac{D_{HC}}{D_{HH}}} \right]} \quad (2)$$

Fig. III-13 Fluid flow model.

where

V_H = velocity of flow on hot side

W = weight of water flowing

p = density of fluid

Δ = flow area

f = friction factor.

Based on this velocity, the Reynold's number was calculated for the flowing fluid; and from a table of friction factor versus Reynold's number[11], the friction factor was established. Using an iterative technique the assumed and calculated values of the friction factor were made to agree. This established the quantity of the flowing fluid for each section. With the weights of water flowing in the annuli C and H known, the heat absorbed by the water was calculated and the heat transfer portion of the problem initiated.

By the establishment of the temperature profile across the system, the temperature drops across the diameter of the P.T. and I.J. were found. The calculations were made assuming one-dimensional heat flow. Since there is heat flow to the water between the S.R. and the I.J. and heat flow to the water inside of the P.T., an adiabatic surface at radius R_3 (as yet unknown) was established since the distribution of heat flow was not known at this point in the solution. For details of the calculations necessary to establish the ΔT 's the values became

$$\Delta T_{PT} = T_{4H} - T_{4C} \quad (3)$$

$$\Delta T_{IJ} = T_{6H} - T_{6C} \quad (4)$$

These values are necessary to establish the thermal bowing in the deflection equations. The final expression for the moment M caused by thermal bowing is

$$M = \frac{\alpha E r_i (r_o - r_i)^2 \Delta T}{3\pi} \quad (5)$$

where the respective ΔT is given by Equations (3) and (4)

where

α = coefficient of thermal expansion

r_i = inside radius

r_o = outside radius

E = Young's modulus

ΔT = temperature drop across tube.

A mathematical model of the P.T. and I.J., Figure III-13, was used in order to find the required deflection since the P.T. and I.J. bow sufficiently for the six concentric tube spacers (buttons) on the P.T. To contact the I.J., the loads and deflections are the same for both tubes at these locations. The P.T. is supported at the top and bottom of the reactor. The I.J. is supported top, bottom, and also at the locator ties and is, therefore, considered as a beam on these supports. Both tubes are considered to be simply supported (can have a slope but no deflection at the supports) since the supports are not rigid (see Figure III-14). Three different moments of inertia are required to describe the different cross sections of the P.T. and I.J. since there are three different size tubes that comprise the P.T. and I.J. The required deflections for the P.T. and I.J. are found as separate beams, and since the deflection at each button must be the same for both, a simultaneous solution is made in order to evaluate the P forces.

Fig. III-14 Model of insulation jacket (I.J.) supports.

The principle of superposition is used in order to find the deflection, slope, and moment equations of the P.T. The loads and moments are assumed known and the required deflections, slopes, and moments are calculated from the fundamental beam equation [12]

$$EI \frac{d^2 y}{dx^2} = -M$$

where

E = Young's modulus, lbs/in.²

I = moment of inertia of cross section, in.⁴

M = moment due to loading, in.-lb

$\frac{d^2 y}{dx^2}$ = approximate expression for curvature, $\frac{1}{\text{in.}}$.

The complete deflection, slope, and moment equations are found by considering the beam with only the left moment applied, then with the right moment only, and finally with each load (P_1 , P_2 , --- P_6) in turn applied. The results of the above analyses are then superimposed for the final solution. Notice that Equation (6) must be evaluated in each of the four sections of the beam for each individual solution.

The deflection, slope, moment, and shear for the insulating jacket, considered as the beam on three supports, are found in a manner similar to the P.T. (beam on two supports). Since the deflections of the two beams must be the same at the buttons (points of contrast), a 6 x 6 matrix equation is formed for the solution of the unknown loads (P 's). The now known loads are substituted into the general deflection equation Δ_c evaluated.

This value of Δ_c is compared to Δ_a (assumed). An iteration procedure followed if they did not agree within the specified limits.

A computer program was written to perform the necessary computations; however, final results have not yet been obtained. This computer program is

general in form and therefore may be used to solve simple beam problems with slight modification.

The complete solution is quite involved and only principles have been given. Work is continuing to better account for the effects of the eccentric annuli, and a new mathematical model which does not use semicircles is in progress.

7. ATR PRIMARY COOLING SYSTEM CHECK VALVE

BEARING ANALYSIS

(N. R. Byers)

Bearing failures in the 20-inch check valves were encountered during the final stages of testing of the primary coolant system of the ATR. This report includes a force and stress analysis thereby providing means of evaluating bearing designs. The analysis may be applied to any kinematic system which can be reduced to an equivalent slider-crank mechanism and also may be extended to fatigue evaluation of bearings.

The maximum loads are imposed on a check valve when one primary coolant pump is lost in a three-pump operation. If, as happened, the check valve sticks open, reversed flow could build up to such a value that the valve would be forced closed; destructive water hammer might result. Bearing galling could and did cause check valve sticking when similar materials were used and therefore must be prevented by adequate bearing design. The solution plan in this analysis consists of a conventional velocity, acceleration, and force analysis (static and dynamic) for finite positions through one complete cycle of flapper movement. From the preceding force analysis a bearing stress analysis is made in order to determine when galling is imminent. The check valve shown in Figure III-15 was reduced to its kinematic equivalent which is a simple slider-crank mechanism. This is shown with the appropriate externally applied loads in Figure III-16. The conventional force analysis will not be included here [12] but only the analysis of bearing stresses; however, an iterative technique was found and incorporated whereby frictional forces in all joints were accounted for. Since the equations for differential pressure acting across the flapper and also across the damper piston (see Figure III-15) were not known, a best fit polynomial with 90% reliability was written using experimental values.

The stress analysis follows procedures found in Theory of Elasticity [13] and Stress Distribution and Strength Condition of Two Rolling Cylinders Pressed Together [14].

The stresses were calculated from the following equations [14]:

$$\sigma_x = \frac{-2q}{\pi b} e^{-\alpha} \sin \beta \frac{-2q}{\pi b} \sin \beta \sinh \alpha \left(1 - \frac{\sinh 2\alpha}{\cosh 2\alpha - \cos 2\beta} \right)$$

$$\sigma_y = \frac{-2q}{\pi b} e^{-\alpha} \sin \beta \frac{+2q}{\pi b} \sin \beta \sinh \alpha \left(1 - \frac{\sinh 2\alpha}{\cosh 2\alpha - \cos 2\beta} \right)$$

Fig. III-15 Sectional view of modified 20-inch check valve.

$$\sigma_z = \frac{-2q}{\pi b} \frac{\lambda}{(\lambda + \mu)} e^{-\alpha} \sin \beta$$

$$T_{xy} = \frac{-2q}{\pi b} \sinh \alpha \sin \beta \left(\frac{\sin 2\beta}{\cosh 2\alpha - \cos 2\beta} \right).$$

$$\sin\beta = \sqrt{\frac{(b^2 - y^2 - x^2) + \sqrt{(b^2 - y^2 - x^2)^2 + 4b^2 x^2}}{2b^2}} \quad (3)$$

$$\sin\alpha = \sqrt{\frac{-(b^2 - y^2 - x^2) + \sqrt{(b^2 - y^2 - x^2)^2 + 4b^2 x^2}}{2b^2}} \quad (4)$$

Notice that this is evaluated at $x = 1.0b$. A computer program which mapped the stresses in all positions as a function of flapper position is included and by a slight extension may be used in a fatigue evaluation of the bearings.

Fig. III-18 Section of fuel element.

8. EQUIVALENT PRESSURE LOADING OF ATR FUEL PLATES (N. R. Byers)

To ensure reliability in operation, ATR fuel elements are subjected to hydraulic testing in simulation of reactor conditions except for radiation effects. An element is tested at 350°F for six hours; however, the effective test time of six hours is extended to 12 due to heat-up and cool-down requirements of the test facility.

In order to reduce the costs of the extended test time while maintaining equal fuel element reliability, this analysis was commenced to investigate equivalence of physical properties and pressure loading of fuel element to allow testing at lower temperatures.

The expressions for forces and moments, which occur in the fuel element, are programmed with differential pressure, ΔP , and material physical properties (ie, Young's Modulus, Poisson's Ratio, etc) as variables. In this manner the analysis is extended to more general considerations and with slight modifications may be used for thermal stress considerations in fuel elements. The minimization of strain energy is the basic approach used in the analysis where the strain energy stored per unit length of element cross section (Figure III-18) is evaluated in terms of the forces and moments occurring as shown in Figure III-19.

Since the fuel plate-side plate joint is roll swaged, no theoretical joint efficiency really can describe the bond; hence, an analysis which considers first a fixed edge plate and next a simply supported plate will bound the actual

Fig. III-19 Free body diagrams for energy method derivation.

condition. Notice that no moment may be transferred from the fuel plate to the side plate at that joint in the simply supported case.

In the fixed edge case, 63 equations for energy arise in the form of moments and force. The partial derivative of each equation is taken with respect to the moments and forces, set equal to zero, and the resulting 63 x 63 matrix equation evaluated in terms of the variable, ΔP .

The same method of solution is used in the simply supported case except, of course, a 42 x 42 matrix since the joint moments are all zero. The two resulting matrix equations are evaluated assuming temperature-dependent fuel element material properties. By selection of these properties at appropriate temperatures, the two-test pressure may be found in order for the equivalent forces and moments to result.

Since the pressure varies with fuel element axial distance, the energy expression (1) is evaluated over the entire length of the element. However, since ΔP is a discontinuous function of axial distance, Equation (1) must be evaluated by sections in which ΔP is continuous.

The expression for pressure drop versus axial distance is approximated by a best fit polynomial, with 90% reliability in each discontinuous section.

The basic energy equations have been derived and their evaluation is in progress[12].

9. ATR NR SIMULATED PRESSURE TUBE - THERMAL JACKET

VIBRATION EXPERIMENT

(E. H. Porter, N. R. Byers)

When the ATR primary coolant system is flowing at steady state design conditions, the NR pressure tube - thermal gas jacket system is excited, resulting in a complex vibration of the pressure tube and jacket. The pressure tube vibrates against the inside of the gas jacket in a semirandom fashion. The effect of this vibration continues to be the object of a surveillance program designed to verify experimentally the extent to which the gas jacket wall thickness may be reduced as a result of wear from the impacting pressure tube centering shoulder.

Local failure of the insulation can present a hazard if the resultant pressure tube thermal transient is sufficient to cause its failure followed by voiding of the flux trap annulus which would have a positive reactivity effect. The overall effect upon the reactor depends upon the rate at which the flux trap annulus is voided; the final safety analysis report presents results for the instantaneous voiding of all the coolant volume within the flux trap baffle. It is calculated that about a third of the core may melt. Therefore, it is prudent to determine experimentally the margin that can be expected to be obtainable during ATR operation.

There is no literature available by which the effects of fatigue and embrittlement (caused by the impact of vibrating tubes) on crack initiation and propagation may be evaluated with the degree of certainty that is necessary. However, it has been definitely established that materials will catastrophically fail (by crack initiation and propagation) well below the allowable stress after repeated cycling. Therefore, experimentation which will conservatively evaluate reactor conditions is carried forth.

Two experiments are designed and operating. The first is a laboratory facility designed and constructed to simulate the in-reactor conditions and to evaluate the effect of the pressure tube gas jacket contact action. The facility consists of an array of four tube-jacket assemblies arrayed horizontally and simply supported at the ends by means of spherical bearings. The inner tube is vibrated (knocked) by a knocker mass. The configuration of the knocker duplicates that of the spacer pads on an NR pressure tube. The weight of the knocker is made to satisfy the energy input requirements for the design input frequency and amplitude. Computer calculations were made

to define the required relationships between frequency of knocking (input frequency), amplitude of knocker excursion at the input end, amplitude of knocker excursion at the center of the tube, length of "tuned" knocker bar, and mass of knocker, to the end that the kinetic energy input from the knocker to the tube would approximate that of the NR pressure tube-to-thermal jacket.

The second experiment employs a spare KAPL NR pressure tube - thermal barrier gas jacket assembly installed in a facility which reproduces the vibrations which pertain in the ATR at full flow conditions.

The nature of the flow-induced vibrations in an NR facility tube in the ATR was measured. Accelerometers were inserted into the pressure tube and attached to the gas jacket, and an in-vessel vertical scan of pressure tube acceleration (displacement) was obtained. Node points were determined experimentally and found to be in agreement with predictions based on theory. The NS, EW pressure tube and gas jacket time-dependent displacement was determined to be in the form of a beat. Oscilloscope displays were photographed for reference to be simulated on the test facility.

The spare KAPL C10 tube assembly was installed in the ATR rod storage area in a vertical position. The end fixity conditions which pertain in the ATR were duplicated on the C10 assembly.

A duplex electrohydraulic servoactuator system was designed and installed by the Reactor Engineering Branch of Idaho Nuclear and the Instrument Development Branch of Phillips Petroleum Co. It functions to provide lateral motion input to the gas jacket in the x-y axes at a common vertical elevation. The motion input is located axially at an approximate elevation of 77 feet. Each servosystem is programmed to provide a 7 cps fundamental with a 0.3 cps amplitude modulation. The two independent systems are phase programmed in such a way as to provide near-random motion to the gas jacket. With NS displacement on the oscilloscope vertical axis and EW displacement on the horizontal axis, the display will appear as shown in Figure III-20.

Fig. III-20 Phase relationship between x and y systems.

The amplitude will vary from zero to a maximum and back to zero. At the present time the facility has operated for approximately 280,000 minutes with few interruptions.

At an elevation of 77 feet 4-7/8 inches there is a centering shoulder on the pressure tube. At approximately 250,000 minutes the assembly was scanned ultrasonically and X-rayed in this area at 0, 45, and 90 degrees. The wear in

the gas jacket produced by the centering shoulder on the pressure tube contacting the inside of the gas jacket was less than 2.5 mils.

Two parameters had to be duplicated in the experimental simulation of reactor conditions: (a) two transmitted energy spectrums were the same and (b) the striking velocities were the same. The experimental system is shown in Figure III-19.

The maximum reflection, $\sigma_{\max}$ of the pressure tube (P.T.), measured in the ATR was used as a means of evaluating the fundamental frequency which was needed in the conservation of energy, the basic approach taken in the solution. The P.T. was assumed as a simply supported beam on two supports and the insulating jacket (I.J.) a simply supported beam on three supports (see Figure III-21). The necessary deflection equations were found. The fundamental beam equation

$$E I \frac{d^2 y}{dx^2} = -m \quad (1)$$

and the method of superposition were used for solution of the I.J.

In order to apply the method of solution, the assumption was made that the P.T. and I.J. contact in only one place and with a known deflection $\sigma_{\max}$. It was necessary to find where this load would be applied. When the load location was found the deflection equation for the P.T. was used to find the load ($P = 0.945$) necessary to operate $\sigma_{\max} = 0.011$ at Elev = 77 ft 6 in. (see Figure III-22). With P known, the equivalent mass, M_e , to be used in the mathematical model could be found from Newton's equation.

Fig. III-21 Equivalent beam for insulating jacket.

Fig. III-22 Mathematical model of insulating jacket.

Since $\ddot{X} = 0.32 \text{ g max}$ (measured value), then

$$M_e = \frac{\sum F}{\ddot{X}}$$

$$M_e = \frac{P}{386 \times l}$$

$$M_e = 0.007651 \frac{\text{LE-sec}^2}{\text{IN}}$$

where

$$X = \frac{PC \left[(l_1 + l_2)^2 - C^2 - l_1^2 \right]}{2l_1 l_2^2} = \text{Statically Indeterminate Reaction.}$$

The normal function for the general solution to the differential equation of motion for beam vibrations is

$$X_r = C_1 \sin kx + C_2 \cos kx + C_3 \sinh kx + C_4 \cosh kx$$

where the constants must be evaluated from the boundary conditions. The solution is resolved down to the evaluation of the expression

$$\phi(kl_1) = -\phi(kl_2) \quad .$$

A graph of ϕ and $-\phi$ as functions of kl is drawn. The solution (kl) is found by trial and error by arriving at a line parallel to the x axis which divides the curves ϕ and $-\phi$ in points whose abscissas are in the ratio of their lengths. From this method is found $k_1 = 2.4347$.

Then the frequency equation in air becomes

$$f_1 = \frac{\overline{k_1 l_1^2}}{2\pi l_1^2} \sqrt{\frac{E I g}{A \gamma}}$$

where

E = Young's modulus

I = moment of inertia

$$A = \pi(r_o^2 - r_i^2)$$

γ = density.

Since the P. T. has different cross sections (this is also true for the I. J.), an upper and lower boundary was made using the maximum and minimum I values. This was to reduce the complication introduced by the variable I value. The bounds were found as

insulating jacket $6.12 \text{ Hz} \leq f_1 \leq 8.46 \text{ Hz}$
 pressure tube $1.31 \text{ Hz} \leq f_1 \leq 4.47 \text{ Hz}.$

The measured value of the insulating jacket was found to be approximately 1.0 Hz. Since the P.T. $f_1 = 3.5$ and the I.J. $f_1 = 7.0$ Hz, the pressure tube impacting the I.J. could cause the I.J. to vibrate.

The problem solution is to be continued from this point since agreement has been found in the evaluation of frequencies.

10. REFERENCES

1. D. R. deBoisblanc and S. Cohen, Safety Analysis Report -- Advanced Test Reactor, IDO-17021 (Vols. 1 and 2) Rev. (April 1965).
2. M. L. Griebenow, Isothermal Hydraulic Measurements in Fuel Elements in ATR Core, IN-1158 (September 1968).
3. M. L. Griebenow and K. R. Dickey, MACABRE, IDO-17096 (November 1965).
4. M. L. Griebenow and K. D. Richert, MACABRE II, IN-1107 (September 1967).
5. Internal Idaho Nuclear Report.
6. D. R. deBoisblanc and R. S. Marsden, Safety Analysis Report for the MTR Phoenix Fuel Equipment, IN-1209 (to be published).
7. G. W. Gibson and M. J. Graber (eds.), Annual Progress Report on Reactor Fuels and Materials Development for FY 1967, IN-1131 (February 1968).
8. K. C. Sumpter, A Primary Study on the Compressive Properties of Composite Nuclear Fuel Plates, IN-1179 (April 1968).
9. G. N. Filmore, Fuel Heat-Up Simulation Progress Report, IN-1217 (to be published).
10. M. L. Griebenow, Ex-Reactor Heat Transfer Experiments Simulating ETR Fuel Element Geometry, PTR-732 (December 1964).
11. Hydraulic Institute, Pipe Friction Manual, 3rd ed., New York: Hydraulic Institute, 1961.
12. N. R. Byers, personal correspondence.
13. S. Timoshenco and J. N. Goodier, Theory of Elasticity, 2nd ed., New York: McGraw-Hill Book Co., Inc., 1951.
14. E. I. Radzimousky, Stress Distribution and Strength Condition of Two Rolling Cylinders Pressed Together, Univ. of Ill. Eng. Experiment Station Bulletin No. 408 (1953).

IV. REACTOR TRAINING

1. REACTOR SIMULATOR (G. E. Morris, R. A. McGuire)

The formal training of Reactor Engineers is accomplished under the supervision of the Reactor Engineering Branch in a ten week training session of lectures, problems, examinations, laboratories, and tours. The lectures include physics, reactor statics and kinetics, reactor control, reactor safety, metallurgy, instrumentation, reactor engineering, and health and safety. The tours and laboratories are designed to give the operator a chance to become familiar with different equipment related to reactors and an understanding of various types of reactors and facilities necessary to operate reactors.

For a number of years the need for more thorough training for Reactor Engineers has been apparent. The training required for such routine operations as scram recoveries is difficult because of the complicated procedures involved. Training in abnormal operating conditions is not permitted because of possible hazards to the reactor, experiments, and personnel. In order to handle the unusual and abnormal operating conditions safely and efficiently it is necessary that he be well trained in these procedures. The purpose of reactor simulators is to provide both routine and emergency operation training without the risks involved in using the reactors. Design of console and electronics began in July 1967 and the assembly started in January 1968. The ATR simulator console was completed in April and training started using basic instrumentation.

The ATR simulator console duplicates the appearance and relay logic of the ATR console (see Figure IV-1) with the exceptions of the digital computer inputs and safety redundancies. The control rods are simulated with a motor-driven potentiometer of special manufacture to generate the rod reactivity function. The neck shims and outer cylinder drives have digital counters geared with the potentiometers to generate the digital readout displays. The safety rods and servo regulating rods drive the dials direct. The motor-operated-rheostat system consists of two ganged potentiometers driven by a slo-syn pulse motor. One potentiometer is a two-decade logarithmic function and the second is a linear potentiometer for the meter readout.

The computer used in the reactor simulation is a partially expanded hybrid computer (see Figure IV-2). At present, there is no digital capability, and the analog section has two-thirds of full capacity. The computer has a removable patchboard and servo set potentiometers thus enabling a complete change of program in less than five minutes if there is no changing of diode function generators.

The computer program used for the simulation can be divided into five separate subgroups: (a) reactor kinetics using logarithmic model-three delayed neutron groups, (b) reactivity summation and power shift computation, (c) xenon-iodine poisoning, (d) heat transfer using open-loop constant-input temperature equations, and (e) outputs to instrumentation and instructors console.

Fig. IV-1 ATR Reactor Simulator Console.

2. NRTS STANDARDS CALIBRATION LABORATORY (G. E. Morris, R. A. McGuire)

The establishment of a standards calibration laboratory for the National Reactor Testing Station has been evaluated periodically during the past few years. In May 1967, Idaho Nuclear was assigned the task of establishing and operating a standards laboratory facility. From a survey of potential users of the facility it was determined that 22 parameters should be covered and that an operating staff of six personnel would be needed. Design for the facility began in April and was completed in June, 1968. A vibration survey of the site made by the U. S. Coast and Geodetic Survey indicated that vibrations were less than 20 micro g's acceleration between one and one hundred cycles per second. This provides exceptional stability for calibration work.

Fig. IV-2 Reactor Simulator Computer.

V. CRITICAL FACILITIES

1. ATR STARTUP, ZERO-POWER EXPERIMENTS, AND COMPARISON WITH ATR CRITICAL FACILITY^[1] (J. W. Henscheid, N. C. Kaufman, J. L. Durney, J. B. Willis)

The first critical fuel loading in the ATR was attained July 2, 1967, using the same fuel elements and the same stepwise loading sequence used previously in the ATR Critical Facility (ATRC). The subsequent zero-power test program, which had also been performed previously in the ATRC, demonstrated adequate nuclear correspondence between the two reactors.

The positions of the outer shims for criticality with the full fuel loading of 39 kg U-235 indicated that the excess reactivity in the ATR is only 0.3\$ less than it is in the same fuel loading in the ATRC. Subsequent shim calibration experiments indicate that this apparent difference in excess reactivity is due to slight physical differences between the outer shim hafnium plates in the two reactors.

In general, the reactivity worths of the ATR outer shims and neck shims are about 4% more than their ATRC counterparts, although the regulating rod worths are essentially the same in both reactors. The ATR hafnium safety rods are worth about 25% more than the ATRC cadmium rods, as predicted from prior measurements in the ATRC.

The kinetic parameter β/Λ , measured in two different shim configurations, is the same in both reactors. The void coefficients of reactivity in several typical fuel element coolant channels were also found to be nearly the same in the two reactors.

Comparisons of ATR and ATRC power distributions were generally quite good. Lobe powers agree within 1.5%. Fuel element powers differ about 2% on the average. Thermal neutron flux and fission rate values in typical experiment positions inside the fuel annulus are generally about 5% lower in the ATR than in the ATRC.

2. REACTOR PHYSICS TESTS DURING THE ATR APPROACH-TO-POWER PROGRAM (N. C. Kaufman, J. L. Durney, J. B. Willis, J. C. Tappendorf)

The initial experiments of the approach-to-power portion (Part II B) of the ATR startup program have been completed at power levels up to 2.5 MW. Although the results of these experiments will be reported in detail at the completion of the startup program, the results thus far obtained are summarized below. The experiments being performed during Part II B are designed to provide engineering and reactor physics information to aid in ensuring safe reactor operation. The experiments thus far performed have further strengthened reactor physics correlations with the ATR Critical Facility (ATRC) and have provided data which were difficult to obtain in ATRC at low power but nevertheless enable extensions of ATRC work to high power ATR operation.

The composition of the ATR core during Part II B is essentially the same as that used in previous ATR studies at zero power, Part II A of the startup program [1]. However, the direction of rotation of the ATR outer shims has been reversed since this earlier work. Experiments thus far completed indicate that this reversal has been properly accomplished and that all resultant effects are those predicted from mock-up experiments in ATRC [2].

2.1 Zero-Power Temperature-Induced Reactivity Effects in ATR

Several of the static reactivity effects induced in ATR by changes in the coolant temperatures of the reactor and experiment loops were measured at nominally zero power. Particular reactivity effects studied with equilibrium temperature conditions were (a) the net effects of increasing the reactor coolant water temperature, (b) the net effect of increasing the temperature of each of five water-filled experiment loops, and (c) the effects of the temperature changes in (a) and (b) on the hafnium control elements -- safety rods, regulating rods, neck shims, and outer shims. Selected results of these measurements are summarized in Figure V-1 and Tables V-1 and -2.

With the water temperatures of six experiment loops maintained at ambient, the inlet temperature of the reactor was increased stepwise to 101, 130, and 150°F using the heat input of the primary coolant system pumps and adjusting temperature with secondary coolant flow. The net reactivity effects induced by these temperature changes, as deduced from resulting reactor periods, are graphically shown in Figure V-1. These results, when corrected for experiment loop effects, are in good agreement with the isothermal measurements made in ATRC and reported in Section V-7.

With the reactor water temperature maintained 150°F (which is characteristic of the mean reactor coolant temperature near full power), the temperatures in the SW, S, N, NW, and SE experiment loops were sequentially increased from 95 to 450°F. The resultant reactivity effects, deduced from the resultant change in critical shim positions, are presented in Table V-1 and listed in the order of measurement. The effects for loops with flux trap fillers (SW, S, N) are greater than for loops without fillers (NW, SE). Superimposed on these effects is a progressively larger second order reactivity effect with sequential increases in loop temperatures. This second order effect is indicative of a flux shift with an associated negative reactivity effect, as observed in detail in subsequent shim and rod worth measurements.

During the coolant temperature changes discussed above, the resultant changes in the reactivity worths of the ATR safety rods, regulating rods, neck shims, and outer shims were measured, using rod-drop and detailed shim calibration techniques. The results of these measurements are summarized in Table V-2.

Fig. V-1 Reactivity change due to increasing the ATR reactor water temperature, excluding NW, N, C, SE, S, SW experiment loops.

TABLE V-1

REACTIVITY EFFECTS DUE TO INCREASING
EXPERIMENT LOOP TEMPERATURES [a]

Experiment Loop	Reactivity Effect of Increasing Loop Water Temperature From 95° to 450°F [b]
SW	0.190 ± 0.011 \$
N	0.191 ± 0.011 \$
S	0.184 ± 0.010 \$
NW	0.144 ± 0.009 \$
SE	0.125 ± 0.009 \$

[a] All outer shims at approximately 65°, both regulating rods withdrawn, No. 1 neck shims partially withdrawn, and all other neck shims inserted.

[b] The indicated errors are estimates of one standard deviation.

The results in Table V-2 have much in common since all shims and rods utilize hafnium absorbers. Increasing the temperature of the reactor water from ambient to 150°F resulted in a small (approximately 2%) increase in the reactivity worths of all shims and rods. However, when the temperatures of the five experiment loops were increased to 450°F, much larger effects were observed (up to 11%). These large changes are principally due to an apparently general shift in the relative neutron distribution in the reactor which resulted from the localized loop temperature changes. When compared with the neutron distribution existing prior to increasing loop temperatures, the relative neutron density appears to have been depressed in the NE and C lobes (which contained no loops at high temperatures), to have remained unchanged within the flux traps of the NW, SW, and SE lobes, and to have been enhanced in the reflector surrounding those same three lobes.

2.2 Power Distribution Comparison

Detailed power distribution data were obtained in ATR for comparison with corresponding ATRC data. A similar comparison was made earlier during Part II A of the ATR startup program which demonstrated the nuclear correspondence between ATR and ATRC [1]. However, the outer shim withdrawal direction has since been reversed from the original design in order to improve the power distribution [2]. In addition to the reversal, all eight of the inboard shim cylinders were repositioned to obtain a reactivity null at their zero position. Because of these changes in both the ATR and ATRC outer shims, the power distribution comparison was repeated. Both the gross and the detailed power distribution comparisons obtained from the repeat measurement data show that the ATR-ATRC nuclear correspondence is still good.

The comparison of the ATR and ATRC power distributions normalized to 250 W is shown in Figure V-2. On the average, fuel element powers agree within about 1%, the largest difference being only 4%. With the exception of fuel

TABLE V-2

REACTIVITY WORTHS FOR THE ATR SAFETY RODS, REGULATING RODS,
NECK SHIMS, AND OUTER SHIMS WITH DIFFERENT REACTOR AND LOOP
TEMPERATURE CONDITIONS

Rods or Shims	Reactivity Worth (Dollars) [a]		
	Reactor @ 70°F Loops [b] @ 80°F	Reactor @ 150°F Loops [b] @ 95°F	Reactor @ 150°F Loops [b] @ 450°F
Safety Rods [c]	12.9 ± 0.1 } [d] 12.9 ± 0.1 }	---	13.1 ± 0.1 } [d] 13.2 ± 0.1 }
Reg Rods [e]			
SW	0.515 ± 0.01 0.555 ± 0.01	0.520 ± 0.01 0.572 ± 0.01	0.505 ± 0.01 0.507 ± 0.01
No. 1 Neck Shims [f]	1.67 ± 0.03	1.71 ± 0.03	1.55 ± 0.03
S34 Outer Shims [f]	1.66 ± 0.03	1.71 ± 0.03	1.92 ± 0.03

[a] The indicated errors represent estimates for one standard deviation of the measured values.

[b] The loops at temperature were NW, N, SW, E, SE. The C loop remained at 80°F. The NE, E, and W loops were always at the same temperature as the reactor water.

[c] All neck shims inserted; both reg rods withdrawn; all outer shims at 76.0°F.

[d] To obtain an estimate of reproducibility, the measurement at each temperature was performed twice.

[e] All outer shims inserted; neck shim sets Nos. 2, 3, 5, and 6 fully withdrawn; reg rods and No. 1 neck shim set partially withdrawn; NW4 and SE4 shims inserted.

[f] All outer shims at 355°F; No. 2 neck shim set fully withdrawn; neck shim sets No. 6 and 5 and NW4 and SE4 fully inserted; reg rods fully withdrawn; neck shim sets Nos. 1 and 3 partially withdrawn.

positions 5, 6, 7, and 8, those positions where differences are greater than the estimated standard deviation of 1.5% are generally scattered and are probably not real. The differences greater than 1.5% in the NE lobe (fuel positions 5 through 8) are apparently due to a slight difference in shim position between the ATR and ATRC irradiations. Near the beginning of the ATR irradiation, there was an inadvertent regulating rod insertion and subsequent outer shim movement which was difficult to duplicate exactly during the ATRC irradiation. However, ATRC irradiations indicate that at least part of the difference in the NE lobe is caused by this difficulty. It is believed that any remaining difference is not large enough to be significant.

Fig. V-2 Comparison of ATR and ATRC power distributions, both normalized to a power level of 250 W. In each fuel element position, the first and second numbers are element powers (in watts) for ATRC and ATR, respectively; the third number is the percent difference from ATR to ATRC. The numbers in the flux traps pertain to lobe powers. Both regulating rods were withdrawn, all neck shims were inserted, all outer shims were withdrawn to 77.7 degrees in ATRC and 80.8 degrees in ATR.

2.3 Fast Neutron Flux in Experiment Positions

Fast (>1 MeV) neutron flux data were obtained in a number of positions in the ATR which are representative of the different types of experiment irradiation positions available. These data were subsequently compared with data from the ATRC to determine how well the ATRC can be expected to predict the results of fast flux measurements in ATR. The comparison of the ATR and ATRC fast flux data at the horizontal midplane in six experiment loop positions and selected A-, B-, and H-holes is shown in Table V-3. In general, the agreement between ATR and ATRC data is good and indicates that the results of an ATR fast flux measurement can be predicted from ATRC data with an estimated standard deviation of 5%. However, to obtain the absolute fast flux, this number must be combined with an estimated standard deviation of 10% to account for the uncertainty in absolute fast flux obtained from nickel activation.

TABLE V-3

FAST (>1 MeV) NEUTRON FLUX AT CORE MIDPLANE NORMALIZED TO 250 MW

Position	$(10^{14} \text{ ATR } n/cm^2\text{-sec})$	$(10^{14} \text{ ATRC } n/cm^2\text{-sec})$	ATRC/ATR
Capsule Positions:			
A-10	3.55	3.61	1.02
A-11	3.77	3.81	1.01
B-4	1.77	1.71	0.97
B-5	1.84	1.84	1.00
H-6	2.84	2.97	1.05
H-8	2.77	2.91	1.05
H-10	2.85	2.87	1.01
H-12	2.80	2.93	1.05
Flux Traps:			
N	1.67	1.66	0.99
S	1.62	1.61	0.99
NW	1.63	1.57	0.96
SW	1.89	1.86	0.98
C	1.96	1.96	1.00
SE	1.61	1.60	0.99

2.4 Gamma Heat Measurements in Experiment Positions

Gamma heat measurements were obtained in a number of typical experiment irradiation positions at a power level of 2.5 MW using ion chambers. The purpose of these measurements was (a) to determine axial distributions of gamma heat in representative experiment positions and (b) to compare results with gamma heat values obtained in the ATRC at nominally zero power using thermoluminescent phosphors.

The measured axial gamma heat profiles were fitted by a nonlinear least squares program to the function $P \cos C(x-x_0)$, where P is the peak gamma heat value in W/g of graphite at 250 MW total reactor power, C is a fitted parameter in inches^{-1} , x_0 is the displacement of the gamma heat peak from core midplane in inches, and x is the number of inches from core midplane for which a gamma heat value in W/g of graphite at 250 MW total core power is to be computed. The values for the parameters of this function for representative irradiation positions are given in Table V-4. The locations of the irradiation positions in and around the core are shown in Figure V-3.

Shown in Table V-5 are the peak gamma heat values of several irradiation positions obtained in the ATR using ion chambers and in the ATRC using

TABLE V-4

VALUES FOR THE FUNCTION $P\cos C(x-x_0)$ DESCRIBING THE AXIAL GAMMA HEAT
DISTRIBUTION IN REPRESENTATIVE ATR POSITIONS AT 250 MW
TOTAL REACTOR POWER

<u>Irradiation Position</u>	<u>P</u> (W/g of graphite)	<u>C</u> (inches ⁻¹)	<u>x₀</u> (inches)
Northwest Flux Trap	11.8	0.056	-0.5
North Flux Trap	13.9	0.059	-0.3
Center Flux Trap	16.5	0.058	-0.6
East Flux Trap	16.6	0.055	-0.6
Southwest Flux Trap	13.5	0.057	-0.2
South Flux Trap	14.2	0.057	0.3
Southeast Flux Trap	14.5	0.057	-0.9
A-10 [a]	20.0	0.057	0.0
B-3 [a]	15.0	0.057	0.0
B-4 [a]	14.0	0.057	0.0
H-6, -7, and -8	19.0	0.057	0.0
I-6	1.4	0.053	-1.0
I-7	1.4	0.053	-1.7
I-8	1.5	0.056	-0.2
I-9	1.5	0.054	-1.4
I-10	1.4	0.051	1.5
I-11	1.4	0.053	-1.0
OS-2	0.45	0.052	1.2
OS-3	0.28	0.051	0.2
OS-4	0.32	0.051	0.1
OS-5	0.33	0.053	0.1
OS-6	0.30	0.048	1.1
OS-7	0.26	0.050	0.3
OS-8	0.10	0.048	-2.5
OS-9	0.11	0.048	0.6
OS-10 [a]	0.12	0.047	0.0
OS-11	0.11	0.046	-0.2
OS-12	0.097	0.048	0.9
OS-13	0.045	0.039	0.6
OS-14 [a]	0.043	0.040	0.0

TABLE V-4 (Contd.)

VALUES FOR THE FUNCTION $P \cos C(x-x_0)$ DESCRIBING THE AXIAL GAMMA HEAT DISTRIBUTION IN REPRESENTATIVE ATR POSITIONS AT 250 MW TOTAL REACTOR POWER

<u>Irradiation Position</u>	<u>P (W/g of graphite)</u>	<u>C (inches⁻¹)</u>	<u>x₀ (inches)</u>
OS-16	0.045	0.040	0.1
OS-17	0.041	0.037	-1.2
OS-18 [a]	0.023	0.038	0.0
OS-20 [a]	0.024	0.038	0.0
OS-21 [a]	0.024	0.038	0.0
OS-22	0.021	0.039	0.1

[a] The parameters, C and x₀, for these positions were estimated on the basis of those determined in nearby positions.

TABLE V-5

COMPARISON OF PEAK GAMMA HEAT VALUES IN THE ATR AND ATRC

<u>Core Position</u>	<u>Gamma Heat (W/g of graphite) [a]</u>		
	<u>ATR (ion chamber)</u>	<u>ATRC</u>	
		<u>(CaF₂:Mn)</u>	<u>(LiF)</u>
N Flux Trap	13.9	16.3	26.6
SW Flux Trap	13.5	15.7	28.2
C Flux Trap	16.5	18.0	31.5
I-6	1.4	2.13	1.7
I-7	1.4	-----	1.7
B-3	15.0	14.8	22.8
A-10	20.0	21.8	27.5
H-7	19.0	20.7	26.2

[a] All data extrapolated to 250 MW reactor power.

thermoluminescent phosphors, CaF₂:Mn and LiF. On comparing data from the two types of phosphors, it is apparent that the gamma heat values obtained from CaF₂:Mn agree more favorably with those obtained in the ATR using ion chambers.

2.5 Noise Analysis, An ATR Surveillance Program

Noise measurements have been made in ATR at nominally zero power with variable primary coolant flow up to full flow. The zero power neutron spectral density of the ATR was determined from these measurements. Since at-power spectral density effects are, to first order, superposed on the zero power, full-flow spectral density, these zero power noise measurements establish a reference which can be used as a diagnostic during the approach to power.

The data from the zero power low-flow measurement were analyzed using a cross-correlation technique to determine the break frequency, β/Λ . A value of 25.7 ± 0.3 Hz was determined from this analysis. This value compares favorably with the value, 24.9 ± 0.5 Hz, determined from similar measurements in the ATR Critical Facility.

The power spectral density obtained from the zero power measurements with variable primary coolant flow is shown in Figure V-4. The resonances that appear with increased flow were caused by vibration of the experiment in-pile tubes. This correspondence was established from individual accelerometer measurements of the in-pile tubes. Using the results of the accelerometer measurements it may be shown that the resonances at 4.2 and 7.0 Hz were caused respectively by vibration of the center and corner in-pile tubes. The cause of the resonance at 11.0 Hz is presently unknown but it is not believed to be due to in-pile tube vibration. The resonant frequencies of the in-pile tubes were so prominent in these zero power measurements as to make the spectral density without flow-noise sources unrecognizable except for the usual white noise associated with the detection process at high frequencies.

Fig. V-3 Cross-sectional diagram of the ATR core.

Fig. V-4 ATR zero-power neutron spectral density as a function of primary coolant flow rate showing effects of in-pile tube vibration.

3. POWER DISTRIBUTION IN ATR 7F FUEL ELEMENT (J. L. Durney)

Comparison of the power distribution in a radially zone-loaded 7F fuel element and in a uniformly loaded standard fuel element shows an 18% reduction in the core hot spot power generation with the 7F fuel element. The data obtained for this comparison also indicate that the 7F fuel element has little, if any, effect on the fission rate in adjacent loop experiments. Additionally, reactivity comparison of the two elements indicates that the 7F fuel element has only a slight positive reactivity effect ($\approx 5\phi$) relative to the standard fuel element.

Detailed power distribution measurements were obtained in three 7F fuel elements in fuel positions 17, 18, and 19 (see core diagram in Figure V-2) with the reactor operating in an unbalanced power division. (In this unbalanced power condition, the core hot spots would occur in these as well as symmetric fuel positions of the opposite high power lobe.) Simultaneously, similar data were obtained for comparison in standard fuel elements in the symmetric fuel positions 37, 38, and 39. These data, normalized on the basis of lobe power, were used to calculate the relative powers generated in each fuel plate. The data shown in Figure V-5 for a typical position indicate a significant change in the power distribution within the 7F fuel element relative to the standard fuel element. However, the total element power is changed by less than 3%.

The ratio of the peak power density (W/cm^3) to the core-average power density at the core hot spot is considerably reduced in the 7F fuel element. The value of this ratio at the right-hand edge of the fuel in plate 19 of fuel position 17 is 2.63 for the 7F fuel element and 3.22 for the standard fuel element.

Fig. V-5 Relative power per plate for the 7F and standard fuel elements in fuel position 17.

4. PRELIMINARY MEASUREMENTS IN ATR CORE 2

MOCK-UP IN ATRC
(J. C. Tappendorf, D. A. Giluck)

Power distribution and reactivity measurements have been made in the ATRC mock-up of ATR Core 2 in a nominally 50-50-50 power division, both with and without simulated xenon equilibrium. The purpose of these measurements was fourfold: (a) to determine the shim configuration for a nominally 50-50-50 power division, (b) to determine any changes in power distribution in converting

Core 1 to Core 2, (c) to determine the effect of equilibrium xenon poisoning on the power distribution, and (d) to determine the excess reactivity in Core 2.

In summary, it may be noted that the power distribution in Core 2 has not changed significantly from that in Core 1. The excess reactivity in the Core 2 mock-up is about 12.5\$, and the reactivity held down by the boron in the fuel elements is 11.0\$. The data from these measurements should be considered preliminary since it is quite probable that a number of revisions will be made in Core 2 before it actually operates in ATR. However, since subsequent revisions are not expected to be very large, the general conclusions drawn from these data are not expected to change.

4.1 Description of Core 2

The major changes from the ATRC mock-ups of Core 1 and Core 2 are the addition of flux trap filler plates on the safety rod followers in the NW and SE flux traps and the addition of five loop experiments in the NW, N, SW, S, and SE flux traps.

Another small difference between Cores 1 and 2 is in the fuel element boron distribution. Although the greatest difference in any one element is about 7% (about 0.3 g), the total difference in natural boron content between the two cores is only 3.8 g. The standard ATRC fuel elements were rearranged to match the Core 2 boron distribution; whereas, for the ATRC measurements on Core 1, the actual Core 1 elements were used.

All of the core changes required to transform Core 1 to Core 2 were mocked up except those in the I-holes. The reactivity effect of these changes in the reflector have been shown to be negligible.

4.2 Power Distributions in Core 2

Two power distributions were measured in the Core 2 mock-up: one in the cold, clean core; the second in the same core with simulated equilibrium xenon poisoning. The data from the cold, clean run have been compared (a) with earlier Core 1 data to show the changes in the power distribution due to changes in experiments and shim positions and (b) with data from the simulated equilibrium xenon run to show the effects of the added poison on the power distribution. In both runs, the power distribution in each fuel element was measured using a standard array of U-235 wires at the reactor horizontal midplane.

The cold, clean run of Core 2 was very close to a 50-50-50 power division with the outer shims nearly balanced. The NW, NE, C, SW, and SE lobe powers normalized to 250 W total core power were, respectively, 48.4, 49.4, 53.4, 50.2, and 48.6 W. The outer shim positions adjacent to the NW, NE, SW, and SE lobes were, respectively, 61.9, 65.6, 65.7, and 65.7 degrees. Both regulating rods were also withdrawn.

A comparison of the power distributions in Cores 1 and 2 is shown in Figure V-6. In this figure, the power in each lobe has been normalized to 50 W for the comparison. The Core 2 power distribution (relative to Core 1) is somewhat depressed around the outer extremities primarily due to the outer shims being inserted more (11.8 degrees on the average) to compensate for the increase in excess reactivity.

Fig. V-6 Comparison of power distributions in ATRC mock-ups of ATR Core 2 and Core 1 with each lobe normalized to 50 W. From top to bottom, the numbers in each location are: Core 2 element power (watts), Core 1 element power (watts), and the percent change from Core 1 to Core 2. For Core 2 both regulating rods were withdrawn, all neck shims were inserted, and all outer shims were withdrawn to 64.7 degrees (average). The shim configuration for Core 1 was the same except all the outer shims were withdrawn to 77.5 degrees.

The most significant change from the Core 1 power distribution was a depression in the inner plates of the fuel annulus about the NW and SE flux traps, a result of displacing water with aluminum filler plates, which reduces the source of thermalized neutrons to the inner plates of the fuel annulus.

The effect of the axially nonuniform loop experiments on the axial fission rate distributions in adjacent fuel elements was found to be negligible. Shown in Figure V-7 is a typical axial fission rate profile in water Channel 2 in a fuel element adjacent to a loop experiment. The curve through the data points was obtained by a nonlinear least squares fit of the data. There is a slight asymmetry in the profile due to the partially inserted safety rods. However, the value of the peak-to-average of the cosine-shaped fission rate distributions was found to be in excellent agreement with the previously measured ratio of 1.42.

In order to assess the effect of equilibrium xenon poisoning on the power distribution, the power distribution was remeasured in the Core 2 mock-up after 76 grams of boron in the form of boron strips were uniformly distributed in the water channels of the 40 fuel elements. The amount of boron used was based on an estimate of the reactivity effect of equilibrium xenon (5\$) at 250 MW.

The addition of 5\$ worth of poison required an additional 31.9 degrees (average) withdrawal of outer shims and the withdrawal of all four No. 5 neck shims for criticality. The actual outer shim positions adjacent to the NW, NE, SW, and SE lobes were 94.7, 97.5, 98.4, and 95.5 degrees, respectively. The NW, NE, C, SW, and SE lobe powers normalized to 250 W total core power were, respectively, 50.1, 51.1, 48.7, 50.3, and 49.8 W.

The power distributions in Core 2 with and without simulated equilibrium xenon poisoning are compared in Figure V-8. This comparison shows how the power distribution flattens as the outer shims are withdrawn to compensate for the added poison.

Azimuthal power profiles in water Channel 19 (the widest channel) were measured in individual fuel elements comprising a representative quarter core. (All data were normalized to 50-W lobe powers.) An increase in fission rate was observed in the outer portions of the fuel annulus during the xenon equilibrium run resulting from the additional withdrawal of the outer shims.

The withdrawal of the No. 5 neck shims in the xenon equilibrium run produced an increase in fission rate in fuel positions adjacent to these neck shims. This effect was most noticed near the side of water Channel 19 closest to the withdrawn No. 5 neck shim.

Fig. V-7 Axial fission-rate profile in the center of water Channel 2 of fuel position 36 and the shrouding of the NW KAPL experiment.

Fig. V-8 Core 2 power distributions with and without simulated equilibrium xenon poisoning with each lobe normalized to 50 W. From top to bottom, the numbers in each location are element power (watts) with simulated xenon, element power (watts) without simulated xenon, and the percent change due to xenon poisoning. For the case with simulated xenon, both regulating rods were withdrawn, all neck shims except No. 5's were inserted, and all outer shims were withdrawn to 96.6 degrees (average). For the case without simulated xenon, both regulating rods were withdrawn, all neck shims were inserted, and all outer shims were withdrawn to 64.7 degrees (average).

5. EFFECTS DUE TO EXPERIMENT CHANGES IN THE ATR (D. A. Giluck, J. W. Henscheid)

The relative effects on fission rates in the ATR core caused by some representative loop experiment changes have been measured in the ATRC. In all cases the results show that the effect of an experiment change or removal on the fission

rates in other experiments is almost negligible if the experiment change is compensated by adjacent shims.

In this study the effects were measured for experiment changes in inner, outer, and center loop positions. Three types of changes of various reactivity magnitudes in the loop positions were measured to simulate probable reactivity effects of ATR experiments. The combinations of these cases were necessary to determine the ability of the shims to compensate effectively for the respective loop changes. Typical of the effects measured were the following in the SW loop:

- (1) Removing an unshrouded, nonfueled experiment train (see Figure V-9)
- (2) Adding a 100 vol% hafnium shroud to a nonfueled experiment train (see Figure V-10)
- (3) Removing a 100 vol% hafnium-shrouded, nonfueled experiment train (see Figure V-11).

The center, SW, S, and SE flux traps contained mock-ups of typical ATR experiments while the N and NW flux traps contained water. The fission rate measurements in these positions were made at core midplane. Each train consisted of aluminum holders and specimens having an overall metal-water ratio of about 2.3 in the flow tube. The NE, E, and W flux traps were not monitored since dummy pressure tubes were located in these positions.

Each power distribution run was made in a nominally 50-50-50 power division. The comparisons were based on a normalization of the power generated by the 28 monitored fuel elements. The percent standard deviation in the fuel element powers is less than 1%, and in the flux trap fission rates it is less than 2%.

Fig. V-9 Percent changes in fuel element power as a result of removing an unshrouded, nonfueled experiment train from the SW loop position (-0.25%). In the flux traps, the first value is the percent change in fission rate in the flux trap whereas the second value is the percent change in lobe power. The outer shim positions before (upper) and after (lower) the experiment change are included to show the reactivity compensation for the experiment change.

Fig. V-11 Percent changes in power as a result of removing a 100 vol% hafnium-shrouded, nonfueled experiment train from the SW loop position (+0.05\$). In the flux traps, the first value is the percent change in fission rate in the flux trap whereas the second value is the percent change in lobe power. The outer shim positions before (upper) and after (lower) the experiment change are included to show the reactivity compensation for the experiment change.

6. A COMPUTER PROGRAM FOR THE ANALYSIS OF DATA FROM A PULSED-NEUTRON EXPERIMENT (N. C. Kaufman)

GRIFE II^[3], a program for the analysis of experimental data taken in pulsed-neutron experiments, was written in FORTRAN IV for use on the IBM-7040 computer. It may be used for experiments in either a critical or subcritical reactor and also for data with one or two exponential decays. The program features a complete error analysis including the significant but often

neglected covariance terms, x-y plots of the data with computed curves included, and routines to detect and reduce the influence of any bias in the experimental data. A value for the reactivity of a subcritical system is calculated using both the extrapolated-area-ratio method and the Garelis-Russel method. This program is operational, a typical processing time on the IBM-7040 being 1.1 minutes for each pulsed-neutron experiment.

7. ATRC ISOTHERMAL TEMPERATURE COEFFICIENT OF REACTIVITY (J. B. Willis)

The isothermal temperature coefficient of reactivity of a 39-kg core with water-filled flux-trap experiment positions and a 50-50-50 power division was determined from measurements in ATRC to be $+0.40\text{¢}/^{\circ}\text{F}$. This value compares favorably with the semi-empirical value, $0.45\text{¢}/^{\circ}\text{F}$, determined for ATR from data taken during Part II B of the ATR startup program.

The effects of the negative temperature coefficient of the ATRC fuel annulus and the slightly larger positive temperature coefficient of the remainder of the reactor were noted qualitatively during the measurements^[4]. The sequence of the measurement was as follows:

- (1) All regions of the reactor were in thermal equilibrium and the reactor was critical.
- (2) A steam valve was opened allowing steam to sparge into the pool water, thereby increasing the temperature of the pool water.
- (3) A mixer circulated the pool water up through the core. Since the fuel element water channels offered the least impedance to flow, the temperature of the water in those channels was first to increase. A corresponding decrease in the power level indicated a negative fuel-element temperature coefficient.
- (4) The steam valve was then closed, and the temperature distribution in the core and reflector was allowed to reach an equilibrium.
- (5) The negative reactivity effect of heating the fuel element region was exceeded by the positive reactivity effect of heating the remainder of the reactor, thereby causing the reactor to become supercritical and the power level to increase.

The reactivity associated with a given temperature increase was determined from the observed positive asymptotic period resulting when the temperature of the critical reactor was increased. This dependence is shown in Figure V-12. A value of $+0.40\text{¢}/^{\circ}\text{F}$ for the isothermal temperature coefficient of reactivity may be determined from a linear approximation to the curve.

Fig. V-12 The reactivity effect of increasing the temperature of ATRC.

8. ETR-ETRC FISSION RATE DISTRIBUTION COMPARISON

(E. E. Burdick, A. L. Bowman)

A comparison in the fission rate distribution between ETR and ETRC was made in the Cycle 94 loading. The reasons for these comparative measurements were as follows: (a) Emphasis on flux stabilization has increased the need for more accurate knowledge of the fission rate distributions in the ETR. (b) The ETR fuel element U-235 content was changed from 400 to 500 g in Cycle 83. (c) Previous comparisons^[5,6] indicate that fission rate distribution differences have existed between the two reactors since their initial start-ups in 1957.

In order to evaluate any differences in fission rate distribution found between the two reactors, a large number (3800) of U-235 fission rate monitor wires were used for these measurements. These were placed on aluminum wands which were carefully fitted to provide accurate wire placement within each fuel element. Care was taken in preparation for and during the flux runs to duplicate operating conditions between the two reactors. The same fuel element loading was used, and the ETR "as-loaded" experiments were duplicated as closely as possible in the ETRC. Since there is a basic reactivity difference between the ETR and ETRC cores, boron polyethylene tapes were inserted into some ETRC fuel element coolant channels to attain duplicate control rod heights.

Comparison measurements consisted of the following:

- (1) Twenty-six-wire maps at the horizontal midplane and nine-wire maps at ± 7 and ± 14 inches from the horizontal midplane in each fuel element
- (2) Vertical traverses next to 12 experiments (one wire per inch) and next to the partially withdrawn rods (7, 9, and 14).

The ETR run was accomplished first so that the ETR control rod positions (approximately 15.7 inches) could be duplicated in the ETRC, and all late experiment changes could be correctly mocked up. During the run the ETR loop temperatures were between 120 and 200°F and reactor inlet water temperature was 80°F. The ETRC core and loop temperature was 67°F.

The data from the horizontal maps and vertical traverses allowed comparisons of fuel element power generation, five-level fuel element power densities and experiment neutron source (ENS) values^[7], and vertical profiles. Figure V-13 is a comparison of the power generated in each element in both the ETR and ETRC normalized to core power. The values shown are based on 62 wires that were irradiated in each element. This figure shows that the largest deviations occur in rows 7, 8, 9, 13, and 14. ETRC fission rates average about 12% higher than ETR fission rates in rows 13 and 14 and about 8% lower in rows 7, 8, and 9.

Power densities and ENS values are compared in Figure V-14. This figure shows some of the relatively large differences occurring in the experiment positions. Figure V-15 is similar to Figure V-14, except the data in the latter are normalized to fuel element power so that the large deviations in fuel element power as seen in Figures V-13 and -14 are suppressed in order to reveal a vertical shift in the flux profile. In practically all elements, the vertical fission rate profile is shifted such that the ETRC fission rates are higher in the top half of the core. In addition, the comparisons of the vertical profiles support the conclusions drawn from Figure V-15, that the ETRC fission rate profiles are higher than those from the ETR in the top half of the core. Furthermore, some specific differences next to experiments in F6, H10, J10, J12, and L12 indicate that there may be some unexplained nuclear differences between the actual experiments in the ETR and their ETRC mock-ups.

Prior comparisons also indicate flux distribution differences between ETR and ETRC. Since the initial ETR-ETRC comparison^[5], other comparisons were made in the Cycle 27^[6], 51B, and 79 loadings. Experiment configurations and loadings, fuel loadings, and rod heights were considerably different for each of the above comparisons. However, an attempt was made to maintain similar operating conditions between ETR and ETRC for any one comparison. Measurement positions in Cycle 27 and 51B were at the geometric center in each fuel element. In Cycle 79, measurements were made at the geometric center in all elements plus 11 vertical profiles.

Figure V-16 shows a comparison of the four most recent comparisons. This figure demonstrates that there is some similarity between past and present comparison runs particularly for Cycles 27, 51B, and 94. It should be pointed out that data errors are about $\pm 3\%$ for the earlier cycles and less than $\pm 1\%$ for Cycle 94. Smaller errors for Cycle 94 are attributed to more wires available per calculation and better flux wand positioning.

	E	F	G	H	I	J	K	L	M	N
5	<div></div>	1.18 1.17 -1	1.35 1.34 -1	0.87 0.85 -3	1.59 1.57 -1	0 0 0	1.28 1.30 +1	1.21 1.15 -4	1.11 1.08 -3	1.07 1.07 -1
6	1.04 1.07 +3	<div></div>	<div></div>	1.83 1.72 -6	<div></div>	1.78 1.69 -5	<div></div>	<div></div>		1.11 1.11 0
7	1.20 1.21 +1	<div></div>		1.22 1.13 -7	2.19 2.03 -7	0.55 0.50 -9	1.88 1.73 -8	<div></div>		1.42 1.44 +2
8	1.48 1.46 -1	<div></div>		2.27 2.11 -7	2.44 2.26 -8	<div></div>	2.31 2.11 -9	<div></div>	1.76 1.66 -5	<div></div>
9	0.81 0.80 -1	1.70 1.62 -4	0.59 0.55 -7	2.26 2.08 -8	1.51 1.39 -8	2.63 2.40 -9	1.33 1.23 -7	2.11 1.95 -8	1.15 1.12 -3	1.93 1.99 +3
10	1.46 1.51 +4	<div></div>	1.96 1.97 0	<div></div>	2.48 2.33 -6	<div></div>	2.15 2.07 -4	<div></div>	1.95 1.94 -0	<div></div>
11	0 0 0	1.80 1.87 +4	1.18 1.20 +1	2.40 2.37 -1	1.35 1.31 -3	2.15 2.11 -1	0.53 0.51 -4	1.64 1.62 -1	0.96 0.96 +1	1.83 1.86 +2
12	1.55 1.75 +13	1.98 2.14 +8	<div></div>	2.32 2.40 +3	<div></div>	<div></div>	1.48 1.48 0	<div></div>	1.24 1.27 +2	1.27 1.35 +6
13	1.60 1.83 +14	<div></div>	<div></div>	1.42 1.52 +7	<div></div>	1.73 1.88 +9	0 0 0	<div></div>	<div></div>	0.85 0.94 +11
14	<div></div>	1.66 1.93 +16	1.95 2.20 +12	1.99 2.23 +12	1.97 2.20 +12	<div></div>	1.28 1.46 +14	1.02 1.12 +10	0.70 0.79 +13	<div></div>

Fuel Element

(position)
Control Rod

Experiment

Fig. V-13 Cycle 94 ETR and ETRC comparison flux run power distributions. Top and middle values are percent of total core power in each fuel element in the ETR and ETRC, respectively. Bottom values are percent differences from ETR to ETRC. Estimated data error is 0.5%.

In conclusion, data from these comparison measurements show that there are significant differences between the fission rate distributions of the ETR and ETRC. Differences in fuel element power are as large as 16%, and differences in ENS values are as large as 20% (+14-inch level in experiment position E14). The comparison also shows a vertical shift in the flux profiles such that the ETRC fluxes tend to be higher in the top half of the core. Causes for these differences are being investigated. It is quite likely that more experimental work will be needed before most of the prominent differences are explained. However, recent studies indicate the following:

	E	F	G	H	I	J	K	L	M	N
5	+7 +7 +1 -2 -6	+2 +3 0 -3 -6	+2 +2 0 -3 -5	5 +4 +1 -1 -6 -12	+11 +5 +2 -4 -15	6 0 0 0 0	+5 +5 +3 -1 -5	-1 -1 -3 -6 -11	-4 -1 -2 -5 -6	+4 +1 0 -3 -6
6	+13 +13 +2 0 -6	+6 +6 +1 -2 -6	+1 0 -2 -7 -10	+1 -2 -4 -10 -13	+3 0 -3 -9 -14	+3 +1 -2 -10 -14	+3 +1 -3 -8 -13	+2 +1 -3 -7 -10		
7	+5 +6 0 -1 -6	+1 -1 -4 -7 -7		1 -2 -4 -6 -10 -11	+2 -2 -6 -11 -14	7 0 0 0 -11 -15	+2 -1 -7 -12 -17			
8	+6 +1 -1 -3 -7			-8 -7 -7 -10 -10	-2 -4 -8 -11 -11	-2 -8 -12 -12	+1 -2 -6 -11 -13	+1 -5 -8 -10	+5 -1 -2 -5	
9	8 +5 +1 0 -3 -3	+6 -2 -2 -8 -9	9 0 0 0 -7 -6	-10 -6 -9 -10 -4	10 -8 -6 -8 -9 -5	-4 -5 -9 -12 -9	2 0 -2 -6 -11 -11	+4 -1 -7 -12 -16	11 +2 +1 -3 -5 -7	+6 +7 +2 +3 +1
10	+4 +3 +3 +8	+6 +4 +2 -3 -3	+11 +6 +1 -4 -7	-3 -1 -4 -6 -3	-17 -7 -7 -5 +3	-4 -2 -5 -8 -5	+3 +2 -1 -9 -9	+2 +1 -3 -6 -7	-2 0 0 -1 +1	+1 +3 +1 0 -1
11	12 0 0 0 0	+5 +7 +5 +2 0	3 +7 +5 +1 -2 -4	+5 +2 -2 -3 -5	13 -3 0 -3 -5 -2	+4 +4 0 -6 -5	14 0 0 0 -5 -5	+8 +3 -1 -3 -4	4 +2 +3 +1 -1 -2	-2 +4 +4 +1 -1
12	+22 +15 +14 +9 +8	+16 +10 +8 +6 +3	+11 +7 +4 +1 -2	+13 +6 +4 -1 -3	+9 +6 +2 -2 -3	+8 +8 +2 -2 -3	+3 +3 -1 -1 -1	+6 +3 0 -2 -3	+7 +5 +3 -1 -5	+11 +10 +7 +2 +3
13	+19 +15 +15 +13 +10	+19 +14 +13 +11 +8	+18 +12 +10 +7 +5	15 +14 +10 +8 +3 +1	+16 +14 +10 +5 +1	+19 +21 +9 +3 -2	16 0 0 0 0	+10 +10 +7 +6 +4	+11 +10 +8 +5 +3	+10 +14 +12 +9 +9
14	+20 +17 +15 +14 +11	+21 +19 +15 +14 +12	+23 +13 +12 +11 +10	+17 +14 +14 +8 +7	+15 +17 +12 +9 +5	+16 +18 +11 +8 +4	+13 +17 +12 +13 +14	+14 +12 +12 +8 +6	+14 +15 +12 +12 +11	+12 +14 +12 +10 +10

Fig. V-14 Percent differences between ETRC and ETR average fission rates in the fuel elements and control rods and ENS values in the experiments. Plus and minus indicate ETRC values higher and lower than ETR values, respectively. From top to bottom, values are from +14, +7, 0, -7, and -14 inches. All data are normalized to core power. Values at 0-inch level (midplane) are based on 26 wires and values at all other levels are based on nine wires. Estimated data error is less than 1%.

	E	F	G	H	I	J	K	L	M	N
5		+3 +4 +1 -2 -5	+3 +3 +1 -2 -4	5 +7 +4 +2 -3 -9	+12 +6 +3 -3 -14	6 +8 +6 +3 -5 -9	+4 +4 +2 -2 -6	+3 +3 +1 -2 -7	-1 +2 +1 -2 -3	+5 +2 +1 -2 -5
6	+10 +10 -1 -3 -9			+7 +4 +2 -4 -7		+8 +6 +3 -5 -9				+5 +3 +1 -2 -6
7	+4 +5 -1 -2 -7			1 +6 +4 +2 -3 -4	+10 +6 +2 -4 -7	7 +10 +6 +10 -2 -6	+11 +8 +2 -3 -8			+6 +4 0 -3 -7
8	+7 +2 0 -1 -6			-1 0 +2 -1 +2	+6 +4 +2 +1		+8 +6 +1 -3 -3		+6 +3 0 -3 -5	
9	8 +6 +2 +1 -2 -2	+10 +2 +2 -4 -5	9 +8 +8 +8 0 +2	-2 +2 -1 -2 +5	10 0 +2 0 -1 -1 +4	+6 +5 0 -3 0	2 +8 +6 +2 -4 -4	+13 +8 +1 -4 -8	11 +5 +4 0 -2 -4	+3 +4 -1 0 -2
10	0 0 -1 -1 +4		+11 +6 +1 -4 -7		-11 -1 -1 +1 +9		+7 +6 +3 -5 -5		-2 0 0 -1 +1	
11	12 +1 +3 +1 -2 -4	+1 +3 +1 -2 -4	3 +6 +4 0 -3 -5	+6 +3 +1 -2 -4	3 +1 +4 +1 -1 +2	+5 +5 +1 -5 -4	14 +4 +4 +4 -1 -1	+9 +4 0 -2 -3	4 +1 +2 0 -2 -3	-4 +2 +2 -1 -3
12	+7 +1 0 -4 -5	+8 +2 0 -1 -4		+10 +3 +1 -4 -6			+3 +3 -1 -1 -1		+5 +3 +1 -3 -7	+5 +4 +1 -4 -3
13	+5 +1 +1 -1 -3			15 +6 +2 0 -4 -6		+9 +11 0 -5 -10	16 +9 +11 0 -5 -10			-1 +3 +1 -2 -2
14		+4 +2 -1 -2 -4	+9 +1 0 -1 -2	+4 +1 +1 -4 -5	+2 +4 0 -3 -7		-2 +2 -3 -2 -1	+4 +2 +2 -2 -4	0 +1 -1 -1 -2	

Fig. V-15 Percent differences between ETRC and ETR average fission rates in the fuel elements and control rods normalized to fuel element power. Plus and minus indicate ETRC values higher and lower than ETR, respectively. From top to bottom, values are from +14, +7, 0, -7, and -14 inch levels.

Fig. V-16 Percent differences of ETRC from ETR fuel element powers (percent of core power) for Cycles 27, 51B, 79, and 94 comparison flux runs. From top to bottom, the values represent the cycles in the order listed above. Asterisks represent experiment positions during that cycle.

- (1) The shift in the vertical profiles is probably not due to any differences between the ETR and ETRC grid plate configurations. PDQ calculations indicate that the effects of these differences would be negligible.
- (2) Horizontal power distribution differences are apparently not caused by differences of experiment mock-ups in the ETRC reflector, which have negligible effects according to PDQ calculations.

- (3) Since the power or fission rate distribution differences between the two reactors have not been constant and since there have been significant experiment changes between comparison measurements, it is likely that some differences may be caused by inadequate ETRC mock-ups.
- (4) In Cycle 79, the comparison differences between ETR and ETRC are smaller than for any of the other three comparisons, indicating closer correspondence between the two reactors. However, rod position differences between the ETR and ETRC flux runs may have tended to minimize flux distribution differences between the two reactors that would otherwise have been more apparent.
- (5) If major experiment changes in the ETR are duplicated in the ETRC, the rather large differences between the two reactors will have minimal effects on cycle-to-cycle flux changes predicted by the ETRC.

9. ETR FLUX STABILIZATION PROGRAM (A. D. Mackley, A. L. Bowman)

The Engineering Test Reactor was designed for neutron irradiation of material samples in a very high neutron flux. Therefore, it became mandatory that these samples be located within the reactor core rather than in the reflector. With this arrangement, significant flux variations in the experiment samples result from (a) the insertion and removal of other experiments, (b) the movement of control rods, and (c) fuel burnup which causes the thermal neutron flux to increase in the core. Pursuant to a request by the AEC Idaho Operations Office, efforts were begun in 1965 to stabilize the neutron flux distribution in the ETR. The ETR Flux Stabilization Program can be conveniently divided into two categories: intercycle and intracycle. Intercycle flux stability requires that, within specified limits, the initial power distribution of all cycles be the same; whereas, intracycle flux stability requires that, within limits, the neutron flux distribution remain constant with time throughout each cycle.

9.1 Intercycle Flux Stabilization

The success of intercycle flux stabilization is measured by comparison of the flux distribution at the beginning of each cycle to a standard power distribution (SPD) that is established by experiment in the ETRC. Having established the SPD, it then becomes a cycle mock-up goal to arrange the fuel and experiment loading in such a way that the neutron flux supplied to each experiment facility is within $\pm 10\%$ of the flux supplied to the same facility according to the SPD. As a measure of the number of neutrons supplied to each facility, an experiment neutron source (ENS) has been defined^[7] in terms of the power generated in the surrounding fuel elements. Cycle mock-ups are carried out prior to each cycle in the ETR Critical Facility (ETRC), which is a full-scale nuclear duplicate of the ETR.

Commencing with Cycle 79, and continuing through Cycle 90, it was the objective to match the power distribution of each cycle to SPD-2/66^[8] or its

revision, SPD-2/66A^[9]. However, the nominal fuel loading of each ETR fuel element was changed, beginning with Cycle 91, from 400 to 500 g U-235. It then became necessary to change the core configuration and, hence, the flux distribution. A new standard power distribution (SPD-7/67) was established as a result.

Aside from the fact that the core configuration and power distributions were different for SPD-2/66 and SPD-7/67, they differed from each other in one other very important respect. SPD-2/66 was defined in terms of fuel element powers which were calculated from power density measurements made only at the reactor horizontal midplane with certain simplifying assumptions concerning the power distribution within each element. There was one number for each fuel element and each experiment facility. In contrast, SPD-7/67 was defined in terms of the measured power density at five levels (midplane, ± 7 and ± 14 inches). There was one number for each of five levels in each experiment facility, and one number in each fuel element.

The variations in the number of neutrons supplied to each facility from SPD-2/66 were presented in a previous report^[10]. The variations from SPD-7/67 for Cycles 91 through 97 are shown in Figures V-17 and -18. Also shown in the latter figure are the average intercycle changes for these cycles. By inspection of the figures, one can see that about 5% of the values are outside the limits ($\pm 10\%$) of the SPD objective. Table V-6 gives a cycle-by-cycle breakdown of the values that are outside the limits, and it can be seen that there is a tendency for the later cycles to have more ENS values outside the limits. This situation arises because, beginning with Cycle 94, the ETRC mock-up duplicated the (ETR) regulating rod position in its normal operating range. At the time SPD-7/67 was established, the position of the ETR regulating rod was simulated at its upper limit. This change represents an improvement in the accuracy of the mock-up rather than an actual change in the power distribution of the ETR. After correcting for the effects of this change, only 4% of the ENS values are outside the limits.

The real measure of success of the intercycle flux stabilization efforts is the average of the cycle-to-cycle ENS changes. By inspection of Figure V-18, one can see that this average is 5% or less in all but six positions of the core (4% of the values). Three of these positions are adjacent to rods 10 and 13 which are used alternately as regulating rods. Generally, rod 10 is inserted to the regulating range (about 30 inches) for regulation during the even-numbered cycles and rod 13 is inserted to the regulating range during the odd-numbered cycles. When either rod is not being used as the regulating rod, it is fully withdrawn. This cycle-to-cycle interchange causes unavoidable ENS changes at the +14-inch level in the vicinity of the regulating rods. The other three positions where the average cycle-to-cycle ENS changes exceed 5% are E5 at -7 and -14 inches and M7 at +14 inches. These positions are in or near extreme corners of the core and it is noteworthy that, even here, the average cycle-to-cycle ENS changes do not exceed 7%.

9.2 Intracycle Flux Stabilization

The transition from 400-g to 500-g elements referred to above was made in order to improve the intracycle flux stability. Reference 10 shows how this transition greatly reduced the net reactivity excursion experienced by the reactor during a normal cycle. Therefore, in order to gain the greatest advantage from

	E	F	G	H	I	J	K	L	M	N
5	-3 -3 -3 -6 -1 -3 -3 -6 -1 -3 -3 -6 -1 -3 -3 -6 -1 -3 -3 -6			5		6				
6		-1 -3 -3 -4 -2 -5 -5 -3 -3 -7 -7 -1 -6 -11 -6 -5 -7 -12 -6 -5	0 0 -6 -2 -1 -1 -3 +3 0 -1 -2 +6 -5 -9 -10 +2 -4 -11 -10 -3		+2 0 -1 -7 0 0 -1 -1 +3 +3 +3 +5 0 -5 -4 -5 +1 -5 -4 -6		-2 -6 -3 -8 -4 -6 -7 -5 -4 -6 -5 0 -4 -11 -6 -7 -5 -11 -9 -8		+9 -3 -1 -8 +2 -4 -3 -4 -1 -3 -1 -1 0 -5 -5 -4 +2 -9 -6 -6	
7		-1 -4 -5 -8 0 -3 -2 +3 -1 -2 +2 +7 -2 -7 -5 -1 -3 -9 -5 -1		1		7				
8						-4 0 -5 -12 0 0 -5 -2 -2 0 -1 +3 +1 -2 -3 0 +3 -3 -5 0		-6 -5 -5 -13 -1 -2 -4 -7 -2 -1 -3 -1 0 -5 -7 -4 0 -6 -8 -4		-3 -4 -3 -12 -1 -1 -2 -8 +2 +1 +1 -4 +2 -2 -2 -4 +1 -6 -5 -4
9	8		9		10		2		11	
10	+1 0 +1 -1 -2 -2 +5 +5 -3 0 +9 +7 -5 -7 +1 -1 -7 -10 -4 +3			+6 +4 +9 -2 +5 +2 +12 +11 +3 +3 +12 +12 +5 +1 +9 +9 +1 -4 +5 +6		+4 -7 -3 -9 +5 -2 0 +3 +2 -1 -1 +5 +5 +1 -4 +1 +4 +3 -6 +2		0 -12 -5 -10 +1 -5 -3 -1 +1 -1 -2 +4 -1 -2 -8 -1 -2 -1 -8 -4		-3 -3 -4 -9 0 +1 -1 -2 +3 +4 0 +2 +1 -1 -4 0 0 -3 -5 -3
11	12		3		13		14		4	
12			+4 +4 +2 +3 0 +2 +3 +5 +1 +1 +3 +4 +1 -2 +2 +3 -2 -5 0 +2		+4 +2 +2 +1 +2 +2 +3 +6 +2 +2 +3 +5 +2 -1 +2 +4 +1 -4 +1 +4		-4 -1 -5 -8 -2 +1 -1 0 -2 +3 +3 +5 -4 -2 -2 0 0 -5 -1 0		-3 -3 -4 -11 0 0 -2 -3 +1 +6 +2 +5 +1 -1 -3 0 -1 -4 -3 -3	
13	+1 -1 -4 -4 -1 -3 -4 -4 0 -3 -4 -5 -3 -5 -4 -8 -3 -5 -3 -8	+4 0 -2 +1 +1 0 0 +2 +1 +1 -1 +2 0 -2 -2 +1 -2 -1 -1 +1	15	+2 0 -3 +1 +1 0 0 +2 +1 2 0 +6 -2 -2 -3 +4 -1 -1 -1 +5		16		-1 0 -4 -1 0 2 -1 -6 +2 +5 +2 -1 -2 -1 -3 -5 +3 -1 -2 -6	+2 +2 -3 -12 +4 +6 -1 -5 +5 +7 +2 -1 +3 +2 -1 -4 0 +3 +1 -5	
14	0 -4 -6 -10 -3 -4 -8 -11 -3 -4 -6 -9 -3 -4 -7 -11 -3 -5 -3 -11					0 -1 -2 -4 1 -0 -2 0 +1 +1 0 +2 -4 -3 -3 0 -2 -1 -1 +1				+5 +3 -1 -9 +5 +9 0 -6 +5 +11 +3 -3 -4 +8 +2 +5 +3 +7 +7 -5

Fig. V-17 Percent deviation of ENS values from corresponding ENS values in SPD-7/67. Each column within a cell presents ENS deviations for a particular cycle. The left-to-right order corresponds to Cycles 91, 92, 93, and 94. The top-to-bottom order within a column corresponds to +14, +7, 0, -7, and -14 inches, with respect to the reactor horizontal midplane.

	E	F	G	H	I	J	K	L	M	N
5	-14 -12 -9 +3 -13 -12 -7 +3 -13 -11 -8 +4 -19 -14 -11 +6 -20 -13 -14 +7			5		6				
6		-11 -9 -6 +2 -10 -9 -6 +3 -10 -9 -6 +4 -15 -11 -9 +5 -17 -11 -10 +5	0 -3 -5 +3 +4 -1 -2 +3 +3 -1 +3 +4 -6 -3 -4 +4 -7 -4 -5 +4		-2 -5 -7 +3 +3 -2 -4 +3 +4 +2 +4 +2 -7 -3 -5 +3 -7 -4 -7 +4		+1 0 -5 +5 +3 -1 -2 +3 +5 +4 +3 +3 -6 -3 -5 +3 -8 -3 -7 +3	+2 +3 -3 +6 +3 +3 +2 +3 +2 +5 +4 +2 -3 +1 +1 +2 -6 0 -1 +3		
7		-6 -11 +13 +3 +2 -4 +4 +3 +5 +1 +3 +3 -3 -3 -4 +3 -5 -4 -7 +4		1		7				
8						+2 -10 -11 +7 +6 +5 +6 +3 +5 +7 +8 +2 -1 +5 +4 +3 0 +5 +2 +4		+1 -2 -1 +5 +3 0 +4 +4 0 +3 +7 +2 -4 -1 +2 +3 -5 0 +1 +8		-2 -7 -7 +5 0 -4 -1 +4 -2 -3 +2 +3 -5 -5 -1 +2 -6 -4 -1 +3
9	8		9		10		2		11	
10		-3 -6 -10 +2 +1 -4 -7 +3 +5 +2 0 +3 -5 -3 -8 +4 -5 -6 -11 +3		-9 -11 -11 +5 +8 +2 +4 +4 +9 +4 +9 +4 +4 +6 +5 +3 +2 +3 +1 +4		-11 -11 -13 +5 +6 +5 +5 +3 +3 +4 +6 +2 -2 +3 +2 +4 +1 +5 -2 +5		-2 -1 -1 +6 +3 +1 +4 +3 +2 +5 +6 +3 -3 -1 0 +3 -2 0 -1 +4		0 -4 +3 +4 +3 -1 +7 +3 +1 +1 +8 +2 -2 -2 +2 +3 -4 -4 -1 +2
11	2		3		13		14		4	
12			-4 +2 -3 +4 +1 0 -3 +2 +1 0 -2 +2 -2 -1 -3 +3 -2 -2 -3 +3		-23 -1 -5 +10 +3 0 0 +2 +2 +1 +1 +1 0 0 0 +2 0 0 -1 +3	-20 -6 -10 +7 +3 -1 0 +3 +7 +5 +6 +2 0 -1 0 +1 0 +2 -3 +3		-8 -2 -6 +4 -1 +1 +1 +2 +4 +7 +7 +3 0 +2 +1 +1 0 +2 +1 +2		
13		-2 -2 -2 +2 -3 -5 -4 +2 -3 -5 +1 -7 -8 -7 +2 -5 -6 -7 +2	0 +4 +2 +3 +1 0 +1 +1 +1 -1 0 +2 -2 -3 -1 +1 -1 -2 +1 +1	15	-1 +2 -2 +4 +2 0 -1 +2 +3 +1 0 +3 0 -3 0 +3 +1 +1 +1 +2		16	-8 -3 -8 +4 -1 -1 -3 +3 +3 +3 +3 +2 -1 -2 -2 +2 0 1 -1 +3	-5 -3 -8 +5 0 +1 0 +4 +1 +1 +4 +2 -1 -1 +3 +2 0 +2 +5 +4	
14	-5 -6 -4 +3 -6 -8 -5 +3 -6 -7 -5 +2 -8 -10 -7 +3 -6 -7 -8 +3					-4 -2 -6 +2 -2 -5 -5 +2 +4 0 -1 +1 +1 -4 -1 +2 +1 0 -2 +1				-3 -1 -9 +5 0 +1 -1 +5 0 +2 +3 +4 0 0 +6 +4 +2 +3 +10 +5

Fig. V-18 Percent deviation of ENS values from corresponding ENS values in SPD-7/67. Each of the three left-most columns within a cell presents ENS deviations for a particular cycle. The left-to-right order corresponds to Cycles 95, 96, and 97. The right-most column presents the average cycle-to-cycle ENS changes at each location for all Cycles 91 through 97. The top-to-bottom order within each column corresponds to +14, +7, 0, -7, and -14 inches, with respect to the reactor horizontal midplane.

TABLE V-6

SUMMARY OF ENS VALUES DIFFERING FROM SPD-7/67 BY MORE THAN 10%

Cycle Number	Number of ENS Values Outside Limits		Number of ENS Values Outside Limits After Correction for Regu- lating Rod Insertion	
	Number	Percent	Number	Percent
91	0	0	0	0
92	8	6	8	6
93	1	1	1	1
94	11	8	10	7
95	13	10	10	7
96	10	7	8	6
97	<u>6</u>	<u>4</u>	<u>4</u>	<u>3</u>
All cycles	49	5	41	4

this reduced reactivity excursion, the core configuration was changed to achieve optimum positioning of the control rods at the beginning of the cycle for minimum rod movement during the course of the cycle.

To evaluate the degree of intracycle flux stabilization, a continuously operating flux monitor system was installed in the ETR using self-powered neutron detectors (SPNDs) in and around the ETR core. Currently, there are 13 reactor positions being monitored with the SPNDs. In 11 of these positions there is a 10-cm SPND at each of five elevations: 0, $\pm 7\frac{1}{2}$, and ± 15 inches, with reference to the reactor horizontal midplane. The other two positions have 23-cm detectors centered at the midplane. Four of the five-detector trains are in-core, while the remainder are in the reflector.

Data from the SPNDs in core positions G13 and L13 are available for Cycles 92, 93, and 94. Figure V-19 shows the envelopes of the intracycle flux variations observed by all of the SPNDs in those two positions. During Cycle 94 the variations at the 13-1/4-inch level in L13 were abnormally great for reasons not yet determined. Therefore, the variations observed in that position for Cycle 94 are separated from the rest.

One may observe from Figure V-19 that, except for the single exception referred to, intracycle flux variations in G13 and L13 from beginning cycle fluxes are less than 10% during the first 5000 MWd of operation. The variations reach maximums between -14 and +17% at about 5500 MWd, coinciding in time with the transition from the 7-9-14 rod gang to the 6-12-16 rod gang. The large variations at this time result primarily from the fact that when a rod gang is partially withdrawn, the vertical flux distribution is skewed downward so that the peak flux is well below the reactor horizontal midplane. This condition prevails throughout the first 5000 MWd of each cycle. Toward the end of the cycle, when the rod gang reaches its upper limit, the skewness of the

vertical flux distribution is considerably reduced, thus causing significant flux changes. As the next rod gang begins to withdraw, the vertical skewness is partially restored, thus reducing the flux change. Only one cycle (93) has operated long enough to see this effect.

10. IMPROVEMENTS IN ARMF

OPERATION (D. W. Knight)

Significant improvements have been made in the operation of ARMF-I and -II. There has been a marked increase in the efficiency of the reactors (operating hours/number of inadvertent scrams). The operating efficiency during the first half of the year was 15 hours/inadvertent scram while the operating efficiency during the second half of the year was 37.5 hours/inadvertent scram. Much of the increase in efficiency resulted from the increased attention that has been given to the control system electronics.

Another area in which improvement has been made is reducing the error of reactivity measurements. The sources of error for reactivity measurements are (a) environmental effects (changes in canal water temperature), (b) the nonreproducibility of experimental capsule positioning, and (c) neutron noise (fluctuations in the number of neutrons available to the reactor caused by natural statistical fluctuations in the rates of neutron absorption and fission). While no improvement can be made in improving measurement accuracy by reducing the error from neutron noise, improvement has been made by reducing the effects of the other two sources of error.

A system was installed last year which permits the canal to be controlled to $\pm 0.006^\circ\text{C}$ over a 16-hour period. A remotely operated solenoid valve has been installed in the canal purge line. The solenoid valve is controlled from a micro-switch mounted on the ARMF-II temperature recorder.

A new system based on kinematic design principles has been built to position capsules. Three ball bearings are firmly fastened to a capsule. The capsule is positioned in the reactor by placing the ball bearings into three "V"-shaped grooves which have been milled into a positioning plate. The positioning plate is placed at the center of the reactor. The capsule makes six-point contacts (two for each ball bearing) thus absolutely positioning the capsule both horizontally

Fig. V-19 Envelopes of ETR intracycle flux variations after initial xenon equilibrium in nine monitors in G13 and L13 for Cycles 92, 93, and 94.

and rotationally. The reactivity measurement error (standard deviation) imposed by neutron noise for a five-minute measurement is approximately $5 \times 10^{-8} \rho$. The error of measurement which has recently been made using the new capsule design was $7 \times 10^{-8} \rho$. The reactivity worth of the capsule used to make the measurements was $500 \times 10^{-6} \rho$. A typical measurement error of capsules with a similar reactivity worth is $0.1 \times 10^{-6} \rho$.

11. REFERENCES

1. J. W. Henscheid, N. C. Kaufman, J. L. Durney, J. B. Willis, ATR Startup, Zero-Power Experiments, and Comparison with ATR Critical Facility, IN-1136 (December 1967).
2. J. W. Henscheid, N. C. Kaufman, J. L. Durney, A Summary of Data from the ATRC Experimental Program, IN-1053 (February 1967).
3. N. C. Kaufman, GRIPE II -- A Computer Program for the Analysis of Data from a Pulsed-Neutron Experiment, IN-1085 (September 1967).
4. E. E. Burdick and J. W. Henscheid (eds.), Advanced Test Reactor Critical Facility Safety Analysis Report, IDO-16950 Rev. (July 1964) p iv-14.
5. D. R. deBoisblanc and E. E. Burdick, A Comparative Study of the Engineering Test Reactor and Its Critical Facility, IDO-16498 (October 1958).
6. J. R. Huffman, D. R. deBoisblanc, J. E. Evans, L. A. Schmittroth, MTR-ETR Technical Branches Quarterly Report July 1 - September 30, 1960, IDO-16658 (February 1961) p 1.
7. E. H. Smith et al, ETR Operations Branch Progress Report for Cycle No. 91 - July 24 - October 1, 1967, IN-1140 (November 1967).
8. E. H. Smith et al, ETR Operations Branch Progress Report for Cycle No. 78 - January 16 - February 27, 1966, IDO-17171 (April 1966).
9. E. H. Smith et al, ETR Operations Branch Progress Report for Cycle No. 81 - May 22 - July 4, 1966, IDO-17201 (August 1966).
10. D. R. deBoisblanc, W. C. Francis, R. G. Fluharty, Nuclear Technology Branches Quarterly Report January 1 - March 31, 1967, IN-1117 (September 1967).

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