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Reactors-Power

ARGONNE NATIONAL LABORATORY

P. O. Box 299

Lemont, Illinois

REACTOR ENGINEERING DIVISION QUARTERLY REPORT

SECTION I

January, February, March, 1956

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REACTOR ENGINEERING DIVISION QUARTERLY REPORT

SECTION I

PART A. ADVANCED WATER REACTOR PROGRAM

SUMMARY - J. M. West

Experimental Boiling Water Reactor (EBWR)

Physics calculations have been performed for various combinations of the four types of fuel assemblies to be used in the EBWR core. Two thicknesses of plates (0.205 in. and 0.274 in., including two 0.020-in. cladding layers) are to be made of both natural uranium and uranium containing 1.44% U^{235} . Any given fuel assembly contains six identical plates. A total of 148 assemblies, 74 natural and 74 enriched, are to be fabricated. Various configurations of these fuel assemblies can be used to (1) change the critical size of the core, (2) change the power distribution in the core or (3) change the amount of reactivity corresponding to a given steam volume in the core. Physics calculations show that any uncertainties in the required critical mass are adequately covered by the number and variety of fuel assemblies, and that the changes in core characteristics possible with the different fuel assemblies should provide valuable information about the factors affecting maximum power density and stability in a boiling reactor.

Production of fuel assemblies by the Argonne Metallurgy Division is under way. A cladding failure occurred in one of the first 18 production plates tested in a 500F static autoclave, but the absence of any failures in subsequent tests has restored confidence in the fabrication procedure.

Designs have been essentially completed on reactor components external to the pressure vessel. Fuel handling and storage mechanisms have also been designed. These and other reactor items which were not adequately covered in previous reports are described in more detail. Many of these items are undergoing fabrication at the present time.

Testing of trial batches of boron-containing Type 304 stainless steel, prepared by various steel companies, has led to satisfactory preparation and fabrication procedures for alloys containing up to 2% boron by weight. The relatively low cost of boron-stainless steel, together with fabrication difficulties encountered with hafnium, led to a decision to make four of the nine control rods for EBWR of boron-Type 304 alloy. Five of the rods had already been made of hafnium. Boron-stainless steel is also to be used in the thermal shield of EBWR.

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Unanticipated complexities encountered in fabrication of the EBWR pressure vessel in the Babcock and Wilcox shops have led to an extension of the completion date. The shipping date is now estimated to be the latter part of May, 1956. This is not expected to delay the startup date of the EBWR, inasmuch as general construction work on the building is also behind schedule.

Massive concrete pours in the lower portion of the EBWR building have been completed. Backfilling around the gastight cylinder is carried out as pouring of concrete linings, pedestals, and floors progresses upward in the building. Installation of mechanical equipment will begin during the April-June quarter.

Specifications have been completed for forced circulation equipment to be installed after an initial period of operation with natural circulation. Bids are being sought on pumps to circulate 10,000 gpm with a net developed head of 50 psi. About 80% of the pressure drop will occur in external piping and the flow-regulating valve. Only about 10 psi of pressure drop will be needed in the reactor.

New heat transfer data, together with changes in fuel element geometry, have led to a slight downward revision in estimates of the steam content of the EBWR core. Steam is expected to occupy about 9% of the total water volume of the core when the reactor is operating at 20 megawatts with natural circulation.

Tests performed on the BORAX-III system have revealed a high oxygen (and hydrogen) content in the steam going to the turbine. The oxygen content is sensitive to water purity and pH, but present indications are that approximately 30 ppm of oxygen should be anticipated in the EBWR steam. Since this is on the order of 1,000 times as high as the oxygen content of steam in most power plants, there is considerable concern over corrosive effects on the turbine and condenser.

Although conclusive proof is not yet available, the high oxygen content of steam from boiling reactors is probably related to the boiling process itself, possibly due to a stripping action. Various in-pile and out-of-pile irradiation tests involving boiling and static systems are planned to clarify this point.

Several possibilities are being considered for coping with the oxygenated steam problem. The second-hand BORAX system is relatively dirty and leaky. The problem may not be nearly so severe in the EBWR system. In the absence of a proven procedure for reducing the net oxygen evolution, however, it must be assumed that the oxygen content of EBWR steam will be high. Serious consideration is being given to nickel plating the stationary parts of the EBWR turbine (already fabricated and delivered) by the Electroless (Kanigen) Process. Tests will also be run to determine the feasibility of catalytic recombination of hydrogen and oxygen before the steam reaches the turbine.

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Research and Development

A number of corrosion tests have been run on aluminum alloys containing about 1% by weight of nickel. Results are somewhat puzzling, inasmuch as a much lower corrosion rate is observed when hydrogen is allowed to build up naturally in the autoclave than when an equivalent amount of hydrogen is added artificially. Pretreatment of Al-Ni alloy samples in static water apparently reduces the corrosion rate resulting when these samples are placed in high-velocity streams. Conditions necessary to assure consistently low corrosion rates in dynamic autoclave loops are still being sought.

A boiling corrosion test on aluminum-1% nickel alloy was run for 1702 hr at 300 psi and an average heat flux of 25,000 Btu/(hr)(sq ft). The over-all corrosion rate averaged only 2.9 mils/year; however, several points of locally severe (pitting) corrosion were observed. A second test, planned for a longer exposure, is now under way.

Comparison of aluminum-nickel alloy samples exposed in the same MTR loop showed much lower rates within the pile radiation zone than outside. Enough results are now available to leave little doubt that radiation reduces the corrosion rate of aluminum-nickel alloys.

Samples of bare, high-purity and high-density thoria-urania exposed to 600F water for several thousand hours exhibited very low solution rates.

An aluminum-nickel alloy cartridge containing four 0.25 in. diameter x 0.50 in. long thoria-urania pellets was irradiated in the CP-5 reactor to a burnup of 0.1% of total atoms. The annulus between the pellets and the aluminum can was filled with lead. Post-irradiation examination of the cartridge revealed small ruptures of the cladding with characteristics suggesting stress corrosion due to the low strength of the aluminum-nickel alloy at the 450 - 500F ambient temperature. Radial cracks were observed in the pellets themselves. Some intrusion of lead into spaces between lead pellets occurred.

Preparations are being made to fabricate a new core for the BORAX-III reactor of thoria-urania pellets bonded with lead to aluminum cans. Plates containing cylindrical holes integrally bonded are prepared by extruding aluminum. These tube plates are welded to side plates to form parallel plate fuel assemblies. Preliminary runs indicate that fuel assemblies made by this procedure are relatively inexpensive, as well as being attractive functionally.

Samples of pressed boron carbide showed very low solution rates in 500F water. Clad boron carbide compacts may be a promising control rod material.

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Corrosion tests on crystal bar hafnium have shown accelerated corrosion rates around the core rod area. Contaminants which increase the hardness of hafnium do not necessarily impair the corrosion resistance.

Development work continues on hydraulic and magnetic jack control rod drives. Use of a tapered cylinder for the hydraulic drive resulted in a linear relationship between the position of the piston and the water flow rate past the piston. A new model of magnetic jack has run through 1125 four-foot cycles without difficulty except for an electrical failure which can be readily corrected. At a load of 313 lbs the power consumption is 500 watts. The maximum lifting capacity is about 900 lbs.

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I. EXPERIMENTAL BOILING WATER REACTOR (EBWR)

A. Reactor Design and Evaluation - L. W. Fromm

1. Core

a. Radial Flattening of Flux Distribution - J. A. Thie

Radial flattening can be achieved in a core composed of plates of variable thicknesses and enrichments¹ by a central zone consisting of a uniform mixture:

$$9.9 \text{ nN} + 9.9 \text{ kN} + 8.41 \text{ nE} + 8.41 \text{ kE}$$

and an outer zone of uniform mixture:

$$37.69 \text{ nE} + 37.69 \text{ kE} \quad ,$$

where

	<u>Total Plate Thickness,</u> <u>in.</u>	<u>Composition,</u> <u>% U²³⁵ in Uranium</u>
nN =	0.205	0.72
kN =	0.274	0.72
nE =	0.205	1.44
kE =	0.274	1.44

The total amount of U²³⁵ in this loading is 71.89 kg, of which 32.46 kg is in the form of U²³⁵ added to natural uranium.

(1) Reactor Constants

The epithermal characteristics of a zone depend essentially on whether the plates are thick or thin, and are given in Table I.

Thermal group characteristics for various zone compositions are given in Table II and include equilibrium Xe and Sm.

¹Reactor Engineering Division Quarterly Report, Section I, ANL-5561 (April, 1956).

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Table I

EPITHERMAL CHARACTERISTICS

Temperature: 486F

Elements	% Voids	τ	ρ
All thin	0	47.589	0.86527
	15	58.423	0.84702
1/2 thin + 1/2 thick	0	47.087	0.84026
	15	56.965	0.81929
All thick	0	46.592	0.81290
	15	55.565	0.78926

Table II

THERMAL CHARACTERISTICS

Temperature: 486F

Elements	% Voids	L^2	ϵ	k_{∞}
54.09nN + 45.91nE	0	7.831	1.0300	1.0265
	15	9.063	1.0324	1.0307
27.045nN + 27.045kN + 22.955nE + 22.955kE	0	7.114	1.0420	1.0335
	15	8.183	1.0456	1.0315
54.09kN + 45.91kE	0	6.336	1.0525	1.0460
	15	7.247	1.0572	1.0363
50nE + 50kE	0	5.665	1.0417	1.1795
	15	6.483	1.0454	1.1724

(2) Power Distribution

Table III

POWER DISTRIBUTION AT 20 mw

	Central Zone				Outer Zone	
	9.9nN	9.9kN	8.41nE	8.41kE	37.69nE	37.69kE
% Total Power	6.38	8.56	8.35	8.91	32	35.8
mw	1.277	1.713	1.669	1.781	6.40	7.16
mw/element	0.1290	0.1731	0.1985	0.2118	0.1699	0.1900

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The maximum-to-average radial fluxes are:

Central Zone only	1
Outer Zone only	1.165
Entire Reactor	1.098

The radial maximum-to-average power due to radial flux shape and intercell flux ratios is therefore the ratio of

$$1.165 \times 0.1900 = 0.2213 \text{ mw per hottest kE in outer zone}$$

to

$$20/112 = 0.1786 \text{ mw per average fuel element}$$

or

$$1.24$$

When multiplied by the control rod channel factor, 1.30, and the axial flux factor, 1.49, the over-all maximum to average power is 2.40. This is somewhat less than 3.45 obtained for a one-zone reactor.¹

(3) Reactivities and Void Coefficients

If the central zone is changed from the half thick, half thin plate combination used above to either all thick or all thin plates, one expects to obtain, respectively, more negative or less negative void coefficients, but does not expect appreciable reactivity changes.

Table IV

RESULTS OF TWO-ZONE REACTOR CALCULATIONS

Temperature: 486F

<u>Central Zone</u>	<u>Outer Zone</u>	<u>Voids, %</u>	<u>Reactivity, ρ</u>	<u>$\frac{d\rho}{dV}$</u>
19.8nN + 16.82nE	37.69nE + 37.69kE	0	0.01547	- 0.1018
		15	0.00021	
9.9nN + 9.9kE + 8.41nE + 8.41kE	37.69nE + 37.69kE	0	0.01730	- 0.1153
		15	0	
19.8kN + 16.82kE	37.69nE + 37.69kE	0	0.02175	- 0.1322
		15	0.00173	

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The 30% span in void coefficients (% reactivity change divided by % void change) is compatible with a larger void coefficient variation obtained¹ by trading larger numbers of thick and thin plates. It should be noted that the void coefficient for the two-zone reactor with half thick, half thin plates is very nearly that of the one-zone reactor of similar composition.

(4) Conclusions

The over-all maximum-to-average power of a one-zone reactor can be reduced to 2.40 by increasing the U^{235} critical mass from 66.18 kg to 71.89 kg and appropriately distributing the fuel.

The requirement of a flat central flux can be satisfied at the expense of approximately one-half of the void coefficient flexibility.

Flattening has little effect on void coefficient if the thick and the thin plates are uniformly dispersed.

Changing the void coefficient has little effect on reactivity at operating conditions.

b. Thermal Analysis of EBWR With Natural Circulation (Revised) - R. J. Weatherhead

Recent dimensional changes in the fuel and side plates have indicated the need for a revised predicted performance of the EBWR.² Such calculations have been made with one change in the basic assumptions.

It has been assumed that flow occurs throughout the entire free (or open) cross-sectional area of the core, exclusive of the area enclosed by the control rod guides, and further, that the local velocity and void fraction are constant across any horizontal plane through the core.

It should be noted that the revised power density and average void fraction are based on the core water volume over the 48-in. active length of the fuel plate. The initial power density and average void fraction were based on the water volume contained by the subassemblies, which amounted to approximately 75% of the revised water volume.

²Reactor Engineering Division Quarterly Report, ANL-5561, Section I (April, 1956).

The values of the variables of interest are as follows:

Exit Void Fraction	<u>0.10</u>	<u>0.25</u>	<u>0.40</u>	<u>0.55</u>
Power Density, kw/liter	5.15	17.6	40.4	76.7
Total Flow Rate, 10^6 lb/hr	2.40	3.68	4.28	4.52
Steam Rate, 10^3 lb/hr	14.3	48.5	112.	212.
Inlet Velocity, fps	1.67	2.57	2.99	3.16
Inlet Subcooling, F	2.0	4.4	8.8	15.8
Exit Quality, %	0.60	1.32	2.62	4.70
<u>Average Void Fraction</u>				
48-in. Active Length	0.036	0.091	0.153	0.222
Subassembly	0.026	0.066	0.110	0.160
Entire Core	0.034	0.091	0.154	0.231
Void Fraction in Riser	0.051	0.140	0.242	0.373

The magnitudes of the various pressure drops for the flow circuit are as follows:

Exit Void Fraction	<u>0.10</u>	<u>0.25</u>	<u>0.40</u>	<u>0.55</u>
<u>Subassembly</u>				
$\Delta P_{\text{head, non-boiling, lb/sq ft}}$	133.5	133.5	133.5	133.5
$\Delta P_{\text{head, boiling}}$	133.5	121.5	108.2	93.5
$\Delta P_{\text{expn. \& contr., non-boiling}}$	3.63	8.57	11.6	12.9
$\Delta P_{\text{exit, boiling}}$	0.62	1.82	2.98	4.45
$\Delta P_{\text{friction, non-boiling}}$	1.86	4.09	5.29	5.86
$\Delta P_{\text{friction, boiling}}$	7.11	17.4	32.2	51.1
$\Delta P_{\text{acceleration}}$	0.47	3.24	8.70	17.6
Driving Head	7.70	20.0	33.1	47.9
$\Delta P_{\text{total}} - \Delta P_{\text{head}}$	13.7	35.1	60.8	91.9

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Riser

ΔP_{head}	131.	119.	105.3	87.2
$\Delta P_{\text{friction, two-phase}}$	0.05	0.14	0.23	0.38
$\Delta P_{\text{exit, two-phase}}$	0.17	0.44	0.67	0.95
Driving Head	6.95	18.9	32.5	50.0
$\Delta P_{\text{total}} - \Delta P_{\text{head}}$	0.22	0.58	0.90	1.33

Comparison of the initial and revised values of the operational variables (Fig. 1) shows:

(1) An increase of 33% in the total flow and steam rates, due, in part, to the increased flow area used in the current calculation.

(2) An increase in total power output of the core, as evidenced by the increased steam rate.

(3) Relatively minor (5-7%) increase in the inlet velocity.

(4) A slight decrease in the average void fraction.

c. Water Corrosion Developmental Tests - C. R. Breden

(1) EBWR-Type Fuel Plates

Eight pre-production development plates,³ each containing uranium-5% zirconium-1.5% niobium and roll clad with Zircaloy 2, have accumulated up to 6,022 hr in static, degassed water at 550F, with no significant change in appearance.

A group of ten plates (53 in. and 54 in. long), each containing uranium (0.4% depleted)-5% zirconium-1.5% niobium and clad with Zircaloy 2, were tested for 336 hr in static, demineralized, degassed water at 550F. One plate had ruptured at one end (Fig. 2); the remaining plates showed some warping and some areas of white corrosion products. Argonne Metallurgy Division records indicate that the ruptured plate was over-pickled.

³Reactor Engineering Division Quarterly Reports: ANL-5561 (April, 1956); ANL-5511 (January, 1956).

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FIG. 1
COMPARISON OF INITIAL AND REVISED
CALCULATED PERFORMANCE OF EBWR

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FIG. 2
ZIRCALOY 2-CLAD U(0.4% DEPLETED)-5% Zr-1.5% Nb PLATE
AFTER 336 hours IN STATIC, DEMINERALIZED, DEGASSED
WATER AT 550 F. (PLATE NO. 221).

(2) Prototype Fuel Assembly

A fuel assembly, intended to demonstrate fabrication procedures, was corrosion tested in 550F degassed, demineralized water. The unit contained one fuel plate which was known to be defective.

A pressure build-up, due to evolved hydrogen, was observed after two hours, indicating a rupture. As shown in Fig. 3, the plate had ruptured near the upper and lower ends, blocking a large portion of the channel on one side, and partially closing the channel on the other side. One side plate was bulged slightly, but there appeared to be no twisting or warpage of the assembly as a whole, either as a result of test environment or plate rupture.

d. Core Components - C. K. Soppet

(1) Dummy Fuel Assemblies

Dummy fuel assemblies (Fig. 4) are used to fill the outer fuel positions when less than a full complement of fuel (5 ft diameter core) is used. Each assembly consists of a lower locating end fitting, body hold-down assembly, and top handling fitting.

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FIG. 3
RUPTURED FUEL PLATE AFTER 2 HOURS IN
DEGASSED, DEMINERALIZED WATER AT 550 F

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FIG. 4
EBWR DUMMY FUEL ASSEMBLY

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The lower locating end fitting is machined from a stainless steel tube, 3-3/4 in. OD x 3 in. ID. The piece contains chamfered seats and a pilot guide, which are the same size as those of the active fuel assembly locating end fittings.

The body is fabricated of aluminum-1% nickel alloy sheet, 1/16-in. thick. The sheet is shaped on a press brake and spot welded to form a square tube, 3-3/4 in. x 64-3/4 in. long. One side plate of the box is extended to form a tab which runs the full length of the body. This tab bears against the side of an adjacent assembly to reduce flow leakage between assemblies into the downcomer space during future forced circulation operation of the reactor.

Forced circulation also requires the use of a hold-down assembly to retain dummy assemblies in the core against the expulsion force of the recirculation pump pressure. The hold-down assembly is made up of the latch bars, the latch-bar actuator, and the actuator lead screw.

The latch bars are round stainless steel rods (1/2 in. OD x 1 in. long) which protrude through four holes drilled through the wall of the dummy locating end fitting. The latch-bar actuator is a stainless steel rod (1/2 in. OD) threaded and shouldered at the lower end and fitted at the top with a cross-bar to allow the application of rotational motion to the actuator. The latch bars are pinned to a lead nut which is housed in the locating end fitting of the dummy. Rotation of the actuator cross-bar moves the lead nut downward and forces the latch bars to protrude past the lower surface of the support plate. This interference between the support plate and the dummy assembly makes upward motion of the dummy impossible while the latch bars are extended.

The top handling fitting used for the dummy assemblies is the same as the fitting described previously⁴ for active fuel assemblies.

(2) Control Rod Channel Closures - C. K. Soppet

The operation of the EBWR as a forced circulation reactor requires that the water flow through all portions of the core other than the fuel assemblies be kept to a minimum. The control rod channels without flow restriction could by-pass as much as 30% of the total available pump capacity. Accordingly, the top of the control rod guide and shroud structure is fitted with a cap at each control rod channel position.

⁴Reactor Engineering Division Quarterly Report, ANL-5461
(April 15, 1955), p. 23.

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The caps are removable extensions of the guide structure, which encloses the tops of the control rod channels. The removable feature is required in the event that it is necessary to remove the control rods from the reactor.

In order to simplify fabrication, the sheet metal and structural parts of the caps are assembled by one-side spot welding, rather than by the riveting method used in the rest of the guide structure. The tops of the caps are fitted with a retrieving head of the same type used on the control rods; thus the control rod retrieving tool is also used to remove the channel caps.

The caps are attached to the guide structure by bolts which pass through the spacer bar extensions of the caps and thread into tapped holes in the guide structure spacer bars. The bolt passage holes in the spacer bar extensions are necked down at the top and tapped to match the bolt threads. The bolts have a turned down shank between the short length of thread at the bolt end and the head. This requires that the bolt be threaded through the tapped portion of the spacer bar extension in the cap before it can drop into position to be threaded into the guide structure spacer bars. This method effectively captures the bolts, so that during cap removal operations, the loosened bolts are still positively retained within the spacer bar extensions. This feature also prevents any bolts which might be loosened during reactor operation from being dropped into the reactor.

(3) Shroud and Grid Plate Handling Assembly - C. F. Bullinger

Space limitations within the reactor pressure vessel necessitate attaching the Zircaloy fuel element shroud and control rod guide to the grid plate outside the vessel and then lowering the assembly into place as a unit. The grid support rests on machined pads welded to the bottom head of the pressure vessel and is secured with eight 17-4 PH stainless steel captive "T" bolts (1 in. diameter). Handling of the shroud and grid plate assembly, both during initial installation and during later replacement for the purpose of altering core geometry, will be accomplished with the aid of the lifting ring spider shown in Fig. 5. The method for carrying out such a replacement is as follows:

Each of the eight "T" bolts is unscrewed with the aid of a bayonet-type extension wrench (21 ft long). The "T" bolts have pilot pins extending upward from the "T" to act as guides for locating the wrench and lifting rods. The lifting rods are then lowered individually by means of locked-on extension rods and rotated on the "T" bolts to the lifting position. The spider ring is lowered into place over the rods until the captive nuts rest against the tops of the lifting rod threads. The long hollow

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FIG 5
EBWR SHROUD AND GRID PLATE
LIFTING RING

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tube extension hex-nut wrench is lowered, in turn, over each extension rod until the captive nut is engaged; the extension rod is held to prevent turning of the lifting rod and "T" bolt, and the nut is tightened.

With the nuts tight, the shroud is placed in compression and the lifting rods in tension to provide a rigid unit for handling. The extension rods are detached from the lifting rods and removed. The unit is then lifted from the vessel by the overhead bridge crane.

The procedure is reversed for insertion of the initial or a replacement assembly.

2. Control Rods and Drive Mechanism

a. Boron-Stainless Steel Control Rod - C. F. Bullinger

Four control rods for EBWR will be fabricated with 2% boron-Type 304 stainless steel absorber sections as shown in Fig. 6.

The rod design is based upon use of formed sheet metal joined by spot-welding and riveting where required. The transition between the boron-stainless steel absorber section and the Zircaloy 2 follower is accomplished by use of a short cruciform section of Type 304 stainless steel (1/4 in. thick) which is welded to the absorber section and riveted to the follower. This method of transition is used because there is a slight tendency for boron-stainless steel to become increasingly notch sensitive under irradiation and this portion of the rod is in the region of highest flux.

The retriever tip at the top of the rod is fabricated from Inconel because of wear qualities required in use with the retrieving tool (see page 45).

The neutron irradiation effects on boron-stainless steel are virtually unknown. For this reason some concern has been expressed about the feasibility of using this material for control rods. However, since in the normal operating position, the rods are completely withdrawn, they will not be subjected to severe fluxes.

One control rod has been fabricated to facilitate cost analysis and performance tests.

b. Hafnium Control Rod Operational Test - C. F. Bullinger

Five control rods for EBWR will be fabricated with hafnium absorber sections.⁵

⁵Reactor Engineering Quarterly Report, ANL-5511 (January, 1956)
Fig. 2, p. 20.

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FIG. 6
BORON-STAINLESS STEEL CONTROL ROD

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One control rod has operated for 40 days at 486F and 600 psi in a full-size stainless steel and Zircaloy 2 guide structure in the Control Rod Drive Test Facility. The unit was inspected at the end of 30 days for evidence of corrosion, warpage, and wear. Both the hafnium and Zircaloy 2 portions of the control rod exhibited a normal autoclaved appearance. The rivet heads at the connection between the 3-in. Zircaloy and stainless extension joint were slightly rusted. The opposite sides of two opposing blades were burnished for a length of 7-1/2 in. on the Zircaloy follower at the bottom end. There was no evidence of fretting corrosion.

c. Cost Comparison of Hafnium and 2% Boron-Stainless Steel Control Rods - C. F. Bullinger

A material and fabrication cost comparison has been made between hafnium and 2% boron-stainless steel control rods, each with Zircaloy 2 followers. The actual cost figures for procurement of materials and fabrication, per rod, in Argonne shops are given in Table V.

The hafnium-zircaloy control rod suffers, cost-wise, because of the low yield (52%) of finished hafnium sheet from the iodide crystal bar. The sheet metal design employed results in a fabrication scrap loss of less than 1%. The low yield of hafnium sheet was due strictly to the ineptness of the commercial vendor in melting and rolling.

A quantity of 454 lb of hafnium crystal bar was supplied to the processor for converting to 10 in. x 0.062 in. cold finished sheets. A total of 233 lb, in 18 usable sheets, was returned. Of this amount, only five pieces were of the specified size. The remainder varied in length from 44 in. to 36 in. Therefore it will be necessary to piece together odd lengths to achieve final dimensions. In addition, the material has been embrittled by contamination so that it is necessary to jacket each sheet for hot forming in an argon atmosphere. Fortunately, contaminated hafnium still has satisfactory corrosion resistant properties, and jacketing of the hafnium for forming was not a problem, aside from the added cost, since this technique is required for hot bending Zircaloy 2.

As an indication of the excessive hardness and brittleness of this lot of hafnium, the Rockwell "B" hardness reading was 102 as compared to 85-88 for normal purity material. Cold bends of less than 15 degrees resulted in cracking, whereas good quality hafnium may be bent easily to 90-degree angles.

It is indeed ironic that 77% of the cost of the boron-stainless steel rod is for Zircaloy 2, which is required only for the purpose of filling the water hole of the guide with a low cross-sectional material when the absorber section is withdrawn.

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Table V

COST COMPARISON BETWEEN HAFNIUM AND 2% BORON-STAINLESS
STEEL CONTROL RODS

Hafnium-Zircaloy 2 Rod

(Thickness: 1/8 in.; Weight: 97 lb)

Material

Hafnium (4 sheets: 1/16 in. x 10 in. x 46 in.)	52 lb at \$154.00	\$8050.00
Zircaloy 2	26.4 lb at 33.00	870.00
Type 304	19 lb at 1.00	19.00
Inconel	3 lb at 1.30	3.90
Total Materials Cost:		\$ 8942.90

Labor

Zircaloy 2 canning and forming	32 hr at \$ 5.50	\$ 176.00
Hafnium canning and forming	20 hr at 5.50	110.00
End fittings	40 hr at 5.50	220.00
Final assembly	8 hr at 5.50	44.00
Total Labor Cost:		550.00
<u>Total Cost:</u>		\$ 9492.90

2% Boron-Stainless Steel - Zircaloy 2 Rod

(Thickness: 1/4 in.; Weight: 141 lb)

Material

2% Boron-Type 304 (4 sheets: 1/8 in. x 10 in. x 46 in.)	66 lb at \$ 3.40	\$ 224.00
Zircaloy 2	52 lb at 33.00	1716.00
Type 304	38 lb at 1.00	38.00
Inconel	3 lb at 1.30	3.90
Total Materials Cost:		\$1981.90

Labor

Zircaloy 2 canning and forming	32 hr at \$ 5.50	\$ 176.00
2% Boron-Type 304 forming	1 hr at 5.50	5.50
End fittings	40 hr at 5.50	220.00
Final assembly	10 hr at 5.50	55.00
Total Labor Cost:		456.50
<u>Total Cost:</u>		\$2438.40

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d. Effectiveness and Burnup of Hafnium and Boron-Stainless Steel Control Rods - H. P. Iskenderian

Calculations were made to compare the effectiveness and burnup of hafnium control rods⁶ with boron-stainless steel rods containing 1, 2, and 3 wt-% boron. The results (Table VI) indicate that the 2% boron-stainless steel rod, 1/4 in. thick, will have somewhat greater blackness than a thermally black control rod and is nearly as effective as a 1/8-in. thick hafnium rod.

Table VI

CALCULATED BURNUP AND EFFECTIVENESS OF HAFNIUM
AND BORON-STAINLESS STEEL CONTROL RODS

(Average reactor flux = 1.5×10^{13})

Rod Composition	Thickness, in.	Effectiveness, %*		% Burnup in 1 year
		Initial	After 1 year Burnup	
1% B-SS	1/4	88	80	28.5
2% B-SS	1/4	109	104	14.5
3% B-SS	1/4	117	114	9.8
Hafnium	1/8	108	108	negligible

*Values are referred to a black thermal rod.

Values >100% are due to absorption of epithermal flux.

e. Zircaloy 2 - Stainless Steel Contact Corrosion -
C. R. Breden

Samples of Zircaloy 2 bolted together with Type 304 and with Type 347 stainless steel show no evidence of contact-crevice corrosion after 8786 hr in dynamic test at from 500 to 600F, or static tests for 9581 hr in 500F air saturated, and degassed water, and 8767 hr in 600F air saturated and degassed water, respectively.

⁶Reactor Engineering Division Quarterly Report, ANL-5371
(January 15, 1955), p. 57.

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3. Shielding Structure - E. H. van Zylstra

a. Bottom Shield Plug

The bottom shield plug forms the lower portion of a 3/4-in. thick mild steel tank surrounding the reactor vessel. The base of the plug (10 ft-2 in. diameter) is also the center portion of the control rod drive room ceiling. The side walls of the plug complete the frustum of a right circular cone whose upper diameter is 8 ft-6 1/2 in. and whose vertical height is 4 ft-4 in. This shape was selected to provide a means for rapidly relieving any pressure that might build up within the shield as a result of pressure vessel rupture following a possible violent metal-water reaction in the reactor core. In line with this thinking a partial penetration seal weld is used to join the top of the shield plug to the cylindrical inner shield form. A ring welded to the bottom of the shielded plug forms a 3-in. wide shelf on which the inner end of the surrounding 3/4-in. thick mild steel control rod room ceiling panels rest. A 1/4-in. continuous fillet weld from the edge of the ceiling plates to the top of the shelf and intermittent 3/8-in. fillet welds from the edge of the shelf to the bottom of the 3/4-in. ceiling plates are used to hold the bottom of the shield plug to the ceiling plates. These joints are adequate for normal operation yet provide points of weakness so that the entire bottom shield plug will be blown downward in the event of the pressure buildup mentioned above. The missiles thus launched will be directed toward the portion of the power plant building shell most adequately protected by concrete. The upper portion of the reactor vessel and shielding is restrained from upward motion by the steel containment structure.⁷

In order to hold the length of the control rod thimbles required to penetrate the lower shielding to a minimum, a combination of lead ferroboron plaster and magnetite and punchings concrete has been used for a total of 51 in. of high-density shielding. Stepped stainless steel sleeves have been provided to fit around the control rod thimbles and the recirculation inlet nozzles with 1/4-in. air gaps all around. Space has also been left at the steps to allow for downward thermal expansion of the pressure vessel and nozzles. Both top and bottom of the stainless steel sleeves are adjustable horizontally to match the locations of the nozzles on the pressure vessel at assembly and to maintain uniform 1/4-in. air gaps at field assembly prior to filling the plug shell with the high-density shielding material.

Duplicate copper cooling coil circuits (1/2 in. OD x 0.032 in. wall) are provided to remove heat from the concrete outside of the sleeves around the four recirculation inlet nozzles. Cooling coils are also embedded in the 3-in. thick layer of lead at the top of the plug.

⁷Reactor Engineering Division Quarterly Report, ANL-5511, Section II (January, 1956), p. 31.

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Mounting rings for expansion bellows are welded to the bottom of the shield plug around each control rod thimble and recirculation inlet nozzle. Each bellows assembly has a modified Vanstone flange at the top and a welding ring at the bottom. The flanged end is bolted to the bottom of the shield plug using a lead ring gasket seal. The ring at the lower end of the bellows assembly is welded to the nozzle. The three-convolution stainless steel bellows are shipped and installed in a compressed position so that maximum travel is available to allow for thermal expansion of the pressure vessel and nozzles.

Thermocouple wells are provided at each control rod thimble in order to monitor the temperature of the thimbles at points approximately 2 ft up from the bottom of the shield plug.

b. Forced Circulation Outlet Nozzle Shielding

Lead shielding is required around the vertical portion of the 12-in. forced circulation outlets to compensate for the punchings concrete removed from the lower reactor shielding for these piping runs. Twenty-inch Schedule 40 pipe sleeves (6 ft-3 in. long) furnished by the contractor are used as inner forms for the poured lead. Double circuit copper cooling coils (1/2 in. OD x 0.032 in. wall) are fastened to the outside of the 20-in. pipes. Sleeves rolled from 1/8-in. mild steel with inside diameters of 26 in. provide an outer form for the lead.

Because of the angle of the radiation path from the core, the outer form is made in two equal lengths. The vertical centerline of the top form is offset 1 in. radially toward the reactor core, and the vertical centerline of the lower form is offset 1 in. radially away from the reactor core. The gap between the two forms at the offset is closed by a ring of 1/8-in. mild steel. The gap between the bottom of the outer form and the 20-in. pipe is also closed with a ring. The top of the outer form is spaced with 1/2-in. diameter rods welded between the outer form and the 20-in. pipe.

Lifting lugs have been provided at the top of the form to facilitate handling in the shop and placing of the shield forms in the power plant building. Lead will be poured 4 ft deep in the forms in the shop. The balance of the lead will be poured by the contractor in the field after the top of the outer shield forms have been welded to the bottoms of the large and small pipe tunnels.

c. Top Shielding and Insulation (Fig. 7)

The inner shield form (8 ft - 6 1/2 in. OD x 3/4 in. thick) is welded to the bottom of a 1/4-in. steel box enclosing the pressure vessel support framing structure. A cylinder of mild steel (10 ft-6 in. ID x 1/4 in. thick) is welded to the top of the box structure and extends upward to the

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LEGEND

- - PUNCHINGS CONCRETE
- - 430 S. ST. WOOL
- - WATER
- - 3/8" DIA. Pb. PELLETS

FIG. 7
REACTOR TOP SHIELDING

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bottom of a circular channel embedded in the concrete flush with the main floor. All of the shielding and insulation in the annular space surrounding the top of the reactor is removable to provide access to the "C" springs installed between the pressure vessel mounting lugs and the support framing structure.

(1) Insulation:

Pads of Type 430 stainless steel wool insulation (3 in. thick) cover the pressure vessel up to the bottom of the mounting lugs. Pads of the same thickness are used in the space around the vessel between the lugs to an elevation $8\frac{5}{8}$ in. below the gasket surface on the top of the vessel. A ring of high-density insulation is placed above the stainless steel wool pads to reduce radiation leakage upward around the pressure vessel. Lead pellets ($3/8$ in. dia.) are poured into an annular space $6\frac{15}{16}$ in. wide x $8\frac{1}{8}$ in. deep. The pressure vessel provides the inner wall of this space, and $1/4$ -in. mild steel plates are used for the outer wall and bottom. A $1/4$ -in. thick mild steel cover rests on the outer wall of the form and contains the lead pellets. Since the vessel support lugs extend out from the pressure vessel in this area the lead forms are made in segments.

The blanket of insulation is continued with a ring of Type 430 bulk stainless steel wool (density = 5 lb/ft^3) inside four stainless steel cans ($3\frac{1}{2}$ in. thick), each spanning a 90-degree segment. Two holes for lifting eyes are also provided in the cover of each can. The space above the pressure vessel closure studs and nuts is filled with canned stainless steel wool in a similar manner.

The top of the pressure vessel cover plate is insulated with a pad of Type 430 stainless steel wool ($5 \text{ ft}-7/8$ in. dia. x 3 in. thick). Sections of stainless steel pipe (3 in. long) penetrate the insulation above the three tapped lifting eye holes in the cover plate.

(2) Shielding:

Various configurations of shielding are used to fill the annular space between the insulation and the outer steel shielding envelope. Four triangular shielding segments rest on the bottom plate of the box structure surrounding the pressure vessel support framing structure at the transition between the cylindrical inner shield form and the rectangular opening in the pressure vessel support framing structure. These triangular segments extend to an elevation $3/4$ in. above the top plate of the box structure around the framing and form a base on which the upper shielding segments rest. The $1/4$ -in. mild steel open top form is filled with magnetite and punchings concrete. A $3/4$ -in. square bar of mild steel, formed to a 10 ft-6 in. diameter, is welded to the 10 ft-6 in. ID upper shield envelope at the elevation at the top of the triangular supports and provides peripheral support for the upper shielding segments.

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A ring of shielding $19\frac{5}{8}$ in. deep is made up of $1/4$ -in. steel box structure segments fitting around the pressure vessel support lugs. Four of these segments rest on the triangular supports and the bar shelf. The segments between rest on the bar shelf and are bolted to the wall of the 10 ft-6 in. cylindrical shield envelope at the top. All open top shielding box structures are filled with magnetite and punchings concrete. Three $1\frac{1}{2}$ -in. square bars welded to the box structure and embedded in the concrete are tapped for lifting eyes to facilitate handling. These segments also have a 4-in. wide shelf on the inside to provide a support for the lead pellet forms described above. In order to provide clearance for the two monitoring lines and one vent line welded to the pressure vessel near the top, the shielding segments in these areas are made in two pieces with a $1\frac{1}{2}$ -in. thick steel spacer plate between the pieces. One segment is placed, the spacer plate is positioned and bolted to the shielding and then the second segment is lowered into place.

The next shielding ring ($9\frac{1}{2}$ in. deep x 10 ft- $5\frac{1}{2}$ in. OD x 7 ft- $5\frac{1}{2}$ in. ID) is made from four, $1/4$ -in. plate, open top box segments filled with magnetite and punchings concrete. Tapped holes for lifting lugs facilitate removal.

A ring (10 ft-5 in. OD x 6 ft-7 in. ID) burned from a 3-in. mild steel plate forms the next shielding layer. Three tapped holes for lifting lugs have been provided to facilitate handling.

A top shield tank has been provided to cover the shield envelope (10 ft-6 in. ID). An 11 ft-2 in. dia. circle burned from a 6-in. thick mild steel plate forms the top of the tank. Two rings $12\frac{1}{2}$ in. high, one 10 ft-5 in. OD, 1 in. thick, and one 5 ft-5 in. OD, 3 in. thick, are welded to the bottom of the plate. A ring and a circle of $1/2$ -in. steel plate are welded to the $12\frac{1}{2}$ -in. high rings and form the bottom of a water tank $11\frac{1}{2}$ in. deep. Fill and vent connections are attached to the 6-in. plate. The 3-in. thick, $12\frac{1}{2}$ -in. high ring rests on the top of the pressure vessel lid and transmits any upward force to the containment structure in contact with the top of the 6-in. plate. The space between the lower face of the plate and the circular channel embedded in the main floor is sealed with a sponge rubber gasket 2 in. thick in its free state and compressed to 1 in. thick to provide an atmospheric seal for the air within the shield.

4. Fuel and Control Rod Handling

a. Fuel Transfer Coffin and Carriage - S. A. Bernsen, C. K. Soppet

Movement of irradiated fuel from the reactor to the storage well is accomplished by use of a lead shielding coffin shown in Fig. 8. The coffin is a thick-walled lead cylinder ($6\frac{5}{8}$ in. ID x $30\frac{1}{2}$ in. OD),

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FIG. 8
FUEL COFFIN AND TRANSFER CARRIAGE

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completely sheathed in steel. During transfer operations, the fuel is housed in a 6-in. diameter compartment in the center of the coffin. This compartment is capped by a lead plug (13-7/8 in. thick) through which the fuel-retrieving tool is operated. The lower end of the fuel compartment is closed by a movable door which can be clamped tightly to provide a water-tight seal. This feature permits the fuel zone of the coffin to be filled with water in the event that extensive cooling of the fuel is required during the transfer operation. If only a small amount of heat removal is required, the coffin is equipped with a cooling coil, wrapped about the fuel zone, sufficient for this purpose.

The fuel-retrieving tool (see page 45) is contained within and operates through the fuel zone of the coffin. Raising and lowering of the fuel and/or tool is accomplished by a hand-operated winch mounted on the top plug of the coffin.

Movement of the transfer coffin between the fuel storage pit and the reactor, as well as positioning of the coffin, is accomplished by means of a coffin carriage operating on rails embedded in the main floor of the power plant building. The carriage is moved in the direction parallel to the axis of the reactor and storage pit by means of a hand-driven chain and sprocket wheel arrangement. Lateral adjustment is accomplished by a hand-operated lead screw. Three screw jacks, gang operated by an electric motor drive, are employed to raise and lower the coffin in the carriage.

A cylindrical skirt is provided around the bottom of the coffin in order to limit the dose rate at the foot of the coffin, as the fuel element enters, to that occurring through the 21 in. of steel on which the coffin rests during unloading.

The coffin has sufficient thickness of lead to permit safe unloading of fuel elements which operate at substantially higher power densities than the present reference core. This feature is necessary in order to make the coffin adequate for future higher power density D_2O cores. For the hottest conceivable element a coffin surface level of several hundred mr/hr is considered tolerable, since the unloading operation is rather infrequent, and the major portion of the operation may be carried out at a distance from the coffin surface. Further, these levels are calculated by a method yielding dose rates which are high by a factor of 4 or 5 when compared with actual test results from several existing fuel handling coffins.

Table VII summarizes the design parameters and calculated radial surface radiation levels for the fuel transfer coffin.

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Table VII

DESIGN PARAMETERS OF EBWR FUEL TRANSFER COFFIN

	<u>H₂O Core</u>	<u>D₂O Core</u>
Reactor Power, mw	20	40
Minimum number of fuel elements	112	37
Active length, in.	48	48
Maximum/average power per element	1.46	1.46
Source strength (2.5-mev γ) for hottest element, $\gamma / (\text{cm})(\text{sec})$	4×10^{13}	24×10^{14}
Thickness of lead in coffin wall, in.	12	12
Thickness of steel in coffin wall, in.	1	1
Decay time to unloading, hr	16	16
Dose rate on coffin surface, mr/hr		
Natural U element	16	100
Enriched U element (no self-shielding)	60	360

The radial surface radiation was calculated by use of an approximate formula for a line source behind a shield with linear buildup:

$$\text{Dose} = \text{STPK} \frac{\mu}{2\pi} \frac{\pi}{2\mu Z} e^{-Z},$$

where

S = source strength of fuel element at shutdown
(gammas/sec/cm of length)

At shutdown, 3% of full power is given off in gammas. This is 6% of full power. Of these, 1.5 mev is contained in 2.5-mev gammas and 4.5 mev is contained in 1-mev gammas. The 2.5 mev gammas determine the surface level. There are, therefore, $(1.5/2.5)$ 2.5-mev gammas per fission/sec at shutdown.

T = decay factor for cooling time after shutdown

$$T \approx [t^{-0.2} - (t_1 + t)^{-0.2}] \quad 10 \text{ sec} < t < 100 \text{ days}$$

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where

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t = cooling time after shutdown (sec)

t_1 = reactor operating time (sec)

P = probability of escape from fuel assembly for gammas emitted (for average EBWR element $P \approx 0.3$)

K = conversion factor from gammas/(cm²)(sec) to mr/hr; for 2.5-mev gamma rays $K = 3.65 \times 10^3$

μ = average attenuation coefficient for coffin wall for 2.5-mev gammas ($\frac{1}{\text{cm}}$)

Z = thickness of coffin wall (cm).

b Fuel Assembly Retrieving Tool - C. K. Soppet

The total distance between the top of the core and the top of the coffin adds one more complication to the usual problems of retrieving tool design. If a solid, continuous rod passing through a tube were used to actuate the pins of the tool head, and the rod and tube assembly were to extend from the top of the transfer coffin to the top of the core in the reactor vessel, then the end of the rod and tube assembly would extend 35 ft above the operating floor with a fuel assembly withdrawn into the coffin. Since this head room requirement is quite inconvenient to over-all reactor plant operation, a telescoping retrieving tool assembly, shown in Fig. 9, has been designed. The use of this tool permits a reduction in the head room requirement from 35 ft to approximately 19 ft. More importantly, the head room necessary during coffin storage is only about 10 ft as compared with almost 26 ft for the continuous rod and tube assembly. This reduction permits storage of the tool in the coffin without interference with the circular building crane.

The locking assembly consists of the telescoping actuator tubes, the actuator rod, the actuator rod blocking tube, the lift tube, the lift tube blocking balls, and the actuator. The telescoping actuator tubes are attached to and act as extensions of the actuator rod.

The actuator rod is fitted with a lead screw, which mates with the blocking nut section of the actuator blocking tube. This combination translates the rotary motion of the tubes to longitudinal actuator motion, which operates the latch pin to engage or disengage the fuel assembly. The actuator tubes are fitted with operator sleeves and pins, so located that rotation of the actuator rod can be accomplished only when the tubes are fully extended. This prevents the operator from accidentally delatching the fuel assembly during lifting operations. The blocking nut

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FIG. 9
EBWR FUEL ASSEMBLY RETRIEVING TOOL

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lead screw combination is also used to hold the retrieving tool assembly in the delatched position, preventing damage to either the fuel assembly or tool head when the latter is lowered onto the fuel assembly. The actuator rod also contains a groove and a turned-down section of the diameter about midway along the rod. The groove allows the lift tube blocking balls to lock the tool in the latched position. The turned-down section provides a recess for the lift tube blocking balls while the actuator is moving between the fully latched or delatched positions, and thus prohibits the operator from removing a fuel assembly until the tool is fully latched and locked.

When the tube blocking balls are in either the groove or the recess area, the lift tube is released and can be raised until the lift tube flange contacts the flange of the body extension tube. Continued raising of the lift tube will raise the retrieving tool head. If the latching or delatching operation has only been partially completed, the blocking balls will be at some point between the groove and the recess. The balls will then protrude thru the wall of the actuator blocking tube and into the relief groove at the upper end of the lift tube. Any further raising of the lift tube will separate the actuator blocking tube from its normal position in the body extension. Since the lead screw is in the blocking nut of the actuator blocking tube, the actuator rod and actuator will also be withdrawn, as the lift tube is raised, delatching the fuel assembly.

The latch pins of the retrieving head also contain a locking feature. A turned-down section is provided just back of the end of the pins. When the pins are extended through the latch pin holes of the fuel assembly top fitting (the latched and locked position), these turned-down sections are in line with the sides of the fittings. As the assembly is raised, the ears of the fitting rest in these sections and resist the withdrawing motion of the latch pins.

In summary, these safeguards yield a retrieving tool which requires that the operator merely lower the tool and rotate the actuator operating wheel. The tool head is provided with guide plates which automatically position the head within the top fitting ready for the latch operator. This guiding action will not occur, however, when the head is 90 degrees out of position. In this case the operator would be required to rotate the head a few degrees so that automatic guiding can occur.

The operations involved in removing a fuel assembly from the reactor are all accomplished manually. The winch used in raising and lowering the retrieving head is a back-lock, manually operated type, which is mounted at the top and at one side of the coffin. The outer telescoping tube is mounted in the center of the top shielding plug of the coffin. With the retrieving tool in the retracted or telescoped position, the actuator lead screw is located above the threaded portion, or blocking nut, of the

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actuator blocking tube. The actuator is now in the delatched position and the retrieving head lock assembly is set as shown in Fig. 9 (a). Operation of the winch wedges the lift tube block balls between the left tube ID and the OD of the actuator rod which locks the lift tube in position. The winch is operated to lower the tool to the top of the fuel assembly the tool coming to rest with the ears of the fuel handling fitting in the recess between the fuel assembly blocking plate and the retrieving tool body. The latch holes are now in line with the latch pins and ready for the latching operation. Simultaneously, the extension of the telescoping actuator tubes has placed the operator pins in the slots of the operator sleeves, making it possible to rotate the lead screw by rotating the tube operator hand wheel.

The hand wheel is rotated until the lead screw moves against the stop provided in the actuator blocking tube. This action accomplishes the latching operation and, at the same time, firmly wedges the lift tube blocking balls between the locking groove of the actuator rod and the ID of the lift tube. The locking assembly is now in the position shown in Fig. 9 (b). The winch is then operated to withdraw the fuel assembly into the coffin.

The release of a fuel assembly is accomplished in much the same manner with the exception of certain minor changes necessitated by the safety features of the locking assembly. The retrieving assembly and attached fuel assembly are lowered in the same manner previously described. After the assembly has been lowered the full travel of the telescoping tubes and the fuel assembly has been placed down, removing the weight of assembly from the latch pins, the delatching operation may be completed. The telescoping tube operator hand wheel is rotated to drive the actuator rod lead screw up, withdrawing the actuator and delatching the fuel assembly. With the actuator rod lead screw withdrawn completely from the blocking thread, the lift tube balls are again wedged between the ID of the lift tube and the OD of the actuator rod resetting the lock assembly to the conditions of Fig. 9 (a). The retrieving assembly can now be raised into the storage position within the coffin.

c Fuel Storage Pit - S. A. Bernsen, C. K. Soppet

The fuel storage pit (Fig. 10) is a water-filled concrete well 25 ft deep and 10 ft square at the top. At the 15-ft depth, the pit has two 1-ft wide ledges running the full length in the North-South direction.

A fuel assembly unloading shield is provided to afford protection to the operators in unloading the fuel transfer coffin. A fuel storage rack is located in the bottom of the pit. A recirculating ion exchange filter system is provided to minimize the water activity and provide for water clarity.

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FIG. 10
FUEL STORAGE PIT

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(1) Fuel Unloading Shield

In order to unload the irradiated fuel assemblies from the coffin into the storage pit, a column-containing vertical chute is constructed in the southeast corner of the storage pit. The column is built up of ordinary concrete from the bottom to a point 4 ft below the main floor level. Heavy (290 lb/cu ft) magnetite and steel punchings concrete forms the upper portion of the column from the point to the main floor. A lead insert is located around the chute and extends 3 ft downward from floor level. The lead is tapered in order to conserve lead volume and still maintain the equivalent of 12 in. of lead shielding between the floor and the fuel assembly as it is lowered through the chute.

From the main floor level to a point 6 ft-6 in. down, the exit slot which connects the chute, through which the fuel assembly passes, with the storage pit proper is only wide enough to pass the upper portions of the fuel handling tool, but not the fuel assembly. Below this point the slot is enlarged to permit passage of the fuel assembly. With this design it is impossible to move an assembly out of the shield into the pit until the assembly has been lowered sufficiently to provide adequate water shielding above it. The chute itself is sufficiently long in the East-West direction to permit the fuel assembly to be lowered into it behind shadow shielding which prevents direct radiation streaming from the assembly through the exit slot. A locally higher level of radiation will still prevail through the slot because of scattering, but this is directed across the storage pit toward the North wall, which will be free of personnel during the unloading operation.

(2) Fuel Storage Rack

The storage rack is a rectangular grouping of 4-in. square boxes formed by interlocking 1/8-in. thick sheets of stainless steel and boron-stainless steel. While this storage method provides a simple solution to the shielding problem, a criticality problem is posed in exchange. The storage rack fully loaded with irradiated fuel and covered with water can form a slab reactor approximately 5 ft wide by 4 ft deep by 4 ft high. This possibility requires that a sufficient quantity of absorber be built into the rack to maintain k_{eff} less than 1.

The calculations of absorber worth, in the described geometry, indicate that as much as 29.5% reactivity could be controlled by using 1 wt-% boron-stainless steel sheet to form two sides of each of the 4-in. square boxes and Type 304 stainless steel sheet to form the remaining two sides. The amount of reactivity which might require control would be approximately 23%. This value is based on a full storage rack loading of 148 fuel assemblies containing 6 plates each of the 0.271-in. thick, 1.4% enriched type. The value also includes a realistic leakage, based on the slab geometry of the storage rack, and a maximum possible error in the

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fuel reactivity calculations of 7%. Some degree of pessimism is also included, since no credit has been taken for burnup or for the xenon and samarium buildup occurring during irradiation.

The worth of the boron-stainless steel was determined by comparing its absorption to that of the water in the fuel assembly and multiplying the ratio by the reactivity controlled by the water in the fuel assembly (15% - for previous calculations):

$$\rho = \frac{\Delta k_{\text{eff}}}{k_{\text{eff}}} = \frac{0.15 V_2 \Sigma_{a_2} d}{V_w \Sigma_{a_w}} \quad (1)$$

where:

V_2 = volume of boron-stainless steel per assembly (24 in.³)

Σ_{a_2} = macroscopic absorption cross section of the boron-stainless steel (3.056 cm⁻¹)

V_w = volume of water per assembly (485.85 in.³)

Σ_{a_w} = macroscopic absorption cross section of the water (0.11959 cm⁻¹)

$d = \frac{\bar{\phi}_{\text{Boron}}}{\bar{\phi}_{\text{Assembly}}} =$ disadvantage factor for the boron-stainless steel.

The value of d was obtained in two steps. First, a value for the ratio of the average flux in the boron-stainless steel to its surface flux was obtained from existing transport theory solutions. This value (d_2) was found to be about 0.4.

Then, the ratio of the flux on the surface of the boron to the average flux in the fuel assembly was calculated from a solution of the diffusion equation with appropriate boundary conditions. The equations used for this step are shown below:

$$D \frac{\delta^2 \phi_1}{\delta X^2} - \Sigma_{a_1} \phi_1 + q = 0 \quad (2)$$

$$\frac{\delta \phi_1}{\delta X} = 0 \quad X = 0 \quad (3)$$

$$-D_1 \frac{\delta \phi_1}{\delta X} = d_2 \phi_1 \Sigma_{a_2} t_2 \quad X = a \quad (4)$$

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where:

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D = diffusion coefficient for the fuel assembly
(- 0.23953 cm)

ϕ_1 = flux in fuel assembly

Σ_{a1} = macroscopic absorption cross section of the fuel
assembly (0.12597 cm⁻¹)

q = thermal neutron source in the fuel (neutrons/cc)

t_2 = half thickness of boron-stainless steel

$d_2 \phi_1(a) \Sigma_{a2} t_2$ = total number of neutron absorbed in the boron-
stainless steel per sq cm of area.

The above system was solved assuming the fuel cell to be an infinite slab 4 in. thick with boron-stainless steel on the faces. The disadvantage factor d_1 was then determined as

$$d_1 = \frac{\phi_1(a)}{\phi_1}$$

The product of d_1 and d_2 is the desired over-all disadvantage factor, d , and was found to be 0.22.

The resulting value for ρ was 25.5%.

d. Fuel Assembly Shear - J. N. Young

In the interest of minimizing the size and weight of the fuel shipping coffin, the locating end fittings and top handling fittings will be removed from the irradiated assemblies in the storage pit and only the fuel plates and adjacent side plates will be handled in the shipping coffin. Removal of fittings is accomplished by shearing the side plates just above and below the fuel plates using a hand-operated shear.

The shear consists of a pair of hardened stainless steel blades mounted at the lower end of two 3-in. aluminum pipes, each 22 ft long. The pipes are sealed at the ends, and the resulting buoyancy causes the device to float at approximately the correct level for use. The blades move horizontally and are provided with a double angle to prevent the side plate from slipping out during shearing. The shear pivot has a rolling action which eliminates the need for lubrication. The 5-ft long tubular handles, with which the horizontal shearing torque is applied, are removable to facilitate storage.

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e. Fuel Shipping Coffin - S. A. Bernsen, J. N. Young

After a suitable decay period in the storage pit, the fuel assemblies will be removed from the pit and transported to a recovery plant in a shipping coffin. The coffin is cylindrical and has an 11-in. radial thickness of lead and a 1-in. thickness of steel. The total weight is 14.7 tons. The coffin is designed to receive four fuel assemblies with end fittings removed.

Calculations indicate that the size and weight of the shipping coffin would be excessive if the coffin were required to meet the Interstate Commerce Commission regulation of 10 mr/hr at one meter from the source using fuel assemblies just removed from the reactor (after an assumed one megawatt heat generation for one year) with no decay period. Moreover, a special cooling system to remove decay heat would be needed. With the coffin as designed, the ICC regulation on shipment is met if an assembly which has operated at the peak flux for one year is allowed to cool three months prior to shipment. After such a decay period no special cooling provision is required, since heat release from the coffin surface is considerably less than the 0.5-watt per square inch value which is acceptable for electrical equipment.

f. Control Rod Handling Equipment and Procedure -
C. F. Bullinger

Control rods may be removed from and replaced in the core, without draining the pressure vessel, in the following manner.

The control rod drive housing, and seal housing assembly⁸ are removed from the bottom adapter, leaving the control rod connector and stationary stem exposed as shown in Fig. 11. The hold-down tool is then attached to the connector to prevent possibility of lifting it up off the valve seat in the adapter and allowing water to come streaming down out of the reactor.

The hold-down stem is threaded into the stationary stem. The hold-down yoke and thrust bearing are assembled over the connector and secured in place by threading the hold-down nut on to the external threads of the connector. The hold-down lock nut is then threaded on and the hold-down stem nut is attached to the end of the stem with a set screw. All threads are made up finger tight to allow free rotation of the connector 45 degrees counter-clockwise to permit disengagement from the control rod, without allowing upward movement. Match marks with crayon or ink are made on the hold-down yoke and nut to facilitate location for reassembly.

⁸Reactor Engineering Division Quarterly Report, ANL-5511
(January 1956), Fig. 3.

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FIG. II
EBWR CONTROL ROD CONNECTOR
HOLD DOWN TOOL

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The control rod is now ready for removal from the core. Transfer and replacement of control rods in the core will be accomplished with the retrieving tool shown in Fig. 12.

The retrieving tool is a quick - disconnect type, made from hardenable stainless steel, engaging an Inconel male member welded to the top of the control rod. When the control rod is in the shut down position the guide structure extends approximately 4 ft above the top of the rod. Therefore, a retrieving tool must be limited in size to one inch diameter.

The load of the control rod is transmitted to the lifting tool through steel balls retained in a sleeve (A). As long as lift is imparted through the inner member the balls are confined to a diameter which forces them into contact with the lifting shoulder of the control rod spindle which permits the control rod to be raised. If the lift is transmitted through the outer sleeve, this sleeve rides up until stopped by a pin through the inner rod at (B). At this point, the diameter of the outer sleeve opposite the balls is large enough to permit their movement outward so that they will be disengaged from the lifting shoulder. The tool can then come away free of the control rod.

Due to design geometry, lift must be applied on the outer tube member. Therefore it becomes necessary to lock the inner and outer parts in their proper relation before lifting. This is accomplished by screwing down on a captive nut with the extension wrench shown. This action places the inner lifting member in tension and the outer member in compression so that it can take the load through the cable ears.

The extension wrench is also used to guide the retrieving tool into the control rod channel as it swings on the ends of the 1/8-in. aircraft cable. It is removed after engagement is effected. The control rod is lifted by the two cables which are suspended over two Micarta sheaves attached to a bracket mounted on the overhead crane trolley. The cables then run back to a winch mounted on the wall behind a temporary shield behind the turbine. All personnel other than those behind the shield are evacuated from the area during transfer of the control rod. After the rod is raised clear of the floor it is moved slowly over to the storage pit with the crane while the supporting cable is paid out as required. The rod is then lowered into a corner of the pit in a vertical position underwater and the retrieving tool removed.

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FIG. 12
EBWR EXTENSION WRENCH AND
CONTROL ROD RETRIEVING TOOL

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B. Status of Construction

1. Fuel Plates and Assemblies

Corrosion Testing - C. R. Breden

All EBWR plates and assemblies are being corrosion tested to prove clad integrity from the standpoint of holes or faulty spots and from the standpoint of serious contamination of the Zircaloy 2. Present plans are to test each plate and each assembly for two weeks, respectively, in 550F degassed, demineralized water.

These tests are being carried out in four large autoclaves equipped with immersion heaters, mixed bed ion-exchangers (Rohm & Haas XE-150) and the required instrumentation. The nominal dimensions of the autoclaves are tabulated below; actual capacity values differ because of the presence of heaters, baffles, domed lids, etc.

Autoclave No.	Inside Diameter, in.	Inside Height, ft	Capacity	
			Plates	Assemblies
1	14	5	40	3*
2	14	5	40	3*
3	12	6	40*	2*
4	22	13	120	20(2 layers)

*Estimated

With autoclaves No. 1, 2 and 3, where the vessel can be easily cleaned with a suction cleaner, open baskets (Fig. 13) are used with additional plates standing alongside the baskets. With autoclave No. 4, (Fig. 14) which cannot be cleaned easily because of a bolted-in plate above the heaters, the plates (and assemblies) are placed in boxes with Neva-Clog filters in the bottom. These boxes (Fig. 15) will retain a high percentage of the UO_2 produced if a plate should rupture and, at least until rupture occurs, will permit circulation of the water through the ion-exchanger. The plates in the boxes are separated by stainless steel spacers (Fig. 15) to insure circulation of water past the plates.

The loading of a number of enriched plates or assemblies together in water introduces the question as to whether or not the mass may become critical. Calculations show that if all cross sections, constants, and methods are in error by the maximum quoted amounts in a direction which adds reactivity, one would calculate 8.5 assemblies, or 51 plates, as being critical. Accordingly, the 10 boxes of 60 plates in one layer of autoclave No. 4 might become critical. The second layer, separated by

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FIG. 13
OPEN BASKET WITH PLATES
BEING LOADED IN AUTOCLAVE
NO. 1. AUTOCLAVES NO. 2
AND 3 IN BACKGROUND

FIG. 14
BOX-TYPE TEST ASSEMBLY
BEING LOADED IN
AUTOCLAVE NO. 4

FIG. 15
BOX-TYPE TEST ASSEMBLY

FIG. 16
BOX SUPPORT RACKS.
SOLID SHEETS
CONTAIN CADMIUM

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about 15 in. of water and Zircaloy from the first layer (core to core), was not considered to add to the problem, particularly since it was planned to add cadmium strips to the structure supporting the baskets in both the first and second layers (Fig. 16).

In order to detect any approach to criticality and to test the effectiveness of poison inserted, the first loading of the first layer of enriched plates was made without the cadmium and with a neutron source and detector at approximately opposite sides of the inside diameter of autoclave No. 4. The boxes loaded with plates (six plates per box, except one box containing eight) were added one at a time, and the activity was surveyed continuously. Although there appeared to be a multiplication of about 5 when all of the boxes were in, the results were not clear-cut, since lowering the water level approximately 2 ft below the top end of the plates did not affect the rate of activity. The experiment, however, showed that a full loading of enriched plates in one layer without cadmium was not critical. Since enough cadmium to hold nine assemblies in EBWR sub-critical will be added to each of the two structures supporting the boxes, the test appears to be safe.

The first 18 natural uranium production plates have received a two-week test in 550F degassed, demineralized water in autoclave No. 1. After about two days an increase in pressure (due to H₂ buildup) indicated a probable plate rupture. After five days the autoclave was opened and one plate (No. 290) was found to be ruptured on both sides near one edge near the center of the plate (see Fig. 17). The other plates were returned to test to finish the two-week period without further incident.

2. Control Rod Drive Mechanisms - C. F. Bullinger

All parts and spares for the nine operational control rod drives⁹ have been fabricated and are undergoing acceptance test at operating pressures and temperatures. As of March 30, three assemblies have been tested and accepted. Each unit is tested for a minimum period of 240 hr at 600 psi. Seal leakage rates vary between 36 and 164 cc/min, and scram times for 43 in. of travel to contact with dashpot plunger vary between 0.332 and 0.348 sec.

Five items comprise the major source of expense and difficulty of fabrication: the thimble flange adapter, seal housing, screw drive housing, lead screw, and the extension rod.

The thimble flange adapter and the seal housing are hogged out of solid Type 304 stainless steel round stock (10½ in. dia.). A cost analysis of methods of construction revealed that this method was the most

⁹Reactor Engineering Division Quarterly Report, ANL-5511, Section II (January 1956), Fig. 3.

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FIG. 17
RUPTURE OF EBWR PRODUCTION FUEL PLATE AFTER
TWO WEEKS IN 550 F DEGASSED, DEMINERALIZED WATER

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economical for the limited quantity of pieces involved. The pieces are rough turned, stress relieved, and then finished. Although the various machining operations are time consuming, no difficulties have been encountered.

The screw drive housing is machined from a Meehanite casting. Machining the ways and grooves to accept the tongues on the latch carriage is accomplished with a long-travel milling carriage. Maintenance of tolerances and clearances between the carriage and ways of the casting is rather difficult. However, a somewhat loose fit is acceptable, as it has been noted that slight tendencies toward vibrations have disappeared after the units have been operated for several hundred cycles. Efforts to isolate the cause of this vibration have been unsuccessful. All units have operated very smoothly by the time they are accepted.

The SAE 4340 5 TPI Acme lead screw with an over-all length of 60 in. has been rather difficult to keep straight. The 0.200-in. lead screw was originally chosen for reasons of position indication. In the future it may be advisable to use a 4 TPI Acme thread since this type of lead screw is available in $1\frac{1}{4}$ -in. and $1\frac{1}{2}$ -in. diameters from lathe manufacturers at a very nominal cost. The 17-4 PH chrome-plated extension rod (1.000-in. dia.) has been difficult to fabricate because of the inability of plating concerns to apply a uniform coating of chrome to a close tolerance. Therefore it has been necessary to overplate and grind down to dimension. The quality of the plating has been very good, as indicated by less than 0.0001-in. wear on the rod diameter after 7500 cycles on a prototype rod.

Much credit must be given to the Argonne Central Shops Division for the fact that not one major item was scrapped during production of the nine assemblies.

A cost summary of the control rod drives and related equipment is given below.

Labor (9,964 hr at \$5.50)	\$ 54,802.00
Raw Material	12,932.99
Purchased Parts	6,189.59
Total for 9 drives	\$ 73,924.58
Average cost of each drive, complete	\$ 8,213.62

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3. Pressure Vessel - N. Balai

The execution of the EBWR pressure vessel design has posed many difficult shop fabrication problems. Generally, the actual shop time required was two to three times longer than originally estimated. It was found necessary to reschedule fabrication from a five-day three-shift basis to a seven-day, three-shift basis in order to ship the vessel by the end of May and thus to meet the requirements of the construction schedule. The status of the individual vessel components is as follows.

Upper Ring Forging

Although previously reported completed, in fitting up the forging to the shell, the inside diameters were found to mismatch by $7/8$ in. After welding the circumferential seam, the discontinuity between the two carbon steel parts was reduced by depositing carbon steel weld metal. A uniform transition between the mismatching diameters was affected by grinding the weld metal deposit. Cladding was then applied manually to cover the bare carbon steel surfaces. The rough cladding welds were likewise ground to provide a uniform transition in the knuckle zone.

Shrinkage of the circumferential weld between the ring forging and the shell was much greater than expected. Measurements were necessary to establish the minimum wall thickness in the weld. A minimum carbon steel wall of $2\frac{3}{8}$ in. is required; a $2\frac{5}{8}$ -in. wall was found.

Lugs to support the upper shielding ring were welded to the ring forging. After grinding the rough welds, dye-checking revealed pinhole flaws which were repaired.

The internal shielding ring segments suspended from the shoulders of ring forging were rolled, machined and drilled for attaching bolts.

The exterior support lugs for the support of the finished pressure vessel were attached to the forging by $1/2$ -in. fillet welds. The undersides of the pads were machined off in a boring mill to yield a common mounting plane.

Shell

The ring forging at the upper (closure) end of the vessel was welded to the shell, as described earlier. Radiographs of this weld, before and after cladding, show a very high quality weld, i.e., the slag inclusions are very small, few in number and widely separated from each other. Other operations included:

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(a) The attachment of brackets for the support of internal distribution rings (steam, feed water and poison line), thermal and shock shields, and spacer buttons to maintain an annulus between the thermal shield and vessel wall.

(b) The drilling of holes in the wall for nozzle connections (except the two 12-in. connections in the lower end) and the removal of bonded cladding around the nozzle connections. After nozzles are welded in, stainless steel Type 304 rings are to be welded to the bonded cladding and the nozzles to allow for the differential expansion between the stainless steel nozzles and the carbon steel walls of the pressure vessel, i. e., the rings are Belleville springs.

(c) The fusion welding of carbon steel rings to the vessel wall at nozzle perforations. The main purpose of these rings is to provide a location of low restraint for the Inconel welds between the carbon steel vessel wall and Type 304 nozzles.

Lower Head

Fabrication of the lower head assembly was hampered by a geometry irregularity. In the heat-spinning operation, the metal was "balled" up in the knuckle region of the ellipsoidal head which resulted in a 1.88:1 instead of the design 2:1 elliptical head ratio. The geometry aberration required redesign and refabrication of most of the anchoring pads affixed to the lower head. Radiographs of the inside and outside fillet strength welds revealed many defects. Inside welds were the most troublesome. Fabrication completed included:

- (1) Welding in of all thirteen lower head nozzle stubs.
- (2) Radiographing and dye-checking of carbon steel and stainless steel Type 304 cladding welds.
- (3) Stress relief of head following welding.
- (4) Deposition of additional weld metal to the control stubs and machining of bores to maintain control rod thimble centers to compensate for the distortion of centers by welding and stress relief operations.
- (5) The grid plate was aligned with the control rod thimbles. The maximum mismatch of the centerlines was 0.018 in., which meets the specified 0°-6' perpendicularity requirements.

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(6) A preliminary stress analysis of the knuckle showed that the combined stress at the 1200 psi hydrostatic test pressure was 33,100 psi (20,000 psi binding plus 13,100 psi hoop stress). Since the yield strength of the plate is 45,500 psi, yielding of the head structure is not expected to destroy the machining tolerances required.

(7) The head was fitted to the shell for welding. The mismatch between the two subassemblies was 7/16 in. on the inside diameter. The discontinuity between the lower head and shell is to be eliminated by depositing carbon steel and stainless steel weld metal deposits.

Control Rod Thimbles and Lower Head Internals

All control rod thimble assemblies, including external shield cans, were welded and the welds were X-rayed. Shield cans were filled with lead and sealed off. In fitting the internal thimble shielding it was discovered that the lead had swaged the diameters of the thimbles enough to require grinding of the bores.

A material and design change were instituted to incorporate stainless steel Type 304 containing 1 wt-% boron in the control rod guide assembly to provide additional shielding of the lower head. Difficulties in welding the boron-containing steel resulted in abandonment of the substitution. Profiles of the lower head also showed that there was enough excess metal to satisfy shielding requirements with stress levels (including thermal stresses) well below Code allowable stresses.

The control rod guide assembly was fitted into the lower head control rod thimble assembly for a check on alignment before welding the completed thimble assemblies to the control thimble stubs.

Grid Plate

The grid plate was rough drilled and heat treated to relieve machining stress and to stabilize the stainless steel Type 304 alloy against warping. The grid plate is completely machined and has been shipped to the Laboratory. The drilling of cooling holes was waived when calculations indicated they were unnecessary. Tapped holes for fuel tube locking mechanisms were also eliminated from the drilling pattern after design changes were made in the fabrication of fuel elements.

The original 6-in. thickness was reduced to $5\frac{3}{4}$ in. to provide machining stock to meet flatness requirements. Hold-down bolts were increased in size to compensate for misalignment of the drilled holes occasioned by walking of the drill through $5\frac{3}{4}$ in. metal thickness.

A rolled ring was added to the grid plate to increase the diameter from 60 to 62 in. to permit centering and seating of a future shroud for forced circulation.

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6.1.
A master template of the grid plate was prepared for reference use in the fabrication of future grid plates. The template is an exact replica of the grid plate and was machined using the finished grid plate as a template. Only the anchor bolt and control rod slots are reproduced.

Miscellaneous Items

All internal ring headers (steam, poison line and feed water) were completed and ready for installation.

A structural frame containing 8-WF column section posts was welded to the lower section of the vessel to serve as a jig for the alignment of the control rod thimbles with the control rod stubs welded into the lower head. The frame will also serve as a protective and alignment device for shipping.

Gaskets initially ordered were found to be bonded with rubber. Tests at the B & W Research Laboratories at Alliance, Ohio, showed that the rubber-bonded gaskets failed in a thermal environment. Gaskets were ordered free of organic binders. Original gaskets are to be used in the early operations of the EBWR facility when frequent shutdowns are envisioned.

Welder and process qualification test plate samples were examined for quality. The weld metal deposits and base plate materials met the ASME Boiler Code requirements for SA-212, Grade B materials for strength and ductility.

The engineering of the carrier plate assembly and shielding plugs was completed and their fabrication was started.

4. Containment and Shielding Components - E. H. van Zylstra

Fabrication of the containment structure at Rock Island Arsenal has been completed and delivered to the Argonne site. Placement of the structure in the power plant building is scheduled after the main floor concrete has been poured.

One hundred and nine of the 120 tons of steel punchings and 160 tons of fine magnetite iron ore, required for shielding concrete, have been delivered to the building site. Forty tons of fine magnetite were delivered to Plant Engineering for use in manufacturing shielding blocks for the pipe tunnels. Two hundred tons of coarse magnetite iron ore are on order and scheduled for early delivery.

Fabrication of the top shield tank has been completed at Rock Island Arsenal, and the unit is ready for shipment.

The shield forms for the two 12-in. recirculation outlet lines have been partially fabricated.

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All material for the bottom shield plug is on hand. The shield plug shell has been completed, and the internals and cooling coils are being fabricated. An order has been placed for the 13 expansion joints below the bottom shield forms.

The curved lead bricks for the gamma shield surrounding the inner shield form were received, inspected, and found unacceptable from the viewpoint of meeting drawing tolerances. The bricks were re-worked to satisfactory dimensions by the vendor and returned to storage at Argonne pending installation.

All materials for the instrument hole shielding plugs have been received, and fabrication is proceeding on schedule.

5. Steam Plant and Auxiliaries - V. C. Hall, A. S. Jameson,
E. A. Wimunc, C. Zitek

The review, modification and approval of manufacturers' and architect engineers' drawings of the steam plant, components, and system piping has continued.

The fabrication of the steam dryer-emergency cooler was completed. All welded joints were X-rayed and welds not meeting the ASME Boiler Code requirements were repaired accordingly. A 20-min hydrostatic test (1550 psi initial - 1490 psi final) proved the design and integrity of the vessel. (The 60-psi drop in pressure was caused by a slight leak through the shop-made gasket fabricated from sheet asbestos packing.) The hydrostatic test was witnessed by the Hartford Insurance Company inspector.

There was insufficient helium available to perform a leak test; a vacuum test was substituted. The vacuum test showed that inleakage from all sources was nil - a vacuum of 0.5 micron held steadily for a 12-hour period. The vessel was shipped to Argonne.

The twin, two-stage air ejectors were also delivered to the site after satisfactory leakage tests at the vendor's plant. The components of the steam by-pass control system were assembled by the manufacturer to test the control characteristics of the system. The elements of the air-drying and recovery system were also assembled for shop testing to check out the mechanical and control features.

6. Power Plant and Service Buildings - A. H. Heineman

Agreement was reached with the general contractor to proceed with furnishing materials and installing supplemental features in the immediate vessel surroundings as incorporated into the architect-engineers' drawings and specifications.

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The final concrete pour completing the supporting slab under the bottom head of the shell was made late in January. After a 21-day curing period, the temporary steel support columns for the shell were severed by burning out a section approximately 6 in. long from each column. The entire load of the power plant building is thus assumed by the concrete slab.

Prior to back-filling operations, a layer of commercial insulating board (1-in. thick) was applied over the bituminous enamel on the outside of the shell below grade. This board protects the enamel against abrasion during back-filling. Back-filling is permitted only in areas where the 24-in. thick concrete outer wall, or envelope, inside the shell has been poured and allowed to set for a few days. Back-filling operations were started early in March.

Form work, placing of reinforcing steel, and pouring of concrete continued throughout the quarter. This work had advanced to the following extent:

(1) The 24-in. thick outer wall was poured to elevation 4 ft-0 in. below finished main floor, except for the last 7-ft pour at the West section (which was completely formed and ready to pour) and the last 11 ft of the section which forms a part of the fuel storage well. (Openings left at the small temporary opening and cable pans pending final welding have not been filled).

(2) The storage pit and octagonal wall around reactor were brought up to a point 15 ft-0 in. below the finished main floor.

(3) Turbine support columns were poured as high as the condenser floor.

(4) The basement floor slab, pump floor slab, and condenser floor slab were completed, except for the pockets in the basement floor for containment column footings.

(5) Service building columns were completed to the basement floor beam elevation.

(6) The pipe well support (outside at southeast) were poured to approximately 12 ft-0 in. below grade.

(7) Forming and reinforcing steel was virtually completed for the balance of the storage pit, octagon, and turbine support columns up to 4 ft-0 in. below the finished main floor.

All pipe lines, pipe sleeves, conduits, and tunnel liners which are embedded in concrete were installed in advance of the various pours.

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The openings were cut out and the main air-lock and emergency air-lock were welded in place. The welds were spot-radiographed prior to pressure tests, and sound welds were indicated. Just prior to the end of the quarter the temporary opening (5 x 10 ft) below grade was closed and welded. These welds were also spot-radiographed, with similar results.

In order to test these new shell installations, the freight door was closed and bolted, and all other openings to the shell were sealed. (The main air-lock had been delivered without doors installed but with a temporary closure dome on the outside.) The emergency air-lock, which was complete with doors and mechanism, was used for final exit and its outer door sealed. The shell was then pressurized to about 10 psig and all new welds were tested with soap solution; no leaks were observed. The pressure was then increased to 18.75 psig for a strength test of the new welds and parts. After these tests were witnessed the shell was depressurized.

Reinforcing members were welded to the shell around the large (12 ft x 18 ft) temporary opening at grade level, and the large opening was cut out. This large opening provides access for large items of equipment, i. e., the condenser, reactor vessel, turbo-generator and containment columns and footings.

Back-filling had proceeded approximately up to the elevation of the service building basement floor slab (about 18 ft below grade) most of the way around the building. At the freight door and large temporary opening, ramps were brought up to a somewhat higher elevation to reduce the amount of cribbing and bridging required to gain access to these openings.

Preparations for moving the condenser into the building are under way.

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II. SUPPORTING AND ALTERNATE DESIGN RESEARCH AND DEVELOPMENT

A. Boiling Heat Transfer

1. Single and Multi-Channel Natural Circulation Boiling at 600 psig - W. H. Cook, J. F. Marchaterre

a. Ratio of Velocities for 24-in. and 48-in. Channels

Traverses were made on a 24-in. long channel (cross section: $7/16 \times 3-11/16$ in.) to determine the effect of channel length on density. Earlier experiments with a 48-in. long channel of identical cross section showed a definite change in slip ratio with length during a series of tests with saturated inlet conditions. Inlet values of the velocity ratio (or slip ratio) were determined from a linear extrapolation of the curve to inlet conditions. Typical plots are shown in Fig. 18.

FIG. 18
CHANGE OF PHASE VELOCITY RATIO WITH CHANNEL
POSITION FOR SATURATED INLET TEMPERATURES

The velocity ratio or slip ratio results of the 24-in. channel measurements at the outlet of the heated channel are plotted in Fig. 19 along with inlet and exit curves of c_s/c_l for the 48-in. channel. This indicates that exit c_s/c_l values for the 24-in. channel are simply the c_s/c_l values for the 48-in. channel at the 24-in. level.

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FIG. 19
EFFECT OF CHANNEL LENGTH UPON
THE RATIO OF PHASE VELOCITIES

The spread of density data for the 24-in. channel at the 24-in. level is attributed to an error in experimental procedure. The density recorder sensitivity was not adjusted to compensate for the decay of the thulium source; thus the signal obtained in calibrating the density traces for the system full of water and, alternately, full of steam gradually decreased. This accentuated the errors in transcribing intensity signals from tape records and produced void fraction readings which appeared erratic. However, runs in which the error was corrected verified the position of the c_s/c_l vs. $\frac{v_l w_t}{A} (1-x)$ curve.

Various correlations of velocity ratio have been attempted. One of the most useful to date has been the c_s/c_l vs. v_{in} correlation.¹⁰ Since $\bar{x}_{bl}$ can be correlated as a function of $\frac{w_t h_{fg}}{Q_t} \frac{L}{L_b}$ it is reasonable to assume that the velocity ratio could be correlated in terms of the same parameters. This has been done for all the available 600 psig data to date. The resulting plot is shown in Fig. 20. The range of variables is listed as follows:

¹⁰

Reactor Engineering Division Quarterly Report, ANL-5471
(September, 1955) p. 80.

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Channel Size: 3-11/16 x 7/16 in. multiple channel
(48 in. and 24 in. long)

2 x 1-2 x 60 in. single channel
(Armadilla data)

Inlet Subcooling: 0 - 30F

Exit Quality: 0 - 10%

Inlet Velocity: 0.7 - 3 fps

Power Density: 10 - 50 kw/liter

FIG. 20
CORRELATION OF VELOCITY RATIO c_s/c_q
WITH THE PARAMETER $Q_T/W_T h_{fg}$

b. Void Distribution in Riser

A series of experimental runs have been made to determine the distribution of voids at a horizontal cross section in the riser section. Preliminary data on a 2 in. x 4 in. riser indicates the steam tends to channel and flow upwards in the center of the riser. A typical horizontal distribution is shown in Fig. 21.

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FIG. 21
TYPICAL DISTRIBUTION OF STEAM
VOID FRACTION ACROSS CHANNEL
FOR UNHEATED RISER SECTION

2. Two-Phase Pressure Drop - W. S. Flinn, P. A. Lottes

It has been postulated that the increase in friction due to the flow of a two-phase mixture might be due to the increased velocity of the water phase and the associated increase in drag at the walls of the channel or tube.

Calculations were made to compare this flow model with data taken from the Armadilla Test Facility.

If the friction multiplier is a function of steam volume fraction and independent of pressure and flow rate as shown in ANL-5561,¹¹ then it might be true that for small values of x

$$R = C \left(\frac{1}{1 - \alpha} \right)^2 ;$$

therefore,

$$\bar{R} \frac{f_{0L} L (V_{in})^2}{2g} \left(\frac{L}{D} \right) = f_b \frac{\rho L}{2gD} \int_0^L V_L^2 dL = f_b \frac{\rho L (V_{in})^2}{2gD} \int_0^L \left(\frac{1}{1 - \alpha} \right)^2 dL$$

¹¹ Reactor Engineering Division Quarterly Report (April, 1956) p. 65.

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The variation of λ with length was known from previous data. The above integral was evaluated for various exit steam volume fractions. The results are shown in Fig. 22. Curve (A) is taken from Armadilla data from 150 to 600 psia, reported in ANL-5561. Curve (B) is the calculated $\bar{R}$ based on the above arguments. Both curves have the same slope but differ by a factor of about 3.6, indicating the possibility of a constant increase in actual surface friction due to the roughness and turbulence of the boiling process.

FIG. 22
TWO-PHASE BOILING FRICTION FACTORS

3. Effect of Restrictions on Steam Voids for Natural Circulation Boiling - W. S. Flinn, P. A. Lottes

A series of tests was made with the Armadilla Facility to investigate, qualitatively, the effect of downcomer restrictions on the steam volume fraction in the riser for fixed power levels.

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The recirculating velocity was adjusted with a butterfly valve near the inlet of the test section. The results of these tests are shown in Fig. 23. In general, restrictions seemed to increase the steam volume fraction although in some cases the changes were not detectable.

FIG. 23
AVERAGE VOID FRACTION AS A FUNCTION OF
INLET VELOCITY AND PRESSURE
IN 1/2 x 2 x 60 in. SECTION

4. Controllable a-c Power Supplies Using Ignitrons - H. H. Hooker

With reference to the accuracy of measurement of the electrical output from power supplies in which output is controlled by variation of the firing angle of ignitron tubes, an analysis of the harmonic content of the output of these supplies has been made. Such a power supply is used, for example, for test section heating in the 600-psi boiling density facility.

The diagram below shows a power supply, of the type described above, connected to a load consisting of inductance in series with resistance. Two ignitrons are connected in inverse parallel, together with the attendant ignitor firing controls.

Control of output power is effected by control of the ignitor firing angle. Thus, ignition conduction is permitted only during a selected portion of each half-cycle of the applied alternating voltage.

The time relationships obtaining between the supply voltage and load current during one cycle of operation are plotted below.

Conduction through one ignitron is initiated at time $t = 0$. Current then flows until it falls to zero after a half-cycle of conduction. Subsequently, a similar negative half-cycle of current flows through the second ignitron. The negative half-cycle of current begins at time $t = \pi/\omega$.

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Referring to both illustrations and assuming a sinusoidal supply voltage, the conditions during conduction in the positive half-cycle may be written

$$\begin{aligned} e &= E_m \sin(\omega t + \phi) \\ &= L \frac{di}{dt} + Ri \end{aligned} \quad (1)$$

where

e = instantaneous value of voltage

E_m = peak voltage

ω = $2\pi \times$ frequency

t = time

ϕ = ignitron firing angle, measured after voltage passes through zero in a positive-going direction

i = instantaneous value of current

L = inductance of load

R = resistance of load.

Subject to the condition that $i = 0$ when $t = 0$, the solution of Eq. (1) for the positive half-cycle is

$$i = I_{om} [e^{-\omega t \cot \psi} \sin(\psi - \phi) + \sin(\omega t + \phi - \psi)] \quad (2)$$

where

$$I_{om} = \frac{E_m}{\sqrt{R^2 + \omega^2 L^2}}$$

and is equal to the peak steady-state current that would flow if a full sine wave of voltage were applied to the load;

$$\psi = \tan^{-1} \frac{\omega L}{R} \quad .$$

Equation (2) is valid between time $t = 0$ and time $t = t_1 = \alpha/\omega$, when the current falls again to zero. A similar expression applies to the following negative half-cycle. Thus,

$$i = -I_{om} [e^{-(\omega t - \pi) \cot \psi} \sin(\psi - \phi) + \sin(\omega t + \phi - \psi - \pi)] \quad (3)$$

Similarly, Eq. (3) is valid between $\omega t = \pi$ and $\omega t = \pi + \alpha$. If we add that

$$i = 0$$

when

$$\alpha \leq \omega t \leq \pi$$

and

$$\pi + \alpha \leq \omega t \leq 2\pi, \quad (4)$$

the current wave is completely defined over the interval

$$0 \leq \omega t \leq 2\pi.$$

Moreover, the function is periodic with a period of 2π from which it follows that a Fourier series which describes the function over one period is valid for all values of ωt . We may say, then,

$$i = I_{om} \left[\frac{a_0}{2} + \sum_{n=1}^{\infty} (a_n \cos n\omega t + b_n \sin n\omega t) \right], \quad (5)$$

where

$$a_n = \frac{1}{\pi} \int_0^{2\pi} i(\omega t) \cos n\omega t \, d\omega t \quad (6)$$

$$b_n = \frac{1}{\pi} \int_0^{2\pi} i(\omega t) \sin n\omega t \, d\omega t. \quad (7)$$

Substitution of the expressions for $i(\omega t)$ in Eqs. (2), (3), and (4) in Eqs. (6) and (7) yields

$$a_n = \frac{1 - \cos n\pi}{\pi} \left\{ \frac{\sin(\psi - \phi) [e^{-\alpha \cot \psi} (n \sin n\psi - \cot \psi \cos n\psi) + \cot \psi]}{\cot^2 \psi + n^2} + \frac{\cos(\phi - \psi) - \cos(\alpha - n\alpha + \phi - \psi)}{2(1 - n)} + \frac{\cos(\phi - \psi) - \cos(\alpha + n\alpha + \phi - \psi)}{2(1 + n)} \right\} \quad (8)$$

and

$$b_n = \frac{1 - \cos n\pi}{\pi} \left\{ \frac{\sin(\psi - \phi) [e^{-\alpha \cot \psi} (-\cot \psi \sin n\psi - n \cos n\psi) + n]}{\cot^2 \psi + n^2} + \frac{\sin(\alpha - n\alpha + \phi - \psi) - \sin(\phi - \psi)}{2(1 - n)} + \frac{\sin(\phi - \psi) - \sin(\alpha + n\alpha + \phi - \psi)}{2(1 + n)} \right\}. \quad (9)$$

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In the unity power factor case ($\psi = 0$) these expressions reduce to

$$\overline{a}_n = \frac{1 - \cos n\pi}{\pi} \left[\frac{\cos \phi - \cos n\phi}{1 - n^2} \right] \quad (8a)$$

and

$$\overline{b}_n = \frac{1 - \cos n\pi}{\pi} \left[\frac{-n \sin \phi + \sin n\phi}{1 - n^2} \right] \quad (9a)$$

Eqs. (8a) and (9a) are indeterminate for $n = 1$. In this case we have

$$\overline{a}_1 = \frac{(\pi - \phi) \sin \phi}{\pi} \quad (8b)$$

and

$$\overline{b}_1 = \frac{1}{\pi} [(\pi - \phi) \cos \phi + \sin \phi] \quad (9b)$$

It will be noted that only the odd harmonics are present.

Now the rms value of the n 'th harmonic component of current can be expressed

$$I_n = I_{om} \sqrt{\frac{a_n^2 + b_n^2}{2}} \quad (10)$$

and the ratio of the amplitude of the n 'th harmonic to that of the fundamental or first harmonic is

$$\frac{I_n}{I_1} = \sqrt{\frac{a_n^2 + b_n^2}{a_1^2 + b_1^2}} = H_n \quad (11)$$

Numerical values for a_n and b_n have been computed for harmonics up to and including the ninth for the case of the non-inductive load ($\psi = 0$) with ϕ (firing angle) as a variable. Figure 24 shows the values of

$$\sqrt{a_n^2 + b_n^2}$$

plotted as a function of ϕ for these harmonics. In Fig. 25 the values of H_n are plotted as a function of ϕ for the same harmonics. The numerical computations for the more general case of current flow through an inductive load are considerably more laborious and have not been completed. It should be noted that while Figs. 24 and 25 apply to the current flow through a non-inductive load only, they are applicable to the terminal voltage of this

FIG. 24
 AMPLITUDE OF $\sqrt{a_n^2 + b_n^2}$ VS FIRING ANGLE

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FIG. 25
 H_n VS FIRING ANGLE

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type of power supply when used with any sort of load, provided that the supply possesses negligible internal impedance. It happens, moreover, that the potential circuits of conventional alternating current instruments exhibit a more severe reduction of response with increasing frequency than the current circuits of such instruments.

In particular, if for a given instrument the voltage response as a function of frequency is known, the relative response to a complex wave consisting of a fundamental component plus higher harmonics may be computed from

$$\frac{E_i}{E} = \sqrt{\frac{\sum_{n=1}^{\infty} (P_n H_n)^2}{\sum_{n=1}^{\infty} (H_n)^2}}, \quad (12)$$

where

E_i = indicated rms voltage

E = actual rms voltage

P_n = fractional response of instrument to the n 'th harmonic

H_n = fractional amplitude of the n 'th harmonic.

B. Water and Steam Conditions During BORAX-III Operation - V. C. Hall, C. B. Zitek

Operation of BORAX-III was terminated on April 7 to allow sufficient cooling time prior to unloading the fuel elements. The BORAX-IV core is scheduled for installation in the same reactor vessel during the month of June.

The intermittent operation of BORAX-III has resulted in the accumulation of considerable data on water and steam cycles, in addition to the actual production of electrical power. These data are discussed in the following sections.

1. Clean-up Loop Operation

The clean-up loop consisted of a cartridge-type pre-filter unit, two ion-exchange columns (in parallel), and an after filter unit. The reactor contained 1000 gal of water; the flow through the clean-up loop was 8 gpm.

During the initial operation in July, 1955, an anion column and a mixed-bed column were used. The mixed-bed operation was not desirable because it permitted the reactor water pH to increase above 8.

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Because of the 2S Al cladding a lower pH (5 to 6) was necessary to minimize Al corrosion. A cation resin was substituted and, with the reactor operating at low power, the reactor water pH decreased to 5.0. However, when the turbine was added to the system, the pH increased to 6.5 due to condenser leakage. After correcting the leakage, the reactor water pH decreased to 5.0. The resulting specific resistance was low (150,000 ohms/cm³). The anion column was put in service and the water quality was increased to between 500,000 - 600,000 ohms/cm³ with a pH of 6.0 to 6.2.

In March, 1956, the anion resin was replaced with a mixed-bed resin with the idea of obtaining higher quality water. A run was made (disregarding pH) to determine maximum water quality attainable during reactor operation. The best quality obtained was 900,000 ohms/cm³ with a pH of 6.75. The pH was allowed to increase to 7.3, but the quality decreased to 540,000 ohms/cm³.

The decontamination factor (activity of reactor water/activity of effluent) across the cation column varied from 90 to 330 while that across the mixed-bed column was 1000 to 1400.

The major radioactive isotope in the reactor water after decay of N¹⁶ appeared to be Na²⁴. Other radioactive isotopes identified were A⁴¹, O¹⁹, Cl³⁸, Mn⁵⁶, Cu⁶⁴, Zn⁶⁵, and Ca⁴⁵.

BORAX-III clean-up loop operation indicates that there should be no trouble maintaining high-quality reactor water (above one megohm/cm³) in EBWR. The double-tube sheet construction of the condenser will restrict condenser leakage while the Zircaloy 2 cladding will eliminate the low pH requirement.

2. Entrainment of Activity

Initial studies indicated that the decontamination factor:

$$DF = \frac{\text{Activity of Reactor Water (cpm/milliter)}}{\text{Activity of Steam or Condensate (cpm/milliter)}}$$

varied with time after sampling. The decay curves for the reactor water, steam, and condensate are plotted in Fig. 26. The sharp slope of the curves during the initial decay period is probably due to vaporization of one of the reactor components or preferential carry-over of activity of short half-life.

FIG. 26
DECAY OF ACTIVITY IN BORAX III WATER, STEAM, AND CONDENSATE

Argonne Chemical Engineering Division personnel conducted an extensive study of DF on BORAX-III at various power levels. The data showed the DF (after 20 hr decay) to vary from 6×10^3 at 12 and 14 mw, to 1.6×10^4 at 4 and 8 mw.

The fact that the DF of the condensed steam and condensate were practically the same indicated that little, if any, radioactivity was being deposited in the turbine. The activity at the exhaust end of the turbine 12 hr after final shutdown was only 1 to 1.5 mr/hr on contact.

The high DF values determined on BORAX-III indicate that the EBWR turbine activity levels should not present a serious maintenance problem (discounting conditions after a fuel plate failure).

3. Water Decomposition

Determination of the water decomposition rate in a boiling reactor was of great interest because the estimated amount (by extrapolating CP-5 decomposition rate) was questioned. The two factors which raised considerable doubt as to the validity of the method of calculation were: (1) the effect of pressure and temperature on the recombination rate, and (2) the possibility of some of the decomposition gases being swept out of the core by the boiling action before recombination could occur.

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The water decomposition rate was determined by quantitatively collecting the non-condensable gases (by water displacement) during sampling of the steam flow to the turbine. The rate is expressed as cc of total gas per liter of condensed steam and does not include the amount of gas dissolved in the condensed steam (about 6 cc/liter).

The water decomposition rate was determined under various reactor water conditions and appeared to vary with the pH of the reactor water although no confirming reference was found in the literature. As shown in Fig. 27, the rate varied from 64 cc/liter of condensed steam at a pH of 8.1, to 197 cc/liter at a pH of 5.2.

Calculated G_{H_2} values, assuming 8 mev of the 195 mev produced per fission is absorbed, vary from 0.145 at a pH of 8.1, to 0.48 at a pH of 5.2.

The high water decomposition rates were first attributed to impurities in the reactor water. Subsequent gamma radiation work on samples of reactor water showed very little water decomposition, indicating high-purity water, nearly as good as the distilled water used as a reference standard. The high rates are now being attributed to the sweeping out of the gases by the boiling action in the reactor.

The high gas content in the steam (particularly 30 to 70 ppm of O_2) caused considerable concern from a corrosion standpoint and methods of decreasing the water decomposition rate were investigated. Hydrogen gas was fed into the reactor by addition into the feed-water line. At high H_2 flow rates (about 200 cc/liter of feed water), the oxygen content in the steam was reduced below that dissolved in the feed water (3 to 4 ppm O_2). Figure 28 shows the effect of H_2 addition on O_2 content in the steam.

Other means of decreasing water decomposition or gas content of the steam in EBWR are being investigated.

4. Radioactivity Levels in Steam Cycle

Radioactivity surveys were made throughout the external steam cycle at various reactor power levels. The principle activity present in the steam was radioactive N^{16} (Table VIII). During BORAX-III operation it was found that these activity levels varied with certain operating conditions. After cleaning up the reactor water of anions, the activity was reduced by a factor of 4 or 5. The lowest activity levels at 8-mw power level occurred when the reactor water pH was increased to 8.1. These changes are discussed in the following section.

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Fig. 27

ADSORPTION OF ACTIVITY ON GLASS
BOILER DURING OPERATION.

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FIG. 28
 WATER DECOMPOSITION IN BORAX III
 OXYGEN EVOLUTION VS HYDROGEN ADDITION

The activity levels to be expected in EBWR system can be extrapolated from BORAX-III data by using the lowest values because the anion concentration of the reactor water will be kept to minimum and the pH can be 7 or above.

5. Mechanism of Radioactive N¹⁶ Carryover in the Steam

When variation of the N¹⁶ activity with operation conditions was observed, it was decided to determine in what form the radioactivity N¹⁶ was present in the steam. The problem was attacked by varying feed-water temperature and making additions of chemical compounds and gases to the reactor feedwater. These additions are given in Table IX with the observed effect on N¹⁶ activity level in the condenser hot well. The results of all the experiments led to the following conclusion.

The radioactive N¹⁶ is not entrained in the steam as N₂ gas only, but as a mixture of N₂ gas and nitrogen compounds (probably NO or NO₂, or both). The changes in radioactive N¹⁶ concentration in the steam are due to shifts in the equilibrium of the nitrogen and nitrogen compounds between the steam and reactor water. The shifts in equilibrium are due to either or both (1) change in solubility of the nitrogen compounds in the reactor water, and (2) the oxidation or reducing conditions present in the reactor water or steam.

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Table VIII

RADIATION SURVEY OF BORAX-III STEAM CYCLE COMPONENTS

Date	Jan. 25	Feb. 4	Feb. 5	Feb. 9	Feb. 11	Feb. 12	Mar. 15	
Time	1004	1100	1930	2235	1400	1745	2110	1900
Reactor Power Level, mw	12 ^a	12 ^b	8 ^a	4 ^a	14 ^a	8 ^c	14 ^c	8 ^d
Condensate Temp., C	39	57						
Gas Temp. - Air Ejector, C	57							
Gas Flow - Gas Meter, cfm	2.4	2.25						
Condenser Vacuum, in. Hg	21.75	19.1						
Reactor Water								
pH	6.6	5.7	5.6	5.25	6.7	6.75	8.1	
Specific Resistance, ohm/cm ³	520,000	270,000	250,000	170,000	490,000	700,000	150,000	
Location	Activity in mr/hr (unless otherwise noted)							
Gas Meter - Front Face	450	1800	275	110				
Air Ejector - Side	420	2600	245	75	2.1-3 r/hr	210		85
Condenser Bottom - Stair Side	200	280	130	15	1 r/hr	47		
Hot Well Top - Stair Side	750	140	370	45		130		65
Hot Well Bottom - Stair Side	400	140	290	39		100		50
Circulating Water Pipe	4	3	6	4		2		
Condenser Bottom - Pump Side	330	390	190	25		80		
Hot Well Top - Pump Side	600	210	350	44		130		
Hot Well Bottom - Pump Side	400	190	270	34		95		
Condensate Line Below Hot Well	370	160	190	30		80		
Condensate Line at Tee	230	170	105	28		60		
Air Ejector Inlet Line	280	450	135	22		70		
Condenser Side - Top	130	130	125	20		38		30
Condenser Side - Above Air Ejector Line	320	240	350	55		80		65
Air Ejector Line from Condenser	900	700	440	70		160		80
Steam Separator - Bottom	280	250	60	13		29		
Steam Line-Vertical Pipe to Separator	200	200	240	95	800	90		55
Turbine Inlet - to Steam Chest	130	140	145	38	450	55		30
Turbine Outlet Casing	50	70	50	12	160	20		15
Vertical Steam Pipe in Reactor Bldg.	600-650		410-440	180-200	1.4-1.6 r/hr	160		120
γ Activity in Hot Well Recorder in Trailer	500	180	350	55	2.6 r/hr	60	600	45

^aNormal (forward) Cooling Tower Fan Operation

^bReversed Cooling Tower Fan Operation

^cAfter Anion Cleanup of Reactor Water

^dLowest Readings - taken after KOH addition

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Table IX

CHANGES IN HOT WELL ACTIVITY RESULTING FROM
ADDITIONS TO FEED WATER

Date	Addition	Amount	Activity, mr/hr		Delay Time to Peak Activity, Minutes
			Before Addition	Peak After Addition	
3-9	KCl	4 gm	70	125	2
3-11	HCl	6.8 cc (conc.)	150	225	15
3-11	NH ₄ OH	2 cc (conc.)	73	180	1.75
		4	100	240	1.1
		4	65	225	1.25
3-11	HNO ₃	1.9 cc (conc.)	62	130	9
		1.9	80	160	8
		6.0	50	290	10
3-16	NH ₄ NO ₃	1.2 gm	95	200	5
3-16	N ₂ H ₄	1 cc (anhydrous)	90	95	
		10	95	310	2
2-16	H ₂ SO ₄	5 cc (conc.)	200	270	8
3-17	H ₂ O ₂	5 gm	no increase		
		9	no increase		
3-15	KOH	2 gm	80	60	6
		2	65	55	5
		2	50	50	
		6	50	50	
3-12	N ₂ (gas)		no increase		
3-14	O ₂ (gas)	26 cc/liter	100	70	5
		50	70	45	
3-13	H ₂ (gas)		75	230	20 sec
3-14			80	170	27 sec
3-17		27 cc/liter	90	160	15 sec

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Before the above conclusion can be accepted as fact more work will have to be done on determining very low concentrations of NO and NO₂ in reactor water, steam, and condensate. This work will be continued during BORAX-IV operation.

6. Decrease in Total Excess Reactivity - J. Thie

After 600 megawatt days of operation a significant amount of reactivity has been lost due to long-term fission products and U²³⁵ burn-out. The amount is not as great as it might have been because of the presence of one plate containing boron in every fuel element. Figure 29 shows both the experimental reactivity loss and the loss calculated early in the reactor's history. Some of the discrepancy can be accounted for by uncertainties in the amount of boron initially present.

Initially, the reactor had 7.5% total excess reactivity in the cold clean condition. Therefore, after 600 megawatt days one has 5% excess reactivity at room temperature without Xe.

FIG. 29
REACTIVITY LOSS DURING LIFETIME OF BORAX III

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C. Fuel and Cladding Materials

1. Corrosion of Unclad High-Uranium Alloys - C. R. Breden

The current results of long-term corrosion tests on unclad, high-uranium alloys in 500F and 550F water are summarized in Table X.

2. Plate Corrosion Tests - C. R. Breden

a. End Closure Samples

Three reference alloy fuel plate end closures with the heliarc welded bond added after induction welding are still intact after 3859 hr in static, degassed water at 550F.

b. Export-Type Plates

Two plates (25 in. x 4 in. x 0.045 in.), containing natural uranium and roll clad with Grade III zirconium, have accumulated 203 hr in 420F, and 3372 hr in 550F degassed, demineralized water. Other than the white corrosion products along the cut edges and some warping noted previously, the plates have shown no significant change.

3. Aluminum Alloys

a. Dynamic Corrosion Tests - N. R. Grant

Samples of aluminum-nickel (M-388) and various aluminum-copper alloys were tested at various temperatures, hydrogen gas concentrations, pH, and water velocities. The weight changes reported here are averages taken from at least two electrochemically-stripped samples. In all cases, the stripped weight loss of the averaged samples did not vary more than 7%.

The problem concerning two different corrosion rates for M-388 that vary by a factor of five has not been resolved. Both of the rates have been determined again during this last report period, but the factors that are required to achieve the lower rate are still not clearly defined.

Loop No. 1, Run No. 3 (500F; pH 5.9 - 6.1; H₂ Gas Added):

This test was originally intended to be a six-month run using M-388 alloy, with withdrawals once a month. Approximately 200 cc H₂/liter were added to the loop at start-up with hopes that this would yield the low rate. After four weeks the first set of samples showed scale

Table X

CORROSION OF UNCLAD HIGH-URANIUM ALLOYS
IN HIGH-TEMPERATURE WATER

Sample No.	Expt. No.	Alloy Composition, Uranium Plus %	Heat Treatment (2)	Water Cond. (3)	Rate of Wt. Change, (4) mg/cm ² /mo		Time to Disintegration, (5) hr		Appearance
					500F	550F	500F	550F	
A-4065 ⁽¹⁾	U-f4	6 Nb	2012F quenched	Deg			>13,941		Cracking slightly enlarged.
A-4114a	U-81	3.8 Si	850C - 7 days	Deg	- 16.5		>10,870		Loose black coating. Rate is for last period. Test discontinued.
A-4297h	U-313	3 Nb-0.75 Sn	800C - 4 min; WQ. 350C in vacuum 6 hr; air cooled	Deg	-139		> 3,321		Loose black coating. No change in pit near one end.
A-4118	U-85	1½ Nb-5 Zr	1470F quenched	Deg	- 83		>13,204		Adherent black coating. No change in pitting. Test discontinued.
A-4207a	U-217	1½ Nb-5 Zr	As cast. Quenched at 850C	Deg	- 87		> 4,324		Adherent black coating. Some pitting, spalling.
A-4207c					- 98				
A-4207d					- 97				
A-4207e					- 99				
A-4207f					- 90				
A-4207g					- 85				
A-4207h					-104				
A-4355f	U-332	1½ Nb-5 Zr	As cast. 750C-1.75 hr; FC 700C-3 min; 725C-10 min; WQ. Aged 400C-2 hr	Deg	-469		2,953		Adherent black coating. Sample badly spalled. Test discontinued.

(1) Sample loosely fitted in stainless steel capsule perforated with 1/32 in. diameter hole drilled through side wall.

(2) FC - Furnace cooled; WQ = Water quench

(3) Water Condition: Deg - Degassed (about 2 ppm O₂)

(4) Rate is averaged over entire period unless otherwise noted.

(5) Symbol (˘) denotes accumulated test time; test still in progress unless otherwise noted.

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beginning to flake off and a high weight loss. The run was continued for two additional weeks but cleaned samples showed high weight loss. The run was discontinued. Weight losses for the two withdrawals were:

Test Time, hr	Water Velocity, fps	Wt. Loss, mg/cm ²
648	18	9.8
	7	6.9
864	18	13.7
	7	8.5

Loop No. 1, Run No. 4 (500F; pH 5.8 - 6.1):

This test was intended to: (1) compare the corrosion behavior of samples prepared from the initial one-ton lot of alloy M-388 with samples from the second shipment, and (2) compare the behavior of test specimens prepared in the usual manner with specimens similarly prepared but pre-conditioned for two weeks in a semi-static section of the same test loop.

There was no detectable difference in corrosion behavior of samples representative of both shipments; all weight losses checked within 2%. The number of pre-treated samples prepared were sufficient for only two dynamic withdrawals; thus only a preliminary evaluation could be made from a weight loss comparison (Fig. 30). Additional samples have been pre-treated to facilitate long-term tests.

Loop No. 3, Run No. 3 (600F; pH 7; Degassed):

ALCOA has developed several new aluminum-copper alloys of which three were tested for 327 hr. The results show a slight improvement in corrosion behavior over that exhibited by alloy X-2219 in an earlier test. All samples showed a flaking scale (Fig. 31) similar in appearance to that observed on alloy X-2219.

Alloy	Composition, wt-%							Weight Loss, mg/cm ²	
	Cu	Fe	Si	Mn	Ni	V	Zr	18 fps	7 fps
173212	7.87	0.74	0.08	0.31	0.28	0.10	0.12	4.3	3.6
173213	10.8	0.73	0.08	0.31	0.28	0.10	0.11	3.9	3.5
173162	6.03	0.76	0.07	0.30	0.25	0.09	0.14	4.2	3.4
X-2219	6.07	0.12	0.09	0.28	-	0.10	0.16	5.0	3.7

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FIG. 30
PRETREATED AND STANDARD SPECIMENS
OF ALUMINUM-NICKEL ALLOY M-388
TESTED IN 500 F WATER (pH = 6) AT
18 fps WITH NATURAL HYDROGEN BUILDUP

FIG. 31
EXPERIMENTAL Al-Cu-Ni ALLOYS TESTED FOR 2
WEEKS IN 600 F DEGASSED WATER AT pH = 7
(LOOP 3 - RUN NO. 3)

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Loop No. 3, Run No. 4 (500F, pH 6.9 - 7.0):

In order to establish that the low rate could once again be reached, it was decided to duplicate the run reported previously.¹² Both holders were loaded with M-388 samples and the loop was run with a natural buildup of hydrogen gas. At the end of two weeks, weight losses were 4.55 mg/cm² for the 18 fps holder, and 3.01 mg/cm² for the 7 fps holder. A gas analysis gave 99.4% H₂, 0.3% inert, 0.1% CO₂, 0.1% O₂ and 0.1% CO. The appearance of the samples was very good (Fig. 32) and the results were almost the same as that of the prior run. The remaining samples have been divided into two groups. The first group will be tested for 4 weeks in degassed water. The second group will be run for 4 weeks with hydrogen gas added to the water.

WATER VELOCITY: 18 fps

WATER VELOCITY: 7 fps

FIG. 32
ALUMINUM-NICKEL ALLOY (M-388) TESTED FOR
317 HOURS IN 500 F WATER (pH = 6.9-7.0)
WITH NATURAL GAS BUILDUP TO 240 ccH₂/liter

¹² Reactor Engineering Division Quarterly Report, Section I,
ANL-5561 (April, 1956), p. 90.

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Loop No. 4, Run No. 4, 6, and 7 (500F; pH 6.8 - 7.0;
 18 fps):

In an attempt to determine the effect of hydrogen on the corrosion rate, three runs were made using different methods of injection and different concentrations of hydrogen.

In Run No. 4 the loop was run for 48 hr degassed and then the gas was injected to a concentration of 210 cc/liter. After 336 hr the weight loss was 9.51 mg/cm² and the appearance of the samples was poor (Fig. 33).

It had been noticed that the hydrogen buildup in an early run in Loop No. 3 was almost linear from 0 to 350 cc/liter in about 7 days with a full loading of samples. Using this for a basis in Run No. 6, hydrogen gas was injected into the loop at a rate of 50 cc/liter for seven days, and then held at 350 cc/liter for an additional seven days. After 339 hr the weight loss of the descaled samples was 9.64 mg/cm². The appearance of the samples was slightly improved (Fig. 34), but still the low initial weight loss had not been achieved.

FIG 33
 ALUMINUM-NICKEL ALLOY (M-388) EXPOSED
 (1) 48 HOURS IN DEGASSED WATER,
 (2) 288 HOURS IN WATER WITH
 210 ccH₂/liter ADDED

FIG. 34
 ALUMINUM-NICKEL ALLOY (M-388) TESTED 2 WEEKS
 IN 500 F WATER (pH = 7.0) WITH PROGRESSIVE
 HYDROGEN INJECTION FROM 0 TO 350 ccH₂/liter

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Another possibility was that a high concentration of added hydrogen might decrease the corrosion. This was investigated in Run No. 7 by injecting 500 cc H_2 /liter of water at the start of the run. Weight loss after 346 hr was 9.78 mg/cm² and the appearance of the samples was very poor (Fig. 35).

Loop No. 4, Run No. 5 (500F; pH 7; Degassed; 18 fps):

Other possible variables and their effect on weight loss were investigated. The lower corrosion rates occurred when the holders were new, so that possibly the current high rates were the result of an oxide film on the holders. Galvanic corrosion was also a possibility. Therefore, one end of one holder was etched until all the oxide had been removed. Other samples were insulated from the holder with a layer of teflon. One set of samples was pretreated by an anodizing method, and the remaining samples were placed into the holder in the regular manner. After 312 hr the results were as follows:

	<u>Weight Loss, mg/cm²</u>
Samples in etched holder	6.92
Samples insulated with teflon	6.85
Anodized samples	10.55
Regular samples	6.72

The appearance of all the samples, except those anodized, was fairly good (Fig. 36). The anodized samples had apparently lost all of their initial scale and then started corroding over again. The other two treatments had no effect as compared to the regularly loaded samples.

Conclusions

Although the low rate under high-velocity conditions appears to be associated with the hydrogen gas present, attempts to produce it by variation of the H_2 content at the start have not been successful. The only procedure that has shown promise has been to give the samples a preliminary test under semi-static conditions before transferring them to the high-velocity conditions. Whether or not the low rate will prevail in long-term test remains to be established.

b. Boiling Corrosion Tests - N. R. Grant

The first test of aluminum-1% nickel alloy (M-388) clad tube in the boiling corrosion facility has been completed. The tube was made of a 3-8-inch stainless steel tube clad with 1/16-inch thick M-388 alloy. It

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FIG. 35
ALUMINUM-NICKEL ALLOY (M-388) TESTED FOR
346 HOURS IN 500 F WATER (pH = 7)
INITIALLY CONTAINING 500 ccH₂/liter

FIG. 36
ALUMINUM-NICKEL ALLOY (M-388) SPECIMENS
TESTED FOR 312 HOURS IN 500 F DEGASSED
WATER (pH = 7) AT 18 fps

was centered in a pressure vessel and water at about 500F was flowed through it. Since the pressure vessel temperature was maintained at 422F (300 psig), boiling occurred on the surface of the M-388 alloy.

The facility was operated for 1702 hr, with an average heat flux of 25,000 Btu/(hr)(sq ft) at the top. The temperature drop across the tube varied from 40F at the start of the run to 63F before the final shut-down. A small part of this latter temperature drop was due to a slight buildup of oxide from the loop on the inside of the tube. This was removed with acetone before weight measurements were made. The water in the pressure vessel varied in pH from 6.5 to 7.5, and in resistivity from 130,000 to 400,000 ohm-cm. The pH value was maintained by by-passing system water through a cation resin bed for 10 to 30 min at daily intervals.

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The measurements taken at each of the three withdrawals are shown in Table XI. The weight changes are limited to the surface area in contact with the water (the area which showed the most corrosion).

Table XI

BOILING CORROSION TEST OF ALUMINUM-NICKEL
ALLOY (M-388)-CLAD TUBE

Test Time, hr	Tube Diameter, in.			Wt. Change, mg/cm ²	Appearance
	Water Phase	Water-Steam Interface	Steam Phase		
0	0.5002	0.5002	0.5002		Metallic
170	0.5008	0.5014	0.5004	+2.22	<u>Water</u> - Typical M-388 adherent grey coating. <u>Junction</u> - Slight reddish coloring, probably iron oxide. <u>Steam</u> - Adherent dull white-grey coating.
1083	0.5012	0.5014	0.5003	+1.61	<u>Water</u> - Adherent red coating with a few very small spots of bare metal along scratches in tube. <u>Junction</u> - Adherent red coating. <u>Steam</u> - Adherent dull white-grey coating with some red coloring near junction.
1702 Before Strip	0.5012	0.5014	0.5005	+1.98	<u>Water</u> - Adherent red coating with more of the very small spots of bare metal showing. <u>Junction</u> - A band of darker red coating. <u>Steam</u> - Adherent dull white-grey coating with some red coloring near junction.
After Strip	0.4996	0.5000	0.5002	-3.95	Dull metallic

The traces of reddish coloring on the tube in the steam phase above the water level were probably iron oxide carried by splashing of water during boiling. Figure 37 shows the appearance of the water-steam junction before the sample was cleaned.

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FIG. 37
ALUMINUM-NICKEL ALLOY (M-388) - CLAD TUBE
EXPOSED FOR 1702 HOURS AT 422 F
IN BOILING CORROSION TEST FACILITY

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Microscopic examination after the tube was cleaned showed a few small shallow pits at various places along scratches on the tube. These scratches occurred during tube withdrawal following each shutdown. The tube was slightly bowed, probably because of the different coefficients of expansion of stainless steel and the aluminum-nickel alloy.

Since aluminum alloys have a high initial rate of corrosion which levels off after a few days, it is not possible to determine the true corrosion rate from this one point. However, the slope of a straight line between this point and zero would give a rate of 2.91 mils/yr. A second tube is currently in test.

c. Corrosion of Spot-welded 2S Aluminum -
C. R. Breden

Samples of 2S aluminum, spot-welded to simulate BORAX-III assembly welds, and tested in 420F degassed water, were satisfactory after exposures totalling 3010 hr.

d. Corrosion-Irradiation Tests - N. R. Grant

A test involving a series of three withdrawals in the Argonne Loop at MTR for the comparison of in-pile and out-of-pile corrosion has been partially completed. Duplicate cartridges (see illustration) consisted of five sections, three for the original samples (sections 1, 2, and 3), and two for replacements (section 4 and section 5-6).

At the end of the first period section 1 was withdrawn and replaced by section 4. At the end of the second period sections 2 and 4 were withdrawn and replaced by section 5-6. Thus at the end of the test, section 3 and section 5-6 remained for weight loss determinations.

The samples and the cartridge combs used to hold them were made of aluminum-1% nickel alloy (M-388). After removal from the holders the samples were stripped of their scale and weighed. The weight losses and total neutron exposure for the first two periods are summarized in Table XII.

The large increase in weight loss for the in-pile samples in the second period was caused by the low-purity water. However, the out-of-pile samples were only affected slightly by this factor. Examination of the samples from the third period is under way.

Table XII

PARTIAL RESULTS OF ALUMINUM-NICKEL SAMPLES
TESTED IN ANL LOOP AT MTR

Water Velocity = 15 fps

Cartridge Section	Hr at 485-500F	Water Conditions		Neutron Exposure		Weight Loss, mg/cm ²	
		pH	Resistivity, ohm-cm	Fast nvt x 10 ¹⁹	Thermal nvt x 10 ²⁰	In-Pile	Out-of-Pile
1	200	6.2-7.8	500,000 to 3,000,000	1.6	1.6	3.04	8.91
4	340	7.4-8.5	*	2.25	2.25	7.84	11.19
2	540	Same as for sections 1 and 4		3.85	3.85	4.72	13.23

*Ion exchange column not turned on; water purity was very low.

4. Urania-Thoria Fuel Materials

a. Solution Tests - R. A. Granacki

The testing of bare urania-thoria specimens, prepared by the Argonne Metallurgy Division, in static degassed and demineralized water at 600F has been discontinued. The results are summarized in Fig. 38.

Specimens 32 to 35 were prepared from incompletely mixed, non-homogeneous materials and contained a large number of dark areas. These areas were the first to pit and resulted in large initial solution rates. However, for the final periods of test, the weight change rates were low and uniform.

Sample 36 was prepared from high purity thoria as supplied by Norton Co.

Samples 37 and 38 were prepared by ball milling resulting in homogeneous specimens with low and uniform solution rates. Sample 37 was compacted by dry pressing, while sample 38 was slip cast; both were fired at 1830C.

Sample 39, 2.5% urania fully enriched, was from a group of samples prepared for irradiation testing. The constituents had been mixed by ball milling and the sample was dry pressed at 500 lb

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NO.	COMPOSITION	TIME, hr	WT. CHANGE, mg/(cm ²)(mo)	
			1ST PERIOD	2ND PERIOD
32	5% U ₃ O ₈ -ThO ₂	2415	-32.0	-0.0195
33	5% U ₃ O ₈ -½% CaF-Thoria	2415	-19.0	-7.41
34	3% U ₃ O ₈ -ThO ₂	2415	-59.1	-10.2
35	3% U ₃ O ₈ -½% CaF-ThO ₂	2415	-26.1	-4.12
36	Thoria (NORTON)	4560	- 5.37	-0.29
37	10% U ₃ O ₈ -Thoria	4470	- 6.4	-0.82
38	10% U ₃ O ₈ -Thoria	3825	- 4.85	-0.10
39	2.5% UO ₂ ENRICHED-ThO ₂	3180	- 2.08	-0.097

FIG. 38
TEST HISTORY OF URANIA-THORIA IN 600 F
DEGASSED, DEMINERALIZED WATER

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and fired at 1850C. The density was 9.58% and porosity was 9.57%. X-ray examination indicated complete solid solution of the urania in the thoria. The appearance of the sample indicated uniform composition, and its solution rate was low and uniform throughout testing.

The large initial weight losses can be attributed partly to end edge spalling due to handling. Samples were transported in glass bottles and were free to strike the glass walls. The differences between ends of the same sample and between samples are believed to be due to variations in hardness resulting from differences in density. Densities of tested samples were in close agreement with densities of controls.

b. Corrosion-Irradiation Tests - R. A. Granacki

The irradiation testing of clad urania-thoria fuel specimens supplied by the Argonne Metallurgy Division has continued in the CP-5 Autoclave Facility. Run No. 21 was complete during the quarter, and the analysis of the water in Run No. 20 was obtained.

The specimen of Run No. 21 consisted of four ceramic pellets (0.25 in. OD x 0.50 in. long) jacketed in a capsule of 2S aluminum-1% nickel alloy, with a 0.005-inch annulus of lead between jacket and pellets to facilitate heat transfer during irradiation. The pellets were composed of 90% ThO₂ and 10% UO₂ with an enrichment of 93.22%. The constituents had been mixed by ball milling, dry pressed at 500 psi and fired at 3360F (1850C). They had a density of 9.58 gm/cc and an average porosity of 9.57%. X-ray examination indicated complete solid solution of the urania in the thoria. Pellets were consecutively numbered in the jacket, with No. 1 at the dome end and No. 4 at the flat end of the capsule.

The capsule was irradiated in distilled and demineralized degassed water at 450-515F for 1790 hr at full CP-5 power, which resulted in a calculated burnup of 3.23% of total uranium atoms and 0.107% burnup of total core atoms. No pressure buildup was noted during the run. After 1490 hr the specimen temperature had decreased from 495F to 475F. After an additional 200 hr of operation, the temperature decreased to 450F. These fluctuations apparently were due to characteristics of pile operation.

Post-irradiation examination of the autoclave and capsule was performed in a high-level cave. The water from the autoclave was filtered preparatory to analysis for fixed nitrogen and fission product cesium.

Analysis of the water showed less than 5 micrograms/ml of ammonium ion and less than 5 micrograms/ml of nitrate ion. The pH of the water was 7.18 and its resistivity was 48,500 ohms. Preliminary examination for fission product cesium showed 2.8×10^4 disintegrations/min/ml. A more accurate analysis will be made after a three-month decay period.

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The specimen was examined after removal from the autoclave, and after dissolving the aluminum-nickel capsule in 10% iodine, 90% ethyl alcohol solution.

The capsule was slightly distorted and was coated with a heavy deposit of iron oxide. Electrolytic removal of the iron oxide scale in a saturated boric acid solution disclosed three small ruptures through the cladding (Fig. 39a, b) which could have accounted for the presence of fission product cesium in the autoclave water. The pellets were cracked in the characteristic lateral-radial pattern, with some lead intrusion between pellets (Fig. 39d, e). The lead in the annulus around pellets 3 and 4 remained uniform and held the cracked pellets together.

(a)
RUPTURES IN CLADDING

(b)
(X2)

(c)
PINHOLES IN CLADDING
END PIECE (X2)

(d)
LEAD INTRUSION BETWEEN PELLETS
NO. 3 AND NO. 4 (X4)

(e)
PELLET NO. 1
(X4)

FIG. 39
ALUMINUM-NICKEL ENCAPSULED URANIA-THORIA
PELLETS IRRADIATED IN 450-515 F WATER
FOR 1790 HOURS AT FULL CP-5 POWER

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The specimen in this run had the longest irradiation of the series, and the capsule showed less distortion than the previous sample.

The nature of the ruptures suggest stress corrosion of the Al-Ni alloy. This was also characteristic of the previous sample, indicating that the alloy is too weak to withstand the test temperature and pressures. Where the alloy is amply supported, as in the end section shown in Fig. 39c, corrosion does not appear to be significant. It is difficult to ascertain if the small pinholes are a result of pitting or weld porosity.

Analysis of the water from Run No. 20¹³ showed less than 5 micrograms/ml of ammonium ions and less than 5 micrograms/ml of nitrate ions. The pH of the water was 7.0 and fission product cesium showed 1.11×10^6 disintegration/min/ml.

c. Fuel Element
Design -
C. K. Soppet

The fuel element for the BORAX-IV loading is composed of thorium-enriched urania pellets contained in an aluminum-1 wt-% nickel jacket. With reference to the illustration, the tube plates are extruded from M-388 alloy and joined, by intermittent spot welding, to the side plates. The end fittings are the same as used in BORAX-III assemblies, except that the material is M-388 alloy.

The fissile material is in the form of sintered pellets, 0.230 in. OD x 1/2 in. long. The annulus between the OD of the pellets and the ID of the tubes in the tube plate is filled with lead to enhance the heat transfer characteristics of the plate.

The method of loading the pellets and lead into the plate is similar to the submarine process used in the Al-Si bonding technique. The tube plate is immersed in a molten lead bath to a depth which places the open upper end of the plate below the surface of the lead. With

¹³ Reactor Engineering Division Quarterly Report, Section I, ANL-5561 (April, 1956), p. 101.

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the tube plate in this position, the pellets are pushed into the tube, expelling the excess lead out of the top of the tube. After all of the tubes have been filled in this manner the plate is withdrawn from the lead bath and the lead is allowed to solidify. The tube ends are then machined to remove all lead from the inside surface of the tubes and the tubes are welded closed. Experiments have demonstrated that a completely lead-filled annulus can be accomplished by this technique (Fig. 40).

FIG. 40
SECTIONED VIEWS OF ALUMINUM-NICKEL
JACKETED THORIA-URANIA FUEL PELLETS

This unique method of assembling small rod-type elements offers several advantages:

- (1) The clearance between the OD of the pellets and the ID of the tube is sufficient to permit the pellets to be used in the as-pressed and sintered condition. No additional sizing treatment is required.
- (2) The lead bond eliminates air space between the heat-generating body and the heat transfer surface.
- (3) The small pellets can be loaded into the plates in batches sufficient to fill each tube in one operation.

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(4) The use of the tube plates permits existing technology to be used to fabricate assemblies containing a large number of individual elements at a low cost, with a maximum of heat transfer area. It is estimated that the tube plate as shown has at least 80% of the heat transfer surface of an equivalent flat fuel plate.

D. Control and Thermal Shield Materials

1. Boron Stainless Steel Development

a. Fabrication and Mechanical Properties - N. Balai

Evaluation of corrosion resistance, weldability, and machining properties of various cast and wrought boron-stainless steel alloys have continued.

Cast Alloys

Two cylindrical bars of stainless steel Type 304, modified by the addition of 4.06 wt-% boron (added as ferroboration), were machined into standard tensile bars (0.505 in. dia. x 2 in. gauge length). The mechanical properties were found to be:

	<u>T.S., psi</u>	<u>Y.P., psi</u>	<u>El., %</u>	<u>R.A., %</u>
Bar No. 1	25,300	-	0.05	0
Bar No. 2	29,100	-	0.05	0

(Electric Steel Foundry Co. Heat No. SP40B-2047)

In machining, the alloy was found to be very brittle and it was necessary to finish grind the gauge length section of the bar.

Attempts to machine tensile bars from higher boron content alloys were discontinued since it was impossible to obtain a finish cut in preparation for grinding. Observations of the machining attempts showed that metal chips were removed by spalling and flaking.

The 2% boron-stainless steel air-melted cast cross, described in ANL-5561,¹⁴ was finish machined on a planer to the 1/4-in. final thickness. Flatness, within a few thousandths of an inch, in both the

¹⁴Reactor Engineering Division Quarterly Report, Section I (April, 1956), p. 108.

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length and breadth directions of the arms were obtained with Crucible's Rex IV-V tool steel bits. Strangely, the Rex IV-V tool bit was ineffective in machining the web of the cross; the web was machined with a 1/2-in. radius tool ground from a Cecobolt tool steel which was found to be ineffective in planing the arms of the cross. All machining was performed dry. None of the available commercial cutting oils (or special concoctions formulated in the machine shop) were useful. A 100-microinch finish was obtained. The appearance of the machined cross is shown in Fig. 41. Following machining, a welded stainless steel Type 304 tail was welded to the cast cross with Arcos Stainlend HCN rod (stainless steel Type 310). Visual examination of the double vee butt welds connecting the tail to the cross showed no cracking in either the weld or stainless steel Type 304 modified by the 2 wt-% boron addition.

Attempts to machine an experimental ESCO cross containing 5.35 wt-% boron met with little success. Heat treatment to relieve casting stresses resulted in multiple fractures as shown in Fig. 42. A visual examination of the fractured surfaces showed numerous non-metallic inclusions which were undoubtedly crack starters. A second experimental cross from the same heat of 5% boron alloy steel was rough planed on all faces when a crack suddenly developed and propagated almost the complete length of one of the arms. Again numerous inclusions were uncovered by the planing operation.

Wrought Alloys

(1) Vacuum Metals Corp - 1½% Boron-Stainless Steel

Preliminary welds, described in ANL-5561, developed low weld joint efficiencies. The 1½ wt-% boron, vacuum-melted stainless alloy (rolled by Vacuum Metals Corp) was most plentiful for establishing welding procedures. A test plate consisting of two outer Type 304 stainless steel plates (15 x 6 x 1 in.) and two inner plates of 1½ wt-% boron-stainless steel Type 304 were welded together manually, sectioned, and turned into standard tensile bars. Welds were located at approximately the midpoint of the 2-in. gauge section. It was found that (1) when the double vee groove joint was separated 1/8 in. and (2) when the root pass was deposited by the Argonarc torch using bare Type 308 filler wire, high strength welds developing the full strength of the plate were easily obtained with manually deposited electrodes.

No visual signs of cracking in the base plate, heat-affected zone or weld metal were apparent, although successive passes were deposited on alternate sides of the double vee weld groove. The double vee groove was adopted as a means of controlling the flatness of the finished plate occasioned by shrinkage of the weld metal. Finished plates were produced which were "veed" less than 5 degrees. The tensile properties of weld specimens are given in Table XIII.

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FIG. 41
APPEARANCE OF BORON (2 wt-%) -
TYPE 304 STAINLESS STEEL CROSS-TYPE RODS

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ASSEMBLED SECTIONS
SHOWING SYMMETRY
OF CRACK PROPAGATION

DISASSEMBLED SECTIONS. ENCIRCLED AREAS
CONTAIN NON-METALLIC INCLUSIONS.

FIG. 42
FRACTURE OF EXPERIMENTAL CROSS FOLLOWING ANNEALING ATTEMPT

Table XIII

TENSILE PROPERTIES OF WELDED BORON-STAINLESS STEEL PLATES

Specimen Size: 0.505 in. dia. x 2 in. gage length

Test Bar	Specimen Description*	Weld** Rod	T.S., psi	Y.P., psi	Fl., %	R.A., %	Remarks
Test Plate No. 2							
1	1 $\frac{1}{2}$ % B to 1 $\frac{1}{2}$ % B; weld transverse to rolling direction; bar parallel to rolling direction.	K	94,000	40,500	11.5	9.6	90° break to bar axis; break at punch mark. Weld started to fail.
3 & 6	1 $\frac{1}{2}$ % B to SS 304; weld parallel to rolling direction; bar transverse to rolling direction.	FeB	80,700 82,900	36,800 40,900	21.0 21.9	22.8 31.0	
4 & 7	1 $\frac{1}{2}$ % B to 1 $\frac{1}{2}$ % B; weld parallel to rolling direction; bar transverse to rolling direction.	K	98,500 98,100	43,000 74,500	10.2 9.2	8.9 18.3	90° break to bar axis and in 1 $\frac{1}{2}$ % B. Weld started to fail. 90° break at gage point. Weld started to fail.
5 & 8	1 $\frac{1}{2}$ % B to SS 304; weld parallel to rolling direction; bar transverse to rolling direction.	K	85,300 80,200	37,000 35,900	39.2 22.6	72.0 22.7	Failure and elong. in SS 304 base plate. Failure in heat affected zone. SS 304 base plate elongated.
9	SS 304 base plate; rolling direction unknown.	-	84,000	-	75.0	69.8	
10	1 $\frac{1}{2}$ % B-SS bar plate.	-	85,700	38,000	11.3	8.1	Fracture 90° across bar and inside gage length; no necking.
Test Plate No. 3							
1 & 2	1 $\frac{1}{2}$ % B to 1 $\frac{1}{2}$ % B; weld transverse to rolling direction; bar parallel to rolling direction.	K	98,500 96,300	42,000 -	9.0 9.3	10.2 9.7	Failed at gage point; 90° break to axis.
3 & 6	1 $\frac{1}{2}$ % B to SS 304; weld parallel to rolling direction; bar transverse to rolling direction.	FeB	83,700 77,500	35,700 -	37.5 22.5	72.0 22.5	Failure in SS 304; all elongation and RA in SS 304. Failure through weld; all elongation and RA in SS 304.
4 & 7	1 $\frac{1}{2}$ % B to 1 $\frac{1}{2}$ % B; weld parallel to rolling direction; bar transverse to rolling direction.	K	97,700 98,000	42,000 -	8.5 9.5	10.2 9.2	Failed at gage point; 90° break to axis. Failed at gage point; 90° break to axis. Weld started to fail.
5 & 8	1 $\frac{1}{2}$ % B to SS 304; weld parallel to rolling direction; bar transverse to rolling direction.	K	85,200 81,400	37,000 -	40.0 41.0	72.0 71.0	Failure in SS 304. All elongation and RA in SS 304. Weld started to fail.
9 & 12	SS 304 base plate; rolling direction unknown.			not determined			
10 & 11	1 $\frac{1}{2}$ % B stainless base plate; weld transverse to rolling direction.		82,400	37,000	9.8	7.7	

*1 $\frac{1}{2}$ % Boron alloy heated 2000F; 1 hr furnace cooled.

**K - Arcos Stainlend K-Type 308; lime coated.

FeB - Experimental rod furnished by B & W; Type 308 core wire, lime coated with boron in coating.

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(2) Vacuum Metals Corp - 2% Boron Alloy

A welded cross (48 in. long x 12 in. across arms x 5/16 in. thick arms) was fabricated from four plates (5/16 x 6 x 48 in.) rolled from vacuum-melted alloy without cracking of either the base material or weld metal. The procedure was first proved by preparing a welded tee section with the outstanding leg of the tee fillet-welded to a 2% boron-stainless steel plate, 5/16 in. x 6 in. wide x 5 in. long. Root passes were placed with an Argonarc torch using bare Type 308 rod. One fillet weld was arc welded with Arcos HCN rod (Type 310) under high weld restraint without cracking either the base material or weld. The fillet weld on the opposite side of the tee was arc welded with the same rod without restraint to permit bending of the 2% boron-stainless steel by the contraction of the weld. Although the 2% boron-stainless steel alloy has low ductility (8-10% elongation), no visual cracking of the base material was observed when the finished tee was sectioned and etched. The induced bending "cocked" the flange approximately 10 degrees.

All four arms were fillet welded to form a 12-in. cross under high weld restraint using the above procedure. No visual cracking was apparent and there was little longitudinal twisting. A 10-in. Type 304 tail was welded on, using Arcos HCN rod (Type 310), without visual distress (Fig. 41).

(3) Universal Cyclops Steel Corp - 2% Boron Alloy

Vacuum-melted stainless steel Type 304 containing 2 wt-% boron was rolled successfully. Sheared sheets (10 in. wide x 1/8 in. thick x 52 in. long) were cold bent into angles on conventional sheet metal shop equipment. Four angles were assembled into a 10-in. cross by spot welding (Fig. 41).

During fabrication it was observed that the cold alloy is short on ductility and that it was impossible to obtain 1/2-in. corner radii without cracking. Moderate and finally high preheating (1000F) showed that the alloy was increasingly brittle at elevated temperatures (hot short). Rounding of the sharp end edges in the vicinity of the radius reduced but did not eliminate the cracking. The alloy always galled slightly on the female die, indicating a lubrication problem. The interposition of a 16-gauge aluminum sheet between the female die and the 2% boron-stainless steel alloy sheet resolved the lubrication and cracking problems, and the desired 1/2-in. corner radius was easily and consistently achieved. Two trial crosses were fabricated, one with residual fabrication (bending) stresses and the second with intermediate stress relief annealing of the angles at 1150-1200F. A lifting tail piece of stainless steel Type 304 was welded to the "sheet iron" cross with Arcos HCN rod (Type 310) without visible cracking.

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(4) Superior Steel Corp - 1% Boron Alloy

Test plates were prepared by the same procedure as adopted for the $1\frac{1}{2}\%$ boron alloy, i.e., double vee groove butt welds with a $1/16$ -in. central land and spaced $1/8$ in. apart. Welds were visibly free of cracks. The plates were sectioned and standard tensile bars are being prepared.

In an attempt to simulate a long weld, which would be used to form a wide plate from the 30-in. maximum width plates now available, a 1% boron bare filler rod deposited by the Argonarc torch process was partially successful. A 36 in. long x 24 in. plate and a 36 in. long x 12 in. wide plate were prepared for a double vee groove butt weld with a central $1/16$ -in. land. Bare filler rod was prepared by shearing 1-in. plate stock material into $1/8$ in. x $1/8$ in. slivers. Alternate passes were deposited on opposite sides to maintain a flat plate. One end of the weld cracked persistently and required periodic repairs. The internal progress of the crack was not apparent until the long weld was almost completed. When the crack "surfaced," it was approximately 6 in. long. The crack was ground out from both sides to the root of the weld and weld repairs started. In the process of repair, the crack suddenly elongated an additional 10 to 12 in.

A crack stopper weld was then deposited and the entire plate was preheated in order to finish the balance of the apparently sound weld. The preheating opened up the cracked end of the weld approximately an $1/8$ in. and the crack started to propagate through the crack stopper weld. Further work was discontinued in order to study the weld failure. The weld and weld zone (approximately $1\frac{1}{2}$ in. wide) was removed from the partially welded plate and sectioned. The sections show that the crack initially followed the edge of the weld groove and later entered the base plate (Fig. 43).

It was concluded that the weld failure was caused by:

- (a) High interpass temperatures since the Argonarc welding was continuous.
 - (b) Low ductility of cast metal.
 - (c) Increasing hot shortness of both the base plate and weld deposit with rising interpass temperature.
 - (d) High thermal stress gradients in the weld zone.
- It was noticed that the heat conductivity of the alloy is much lower than for an austenitic stainless steel, i.e., plate temperatures 6 to 8 in. away from the weld were only 140-150F.

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FIG. 43
SECTIONAL VIEWS SHOWING PROGRESS OF FRACTURE THROUGH
WELD AND PARENT METAL OF 1% BORON-STAINLESS PLATES

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(5) Republic Steel Corp - 1/4% Boron Alloy

After establishing that a large root opening, 1/8 in., was a prerequisite for welding boron-containing steels, the air melted alloy, melted and rolled by Republic Steel, was welded. In ANL-5561, it was reported to be singularly refractory toward welding. Its weldability is below that of any alloy welded thus far.

b. Corrosion Tests - N. R. Grant

Static and dynamic corrosion tests on various boron-stainless steels have been continued¹⁵ with results as shown in Tables XIV, XV, and XVI.

Most of the samples that have been tested for only a short time show high weight change rates; however, based upon past performance, the rates tend to decrease with continued exposure.

¹⁵ Reactor Engineering Division Quarterly Report, Section I, ANL-5561 (April, 1956), p. 101.

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Table XIV

STATIC CORROSION TESTS OF BORON-STAINLESS STEEL

Source	Sample No.	Nominal Composition, %			Analyzed % B	Temp., F	Condition	Time, hr	Rate of Weight Change, (mg)/(cm ²)/(mo)	Comments
		Cr	Ni	B						
Electric Steel Foundry Co.	A-495	12	8	1	NA***	600	Degassed		+0.0130	Adherent black coating.
							Air satd.	2802	+0.0135	Adherent brown-black tarnish.
							Degassed		+0.0291	Pickled before test in 40% HNO ₃ - 4% HF. Adherent black coating.
	A-496	12	8	2	NA	600	Degassed		0.000	Adherent black coating.
							Air satd.	2802	-0.0482	Adherent grey-black coating.
							Degassed		+0.0065	Pickled before test in 40% HNO ₃ - 4% HF. Adherent black coating.
	A-497	12	8	3	NA	600	Degassed		-0.0101	Adherent black tarnish.
							Air satd.	2802	+0.0130	Adherent black coating.
							Degassed		-0.2370	Pickled before test in 40% HNO ₃ - 4% HF. Adherent black coating.
	A-4473b-1*			2.8					+0.0260	
	b-2*			2.8					+0.0199	
	a-1**	18	14	2.2	NA	550	Degassed	2377	-0.0494	Adherent dull black coating.
	a-2**			2.2					-0.0077	
	A-4561	19.7	12.2	4	NA	550	Degassed		-0.1242	Adherent blue, brown-black coating.
							Air satd.	329	-0.3880	Adherent blue, red-black coating.
Superior Steel Co.	A-4480	18	11	1	0.96, 1.01	550	Degassed		-0.0179	Adherent brown-black coating.
							Air satd.	2517	-0.0273	Adherent black coating.
Crucible Steel Co. (Heated to 2050F, forged at 1850F, and air cooled.)							Degassed		+0.0474	Adherent black coating. Area where samples are in contact is noticeable.
	A-4494a	18.8	8.7	2	1.85	550	Air satd.	1908	+0.0808	Adherent brown and black coating. Loose black coating where samples are in contact. Dark tarnish spot at one end.
(Same as above, then heat treated at 2000F for 1 hr and furnace cooled.)	A-4494g	18.8	8.7	2	1.96	550	Degassed		+0.1070	Adherent black coating.
							Air satd.	680	+0.0976	Adherent dark brown and blue tarnish.
Universal Cyclops	A-4538	18.1	14.8	1	1.16	550	Degassed		+0.0534	Adherent dark straw, red-brown, and blue tarnish.
							Air satd.	329	+0.0357	Adherent red-brown tarnish.

*As cast.

**Heat treated at 2256F for 3 hr.

***NA = Not analyzed.

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Table XV

STATIC CORROSION TESTS OF WELDED BORON-STAINLESS STEEL

Source	Sample No.	Origin of Sample	Type of Weld	Nominal Composition, %			Analyzed % B	Test Conditions			Rate of Weight Change, mg/cm ² /mo	Comments
				Cr	Ni	B		Temp., °F	Water Condition	Time, hr		
Universal Cyllops (vacuum melted)	A-4483b	Cross section of weld, then cut in half through weld.						600	Degassed	1345	-0.0253, -0.0380	Adherent dark brown, red, and blue tarnish. Grey-black tarnish on weld. Blue-black coating on contact area.
								550	Degassed	1345	-0.0253, -0.0211	Adherent brown-black tarnish. Grey-black tarnish on weld. Red-black coating on contact area.
								550	Air satd.	1345	-0.0168, -0.0422	Adherent dark brown tarnish. Grey-black tarnish on weld. Black coating on contact area.
	A-4483c	Cut from welded slab out of heat zone.	308 SS rod	18	15	1.2	0.68, 0.63	600	Degassed	1345	-0.0115, -0.0114	Adherent reddish-brown tarnish, blue-black on contact area.
								550	Degassed	1345	-0.0039, -0.0114	Adherent brown-black tarnish, black on contact area.
								550	Air satd.	1345	-0.0068, -0.0383	Adherent blue tarnish, brown around edges. Black tarnish on contact area.
	A-4483d	Cut from welded slab out of heat zone, parallel to rolling direction.						550	Degassed	1345	-0.0054, +0.0135	Adherent black tarnish. Contact area is noticeable.
	A-450 ⁻ -1,2		Helarc, bare 304 SS-1 ^o P rod, single bead, 1 ^o B to 304 SS.						Degassed	1019	+0.242	Slightly loose black coating.
									Air satd.	1019	+0.0040	Adherent brown, blue, and purple tarnish.
Superior Steel Co.	A-450 ⁻ -3,4		Helarc, bare 304 SS-1 ^o P rod, double bead, 1 ^o B to 1 ^o B.						Degassed	1019	+0.0397	Adherent black coating.
									Air satd.	1019	+0.0040	Adherent brown and blue tarnish.
	A-450 ⁻ -5,6		Helarc, bare 304 SS-1 ^o P rod, single bead, 304 SS to 1 ^o B.						Degassed	1019	+0.0040	Adherent black coating.
									Air satd.	1019	+0.0323	Adherent brown and blue tarnish.
	A-4508-7,8	Cross section of weld.	308 SS coated rod, single bead, 1 ^o P to 304 SS.	18	8	1	NA*	550	Degassed	1019	+0.0243	Adherent black coating.
									Air satd.	1019	+0.0040	Adherent grey-brown and blue-tarnish, brown on weld.
	A-4508-9,10		308 SS coated rod, double bead, 1 ^o B to 1 ^o B.						Degassed	1019	+0.0568	Adherent dull black coating.
									Air satd.	1019	-0.0202	Adherent brown, blue, and brown-black tarnish.
	A-4508-11,12		308 SS coated rod, single bead, 304 SS to 1 ^o B.						Degassed	1019	+0.0485	Adherent black coating.
Crucible Steel Co.									Air satd.	1019	+0.0081	Adherent brown, blue, and brown-black tarnish.
	A-4494		Arcos Chromend k rod, lime coated.	6.8	8.7	2	1.85		Degassed	986	+0.0497, +0.0497	Slightly loose black coating over adherent black coating. Black tarnish on weld.
									Air satd.	986	+0.0058, -0.0058	Adherent brown, blue, and green-black tarnish.
	A-4534a	Cross section of weld.	Inco A rod, 1.5 ^o B welded to 304 SS.	18.8	8.7	1.5	NA	550	Degassed	680	-0.0205	Adherent green-brown tarnish on boron-stainless steel. Adherent blue, brown, and purple tarnish on rest of sample.
									Air satd.	680	+0.1232	Adherent green, brown, and black tarnish on boron-stainless steel. Black tarnish on weld and brown on stainless steel.
	A-4534b		Arcos Chromend k rod, lime coated, 1.5 ^o B welded to 304 SS.	18.8	8.7	1.5	NA		Degassed	680	+0.0751	Adherent brown and blue tarnish.
									Air satd.	680	+0.0751	Adherent blue, straw, and brown tarnish.

*Not analyzed.

Table XVI

DYNAMIC CORROSION OF BORON-STAINLESS STEEL IN DEGASSED WATER AT 550FWater Velocity: 22 fps

Source	Sample No.	Type of Weld	Nominal Composition, %			Analyzed % B	Time, hr	Rate of Weight Change, (mg)/(cm ²)/(mo)	Comments
			Cr	Ni	B				
KAPL	A-4403a b	-	18	14	1.6 2.5	NA*	4379	-0.0615 -0.0485	Adherent black coating.
Electric Steel Foundry Co.	A-4463	Welded with 308 SS rod. Cross section of weld.	18	8	2.18	1.5, 1.49	3114	-0.0496, -0.0283	Adherent lustrous black tarnish. Loose black coating on edges. Weld same as sample.
Superior Steel Co.	A-4480	-	18	11	1	0.96, 1.01	2360	-0.0138, -0.0085	Thin loose black coating heavier on edges, over adherent black tarnish.
Universal Cyclops	A-4483a	Welded with 308 SS rod. Cross section of weld.	18	15	1.2	0.68, 0.63	2360	-0.0464, -0.0072	Adherent green-brown tarnish. Purple tarnish on weld. Loose black coating on edges.
	A-4483d	Sample cut from welded slab out of heat zone, parallel to rolling direction.					2360	-0.0213, -0.0214	Adherent brown-black tarnish. Loose black coating on edges.
	A-4538	-	18.1	14.8	1	1.16	725	-0.1355, -0.1712	Adherent black tarnish. Loose black coating on edges.
Crucible Steel Co. (Heated to 2050F, forged at 1850F, and air cooled.)	A-4494b	-	18.8	8.7	2	1.85	2360	-0.0477, -0.0346	Adherent black tarnish. Loose black coating on edges.
(Same as above, then heat treated at 2000F for 1 hr, furnace cooled.)	A-4494i, j	-	18.8	8.7	2	1.96	725	-0.1395, -0.1434	Adherent blue-black tarnish. Loose black coating on edges.
	A-4494	Welded with Arcos Chromend K rod, lime coated. Cross section of weld.	18.8	8.7	2	1.85	1448	-0.0685, -0.0630	Adherent blue-black tarnish. Brown-black tarnish on weld. Loose black coating on edges.

*Not analyzed.

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2. Corrosion of Miscellaneous Control Materials -
C. R. Breden

a. Hafnium

Hafnium-Hafnium Couples

A small assembly of two hafnium plates fastened together by spot welding, and with rivets of stainless steel, tantalum and zirconium, has accumulated a total of 3801 hr in degassed, demineralized water. The first 160 hr were at 500F, at which time the water temperature was increased to 550F. There has been no significant change in appearance.

Hafnium Crystal Bar (Foote Mineral Co.)

Eight samples representing eight crystal bars of AEC material from Westinghouse were exposed to 550F degassed, demineralized water for about 300 hr. All but one showed corrosion around the core rod area. In two samples the corrosion was greater than in the remainder. The Rockwell "B" hardness of the samples ranged from 104 for the samples showing the most corrosion, to 85 for the sample showing no corrosion.

One sample of crystal bar from the same source was tested in 600F degassed, demineralized water. After one week it showed the usual iridescent tarnish except for the core rod bond area which was white. After two additional weeks, the tarnish had darkened and the white ring had increased. After a total of five weeks, the depth of attack in the white ring had increased further, and the specimen showed radial cracks. The test was discontinued.

An additional six crystal bar pieces from the same source were exposed to 600F degassed, demineralized water. After one week all of the samples showed adherent, lustrous, multi-colored tarnish. One sample showed considerable attack (white corrosion products in the core rod bond area). Two additional weeks showed increased attack on the one sample. After a third period, also two weeks, the core had corroded out of the one sample. After a total of seven weeks exposure, a piece had cracked out of this sample and the main piece showed a large number of radial cracks. Hardness values on the corroded sample pieces at this time ranged from 44 to 85 R_B . One uncorroded sample showed hardness values ranging from 29 to 98 R_B . The largest grains in this specimen were the softest. Another sample showing no corrosion had hardness values ranging from 66 to 100 R_B . These results indicate the wide range of hardness that can exist in a single bar cross section. They also indicate that the corrosion-accelerating contaminants are confined to the core bond area and the hardness-producing factor is not necessarily corrosion producing.

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A total of 544 lb of crystal bar, each bar with an end cut off to expose the core area, was tested 351 hr in 550F degassed, demineralized water. After the test the bars were inspected and graded good, fair and poor, based on the degree of corrosion visible. The approximate yields were as follows:

Good	284 lb
Fair	135 lb
Poor	125 lb.

Corrosion, if it occurred, was mostly in the core bond area. The attack varied from a very slight ring of white corrosion products to heavy attack, where the core was corroded out or deeply pitted in the bond line area.

The crystal bars graded fair were cut in half at the bend of the hairpin and subjected to a 339-hour test in 550F degassed, demineralized water. No corrosion attack was observed at these cuts, indicating that the corrosion-accelerating contaminants tend to concentrate at the ends of the hairpin.

Rolled Hafnium

A sample of rolled hafnium processed by Allegheny-Ludlum was corrosion tested in degassed, demineralized water at 600F. After nine weeks, the weight changes were low, and no white or gray corrosion products were visible.

Two samples from the same stock which were tested in degassed, demineralized water at 680F for seven weeks exhibited low weight changes and satisfactory tarnish films. The hardness of these samples ranges from 98 to 107 R_B. This confirms observations above that the hardness-producing factor is not necessarily corrosion producing.

Two samples were dynamically tested in 550F water, followed by two periods in 525F water at about 20 fps (723 and 725 hr). Weight change rates for the two samples for the first period were -0.033 and -0.056 mg/(cm²)(mo). For the second period they were -0.011 and -0.026 mg/(cm²)(mo). The appearance of the films was satisfactory.

b. Aluminum-1% Boron-0.5% Nickel

Static corrosion tests on aluminum-boron-nickel alloys (used in BORAX-III as a burnable poison) have accumulated 2119 hr in 440F water. The samples show little change except for black, glossy slag, holes, and spalling, in the weld areas.

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c. Boron Carbide Solution Tests

Two samples of pressed boron carbide (Norton) were tested, respectively, in 420F and 500F degassed, demineralized water.

As shown in Table XVII both samples showed a decrease in weight change during the first test period. After the third period, the weight change of the sample in 500F began to increase, while the 420F sample did not. This is reflected by the amount of boron in the test water.

Table XVII

SOLUTION TESTS ON PRESSED BORON CARBIDE

Specimen Size: 1/4 in. square x 3 in. long

Test Period, hr	420F			500F		
	Wt. Change, mg/(cm ²)(mo)	Comments	B in Water, gm/ml	Wt. Change, mg/(cm ²)(mo)	Comments	B in Water, gm/ml
166	-0.316	Slightly loose, grey coating	0.44 0.43 0.47	-0.384	Slightly loose, grey coating	0.65 0.67
353	-0.085	Adherent, grey coating	1.06	-0.095	Adherent, grey coating	0.26 0.26
357	0.00	Adherent, grey coating	0.12 0.12	-0.042	Adherent, grey coating	0.29 0.30
353	-0.021	Adherent, grey coating	2.7	-0.181	Adherent, grey coating	21.0
330	-0.057	Adherent, grey coating	1.72 1.72	-0.408	Slightly loose, grey coating	18.0 18.0

E. Structural Material Corrosion Tests - N. R. Grant

The current results of static and dynamic corrosion tests of various materials for EBWR and other purposes are given in Tables XVIII and XIX. All tests are being continued unless otherwise indicated.

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Table XVIII

STATIC CORROSION TESTS ON MISCELLANEOUS STRUCTURAL MATERIALS

Material	Source	Sample No.	Nominal Composition	Test Conditions			Rate of Weight Change, $\text{mg}/(\text{cm}^2)(\text{mo})$	Comments
				Temp., F.	Water Condition	Time, hr		
304 Stainless Steel (Cladding for FWH pressure vessel.)	Babcock & Wilcox	A-4462	12% Cr, 8% Ni	550	Degassed	2541	+0.0135, +0.0030	Adherent dark brown coating. Area where the samples are in contact is noticeable.
					Air satd.		+0.0539, +0.0389	Adherent black coating. Loose brown-black coating where samples are in contact.
Stainless Steel	Universal Cyclops (SS A-202)	A-4526	18% Cr, 5% Ni, 8% Mn	500	Degassed	734	-0.0368	Adherent straw and blue tarnish.
					Air satd.		-0.0164	Adherent blue, purple, and brown tarnish.
				600	Degassed		+0.0062	Adherent brown and blue-green tarnish.
					Air satd.		-0.0428	Adherent blue and straw tarnish with streaks of brown.
Aluminum	Argonne	A-4336	2% Al + 0.5% Ni	500	Degassed	3055	+0.461	Test discontinued. Actual metal loss after removal of oxide -0.2 $\text{mg}/(\text{cm}^2)(\text{mo})$.
				550	Degassed	2932	+0.968	Test discontinued. Actual metal loss after removal of oxide -1.28 $\text{mg}/(\text{cm}^2)(\text{mo})$.
				600	Degassed	2932	+1.490	Test discontinued.
Aluminum (welded)	Argonne	A-4395	2% Al	420	Degassed	2682	+2.280	Adherent medium grey coating. Surface slightly blistered.
							+1.570, 0.641	Adherent medium grey coating.
Thermonol	Glenn I. Martin	A-4540	80% Fe, 16% Al, 3.4% Mo	500	Degassed	680	-0.0649	Thin, slightly loose black coating over adherent black tarnish.
				600	Degassed	680	+0.0157	Adherent black tarnish.
Thermonol	Universal Cyclops	A-4525	82% Fe, 14.8% Al, 3% Mo	500	Degassed	430	-0.0281	Thin loose black coating over adherent black tarnish.
					Air satd.		-0.0281	Adherent black tarnish with areas of brown.
				600	Degassed		-0.0844	Thin loose black coating over adherent black tarnish.
					Air satd.		-0.0562	Thin loose black coating over adherent black tarnish.
				680	Degassed		+0.1689	Thin loose black coating over adherent black tarnish.
					Air satd.		+0.0702	Adherent spotted red-brown coating.

Table XIX

DYNAMIC CORROSION OF MISCELLANEOUS STRUCTURAL MATERIALS

Water Velocity 22 ft./s.

Material	Source	Sample No.	Nominal Composition	Temp., F.	Condition	Time, hr	Rate of Weight Change, $\text{mg}/(\text{cm}^2)(\text{mo})$	Comment
Stainless Steel	Universal Cyclops (SS A-202)	A-4526e	18% Cr, 5% Ni, 8% Mn	550	Degassed	725	-0.0825	Adherent lustrous dark blue and brown tarnish. Loose black coating on edges.
Thermonol	Glenn I. Martin	A-4540c	80% Fe, 16% Al, 3.4% Mo	500	200 cc 1 H ₂	314	-8.97	Loose black coating.
Thermonol	Universal Cyclops	A-4525	82% Fe, 14.8% Al, 3% Mo	550	Degassed	725	-0.0908	Loose black coating.

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F. Control Rod Drive Mechanisms - C. F. Bullinger

1. Hydraulic - W. J. Kann

Testing of the hydraulic control rod drive system¹⁶ was interrupted after 1600 cycles to incorporate certain experimental variations and to examine the components.

The cylinder showed only a few burnish marks and no scoring or galling of the bore. The piston, piston rod, valve seat and dashpot showed no signs of wear or damage.

The original straight cylinder was then replaced with a cylinder of approximately 0.004 in. tapered bore per inch of length. Figure 44 shows the relationship of water flow versus piston position based on two early experiments. After break-away (1.8 gpm) the flow could be reduced. The instrumentation set the lower practical limit of piston position at about 5 in. The orifice diameter set the upper travel limit at 25 in. Small variations between up and down movement were attributed to instrument sensitivity. As expected, the data indicate that a linear relationship exists between flow and piston position.

FIG. 44
TAPERED CYLINDER - FLOW RELATIONSHIP

¹⁶ Reactor Engineering Division Quarterly Report, ANL-5561
(April, 1956) Fig. 54, p. 123.

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Future tests will resolve flow measurement and control limitation, and an orifice of sufficient diameter will be used to allow adequate flow for the full piston travel.

Scram tests were conducted with a small (3.8-in. ports), three-way, pilot-type solenoid valve. The valve reduced appreciably the initial actuation time lag but restricted the flow to such an extent that the net result was a longer scram time.

2. Magnetic Jack - J. N. Young

The experimental model magnetic jack control drive unit No. 18B¹⁷ has been operated a distance equivalent to 1125 cycles of 4-ft strokes with negligible wear of components. Most of the testing was done with a step length of 0.1 in. at 30 steps/minute and a load of 313 lb. The power consumption was about 500 watts. The power required to hold a given position was 50 watts. Maximum lifting capacity is about 900 lb. The only malfunctions experienced were electric coil failures.

A prototype model magnetic jack unit (No. 20) for EBWR has been built with waterproof, replaceable coils (Fig. 45). The performance characteristics of Units No. 18B and No. 20 differ only in power consumption. The latter requires 1000 watts for lifting and 100 watts for holding.

G. Equipment for Forced Circulation Operation of EBWR - S. A. Bernsen

1. Recirculation Pump

The operating conditions for the forced circulation pump have been established and pump specifications have been sent out for bids.

Nominal Operating Pressure	600 psig
Design Pressure	800 psig
Capacity	4×10^6 lb/hr
Net Positive Suction Head	15 psi
Net Developed Head	50 psi

¹⁷ Reactor Engineering Division Quarterly Report, Section I, ANL-5561 (April, 1956) p. 125.

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The specifications indicate a preference for a single unit and a desire that the pump have limited, but recoverable, leakage. However, dual pumps and/or canned rotor design will be acceptable. Further, it is hoped that the pump can be installed on flexible mounts to accommodate vertical and horizontal deflections due to thermal expansion of the piping, so that pipe lengths will not have to be increased or expansion joints employed in order to obtain the desired flexibility.

2. Forced Circulation Piping

The header system for the forced circulation piping in the area below the reactor has been sized and located.

It is estimated that the total pressure drop in the piping (including entrance and exit losses but exclusive of control valve pressure drop) will be about 22 psi at rated flow. It is estimated that the control valve will take 10-15 psi drop, leaving 13-18 psi (pump head) available for the core, orifices, and flow meters. It is expected that a core allowance of 10 psi should cover any conceivable core configuration for a 40-mw reactor.

3. Core Shroud Design

The lower part of the core shroud, constructed of a 1/2-in. thick stainless steel cylinder approximately $2\frac{1}{4}$ ft high, forms the forced circulation inlet plenum. The upper portion is a shroud to separate the core flow from the downcomer flow for the light water-moderated reactor. This upper section is constructed of a 1/8-in. thick aluminum-nickel cylinder approximately 5 ft high. The two cylinders are riveted together.

Since the pressure drop across the core is relatively small and the flow rates are large, some leakage from the inlet plenum to the downcomer and to the core zone outside the fuel element area can be tolerated. The lower shroud ring is designed to minimize the leakage at the bottom by seating on a machined ring welded to the lower pressure vessel head and parallel with the horizontal plane of the core support plate. In order to minimize the leakage between the lower shroud and the core support plate and still have the shroud seat in the ring on the lower head, a slit tubular stainless steel filler ring is placed between the shroud and the support plate. This tube is backed up by a 1/2-in. ring welded inside the shroud; the entire assembly is fastened to the support plate (which is fastened to the lower head) by four equally spaced sets of double lugs. This lug arrangement, while not very desirable, is necessary in order to fit the shroud over existing upper core structures.

Tests made on a full-size model of the support plate shroud seal show that the tube will fill the gap and allow for a vertical deflection of at least 0.100 in. without undue distortion of the shroud or stress on the bolts.

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This implies that the two gaps can be filled simultaneously (one at the lower head and one at the support plate), in spite of measurement errors in excess of $\pm 1/32$ in. on the distance from the lower head to the support plate seating surfaces.

H. Argonne Water Loop at MTR - G. A. Freund, E. L. Martinec

The irradiation of cartridge MTR-W-7 (a three-cycle corrosion rate determination of aluminum-nickel alloy) was extended until January 30, midway through MTR cycle 64. The loop was then shut down to permit certain modifications for the MTR-W-8 test. These include:

- (1) Process water repiping.
- (2) Replacement of main flowmeter with a pneumatic unit of adequate response. The flow condition is then handled by two pressure switches, one causing pump switching and the other, with a lower flow setting, causing reactor scram.
- (3) Enclosure of external equipment cubicle and back of instrument panel as a protection against building contamination in case of a leak.

Cartridge MTR-W-8 (an EBWR prototype fuel plate) was inserted at the start of MTR cycle 66 on February 27. Actual irradiation was begun on March 2. After four days at 440F while the reactor reached full power and a preliminary heat balance was taken, the loop was brought to nominal operating conditions of 485F and 1500 psi and continued to operate without major interruption until the end of the cycle.

Operation of the loop was satisfactory during the cycle with two minor exceptions:

- (1) The high-temperature switch on the ion-exchanger inlet was inadvertently reading the cubicle ambient temperature which was 140F because of the newly completed enclosure. An alarm resulted and operating personnel shut the ion-exchange system off causing it to reach ambient. The ion-exchange resin was apparently not damaged and the situation has been rectified by providing ventilation in the cubicle.
- (2) High pressure drop (greater than 5 psi) was observed on two of the main flow filters shortly after startup. These were replaced at the end of the cycle.

A preliminary heat balance indicates that the test plate generates 12 kw. This corresponds to a maximum heat flux of 300,000 Btu/(hr)(sq ft) and a maximum thermal neutron flux of 6×10^{13} at the plate.

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During the 610-MWD exposure of cycle 66, the estimated burnup of total uranium atoms was, therefore, 0.055%. Essentially all of this exposure was above 400F with 60% at 485F. A more accurate heat balance will be taken later when the test cartridge will be temporarily withdrawn from the high flux zone and its heat output will be determined by the difference in line heater power demand.

The EBWR prototype test plate contains the U-1.5% Nb-5% Zr alloy enriched to 1.44% U^{235} and is clad with Zircaloy 2. An identical natural uranium alloy plate is being exposed in the out-of-pile zone of the loop.

The tip of the second in-reactor tube, removed on October 31, 1955, was cut off by MTR personnel and has been returned to Argonne for examination. This tip, made of Type 316 stainless steel, has received an estimated maximum fast neutron exposure of 4.8×10^{20} nvt.

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PART B. FAST POWER BREEDER REACTOR PROGRAM

SUMMARY - L. J. Koch

The EBR-II Working Model has operated very successfully during the past quarter with only brief shutdowns to effect minor equipment modifications. The primary d-c electromagnetic pump has operated approximately 400 hr at 1,400 gpm with no difficulty. The auxiliary equipment, including the by-pass cold trap circuit, continues to operate very satisfactorily.

A prototype shutdown cooler, which duplicates the essential features of the shutdown coolers to be employed in the EBR-II system, was installed in the model. An analysis of the shutdown cooling requirements for EBR-II (fission product decay heat) was completed, and a cooler design was prepared to meet these requirements. A comparable analysis was made of the anticipated performance of the shutdown cooler for the Working Model which will be tested to correlate the experimental data with the analysis. This system operates entirely by natural convection, and it will be necessary to achieve the proper balance between thermal head and fluid friction to achieve the proper heat removal characteristics.

The irradiation tests of EBR-II pin-type fuel elements were continued in CP-5. No new samples were removed from the reactor during the quarter. The three samples presently under irradiation are uranium-fissium alloys of different alloy compositions. The first of these samples is scheduled for removal in early May, at which time it will have received an estimated burnup of approximately 1% of its total atoms.

Thermal cycling tests were conducted on fissium alloy samples, including both alpha cycles and beta cycles. The fissium alloy, which contains largely ruthenium and molybdenum as the alloy constituents, exhibited characteristics similar to the molybdenum alloys tested previously. Their behavior is comparable to the uranium-2% zirconium alloy under alpha cycling, but exhibit a much greater stability to thermal cycling into the beta temperature range (through the alpha-beta transformation temperature). These thermal stability data indicate that the "natural alloy" provided by pyrometallurgical processing may have desirable reactor fuel properties.

A 3,800-gpm, canned, rotor-type centrifugal pump was tested as a part of the Laboratory's sodium pump development program. This pump was originally constructed as a part of the SIR development program and was made available for test. Of particular interest was the performance of the sodium hydraulic bearings employed in this pump design. The pump failed before any significant data could be obtained. One of the bearings in question seized and galled very badly. Unfortunately, the failure occurred

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before the pump reached its normal operating speed, and the bearing, therefore, failed before adequate hydraulic pressure was available to permit it to perform as a "hydraulic bearing." The Laboratory is not contemplating the use of large, canned rotor pumps in the EBR-II, however, large centrifugal pumps can be expected to be more successful if a satisfactory bearing of this type can be developed. A 5,000-gpm mechanical pump presently under construction, as a prototype unit for the EBR-II secondary sodium system, incorporates a hydraulic bearing of this type.

The analysis of the data obtained from the liquid metal heat transfer loop was completed. The measured heat transfer coefficients agree reasonably well with the accepted correlations, except in the region of low flow velocities. These experiments were carried out with local heat fluxes as high as 6,000,000 Btu/(hr)(sq ft). These results provide considerable confidence in the design of the EBR-II where the maximum heat flux in the reactor core is only slightly in excess of 1,000,000 Btu/(hr)(sq ft).

An experimental program was initiated to study diffusion and the formation of iron-uranium compounds between surfaces of uranium and stainless steel immersed in sodium. If the assumed maximum fuel temperature limitation of 1200F can be raised by the development of thermally stable fuel alloys, it is anticipated that eutectic formation between the uranium fuel and the stainless steel fuel element tube will be limiting. The low melting point of the iron-uranium eutectic (1337F) also creates an undesirable temperature limitation with regard to short-term reactor transients. A test was conducted to evaluate a variety of surface conditions on both the uranium and stainless steel samples to determine if surface condition would inhibit diffusion. The results obtained were not very encouraging, in that there was little difference between the variety of samples tested. A second test included stainless steel samples which had been chromium plated. Chromium forms a Cr-U eutectic which has a melting temperature of 1575F. It cannot, therefore, be considered a permanent solution to this problem, but an increase of over 200F in the melting temperature would certainly represent an improvement. Work is also being carried out to develop a "barrier" which is essentially inert (for example, molybdenum); however, the more desirable barrier materials do not lend themselves to deposition on the inside surface of the small diameter fuel tubes. Investigations are under way in an effort to develop suitable techniques to accomplish the required surface deposition. Chromium is of interest because of the successful development in plating of gun barrels, etc.

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I. EXPERIMENTAL BREEDER REACTOR-II (EBR-II)

A. UNIVAC Calculations on EBR-II - W. B. Loewenstein

The analysis of several two-dimensional calculations pertinent to EBR-II Design Study No. 1 indicates that some previously reported results¹⁸ pertaining to the design may be improved. The latter were based on one-dimensional calculations with the usual separability assumption.

The reactor model chosen for the analysis is similar to the model illustrated in Fig. 46 of the referenced report. The hexagonal-shaped regions of the reactor have again been cylindricized on an equal area basis. The compositions (in vol-%) of the various regions are given below:

<u>Region</u>	<u>Fuel</u>	<u>Depleted U</u>	<u>Stainless Steel</u>	<u>Na</u>
Core	32	-	17	51
Central Blanket	-	55	13	32
Upper Blanket	-	38	18	44
Lower Blanket	-	38	18	44
Inner Blanket	-	55	13	32
Outer Blanket	-	70	12	18
Control Rod (active)	22	-	18	60
Control Rod (inactive)	-	-	13	87
Upper Gap	-	-	40	47
Lower Gap	-	-	37	57

The term "fuel" describes the mixture of U^{235} and U^{238} in the fuel elements. The major problem in the analysis is the determination of the relative amounts of these two constituents with all other design parameters fixed.

The previously reported analysis was based on an iterative procedure whereby cylindrical and slab calculations were ultimately obtained which were critical with the same core enrichment and also satisfied the relationship:

$$K^2 = b_r^2 + b_z^2 \quad ,$$

¹⁸Reactor Engineering Division Quarterly Report, ANL-5461 (April 15, 1955), p. 119.

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where K^2 is the material buckling of the critical core composition, b_r^2 is the radial buckling of a critical slab calculation, and b_z^2 is the axial buckling of the critical cylindrical calculation.

The application of two-dimensional calculations using the KAPL-MUG-II code provides a test of the aforementioned analysis and assumptions. These new calculations and their one-dimensional analogues are based on two-energy group analysis, because the present two-dimensional code does not provide for multigroup inelastic scattering effects. Because of geometrical code limitations, it is also impossible to calculate the "best" cylindrical version of the design. The latter difficulty is caused principally by the requirement of uniform mesh point spacing, in a given direction, for all regions.

The two-dimensional (r,z) calculation describing Design Study No. 1 gives a critical enrichment $C = 0.6067$, where

$$C = \frac{N^{235}}{N^{235} + N^{238}}$$

and N^{235} and N^{238} give the number of U^{235} and U^{238} atoms per unit core volume, respectively. This indicates the core U^{235}/U^{238} ratio to be 1.54.

It is possible to compare this result with that obtained using two-group, one-dimensional and bare core buckling calculations (UNIVAC Spectrum 14). In cylindrical geometry, several calculations have been prepared featuring the radial dimensions of the design to determine the relationship between axial buckling and core enrichment. Similarly, in slab geometry, the axial dimensions of the design are used to determine the relationship between radial buckling and core enrichment. In addition, it is possible to obtain a relationship between the core material buckling and core enrichment, all of these calculations being referred to critical conditions.

In a one-dimensional slab calculation, an ambiguity exists in the proper model to use. For the core region it was decided to use a composition representative of a fuel element, rather than an average over central blanket, core and control rod region. For the blanket, again various possibilities exist. Calculations were prepared using both the higher density central and inner blanket compositions as well as the lower density upper and lower blanket compositions. The reactor core must "see" an effective blanket which has a mean composition of which the typical calculations are upper and lower limits, respectively.

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For typical EBR-II type enrichments ($0.55 < C < 0.71$) the relationships referred to above are essentially linear and are given by:

$$K^2 = 0.013845 C + 0.000068 \quad (1)$$

$$b_z^2 = 0.009797 C - 0.003495 \quad (2)$$

$$b_{r\ell}^2 = 0.01160 C - 0.001752 \quad (3)$$

$$b_{rh}^2 = 0.011604 C - 0.001694 \quad (4)$$

where

K^2 is the material buckling

b_z^2 is the axial buckling of a cylindrical calculation

$b_{r\ell}^2$ is the radial buckling of a slab calculation using the low-density blanket

b_{rh}^2 is the radial buckling of a slab calculation using the high-density blanket

$$C = \frac{N^{235}}{N^{235} + N^{238}} \text{ in core.}$$

The iterative one-dimensional analysis then asks for which enrichment is

$$K^2 = b_z^2 + b_r^2$$

satisfied and gives $C_\ell = 0.7035$ and $C_h = 0.6958$, neither of which being the critical enrichment for the corresponding two-dimensional calculation where $C = 0.6067$. Since C is directly proportional to the critical mass, it is apparent that the one-dimensional solution based on the separability assumption predicts a critical mass which is about 15% higher than that based on the two-dimensional calculation. The obvious conclusion to be drawn is that the separability assumption is not reliable for an assembly with as complicated a geometry as that suggested by Design Study No. 1. It may be further suggested that the relatively simple slab calculation does not truly represent the axial properties of the system.

It is possible to assign an axial reflector saving by assuming the critical enrichment specified by the two-dimensional calculation and determining the bare core height with which a corresponding cylindrical one-dimensional calculation will be critical with the same core enrichment. This results in an axial reflector saving of about 13.5 cm, the total bare core height being 63.46 cm. It should be noted that this is quite different from the 9.2-cm reflector saving reported in ANL-5461. The latter was based on the one-dimensional iterative procedure already discussed. However, the new value is perhaps somewhat optimistic because the two-dimensional r, z calculation is one symmetric about the $z = 0$ plane and features the parameters of the structural gap between the core and the lower blanket.

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It is not unreasonable to suppose that a calculated reflector saving does not vary too much with the particular set of cross sections used in the calculation thereof. Therefore, it is possible to use the bare core height implied by the two-group analysis and apply it to a ten-group calculation (UNIVAC Spectrum 16). With an extrapolated height of 63.45 cm, the critical enrichment is given by $C = 0.5886$, with the U^{235}/U^{238} ratio being 1.43.

The two-group calculations for Design Study No. 1 based on the two-dimensional analysis with eight of the fuel sections of the twelve control rods adjacent to the core indicate that the critical mass is 187 kg U^{235} . The ten-group calculations using the 13.5-cm reflector saving implied by the two-dimensional analysis indicate that the critical mass is 182 kg. The latter calculation is perhaps too optimistic, not only on the basis of reflector saving already discussed, but also because $\alpha^{U^{235}}$ is slightly too low for UNIVAC Spectrum 16, since the measured values of α in the EBR core are slightly larger than those calculated by using the ten-group cross sections.

It is also possible to obtain expressions similar to Eqs. (1) to (4) for the ten-group, one-dimensional calculations. These are given by:

$$K^2 = 0.0130 C + 0.0006 \quad (5)$$

$$b_z^2 = 0.00915 C - 0.00293 \quad (6)$$

$$b_{r\ell}^2 = 0.01060 C - 0.001206 \quad (7)$$

$$b_{rh}^2 = 0.01066 C - 0.001189. \quad (8)$$

If the two- and ten-group enrichments are $C = 0.6067$ and 0.5886 , respectively, it can be seen that at criticality the following conditions will apply.

	<u>2-Group</u>	<u>10-Group</u>
C	0.6067	0.5886
K^2	0.00848	0.00826
b_z^2	0.00245	0.00245
b_{rh}^2	0.00535	0.00503
$b_{r\ell}^2$	0.00529	0.00503
Δ_h	0.00068	0.00072
Δ_ℓ	0.00074	0.00078

where

$$\Delta \equiv K^2 - b_z^2 - b_r^2$$

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In conclusion, the calculations indicate that the present "best" critical mass of EBR-II, as described by Design Study No. 1, is 182 kg U²³⁵.

Further tests of these analyses are under investigation. These are concerned with Assemblies 2 and 3 of the ZPR-II program. Assembly 2 is one not involving the use of a central blanket, while Assembly 3 does incorporate this feature.

B. Irradiation Tests of EBR-II Type Fuel Elements - E. S. Sowa

Irradiation tests of the EBR-II type fuel elements were continued. Two elements were loaded into CP-5 during this period. The first, CP-5-8, loaded on January 16, was a 20% enriched, fission alloy fuel pin containing 96.91% U, 0.09% Zr, 0.01% Nb, 1.54% Mo, 1.16% Ru, 0.19% Rh and 0.10% Pd. The fuel pin was static cast about 0.144 in. diameter and was reduced to the final diameter of 0.1423 in. (average) by swaging out of lead at 800C. The entire length of 14.22 in. was made by percussion welding a 5-in. long piece to a 9.22-in. piece. During the loading process the fuel was broken into three pieces; one fracture occurred at the weld, and the second 3.17 in. below. The thermocouples on this sample did not show any erratic behavior after startup of CP-5. As of March 31, the sample had accumulated an estimated burnup of 0.54 total atom per cent and had been exposed to 160 thermal cycles. The fuel is operating at an approximate central metal temperature of 600F.

The second element loaded (CP-5-9) was a 30% enriched, fission alloy pin containing 95% U, 0.2% Zr, 2.5% Mo, 1.5% Ru, 0.3% Rh, 0.5% Pd. The fuel was cast by forcing the molten alloy up into a quartz tube. The 0.145-in. diameter cast pin was centerless ground to a final diameter of 0.140 in. To achieve a length of 14.22 in., two pieces cast by the above-described technique, one 5.41 in. long and one 8.81 in. long, were percussion welded together. This element was loaded on March 19, 1956, and, as of March 31, had accumulated an estimated burnup of 0.09 total atom per cent and 32 thermal cycles. The approximate central metal temperature is 680F.

As of March 31, element CP-5-7 had been irradiated to an estimated burnup of 0.88 total atom per cent and had accumulated 245 thermal cycles. The element is scheduled to be unloaded on or about May 7.

C. Natural Convection Shutdown Cooling System - W. H. Bond

A natural convection cooling system has been designed for EBR-II to remove the heat generated by fission product decay after shutdown of the reactor. The system cools the bulk sodium in the primary tank, removing the heat accumulating due to natural circulation in the primary coolant circuit. The complete system operates by natural convection and employs no

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pumps or valves which might influence the system reliability. In the event of complete loss of auxiliary power after shutdown, this system will provide adequate cooling for the reactor and its associated equipment.

In order to choose a reasonable capacity for the EBR-II shutdown cooling system, the temperature of the primary tank sodium was determined as a function of time after shutdown, and the amount of heat dissipated from the primary tank. The results of this analysis are shown on Fig. 46. From this information it was considered desirable to provide 500 kw of cooling capacity. In order to improve the reliability of the emergency cooling, two independent systems are employed, each with a capacity of 250 kw. It can be seen from Fig. 46 that, normally, a temperature rise of 20F or less can be expected after shutdown. In the event one cooler becomes inoperative, the temperature may rise about 90F about 50 hr after shutdown. The large heat absorption capacity of the primary tank and the contained sodium allows ample time to initiate other emergency cooling measures should this become necessary.

The primary tank cooler is shown in Fig. 47. Figures 48 and 49 show the estimated performance of one cooler. It is interesting to note that the bayonet cooler at 0.5-fps NaK annulus velocity is operating at a rather high effectiveness. This indicates that the unit should function as planned, even though the heat transfer coefficients used in predicting the performance are not known precisely.

An experimental cooling loop has been installed in the EBR-II Working Model in order to substantiate the design features of this system. The experimental system was arbitrarily designed to remove approximately 50 kw of heat from the model tank at 500F.

The test system is shown schematically in Fig. 50. Heat is removed from the model tank by the concentric tube "bayonet" cooler. Since NaK is employed in the cooler loop, and sodium in the model tank, a double-walled construction is employed with sodium filling the annulus between in order to avoid contaminating the reactor sodium with potassium in the event of a tube failure. The sodium-filled annulus serves to thermally bond the two pipes. The use of two pipes also reduces thermal stresses by a factor greater than two compared to a single wall. The central downcomer pipe is also of double-wall construction. This insulates the two NaK streams as the space between the pipes is filled with a gas. This insulating feature improves the average heat transfer temperature difference and increases the thermal head for natural convection by keeping the cold leg of the system cool to the very lowest extremity. The construction is such as to prevent any thermal stresses being set up between the various pipes of the bayonet cooler.

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K. KUCZEN: J.K., 4-17-56

FIG. 47
EBR-II BAYONET HEAT EXCHANGER

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FIG. 48
PREDICTED PERFORMANCE CHARACTERISTICS OF
EBR-II BAYONET COOLER (CAPACITY: 250 kw)

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FIG. 49
PREDICTED HEAT TRANSFER CHARACTERISTICS
OF EBR-II BAYONET COOLER

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FIG. 50
SCHEMATIC DIAGRAM OF EXPERIMENTAL NATURAL CONVECTION LOOP

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The NaK flows upward after being heated in the bayonet exchanger and is cooled at the top of the system by air in a finned-tube exchanger of conventional design. The air is drawn through the exchanger by natural convection in the stack above the exchanger. Dampers are installed in the stack to control the output of the complete system by regulating the amount of cooling air flow.

In order to evaluate the operation of this system, thermocouples are placed at the inlet and outlet of each exchanger, an electromagnetic flow meter is installed in the NaK loop, power to the model tank can be monitored, and the stack air velocity and temperature can be determined by a sensitive thermoanemometer and thermocouple.

In designing the piping system, it was necessary to place the dump and fill line in the cold leg, so that on filling the NaK loop, the hot leg will be hot and the cold leg will be cold. With this arrangement, the NaK filling the hot leg must first pass through the bayonet exchanger, and the NaK filling the cold leg by-passes the bayonet exchanger until the system is filled and starts to operate.

The heat transfer effectiveness of the bayonet exchanger as a function of NaK velocity in the annulus is shown in Fig. 51. Actually, a series of similar curves depending on the bulk-sodium temperature can be developed, because of the variation in the properties of sodium with temperature. However, these variations are much smaller than the estimated accuracy of the method, and the data shown are for a sodium temperature of 500F.

The matched performance of the bayonet exchanger with the thermal head and friction characteristics of the NaK loop are shown

FIG. 51
PREDICTED PERFORMANCE CHARACTERISTICS
OF MODEL HEAT EXCHANGER

The matched performance of the bayonet exchanger with the thermal head and friction characteristics of the NaK loop are shown in Fig. 52. Since the bulk sodium temperature in the model tank is variable, temperature differences are shown so that the curves are applicable for any sodium temperature. The choice of coordinates and the differences plotted permit rapid and simple comparison with experimental data.

An interesting result of this analytical investigation is the shape of the friction characteristic of the loop. The cubic characteristic of this line indicates that the dampers can be used to throttle the heat transferred to a small value without decreasing the loop flow velocity to a very low value. During normal reactor operation, only a small amount of heat need be dissipated in order to keep the loop circulating at a reasonable velocity, permitting the loop to quickly attain operation at high heat transfer rates.

The experimental cooling loop will be operated to correlate the analytical performance predicted in order to further refine the EBR-II design analysis, and to provide working experience to indicate any mechanical or operational weaknesses in the system. Two preliminary shakedown runs have shown that natural circulation is rapidly established in the proper direction. An accurate quantitative evaluation has been initiated.

D. Iron-Uranium Compound Formation at Sodium-Wetted Uranium-Stainless Steel Interfaces - R. A. Stella

Since irradiation growth of the fuel pin in an EBR-II fuel element would result in contact, under pressure, of the sodium-wetted uranium and the stainless steel clad, a series of tests are in progress to investigate diffusion and compound formation at such an interface.

In each test there are 30 samples which afford 24 diffusion interfaces for examination. The samples (1/4-in. right cylinders) are stacked five samples to a group. Each group, consisting of alternate stainless steel and uranium samples, is attached to a loading pin which can be weighted to effect up to 15,000 psi pressure at the interface. The pot is filled with sodium and heated to test temperature. No load is applied to the samples until sodium has wetted the sample surfaces.

Tests 1 and 2 have been completed. The samples were Type 304 stainless steel and 2% zirconium-uranium alloy.

Test No. 1

This test was conducted to determine whether surface treatments which are general practice, or arise from manufacturing processes, inhibit diffusion and compound formation at uranium-stainless steel interfaces.

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FIG. 52
PREDICTED HEAT TRANSFER CHARACTERISTICS
OF MODEL PROTOTYPE BAYONET COOLER

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Surface treatment for the stainless steel samples included electropolishing; acid passivating; and oxidation in sodium, room temperature air, and 1000F air. Surface treatment of uranium samples included electropolishing, oxidation in sodium, and oxidation to a first straw color in air. Test temperature was $1300\text{F} \pm 15\text{F}$; duration 250 hr with interface pressures of 15,000 and 1,500 psi.

Results

An iron-uranium compound formed at each interface to a varying degree regardless of previous surface treatment of either the uranium or stainless steel. Spectrochemical analyses showed the diffusion compound composition to be approximately 90% U, 10% Fe.

Within the range of test pressure (15,000 and 1,500 psi) the rate of compound formation appeared independent of unit pressure at the interface. The samples under 15,000 psi pressure deformed to the extent that the stainless steel samples were embedded in the uranium samples to a depth of $1/16$ in. The final stress in these samples was between 15,000 and 1,500 psi.

The iron-uranium compound thickness was relatively uniform over each sample; the thickness variation was within 20%. The interface which formed the least thick compound was that between the stainless steel sample passivated in acid (2% HF, 28% HNO₃, 70% H₂O) and the uranium sample oxidized in air to a straw color. Chemical analysis of the passivating acid indicated Fe, Ni, and Cr were removed from the surface in direct proportion to the amount of these elements present; hence, the absence or predominance of one element at the surface did not cause the slower diffusion rate. The uranium samples for these interfaces had a discoloration band that appeared in the uranium, just ahead of the advancing front of spherical points of compound formation which eventually blended into a solid band of iron-uranium. This discoloration (blue on a reddish-brown background) may have indicated the presence of oxides formed on the stainless steel surface during acid passivating and carried into the uranium by the diffusing iron, which may have inhibited diffusion for a short time. Figure 53 shows a typical layer formation of the iron-uranium compound: (a) the iron-uranium compound (golden color) layer of approximately 0.001 in. thickness; (b) circular points of compound formation in the uranium; (c) the zirconium-uranium alloy (reddish-brown); and (d) dark bakelite-filled region occupied by stainless steel during test.

FIG. 53
LAYER OF Fe-U FORMED AT INTERFACE
OF URANIUM-STAINLESS STEEL SAMPLES
(X850)

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Test No. 2

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This test was conducted to determine whether a thin chrome plate would inhibit Fe-U compound formation. (Chromium forms a Cr-U eutectic alloy at $1575^{\circ}\text{F} \pm 18^{\circ}\text{F}$ as compared to Ni-U at 1364°F and Fe-U at 1337°F .) One-half the stainless steel cylinders were plated with different thicknesses (0.001 - 0.005 in.) of chrome plate; the remainder were Type 304 stainless steel machined to a finish of 5-15 microinches and oxidized in room temperature air. Test conditions were: temperature $1300^{\circ}\text{F} \pm 15^{\circ}\text{F}$; duration 40 hr, interface pressures 1,500 and 250 psi.

Results

No measurable compound formation occurred.

On visual inspection, the surface of the uranium samples in contact with stainless steel exhibited a bronze or golden color characteristic of the iron-uranium compound in Test No. 1, while uranium samples in contact with chrome plated surfaces did not.

E. EBR-II Shield Design Calculations - M. Grotenhuis, H. I. Kraig,
A. E. McArthy

Shielding a reactor consists primarily of slowing down and absorbing the fast neutrons that escape from the core, as well as the secondary radiation produced in the elimination of these neutrons. Other radiation escaping from the core can produce only local effects, e.g., gamma-ray heating near the core. There are no extremely efficient materials for stopping fast neutrons, such as is boron in the case of thermal neutrons. Fast neutrons must be reduced in energy by elastic or inelastic scattering and then absorbed at thermal or near thermal energies where neutron absorption begins to be appreciable. Probably the most efficient way to reduce fast neutrons to thermal is to scatter them inelastically to intermediate energies and then scatter them elastically to thermal.

If there is enough moderating material in the shield, the fast neutron flux may be adequately represented by removal theory.¹⁹ In turn, the thermal neutron flux distribution may be calculated by diffusion theory with the source term equal to the loss of fast neutron flux,

$$D_s \nabla^2 \Phi_s(x) - \sigma_{as} \Phi_s(x) + \sigma_r \Phi_r(x) = 0. \quad (1)$$

If it can be established that the neutrons of intermediate energy are not as penetrating as the fast neutrons, the shield may be completely designed knowing just the fast and thermal neutron flux. This will be referred to as the two-group method, although not in the usual sense.

¹⁹J. W. Butler, M. Grotenhuis, "EBWR Shield Design," ANL-5544, (March 1, 1956).

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A fast reactor generally has a preponderance of inelastic scattering materials. In order to describe the neutron flux in such a reactor it is necessary to use a large number of energy groups. This will evidently be as adequate for shielding studies as for reactor characteristics. However, in obtaining required information pertaining to reactor core design, the given machine program RE-7²⁰ cannot at the same time yield neutron flux distributions throughout the entire shield. It may be desirable to set up a separate program for shielding calculations, although it is doubtful that even then the fluxes throughout the entire shield could be adequately described because of the number of significant figures required.

A first glance at the EBR-II reactor indicated that there was a lack of moderating material. Inelastic scattering materials are not only inefficient shields by themselves, but, also, are more difficult media in which to calculate neutron fluxes. Under the circumstances a moderator was in order for the neutron shield material. In addition, a thermal neutron absorber is needed to eliminate the thermal neutrons before they get into the sodium tank. An ideal material, one that satisfies all these requirements for the neutron shield, is boron carbide. In addition to the other features, it withstands high temperatures.

The thickness of the neutron shield was determined by a maximum permissible neutron activation²¹ in the sodium tank. The thermal neutron flux was assumed to be the source of the activation and was computed by the two-group method as was the EBR-1 shielding data analysis.²² The thickness chosen was two feet, although in the radial direction this was one foot of boron carbide preceded by one foot of graphite. The one foot of graphite could not have an undesirable effect on the thermal neutron flux in the sodium and it would presumably permit more neutrons to be absorbed in the blanket.

The RE-7 program on the UNIVAC is set up to operate a twenty-group problem. Two such problems, 25W01 and 25W02, have been done to describe the EBR-II neutron fluxes for shielding purposes. These problems extended only through the neutron shield, although at the present time attempts are being made to extend them out into the sodium tank. With these two problems to serve as a guide, "two-group" calculations were made to extend the fast (removal) flux and the thermal flux through the remainder of the shield.²³

²⁰R. Brodsky, A. Radkowsky, "A Bibliography of Available Digital Computer Codes for Nuclear Reactor Problems," AECU-3078, (October 14, 1955).

²¹Reactor Engineering Quarterly Report, ANL-5371 (January 15, 1955), p. 247.

²²Reactor Engineering Quarterly Report, ANL-5297 (July 15, 1954), p. 63.

²³Reactor Engineering Quarterly Report, ANL-5561 (April, 1956).

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According to these studies the shielding is adequate, although a reasonable doubt exists as to whether the thermal neutron flux is properly represented by the two-group method. The assumption that the fast neutron flux becomes thermal as soon as it leaves the fast range is not a valid representation in a region such as sodium. Therefore, an intermediate energy group is defined between the removal fast flux and the thermal flux. The thermal neutron flux is then described by

$$D_s \nabla^2 \Phi_s - \sigma_{as} \Phi_s + \sigma_i \Phi_i = 0 \quad , \quad (2)$$

and the intermediate neutron flux by

$$D_i \nabla^2 \Phi_i - \sigma_{ai} \Phi_i + \sigma_r \Phi_r = 0 \quad . \quad (3)$$

This will be referred to as the three-group method, although again not in the usual sense of the term.

The energy ranges involved are roughly 1 Mev and up for removal fast flux, and 1 Mev to thermal for the intermediate flux. Thus the removal fast flux corresponds very closely to the first group in the 20-group calculation, the intermediate group represents the groups 2 through 19, and the thermal group is group 20.

The three-group problem depends upon the constants D_f and τ . As both of these constants depend upon the neutron spectrum, they are difficult to predict in a situation where the spectrum is not well known. Although the twenty-group problems did not extend into the sodium tank they do provide a measure of the spectrum change from the reactor through the neutron shield. The constants were then computed for the spectra as given by 20-group points in graphite and boron carbide. The results are given in Table XX. Although these constants cannot apply directly and, in fact, are not quite properly weighted with the spectrum, they may serve as a measure of the change in magnitude and thus may be a guide to their use in the diffusion problem. The problem is, in fact, relatively insensitive to these constants. The thermal neutron flux has then been computed in both sodium and graphite by both two-group and three-group methods.

The graphite serves as a comparison region between the twenty-group problem and the two- and three-group problems. It is hoped that the comparison will serve to indicate relative values of these methods of computing neutron fluxes and can thus be a guide to the computations in the axial direction.

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Table XXFAST NEUTRON CONSTANTS FOR SODIUM AND GRAPHITE

Spectrum*	Graphite		Sodium	
	τ	D_f	τ	D_f
Fission	350	1.04	11,100	4.61
700	180	0.912	8,100	4.74
703	130	0.915	6,200	4.84
706	100	0.916	5,400	-
800	200	0.980	8,400	4.78
803	310	1.03	10,600	-

$$3 D_f = \frac{\int_0^{u_{th}} du \frac{\int_0^u d\eta f(\eta)}{\xi \Sigma_s (1 - \bar{\mu}_0) \Sigma_s}}{\int_0^{u_{th}} du \frac{\int_0^u d\eta f(\eta)}{\xi \Sigma_s}}$$

$$3\tau = \int_0^{u_{th}} du \frac{\int_0^u d\eta f(\eta)}{\xi \Sigma_s (1 - \bar{\mu}_0) \Sigma_s} + \int_0^{u_{th}} du \frac{f(u)}{\Sigma_s^2(u)} + \frac{1}{\Sigma_s^2(u_{th})}$$

$$\bar{\tau} = \frac{\int_{u_{th}}^{u_0} du \tau(u) f(u)}{\int_{u_{th}}^{u_0} du f(u)}$$

*Numbers refer to the neutron spectra at positions in the neutron shield as derived from Problem 25W02.

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Figure 54 is a comparison of the fast neutron fluxes in graphite as computed by removal theory with the equivalent fast neutron flux computed by

FIG. 54
COMPARISON OF CALCULATED FAST
NEUTRON FLUX IN GRAPHITE

25W02. The removal flux Φ_{r1} was computed from a constant source term obtained by averaging the source term of fast neutrons linearly over the source volume,

$$\bar{S} = \frac{\text{n/sec in source}}{\text{volume of source}}$$

The removal flux Φ_{r2} was computed from a source term obtained by averaging the source term of fast neutrons over the source volume with an exponential weighting factor,

$$\bar{S} = \frac{\int_V dv S(r) e^{-\sigma r}}{\int_V dv e^{-\sigma r}}$$

This was done for the outer blanket region, as investigation proved this to be the only appreciable source of fast neutrons in the shield. The latter process yields a more exact representation, however, since it is desirable for shielding calculations to be pessimistic; Φ_{r1} , the higher flux, was used throughout.

Figure 55 is a comparison of the intermediate fluxes in graphite as computed by the three-group method and the intermediate flux given by 25W02. The problems are not independent in that the boundary condition

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for the three-group intermediate flux was taken from the 20-group problem. If the boundary flux is not so available it may not be too bad an approximation to call it zero.

FIG. 55
COMPARISON OF CALCULATED INTERMEDIATE NEUTRON FLUX IN GRAPHITE

Figure 56 is a comparison of the thermal neutron flux in graphite as given by the three methods. A noteworthy feature, also indicated in Fig. 55, is that the three-group problem is relatively insensitive to the variations of τ used in the problems.

FIG. 56
COMPARISON OF CALCULATED THERMAL NEUTRON FLUX IN GRAPHITE

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Figure 57 is a single three-group problem given separately for the purpose of comparing the relative magnitudes of the three fluxes.

FIG. 57
RESULTS OF 3-GROUP CALCULATION
($D_f = 1.64$; $\tau = 100$)

Figure 58 is a comparison of the two-group and three-group thermal neutron fluxes, each of which had the same removal fast flux as a primary source. Indications are that the sodium is not enough of a moderator for two-group theory to be an adequate representation for the thermal flux. Another way of stating this is that the intermediate energy flux is more penetrating than the fast, or, that the assumption that a fast neutron collision results in a thermal neutron immediately is not very realistic.

From this study it would seem that the twenty-group computations may be extended by either two- or three-group methods, the number of groups depending on the moderating value of the medium. For example, either two- or three-group methods in the graphite would be adequate for shielding purposes. However, the three-group method would be preferred in the sodium. In the concrete following the sodium, the two-group method may be expected to represent the thermal flux once the intermediate energy flux from the sodium has been thermalized.

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FIG. 58
COMPARISON OF CALCULATED NEUTRON FLUXES IN SODIUM

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II. EBR-II WORKING MODEL - D. V. Hammond, E. Hutter, D. Nicoll

During the past quarter, the EBR-II Working Model system has been maintained at a temperature of 750F, except on three occasions when the temperature was lowered to effect modifications of equipment.

During this period the d-c electromagnetic pump has operated at 1400 gpm for approximately 400 hr with no difficulty.

A total of 47 fuel assembly transfers were completed satisfactorily. The redesigned fuel rod gripper mechanism, as shown in Fig. 59, has eliminated the problem of the assemblies "hanging up" with the jaws open. The new design incorporates a funnel-shaped guide sleeve into which the jaws recede when they are opened, eliminating all surface discontinuities. The new gripper mechanism also includes a sensing device which indicates the position of the fuel assembly with respect to the gripper jaws. This sensing device acts as an ejection mechanism to forcibly remove the assembly when the jaws are opened. This feature has also been successful.

Circuitry was added to the control drive system to permit automatic cycling. A total of 3,000 cycles were completed. After approximately 1,500 cycles, some difficulty was experienced with the sensing mechanism which performs the same function as the mechanism described above. The sensing mechanism is only operated when the control rod is released from the drive mechanism and does not function during normal control rod operation. At the end of 3,000 cycles, the sensing mechanism was inoperative and the latch mechanism was not operating properly. The control drive unit was removed from the system for inspection. It was found that the pins in the latching mechanism had become loose, and one pin had fallen out of the assembly. In this experimental unit, the pins were held in place by snap rings to permit simple assembly and disassembly. The snap rings apparently had lost their tension due to the high-temperature operation and had failed. The remainder of the latch mechanism was in satisfactory condition, and new pins were installed with a more secure fastener. The failure of the sensing mechanism was due to sodium which entered the sealed lower assembly and destroyed the lower graphite guide bushing. In this experimental unit, the lower assembly was sealed by flat, gasketed joints to permit ease of disassembly. Bellows are employed where relative motion is required. The bellows were satisfactory; however, one of the gasketed joints had leaked. A new bushing was installed, and new gaskets were employed in reassembling the unit.

All of the auxiliary equipment, including electrical heating systems and the cold trap system, have worked satisfactorily during this period. The large sodium system has operated with surprisingly few difficulties; this has been very reassuring with regard to the feasibility of the EBR-II reactor system design.

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III. SUPPORTING AND ALTERNATE DESIGN AND RESEARCH DEVELOPMENT

A. Thermal Cycling of Fissium Alloys - J. E. Kemme, H. O. Monson

A series of thermal cycling tests comparing the thermal stability of low molybdenum-uranium and low zirconium-uranium alloys has previously been completed and reported.²⁴ Cycling was accomplished through two temperature ranges, approximately 150 - 1150F and 200 - 1320F. The general excellence of the molybdenum-uranium alloys under cycling in either range indicates that these alloys should permit operation, or at least considerable cycling, at temperatures above the alpha-beta transition temperature (1220F) of unalloyed uranium (provided that these alloys exhibit suitable resistance to radiation damage, which has not yet been established). The tests further indicated that the low molybdenum alloys are particularly unaffected by cycling when canned in fuel element tubing such as that prescribed for the fuel alloy of EBR-II.

During the present period, thermal cycling tests have been conducted on certain "fissium" alloys. Of great interest in the EBR-II fuel element program is the effect of accumulated fission product impurities on the thermal stability of the fuel. The reprocessing cycle selected for use in the EBR-II plant employs pyrometallurgical processing of the fuel. This method features simple process steps with uncomplicated equipment, but does not achieve complete removal of fission products. Certain of the noble metals, for example, build up to equilibrium values which are established by the characteristics of the process. To obtain data concerning the effects of retained fission products on thermal stability, synthetic alloys (termed "fissium") were prepared which contained amounts of fission product elements approximately equivalent to those expected to remain in equilibrium in the EBR-II process cycle. A total of 16 samples were employed, four samples of each of four types as shown in Table XXI. It should be noted that molybdenum constitutes one-half the total impurity content in each of the two fissium alloys employed.

All specimens were originally cast about 30% oversize. Specimens designated "as cast" were centerless ground to size. Specimens designated "wrought and gamma treated" were swaged to size at 800C and cooled in air. Because of the high temperature necessary for swaging, these samples are considered heat treated. The final diameter of all specimens was approximately 0.144 in., the reference diameter of EBR-II fuel pins. Specimen length was approximately 2 in.

²⁴Reactor Engineering Division Quarterly Reports: ANL-5511 (January, 1956), p. 141; and ANL-5561 (Section I) (April, 1956), p. 141.

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Table XXI

COMPOSITION AND FABRICATION OF FISSIUM
ALLOY TEST SPECIMENS

<u>No.</u>	<u>Composition</u>	<u>Fabrication</u>
C1	3% Fissium*	As Cast
C2	5% Fissium**	As Cast
S1	3% Fissium	Wrought and gamma treated
S2	5% Fissium	Wrought and gamma treated

*3% Fissium contains (in %): 96.83 U, 1.58 Mo; 1.18 Ru; 0.2 Rh; 0.1 Zr; 0.1 Pd; 0.01 Nb.

**5% Fissium contains (in %): 95.00 U; 2.50 Mo; 1.50 Ru; 0.3 Rh; 0.2 Zr; 0.5 Pd.

All of the specimens were thermal cycled as follows: All specimens were stood loosely on end (approximately vertical) within a wire basket which was caused to move alternately from hot to cold NaK (at the temperatures shown in Table XXII) within the vertical thermal cycler. The immersion time at each temperature was 3 min. The transition time was approximately 10 sec. These conditions were chosen to be identical with those previously employed with zirconium-uranium and molybdenum-uranium alloys, so that direct comparison between the various alloys could be made.

Table XXII

THERMAL CYCLING CONDITIONS

<u>Group* No.</u>	<u>Temperature Cycled, F</u>	<u>Cycles</u>	<u>Canning</u>
1	150-1150	200	Bare
2	200-1320	200	Bare
3	150-1150	200	Canned
4	200-1320	200	Canned

*Each group contained one specimen each of the C1, C2, S1, and S2 types.

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The canned specimens were sealed in a Type 304 stainless steel tube of 0.008-in. wall thickness, with a 0.006-in. annulus of sodium surrounding the sample. Initially, the specimens were unrestrained both axially and normal to the axis. The axial growth gap was sufficiently large that the samples never were restrained longitudinally, even during cycling. Groups 1 and 3 were thermal cycled together, as were Groups 2 and 4; thus, identical cycling conditions were obtained in all samples in each group.

Examination of the pictured specimens and included data (Figs. 60 and 61) yield the following observations:

Samples cycled between 150F and 1150F (Groups 1 and 3)

- (1) Weight changes for all specimens were negligible, indicating good "corrosion" resistance of these alloys in either Na or NaK.
- (2) All specimens appeared unaffected regarding warpage or surface roughening.
- (3) There was no elongation in any of the canned specimens, and negligible length changes for the swaged specimens cycled bare.
- (4) Although small volume increases (as indicated by density decreases) occurred in some of the bare specimens, it is noteworthy that all of the canned specimens appeared to decrease in volume.

Samples cycled between 200F and 1320F (Groups 2 and 4)

- (1) Weight changes were again negligible for all specimens.
- (2) The 5% fissium samples showed superior cycling stability. The 3% fissium showed a definite warpage and surface roughening when cycled bare. It is of particular interest that this alloy appeared unaffected when cycled in cans. The effect is more noticeable in the as-cast specimens (which were centerless ground), since the surfaces of all of the swaged specimens were quite rough prior to cycling.
- (3) Although all of the specimens showed some elongation, the length change was essentially negligible for all canned specimens and for the swaged 5% fissium specimens cycled bare.
- (4) Most of the specimens increased somewhat in volume, the increase being greatest for those specimens cycled bare.

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The following general conclusions may be drawn from these data:

(1) Of the two alloys studied, the 5% fissium alloy shows better promise for reactor operation above the uranium alpha-beta phase transition temperature without further alloying additions.

(2) Canning of these alloys, as prescribed for EBR-II fuel pins, improves their effective thermal stability. This effect is more obvious with the 3% fissium alloy than with the 5% fissium.

Figure 62 is included to illustrate the superior thermal stability of fissium alloys over 2 wt-% Zr alloys.

B. Heat Transfer Characteristics of Liquid Metals - T. R. Bump,
K. D. Kuczen

The following is a summary of the final results of data taken from the liquid metal heat transfer loop. A detailed description of the loop and experiment is given in ANL-5260.²⁵

In brief, local heat transfer coefficients were measured in a circular, copper tube having an inner diameter of 0.25 in., an outer diameter of 0.55 in., and a length of 36 in. Direct resistance heating of the tube wall from a direct current power source dictated the size and material of the test section. Since the electrical resistivity of copper varies appreciably with temperature, nonuniform heating was present in all of the runs. The test section was cooled from the inside with the eutectic alloy of sodium and potassium (22% Na, 78% K) flowing turbulently in a vertical direction.

The range of variables covered in the experiment was as follows:

Fluid temperature	85 to 1175F
Fluid velocity	4 to 60 fps
Reynolds Number	13,000 to 466,000
Peclet Number	268 to 3850
Nusselt Number	1.4 to 22.4
Average heat flux	28,600 to 3,200,000 Btu/(hr)(sq ft)
Maximum local heat flux	6,000,000 Btu/(hr)(sq ft)

1. Results

A plot of N_{Nu} versus N_{Pe} , is presented in Fig. 63. Also plotted, for comparison purposes, are: (1) the widely used Lyon²⁶ theoretical correlation, which was derived for heat transfer to liquid metals in a

²⁵Reactor Engineering Division Quarterly Report (April 15, 1954), p. 150.

²⁶R. N. Lyon, "Forced Convection Heat Transfer Theory and Experiments with Liquid Metals," ORNL-361 (April, 1949).

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FULL SIZE VIEWS TO
SHOW OVER-ALL
DISTORTION

CYCLED BARE IN NaK

CYCLED CANNED IN Na

MAGNIFIED VIEWS
SHOWING SURFACE
CONDITION (4X)

ALLOY COMPOSITION	3% FISSIUM	5% FISSIUM	3% FISSIUM	5% FISSIUM	3% FISSIUM	5% FISSIUM	3% FISSIUM	5% FISSIUM
FABRICATION	← CAST & GROUND →		← CAST & SWAGED →		← CAST & GROUND →		← CAST & SWAGED →	
HEAT TREATMENT	← NONE →		← AIR COOLED FROM 800 C →		← NONE →		← AIR COOLED FROM 800 C →	
LENGTH CHANGE, %	+1.0	+0.6	+0.1	-0.1	0	0	-0.3	-0.1
DENSITY CHANGE, %	-0.4	-0.7	-0.1	+0.7	+0.3	+0.2	+0.8	+1.5
WEIGHT CHANGE, %	+0.1	+0.15	0	+0.05	0	0	0	0

FIG. 60
FISSIUM ALLOYS CYCLED 200 TIMES FROM 150 F TO 1150 F
HOLDING TIME: 3 min. TRANSITION TIME: 3 sec.

FIG. 61
FISSIUM ALLOYS CYCLED 200 TIMES FROM 200 F TO 1320 F
HOLDING TIME: 3 min. TRANSITION TIME: 3 sec.

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<u>ALLOY</u>	<u>CYCLED BARE IN NaK</u>	<u>CHANGE IN LENGTH, %</u>	<u>CYCLED CANNED IN Na</u>	<u>CHANGE IN LENGTH, %</u>
2 wt-% Zr AS CAST		~ 5		+11.5
2 wt-% Zr WROUGHT & HEAT TREATED		~ 4		+5.0
3 wt-% FISSIUM AS CAST		~ 2		+0.3
5 wt-% FISSIUM AS CAST		~ 0.5		+0.3

FIG. 62
COMPARISON OF FISSIUM AND 2 wt-% ZIRCONIUM ALLOYS
CYCLED 200 TIMES FROM 100-715 C

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Fig. 63

RATE OF DISSOLUTION OF ZIRCALOY
AS MEASURED BY
HYDROGEN EVOLUTION^a

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round tube with turbulent flow, fully developed velocity and temperature profiles, and a uniform rate of heat input along the tube; (2) the Lubarsky-Kaufman²⁷ empirical correlation intended to meet the same conditions; and (3) the correlation presented by Mikheyev²⁸ of the U.S.S.R. The conditions of turbulent flow and fully developed velocity and temperature profile were met fairly closely by the plotted experimental data. However, the requirement of uniform heat input was violated rather severely in several instances, due to the effect of temperature upon the resistance of the test section wall.

In Fig. 64, N_{Nu} is plotted against N_{Pe} for the entrance region where the L/D ratio was about $(1.5 + 1.18)/0.25$ or 10.7. The Deissler²⁹ theoretical correlation for an x/D ratio of 10.7 is included for comparison. Deissler attempted to provide for uniform heat input and a fully developed velocity profile, which conditions were met quite well by the plotted experimental data. Also shown are the data of Johnson, et al, (as reported by Lubarsky and Kaufman) and the work of Mikheyev which appears to be an empirical relation.

2. Discussion of Results

The Nusselt Numbers for fully developed, turbulent flow found in this experiment agree with the empirical correlation offered by Lubarsky and Kaufman ($N_{Nu} = 0.625 N_{Pe}^{0.4}$) for $450 < N_{Pe} < 2000$. Above a Peclet Number of 2000 the data are higher, and below a Peclet Number of 450 the data are lower, than the above-mentioned correlation. The Nusselt Numbers for the entrance region are approximately 10% higher than those found by Johnson (discussed by Lubarsky and Kaufman) and approximately 40% higher than the values predicted by Deissler. In general, the experimental results of the investigation are in error by less than 20%.

The abnormally low Nusselt Numbers, near a N_{Pe} of 300, are believed to be in error because of the mass of experimental and theoretical evidence which refutes the existence of such low Nusselt Numbers.

One possible source of error was in the measurement of the fluid outlet temperatures. It was observed that at such comparatively low flows an extraordinarily long time was needed for the fluid outlet temperature to rise and reach steady-state conditions. It was thought that

²⁷S. J. Kaufman, B. Lubarsky, "Review of Experimental Investigations of Liquid-Metal Heat Transfer," NACA Tech. Note 3336 (March 1955).

²⁸M. A. Mikheyev, et al, "Heat Delivery of Molten Metals," Geneva Conference Paper, A/Conf. 8/P/639, U.S.S.R., (June 30, 1955).

²⁹R. G. Deissler, "Analysis of Turbulent Heat Transfer and Flow in the Entrance Regions of Smooth Passages," NACA Technical Note 3016, (October, 1953).

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Fig. 64

FUSED FLUORIDE CORROSION TEST APPARATUS.

Details of Two
Sparge Assemblies.

adequate time was allowed during these runs for steady state to be obtained, but there is a possibility that such was not the case. Therefore, the low Nusselt Numbers could have resulted from the measurement of low, non-steady state fluid outlet temperatures. The foregoing reasoning is supported by the fact that the measured flow during these three runs was noticeably less than the calculated flow based on the heat input and fluid temperature rise.

It is also possible that the experimental data in this region were analyzed incorrectly. The most probable location of such error is in the calculation of heat losses between the test section outlet and the mixer where the fluid outlet temperature was measured. Since only limited instrumentation was used during these runs, the losses along the electrical terminals to the test section could only be estimated.

Unfortunately the low NN_u runs could not be repeated because the test section had been deliberately burned out in an effort to attain extremely high heat flux.³⁰

3. Recommendations

The method for heating the test section was chosen for attaining high heat flux, the thought being that greater accuracy could be achieved since the film temperature drop would be large. However, the inaccuracies of flow and surface temperature measurements, because of voltage pickup, introduced additional sources of error. Therefore, with this method of heating, surface thermocouples should be electrically insulated; fluid flows should be measured with a primary element not affected by a voltage gradient. In addition, the electrical terminal junction regions should be provided with enough thermocouples to establish isothermal lines and facilitate accurate determination of losses.

As reported in ANL-5461, one phase of this experiment involved an attempt to measure the effect of gas entrainment on heat transfer. The results of this attempt indicate that the use of an electromagnetic pump for such an investigation is questionable, since the pump tube may become gas bound and burn out. If such a pump must be used, the injection of gas should take place at the discharge side of the pump. Also, a means for gas separation should be included in the system before the fluid reaches the suction side of the pump.

³⁰Reactor Engineering Division Quarterly Report, ANL-5461 (April 15, 1955), p. 100.

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C. Sodium Pump Test (3800 gpm, 125-ft Head) - J. J. Dickson,
D. V. Hammond, O. S. Seim

1. History

A canned, rotor-type centrifugal pump was designed and constructed as a part of the Submarine Intermediate Reactor Development Program in 1952. The design rating of the pump was 3800 gpm at 125 ft head of sodium at about 250F. The pump was not used for the intended application and was made available to the Laboratory in 1955. The pump was tested in conjunction with the Laboratory's sodium pump development program.

A preliminary test, with water as the circulating fluid, was made by the manufacturer on completion of construction. The pump operated satisfactorily, with a measured over-all test efficiency (line to fluid) of 49%.

2. Sodium Test Loop Arrangement

A closed circuit sodium test loop was designed and constructed (Fig. 65). The principal loop piping was 12-inch, schedule 10, Type 304 stainless steel pipe.

FIG. 65
TEST LOOP ARRANGEMENT OF 3800-gpm,
125-ft HEAD SODIUM PUMP

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Pump inlet and discharge pressures were measured by diaphragm-actuated, pneumatic pressure transmitters with indicating gages on the instrument panel. Flow was measured by an orifice welded into the lower loop pipe, with similar pressure devices connected to a manometer. Temperatures were measured by thermocouples built into the motor stator and also distributed over the piping.

3. Description of Pump

The pump was of the hermetically sealed or completely canned type, with the 60-cycle, 3-phase, 150-volt, 1690-rpm driving motor running submerged in the liquid metal. The stator and rotor components were completely sealed from the pumped fluid. The required motor output at rated pump capacity was 189 brake horsepower.

All bearings employed the pumped fluid (sodium) as the lubricant and coolant supplied by an internal auxiliary pump impeller. An integral cooler was installed in the top portion of the pump assembly. Figure 66 shows schematically the location of the seven bearing faces, their measured clearances, and the cooling and lubricating circuit. The bearings were the "fluid type" in which the fluid forms a running cushion between the corrugated bearing main surface and cylindrical stainless steel shaft sections. The lower main bearing was located on the motor end of the main pump impeller. Figure 67 shows the main pump impeller in the position that it occupied in the pump. The inner and outer lower main bearing surfaces can be seen at the top. The lower impeller guide bearing, which had only an external bearing surface, is at the bottom. The upper main bearing was located at the top end of the motor shaft and was of similar construction as the lower main bearing. To provide the required much lower bearing loading per unit area than standard journal bearings, bearing areas were formed on the inside and outside surfaces of the cylindrical rotating shaft portions. The external lower main bearing, shown on Fig. 68, presents a typical bearing surface. Close tolerances at the Stellite raised lips at each edge, and the center of the bearings, provide starting alignment and sodium lubricant control. Each bearing pocket (corrugated areas) forms a hydraulic piston individually fed with liquid metal through orifices in the bearing wall.

The thrust bearing flat surface was Stellite faced on the lower, more heavily loaded face. The upper and lower thrust bearings were built with eight pie-shaped pockets surrounded by Stellite raised lips. In operations, the liquid metal filled the open pockets to form a loading surface.

Permanent magnets were enclosed in a 0.015-in. stationary stainless steel shell located in the lower cap of the pump motor. Magnetic attraction on soft iron embedded in the bearing impeller, which was also sealed by a thin stainless steel membrane, produced a lifting force to relieve the dead weight rotor load.

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FIG. 66
SCHEMATIC OF SODIUM PUMP SHOWING LUBRICATION
CIRCUIT AND OPERATING CLEARANCES

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FIG. 67
MAIN PUMP IMPELLER

FIG. 68
EXTERNAL LOWER MAIN BEARING

4. Pump Operating Test

The pump was inspected and assembled by the pump manufacturer's representatives. Prior to installation of the pump in the test loop, torque and end play measurements at the impeller nut indicated that the pump was correctly assembled.

The test loop, including the pump assembly, was heated electrically by means of the external resistance heating elements visible in Fig. 65. These covered the pump and all sodium piping, and were used to raise the temperature to 250F.

The system was purged several times with helium (evacuated and filled) and vacuum-filled with liquid sodium. Approximately 29 in. of vacuum was employed. The manufacturer agreed to this procedure, providing that a vacuum was also drawn on the stator can, which was done. The sodium was dumped several times from the test loop into the storage tanks to check out the emergency dump valve and to remove any oxidation products out of the test loop into the storage tank, which was kept at a lower temperature than the loop.

Pressure gauges were checked by pressurizing the system with the cover gas. A maximum pressure of 30 psig was imposed on the system. The system pressure was adjusted to 10 psig, and the pump was judged mechanically ready to run.

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Power at 480 volts was supplied through a 3-phase combination magnetic starter, to three 75-kva, 480/120-volt, single-phase, 60-cycle, dry-type transformers directly connected to the pump motor terminals.

For the first test, the transformers were connected in delta-delta arrangement. The primary voltage measured 470 volts, and the secondary measured 130 volts. The rated operating voltage of the pump was 150 volts, and 130 volts should have been sufficient to run the pump. The motor failed to start in about ten attempts. The motor terminal leads were switched between attempts to start the pump. (The direction of rotation was to be ascertained by observing the sodium instrumentation.)

On the second test, the transformers were connected in delta-wye arrangement. With 470 volts on the primary, this produced 225 volts on the secondary to the motor. The pump started on about the third try. Pressure gauges rose and then began to subside, whereupon the stop button was manually actuated. While the pump ran, and when it was stopping, a scraping noise was heard. The pump running time was estimated at approximately five seconds. The pump failed to start again in about seven more attempts.

On a third test attempt, the transformers were connected in delta-delta arrangement, so that with 470 volt input, the voltage to the pump was 260. The motor failed to start in about five attempts. The starting torque of the motor was about three times the normal starting torque of the motor based on a 150-volt rating.

It was apparent that the pump rotor had seized and operating tests were terminated.

5. Inspection on Disassembly

The pump was removed from the test loop, and disassembly was started from the lower end. The inner and outer lower radial main bearings, the impeller guide bearing, and the impeller wearing ring assembly were in good condition. A small amount of scoring took place on the outer edge of the lower thrust bearing Stellited surface. The upper thrust face was relatively unchanged.

Difficulty was experienced in pulling the auxiliary bearing pump impeller. The thin sealing diaphragms on the permanent magnet lifting assemblies were fused together, but parted with the result shown on Fig. 69. The scraping noise which was heard when the pump ran was probably the rubbing of these diaphragms, which sustained some deformation during the vacuum evacuation.

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FIG. 69
APPEARANCE OF AUXILIARY BEARING SUPPLY PUMP
IMPELLER (a) AND MAGNETIC LIFT ASSEMBLY (b)

It was impossible to pull the rotor from the upper radial main bearings. Electrical heating at about 200F, with a constant axial load, did not free the bearings. The stator assembly was then removed, leaving the rotor standing in the upper bearing housing, and the bolts were removed from the external bearing. With additional axial force the rotor was pulled out, with the bearings adhering to the bearing surface, and the inaccessible inner bearing bolts parted in tension. (There was no way to remove these bolts with the rotor in place.)

The upper main bearing internal and external bearing shells remained frozen in position such that mechanical force could not separate the bearing shells from the bearing surface. The bearing area was cut from the rotor, and the external bearing was split at one of the lubricant drain slots. Figure 70 shows the condition of the external bearing surface after the bearing shell was removed. The bearing surface was then cut and removed from the internal bearing shell by spreading. The Stellite facing on both the internal and external bearing surfaces galled so as to weld to the rotating bearing surfaces at the positions which had been determined as having the minimum bearing clearance. These were the minimum pump external bearing clearance of 0.007 in. on the top lip of the nine-inch diameter upper main external bearing, and the minimum pump internal bearing clearance of 0.0062 in. at the center lip of the eight-inch diameter upper main internal bearing.

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FIG 70
FUSION OF EXTERNAL UPPER MAIN BEARING
LIP METAL (a) TO BEARING SURFACE (b)

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A minimum speed of about 700 rpm is required with this type of bearing arrangement in order to center the rotor and eliminate sliding on the stationary bearing shell projecting lips. In this test seizure occurred in advance of this speed.

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