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REACTORS—POWER

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ARGONNE NATIONAL LABORATORY
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REACTOR ENGINEERING DIVISION QUARTERLY REPORT

SECTION I

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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)

The gastight steel building (400,000 cu ft) in which all radioactive components are to be housed has been completed by the Graver Tank Company. This structure was tested for strength at 18.75 psig (20% above design pressure) and then tested for leaks. No leaks were found in soap bubble testing of all welded seams. Continuous measurements of temperature and pressure over a ten-day period showed the leakage, if any, to be less than the 500 cu ft/day at 15 psig specified. The gastight cylinder was, therefore, accepted. General construction work by the Sumner Sollitt Company on the remainder of the plant has begun.

The turbine, generator, exciter, condenser, feed-water pumps, circulating pumps, and transformers have been delivered by the Allis-Chalmers Manufacturing Company and stored at the site. The only major items yet to be supplied by Allis-Chalmers are the pressure-regulating by-pass valve system, the fluid-leakage recovery system and two motor control centers.

Completion of the reactor pressure vessel by the Babcock and Wilcox Company is expected in March or April.

Specifications are being prepared for equipment to be installed for operation of EBWR with forced circulation cooling. Experiments with forced circulation are expected to begin after only a few months of operation with natural circulation.

The earlier concept of a "spiked" reactor core has been abandoned in favor of slightly enriched fuel. The original decision to use natural uranium and fully enriched uranium-zirconium alloy fuel assemblies in the core was made in order to utilize existing chemical processing facilities. Recent price quotations from industry for fabricating fully enriched plates were so high that it has now been decided to make all fuel plates metallurgically identical. This makes it possible to use the process developed by the Argonne Metallurgy Division for fabricating the entire fuel charge.

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Since no critical experiments are planned for EBWR, flexibility in the core loading is necessary to cover uncertainties in the absolute magnitude of the reactivity and in the variation of reactivity with temperature and steam content. This is being achieved by varying the proportions of natural and slightly enriched (1.44% U^{235}) uranium assemblies, each with two different plate thicknesses. The required ratio of thick (0.271 in.) to thin (0.208 in.) plates to give the desired steam void coefficient of reactivity will be determined in the initial loading of EBWR.

The variety of fuel elements being provided for EBWR allows changes to be made in the core which will help determine the inherent limits to the specific power of boiling reactors. For example, by concentrating thick assemblies near the center of the core the effect of the increased sensitivity of the reactor to steam content on stability can be observed. The effect of power density itself can be studied over a considerable range.

From a physics standpoint, the radical change from a "spiked" core to a slightly enriched core requires extensive recalculations. An extremely large number of combinations of the four varieties of fuel are possible; however, only the most likely combinations will be calculated.

Tests under closely simulated conditions of anticipated temperature and pressure continue to give encouraging results for the mechanical control rod drive and the cross-type control rod. The number of cycles and scrams completed are equivalent to at least 12 years of normal operation.

Sample fuel plates and complete assemblies have been made to EBWR specifications and tested for corrosion. Preparations are nearing completion for initiation of routine production of fuel assemblies by the Argonne Metallurgy Division. Rolling of Zircaloy 2 sheet and coating of uranium-zirconium-niobium billets is under way. Delivery of the 1.44% enriched uranium is expected in January.

Specimens of leftover material from the EBWR pressure vessel are being prepared for irradiation tests in the MTR. Periodic examination of these specimens will afford knowledge of the effect of irradiation on the EBWR pressure vessel in advance.

Development work on boron-stainless steel alloys continued, with current emphasis on determining the best heat treatment and welding procedures.

New data from boiling heat transfer tests have been used to re-estimate the performance of the EBWR core with natural circulation. The new estimate of 12% average steam voids in coolant channels is considerably lower than previous values.

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General

Fabrication experiments with uranium fuel plates containing up to 10 wt-% zirconium have shown much more difficulty in rolling to dimensional tolerances and in achieving bonding with the cladding than has been the case with the uranium-zirconium-niobium alloy. Niobium has also been shown to increase the corrosion resistance substantially, even though the heat treatment is optimized for dimensional stability rather than corrosion resistance.

Aluminum-nickel alloys continue to show encouraging, although not entirely understandable, corrosion resistance. Dissolved hydrogen decreases the corrosion rate markedly. Irradiation tests in an MTR loop showed greatly reduced corrosion rates for samples exposed to the reactor flux as compared with samples in the same loop but outside the reactor core.

Addition of a small amount of iron to aluminum-nickel alloys apparently does not change the corrosion rate significantly. Aluminum-copper alloys corrode generally at about the same rate as aluminum-nickel alloys, but objectionable localized corrosion (pitting) has been observed.

Cladding rupture occurred on a sample of thorium-uranium irradiated to about 0.10 at-% burnup in the CP-5 reactor. Post-irradiation examination showed evidence of intrusion of the lead bonding material between one pair of adjacent oxide pellets but not between others.

Thermal shock tests on thorium-uranium pellets clad in aluminum-nickel and bonded with lead showed considerable resistance to cracking up to 750F. At much higher temperatures thermal stress cracking was the rule rather than the exception.

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I. EXPERIMENTAL BOILING WATER REACTOR (EBWR)

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

1. Core

- a. Nuclear Properties - R. C. Howard, H. P. Iskenderian, O. A. Schulze, D. V. Steward, J. A. Thie.

A decision has been made to use several types of fuel elements in order to achieve flexibility in the core loading and thus to provide for uncertainties in the absolute magnitude of the reactivity and in the variation of reactivity with temperature and steam content. The decision did not reflect any changes in the number of fuel plates (6) in the sub-assembly (4 x 4 in.) nor in the active fuel length (4 ft); however, it did initiate extensive physics calculations to determine the combinations of fuel plate thicknesses and compositions most likely to achieve the desired operating characteristics of the reactor. The combinations selected are listed below.

<u>Total Plate Thickness,</u> <u>in.</u>	<u>Composition</u> <u>% U²³⁵ in Uranium</u>
0.205	0.72
.274	.72
.205	1.44
0.274	1.44

With four types of fuel elements the reactor can be loaded in a variety of ways; however, owing to the inherent limitations of such calculations, the optimum loading cannot be determined without recourse to experiment. Calculated reactor characteristics reported here are on the basis of a uniform mixture of 18 thin-plate natural elements, 18 thick-plate natural elements, 38 thin-plate enriched elements, and 38 thick-plate enriched elements. Table I gives the structure of these elements.

Two-group diffusion theory is considered to be adequate in the calculation of this reactor, provided a judicious choice of the group constants is made. For multiregion problems the UNIVAC is often used.

In the calculation of the characteristics of the thermal neutron group, the average thermal flux in every material of every fuel element was calculated by the P₃ spherical harmonic method on the AVIDAC, the source being proportional to the amount of water. These disadvantage factor calculations logically fall into two classes: (1) intra-element, i.e., plate and water channel fluxes; and (2) inter-element, i.e., relative flux levels in the four kinds of elements.

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Table I

AREAS OCCUPIED BY MATERIALS AT ROOM TEMPERATURE IN A
12.75 IN. BY 12.75 IN. CELL CONSISTING OF NINE IDENTICAL
SUBASSEMBLIES

<u>Material</u>	<u>Area, sq in.</u>	
	<u>Thin Elements</u>	<u>Thick Elements</u>
U	25.06	35.54
Nb	0.88	1.24
Zr meat	3.86	5.48
Zr clad	10.32	11.38
Zr sides	2.93	2.70
Zr guides	3.33	3.33
Zr spacers	1.25	1.25
Zr follower	2.50	2.50
H ₂ O between plates	104.12	90.83
H ₂ O in control channels	8.31	8.31
	162.56	162.56

Phenomenological methods based on experiments were used wherever possible for epithermal characteristics. Experiments in light water-uranium rod lattices¹ provided values for the fast effect and age from which phenomenological cross sections could be inferred for use in a plate lattice. On the other hand resonance escape was calculated by the standard procedure for heterogeneous lattices.²

Using methods outlined above the results obtained are listed in Tables II, III and IV. This section, as well as the ensuing sections, should be viewed as applicable to an EBWR-type reactor rather than to the "as built" EBWR. The difference between the latter and the various designs used for calculations is probably small. An approximately critical (at poisoned operating conditions) loading would be a uniform mixture of 18 thin natural, 18 thick natural, 38 thin enriched, and 38 thick enriched uranium plates.

¹H. Kouts, et al, "Exponential Experiments with Slightly Enriched Uranium Rods in Ordinary Water." Paper 600, International Conference on the Peaceful Uses of Atomic Energy, Geneva, Switzerland (August, 1955).

²Samuel Glasstone and M. C. Edlund, "The Elements of Nuclear Reactor Theory," (New York: D. Van Nostrand Co., Inc., 1952), pp. 258-275.

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Table II

LATTICE CONSTANTS OF THE REFERENCE EBWR

(with Xe + Sm Poisoning)

	<u>Cold</u>	<u>Hot</u>	<u>Operating</u>
Moderator Temperature, F	68	488	488
Control Channel H ₂ O Density	1	0.8	0.8
Fuel Element H ₂ O Density	1	0.8	0.68
ϵ	1.0322	1.0365	1.0398
ρ	0.8701	0.8422	0.8206
ηf	1.2437	1.2683	1.2908
k_{∞}	1.1169	1.1071	1.1014
L^2	3.04	6.62	7.59
τ	35.80	48.35	58.40
Reflector Savings	7.52	9.99	11.23
B^2	0.001643	0.001533	0.001483
Reactivity	0.0461	0.0180	0
Conversion Ratio	-	-	0.734

Table III

REACTIVITIES OF REFERENCE EBWR

Operating voids	0.0183
Temperature and density effects from room temperature to 488F and no bubbles.*	0.0281
Xe	0.0216
Sm	0.0074
Total:	0.0754

*The results of equivalent calculations on BORAX were somewhat greater than determined by experiment.

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Table IV

NEUTRON ECONOMY OF THE REFERENCE EBWR AT OPERATING
CONDITIONS

Neutrons from fission			
thermal		1.2908	
fast		<u>0.1035</u>	
			1.3943
Thermal captures			
U ²³⁸	0.2261		
U ²³⁵	0.6206		
Nb	0.0036		
Zr	0.0145		
H ₂ O	0.1062		
Xe	0.0216		
Sm	<u>0.0074</u>		
		1.0000	
Resonance captures			
U ²³⁸	0.2188		
Nb	<u>0.0035</u>		
		0.2223	
Fast captures by U ²³⁸		0.0521	
Leakage			
Thermal	0.0101		
Fast	<u>0.1098</u>		
		0.1199	
		<u>1.3943</u>	<u>1.3943</u>

Tables II and III show that the "operating reactor," which is arbitrarily defined as one having 15% voids in the fuel channel water everywhere, has 1.8% reactivity associated with these voids. The operating void coefficient, 0.12, is the quotient of these numbers. At room temperature (with all control rods out) the void coefficient is essentially zero. The uranium-to-water ratio achieved by equal amounts of thick and thin elements was chosen in order to attain these void coefficients. Departure from a uniform mixture of thick and thin elements enables one to vary the void coefficient; for example: a loading with thick elements in the center and thin elements on the outside should have void coefficients somewhat more negative than the above values.

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In addition to void coefficient flexibility it is readily seen that the reactor also has reactivity flexibility; for example: by loading entirely with enriched elements, it is possible to achieve about 7% reactivity above that needed for poisoned operating conditions.

Figures 1 and 2 show the two-group fluxes. The effect of voids is seen to reduce the flux in the region where the voids are greatest.

Fig. 1
Radial Neutron Flux and Void Distribution

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Fig. 2
Axial Neutron Flux and Void Distribution

Predictions as to the reactor's characteristics after appreciable amounts of plutonium and fission products are present are difficult to make due to uncertainties in cross sections as well as to lack of any knowledge (at present) on loading and operating policies. However, certain statements can be made: The conversion ratio is probably large enough to cause an initial increase in reactivity, and a need for more reactivity may not come until ϕt is of the order of magnitude of $5 \times 10^{20} \text{ cm}^{-2}$. As plutonium builds up, its fission resonance should make the temperature coefficient of the reactor less negative.

The worth of the control elements in the EBWR core has been calculated by three independent methods; the results have been consistent. Table III shows that at least 7.5% reactivity must be controlled.

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The first detailed method was used on the actual reflected reactor in which the control elements were replaced by an equivalent uniform poison giving the same integral flux as obtained from a two-dimensional calculation for a cross rod in a square cell. Figure 3 shows calculated flux contours.

CELL: 12.75 x 12.75 in.
CROSS ROD: 10 inch SPAN; BLADE THICKNESS: 0.125 in.

Fig. 3
Thermal Flux Near Cross-Type Rod

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In the second detailed, but less numerically involved, method, the absorption area technique was applied to an equivalent bare reactor with corrections made for grayness of the elements, self-shielding of surfaces and corners, end effects of tips, and changed leakage out of the reactor.

The third method approximated one-half the control elements by three planes of zero flux in an infinite slab bare reactor. One purpose of this method was to demonstrate that one-half the control elements are more than sufficient to overcome the expected 7% excess reactivity of the EBWR.

A simplified calculation was made in which an equivalent uniform poison was obtained by elementary diffusion theory with intuitive estimates made of self-shielding and leakage changes. The results were also greater than the minimum required worth.

The results obtained for the H₂O EBWR core by the three methods are summarized below. The worth is expressed as the ratio of k_{eff} with control elements withdrawn, to k_{eff} with control elements inserted.

<u>Method</u>	<u>Worth of all the elements</u>	<u>Worth of all the elements</u>
Cell method	1.165	
Absorption area	1.14	
Slab	1.16*	1.11

b. Thermal Analysis of EBWR with Natural Circulation -
R. J. Weatherhead

Using two-phase friction factors developed from Armadilla data,³ an analytical prediction of the natural circulation performance of EBWR at 600 psig has been made. At the design core power density of 26 kw/liter, the calculated velocity and average void fraction are 2.65 fps and 0.12, respectively.

*Assuming that the total control element worth is only 1.5 times that of one-half the elements due to self-shielding.

³Reactor Engineering Division Quarterly Report, ANL-5511, (January, 1956), p. 74

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Schematic flow and pressure drop diagrams are shown in Figs. 4 and 5. The following assumptions were made for this analysis:

- (1) Uniform radial heat flux.
- (2) Uniform heat generation along the 48-in. active length of fuel plates.
- (3) No cross-flow in the core - all flow vertically upward through the subassembly channels.
- (4) Slip ratio = 1.5.
- (5) Riser voids $\alpha_r = \frac{1}{\frac{A_r/A_{ex}}{\alpha_{ex}} - \frac{(A_r - A_{ex})}{A_{ex}}}$

where

A_r = Riser flow area/subassembly

A_{ex} = Subassembly flow area

α_{ex} = Subassembly exit void fraction

- (6) Effective riser length = 33-1/4 in.
- (7) Uniform liquid density and depth in the upper plenum above the riser.
- (8) Make-up water temperature = 110F.

Fig. 4
Schematic Flow Diagram of EBWR Core, Riser and Pressure Vessel.

$$\begin{aligned} \Sigma \Delta P = & \Delta P_{\text{cont}_1} + \Delta P_{f_{1-2}} + \Delta P_{\text{exp}_{2-3}} + \Delta P_{\text{cont}_3} + \Delta P_{f_{3-4}} + P_{f_{4-5}} \\ & + \Delta P_{\text{head}_{1-5}} + \Delta P_{f_{5-6}} + \Delta P_{f_{6-7}} + \Delta P_{\text{accel}} + \Delta P_{\text{head}_{5-7}} + \Delta P_{\text{exp}_7} \end{aligned}$$

FIG. 5
SCHEMATIC PRESSURE DROP DIAGRAM
OF EBWR SUBASSEMBLY

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To allow for surface deposition, the non-boiling friction factor for smooth surfaces was taken as 10% greater than the value for drawn tubing. For the subassembly channels, where perforated side plates form both edges of the subassembly channels, a non-boiling friction factor 30% greater than the drawn tubing value was used. This factor of 1.3 was selected on the basis of the results of informal tests recently conducted on a rectangular channel with perforated edges roughly approximating an EBWR channel. All expansion and contraction losses were evaluated on the basis of the local liquid velocity.

To furnish a basis for comparison, a similar calculation was made using a uniform non-boiling friction factor 10% greater than the drawn tubing value. Comparison of the results (Fig. 6) of the two calculations shows that the use of the lower friction factor gives a 5% increase in the inlet velocity and a 2-1/2% decrease in the average channel void fraction at the 26 kw/liter power density. A breakdown of the calculated subassembly pressure drop for the two cases mentioned is tabulated below.

	1.3 f (drawn tubing)	1.1 f (drawn tubing)
Perforated surface friction factor:		
Expansion and contraction losses	15.1%	16.9%
Non-boiling friction-smooth surfaces	2.4%	7.6%
Non-boiling friction-perforated surfaces	5.5%	-
Boiling friction-smooth surfaces	4.8%	54.2%
Boiling friction-perforated surfaces	53.9%	-
Acceleration loss	12.4%	14.0%
Per cent of total drop in subassembly	94.1%	92.7%

The results of these calculations indicate that, if perforated side plates must be used in the subassembly, the subassemblies should be so oriented in the core that the side plate of one subassembly does not form the side for an adjacent subassembly end channel. Disregarding subassembly clearances, this normal orientation (as contrasted to parallel orientation) would result in a channel with 45% of the flow area of an internal channel and with 45% of the channel wall perforated instead of 9% for an internal channel.

Fig. 6
Calculated Performance of EBWR with Smooth Side Plates
and with Perforated Side Plates

c. Effect of Riser Height and Equivalent Diameter on
EBWR Performance - R. J. Weatherhead

Calculations, similar to those discussed in the preceding section and based on the same assumptions, were made to determine whether simple changes in the riser length and/or geometry would favorably affect the performance of the EBWR. These included:

- (1) The effect of riser length, using an equivalent diameter of 11.4 in. (see Figs. 7 and 8).
- (2) The effect of riser equivalent diameter, using a riser length of 33-1/4 in. (see Figs. 9 and 10).
- (3) The "optimum" riser, using values from calculations (1) and (2). (Fig. 11.)

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FIG. 7
EFFECT OF RISER LENGTH ON CALCULATED
PERFORMANCE OF EBWR AT 600 psig

FIG. 8
CALCULATED PERFORMANCE OF EBWR AS A FUNCTION OF
RISER LENGTH AND CORE POWER DENSITY AT 600 psig

FIG. 9
INFLUENCE OF RISER EQUIVALENT DIAMETER ON
CALCULATED PERFORMANCE OF EBWR AT 600 psig

FIG. 10
CALCULATED PERFORMANCE OF EBWR AS A
FUNCTION OF RISER EQUIVALENT DIAMETER
AND CORE POWER DENSITY AT 600 psig

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Fig. 11
Influence of Riser Length on Calculated Performance of
EBWR with Optimum Riser $D_e = 3.75$ in.

These calculations indicated that the most advantageous riser geometry would be a box-like structure approximately equal to the subassembly perimeter. The length of riser should be the maximum practical length that can be fitted into the existing design. As an illustration, the use of a 15-ft riser with the optimum D_e will approximately double the power output of the existing EBWR design at the same average channel void fraction.

Recent measurements of riser void fraction made in the 600-psi Boiling Density Loop indicated somewhat higher values than those calculated in the EBWR performance studies. These measurements are still in the preliminary stage. Should they be validated, the calculations reported both here and in the EBWR performance studies will prove to be more conservative than at first was believed.

The riser void fraction equation is derived as follows:

The 250-psi boiler data shows the slip ratio to be a function of inlet velocity. Using an equivalent inlet velocity reference as the criterion, and knowing that

$$V_{\text{riser}} = V_{\text{exit}} \left(A_{\text{exit}} / A_{\text{riser}} \right),$$

the slip ratio can be based on V_{riser} . For the inlet velocity range 1-3 fps this function can be approximated by

$$c_s/c_l \sim 1/V,$$

where

$$c_s/c_l = \text{slip ratio}$$

$$V = \text{inlet velocity;}$$

also

$$c_s/c_l \sim \frac{1-\alpha}{\alpha}$$

for constant pressure and constant quality;

therefore,

$$\frac{\left(\frac{1-\alpha}{\alpha}\right)_{\text{exit}}}{\left(\frac{1-\alpha}{\alpha}\right)_{\text{riser}}} = \frac{V_{\text{riser}}}{V_{\text{exit}}} = \frac{A_{\text{exit}}}{A_{\text{riser}}}$$

$$\frac{1-\alpha_r}{\alpha_r} = \frac{1-\alpha_{\text{ex}}}{\alpha_{\text{ex}}} \frac{A_r}{A_{\text{ex}}}$$

$$\frac{1}{\alpha_r} = 1 + \frac{A_r}{A_{\text{ex}}} \frac{(1-\alpha_{\text{ex}})}{\alpha_{\text{ex}}}$$

$$\alpha_r = \frac{1}{1 + \frac{A_r}{A_{\text{ex}}} \left(\frac{1}{\alpha_{\text{ex}}} - 1\right)} = \frac{1}{\frac{A_r/A_{\text{ex}}}{\alpha_{\text{ex}}} - \frac{A_r - A_{\text{ex}}}{A_{\text{ex}}}}.$$

It should be noted that extension of the inverse linearity approximation beyond the 1 - 3 fps inlet velocity range will give riser void fraction values lower than those derived by a more precise calculation. This will, in turn, give lower values of riser driving head which, in the case of EBWR, tend to make the performance predictions more conservative, because any increase in the riser driving head greatly overbalances the resulting increase in two-phase friction loss. This results from the low (5 - 10%) percentage of the total losses which occur in the riser.

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d. Design-Economics Evaluation of EBWR Fueling -
C. K. Soppet

The original proposal of a spiked, natural uranium, EBWR core was based on industry-Laboratory cooperation with regard to fuel plate fabrication. It was intended that the natural uranium alloy plates would be produced by the Argonne Metallurgy Division, while the fully enriched Zircaloy 2 plates, or spikes, would be fabricated by a private industrial source selected on a low-bid basis. The high prices quoted by industry for the spike plate fabrication forced the abandonment of this method of procurement. The possibility that the spike plates could not be fabricated by the Argonne-developed method for rolling natural uranium alloy plates necessitated a re-evaluation of the spiked core concept. The object of this study was the determination of an EBWR core having nuclear characteristics similar to the spiked type, which could be fabricated for the same investment.

The study was based upon Argonne's experience in producing pilot plates of the uranium-5 wt-% zirconium-1-1/2 wt-% niobium reference alloy, and a number of experimental, fully enriched Zircaloy 2 plates. The results are summarized in Table V.

An analysis of expected heat fluxes based upon an average reactor operating heat flux of 40,000 Btu/(hr)(sq ft) yielded the following results:

	Heat Flux, Btu/(hr)(sq ft)	
	<u>Average</u>	<u>Maximum</u>
<u>Case I</u>		
Reference	24,500	75,000
Enriched Reference Alloy Plates	41,650	127,450
Fully Enriched Spikes	49,000	150,000
<u>Case II</u>		
Reference Alloy Plate	36,800	112,600
Fully Enriched Spikes	46,000	140,800
<u>Case III</u>		
Reference Alloy Plates	23,500	71,900
Enriched Reference Alloy Plates	47,000	143,850

Table V

SUMMARY OF DESIGN -ECONOMICS ANALYSES OF EBWR FUELING

Case		Core Arrangement and Composition	Plate Dimensions, in.						
			Over-all	Fuel Alloy					
I	(1)	96 ref. alloy plates; 16 assemblies; natural uranium.							
	(2)	672 ref. alloy plates; 112 assemblies; total U ²³⁵ enrichment: 1.16%		(all plates)					
	(3)	114 spiked plates; 19 assemblies; 107.3 gm U ²³⁵ per plate.	0.223 x 3.75 x 51	0.183 x 3.43 x 48					
II (Former Reference Core)	(1)	576 ref. alloy plates; 96 assemblies; natural uranium	0.280 x 3.75 x 51	0.240 x 3.43 x 48					
	(2)	312 spiked plates; 52 assemblies; 87.3 gm U ²³⁵ per plate	0.280 x 3.75 x 51	0.260 x 3.43 x 48					
III	(1)	216 ref. alloy plates; 36 assemblies; natural uranium	0.223 x 3.75 x 51	0.183 x 3.43 x 48					
	(2)	672 ref. alloy plates; 112 assemblies; total U ²³⁵ enrichment: 1.417%.	0.223 x 3.75 x 51	0.203 x 3.43 x 48					
Materials Requirements and Costs									
Case	U ²³⁵ , kg	Uranium	Zirconium, Zircaloy 2, Niobium		Labor		Total Fuel Cost	Total* Direct Cost	
		Total U, lb	Cost	Total Weight, lb	Total Cost	Man hours			Cost @ \$6/hr
I	35.2	13,190	\$759,620	5,334.6	\$237,080	23,058	\$138,350	\$1,135,000	\$375,380
II	27.2	13,000	704,080	8,252.0	355,200	24,820	148,920	1,208,200	504,120**
III	34.3	15,520	777,200	4,826.3	219,700	22,042	132,250	1,129,200	352,000

*Total cost minus Uranium Costs.

**If the spike plates had been purchased from industry, as originally proposed, the cost of Case II would be increased by more than \$100,000. This figure is the difference between the expected cost of production at Argonne and the lowest bid which was submitted by industrial suppliers.

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The analysis indicated that the over-all advantage lay with Case III; therefore this loading was chosen to replace that originally proposed (Case II). It was further decided that an increase in void coefficient and power distribution flexibility could be obtained by using two thicknesses of plates instead of the single thickness used in the analysis. Simultaneously, to insure that adequate clearance would be maintained between adjacent fuel assemblies, the cross section of the assembly was changed from $3\text{-}7/8 \times 3\text{-}7/8$ in. to $3\text{-}3/4 \times 3\text{-}3/4$ in. The quantities and dimensions decided upon for the two types of plates for the new reference core (Case III) are as follows:

	No. Required	Plate Size, in.	
		Over-all	Fuel Alloy
Reference Alloy	108	0.208 x 3.625 x 51	0.168 x 3.345 x 48
Plates	108	0.271 x 3.625 x 51	0.231 x 3.345 x 48
Enriched Reference	336	0.208 x 3.625 x 51	0.168 x 3.345 x 48
Alloy Plates	336	0.271 x 3.625 x 51	0.231 x 3.345 x 48

The approximate projected cost of this loading, based upon the foregoing analysis, is as follows:

Uranium (35.7 kg U^{235} above natural); (16,150 lb)	\$ 808,000
Zirconium, Zircaloy 2 and Niobium (5,015 lb)	229,100
Labor (22,040 hr @ \$6/hr)	132,240
Total Cost of Fuel	\$1,169,340
Total Direct Cost (Total Cost less Uranium Cost)	\$ 361,340

The water-to-metal ratios for the reactor have been calculated for three possible loadings as follows:

	<u>H₂O: Total Metal</u>	<u>H₂O:U</u>
1. Light loading, using all 0.208-in. thick plates	2.24:1	4.49:1
2. Heavy loading using all 0.271-in. thick plates	1.56:1	2.79:1
3. Average expected loading of 0.208 and 0.271-in. thick plates	1.87:1	3.49:1

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The range of these values illustrates the latitude with which the reactor may be loaded to meet a given condition. The water-to-uranium ratio of the average loading compares quite favorably with the value of 3.56 obtained for the former reference core (Case II).

The plate dimensions, derived from the re-evaluation study, are considered firm specifications; however, small changes in plate thickness (of the range of 0.002 in.) may be made as the result of future rolling experience. These changes, if any, will be incorporated in the final fuel specification.

e. Water Corrosion of EBWR-Type Fuel Plates -
C. R. Breden

Eight EBWR-type plates, each containing uranium-5% zirconium-1.5% niobium and roll clad with Zircaloy 2, have accumulated exposure times ranging up to 5856 hr in static, degassed water at 550F without failure. A detailed description of the plates and previous test history are reported in ANL-5511.⁴

f. Boiling Water Corrosion of Zircaloy 2 - C. R. Breden

Continuing the experiment described previously,⁵ a new Zircaloy 2-jacketed thimble of carbon steel SA-212 was made, with bismuth-lead eutectic as a heat-conducting bond between the thimble and the jacket. This unit was operated for 53 hr at 500F and 800 psig, and then for 66 hr at 550F and 1045 psig. The distilled, demineralized water placed in the boiler at the start was not replaced during the run. The average amount of heat put through the thimble was estimated at about 14 kw, with a maximum of about 20 kw. The latter corresponds to a heat flux of about 525,000 Btu/(hr)(sq.ft). It is believed that, at least for part of the time, film boiling instead of local boiling occurred. At the higher power output rates, there was considerable variation in the rate of heat transfer as measured by the condenser coil pickup. After the run, terminated by a leak between jacket and thimble, the thimble showed some reddish-brown scale deposit. Figure 12^{P.33} shows the appearance of the jacketed thimble after the test, along with a magnified section of the scaled area.

g. Structural Modifications to Facilitate Forced Circulation - C. K. Soppet

The adaptation of the EBWR to forced circulation experiments requires that a number of changes be made in the structural

⁴ Reactor Engineering Division Quarterly Report (January, 1956) p. 17.

⁵ Ibid.

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TYPICAL SCALE
DEPOSIT (X52)

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SCALE
DEPOSIT

FIG. 12
ZIRCALOY 2-JACKETED SA-212 CARBON STEEL
THIMBLE EXPOSED IN DISTILLED, DEMINERALIZED
WATER FOR 53 hr AT 500 F AND 800 psig
FOLLOWED BY 66 hr AT 550 F AND 1045 PSIG

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portions of the reactor to reduce the flow leakage permitted by the natural circulation design. Specifically, these changes are:

- (1) The removal of large cross-communication holes in the control rod guide and top shroud structure.
- (2) Extension of dummy fuel element boxes downward to the top surface of the support plate.
- (3) The addition of flow restrictors to the control rod channels.

The first two changes have been completed; the third is being accomplished at present.

The expected pressure drop through the core necessitated a change to the locating end fitting of the dummy elements to incorporate a hold-down feature. This change consists of three toggle-operated, retractable fingers which protrude through holes in the wall of the end fitting. The fingers, when extended, bear against the bottom surface of the core support plate clamping the dummy element into the fuel-locating hole of the support plate. This addition prevents the dummies from being lifted out of the core, by pump pressure, during forced circulation operation.

2. Control Rods and Drive Mechanism

a. Design and Prototype Testing - C. F. Bullinger

The Model II reference design prototype control rod drive⁶ has been operated for a total of 5500 cycles and 3300 scrams at 600-lb saturated steam pressure.

The Stellite No. 3 guide bushing above the seal unit has been replaced with a nylon #FM-10001 bushing. Difficulty had been experienced with the extension rod flaking off chrome particles as it passed through the Stellite bushing during scrams. Substitution of the nylon bushing has eliminated this tendency to score the shaft.

The scram-time curve shown in Fig. 13 illustrates the short initial lag (10 milliseconds) between the original signal and latch release. Full speed of 12 fps is attained in 65 milliseconds or 4 in. of travel.

⁶ Reactor Engineering Division Quarterly Report, ANL-5511
(January, 1956) p. 18.

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Fig. 13
EBWR Control Rod Scram Time at 600 psig

The floating seal rings have worn from 1.0005 in. average diameter to 1.002 in. diameter during the 3300 scrams, with a leakage increase from an original rate of 350 cc/min up to 1800 cc/min. However, the service life of 5500 cycles and 3300 scrams may be considered as equivalent to a minimum of 12 years of normal reactor operation.

b. Evaluation of Control Rod Materials - C. R. Breden,
L. W. Fromm

(1) Screening of Crystal Bar Hafnium

About 500 lb of crystal bar hafnium were subjected to autoclave testing in 550F water to select the best material to be used in control rod fabrication. Previous to testing, a small piece of

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each bar was cut off to expose a cut cross section. The material was pickled and carefully washed before autoclaving. After a two-week test, about one-third of all the material was found to be free of corrosion around the wire core.

(2) Zircaloy 2 - Stainless Steel Contact Corrosion

Samples of Zircaloy 2 bolted together with Type 304 and with Type 347 stainless steels showed no detectable contact-crevice effects after 7338 hr in dynamic test (1470 hr at 500F; 5868 hr at 600F), or in static tests of 6668 hr in air-saturated and in degassed water, both at 500F, and for 5850 hr in air-saturated and in degassed water at 600F.

(3) Hafnium-Hafnium Contact Corrosion

Contact corrosion tests are also being conducted on samples of hafnium plates fastened together by spot welding and by riveting with stainless steel, zirconium, and tantalum rivets.

One group of samples was examined after 1681 hr in 550F degassed water. The best appearance was exhibited by the spot welded samples. The zirconium rivets showed the most corrosion, possibly due to nitrogen pickup during peening operation. None of the fastenings showed accelerated crevice or galvanic attack.

The appearance of a second group of similar couples is still satisfactory after 2767 hr.

3. Pressure Vessel - N. Balai

a. Measurement of Vessel Shell Temperatures

Thirteen thermocouple wells, with duplicate thermocouples in each well, are to be welded to the walls of the EBWR pressure vessel to permit measurement of shell temperatures and evaluation of thermal stresses during heating up and cooling down transients, as well as for steady-state conditions. Thermocouples will be held in place in the wells by a high-temperature, 95% cadmium - 5% silver solder (liquidus 740F - solidus 640F). The calculated pressure vessel shell operating temperature (~500F) precludes the use of conventional lead-tin solder. The heat generation contributed by the neutron-cadmium reaction was calculated to be less than 1F and thus unimportant in shell temperature measurements.

In a trial installation, torch-soldering of thermocouples into sample wells revealed that metallic bonding between the steel and the thoroughly cleaned wires was easily accomplished with an acidified (HCl) zinc chloride flux solution (see Fig. 14).

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FIG. 14
SECTIONAL VIEWS OF SOLDERED THERMOCOUPLES
SHOWING (a) EMBRITTLED CONSTANTAN WIRE AND
(b) UNBONDED AREAS IN CHROMEL-ALUMEL WIRE WELLS

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The constantan alloy wire in iron-constantan thermocouples was severely embrittled by the cadmium-silver solder (see Fig. 14 (a)). The chromel-alumel thermocouples while not embrittled, exhibited unbonded areas (Fig. 14 (b)) which exemplify the need for thorough cleaning of the chromel-alumel wires to obtain good metallic soldered bonds.

The soldering procedure used was:

- (1) Fill wells with flux solution.
- (2) Heat wells while feeding solder alloy to fill well.
- (3) Dip thermocouples in flux and preheat slightly.
- (4) Insert thermocouples in wells while maintaining solder molten until thermocouples are seated at bottom of well and surface of solder becomes quiescent.
- (5) Wash off acidified zinc chloride flux and solder residues with hot water to prevent chloride corrosion of steel.

Prior to the test installation of thermocouples, the sample thermocouple well blocks were fillet-welded to a 1-in. thick carbon steel plate to simulate heat transfer to the vessel wall during the soldering operation. The fact that a painted surface two inches away from the well location was not affected by the heat of the soldering operation indicates that damage to the EBWR pressure vessel (designed to Section I of the ASME Boiler and Pressure Vessel Code) will be negligible.

The asbestos-insulated thermocouple wires, selected for resistance to irradiation damage, will be encased by a sheet metal channel bolted to the thermocouple blocks for protection against mechanical piercing of the wire jackets by the stainless steel-wool vessel insulation.

b. Irradiation of EBWR Pressure Vessel Steel

A welded test section of fine-grained SA212 Grade B plate (3-15/16 in. gage), prepared in accordance with welding procedures utilized in the fabrication of the EBWR pressure vessel, has been received from the Babcock and Wilcox Company. The test section was prepared from the corner remnants of a square plate from which the lower head blank had been spun. A cutting diagram has been prepared for the preparation of miniature tensile and impact specimens for irradiation in the MTR. Sectioning of the welded plate is in progress.

Irradiation specimens are to be removed from the MTR at regular intervals (approximately once a month) and examined to determine the progressive changes of mechanical properties as a function

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of irradiation. At the end of the irradiation (approximately 18 months), a history corresponding to a 100-year EBWR lifetime will be available. Fracture and ductility transition temperature data are to be obtained.

c. Physical and Mechanical Tests on "C" Shock Springs

A design incorporating "C" shock springs interposed between the vessel support lugs and the alloy structural support system for the EBWR pressure vessel was described in ANL-5511. The final design of these springs is shown in Fig. 15. Since design methods for such spring members are not too accurate, an experiment to measure the spring characteristics was performed. A test spring, with a diameter to thickness ratio similar to that required for the EBWR application, was fabricated from a section of a chrome-molybdenum steel hollow forging obtained from the Babcock and Wilcox Company. The chemical and mechanical properties⁷ of the material are as follows:

Material: Seamless Chrome-Moly Steel Pipe (ASTM A-158-46T)

Heat No.: 17833

Mech. Prop.: T.S. = 75,000 psi; Y.P. = 35,000 psi; Elong. (in 2 in.): 30%

Chemical Analysis: C - 0.11; Mn - 0.45/0.47; P - 0.023; S - 0.029;
Si - 0.24; Cr - 4.87; Mo - 0.53/0.54.

A ring from which the test "C" spring was fabricated was heat treated to obtain a yield strength of 100,000 psi (min.) by heating to 1750F/1800F, holding at temperature for one hour, and air cooling, to temper to a Rockwell C hardness of 28. For the test, the ring was mounted between the lower platen and the crosshead of a 60,000-lb Baldwin-Southwork testing machine, and loaded until the ends of the spring touched. A central alloy steel spindle was threaded into the lower cylindrical seat for the containment of heavy fragments in the event of an unexpected spring failure. A load-strain diagram was obtained in order to determine the yield point of the spring. The test apparatus and the load-strain curve are shown in Figs. 16 and 17, respectively.

⁷ Mill Test Report furnished by Spang-Chalfant Division of The National Supply Co., Ambridge, Pa.

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MATERIAL: CARILLOY T-1 STEEL

HEAT TREATED TO OBTAIN:

TENSILE STRENGTH: 105,000 psi (min)

YIELD POINT: 90,000 psi (min)

ELONGATION: 18% (min)

REDUCTION OF AREA: 50% (min)

Fig. 15

Approved "C" Shock Spring Design for EBWR Pressure Vessel

Fig. 16

"C" Spring Compression Test Setup

Fig. 17
Load-Deflection Diagram for Chrome-Moly Heat
Treated "C" Shock Spring

Upon completion of the compression test an unstressed section of the "C" spring (the portion between the end of the "C" and the spindle hole) was milled to form a flat bar from which tensile specimens (0.438 in. dia. x 1-1/2 in. gage length) were prepared. (Standard 0.505-in. specimens could not be prepared for lack of available material in the milled bar.) Results of the tensile tests confirmed the selected hardening heat treatment:

	Bar No. 1	Bar No. 2
Tensile Strength, psi	130,100	128,750
Yield Point, psi	111,200	103,200
Elongation (1-1/2 in.), %	15.4	16.9
Reduction of Area (R.A.)(0.438 in. dia.), %	60.4	62.0

Note: The stress-strain curve obtained shows that the material follows Hooke's law exactly to the yield point as in the case of the more familiar unalloyed carbon steels.

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The load-deflection diagram (Fig. 17) shows that the strength of the "C" spring configuration is twice as great (at the yield point) as can be calculated from the simple combined stress relation:

$$S = \frac{P}{A} + \frac{Mc}{I} ,$$

where

S = combined stress

P/A = stress contribution by column effect

Mc/I = stress contribution by bending.

At a yield point stress of 100,000 psi, the calculated safe load is 20,500 lb; at the sharp change in slope, which is interpreted as the yield stress, a load of 39,500 lb was sustained.

The spring sustained a permanent set of 7/32 in. after the application of the 48,600-lb load required to close the opening in the spring. The spring showed no other signs of distress.

Since the diameter-to-thickness ratios of the experimental spring and the EBWR pressure vessel support shock springs are virtually identical, the experimental load data were applied to the design of shock springs for the protection of the vessel support structure. The shock springs are designed to sustain a static load of 16,000 lb per spring (8 springs required) which includes the weights of: (1) the vessel, filled with water; (2) the 12-in. thick indexing plugs in place; and (3) the carrier plate in place for the manipulation of the indexing plugs. The static stress in the springs for this condition, as determined by the conventional method of determining maximum stress ($S = P/A + Mc/I$), is 85,500 psi, while the actual stress as determined by the experiment is approximately 45,000 psi. Under impact loadings, such as dropping the indexing plugs, cover plate or fuel-unloading coffin, the springs will deform and cushion the blow on the support structure. The spring material selected is structural quality Carilloy T-1 (U.S. Steel Corp.) bar quenched and tempered, after rolling, to the following minimum mechanical properties:

T.S. - 105,000 psi	Elong. (2 in.) - 20%
Y.P. - 90,000 psi	R. A. - 50%

Removable biological shielding permits access to the vessel support structure for the replacement of damaged shock springs in the event that excessive permanent deformation from impact loadings occurs.

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4. Shielding Calculations for D₂O Operation - M. Grotenhuis,
A. E. McCarthy

Table VI lists the gamma-ray flux and corresponding heat generation at the extremities of each region of the D₂O-moderated EBWR, along with the thickness of the region as assumed for the calculation. The associated slope is given on the basis that the gamma-ray flux is exponential within the region.

B. Status of Construction - L. W. Fromm, A. S. Jameson

1. Power Plant Building - A. H. Heineman, E. H. Van Zylstra

During this quarter, all welding on the power plant building steel shell required for pressure and leakage tests was completed, and the tests were carried out. In order to avoid delaying the tests and subsequent construction work inside and beneath the shell, the two air-locks and the freight door (and their reinforcing plates), fabrication of which had not been completed, were not welded in place. These units will be installed and tested at a later date.

The building shell specifications require that 800 ft or 10% of the number of feet of weld completed be X-rayed. Welds were X-rayed by Graver Tank and Manufacturing Company (the shell contractor) with portable equipment shortly after they were completed. A Walter H. Flood Company inspector, representing AEC, also visually inspected every weld and marked surface defects for repair. The X rays were interpreted first by Graver and then by Flood. Welds with questionable internal defects were marked for repair. Forty-eight, or 6.3%, of the original X-rayed sections disclosed defects. Typical defects included lack of root fusion and isolated slag inclusions. The defective welds were chipped out, re-welded and X-rayed. The process was repeated, where necessary, until both Graver and Flood were satisfied that the welds conformed with Code requirements.

As of November 6, 1955, 766 satisfactory X rays had been taken. This represents 746.1 ft of the 6,655 ft of welding and includes re-worked defective sections and 54 junctions between horizontal and vertical welds.

Pressure and leakage tests were carried out in the following sequence:

(1) The shell was pressurized, by four portable compressors, to approximately 10 psig. A gate valve in the temporary inlet line was closed and the compressors stopped. All welds in the shell were painted with a thick soap solution and observed for bubbles, or dry flaking, which would indicate air leaks. No leaks were found by this method.

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Table VI

GAMMA-RAY FLUX AND CORRESPONDING HEAT GENERATION
IN D₂O-MODERATED EBWR

Region	$\phi\gamma$, mev/(cm ²)(sec)	H, watt/cm ³	Thickness, cm	Slope, cm ⁻¹
Radial Shield				
Thermal shield	1.3 x 10 ¹³	6.3 x 10 ⁻¹	2.54	0.27
	6.6 x 10 ¹²	3.2 x 10 ⁻¹		
Pressure vessel	6.6 x 10 ¹²	3.2 x 10 ⁻¹	6.35	0.19
	2.3 x 10 ¹²	9.7 x 10 ⁻²		
Lead	2.3 x 10 ¹²	1.8 x 10 ⁻¹	7.62	0.30
	2.3 x 10 ¹¹	1.9 x 10 ⁻²		
Magnetite concrete (220 lb/cu ft)	2.3 x 10 ¹¹	3.4 x 10 ⁻³	152.4	0.088
Axial Shield Above the Core				
Steel plug	1.9 x 10 ¹¹	7.3 x 10 ⁻³	30.48	0.23
	1.7 x 10 ⁸	6.7 x 10 ⁻⁶		
Vessel closure	1.7 x 10 ⁸	6.7 x 10 ⁻⁶	22.86	0.23
	9.9 x 10 ⁵	3.8 x 10 ⁻⁸		
H ₂ O tank bottom	9.9 x 10 ⁵	3.8 x 10 ⁻⁸	2.54	0.23
	5.7 x 10 ⁵	2.2 x 10 ⁻⁸		
Axial Shield Below the Core				
Pressure vessel	3.3 x 10 ¹²	1.3 x 10 ⁻¹	6.35	0.29
	5.5 x 10 ¹¹	2.1 x 10 ⁻²		
Lead	5.5 x 10 ¹¹	4.5 x 10 ⁻²	7.62	0.45
	1.7 x 10 ¹⁰	1.4 x 10 ⁻³		
Heavy concrete (290 lb/cu ft)	1.7 x 10 ¹⁰	3.1 x 10 ⁻⁴	121.92	0.12

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(2) The pressure was increased to 18-3/4 psig (25% over the design pressure) for the pressure strength test. The valve was then opened to depressurize the shell preparatory to the leakage rate test.

(3) The temporary manhole in the side wall of the shell was opened and the following equipment was installed within:

(a) Ten thermocouples were wedged against the inside surface of the shell.

(b) Eight resistance thermometer elements were suspended in the air space.

(c) Three dew point cells of the high-level-alarm-type for determining the approximate relative humidity of the contained air were placed in the air space.

(d) Six blowers were placed at various locations inside the shell. The total capacity of these blowers (9,700 cfm) was

sufficient to recirculate the entire building contents (497,500 cu ft) in a little less than one hour. The amount of air motion produced by the blowers proved sufficient to reduce stratification to a tolerable degree during periods, as defined below, when reliable readings could be obtained.

The arrangement of the thermocouples, resistance thermometer elements, and blowers are shown in Fig. 18. All electrical leads were brought through temporary flanged openings in the shell and thence to a trailer which served as an

instrument room. The method of sealing the leads in the flanged openings is illustrated. For pressure measurement, copper tubes were led from a temporary pipe connection to a commercial pressure recorder hung on the side of the building, and to the instrument trailer.

Thermocouple readings were made in the conventional manner by means of a manually adjusted potentiometer and calibration table. Resistance thermometer readings were made by means of a calibrated Wheatstone Bridge with all elements and their leads calibrated in

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LEGEND: - THERMOCOUPLE

- RESISTANCE THERMOMETER ELEMENT

- BLOWER (CAPACITY AS NOTED)

FIG. 18
LOCATION OF INSTRUMENTATION AND BLOWERS
FOR EBWR STEEL SHELL LEAKAGE-RATE TEST

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connection with the bridge. Alarm signals from the dew point cells were received by their own commercial instruments. Pressure readings were made on a mercury manometer, and readings from a mercury barometer were added to those from the manometer to obtain total absolute pressure.

The manhole cover was replaced, the shell was again pressurized to approximately 15 psig, and the valve closed. Temperature and pressure readings were taken over a period of a little more than eight days at approximately 2-hr intervals. Readings were converted, using Boyle's and Charles' Laws, to terms of volume of dry air at standard temperature and pressure within the building, taking into account the variations in building volume by metal expansion and contraction, and the variation of water (or ice) vapor pressure with temperature.

In determining what might be considered as reliable readings during the leakage rate test, several factors were taken into consideration. All erratic readings (except one) occurred at times when the skin temperature was very high, or else when the skin temperature was changing rapidly. Both of these conditions might be expected to give unreliable results for the following reasons:

(1) When the temperature is very high the moisture in the air is not saturated; thus the relative humidity is unknown and cannot be corrected for. If saturation were assumed, an over-correction would result.

(2) When the temperature is changing rapidly, there can be no assurance that the arithmetical average from the several elements is indicative of the true average temperature.

It was decided, therefore, that only those readings which complied with the following limitations would be considered reliable:

(1) No readings were considered valid where the average shell metal temperature was higher than 50F.

(2) No readings were considered valid where the average shell metal temperature differed by more than 4F from the temperature read two hours before or after the reading in question.

Calculated dry air volumes are plotted against time in Fig. 19, using only those readings which satisfied the above limitations. Curve (A) represents the specified allowable leakage for the building shell (500 cu ft/24 hr at STP). The exact location of the point of origin for this curve is, of course, unknown; however, an attempt was made to locate it judiciously from the general trend of the plotted points.

Fig. 19
Leakage-Rate Test of EBWR Steel Shell

The specifications further state that a second soap solution test may be omitted provided the indicated leakage is less than 100 cu ft per 24 hr period. Inasmuch as the actual leakage rate was less than 500 cu ft, and might be interpreted as somewhat less than 100 cu ft per 24 hr (see Curve B), the second soap test was terminated when about one-half completed in order to permit subsequent construction to proceed.

In view of the foregoing test results, the steel shell construction at this stage of completion was formally accepted on November 22, 1956.

Subsequently a temporary opening (5 ft wide by 10 ft high) was cut through the shell at an elevation about 30 ft below grade level. This opening, along with a temporary bridge provides convenient access to the lower portion of the shell for materials and concrete pipes extending from the Pumpcrete machines located at ground level.

Light reinforcing rings were welded to the outside of the shell dome to provide added support for the 15,000-gallon emergency water supply tank, located in the dome. These reinforcing rings, spaced

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on 16-in. centers, are located in the area immediately surrounding the tank, beginning at a point where the dome radius is 21 ft-4 in. The radius of the tank at the point of attachment to the dome is 13 ft-8 in.

The freight door was delivered to the site, fitted, and welded in place. Ten X rays showing approximately 6 ft of sound weld were taken.

The remainder of the quarter was spent in preparing for and making the initial concrete pours inside and beneath the shell. Prior to pouring of any concrete, studs were welded to the inside of the cylindrical portion of the shell, on 24-in. centers in both directions, for attaching form support ties. The entire shell below grade was then coated, both inside and outside, with bituminous enamel for protection against corrosion.

The first concrete pour, which required no form work, was made outside of the shell under the bottom head, and consisted of approximately 360 cu yd.

Forming and placing of reinforcing steel for the first inside concrete pour was nearing completion at the end of the quarter.

Since the power plant building is constructed approximately one-half below grade, there is danger of flotation of the shell during construction in case of heavy rains. Therefore, at all times except during the pressure and leak tests, ballast holes in the bottom head must be left open. The lower set of holes are left open until the first two inside concrete pours are completed. These lower holes are then closed and permanently welded. The upper holes are about 24 in. below the tangent line of the bottom head and are equipped with short pipes which extend inward beyond the inner face of the concrete. These holes will be left open until all concrete is poured inside the building, at which time they will be permanently welded closed. In this way ballast holes are provided until sufficient weight has been added to the building to prevent flotation under any circumstances.

Concrete pours are sequenced to minimize distortion of the shell: Pour No. 1 is made outside, under the bottom head, and contacts the head to a radius of 16 ft-6 in. from center line of building. Pour No. 2 is made inside the shell up to basement floor elevation and out to radius 16 ft-6 in.; Pour No. 3 is outside to radius 27 ft-0 in., and Pour No. 4 is made inside to this same radius. The sequence is continued until the space beneath the shell is completely filled with concrete out to the full shell radius of 40 ft.

The architect-engineers completed incorporation into their drawings and specifications of all features of the connecting piping, access tunnels, removable shielding, lead shielding, cooling coils, top and bottom shielding, vessel support and containment structure, vessel envelope seals

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and vent, thermocouples, instrument wells, and constituency of the biological shield immediately surrounding the reactor vessel. These drawings and specifications were turned over to the general contractor for estimating the costs for this supplemental work.

2. Pressure Vessel - N. Balai

The design of the EBWR pressure was finalized and fabricators drawings were approved for construction. The status of individual vessel components is as follows:

Cover Plate

The 9-in. thick flat head forging was completed. Fabrication included:

- (1) Deposition of stainless steel cladding (nominally Type 304 with 0.08% carbon max.) by the Union-melt process.
- (2) Machining of rough cladding to a final minimum thickness of 3/16 in.
- (3) Attachment of stainless steel decelerating pads.
- (4) Boring of 44 bolt holes.
- (5) Welding of a carbon steel bearing ring (3 x 2 in.) to the upper face of the cover plate. The 3-in. plenum formed by the ring is to be filled with stainless steel wool insulation.

Upper Ring Forging

The upper ring forging which constitutes the vessel closure was completed. Fabrication included:

- (1) Deposition of cladding (nominally Type 304 stainless steel with 0.08% carbon max.) by the Union-melt process.
- (2) Machining of rough cladding to a final minimum thickness of 3/16 in. except for the gasket-bearing surface.
- (3) Machining of gasket and leak-off grooves.
- (4) Boring and tapping of 44 stud bolt holes.
- (5) Attachment of stainless steel Type 304 bracket supports, by welding, for the support of the inner Type 304 shielding ring.

Shell

Stainless steel Type 304 cladding sheets (0.109 in. thick) were applied to the flat 2-3/8 in. thick, SA-212, Grade B, fine-grained carbon steel shell plates by pressure-resistance welding prior to rolling of the

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shell sections. Resistance welds are approximately 1 in. in length on 2-in. centers; adjacent rows of welds are $3/4$ in. apart. Edges of abutting sheets were manually arc-welded to obtain a continuous surface. Cladding integrity was checked pneumatically by introducing nitrogen under the stainless steel skin. At 900-psi nitrogen pressure, a few small flaws were found by soap tests; the welds and unbonded cladding showed no visible signs of distress otherwise.

The clad plates were rolled into two semi-cylindrical sections, which were then welded together longitudinally and circumferentially to form the shell of the vessel. All welds were radiographed, and minor weld defects were discovered and corrected. The ends of the shell were beveled for welding to the upper ring forging at one end and to the lower head at the other.

Lower Head

The lower head was hot spun from a 3-15/16-in. thick, fine-grained SA-212, Grade B plate, clad with a 0.109-in. thickness of stainless steel Type 304 sheet by the pressure-resistance method described above. Thirteen holes were drilled to receive nozzle stubs (four 6-in. forced circulation nozzles and nine 5-in. control rod thimbles); all 6-in. and 5-in. diameter stubs were welded into the head. Nozzle stubs were formed by welding two semi-cylindrical sections of roll-bonded stainless steel clad (Type 304 ELC) fine-grained SA-212, Grade B plate. A central solid steel mandril was used by the fabricator to maintain nozzle stub concentricity while welding the two longitudinal seams. The steel mandril was then drilled out and internal stainless steel cladding was deposited over the seams by manual arc-welding. All seams were radiographed, and flaws were repaired as revealed by the films.

The clad plate nozzle stub technique permits the nozzle-to-lower head strength weld, which is under high restraint, to be a carbon steel-to-carbon steel joint. The Inconel transition weld between the nozzle stub and the stainless steel nozzle proper is thus moved away from the lower head to a point where the weld is under low restraint. This design was adopted on the recommendation of the fabricator, on the basis of steam power piping installation service histories which indicated greater reliability for dissimilar metal weldments under conditions of low restraint.

3. Containment Structure and Shielding - E. H. Van Zylstra

a. Containment Structure

Fabrication of the reactor pressure vessel containment structure⁸ is proceeding at Rock Island Arsenal under the Argonne supervision.

⁸Reactor Engineering Division Quarterly Report, ANL-5511 (January, 1956) p. 31.

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To develop the various welded joint configurations and to qualify the welders for fabrication of the containment structure, sample "T" welds and fillet welds were made on the Carilloy T-1 quenched and tempered alloy steel to be used. All samples were sectioned, etched and examined microscopically. Certain preparation changes were made and those welders qualified were assigned to the fabrication work. It was decided that magnetic particle tests at various stages of the welding were of more value than X-ray examination due to the difficulty of obtaining good X-ray exposure and the questionable interpretation of negatives. Hardness traverses indicated typical behavior of the Carilloy T-1 parent metal and the E-12015 electrode-deposited weld metal.

The two tie-beams connecting the base assemblies, and the stub column and base assemblies themselves, were completed and delivered to the Argonne site. The tie beams were placed within the building shell prior to completion of the dome, and the column and base assemblies were stored on blocks and under canvas outside the building to await installation in proper sequence.

Fabrication continues on the upper columns, the hold-down beams, and the pressure vessel support framework at Rock Island. All the Carilloy T-1 required for completion of the structure has been received.

Late in the quarter it was decided also to fabricate the blast shields at Rock Island Arsenal. These assemblies are laminated from 1-in. steel plates, 3-in. redwood boards and 3-in. celotex sheets and provide a crushable energy-absorbing medium between the two sets of 3 columns and the source of a possible explosion at the center of the reactor core. In adding fabrication details to the conceptual design drawing, certain simplifications were made. The No. 16 gage sheet metal waterproof covers for these assemblies were replaced by a coating of hot pitch. Each 5-plate assembly was scheduled for factory assembly and two lifting lugs were provided to facilitate package placement in the field. Field tack-welding of the last steel plate in each assembly to the T-1 containment columns was substituted for a bolted assembly. Support shelf and gusset assemblies were scheduled for shop fabrication to reduce the amount of field welding required. All materials necessary for fabrication of the shields are now on hand at Rock Island.

b. Shielding

Specifications have been written for magnetite bulk shielding concrete, magnetite and punchings concrete, and removable magnetite concrete shielding blocks. Trial mixes have been made with samples of fine and coarse magnetite ore obtained from several sources. Compression tests were performed at various times after pouring of these samples (Table VII).

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Table VII

COMPRESSION TESTS ON SPECIMEN CONCRETE SHIELDING

Sample No.	Curing, days	Concrete Mix	Density, lb/ft ³	Compression Strength, psi
1*	7	Magnetite & punchings concrete Jones & Laughlin - fine	268	1586
2*	7	Same as No. 1	268	1815
3	18	Magnetite & punchings concrete Jones & Laughlin - fine	277	1715
4	28	Same as No. 3	266	2777
5	18	Magnetite & punchings concrete National Lead - fine	298	3272
6	28	Same as No. 5	293	4597
7	18	Magnetite concrete Montana - coarse National Lead - fine	226	1821
8	28	Same as No. 7	226	1768

* Tested at Argonne. All other tests at Pittsburgh Testing Laboratory.

On the basis of the above tests and evaluation of quotations from the various suppliers, Montana-coarse and National Lead-fine magnetite were selected as coarse and fine aggregate for the EBWR biological shielding; appropriate orders were placed.

Orders have also been placed for 50 of the 120-ton total requirement of steel punchings for the heavy concrete biological shielding below the reactor, and for the curved lead brick to be placed between the shielding concrete and the reactor vessel.

4. Steam Plant - V. C. Hall, E. Wimunc, C. Zitek

The status of the major equipment items to be furnished by the Contractor is as follows:

Equipment	Drawing Status
Cooling Tower	Approved with exceptions
Filters	Final approval
Air Compressors	Approved with minor exceptions
Engine-Driven Generator	Approved with minor exceptions

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Equipment	Drawing Status
Reactor Water Purification Circulating Pumps	Approved with minor exceptions
Miscellaneous Water Pumps	Final approval
Sump & Condensate Pumps	Final approval
Heat Exchangers	Final approval
Water-Treating Equipment	
(a) Reactor Purification	Awaiting final outline dimensional drawings
(b) Make-up	Final approval
Butterfly Valves	Approved with minor exceptions
Tanks	Final approval
Piping	Approved with exceptions

The majority of valves, pipe hangers, sight flow, pressure and temperature indicators have been approved.

The Allis-Chalmers power equipment, the turbine-generator exciter unit, condenser, circulating water pumps and transformers were delivered to the site. Prior to shipment, Allis-Chalmers conducted mechanical no-load running, over-speed trip, and gland leakage tests on the turbine generator; the machine performance meets design expectations.

The Freon leakage tests of the condenser, to check leakage from the shell, between the water box and tube sheet, and between the double-tube sheets, indicated the unit to be exceptionally tight in comparison to a conventional steam plant condenser.

Performance tests conducted on the reactor feed pumps show the units, as designed, can deliver high head at rated flow with lower motor power, and can operate down to a net positive suction head of 5 ft without cavitating.

The components to be delivered during the next quarter (feed-water pumps, air ejectors, circulating water treatment equipment, and the air-drying, vapor-recovery system) are in various stages of fabrication assembly and related tests.

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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. Riser Studies

A preliminary series of experimental runs have been made on two riser configurations to determine the contribution to driving head in natural circulation of various geometries and operating parameters. The riser discussed here is a channel of constant cross section extending from the outlet end of the heated plates to the liquid-vapor interface. The two geometries were: (1) 4.5 x 2.625-in. single-channel rectangular section and (2) a multi-channel section identical to the heated plates having three 7/16 x 3-11/16-in. channels and two 7/32 x 3-11/16-in. channels.

Void traverses along the length of the riser showed that the voids in the riser were only slightly different from those at the channel outlet for the single riser channel, but the voids decreased considerably upon passage of the steam-water mixture into the multi-channel riser section.

The data could be correlated in terms of the ratio of steam-to-liquid velocities (c_s/c_ℓ) vs the volumetric flow of liquid in the riser per unit of total flow area ($V_\ell W_t/A$)(1-X), where

$$\frac{c_s}{c_\ell} = \left(\frac{X}{1-X} \right) \left(\frac{1-\alpha}{\alpha} \right) \left(\frac{v_g}{v_\ell} \right)$$

and

X = steam weight fraction

α = steam void fraction

v_g/v_ℓ = ratio of specific volumes of liquid and vapor

W_T = total weight flow (steam and liquid),

The average values of c_s/c_ℓ for the riser sections, along with the curve obtained for c_s/c_ℓ at the exit of the heated channels, are plotted in Fig. 20. It was noted that for steam weight fractions above 10% a slightly better plot of channel data was obtained than shown in plots of c_s/c_ℓ vs V_{in} reported in ANL-5511.⁹

⁹Reactor Engineering Division Quarterly Report (January, 1956) p. 70.

Fig. 20
Slip Ratio Measurements of Unheated Sections
Compared with Heated Channels

Separate functional relationships are shown between c_s/c_l and $(V_l W_t/A)(1-X)$ for the heated channel and the risers. At present there is insufficient data to determine a generalized way of representing data for heated risers and channels; however, the 4.5 x 2.625-in. riser data and the heated channel data may be plotted on a single curve by use of the parameter

$$\left(\frac{V_l W_t}{A}\right) \left(\frac{A}{A_{\text{heated}}}\right)^{0.57} (1-X)$$

Since riser data obtained at two height levels (52 in. and 35.75 in.) are represented on Fig. 20, it may be concluded that riser height is not too important a parameter in influencing the ratio of velocities of steam and liquid.

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b. Preliminary Two-Phase Pressure Drop Investigation

An investigation of the pressure drop resulting from two-phase flow has been completed. The range of variables was as follows:

Pressure	100 - 600 psig
Exit Quality	0 - 14%
Exit Steam Volume Fraction	0 - 75%
Subcooling	0 - 30F

The results are plotted (Fig. 21) in the form of the ratio of the frictional loss in two-phase flow to the loss in single phase flow vs the average steam volume fraction. The considerable scatter in the data, along with the absence of any trend within the scatter, indicates no observable effect of pressure upon the two-phase frictional loss. It is expected that refinements in the method of measuring the frictional losses will reduce the scatter considerably.

c. Transient Behavior of the

Fig. 21
Two-Phase Pressure Drop Ratio vs Average Steam Volume Fraction

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c. Transient Behavior of the Multi-Channel Natural Circulation Loop

The BORAX experiments produced oscillations in temperature, pressure, and power when a sufficiently high-power density was experienced. A series of runs were made with the multi-channel, natural circulation loop to determine if the same type of transient behavior is observable in an electrically heated system.

The BORAX system consisted of a 24-in. core, 24-in. riser and a 24-in. reflector. The channel spacing was 0.264 in. The test loop system consisted of a 24-in. heated test section with a 35.75-in. riser. The plate spacing was 7/16 in.

The performance of the system was observed over a range of power densities at 300 and 600 psig, with inlet water at saturation temperature.

The transient behavior was observed to take three different forms:

- (1) Rapid oscillations of water level (0.5 - 1.5 cps).
- (2) Rapid minor oscillations in flow of approximately 15 cpm.
- (3) Slower, larger oscillations in flow (0.5 - 1 cpm).

The slower, larger oscillations in flow were thought to be caused by the condenser and are thus a function of the specific system. Probable changes in void level could not be observed in view of the response time of the instruments.

The oscillations of the water level were probably a good indication of the mechanism of boiling in the test section and riser, i.e., slugs of steam and water being expelled from the test section. These oscillations would not be directly coupled to the expulsions, but will serve as a qualitative indication of the transient behavior of a natural circulation system. The results of the visual observations are tabulated below.

Oscillations of Water Level at 300 psig

Power Density, kw/liter	Frequency, cps	Exit Void Fraction, α_{ex}
13	0.5	0.290
26	0.6	0.414
33	0.6	0.516
45	1.0	0.612
50	1.4	0.620

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The rapid change in frequencies observed at and above 33 kw/liter correspond to the power level at which oscillations were first noticed in the BORAX experiments.

Oscillations of Water Level at 600 psig

Power Density, kw/liter	Frequency, cps	Exit Void Fraction, α_{ex}
13	0.5	0.25
26	0.6	0.33
33	0.5	0.41
45	0.6	0.44
50	0.7	0.52

The severity of the oscillations increased with the frequency. Comparable void fractions gave oscillations of comparable frequency and magnitude.

The experimental oscillations of the water level were of the same order of magnitude as the oscillations observed in temperature, pressure, and power in the BORAX experiments. If the voids were coupled to the power as in a reactor, the same type of behavior could be expected to occur.

The system may have some effect on the type and frequency of the oscillations, but the oscillations of water level, as caused by alternate steam-water expulsions from the test section, should be relatively independent of the system.

d. Theory of Gravitational Oscillation - J. A. Thie

To assist in interpreting the oscillations described in the preceding section, the fundamental frequency of the system's gravitational oscillations was calculated. Ignoring the presence of heating elements and voids, one can obtain, merely from the system's kinetic energy and gravitational potential energy, the equation of motion:

$$C \frac{d^2x}{dt^2} + (1 - B^2) \left(x \frac{d^2x}{dt^2} + \frac{1}{2} \frac{dx^2}{dt} \right) = g A - g (1 + B) x \quad ,$$

where A, B, and C are known functions of the geometry, x is the water level height, and g is the gravitational acceleration.

The fundamental frequency of this nonlinear oscillation is

$$(2 \pi)^{-1} x g^{1/2} (1 + B)^{1/2} (A - AB + C)^{-1/2} = 0.70 \text{ cps} \quad .$$

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Any disturbance of the system is sufficient to excite these oscillations. For exit void fractions less than 0.5, the experimental frequency was 0.5 to 0.7 cps. However, this agreement may be fortuitous, and the oscillations could be of a thermodynamic and/or hydraulic nature.

2. Single-Channel Unrestricted and Restricted Natural Circulation Boiling Studies - W. S. Flinn, P. A. Lottes,
M. Petrick, G. F. Popper, E. A. Spleha,
R. J. Weatherhead

The following is a summary of the results obtained from analyses of tests with a vertical single channel (1/2 x 2 x 60 in.) in the Armadilla test facility.¹⁰

These tests were one of a series of experiments planned for Armadilla to determine the relationship of power density, voids, quality, recirculation flow, pressure and geometry for natural and forced circulation boiling.

Range of Variables for 1/2 x 2 x 60 in. Section

Power Density, kw/liter of active channel	16-49			
Pressure, psig	150	250	400	600
Max. Exit Steam Volume Fraction	0.79	0.72	0.62	0.46
Max. Exit Steam Wgt. Flow Fraction	0.082	0.077	0.059	0.041
Test Section Inlet Water Velocity, fps	1.3-3.05	1.38-3.07	1.38-3.08	1.16-3.03
Inlet Subcooling, F	4.7-23	4.6-24.2	4.9-21	5.2-29

a. Conclusions

The relationship between power density, voids, quality, recirculation flow, pressure and geometry for boiling systems is still not completely understood. At this time the best correlation to show this relationship still appears to be in the form of slip ratio (steam velocity/water velocity) vs. inlet water velocity. This correlation was originally developed

¹⁰ Reactor Engineering Division Quarterly Report, ANL-5511 (January, 1956) p. 74.

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 from the 250-psi boiler data.^{11,12} The relationship (essentially independent of power density, quality, voids, subcooling, or position along the length) obtained from the Armadilla tests checks fairly well with that obtained from the 250-psi boiler data (Fig. 26).

Two-phase frictional pressure drop can be expressed in terms of a friction factor multiplier vs exit voids independent of pressure in the range of the variables tested, as previously postulated by analysis in ANL-5511 (see Fig. 27). Should this relationship be found to exist for other systems with different ranges and combinations of variables, the major obstacle in the evaluation of performance of boiling systems will have been removed.

b. Results

The data are summarized in Table VIII.

The data points shown in the following referenced curves are specific test points and do not represent any averaging, other than for $\bar{\alpha}$, or cross plotting of data. There is a constant boiling length for all data obtained at a given pressure. The per cent boiling lengths for the 150, 250, 400, and 600-psig data are 75, 72, 68, and 64, respectively, for 80F make-up water.

Figure 22 shows the relationship between average voids and power density for 150, 250, 400, and 600-psig, unrestricted natural circulation. Also shown is a comparison between experimental results and calculated predictions for 150 and 600 psig.

Figure 23 shows the relationship between test section inlet water velocity and power density for 150, 250, 400, and 600-psig, unrestricted natural circulation. Also shown is the comparison between experimental and calculated values for 150 and 600 psig.

Figure 24 shows a plot of inlet channel water velocity vs average voids for all unrestricted runs and for all pressures. Figure 25 shows a plot of slip ratio (for the top 1-1/2 ft of the channel) vs velocity for all runs at 150, 250, 400, and 600 psig. Figure 26 shows a comparison between slip ratio-velocity points of Armadilla and the 250-psi boiler for 150, and 250 psig. Figure 27 shows the relationship obtained between friction factor multiplier $\bar{R}$ and α_e based on existing Armadilla data. Also shown for comparison purposes is the predicted $\bar{R}$ vs α_e curve that was previously reported in ANL-5511.

¹¹Ibid.

¹²Reactor Engineering Division Quarterly Report, ANL-5371
 (January 15, 1955) p. 149.

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Table VIII

SUMMARY OF SINGLE CHANNEL RESTRICTED AND UNRESTRICTED
NATURAL CIRCULATION BOILING DENSITY TESTS

Channel Size: 1/2 x 2 x 60 in.

Run No.	Power Density, kw/liter	Inlet Water Velocity, fps v	Average Voids over Active Length, $\bar{\alpha}$	Exit Void Fraction, α_e	Exit Quality, x_e	Slip Ratio Average of Top 19 in., c_s/c_l	Inlet Subcooling, F ΔT_s	Fraction of Boiling Length, L_B/L_T^*
Unrestricted Flow; 150 psig								
123	19.3	3.05	0.241	0.505	0.0129	--	4.9	0.675
124	29.3	2.95	0.314	0.560	0.0219	--	6.2	0.729
125	24.3	2.96	0.268	0.540	0.0166	--	6.22	0.685
126	34.3	2.90	0.353	0.623	0.0268	--	7.0	0.754
141	32.3	2.80	0.337	0.623	0.0212	--	7.65	0.635
142	37.3	2.77	0.342	0.600	0.0290	--	8.9	0.726
143	42.3	2.65	0.388	0.624	0.0324	--	10.12	0.705
144	47.3	2.55	0.405	0.653	0.0386	--	11.4	0.735
145	24.3	2.95	0.288	0.513	0.0167	--	6.12	0.690
146	21.3	2.97	0.244	0.473	0.0138	--	5.3	0.690
147	19.3	2.97	0.234	0.478	0.02012	--	4.72	0.695
205	49.3	2.57	0.449	0.740	0.0417	2.58	11.28	0.745
212	39.3	2.70	0.407	0.690	0.0323	2.25	7.60	0.773
220	29.3	2.88	0.323	0.570	0.0222	2.45	5.70	0.759
229	24.3	2.90	0.294	0.545	0.0182	2.21	4.90	0.750
255	45.3	2.58	0.421	0.676	0.0384	2.65	10.10	0.755
256	42.3	2.62	0.418	0.672	0.0350	2.62	9.35	0.752
Restricted Flow; 150 psig								
206	49.3	2.38	0.461	0.740	0.0456	2.45	12.48	0.746
207	49.3	2.13	0.448	0.720	0.0490	2.75	14.08	0.737
208	49.3	1.83	0.458	0.750	0.0565	3.11	14.98	0.751
209	49.3	1.74	0.482	0.715	0.0625	3.66	16.28	0.755
210	49.3	1.68	0.489	0.790	0.0587	2.73	20.4	0.700
211	49.3	1.30	0.479	0.710	0.0822	4.40	23.1	0.745
213	39.3	2.48	0.422	0.710	0.0351	2.45	8.6	0.766
214	39.3	2.29	0.429	0.690	0.0383	2.61	9.1	0.773
215	39.3	2.10	0.429	0.720	0.0418	2.52	10.3	0.766
216	39.3	1.86	0.387	0.600	0.0455	3.77	12.7	0.744
217	39.3	1.75	0.409	0.643	0.0481	3.74	13.75	0.740
218	39.3	1.55	0.414	0.660	0.0540	3.85	15.40	0.740
219	39.3	1.46	0.425	0.680	0.0555	3.84	17.9	0.715
221	29.3	2.65	0.323	0.590	0.0238	2.48	6.2	0.756
222	29.3	2.53	0.329	0.570	0.0241	2.46	7.7	0.717
223	29.3	2.32	0.339	0.580	0.0270	2.60	8.1	0.730
224	29.3	2.12	0.355	0.600	0.0294	2.68	9.0	0.726
225	29.3	1.85	0.378	0.625	0.0346	2.94	9.15	0.754
226	29.3	1.70	0.387	0.650	0.0374	2.91	10.4	0.744
227	29.3	1.50	0.390	0.660	0.0416	3.21	11.75	0.741
228	29.3	1.40	0.395	0.710	0.0445	3.61	13.1	0.734
230	24.3	2.72	0.296	0.565	0.0200	2.41	5.35	0.744
231	24.3	2.50	0.305	0.560	0.0212	2.45	5.70	0.750
232	24.3	2.28	0.305	0.560	0.0229	2.59	6.35	0.744
233	24.3	2.10	0.325	0.558	0.0246	2.78	7.15	0.735
234	24.3	1.95	0.334	0.573	0.0270	2.78	7.45	0.746
235	24.3	1.66	0.350	0.615	0.0303	3.00	9.25	0.728
236	24.3	1.50	0.354	0.620	0.0313	2.93	9.65	0.732
237	24.3	1.32	0.369	0.663	0.0383	3.15	10.94	0.741

*Based on measured ΔT_s

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Table VIII (Cont'd.)

SUMMARY OF SINGLE CHANNEL RESTRICTED AND UNRESTRICTED
NATURAL CIRCULATION BOILING DENSITY TESTS

Channel Size: 1/2 x 2 x 60 in.

Run No.	Power Density, kw/liter	Inlet Water Velocity, fps v	Average Voids over Active Length, $\bar{\alpha}$	Exit Void Fraction, α_e	Exit Quality, x_e	Slip Ratio Average of Top 19 in., c_s/c_l	Inlet Subcooling, F ΔT_s	Fraction of Boiling Length, L_B/L_T^*
<u>Unrestricted Flow; 250 psig</u>								
148	17.2	2.94	0.183	0.392	0.0121	--	4.72	0.660
149	19.2	2.97	0.186	0.403	0.0135	--	5.1	0.665
150	21.2	3.00	0.195	0.430	0.0195	--	6.22	0.625
151	23.2	3.05	0.211	0.483	0.0152	--	6.6	0.635
152	25.2	3.07	0.230	0.450	0.0164	--	7.1	0.636
180	49.2	2.83	0.315	0.552	0.0387	2.77	12.03	0.706
181	44.2	2.89	0.290	0.520	0.0357	2.71	9.63	0.738
182	39.2	2.94	0.279	0.524	0.0313	2.57	8.5	0.735
188	49.2	2.95	0.379	0.678	0.0389	1.83	11.60	0.717
194	39.2	2.96	0.319	0.550	0.0299	2.10	8.7	0.721
245	45.2	2.87	0.345	0.618	0.03605	2.19	9.85	0.733
246	35.2	2.98	0.295	0.548	0.02655	1.99	7.85	0.717
247	29.2	3.01	0.260	0.508	0.02245	2.12	5.85	0.743
248	17.2	2.95	0.183	0.390	0.01337	1.98	3.65	0.734
249	49.2	2.81	0.333	0.586	0.0401	2.51	11.0	0.733
250	42.2	2.92	0.326	0.580	0.0334	2.30	8.9	0.739
251	32.2	2.99	0.288	0.512	0.0251	2.12	6.3	0.750
252	26.2	3.02	0.255	0.488	0.01973	1.96	5.5	0.730
253	23.2	3.00	0.227	0.442	0.01777	2.04	4.82	0.735
254	20.2	2.99	0.209	0.430	0.01502	1.86	4.62	0.710
257	47.2	2.86	0.3318	0.570	0.0380	2.75	10.5	0.734
258	45.2	2.90	0.3288	0.550	0.0374	2.55	9.46	0.749
259	43.2	2.93	0.3329	0.555	0.0354	2.35	8.75	0.753
260	41.2	2.97	0.3417	0.600	0.0338	2.20	8.18	0.758
<u>Restricted Flow; 250 psig</u>								
189	39.2	2.36	0.332	0.580	0.0381	2.53	10.5	0.732
190	49.2	2.28	0.388	0.694	0.0493	2.36	13.8	0.728
191	49.2	2.01	0.387	0.642	0.0556	2.72	15.6	0.726
192	49.2	1.71	0.430	0.720	0.0655	2.61	18.6	0.725
193	49.2	1.42	0.405	0.690	0.0768	3.12	24.2	0.705
195	39.2	2.43	0.345	0.554	0.0364	2.30	10.7	0.720
196	39.2	2.02	0.358	0.616	0.0436	2.48	12.9	0.718
197	39.2	1.74	0.368	0.650	0.0512	2.60	14.3	0.730
199	39.2	1.58	0.363	0.608	0.0535	3.01	15.5	0.721
201	29.2	2.30	0.283	0.520	0.0286	2.18	8.7	0.721
202	29.2	2.02	0.297	0.563	0.0326	2.31	9.8	0.713
203	29.2	1.65	0.318	0.573	0.0407	2.74	11.4	0.731
204	29.2	1.38	0.332	0.603	0.0464	2.83	14.5	0.707

*Based on measured ΔT_s

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Table VIII (Cont'd.)

SUMMARY OF SINGLE CHANNEL RESTRICTED AND UNRESTRICTED
NATURAL CIRCULATION BOILING DENSITY TESTS

Channel Size: 1/2 x 2 x 60 in.

Run No.	Power Density, kw/liter	Inlet Water Velocity, fps v	Average Voids over Active Length, $\bar{\alpha}$	Exit Void Fraction, α_e	Exit Quality, x_e	Slip Ratio Average of Top 19 in., c_s/c_l	Inlet Subcooling, F ΔT_s	Fraction of Boiling Length, L_B/L_T^*
<u>Unrestricted Flow; 400 psig</u>								
238	19.0	2.87	0.155	0.350	0.01589	1.82	4.85	0.695
239	49.0	3.00	0.275	0.530	0.0388	2.01	12.35	0.686
240	44.0	3.03	0.258	0.510	0.03575	2.05	10.00	0.712
241	39.0	3.05	0.252	0.490	0.0321	1.99	8.50	0.724
242	34.0	3.06	0.227	0.460	0.0264	1.83	8.44	0.684
243	29.0	3.01	0.192	0.400	0.0232	2.07	7.10	0.693
244	24.0	2.98	0.173	0.340	0.0191	1.93	6.16	0.682
261	47.0	3.01	0.276	0.544	0.0356	1.74	12.52	0.665
262	42.0	3.06	0.261	0.520	0.0304	1.61	11.7	0.641
263	37.0	3.09	0.233	0.462	0.02755	1.85	9.5	0.668
264	31.0	3.08	0.208	0.422	0.0241	1.86	7.3	0.696
267	39.0	3.03	0.250	0.544	0.0297	1.75	10.2	0.669
<u>Restricted Flow; 400 psig</u>								
265	31.0	1.38	0.284	0.550	0.0485	2.29	19.5	0.635
266	31.0	1.70	0.260	0.492	0.0400	2.11	15.6	0.595
268	39.0	2.54	0.274	0.548	0.0372	1.87	11.2	0.697
269	39.0	2.27	0.266	0.536	0.0401	2.07	13.1	0.680
270	39.0	1.88	0.295	0.568	0.0491	2.15	15.8	0.682
271	39.0	1.47	0.326	0.616	0.0589	2.20	20.9	0.661

*Based on measured ΔT_s

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Table VIII (Cont'd.)

SUMMARY OF SINGLE CHANNEL RESTRICTED AND UNRESTRICTED
NATURAL CIRCULATION BOILING DENSITY TESTS

Channel Size: 1/2 x 2 x 60 in.

Run No.	Power Density, kw/liter	Inlet Water Velocity, fps v	Average Voids over Active Length, $\bar{\alpha}$	Exit Void Fraction, α_e	Exit Quality, x_e	Slip Ratio Average of Top 19 in., c_s/c_d	Inlet Subcooling, F ΔT_s	Fraction of Boiling Length, L_B/L_T^*
<u>Unrestricted Flow; 600 psig</u>								
113	28.9	2.88	0.134	0.346	0.022	--	9.3	0.599
114	28.9	2.93	0.120	0.310	0.0219	--	9.15	0.601
115	33.9	3.00	0.149	0.368	0.025	--	10.65	0.599
118	23.9	2.68	0.126	0.342	0.0180	--	8.3	0.599
119	15.9	2.67	0.083	0.184	0.0119	--	6.6	0.540
120	18.9	2.75	0.109	0.290	0.0147	--	6.62	0.585
121	20.9	2.80	0.102	0.298	0.0163	--	6.82	0.602
128	20.9	2.72	0.105	0.294	0.01625	--	7.72	0.572
130	25.9	2.82	0.117	0.284	0.0193	--	8.92	0.580
135	25.9	2.82	0.122	0.270	0.0194	--	8.30	0.595
136	28.9	2.87	0.127	0.330	0.0231	--	8.8	0.628
137	33.9	2.95	0.150	0.362	0.0261	--	10.5	0.612
153	28.9	2.90	0.129	0.318	0.0236	2.06	8.6	0.634
154	38.9	2.95	0.162	0.372	0.0316	2.01	10.66	0.653
155	43.9	2.98	0.165	0.386	0.0344	2.15	12.4	0.636
156	48.9	3.0	0.166	0.422	0.0384	2.19	13.45	0.641
157	36.9	2.98	0.144	0.410	0.0286	1.97	9.92	0.643
158	27.9	2.92	0.128	0.336	0.0227	2.03	7.92	0.643
159	24.9	2.78	0.122	0.304	0.0198	1.84	7.15	0.639
164	28.9	2.92	0.132	0.308	0.0237	1.91	8.4	0.643
169	38.9	3.0	0.152	0.300	0.0307	2.18	10.92	0.638
174	16.9	2.63	0.098	0.243	0.0153	1.91	5.2	0.649
175	18.9	2.70	0.093	0.248	0.0169	2.09	5.7	0.652
176	22.9	2.78	0.108	0.263	0.0195	2.02	7.1	0.630
177	30.9	2.87	0.127	0.282	0.0250	2.21	9.22	0.630
178	40.9	2.96	0.167	0.385	0.0334	2.25	11.02	0.655
179	45.9	2.99	0.156	0.370	0.0384	2.43	11.42	0.680
183	48.9	3.03	0.170	0.370	0.0412	2.54	13.76	0.638
<u>Restricted Flow; 600 psig</u>								
165	28.9	2.56	0.148	0.324	0.0241	1.78	9.25	0.651
166	28.9	2.0	0.152	0.338	0.0328	2.24	13.2	0.610
167	28.9	1.64	0.173	0.390	0.0328	2.23	16.1	0.614
168	28.9	1.33	0.181	0.420	0.0492	2.58	20.9	0.604
170	38.9	2.53	0.172	0.360	0.0363	1.88	13.12	0.635
171	38.9	2.10	0.195	0.410	0.0441	2.18	16.0	0.635
172	38.9	1.46	0.227	0.456	0.0634	2.62	22.9	0.638
173	38.9	1.16	0.229	0.516	0.0791	3.22	28.7	0.637
184	48.9	2.62	0.185	0.440	0.0436	2.24	16.36	0.626
185	48.9	2.08	0.210	0.460	0.0549	2.50	19.76	0.640
186	48.9	1.85	0.211	0.440	0.0628	2.72	22.02	0.642
187	48.9	1.53	0.224	0.448	0.0790	3.16	25.26	0.667

*Based on measured ΔT_s

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FIG. 22
AVERAGE STEAM VOLUME FRACTION AS A
FUNCTION OF POWER DENSITY AND PRESSURE
FOR $1/2 \times 2 \times 60$ in. VERTICAL HEATED CHANNEL

FIG. 23
INLET VELOCITY AS A FUNCTION OF POWER
DENSITY AND PRESSURE FOR $1/2 \times 2 \times 60$ in.
VERTICAL HEATED CHANNEL

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FIG. 24
INLET VELOCITY AS A FUNCTION OF AVERAGE
VOIDS AND PRESSURE FOR $1/2 \times 2 \times 60$ in.
VERTICAL HEATED CHANNEL
(UNRESTRICTED FLOW)

FIG. 25
SLIP RATIO VS INLET VELOCITY
AT UPPER SECTION OF CHANNEL

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FIG. 26
 CORRELATION OF ARMADILLA DATA
 WITH 250 psig BOILER DATA

FIG. 27
 TWO-PHASE BOILING FRICTION FACTORS FOR
 1/2 x 2 x 60 in. VERTICAL HEATED CHANNEL

c. Discussion of Results

(1) Pressure Drop Measurements

The original design of the channel test section did not provide for pressure drop instrumentation; consequently, pressure drop measurements were made using non-conventional means. The measurements included an entrance loss, a two-phase exit loss and a turbulence loss in a short section of chimney. The downstream pressure tap was a pitot static-type instrument projecting into the fluid stream. The angle between stream lines and pressure tap hole was 35 degrees. This position was chosen to represent static pressure based on pressure traverses made around the pitot tube.

The data shown in Fig. 27 may be slightly high or low depending upon possible errors in exit losses. These exit losses are quite high at volume fractions of 40% and above, and the values of two-phase friction drop are directly affected by errors in calculated exit loss. Further tests are to be made using more direct and thorough methods for measuring pressure drop.

Figure 27 demonstrates that two-phase friction factors during boiling in a vertical channel can be correlated as a function of exit steam volume fraction independent of pressure over the range tested. The data are higher than the adjusted Martinelli-Nelson curve reported in ANL-5511. Again, this difference may be due to uncertainties in exit losses during the tests. The curves in Fig. 27 are based on drawn tubing, single-phase friction factors. The adjusted Martinelli-Nelson curve has been modified by a factor of 1.2 from the values reported in ANL-5511, since this original presentation was based on a single phase-friction factor 1.2 times the drawn tubing values.

(2) Analytical Method of Calculating Natural Circulation Boiling Performance

The general method that has been used in calculating performance was based on the following equations and curves.

It was assumed that the flow equation for a natural circulation loop may be written as

$$\Delta P_T = \Delta P_f + \Delta P_a + \Delta P_h + \Delta P_L \quad ,$$

where

ΔP_T = total pressure drop

ΔP_h = hydrostatic head

ΔP_f = frictional pressure drop

ΔP_L = entrance, exit losses and external loop losses.

ΔP_a = acceleration pressure drop

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Values of frictional pressure drop may be calculated from the friction factor data shown in Fig. 27.

Values of acceleration pressure drop may be determined from the relation

$$\Delta P_a = R \frac{G^2}{g}$$

Values of R are shown in Fig. 28. Hydrostatic head is determined from the relation

$$\Delta P_h = \left(\frac{\bar{\rho}}{\rho_f} \right) \rho_f L_B$$

Calculated values of $(\bar{\rho}/\rho_f)$ are shown in
(a) Figs. 29 and 30 for constant heat flux, constant slip ratio; and
(b) Fig. 31 for cosine heat flux and constant slip ratio.

Fig. 28
Acceleration Multiplier (R) vs Exit Steam Volume Fraction at 600 psia

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FIG. 29
RATIO OF MEAN DENSITY IN VERTICAL BOILING
CHANNEL TO LIQUID DENSITY FOR UNIFORM HEAT
GENERATION AND SLIP RATIO OF UNITY

FIG. 30
RATIO OF MEAN DENSITY IN VERTICAL BOILING
CHANNEL TO LIQUID DENSITY FOR UNIFORM
HEAT GENERATION FOR PRESSURES OF 500
AND 2000 psia AND SLIP RATIOS FROM 1 TO 3

Fig. 31
Mean Density Ratio in Boiling Section of Vertical
Channel as a Function of Boiling Length Ratio at 600 psia

(3) Correlation of Variables

Correlation of data in the form of slip ratio vs inlet velocity gave fairly good results (Figs. 25 and 26). The variation in slip ratio along the top 19-inch portion of the section can be seen in Fig. 25.

With reference to Fig. 26, it is interesting to note that Armadilla data agreed rather well with the 250-psi boiler data even though the flow cross section and peripheral heat flux distribution were different. The flow cross section in Armadilla is $1/2 \times 2$ in., while the boiler section was $1/2 \times 3-1/2$ in. The Armadilla section has equal heat flux at the $1/2$ -in. edge surfaces and the 2-in. side surfaces with indeterminately higher local heat fluxes in the very corners of the channel. The boiler section essentially only has a heat flux over the $3-1/2$ -in. side surfaces.

In the 2-1/2 - 3-fps region, the slip ratio data seems to be rising although a definite trend is not obvious. This is the region where the boiler data seemed to indicate a hump or instability. In general, scatter is greater for the restricted or higher subcooled runs. The scatter of Armadilla data near 2.9 fps at 250 psig cannot be explained as yet unless this is that transition effect. This velocity region will be more completely covered as part of the forced circulation test program.

(4) Comparison of Calculated and Measured Performance

As is evident in Figs. 22 and 23 there is close agreement between calculated and measured performance. The slight deviation at 150 psig (Fig. 22) is believed to be due to errors in the assumed friction factors (Fig. 27). As mentioned previously, additional tests are planned to obtain more accurate two-phase friction factors.

3. Direct Linear Recording of Void Measurements - H. H. Hooker

Void fraction determinations in boiling steam-water mixtures are currently being made by measuring the variation in gamma-ray attenuation through the mixture. Assuming exponential attenuation of the gamma rays, the following relation holds:

$$\alpha = \frac{\log \left(\frac{v_1}{v_{1f}} \right)}{\log \left(\frac{v_{1e}}{v_{1f}} \right)}, \quad (1)$$

where

α = void fraction

v_1 = a voltage proportional to the radiation falling on the radiation detector

v_{1f} = a constant equal to v_1 measured with zero void fraction, or the "hot full" measurement

v_{1e} = a constant equal to v_1 measured with unit void fraction, or the "hot empty" measurement.

Void fraction is, then, a linear function of $\log v_1$. If the values of v_1 for zero and unit void fraction are known, intermediate values of void fraction may be obtained from a linear plot of void fraction versus v_1 on semilogarithmic coordinates. This operation must be performed for each data point.

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With a view toward eliminating the necessity for a computation of logarithms in void fraction determinations, equipment has been constructed which extracts the logarithm electrically. A constant bucking voltage, which yields zero output for zero void fraction, is next added. Finally, the resulting voltage is amplified to produce full-scale deflection of a recording instrument for unity void fraction. The following relation then holds:

$$\alpha = \theta \quad , \quad (2)$$

where θ is the recording instrument deflection and is unity at full scale.

As implied above, two adjustments must be made to the logarithmic equipment depending upon the particular experimental conditions: (1) the bucking voltage is set so that zero instrument deflection is produced with the test section filled with water (zero void fraction); and (2) amplifier gain is adjusted to produce full-scale deflection with the test section filled with steam (unit void fraction).

Figure 32 shows a comparison of void data determined by linear recording plus graphical extraction of logarithms with that obtained simultaneously using the logarithmic amplifier. These data are typical of those obtained with the Armadilla test facility. It should be noted that it was not practical to obtain hot-empty readings immediately prior to making test runs with this facility; therefore these values were obtained necessarily from previous tests at the same pressure. The unexplained variation of these hot-empty values from day to day resulted in reduced accuracy of void data.

While the logarithmic amplifier suffers from a loss of accuracy as applied to the Armadilla test facility, it should be applicable in situations where the hot-empty and hot-full settings can be made as based on measurements made immediately prior to test runs. Under these conditions the labor involved in making many void fraction determinations is greatly reduced with no sacrifice in accuracy.

4. Evaluation of Some Sources of Error in Steam Void Measurement - H. H. Hooker

As part of a continuing investigation of the accuracy of gamma-ray void measurements, certain sources of error are evaluated below.¹³

¹³Reactor Engineering Division Quarterly Report, ANL-5462
(April 15, 1955) p. 6.

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FIG. 32
COMPARISON OF VOID DATA OBTAINED
BY GRAPHICAL METHOD AND BY
DIRECT RECORDING METHOD

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a. Effect of Extraneous Parallel Radiation Paths

Let a fixed amount of radiation reach the detector by some path not passing through the steam-water mixture so that every measurement of ϕ (the radiation reaching the detector) is increased by some amount $\Delta\phi$. Then, since void fraction determinations are based on the relation

$$\alpha = \frac{\log\left(\frac{\phi}{\phi_f}\right)}{\log\left(\frac{\phi_e}{\phi_f}\right)},$$

an apparent void fraction will result which is

$$\alpha' = \frac{\log\left(\frac{\phi + \Delta\phi}{\phi_f + \Delta\phi}\right)}{\log\left(\frac{\phi_e + \Delta\phi}{\phi_f + \Delta\phi}\right)},$$

where

α = void fraction

α' = apparent void fraction

ϕ = radiation falling on detector

ϕ_f = ϕ measured when test vessel is filled with water

ϕ_e = ϕ measured when test vessel is filled with steam.

Letting

$$\phi_e/\phi_f = a$$

and

$$\Delta\phi/\phi_f = b,$$

the apparent void fraction becomes

$$\alpha' = \frac{\log\left(\frac{a\alpha + b}{1 + b}\right)}{\log\left(\frac{a + b}{1 + b}\right)}$$

In Fig. 33, α' is plotted as a function of α for three pairs of values of a and b .

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Fig. 33
Apparent α' vs True α with Parallel
Path Error Under Various Conditions

b. Effect of an Error in Measurement of ϕ_e

Void fraction determinations at the Armadilla facility are based on frequent measurements of ϕ_f and relatively infrequent measurements of ϕ_e . For this reason an analysis of the effect on void fraction accuracy of an error in ϕ_e was made.

Assume that, instead of a true value, ϕ_e , an erroneous value, ϕ'_e , is being used. As a result, the apparent void fraction is

$$\alpha' = \frac{\log \frac{\phi}{\phi_f}}{\log \frac{\phi'_e}{\phi_f}}$$

Since

$$\alpha = \alpha' \left(\frac{\alpha}{\alpha'} \right),$$

the correction factor is then,

$$\frac{\alpha}{\alpha'} = \frac{\log \frac{\phi'_e}{\phi_f}}{\log \frac{\phi_e}{\phi_f}}.$$

This correction factor is plotted as a function of $\frac{\phi'_e}{\phi_e}$ in Fig. 34 for various values of $\frac{\phi_e}{\phi_f}$ (the apparent empty-to-full ratio).

5. Exponential Heating Power Supply - H. H. Hooker

In order to experimentally determine the physical response of reactor components during a reactor power excursion having a positive period, it is desirable that a heating power source be available whose output is of the form

$$P = P_0 \exp (t/\tau),$$

where

P = power dissipated in the component as a function of time

P_0 = power dissipation at zero time

t = time

τ = a constant defined as the period.

Because of the relative ease with which it can be applied, electrical heating naturally suggests itself. However, periods of the order of a few milliseconds and less are desired as well as maximum power levels of several hundred kilowatts. It follows that if the above function is to be accurately reproduced using a power source capable of sustained output at the maximum power level, large and expensive equipment would be required.

As a result, an investigation and test program have been undertaken to determine the feasibility of using capacitor banks as a stored energy source. It is proposed that a power transient having a duration of four periods be divided into 20 intervals, during each of which one of 20 capacitor banks is discharged through the load. If each capacitor bank is initially charged to the proper voltage, the resulting power function is a sawtooth approximation of the ideal function. The required capacitance per bank is

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FIG. 34
CORRECTION FACTOR FOR ERROR IN α
DUE TO ERROR IN Φ_e FOR VARIOUS
APPARENT EMPTY-TO-FULL RATIOS

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$$C = \frac{-2 \Delta t}{R \ln(1-A)}$$

where

C = capacitance per bank, farads

Δt = duration of one interval, seconds

R = resistance of load, ohms

A = fraction of stored energy removed during interval of discharge.

Equating the energy dissipated per capacitor discharge interval to the energy dissipated during the same interval of an ideal exponential power rise yields

$$v_{co} = \sqrt{\left[\frac{2P_0 \tau \exp(t_n/\tau)}{C} \right] \left[\frac{\exp[(\Delta t/\tau) - 1]}{1 - \exp(-2 \Delta t/RC)} \right]} \quad \text{volts,}$$

where

v_{co} = initial potential to which a capacitor bank is charged

t_n = time at which a given capacitor bank begins its discharge.

The required switching between capacitor banks and the load is to be accomplished with ignitron tubes, whose firing is to be determined by an electronic timing device. A simplified diagram of such a system is shown in Fig. 35. An approximate plot of power relations during the transient is shown in Fig. 36.

The following specifications apply to the proposed power supply:

Load resistance	0.01877 ohms
Power period	0.01 sec
Duration of transient	0.04 sec (four periods)
Power at 0.04 sec	585 kw (in the ideal transient case)
Initial power at zero time	10.7 kw
Capacitance per bank	0.154 farad
Total capacitance for 20 banks	3.08 farad

A single capacitor bank, with its associated ignitron and firing controls, has been assembled. Tests with a view toward establishing the feasibility of, and optimizing, the mechanical and electrical design before proceeding with construction of the balance of the apparatus are in progress.

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FIG. 35
SIMPLIFIED CIRCUIT DIAGRAM FOR
EXPONENTIAL TRANSIENT APPROXIMATION

FIG. 36
IDEAL POWER TRANSIENT SHOWN WITH
ONE STEP OF CAPACITOR DISCHARGE
APPROXIMATION SUPERIMPOSED

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B. Fuel and Cladding Materials - L. W. Fromm

1. Zircaloy 2 - Clad Uranium - 5% Zirconium Plates -
C. R. Breden

Table IX summarizes the series of tests conducted to observe the corrosion behavior of Zircaloy 2-clad uranium-zirconium plates in the absence of the niobium constituent.¹⁴

Based upon the test history and appearance, it is concluded that the corrosion behavior of the plates after clad penetration is more catastrophic in nature than that of the reference EBWR alloy (uranium-5% zirconium-1.5% niobium). The greater difficulty experienced in fabrication of the former lends further justification to the selection of the latter reference alloy.

Table IX

SUMMARY OF CORROSION TESTS ON ZIRCALOY 2-CLAD
URANIUM-ZIRCONIUM PLATES IN 550F DEGASSED WATER

Plate No.	% Zr	Total Test Time, hr	Comments
153 T	5	70	Large blister at end, Zircaloy-Zircaloy bond.
154 T	5	70	Warped.
155 T	5	70	Warped.
158 T	5	70	Ruptured.
187 T	7½	42	Ruptured after 20 hr.
188 T	7½	615	White area on both sides. A few small blisters near end.
189 T	10	615	White area on both sides.
190 T	10	615	White areas on both sides; slight warpage.

¹⁴Reactor Engineering Division Quarterly Report, ANL-5511
 (January, 1956) p. 88.

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2. Fuel Plate End Closures - C. R. Breden

Three reference alloy fuel plate end enclosures with the heliarc welded bond added after induction welding are still intact after 2976 hr in static, degassed water at 550F.

3. Unclad High-Uranium Alloys - C. R. Breden

The long-term corrosion test data on samples of high-uranium alloys are listed in Table X.

Table X

CORROSION OF UNCLAD HIGH-URANIUM ALLOYS IN 500F WATER

Sample No.	Expt. No.	Alloy Comp., U Plus %	Heat Treatment ⁽²⁾	Water Cond. ⁽³⁾	Rate of Wt. Change ⁽⁴⁾ mg/(cm ²)(mo)	Time to Disintegration ⁽⁵⁾ hr	Appearance
A-4065 ⁽¹⁾	U-64	6% Nb	2012F quenched	Deg		>12,301	After 9371 hr, three cracks appeared in top edge of can. After 10,083 hr, cracks were enlarged and the top end showed a slight bulge. After 11,512 hr, cracks and bulge were slightly enlarged. After 12,301 hr, a new bulge was evident.
A-4114a	U-81	3.8% Si	850C - 7 days	Deg	- 21.8	> 9,230	Rate is for last period. Slight loose black coating. Crack evident.
A-4297h	U-313	3% Nb-0.75% Sn	800C - 4 min; WQ. 350C in vacuum 6 hr; air cooled.	Deg	-156	> 1,681	Loose smooth black coating.
A-4118	U-85	1½% Nb-5% Zr	1470F quenched	Deg	- 82.6	>11,564	Adherent rough black coating.
A-4207a	U-217	1½% Nb-5% Zr	As cast. Quenched at 850C.	Deg	- 86	> 2,684	Adherent black coating. Pitting generally increased; some spalling.
A-4207c					- 92		
A-4207d					- 93		
A-4207e					- 97		
A-4207f					- 86		
A-4207g					- 84		
A-4207h					-102		
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	-400	> 2,049	Adherent black coating. Many small pits.

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

(2) WMQ = Woods Metal Quench; FC = Furnace Cooled; WQ = Water Quench.

(3) Water Condition: Deg = Degassed

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

(5) Symbol (>) denotes accumulated test time; test still in progress.

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4. Aluminum Alloys

a. High-Temperature Water Corrosion - G. A. Reed

Approximately 150 samples of aluminum-nickel (M-388), aluminum-copper (X-2219), and aluminum-nickel-iron (M-400) were tested at various temperatures, hydrogen gas concentrations, pH, and velocities. The corrosion data represent an average taken from at least three electrochemically stripped samples. In all cases the stripped weight losses of the averaged samples did not vary more than 5%.

The inconsistencies in aluminum-nickel corrosion at 500F, i.e., the marked difference between corrosion rates of originally loaded samples and replacement samples in water at higher velocities have not been resolved. The enigma is enhanced by the following observations: (a) at 500F the corrosion rates of originally loaded and replacement samples in semi-static water are similar; (b) at 600F there is no difference between the corrosion rates of original and replacement samples in either high-velocity or semi-static water.

Loop No. 1 (600F, pH 5.5) - Originally Loaded Samples:

Primarily, aluminum-nickel alloy (M-388) was tested in this loop using cation bed pH control. The corrosion product hydrogen was allowed to build up at will; monitoring showed a buildup of from 0 to 210 cc H₂/liter in the first two-week period, and from 0 to 100 cc H₂/liter in each of the subsequent three periods. There was little or no difference between the stripped corrosion weight losses of originally loaded and replacement samples under these loop conditions. The corrosion rates (see Fig. 37) and oxide scale thicknesses at the end of eight weeks were as follows:

<u>Water Velocity</u>	<u>Corrosion, mil/yr</u>	<u>Scale Thickness, in.</u>
18 fps	6.0	0.0021
7 fps	4.7	0.0018
Semi-static	2.8	0.0016

Some cracking off and flaking of the oxide scale was evident on the samples exposed to high-velocity water (Fig. 38).

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Fig. 37

Corrosion of Aluminum-Nickel Alloy (M-388) in 600F
Water (pH = 5.5) - Hydrogenation Uncontrolled

Loop No. 3 (500F, pH 6.5 - 7.0) - Originally Loaded Samples:

This loop had completed six weeks of its scheduled eight-week test period during the previous quarter.¹⁵ The final corrosion rates established for aluminum-nickel alloy (M-388) in the eight-week period were as follows:

Water Velocity	Corrosion mil/yr
16 fps	2.0
6 fps	1.5
Semi-static	1.5

¹⁵ Reactor Engineering Division Quarterly Report, ANL-5511
(January, 1956) p. 92.

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FIG. 38
AS-REMOVED SAMPLES AFTER EXPOSURE
IN 600 F WATER (pH = 5.5)

The gas concentration varied from zero at the start of the first two-week period to 330 cc H₂/liter. The hydrogen production during subsequent two-week periods between shutdown ranged from 0 to 70 cc H₂/liter.

Loop No. 3 (600F, pH 7.0) - Originally Loaded Samples:

This loop was operated using added bottled hydrogen gas at approximately 200 cc/liter during the four 2-week test periods. Little difference was noted between the corrosion rates of the originally loaded and the replacement samples of aluminum-nickel alloy (M-388). The corrosion rates (see Fig. 39) for the originally loaded specimens and oxide scale thicknesses at the end of the eight weeks were as follows:

<u>Water Velocity</u>	<u>Corrosion, mil/yr</u>	<u>Scale Thickness, in.</u>
16 fps	8.5	0.0020
6 fps	6.5	0.0020
Semi-static	5.5	0.0020

Fig. 39

Corrosion of Aluminum-Nickel Alloy in 600F Water
(pH = 7.0) with Hydrogen Addition (160 - 300 cc/liter)

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The oxide scale on the samples in 6 fps and 16 fps water was scabby and ruptured at the end of eight weeks; some of the scale had been washed away (Fig. 40).

It is believed that the corrosion rates for aluminum-nickel (M-388) at 600F with the presently used water treatment are too high to be acceptable. Furthermore, the large amount of corrosion product scale, which tends to wash off at high water velocities, will present a difficult filtration problem.

Loop No. 4 - Degassed, 500F, pH = 7.0

Loop No. 4 was operated to study the inconsistencies in aluminum-nickel corrosion in 500F water.

In test loops containing 500F water (pH = 7.0) at 18 fps, and where evolved hydrogen gas has been allowed to build up, the nominal corrosion rate has been about 2 mil/yr [Curve (b), Fig. 41].

In the degassed loop the corrosion rate was about 11 mil/yr [Curve (a), Fig. 41]. The surfaces of the samples had scaled, ruptured, and sloughed off considerably (Fig. 42).

b. Boiling Water Corrosion - G. A. Reed

Boiling water corrosion tests are being conducted on aluminum-nickel (M-388) alloy in a "figure 8" heat transfer loop. Loop No. 2 is used as a heat source of 550F, 1800-psi water which flows up the inside of a vertical, double-walled tube of stainless steel (for pressure containment) with a drawn-on jacket of aluminum-nickel. The tube is exposed to boiling water in a pressurized autoclave. Pressure is maintained by a pressure regulator, closed condenser coil in the steam dome. The boiling water refluxes in the vessel with intermittent cleanup loop operation.

The loop has been operated approximately 400 hr at 300 psig and 422F, with an average heat flux of 25,000 Btu/(hr)(sq ft). The heat flux varies from approximately 15,000 to 40,000 Btu over the length of the tube. After 160 hr of operation the weight change was +2.5 mg/cm². The increase in the diameter of the 0.5-inch rod with the corrosion product scale was 0.0008 inch, and the scale was of the smooth adherent type. The effect of the corrosion product scale on heat transfer is as follows:

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FIG. 40
AS-REMOVED SAMPLES AFTER EXPOSURE IN 600 F WATER
(pH = 7.0) AVERAGE H₂ CONCENTRATION: 200 cc/liter

Fig. 41

Corrosion of Aluminum-Nickel Alloy Exposed to 500F Water (pH = 7.0) at 18 fps in (a) Degassed Loop and (b) Loop with Allowed Buildup of Evolved Hydrogen

Operating Time, hr	Mean Average ΔT at 25,000-Btu Heat Flux, F
Clean-new	40
3	42
7	59
10	69
15	60
17	58
177	58
388	59

This test is continuing.

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FIG. 42
SCALED AND SLIGHTLY PITTED SURFACES OF
Al-Ni (1 WT-%) ALLOY AFTER 399 hr IN 500 F
DEGASSED WATER (pH=7.0) AT 18 fps.
(LOOP NO. 4)

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Comparative Corrosion of Aluminum Alloys

The corrosion loop operations described above included tests of numerous samples of aluminum-copper and aluminum-nickel-iron. There appears to be little difference in corrosion behavior between aluminum-nickel and aluminum-nickel-iron. Aluminum-copper (Alcoa X-2219) has approximately the same corrosion rate, but shows small surface pits under some water conditions; however, no pitting was observed in hydrogenated 600F water (pH = 7.0). In addition, aluminum-copper loses its outer oxide scale in flowing water and leaves only a thin adherent scale.

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

Samples of 2S aluminum, spot welded to simulate BORAX-III assembly welds, are still satisfactory after tests totaling 1928 hr in 420F degassed water.

d. Corrosion-Irradiation of Aluminum-Nickel Alloy Plates - N. R. Grant, R. A. Granack.

Sample plates of 1/2%,* 1%,** and 2%** nickel-aluminum alloy were tested in the Argonne Water Loop at MTR for a total of 850 hr under irradiation between 400F and 500F. In addition, the samples received 75 hr of operation in the same temperature range without irradiation (reactor shutdown) and 400 hr below 300F, some of this with MTR process water cooling. The velocity of the water flowing past the samples was about 30 fps, and the pH ranged from 6.8 to 7.2. The in-reactor plates received an estimated integrated thermal flux of 1×10^{21} at the higher flux end. The upper limit for fast flux exposure was less than 5×10^{19} . This is equivalent to about 0.2% burnup of all the core atoms in a natural uranium fuel plate.

Identical plates were exposed to the same flowing water, without irradiation, in the external test facility of the loop.

The plates were stripped of scale and cleaned electrolytically in a saturated boric acid solution. As evidenced by the surface appearance and weight changes (Figs. 43 and 44), the out-of-reactor samples suffered greater corrosion attack than their counterparts in the reactor. The relative roughness of the surfaces correlates with the weight loss. Figure 45 shows areas of localized corrosion attack on the in-pile samples.

* air cast

** vacuum cast

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FIG. 43
 REPRESENTATIVE VIEWS OF OUT-OF-REACTOR SAMPLES OF
 ALUMINUM-NICKEL ALLOYS. (CARTRIDGE NO. MTR-W-6-E)
 (X7)

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REGION OF
HIGH FLUX

FLOW

ALLOY	WT. LOSS, mg/cm ²	POSITION	ALLOY	WT. LOSS, mg/cm ²
Al-2% Ni	3.11	TOP	Al-2% Ni	5.58
Al-1% Ni	3.61	CENTER	Al-1% Ni	5.28
Al-.5% Ni	4.71	BOTTOM	Al-.5% Ni	5.02

Al-2% Ni

Al-1% Ni

Al-.5% Ni

FIG. 44
REPRESENTATIVE AREAS OF IN-REACTOR ALUMINUM-NICKEL PLATES (X7)

100

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(a)

END CAVITATION ON LEADING
EDGE. (Al-2% Ni)

(b)

PITTING AT CLAMP
(Al-1% Ni)

(c)

ATTACK BEHIND CLAMP
(Al-0.5% Ni)

FIG. 45
LOCALIZED CORROSION ATTACK ON IN-
PILE ALUMINUM-NICKEL ALLOYS. (X7)

. 02

The corrosion behavior of the in-reactor samples is similar to that of samples tested at Argonne in 500F neutral water in which hydrogen has been allowed to build up to 200-500 cc/liter. The behavior of the out-of-reactor samples is similar to that of samples tested in 500F neutral, degassed water. The warping of the plates is probably due to the difference in linear expansion of the aluminum-nickel and stainless steel, since the sample plates were clamped tightly at both ends.

The decreased corrosion in the presence of irradiation was observed previously.¹⁶

5. Urania-Thoria Bodies - R. A. Granacki, R. J. Schiltz

a. Solution Tests

Samples of Norton thoria, 10% urania-thoria, and 2.5% urania (fully enriched) - thoria have accumulated up to 4566 hr exposure to static, degassed, demineralized water at 600F.¹⁷ No significant changes have been observed.

b. Solution-Irradiation Tests

Irradiation testing of urania-thoria fuel specimens supplied by the Argonne Metallurgy Division has continued in the CP-5 autoclave facility. Run No. 20 was completed.

The specimen consisted of four ceramic pellets (0.25 in. diameter x 0.50 in. long) jacketed in a capsule of 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 were 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 500 - 515F for 1630 hr at full CP-5 power, which resulted in a calculated burnup of 3.16% of total uranium atoms and 0.104% burnup of total core atoms. No pressure buildup was noted during the run;

¹⁶Reactor Engineering Division Quarterly Report, ANL-5461 (April 15, 1955) p. 62.

¹⁷Reactor Engineering Division Quarterly Report, ANL-5511 (January, 1956) p. 100.

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however, a leak corresponding to a loss in helium pressure of 5 psi per week developed in the system during the first week of operation. The leak may have masked a pressure buildup which otherwise would have occurred.

During the final 470 hr of operation, occasional unexplained fluctuations in pressure were noted. These fluctuations took the form of a decrease in pressure of 20 to 40 psi in 45 minutes and an accompanying temperature drop of 10 to 15F, followed by operation at this level until the next reactor scram. Upon resumption of reactor operation, system temperature and pressure returned to the former values. No corresponding fluctuations were detected in any other Reactor Engineering Division autoclave or facility operating in CP-5 at the time.

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

The capsule was examined in the "as received" condition and after partially dissolving the aluminum-nickel capsule in 5% sodium hydroxide.

As shown in Fig. 46, the "as-removed" capsule was coated with a heavy deposit of iron oxide. Noticeable distortion, and growth of 0.25 in. in length, had occurred [Fig. 46 (a)]. The jacket had ruptured in several places and the lead bond was visible in the largest rupture (b). Ruptures occurred in areas corresponding to spaces between successive pellets. A lead plug (c) approximately 1/4 in. by 1/4 in. was found between pellets Nos. 1 and 2, and was judged responsible for the pronounced bulge in view (a). There was a decrease in capsule diameter at the juncture of pellet Nos. 3 and 4, which showed a tendency toward sub-surface rupturing. A ridge beginning at the flat end of the capsule and terminating at pellet No. 1 was found to contain lead.

The deformation of the capsule is believed to have been the result of collapse under the system pressure at the operating temperature where the metal strength is low. Buckling of the capsule wall caused the ridge, and the blister or bulge resulted from lead being squeezed out of the annulus between capsule wall and the pellets.

Examination after partial dissolution of the jacket (d) showed that pellet Nos. 1 and 4 remained intact with only minimum end spalling. Pellet Nos. 2 and 3 were broken in the characteristic lateral-radial manner. There was no evidence of lead intrusion into cracks of pellet Nos. 2 and 3.

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(a) SPECIMEN AS REMOVED (X4)

(b) RUPTURE SHOWING
LEAD BOND (X13)(c) LEAD PLUG FOUND
BETWEEN PELLETS 1 & 2

NO. 1

NO. 2 AND 3

NO. 4 (END VIEW)

(d) APPEARANCE OF EXPOSED PELLETS

Fig. 46

Appearance of Aluminum-Nickel Encapsuled Urania-Thoria Pellets
Irradiated in 500 - 515F Water for 1630 hr at Full CP-5 Power

c. Thermal Cycling Tests

Four bare and one jacketed sample of urania-thoria pellets were thermal cycled by heating 15 minutes in a tube furnace at designated temperature, dropping into distilled water at room temperature, drying at a temperature lower than the test temperature for three minutes, and returning to designated temperature. The samples were composed of 10% UO_2 and 90% ThO_2 and were prepared as described in the preceding section. The jacketed specimen contained four pellets

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and a 0.005-inch lead annulus between them and the aluminum-1% nickel capsule. Thermal cycling was performed in the NaK cyler between 150F and 750F for 75 cycles. The specimen was held at each extreme temperature for three minutes.

The results are summarized in Table XI. It appears that the particular samples tested can withstand 20 - 30 cycles between room temperature and about 750F without cracking, but their resistance to higher temperature differentials is much less. Disintegration of the aluminum-1% nickel capsule is believed to have resulted from attack by NaK at 750F.

Table XI

SUMMARY OF THERMAL CYCLED URANIA-THORIA PELLETS

Sample	Surface Condition	Test No.	No. of Cycles	Temp., F	Comments
A	Bare	1	20	75-500	No visible change.
		2	13	75-750	Cracked.
B	Bare	1	20	75-750	No visible change.
		2	1	75-1000	Cracked.
C	Bare	1	2	75-1000	Cracked.
D	Bare	1	3	75-900	Cracked.
A-4397-18	Al-1% Ni capsule; Pb in annulus.	1	75	150-750	Cladding and pellets disintegrated.

C. Control and Thermal Shield Materials - L. W. Fromm

1. Alternate Control Rods for EBWR - C. F. Bullinger,
J. H. Monaweck

A model of the rare earth oxide powder-filled control rod¹⁸ has been thermal cycled and exposed to water at 600 psig and 486F in the EBWR control drive test facility.

¹⁸Reactor Engineering Division Quarterly Report, ANL-5511 (January, 1956) p. 107.

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The cross-shaped can was filled to a density of 4.1 gm/cc with rare earth mixture "920" [50% (min.) samarium oxide, 25% (min.) gadolinium oxide, remainder other rare earths]. The mixture had previously been densified by addition of 1/2% B_2O_3 , pressing, sintering at 1600C, and crushing and screening the resultant mass. The can was closed with a welded stainless steel plug and thermal cycled fifty times between 550F and 170F, with a cycling time of five minutes cooling and two and one-half minutes heating. No leaks were found, and warpage remained well within required limits.

After one week of exposure of the unit to water at 600 psig and 486F the rod stuck in the guide channel. After cooling and removal from the guide (by disassembly of the guide) it was found that the control rod blades had swelled to twice their normal thickness (Fig. 47). Water had entered the cross at a point where one of the internal support studs was spot-welded to the casing. Apparently the steam pressure in the autoclave external to the can had caused an inward buckling of the stainless steel can because of the fact that the powdered oxides had a density of only 4.1 gm/cc, compared with a theoretical density of 7.25 gm/cc and thus could be compressed by the side walls of the can. When the can wall buckled inward, the wall ruptured at the welded stud. After the entry of water the can began to swell.

After cutting open the stainless steel can, it was found that the rare earth oxides had fluffed up and returned to their original low density. Apparently this phenomenon is a physical one akin to capillary attraction, and the calcined oxides swell upon wetting. Investigation of methods of avoiding this property in rare earth oxides is continuing.

2. Boron Stainless Steel Development

a. Fabrication and Mechanical Properties - N. Balai

Boron-contained stainless steel alloys were briefly considered as a control rod material for the Submarine Thermal Reactor, and preliminary corrosion tests were made on castings containing 1, 2, and 3 wt-% boron.¹⁹ The castings were found to be very brittle and further work was discontinued. However, the recently demonstrated need for a boron-containing thermal shield for the EBWR pressure vessel²⁰ has resulted in a renewal of development work on these materials, and the successful procurement of some satisfactory alloys has engendered renewed interest in their use as control rod materials as well.

¹⁹L. W. Fromm, et al., "Pilot Channel Test at Oak Ridge National Laboratory for a Water-Cooled Naval Reactor," ANL-4465 (August 24, 1950) p. 65.

²⁰Reactor Engineering Division Quarterly Report, ANL-5511 (January, 1956) p. 27

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FIG. 47
RARE EARTH POWDER-FILLED STAINLESS
STEEL CROSS ROD TESTED FOR 1 WEEK
IN 486 F WATER AT 600 psig

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Cast Alloys

Electric Steel Foundry Company, Portland, Oregon (supplier of metal for the earlier effort) furnished two experimental castings (14 in. x 14 in. x 1 in. thick) for evaluation. The castings contained approximately 2 wt-% natural boron in a basic CF-8 (American Castings Institute designation) composition. One plate was cast with the gage thickness in the horizontal position, and the second plate was cast with the gage thickness in the vertical position. Radiographs of the two plates showed much variation in density (estimated at 10%) which indicated the presence of either voids or segregation. Sectioning of the plates for the preparation of standard 0.505-in. (2 in. gage length) tensile specimens, and welding grooves for weld tests failed to disclose voids. Chemical analyses for boron by several sources, confirming segregation, are as follows:

Electric Steel Foundry Co.	2.0 % (nominal) 1.68% (recheck)
Argonne Chemistry Division	1.50%
KAPL (Met. & Chem. Analysis)	1.44%
WAPD (Materials Section)	1.38% one end; 1.46% center 1.20%/1.26% ends; 1.35% center

A single vee-groove butt weld was made in a 10-in. width of the flat cast plate with Arcos Stainlend K (titania-covered Type 308) manual arc electrodes without weld restraint. A crack-free weldment was obtained. Attempts to cold straighten a 6-in. wide section of the plate resulted in a very sudden and brittle failure in the base metal approximately 1/2 in. from the fusion zone. During welding it had been observed that the heat discoloration of the base metal was limited to a very narrow zone approximately 1/8 in. from the edge of the weld groove. The remaining 4-in. wide welded section was hot straightened at 1900 - 2000F without cracking. The mechanical properties, for 0.505-in. (2 in. gage) specimens, were as follows:

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Material and Heat Treatment	Tensile Strength, psi	Yield Point, psi	Reduction in Area, %	Elongation, %	Remarks
Horizontally cast plate. As received.	51,200	30,200	0	0.9	Brittle fracture, 90° to diameter.
Horizontally cast plate. Welded, hot straightened 1900 - 2000F.*	36,700	28,200	0	0.4	Brittle fracture, parallel to 37½°. Vee of weld and in heat dis- colored weld zone.
Vertically cast plate. As received (two speci- mens).	41,200 38,800	28,800 -	1.0 1.0	0.8 0.8	Brittle fracture, 90° to diameter.

* Held one hour at 2000F before straightening.

The machinability of the castings gave no indication of the brittleness shown by the tensile specimens; curly chips were obtained with ordinary high-speed steel tool bits (Rex AAA and Rex 95). Tool life was short and lower machining speeds (in comparison with the austenitic steels) were necessary, indicating a high degree of toughness and strength.

Heat treatment at 2000F, followed by either (1) slow furnace cooling or (2) water quenching, were found ineffective in producing metallurgical changes. A slight drop in hardness was noted in the base metal (from R_B 95 - 100 to R_B 85 - 90) after a 4-hour soaking at temperature; at the same time the stainless steel Type 308 weld metal in the sample (deposited from titania-covered rod) was embrittled, the hardness increasing from R_B 85 - 90 to R_C 15 - 17.

The initial 14 in. x 14 in. x 1 in. experimental casting, with ship-lap joints on two edges, indicated good casting qualities of the boron-modified CF-8 alloy. This was proved by the casting of a 54-in. long cross, 10 in. across the arms (Fig. 48). The arms are one-half inch thick and intersect the web with a root radius of one-half inch. Radiographic examination of the casting again revealed a non-uniform structure in the arms of the cross and in the center web section. The two ends were sawed off to determine the nature of the web flaws; internal "pipes" were found.

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ANALYSIS: CF-8 ALLOY - 2 WT-% BORON
 ORIGINAL LENGTH: 54 in.
 BLADE SPAN: 10 in.
 BLADE THICKNESS: 1/2 in.

Fig. 48

Cross-Type Rod Casting Produced by Electric Steel Foundry Company

The cast cross was heated to 1750F, held at temperature for one hour and furnace cooled to relieve casting stresses. Roughing planer cuts uncovered segregated inclusions, both metallic and non-metallic.

For machining of flat surfaces it was found that Rex AAA, Rex 95, Mo-Max-Moly high-speed steels were ineffective, Cecobolt (R_C-62) was badly abraded, and Carboloy broke down under the impact conditions. Rex-IV-V (R_C-68) was particularly effective (with minimum side, top and end clearance, and minimum side and top rake) after the ground tool tip broke down. For cutting the web radius (with minimum side and end clearance and minimum top rake), Mo-Max-Moly was particularly effective. Again, chips were curly. Heavy cuts (0.015 in.) at 9 fpm (maximum) were required to avoid chattering. Soluble oil coolants and lubricants were found to be ineffective; dry machining is being employed.

Additional experimental castings in the form of 1-1/2-in. diameter rounds, 12 in. x 12 in. x 1 in. plates, and 12-in. long crosses in the same cross section as above, in the CF-8 basic composition having boron additions of 4, 5, 6, 9, and 10 wt-%, have been attempted by the Electric Steel Foundry Company.

Successful castings in the 4, 5, and 6% compositions have been obtained (Fig. 49). It was found that these materials could be cut with a single cut mill file.

The 9 and 10 wt-% boron alloys were not castable in shapes other than cylindrical. The castings break up in the molds while

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4.06% BORON

5.35% BORON

6.7% BORON

FIG. 49

EXPERIMENTAL BORON-CONTAINING CF-8 ALLOY CASTINGS
PRODUCED BY ELECTRIC STEEL FOUNDRY COMPANY

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cooling (Fig. 50). The cylinders are non-metallic when fractured and low in density. Under magnification (50X) the fractured surfaces were spongy in appearance. The castings were not machinable, including grinding.

Rolled Boron Stainless Steel Plate

Experimental ingots fabricated by both conventional air melting and vacuum melting techniques and containing boron concentrations up to 2 wt-% have been rolled into sheet and plate. The following tabulation summarizes the melting practice of suppliers from whom materials have been obtained:

Firm	Process	Alloy Composition	Ingot, lb	Products
Superior Steel Corporation Pittsburgh, Pa.	Induction furnace, air-melted	18% Cr, 11% Ni, 1% B, 0.08% C max.	300 2000	plate and sheet (to 0.015" ga.) plate (30" x 1" ga.)
Republic Steel Corp., Massillon, Ohio	Induction furnace, air-melted	18% Cr, 10% Ni, 1.4% B	600	plate (12" wide x 1" ga.)
Vacuum Metals Corp., Syracuse, New York	Induction furnace, vacuum melted	Type 304 - 1½% B Type 304 - 2% B	600 600	bars (1" x 6") bars (1" x 6" and 5/16" x 6") wire 3/32" and 3/16" dia.
Universal-Cyclops Steel	Induction furnace, vacuum melted	18% Cr, 15% Ni, 1.2% B	600 600 - 850	plate (20" wide x 1" ga.) plate (20" wide x 1" ga.) sheets (20" wide x 1/8" ga.) wire 3/16" dia.

The as-cast brittle ingot structures have been broken down by: (1) hot press forging (from 2000F) and (2) standard blooming mill practice. The melting points were depressed markedly by the boron additions; the cooling of a stainless steel Type 304 ingot modified by the addition of 2 wt-% boron, observed with an optical pyrometer, indicated the surface "dried" at 2240/2250F. Hot forging of the 1-1/2% and 2% boron alloys, from 2000F (after the breakdown of the cast ingot structures), was not too different from that of conventional austenitic stainless steels. A lower limit of 1725F, as determined by fracture, was found for both compositions. Mill recovery (useful product of an ingot) was at least as good as for the austenitic stainless steels.

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9 WT-% BORON

10 WT-% BORON

FIG. 50
TYPICAL CRACKED APPEARANCE OF 9 AND
10 WT-% BORON-CONTAINING CF-8 ALLOY
CASTINGS AS REMOVED FROM MOLD

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Standard 0.505-in. diameter tensile specimens (2 in. gage length) showed marked improvement of the wrought products over castings in strength and ductility. The ductility of the as-received plate materials was low in comparison with the austenitic stainless steels and conventional boiler steels. Ductility, particularly in bending, may be improved by heat treatment at 2000F with furnace cooling, but the heat treatment followed by a water quench was found to be ineffective (Fig. 51).

Fig. 51

Effect of Heat Treatment on Ductility of Wrought
Type 304 - 2% Boron Plate Bend Test Specimens
(5/16 in. gage)

The low ductility of the as-rolled 0.075-in. sheet, bent parallel to the rolling direction, and the improvement effected by furnace cooling, is illustrated in Fig. 52. Both heat treatments resulted in a slightly magnetic material, whereas initially the alloy was non-magnetic.

The mechanical properties of the as-received plate are given in Table XII.

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HEAT TREATED AT 2,000 F, FURNACE COOLED

FIG 32
EFFECT OF HEAT TREATMENT ON DUCTILITY OF
AS-ROLLED 1 5% BORON-TYPE 304 PLATE
(0 075 in THICK)

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Table XII

CHEMICAL AND MECHANICAL PROPERTIES OF BORON-STAINLESS STEEL SPECIMENS

Source	Fabrication History	Chemical Properties, %								Mechanical Properties			
		B	Cr	Ni	Mn	Si	P	S	C	T.S., psi	Y. P., psi (0.2% offset)	R.A., %	Elong., %
Superior Steel	Air melted 1 in. gage cross- rolled plate H.T.: 30 min. at 1950F; air cooled; Mech. prop. based on 1 x 34 in. wide plates	0.95	18.27	11.03	1.58	0.53	0.028	0.016	0.058	95,800 (L)*	54,700	13.	18.
										86,100 (T)	56,500	15.	17.
Vacuum Metals	Vacuum melted 1 in. gage x 6 in. bar H.T.: 1 hr at 1950F; air cooled	1.57	18.89	8.76	1.43	0.51	0.004	0.015	0.031	101,000 (L)	-	11.8	15.6
										93,500 (L)	37,600	13.8	13.6
										98,500 (T)	42,500	14.4	12.3
										92,000 (T)	-	11.7	10.2
Vacuum Metals	Vacuum melted 1 in. gage x 6 in. bar H.T.: 1 hr at 1950F; air cooled	2.02	18.82	8.70	1.47	0.53	0.004	0.013	0.07	100,250 (L)	-	7.0	8.3
										100,000 (L)	70,000	8.0	8.3
										98,750 (L)**	37,000	9.5	8.1
										97,700 (L)**	-	9.8	8.8

*(L) Test bar parallel to rolling direction; (T) transverse to rolling direction.

**As-received bar heat treated 1 hr at 1750F; furnace cooled overnight.

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Superior Steel Corp. Air-Melted Alloy (18% Cr-11% Ni-1% B)

Two bars (4 in. x 0.340 in. gage) of wrought alloy were obtained from an experimental 300-pound ingot produced by the Superior Steel Corporation. Two test bars of the alloy, approximately 15 in. long, were welded to each other and to 3/8 in. stainless steel Type 304 end plates to form a composite weld test plate. Standard double-vee grooves, with an included angle of 75 degrees, separated by a 1/16-in. land were held in contact and welded with Arcos Stainlend K electrodes (titania-covered ac-dc rods). After cooling, the stainless weld joining the boron-stainless steel alloy plates cracked badly; the dissimilar weld metal joints between the Type 304 and the boron-contained alloy were only slightly cracked. The root of the weld was unfused.

A second similar composite test plate was welded with boron-contained weld metal (prepared from slivers sawed from the parent plate) by the Argonarc process. Welds were crack free and penetration at the root of the weld was complete. The composite plates, with and without cracked welds, were fabricated into corrosion samples for an evaluation of the corrosion resistant properties of the base plate, heat-affected zone, and weld deposit (see page 119).

Vacuum Metals Corporation Vacuum-Melted Alloy (18% Cr-8% Ni-1.5% B)

Several attempts were made at performing double-vee butt welds in boron-containing Type 304 plate (1 in. gage) before the apparently satisfactory results shown in Fig. 53 were obtained. Since the tests on 0.340 in. gage air-melted plate had indicated root fusion to be a problem, the root opening for the 1-inch plate welds was increased to 1/16 in. and the root passes were deposited from bare stainless Type 308 wire by the Argonarc process. Attempts to complete the welds with Arcos Stainlend K (titania-coated Type 308) electrodes resulted in failure by cracking in the 5th or 6th pass (successive passes were deposited in alternate grooves to control distortion and to maintain a uniform temperature of deposited weld metal). Visually crack-free welds were obtained with International Nickel Company Type A and with Arcos Chromend K (lime-coated Type 308) electrodes. Results with an experimental lime-coated Type 308 electrode containing boron in the coating were as poor as with the Arcos Stainlend K electrodes.

The test plate was sectioned and standard tensile bars were prepared for an evaluation of the strengths of welds (Table XIII). All welded bars broke across the welds and showed abnormally low ductilities for stainless steel welds. The broken bars also showed that the problem of root fusion had not been completely solved by the method described.

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Fig. 53

Double Vee Butt Welded 1.5% Boron-Type 304
Alloy Plate (1 in. Gage)

Republic Steel Corporation Air-Melted Alloy
(18% Cr-10% Ni-1.4% B)

This air-melted composition has been singularly refractory; double vee-groove welds transverse to the rolling direction have cracked in every case. Other alloys have displayed this trait only in welds parallel to the rolling direction.

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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	R.A., %	Elong., %
1 & 2	1½% B to 1½% B; weld transverse to rolling direction; bar parallel to rolling direction.	K	79,500 82,800	- -	6.0 10.3	2.6 3.7
3 & 6	Type 304 to 1½% B; weld parallel to rolling direction; bar transverse to rolling direction.	A	45,250 69,300	37,500 -	12.0 12.8	2.6 5.0
4 & 7	1½% B to 1½% B; weld parallel to rolling direction; bar transverse to rolling direction.	K	76,000 81,500	51,000 -	12.0 5.3	2.6 2.8
5 & 8**	Type 304 to 1½% B; weld parallel to rolling direction; bar transverse to rolling direction.	K	60,600 89,000	40,000 -	- 12.5	3.2 20.2
9 & 12	Type 304 base plate; rolling direction unknown.	-	88,200 89,200	- -	72.5 73.1	78.9 80.0
10 & 11***	1½% B base plate; transverse to rolling direction.	-	98,500 92,000	- -	14.4 11.7	12.3 10.2
13 & 14***	1½% B base plate; parallel to rolling direction.	-	101,000 93,500	- 37,600	11.8 13.8	15.6 13.6

* K - Arcos Chromend K (lime coated)

A - International Nickel Co. Type A

** Weld in bar 8 developed full strength of Type 304; elongation and reduction of area occurred only in Type 304 section of bar.

*** Three of bars failed at extensometer gage marks on one end.

Note: All breaks except in bar No. 7 were at 90° to bar axis and were initiated either by flaws in root of weld or extensometer gage marks. Bar No. 7 was the only "wolf ear" failure.

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b. Corrosion - N. R. Grant

The corrosion characteristics of boron-stainless steels of various compositions and from various sources are under investigation in both static and dynamic tests. Results are summarized in Tables XIV to XVII. Most of the samples show good corrosion resistance. All of the static samples tend to gain weight at first and after about a month begin to lose weight. The dynamic samples consistently lose weight.

Table XIV

STATIC CORROSION OF BORON-STAINLESS STEEL

Source	Sample No.	Nominal Composition, %			Test Conditions			Rate of Wt. Change, mg/(cm ²)(mo)	Comments
		Cr	Ni	B	Temp., F	Water Condition	Time, hr		
Electric Steel Foundry Company (as cast)	A-495	12	8	1	600	Degas	2044	+ 0.0179	Adherent black coating.
						Air Satd.		+ 0.0324	Adherent brown-black tarnish.
						Degas		+ 0.0488	Sample pickled in 40% HNO ₃ and 4% HF before test. Post test: adherent black coating.
	A-496	12	8	2	600	Degas	2044	- 0.0045	Adherent gray-black coating.
						Air Satd.		- 0.0490	Adherent gray-black coating.
						Degas		+ 0.0178	Sample pickled in 40% HNO ₃ and 4% HF before test. Post test: adherent black coating.
	A-497	12	8	3	600	Degas	2044	- 0.0139	Adherent black tarnish.
						Air Satd.		+ 0.0268	Adherent black coating.
						Degas		- 0.2580	Sample pickled in 40% HNO ₃ and 4% HF before test. Post test: adherent black coating.
	A-4473a-1	18	14	2.2	550	Degas	1623	- 0.0536	Adherent dull black coating.
	A-4473a-2	18	14	2.2	550	Degas	1523	+ 0.0019	
	A-4473b-1	18	14	2.8	550	Degas	1623	+ 0.0451	
	A-4473b-2	18	14	2.8	550	Degas	1623	+ 0.0396	
Superior Steel Co. (Ingot forged, hot rolled, machined, hot rolled, annealed, air cooled)	A-4480-1	18	11	1*	600	Degas	1435	- 0.0407	Adherent brown-black coating.
	A-4480-2	18	11	1*	600	Degas	1435	- 0.0702	Adherent black coating.
	A-4480-3	18	11	1*	600	Air Satd.	1435	- 0.0185	Adherent brown-black tarnish.
	A-4480-4	18	11	1*	600	Air Satd.	1435	- 0.0221	Adherent dark blue, brown, and purple tarnish.
Crucible Steel Co. (Heated to 2050F; forged at 1850F; air cooled)	A-4494a	18.8	8.76	2**	550	Degas	802	+ 0.0369	Adherent black coating.
	A-4494b	18.8	8.76	2**	550	Air Satd.	802	+ 0.1090	Adherent brown and black tarnish. Black marks where samples were in contact.

*Analyzed 0.96 and 1.01% B.

**Analyzed 1.85% B.

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Table XV

STATIC CORROSION OF WELDED BORON-STAINLESS STEEL PLATE

Source: Universal Cyclops Steel (vacuum melted).

Nominal Composition: 18% Cr-15% Ni - 1.2% B;* welded with
Type 308 electrodes.

Test Duration: 662 hr

Sample No.	Origin of Specimen	Test Conditions		Rate of Wt. Change, $\text{mg}/(\text{cm}^2)(\text{mo})$	Comments
		Temp., F	Water Treatment		
A-4483b-1,2	Cross section of weld then cut in half through weld.	600	Degas	-0.0471	Adherent brown and black tarnish; lighter on weld. Black marks where samples were in contact.
A-4483b-3,4		550	Degas	-0.0128	Adherent brown tarnish; lighter on weld. Black marks where samples were in contact.
A-4483b-5,6		550	Air Satd.	-0.0344	Adherent brown tarnish; brown and blue on weld. Black marks where samples were in contact.
A-4483c-1,2	Cut from welded slab out of heat zone.	600	Degas	-0.0234	Adherent brown and blue tarnish. Black marks where samples were in contact.
A-4483c-3,4		550	Degas	-0.0506	Adherent brown-grey tarnish. Black marks where samples were in contact.
A-4483c-5,6		550	Air Satd.	-0.0699	Adherent blue tarnish, brown around edges. Black marks where samples were in contact.
A-4483e	Cut from welded slab out of heat zone, parallel to rolling direction.	550	Degas	+0.0382	Adherent black tarnish. Outline of sample contact area noticeable.

* Analyzed 0.68%, 0.63% B

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Table XVI

STATIC CORROSION OF WELDED BORON-STAINLESS STEELS

Source: Superior Steel Co.

Nominal Composition: 18% Cr - 8% Ni - 1% B;
welded as shown below.

Test Conditions: 332 hr in 550F water.

Sample No.	Material Welded	Type of Weld	Water Treatment	Rate of Wt. Change, mg./cm ² (mo)	Comments
A-4507-1,2	1% B to Type 304	Heliarc; bare Type 304 - 1% B rod; single bead.	Degas	+0.0247	Adherent dark brown tarnish on Type 304. Slightly loose black coating on weld and boron-stainless.
			Air Satd.	+0.0495	Adherent blue and straw tarnish.
A-4507-3,4	1% B to 1% B	Heliarc; bare Type 304 - 1% B rod; double bead.	Degas	+0.0744	Slightly loose black coating.
			Air Satd.	+0.0371	Adherent brown and blue tarnish.
A-4507-5,6	Type 304 to 1% B	Heliarc; bare Type 304 - 1% B rod; single bead.	Degas	±0.00	Slightly loose black coating, more on the boron steel than on the Type 304.
			Air Satd.	+0.0371	Adherent brown and purple tarnish on boron steel. Blue-green on weld, purple-brown on Type 304.
A-4508-7,8	1% B to Type 304	Welded with Type 308 coated rod; single bead.	Degas	+0.0741	Adherent brown-black coating, darker on boron steel.
			Air Satd.	+0.0124	Adherent brown and blue tarnish. Brown tarnish on weld.
A-4508-9,10	1% B to 1% B	Welded with Type 308 coated rod; double bead.	Degas	-0.0495	Adherent black coating; dark brown on weld.
			Air Satd.	-0.0620	Adherent brown and blue tarnish.
A-4508-11,12,	Type 304 to 1% B	Welded with Type 308 coated rod; single bead.	Degas	+0.1486	Adherent black coating; duller on boron steel.
			Air Satd.	+0.0247	Adherent brown, blue, and purple tarnish.

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Table XVII

DYNAMIC CORROSION OF BORON-STAINLESS STEELS
IN 550F DEGASSED WATER

Water Velocity: 22 fps

Source	Sample No.	Nominal Composition, %			Test Period, hr	Rate of Wt. Change, mg/(cm ²)(mo)	Comments
		Cr	Ni	B			
KAPL	A-4403a	18	14	1.6	3031	-0.0836	Adherent black coating.
KAPL	A-4403b	18	14	2.5	3031	-0.0738	Adherent black coating. Loose black coating in holder contact areas.
Electric Steel Foundry Co.	A-4463	18	8	2.18* (welded with Type 308 electrode)	1666	-0.0953	Adherent lustrous blue-black tarnish. Loose black coating on edges. Weld same as sample.
Superior Steel Co.	A-4480a A-4480b	18	11	1**	912	-0.0851	Adherent black tarnish. Loose black coating on edges.
	A-4483a (cross section of weld)				912	-0.0203	Adherent brown tarnish; purple tarnish in weld. Loose black coating on edges.
Universal Cyclops		18	15	1.2*** (welded with Type 308 electrode)			
	A-4483d (heat zone of weld parallel to rolling direction)				912	-0.0032	Adherent brown tarnish. Loose black coating on edges.
Crucible Steel Co.	A-4494b	18.8	8.7	2	912	-0.1365	Adherent black tarnish. Loose black coating on edges.

* Analyzed 1.50, 1.49% B

** Analyzed 0.96, 1.01% B

*** Analyzed 0.68, 0.63% B

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3. Corrosion of Aluminum-Boron-Nickel Alloy - C. R. Breden

Static corrosion tests on aluminum-boron-nickel alloys (used in BORAX-III as a burnable poison) have accumulated 2119 hr in 440F water. No significant changes have been observed; the weld areas still exhibit the greatest attack.

D. Control Rod Drive Mechanisms - C. F. Bullinger, L. W. Fromm

1. Hydraulically-Operated Drive Mechanism - W. J. Kann

The hydraulic control rod drive mechanism, shown schematically in Fig. 54, has successfully completed 602 cycles and 83 scrams.

Fig. 54

Hydraulically Operated Control Rod Drive
and Position Indicator

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The drive is similar to the one described in ANL-5461,²¹ but has been re-designed to include an internal hydraulic-type dashpot.

The rod scrams were initiated by a 1-inch Sentry piston-operated deluge valve. Figure 55 indicates that although a 0.180-second lag time is experienced from the initiation of the scram signal until the rod begins to drop, full speed of approximately 8 fps is attained in 0.060 sec or 4 in. of travel. The total scram time, including actuation lag, for the 48-inch travel is 0.780 sec.

Fig. 55

Scram Time for Hydraulically Operated Control Rod

The position indicator is operated by a simple servo system shown schematically in Fig. 56. When the strength of the magnetic field in the follower coil coincides with that of the controller coil, the circuit is in balance and the system is at rest. Movement of the control rod and, hence, the magnetic probe extension, creates a condition of unbalance between the follower and controller coils. The circuit senses this unbalance and initiates servo motor rotation in the correct direction to return the system to balance.

²¹ Reactor Engineering Division Quarterly Report (April 15, 1955) p.31.

Fig. 56

Schematic Wiring Diagram for Position Indicator Circuit

The lead screw revolutions, and therefore piston position, are indicated directly by two master-slave selsyn circuits. Present indication is accurate to 0.010 in. and wholly reproducible.

2. Magnetic Jack Drive Mechanism - J. N. Young

The magnetic jack, Unit No. 18,²² operated with a 116-lb load for 41 hr (72 cycles of 4-foot stroke) after which it was found that the length of step in the downward direction had decreased from 0.10 in. to 0.05 in. This was apparently caused by wear of the end guide holes, which adversely affected the concentricity of the rod and, consequently, the gripper bars. The resulting excessive magnetic side forces prevented the rod from moving freely when the magnet coils were energized. Testing of this unit has been discontinued.

²²Reactor Engineering Division Quarterly Report, ANL-5511 (January, 1956) p. 118.

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A new type of gripper unit (No. 18-B) has been designed to eliminate the difficulty described. Whereas all previous jack mechanisms used a constricting-type gripper, which grasped a one-piece rod which was not necessarily magnetic, the new type employs a magnetic, split rod which is held to a one-piece rigid gripper by magnetic forces generated when the surrounding coils are energized. The arrangement is shown in Fig. 57.

The superiority of this type of gripper is attributed to the more efficient magnetic circuit, which provides a low magnetic flux density at the radial gap between the gripper and the magnetic pressure shell and eliminates the gap between the gripper and the rod, where the highest flux density is required for gripping. The low flux density at the radial gap also improves mechanical efficiency by reducing magnetic side forces which arise in a gripper which is not concentric because of clearances. For this reason clearances for moving parts may be increased, and a considerable amount of fabrication inaccuracy, wear, and dirt accumulation may be present without materially affecting performance. Finally, the greater efficiency reduces heat generation to the point where special cooling is unnecessary, and the component parts are simple and low in cost.

Magnetic jack unit No. 18B has been fabricated using the coils and pressure shell of jack No. 18 and the new gripper and rod system described. During the first portion of test operation Belleville washers were used at either end of the gripper as guides. These washers wore out after 375 cycles with 0.071-inch steps upward and 0.077-inch steps downward. They were replaced by a non-magnetic, loosely fitting sleeve between the gripper and pressure shell (see Detail A, Fig. 57). The test was resumed using step lengths of 0.100 in. upward and 0.105 in. downward; the unit operated most satisfactorily, with negligible wear of parts.

To date this unit has been run a distance equivalent to 1030 cycles of 4-foot strokes in 607 hr; 502 hr (890 cycles) of test were carried out with step lengths described above in an open test facility with a 313-lb load; the remaining 105 hr (140 cycles) of testing were carried out in a steam-pressurized facility simulating EBWR conditions. A dummy control rod and a full-length scram spring gave a load of 155 lb at the bottom position and increased to 253 lb at the top position. Most of the testing was done at 30 steps per minute, while 23 hr (70 cycles) was done at 60 steps per minute. The jack when new (with parts oily from fabrication) lifted about 600 lb before the gripper began to slip. After the device was run with water for several days, it lifted about 900 lb, which is the limit of the lifting magnet.

A scramtime curve for the Model 18B Unit for a pre-scram condition of Coil No. 4 holding with two amperes is shown in Fig. 58. A control rod weight of 97 lb and a full-length accelerating spring (127-lb force, maximum) were employed for this test.

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Fig. 58

Scram Time for Magnetic Jack Control Rod Drive

The only malfunction experienced with jack No. 18B, other than the Belleville spring wear described above, occurred when one of the coils became wet and short-circuited. A prototype model magnetic jack unit for EBWR installation, currently in fabrication, is designed with waterproof, replaceable coils to eliminate this difficulty.

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

The start of the irradiation of Cartridge MTR-W-7 (a three-cycle corrosion rate determination of aluminum-nickel alloy) was delayed until late November because the installation of a new in-reactor tube had to coincide with the installation of other equipment in the adjoining HT-1 test hole. During sporadic operation in October and early November, three coolant pumps were burned out because operating personnel failed to take the necessary corrective action after water was discovered in the cooling oil.

The installation of the new in-reactor tube was carried out without difficulty during the shutdown starting November 14 and the loop reached full operating conditions (500F and 1500 psi) within two days after reactor startup on November 23. The loop has operated satisfactorily since that

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date, and as of the December 27 shutdown the first two cycles of the MTR-W-7 irradiation have been completed. The third cycle will be continued until such time as everything is in readiness for Cartridge MTR-W-8 (an EBWR prototype fuel plate).

As time permits, modifications are being made to the loop to ready it for this irradiation. These include:

(1) Re-piping of process water to permit its return to the MTR water system and hence eliminate the restrictions on the amount of flow available for process water cooling.

(2) Modification of the main flow meter to give it adequate response for actuating the pump-switching mechanism and the low-flow scram circuit. These functions are currently carried out by a small rotameter in a by-pass line.

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PART B. FAST POWER BREEDER REACTOR PROGRAM

SUMMARY - L. J. Koch

A preliminary draft of an Architect-Engineer prospectus covering the design requirements for EBR-II has been prepared. The prospectus defines the engineering work to be performed by the Architect-Engineer and the engineering work to be accomplished by the Laboratory. Because of the nature of the EBR-II plant design certain parts of the plant will be handled directly by the Laboratory, including detailed design, procurement, and installation.

The EBR-II Working Model was shut down to make changes in the components, including the fuel-removal gripper unit, the control drive, and the fuel storage rack, which were not operating satisfactorily. Sticking of mating parts was observed in the first two units; this was corrected by minor design modifications.

The difficulty with the storage basket was due to misalignment of this unit with respect to the transfer arm due to differential thermal expansion. This was corrected by employing a new plug in which the unit is mounted, providing additional clearance between the plug and the tank. This permits adjustment to be made at temperature to alleviate the misalignment problem.

The system was shut down for 33 days to accomplish these modifications. The sodium level was lowered approximately halfway in the tank and then permitted to freeze. During the process of removing the various pieces of equipment, the system was opened many times and the sodium exposed to air. A large amount of oxide was formed on the sodium surface and the sodium adhering to the internal structural surfaces. After the system changes were completed, the sodium was melted and the tank refilled to the normal operating level. The temperature was then raised, and the cold trap system operated for approximately two days. At this time, the sodium system was clean. This experience is quite significant to the EBR-II program because a very contaminated sodium system was returned to operation with essentially no difficulty. It is expected that the contamination of the sodium in the model system was more severe than would ever be anticipated in normal reactor operation.

The irradiation tests of EBR-II pin-type fuel elements were continued in CP-5. Fuel elements No. 3, 4, and 5 were removed from the reactor and disassembled and inspected. They had been irradiated to an estimated burnup of 0.6% to 0.8% total atoms. Elements No. 3 and 4 revealed sections at the upper end of the fuel element where the uranium had melted through the stainless steel container. Except for the melted sections, the remainder of the fuel elements appeared to be in good condition.

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The failure was similar to that observed in fuel element No. 2 which had been inspected previously. All three failures are believed to be due to a failure in the sodium bond (either the bond between the fuel pin and fuel element tube, or the bond between the fuel tube and the inner wall of the capsule). These units were under irradiation at the time the failure of element No. 2 was observed, and no change in assembly procedure could be effected. Commencing with element No. 5 more care has been taken in the bond-filling operations and the heat treatment to obtain wetting.

Element No. 5 was removed from its capsule and was in excellent condition. There was essentially no distortion or bumping of the element, and no indication that it had been irradiated to its ultimate limit.

A series of "eutectic tests" was evaluated to determine the temperature sensitivity of the formation of the iron-uranium eutectic in the EBR-II fuel element system. These tests had been initiated many months ago and consisted of groups of EBR-II fuel elements soaked for long periods of time in furnaces at various temperatures. Some of the units were thermal cycled, and others were maintained at constant temperature. The duration of the tests ranged from 90 to 280 days, and the temperatures from 1000F to 1380F. None of the elements indicated evidence of eutectic formation, with the exception of two cases where the elements were melted due to failure of the temperature controller on the furnace. The samples held at 1380F, both constant temperature and thermal cycled, did not exhibit indications of eutectic formation. The units that failed were at lower temperatures. These tests essentially confirm the opinion that the difficulty observed in the irradiation experiments was probably due to a void in the sodium bond, because this is the most probable manner in which the necessary high temperatures could be reached.

A series of thermal cycling tests have been conducted on various uranium alloys with various heat treatments. Of particular interest were some comparisons between uranium-2% zirconium alloys and uranium-1.4% molybdenum alloys. These were compared by cycling between 150F and 1150F (below the alpha-beta transformation of uranium), and between 200F and 1320F (through the alpha-beta transformation temperature). There were relatively modest differences between the various samples after alpha cycling, but there was a very large difference between the samples after beta cycling. The molybdenum alloys were far superior and were relatively unaffected by cycling through the alpha-beta transformation temperature. This confirms earlier results, and further testing is planned, including use of alloys of higher molybdenum concentration (2.0% and 2.5% molybdenum). These results are particularly significant for the EBR-II program, because the reactor performance can be improved appreciably if the present fuel temperature limitation of 1200F can be raised. It is also significant that the addition of molybdenum is beneficial because it is one of the major constituents in the "fissium alloy" which results from the pyrometallurgical process to be employed in the EBR-II plant.

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A series of tests was conducted to determine the shutdown cooling requirements of an EBR-II fuel subassembly after irradiation. Present plans include the cooling of the subassembly for 15 days in sodium prior to removal from the system. After this cooling period, the subassembly is removed to the disassembly cell where the hexagonal containing tube is removed and the individual fuel pins separated from the assembly. Tests were conducted to determine the cooling requirements after the subassembly is removed from the sodium. An electrically heated model of the fuel section of the subassembly, which consisted of 91 fuel element tubes containing electrical resistance heaters, was constructed. The heat generation was controlled, and the temperature distribution was measured under various conditions. The subassembly produces approximately 2 kw of heat after the cooling period, and it was found that the unit will not cool adequately in air without a system of forced convection. Since the subassembly is removed from the sodium into a cell containing an argon atmosphere, tests were made and it was determined that a gas flow of approximately 15 cfm will remove the heat under all anticipated conditions of operation.

In the process of disassembly, the hexagonal container tube is removed from the fuel cluster; thus it was necessary to determine the cooling requirements for the cluster of fuel pins after the tube is removed. Since axial flow of the gas is no longer possible, a system of cross flow was employed. It consisted of a perforated support plate of hexagonal cross section connected by a manifold to the gas supply. Gas was forced across the cluster, and the temperatures measured. It was found that during this stage of the disassembly process a gas flow of approximately 15 cfm will adequately cool the fuel elements.

A single fuel tube was heated to determine the cooling requirements of the fuel element after removal from the cluster. It was found that an individual fuel element will cool satisfactorily in air without any provisions for movement of the atmosphere.

The large homopolar generator was tested with a temporary 100-hp motor drive to determine the no-load characteristics of the machine and to obtain some information regarding its behavior when delivering moderate currents at low voltage. The machine has been operated without load at outputs up to 130,000 amperes at a terminal potential up to 1.4 volts. The electrical performance of the machine was not entirely satisfactory -- the open circuit electrical losses were higher than anticipated. The source of the difficulty was in one of the insulating sleeves surrounding the rotor. The sleeve consisted of a thin wall, hollow stainless steel spool with the air gap between the shells providing the insulation. The sleeve had failed and had filled with sodium, forming a short circuit path for the current. New sleeves are being made with mica insulation between the two shells to improve the electrical resistance and also to provide mechanical separation of the shells which are quite thin and flexible.

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The mechanical operation of the generator was quite satisfactory. There was no difficulty with the rotor shaft seal, and very little oxide formed in the inert gas chamber at the top of the machine.

A full-scale prototype control drive unit for EBR-II has been constructed. The upper half of the assembly, including the drive mechanism, has been assembled to determine acceleration characteristics and unlatching time. This is a mechanical unit employing a pneumatic cylinder to provide the accelerating force during scram operation. Since it is a combination control and safety rod drive, the control motion is achieved with a rack and pinion drive which is constantly opposed by the pneumatic cylinder which always functions in the scram direction. Scram is effected by releasing a latch which is actuated by a magnetic coupling. The lower portion of the assembly, which will operate in sodium in the reactor system, will be assembled to the external portion of the system after these initial tests are completed, and after the sodium-containing system has been completed.

The high-temperature sodium loop which has been operating at 800C failed after 3,254 hr of continuous operation. The test loop is part of a program directed primarily toward the development of d-c electromagnetic pumps for operation at high temperatures. Operation at 800C is possible because of the development of an all-welded, d-c current, electrode-pump tube joint. The pump tube failure did not occur at any of the welds, but appeared to originate in the transition from the round cross section to the rectangular pump section.

The extent of the pump tube rupture was sufficient to release the total volume of sodium in the loop system (5 gal). However, since the loop is contained within a relatively tight steel box, there was no serious damage, and the resulting fire and most of the smoke were confined to the enclosure. This was the largest volume of sodium released from an experiment at this temperature at the Laboratory. It is encouraging to note that a simple steel box is sufficient to contain the sodium under conditions of failure.

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

Danger Coefficient Experiments on EBR-I - Howard Kraig

In June 1954, danger coefficient experiments employing twelve sample materials were run in holes 1 and 145 of EBR-I. The samples were enclosed by capsules of three materials, all capsules having a length of 2.013 ± 0.001 in. and a diameter of 0.374 in. The samples are described in Table XVIII.

The initial runs were made to determine the nuclear center of the reactor. A sample of U^{235} was placed at several positions in each of the test holes. When the sample was positioned, the rods were raised to 30 in. to put the reactor on a period, the jacks being maintained at 0.35 in. throughout (cup position fixed). The flux was recorded on a Brown recorder; its signal was supplied by a BF_3 chamber which fed a vibrating reed electrometer. The flux was allowed to increase through three decades, after which the rods were dropped. The last two decades of the flux plot were replotted on semi-log paper as straight lines and the period obtained from the e-folding time. The two periods obtained were averaged arithmetically. The Brown recorder was arranged to trip limit switches at scale values of 30 and 81, starting and stopping a clock on the console. The clock readings served to check plotted values of period. A Leeds and Northrup Type K potentiometer was used to indicate the fuel temperature transmitted from a thermocouple inserted in fuel rod 8 (third row). Temperature deviations were expressed as reactivity effects, using the relation $1C = 1.37$ in hours. The recorded periods were adjusted accordingly and plotted against sample position. Temperature readings were taken at midscale reading of the second and third decades on the flux recorder. These temperatures were averaged, and the difference between the average operating temperature and some reference temperature was used to determine temperature reactivity correction. Throughout most of the experiment the mechanical coolant pump was used instead of the electromagnetic pump to minimize the heat input to the coolant and the reactor.

The nuclear center of the reactor was taken to be the position corresponding to the shortest corrected period and was found to be 8.511 and 8.261 in. from the bottom of the thimble in holes 1 and 145. Geometric center of the reactor is 8.75 in. from the bottom of the thimble. The accuracy of the experimental determination of nuclear center is affected by the inherent inaccuracy in determining period and by the flattening of the period vs position curve close to the nuclear center. A spacer was fabricated to shim the sample center to nuclear center.

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Table XVIII
PROCESSED EXPERIMENTAL DATA

Run	Sample	Average Period, sec	Temp. Correction, inhours	Run	Sample	Average Period, sec	Temp. Correction, inhours
Hole No. 1				Hole No. 145			
3	Indium	83.75	0.2	54	Plutonium	26.0	0
4	Copper Blank	74	0.4	55	SST Blank	47.7	0.3
5	Indium	83.5	0.8	56	Plutonium	26.0	0.7
6	Copper Blank	73.5	0.9	57	SST Blank	48.5	1.9
7	Polythene	37.75	1.1	58	Steel	44.2	1.4
8	Copper Blank	74.5	1.2	59	Armco Blank	49.5	1.8
9	Polythene	38.0	1.55	60	Steel	46.0	2.1
10	Gold	87	1.57	61	Armco Blank	51.35	2.1
11	Copper Blank	77.8	1.85	62	Boron-10	78.5	2.9
12	Gold	88.75	1.80	63	Armco Blank	53.5	3.2
13	Copper Blank	67.0	0.78	64	Boron-10	82.0	3.6
14	Thorium	70.25	1.03	65	Nat. Boron	54.7	3.9
15	Copper Blank	69.5	1.16	66	Armco Blank	56.0	4.2
16	Thorium	73.0	1.37	67	Nat. Boron	56.5	4.1
17	Nat. Uranium	59.7	1.6	68	Armco Blank	56.5	5.0
18	Copper Blank	72	2.0	69	Lithium	65.5	4.7
19	Nat. Uranium	59.5	1.6	70	Armco Blank	56.7	5.2
20	Copper Blank	73.0	2.3	71	Lithium	65.0	5.4
21	Enrich. Uranium	23.75	1.77	72	Carbon	45.5	0.1
22	Enrich. Uranium	24.55	2.15	73	Armco Blank	50.2	0.1
23	Copper Blank	76.3	1.9	74	Carbon	46.5	0.2
24	Plutonium	25.5	3.7	75	Armco Blank	52.2	0.4
25	SST Blank	77.5	4.06	76	Copper Blank	57.2	0.7
26	SST Blank	77.2	4.06	77	Nat. Uranium	42.5	0.9
27	Plutonium	25.2	4.46	78	Nat. Uranium	44.7	1.0
28	SST Blank	76.0	4.48	79	Copper Blank	53.0	1.6
29	Steel	82.0	4.65	80	Thorium	50.2	2.0
30	Armco Blank	78.2	4.8	81	Thorium	51.2	2.1
31	Steel	80.7	4.9	82	Copper Blank	54.2	2.5
32	Armco Blank	80.0	5.1	83	Enrich. Uranium	27.5	2.5
33	Carbon	71.2	5.5	84	Enrich. Uranium	28.7	2.8
(Note: Change in position at U^{235} and U^{238} slugs increased reactivity by 1 inhour)				85	Copper Blank	57	3.4
34	Armco Blank	76.7	5.6	86	Indium	55.5	3.8
35	Carbon	69	5.9	87	Indium	55.0	4.2
36	Nat. Boron	80.7	6.0	88	Copper Blank	59.5	4.5
37	Armco Blank	77.5	4.47	89	Gold	53.5	4.7
38	Nat. Boron	85.2	6.2	90	Gold	52.5	5.1
39	Boron-10	421	6.6	91	Polythene	40.0	5.4
40	Armco Blank	80	6.8	92	Polythene	39.0	5.5
41	Boron-10	171	-5.0	93	Copper Blank	58.7	5.7
42	Lithium	75.7	-4.4	94	Lithium	67.5	5.8
43	Armco Blank	55.2	-4.0				
44	Lithium	78.7	-3.7				
45	Armco Blank	57.7	-3.4				
46	Copper Blank	58.2	-3.3				

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The core loading of the EBR during the danger coefficient experiments was as follows:

- (1) Danger coefficient samples in hole 1.
- (2) Natural uranium blanket rods in holes 170, 171, 176, 178, 182, 184, 186, 189, 191, 192, 193, 194, 197, 199, 200, 201, 202, 204, 207, 208, 211, 216.
- (3) Metallurgical samples in holes 177, 209, 210.
- (4) Fuel rods in all others.

When the danger coefficient samples were inserted in hole 145 a fuel rod was placed in hole 1.

1. Procedure

The sequence of events during the subsequent tests was as follows:

a. The fuel rod was removed from the test hole and a thimble inserted. The shim spacer, a U^{238} slug and a U^{235} slug were placed successively in the thimble. The uranium slugs were dimensioned to simulate blanket and core composition below the sample.

b. The sample was lowered into place with a vacuum device. All samples were run with alternate ends in the up position, to provide a reproducibility check, and to indicate any changes in reactivity due to asymmetry in the sample. The blank capsule in each group was run at least twice for each sample, depending on the reproduction of results.

c. A liner containing the uranium slugs was lowered into place.

d. Cup position was determined to afford a safe period for the plutonium sample (which was anticipated as the most reactive) and was not changed thereafter.

e. The safety rods were raised. Control rods were maintained at 30 in.

f. Flux was recorded on the Brown recorder. The flux was allowed to increase through 3 decades before the rods were dropped.

g. At the half-scale indication on the second and third periods on the Brown recorder, time and temperature were recorded and averaged.

h. Fluxes were replotted on semi-log paper and analyzed as straight lines to give folding times. Console clock periods were used to check the plots.

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i. Resulting period was expressed as reactivity in inhours (see Fig. 59), and the temperature correction applied (see Table XVIII).

The error in reactivity corresponding to an error in period is obtained by differentiation of the inhour equation:

$$\rho_{ih} = \frac{\frac{\ell}{T k_{eff}} + \sum_{i=1}^n \frac{\beta_i}{1 + \lambda_i T}}{\frac{\ell}{3600 k_{eff}} + \sum_{i=1}^n \frac{\beta_i}{1 + 3600 \lambda_i T}} \quad (1)$$

If $\ell < 10^{-5}$ for near critical reactors, Eq. (1) reduces to:

$$\rho_{ih} \sim \frac{\sum_{i=1}^n \frac{\beta_i}{1 + \lambda_i T}}{\sum_{i=1}^n \frac{\beta_i}{1 + 3600 \lambda_i T}} \quad (2)$$

Inserting the following values²³ in Eq. (2)

β_i	λ_i
0.0001935	6.0798
.0009600	1.5788
.003143	0.3285
.001394	1.1260
.001780	0.0350
0.0002710	0.0127

and differentiating with respect to period (T) gives:

$$d\rho_{ih} \sim - 3.8768 \times 10^4 \sum_{i=1}^n \frac{\lambda_i \beta_i}{(1 + \lambda_i T)^2} dT \quad (3)$$

Figure 60 is a plot of reactivity error vs period for an error in period of 1 second. When longer periods are considered, the reactivity contribution is approximately of the same order of magnitude as the temperature correction; hence it can effect an appreciable error in the net value of reactivity.

²³G. R. Keepin, T. F. Wimett, "Delayed Neutrons," Paper 831, International Conference on the Peaceful Uses of Atomic Energy, Geneva, Switzerland (July 28, 1955).

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FIG. 59
PERIOD VS REACTIVITY
($\ell < 10^{-5}$ sec)

FIG. 60
REACTIVITY ERROR VS PERIOD
FOR AN ERROR IN PERIOD OF 1 sec

The inhour values of fuel rods as a function of distance from the center of the reactor are reported in ANL-5023.²⁴ A rod in hole No. 145 (3 in. from center of reactor) is worth about 60% of a rod in hole No. 1 (reactor center). It is interesting to note that the effectiveness of the three materials contributing strongly to reactivity (plutonium, enriched uranium, and polythene) is diminished by the same fraction.

2. Results

The reactivity contributions of the test samples are given in Table XIX.

Table XIX.

SUMMARY OF DANGER COEFFICIENT EXPERIMENTS

Sample	Sample Weight, g	Reactivity, inhours		Reactivity, inhours/ gram of sample	
		Hole 1	Hole 145	Hole 1	Hole 145
Plutonium	24.094	+36.2	+20.8	+1.5025	+0.8633
Nat. Uranium	49.531	+ 5.0	+ 5.5	+0.1009	+0.1110
Thorium	30.651	- 1.2	+ 1.5	-0.0392	+0.0489
Enriched Uranium	49.303	+37.9	+23.0	+0.7687	+0.4665
Indium	18.970	- 3.2	+ 1.6	-0.1687	+0.0843
Gold	50.281	- 3.4	+ 3.1	-0.0676	+0.0617
Polythene	2.152	+21.3	+13.4	+9.8978	+5.2268
Carbon	3.744	+ 3.2	+ 3.4	+0.8547	+0.9081
Natural Boron	1.870	- 1.9	- 0.6	-1.0160	-0.3209
Enriched Boron	3.253	-25.3	-11.3	-7.7774	-3.4737
Lithium-6	0.936	- 9.1	- 4.3	-9.7222	-4.5940
Iron (Drill Rod)	20.343	- 1.2	+ 3.0	-0.0590	+0.1475

Note:

1. Each sample was run and then inverted and run again. Two capsule runs accompanied the sample runs. The reactivity worth of the sample was obtained by subtracting the average of the two capsule runs from the average of the two sample (sample plus capsule) runs.

2. Inversion of the test sample did not introduce a reactivity effect greater than the experimental error. Those samples which were originally in powder form or in small pieces (boron, carbon, lithium) reproduced data as well as those originally in slug form.

3. The estimated accuracy of the results is no better than 2 inhours, and is due primarily to the inherent errors in determining period.

²⁴H. V. Lichtenberger, et al, "Progress Report on the Experimental Breeder Reactor," Fig. 11, p. 23.

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3. Recommendations

The following recommendations are made with respect to future danger coefficient experiments in EBR-I:

- a. All samples should be sealed in capsules of the same material.
- b. All capsules should be of the same length. The ends should be machined flat and square and should be free of surface irregularities.
- c. Slugs of fissile material which enter the thimble above the test sample should be enclosed in a tube and removed as a unit. Connect each sample capsule to the slug tube by means of a quick-disconnect device (detents, interrupted thread) and insert or withdraw the unit by vacuum or with a magnet.
- d. Since the unit becomes quite radioactive (even during low-power runs), provision should be made for storing the unit while test samples are being interchanged.

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

A. Irradiation of EBR-II Pin-Type Fuel Elements - J. E. Kemme, E. S. Sowa

Table XX summarizes the history of CP-5 irradiation tests, both completed and currently in progress, on EBR-II pin-type fuel elements.²⁵

The objectives of the tests are to: (1) learn of existence of any unforeseen effects on fuel element performance which may be brought to light solely under actual reactor operating conditions; (2) determine the extent of fuel alloy growth; and (3) determine the amount of fission product gas released.

The irradiation data for fuel element No. 1 are shown both graphically and pictorially in Fig. 61.

Failure of fuel elements Nos. 2, 3, and 4 occurred at the top of the fuel. Cooling air in the irradiation facility flows in the direction from top to bottom of the fuel element; therefore, the failures occurred in the region of most effective cooling. Since the melting of the uranium requires a high temperature, the only way a failure by melting could occur is by excessive fuel temperature caused by a void existing in the inner or outer sodium thermal bond. Such a void would act as a barrier to effective heat transfer and would cause the uranium metal temperature to rise to a very high value, probably exceeding its melting point. This molten uranium would form a eutectic alloy on contact with the stainless steel fuel cladding and would penetrate the wall. The presence of such voids in the thermal bond was verified during the irradiation experiments, since the thermocouples opposite the melted-through sections read lower than normal during the first day of operation. After this initial induction period, the thermocouple temperatures climbed to a higher value where they remained to the end of the run. This behavior could only be explained by the presence of a void acting as a heat transfer barrier until the uranium melted and closed the gap.

Photographs of the melted and unmelted sections of elements No. 3 and No. 4 are shown in Fig. 62. Some difficulty was experienced in removing these elements from the fin capsule and considerable force was exerted on the ends during the operation. This force probably caused the distortion visible in the photographs.

²⁵Reactor Engineering Division Quarterly Reports: ANL-5371 (January 15, 1955) p. 184; ANL-5461, (April 15, 1955) p. 113; ANL-5471, (September, 1955) p. 130; ANL-5511, (October 15, 1955).

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Table XX

SUMMARY OF CP-5 IRRADIATION TESTS ON EBR-II PIN-TYPE FUEL ELEMENTS

Test No.	Alloy and Metallurgical Treatment	Uranium Enrichment, %	Maximum Central Metal Temp, F	No. of Thermal Cycles*	Total Hours at 1000 kw	Estimated Burnup, at-%	Final Condition
1.	2% Zr-U Cast	15	1120	183	1374	0.38	Fuel element intact; few bumps on bottom section
2.	2% Zr-U Wrought, heat treated (800C); Furnace cooled	17	879	212	1316	0.43	Melted through tube at top for about 2 in.
3.	2% Zr-U Wrought, heat treated	20	829	268	2005	0.74	Badly melted along entire top half; was welded to fin tube interior
4.	2% Zr-U Wrought, heat treated	17	935	294	2227	0.73	Melted through top 2 in. of fuel
5.	2% Zr-U Cast; butt welded sections	20	715	219	1977	0.62	Completely intact; no bumps visible along tube
6.	2% Zr-U Wrought, heat treated	20	724	199	1680	0.62	Removed from CP-5; pending examination
7.	Fissium** Cast	20	-	50 (to 12-31-55)	-	-	In test

*Thermal cycles occasioned by reactor shutdowns, accidental scrams, and scrams resulting from checks of over-power scram circuits.

**96% U; 2.00% Mo; 1.20% Ru; 0.40% Pd; 0.24% Rh; 0.16% Zr.

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MELTED SECTION

LOWER UNMELTED
SECTION MELTED SECTION
IN FIN TUBE LOWER UNMELTED
SECTION

(a) ELEMENT NO. 3

(b) ELEMENT NO. 4

FIG. 62
EBR-II PIN-TYPE FUEL ELEMENTS NO. 3
AND 4 FOLLOWING IRRADIATION IN CP-5

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Elements No. 3 and No. 4 did not have their cladding removed. It is planned to perform this operation on elements 3, 4, and 5 simultaneously.

Element No. 5 was removed from its fin capsule with very little effort and was found to be in good condition (Fig. 63). It had been placed in the fin capsule with an 0.060-inch hole in its top and the interior thermal bond was continuous with the exterior bond through this hole. The exterior and interior bonds were sampled and will be analyzed for the presence of fission products. The ratio will show the extent to which fission products diffuse from a fuel element in this type of static system.

Element No. 6 was removed from test and is being prepared for removal from its fin capsule. Element No. 7 is currently in test.

B. Decay Heat Cooling Requirements of Irradiated Fuel Subassemblies - C. H. Scheibelhut

An electrically heated model of an EBR-II fuel subassembly was constructed to determine the cooling requirements to remove the decay heat after irradiation. The subassembly consists of a cluster of 91 closely packed, pin-type fuel elements contained in a hexagonal tube approximately 2.3 in. across flats. Under anticipated operation of the EBR-II, a subassembly will be removed from the reactor and stored in sodium for 15 days. During this period the decay heat is removed by natural convection of the sodium. After 15 days the maximum heat generation in a subassembly is approximately 2300 watts. When the subassembly is removed from the sodium, this heat is to be removed by forced convection of argon gas, the "atmosphere" of the disassembly cell into which the subassembly is transferred.

The initial work on this problem²⁶ established the gas circulation required through the subassembly after removal from the sodium. The cooling requirements have since been established for the subassembly during subsequent operations in the disassembly process.

Disassembly of the fuel subassembly consists of first removing the hexagonal tube and then removing the individual fuel elements. The hexagonal tube is cut at one end by a modified "tube cutter" and then pulled from the assembly. During this operation (and all preceding transfer operations) gas must be circulated through the subassembly. After the hexagonal tube is removed, the fuel elements are still held in a hexagonal array by the end grid structure to which each fuel element is attached (Fig. 64). The individual elements are now ready to be removed from the end grid structure.

²⁶Reactor Engineering Division Quarterly Report, ANL-5511, (January, 1956) p. 132.

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FIG. 63
TYPICAL APPEARANCE OF IRRADIATED EBR-II
PIN-TYPE ELEMENT NO. 5 BEFORE DE-CANNING

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FIG. 64
EBR-II FUEL CLUSTER AS REMOVED
FROM HEXAGONAL TUBING

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After removal of the hexagonal tube from the cluster of pins, axial gas cooling is no longer possible and cross-flow gas cooling through the cluster becomes necessary. To determine the cooling requirements of this configuration, the 91 electrically heated fuel tubes were removed from the hexagonal can and the hexagonal cluster was supported horizontally in air. With no forced cooling, the hottest tube achieved a temperature of 570C (1058F) with a power input of only 490 watts (21% of the expected decay heat power). The cluster was next supported in a perforated half-hexagonal tube to which was attached a manifold so that cooling air could be forced into the bottom and sides of the bundle. With 2200 watts heat input (95% of the expected decay heat power) and a cooling flow of 9.62 standard cubic feet per minute (scfm), a maximum fuel tube temperature of 645C (1193F) was recorded. To maintain this temperature while using argon as the coolant, a calculated 13.4 scfm would be required while the individual pins are being removed from the cluster. When the pins are kept separate, no further cooling is necessary. A single element, laid flat upon a table, was heated electrically at a rate corresponding to the decay heat developed in an irradiated fuel element. Under these conditions, the surface temperature was 225C.

Calculations were made to determine the rate of temperature rise in a subassembly if the coolant gas flow should fail. It was assumed that the subassembly was perfectly insulated. The rate of temperature rise vs the subassembly temperature is given in Fig. 65.

The cooling tests were run on a section of the subassembly containing only the fuel section. In practice, the cooling gas must flow through the entire subassembly, including the fuel section, upper and lower blanket sections, and the upper and lower attachments. Pressure drop vs flow data for air were obtained on a complete subassembly. From these data, the pressure drop of argon gas was calculated. Figure 66 shows the pressure drop vs flow of coolant gas relationship.

It can be concluded that forced gas cooling of the fuel subassembly will be necessary from the time that it is removed from the reactor sodium until the individual fuel pins are separated. A flow of 15 scfm of argon gas at room temperature at a pressure of about 3 in. H₂O will maintain a maximum temperature below 1100F. The dangerously high temperatures which would be attained in a very short time following cessation of coolant gas flow emphasizes the need for a reliable gas system.

C. Activation of Coolant Sodium by Fuel Element with a Hole in the End Closure - C. H. Scheibelhut

The EBR-II fuel element consists of a stainless steel tube into which is inserted a rod of fuel alloy. A 0.006-inch annulus between the fuel and tube is filled with sodium to effect a heat transfer bond. End closures

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FIG. 65
RATE OF TEMPERATURE RISE IN SUBASSEMBLY.
NO COOLING - 2330 watts DECAY HEAT

FIG. 66
PRESSURE DROP VS COOLING GAS FLOW
IN FULL-SCALE MODEL EBR-II FUEL SUBASSEMBLY

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are welded to the tube to seal the unit during operation. Since it is a sealed unit, it is necessary to provide very reliable filling of the sodium bond and also to provide space for sodium expansion inside the tube.

It is possible that some of the fuel elements will fail during reactor operation, and it is also possible that imperfect elements will be loaded into the reactor. Either event would permit the interchange of bond sodium and the flowing coolant sodium. Also of interest is the possibility of operating with fuel elements which have an opening in the top (downstream) end closure which would also permit the interchange of bond and coolant sodium. If this condition could be tolerated, the preparation of fuel pins would be simplified since the precise sodium filling, sealing, and testing of the individual fuel elements would be eliminated.

A loop was constructed to obtain data on the release of activity to the coolant sodium in a dynamic system from a fuel pin containing irradiated uranium and having a small hole in the top end closure.

The loop was constructed mainly of 2-inch pipe and contained an electromagnetic pump, electromagnetic flow meter, cold trap, expansion tank, test section, and sodium-sampling station. The loop volume was about 10 liters. The loop was heated electrically and could be controlled at any desired temperature up to 1000F (540C).

The fuel pin was made of stainless steel tubing (0.188 OD with an 0.008-in. wall and 18 in. long). A plug was welded into the bottom end and a spiral of stainless steel wire (0.065 in. in diameter with a 6-in. pitch) attached around the outside of the tube. An irradiated uranium rod (0.164 dia. by 14.22 in. long) was inserted into the tube and a top plug with a 0.040-in. diameter hole drilled through was tack welded to the tube. The test section consisted of a stainless steel rod (1 in. OD, 19 in. long) with a 21/64-in. hole drilled through. The fuel pin was placed in the test section so that the hole was at the downstream end, simulating possible reactor design.

To fill the test section and the annular space between the uranium and the fuel pin with sodium, the sodium in the balance of the loop was melted and a vacuum drawn on the entire loop. When the inert gas blanket was re-established, the sodium filled all these spaces. To prove this, a test run was made using non-irradiated uranium in the fuel tube. The test section was removed and the fuel tube torn apart. Sodium had filled all the spaces. The same observation was made at the conclusion of the test with the irradiated uranium.

Before the test section was placed in the loop, the loop was operated at 1000F (540C) until the operation of the cold trap reduced the oxygen content of the loop to 0.002% as determined by analysis. After the test section was inserted and filled with sodium, the pump was started and

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the temperature maintained at 1000F (540C). At one-week intervals, the temperature was lowered to 482F (250C) and immediately raised back to 1000F (540C). This induced a "breathing" of the sodium in the annular space of the fuel pin and promoted the escape of activity to the flowing stream. The test lasted 3500 hr and included 14 temperature cycles. A sodium velocity of 35 to 40 fps was maintained in the test section. Samples of the sodium were taken weekly and analyzed for activity. An activity survey of the exterior of the loop components was made weekly.

No activity was detected at any time in the sodium and the exterior surveys indicated only the background caused by the irradiated uranium. The uranium pin weighed 94 gm and was irradiated for 4 weeks in CP-5 at a flux of 1.2×10^{12} . The total integrated flux was 1.88×10^{18} . The uranium had decayed 150 days at the time the test started and contained 0.5 curie of fission product activity. At the end of the test, this activity had further decayed to 0.175 curie. The sensitivity of the analysis for activity in the sodium was such that 5.57×10^{-9} curie of activity could have been detected in the 10 liters of sodium in the loop. Since no activity was detected in the sodium, less than three parts in 100,000,000 of activity escaped to the coolant in 3500 hr. Using this figure as a limit and assuming a burnup of 1% in a three-month core life in EBR-II, 0.265 curie of activity would be released to the coolant sodium every three months.

D. Studies of Uranium - Iron Eutectic Formation in EBR-II-Type Fuel Elements - E. L. Currier

A series of tests have been made to determine the compatibility of the materials comprising the EBR-II fuel element design, with particular reference to the possible eutectic point formation of uranium with iron in the stainless steel fuel tube.

The elements used in these tests were of two sizes: (1) the uranium was 0.164 in. diameter, the tube cladding was 0.172 in. ID; and (2) the uranium was 0.101 in. diameter, the tube cladding was 0.109 in. ID. In both cases, the Type 304 stainless steel tube wall thickness was 0.008 in., the uranium was 14-1/4 in. long, and the radial annulus was 0.004 in. The materials were all degreased with trichloroethylene, pickled in dilute HNO_3 , rinsed, dewatered with absolute ethyl alcohol, and vacuum dried. They were canned with sodium in the annulus between the uranium pin and the tubing, and were closed with welded end plugs. Approximately 1/2 in. of sodium extended above the top of the uranium.

The sample elements were tested in static heat tube furnaces. The test conditions and results are listed in Table XXI. The design fuel element lifetime is presently 90 days.

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Table XXI

SUMMARY OF TESTS ON TYPE 304-CANNED
PIN-TYPE FUEL ELEMENTS

<u>Sample No.</u>	<u>Number Tested</u>	<u>Type of Element</u>	<u>Test Time, Days</u>	<u>Test Temp., F</u>	<u>Number of Cycles</u>	<u>Comments</u>
1	1	large	280	1000	none	Still under test
2	4	large	240	1100	none	Note 1
	12	small	122	1100	none	None decanned
3	5	large	240	1150	none	Note 1
4	3	large	196	1200	none	Note 2
	12	small	89	1200	none	
5	8	small	138	1380	none	Note 1
6	12	small	122	1100	69	Note 1
7	12	small	89	1200	58	Note 3
8	4	small	138	1380	72	Note 1

1. One element decanned. No evidence of metallic fusion.
2. Temperature controller failed with switch "on"; samples were melted.
3. After opening the furnace at a later period, the elements were found to be melted.

The thermal cycle was achieved by merely turning off the temperature controllers and allowing the temperature to fall over a 6-hour period. The heat would then be turned on again for the balance of the 24-hour period.

The test samples pictured in Fig. 67 show the integrity of the cladding wall and the straightness of both the static and thermal cycled samples. The anomalous results of sample No. 7 are attributed to temperature instrument failure in view of the successful performance of samples No. 5 and 8.

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Fig. 67
Appearance of Fuel Elements Used in Uranium-Iron
Eutectic Formation Tests

E. Control Rod Mechanism, Mark-A - E. Hutter

Figure 68(a) shows the temporary test setup of the Mark-A control rod mechanism. The position of the controls within the EBR-II primary system is shown in Fig. 68(b).

The control mechanism proper is mounted on a platform which can be raised 5 feet in order to retract the gripper part of the mechanism from the reactor tank cover. This allows the reactor tank cover to be rotated away from the reactor tank, thus giving access to the fuel subassemblies.

The gripper which holds the control subassembly is connected by a long shaft to the piston of the pneumatic cylinder on top of the mechanism. Air pressure in the cylinder acts against the piston in a downward direction at all times, causing the drive and rod to scram when the latch connections between the shaft and the control rack tube are separated.

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FIG. C8
EBR-II CONTROL DRIVE MECHANISM (MARK-A)

A motor and gear assembly drives the control rack tube over a 15-inch distance, overriding the scram pressure in the pneumatic cylinder as long as the latch mechanism is engaged.

Figure 68(c) shows the latch mechanism for the fast release of the shaft from the control rack tube. Two rollers are engaged in two notches on the shaft and are held in this position by magnetic clutches. Upon opening the electric circuit to the magnetic clutches, the two rollers are allowed to swing out of the notches and the control rod scrams. A hydraulic shock absorber below the pneumatic cylinder absorbs the energy of the mechanism at the bottom of the scram stroke.

Additional instruments are being installed prior to starting the preliminary test program.

F. Shielding Analysis - E. F. Fricke

The total number of fissions in the core structure, the gamma-ray source densities throughout the core and shielding, the fast neutron densities in the core structure and the gamma, fast and thermal neutron fluxes have been calculated for both radial and axial spherical representations of the EBR-II core. Spherical integration over the core structure indicates that the source of gamma and fast neutron flux is the outer blanket. This is due to the high attenuation for both gamma and fast neutrons beneath this blanket. However, the delay gammas originating from the Na^{24} atoms produced in the core coolant passages, and held up in the sodium storage tank, provide the chief source of gamma rays for which shielding must be provided on the outside boundaries of the reactor structure by means of concrete. Figure 69 shows the axial and radial gamma flux plots through the shields.

The contribution to the original delay gamma source densities in the sodium storage tank due to flow of coolant from the core, by thermal neutron absorption in the sodium atoms in the storage tank, has been calculated. The calculations assumed that the thermal neutron flux is zero at the outer boundary of the thermal neutron shield as well as at an infinite distance in the concrete. The source term in the diffusion equation has been estimated by two methods: (a) assuming the source term is equal to the negative of the gradient of the fast neutron flux through the sodium tank; and (b) by assuming that the source term is equal to the product of the fast neutron diffusion constant and the divergence of the gradient of the fast flux in the same region. In Fig. 70(a), ϕ_s is based upon assumption (a) while ϕ_s' results from assumption (b). Figure 70(b) shows the axial fast neutron flux.

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FIG. 69
CALCULATED RADIAL (a) AND AXIAL (b) GAMMA
FLUX IN EBR-II. (SPHERICAL GEOMETRY)

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FIG. 70
CALCULATED RADIAL (a) AND AXIAL (b) NEUTRON
FLUX IN EBR-II. (SPHERICAL GEOMETRY)

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G. Homopolar Generator - A. H. Barnes

The homopolar generator which is being developed as a direct-current source for driving a 10,000-gpm electromagnetic pump has been operated at low power in its initial tests. This machine has been designed to supply currents in excess of 250,000 amperes at 2.5 volts. The generator will normally be driven by a 1250-hp induction motor but, because of present lack of handling and power supply facilities, the tests are being carried out with a 100-hp motor drive. The purpose of these preliminary tests is to determine the no-load characteristics of the machine and to obtain some information on its behavior when delivering moderate currents at low voltage. Under these restricted conditions the machine has been operated without load at terminal potentials up to 1.4 volts and at output currents up to 130,000 amperes.

The mechanical behavior of the generator was very satisfactory. There was negligible rotor vibration and no evidence of excessive shaft whip during start-up. No difficulty was experienced with NaK leakage at the rotor shaft seal and very little oxide formed in the inert gas chamber at the top of the machine. It has been found that the mechanical losses (principally viscous drag on the rotor) were close to the calculated value (17.5 kw at 1800 rpm), but the open circuit electrical losses were higher than expected. The origin of these losses has been traced to electrical leakage in one of the "insulating" sleeves which surround the rotor and to the presence of local circulating currents in the vicinity of the current collector rings. The circulating current losses are due to magnetic field fringing at the extremities of the core structure.

The machine has been disassembled so that new sleeves may be installed. Before reassembly, iron will be removed from both core and rotor to reduce field fringing at the collector rings.

III. EBR-II WORKING MODEL

A. Fuel Unloading System - E. Hutter

As a results of earlier tests,²⁷ several components of the fuel unloading system have been removed from the primary tank and the following changes made:

Gripper Mechanism: The gripper jaws were redesigned and a fuel rod-ejecting feature was incorporated.

Hold-down Mechanism: Holes were drilled into the bearing support tube above the sodium level to prevent siphoning of sodium into the bellows shaft seal.

Transfer Arm: Lead-in angles on the forked pick-up end were enlarged.

Storage Rack: Clearances on the fuel rod-holding tubes were enlarged to compensate for differential thermal expansion in the system.

Storage Rack Transfer Stop: Transfer stop was redesigned to eliminate binding.

Transfer Arm Shield Plug: A new plug was fabricated allowing more clearance for positioning in the primary tank top shield.

Large Rotating Shield Plug: Radial clearance between the plug and the liner was enlarged.

All components have been replaced in the primary tank for further performance-evaluation tests.

B. Sodium Purification - J. R. Humphreys

Model Tank operations were suspended to permit the removal of several of the larger components. The sodium level in the tank was lowered and the residual sodium frozen (175F) before the units were removed from the tank. The components were then withdrawn directly through their access openings in the tank cover, exposing the frozen tank sodium to the atmosphere for a period of approximately three days. As each of the components was exposed to air upon removal, the residual sodium films ignited. The components remained suspended over the openings in the tank cover

²⁷Reactor Engineering Division Quarterly Report: ANL-5511, (October 15, 1955) p. 146.

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plate while the sodium burned, with the result that appreciable quantities of burning sodium and sodium oxide flakes dropped into the tank on the frozen sodium surface. Figure 71 shows the resulting condition of the sodium surface as photographed through an open manhole in the tank cover.

Following reinstallation of the test components, the tank was sealed and the residual sodium melted. When the tank sodium temperature equaled that of the sodium in the storage tank (250F), purified sodium was added from the storage tank to bring the model tank sodium to operational level. The sodium temperature was then increased to permit cold trap purification. Continuous by-pass cold trap cleanup operations were started when the sodium attained a temperature of 340F. The cleanup operation was continued for 18 hr while the system temperature was raised to the control point of 600F, and for 27 hr after reaching the control temperature. At this time a sample of the system sodium was taken using the vacuum cup sampler. Analysis showed a dissolved oxygen concentration of 0.001 wt-%. At the time the sample was taken the sodium surface appeared bright and lustrous.

During system cleanup operation the cold trap temperature was maintained between 275 and 325F. Holdup time in the crystallizer was five minutes with a flow rate such that one system volume passed through the cold trap every six hours.

The results of the cleanup operation are highly significant since the sodium surface shown in Fig. 71 exceeds the most severe condition anticipated for shutdown oxygen contamination in EBR-II. Thus in rapidly accommodating this quantity of oxide contaminant, the prototype cold trap cleanup system has demonstrated very satisfactory operational characteristics.

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Figure 71

Appearance of Surface of Bulk Sodium in Tank Following
Removal of System Components

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IV. SUPPORTING AND ALTERNATE DESIGN RESEARCH AND DEVELOPMENT

A. Thermal Cycling of Molybdenum-Uranium and Zirconium-Uranium Alloys - J. E. Kemme, H. O. Monson

A series of thermal cycling tests to determine relative thermal stability of various high-percentage uranium alloys has been completed. Of particular interest is the effect produced by cycling these alloys above 656C, which is the alpha-beta transformation temperature of unalloyed uranium.

Tentative conclusions based upon earlier test results²⁸ were that low-percentage molybdenum-uranium alloys may permit reactor operation above the alpha-beta transformation temperature, provided their irradiation damage resistance is satisfactory.

In continuing this work, studies were made of two alloy compositions (1.4% molybdenum-uranium and 2% zirconium-uranium by weight) fabricated in five different ways (Table XXII). A total of 40 samples were prepared and divided into four groups. Each group consisted of two samples representative of each method of fabrication.

Table XXII

FABRICATION HISTORY OF Mo-U AND Zr-U TEST SPECIMENS

No.	Alloy	Fabrication
	Composition	
1	1.4% Mo-98.6% U	Cast, wrought, gamma treated
2	1.4% Mo-98.6% U	Sintered powder, wrought, gamma treated.
3	2.0% Zr-98% U	Sintered powder, wrought, gamma treated.
4	2% Zr-98% U	Cast, wrought, gamma treated.
5	2% Zr-98% U	As cast.

Alloys 1, 2, and 3 were prepared by Sylvania Electric Products.
Alloys 4 and 5 were prepared by Argonne Metallurgy Division.
Wrought is defined as swaging at 550-600C.

²⁸ Reactor Engineering Division Quarterly Report, ANL-5511, (October 15, 1955) p. 151.

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Gamma treatment for Sylvania samples is defined as heating the swaged specimen at 800C for 15 min followed by water quenching.

Gamma treatment for Argonne samples is defined as heating the swaged specimen to 800-810C for 8 min followed by furnace cooling in vacuo (5×10^{-5} mm) to room temperature in 11.3 hr.

"As cast" consists of statically casting the specimen oversize and centerless grinding to size to remove a majority of surface imperfections.

All of the specimens were thermal cycled as follows: The samples were moved alternately from hot to cold NaK at specified temperatures (see tabulation). The immersion time at each temperature was 3 min. The transition time between temperatures was 10 sec.

Group No.	Temp. Cycled, F	Cycles	Condition
A	150-1150	200	Bare in NaK.
B	150-1150	200	Canned in sodium.
C	200-1320	200	Bare in NaK.
D	200-1320	200	Canned in sodium.

Bare samples are described as samples standing loosely on end in a basket.

The canned samples were contained in a Type 304 stainless steel tube of 0.008-inch wall thickness with a 0.006-inch annulus of sodium surrounding the sample. Initially, the sample was unrestrained both longitudinally and normal to the axis. The growth gap was sufficiently large so that the samples were never restrained longitudinally.

Groups A and B were thermal cycled together, as were groups C and D; thus identical cycling conditions were obtained on all samples in each group.

Examination of the pictured samples and included data (Figs. 72 and 73) yielded the following observations:

1. Samples Cycled Between 150F and 1150F (Groups A and B)

a. Length and weight changes of the canned samples were less than for the bare samples.

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BARE IN NaK

FULL SIZE VIEWS
SHOWING OVER-
ALL DISTORTION

MAGNIFIED VIEWS
SHOWING SURFACE
CONDITION (4X)

COMPOSITION, %	1.4 Mo-U	1.4 Mo-U	2 Zr-U	2 Zr-U	2 Zr-U
FABRICATION	CAST	← SINTERED POWDER →	CAST	CAST	CAST
HEAT TREATMENT	← WROUGHT AND GAMMA HEATED →		→	NONE	
LENGTH CHANGE, %	+1.7	+0.1	+0.7	+1.4	+0.6
DENSITY CHANGE, %	-1.6	-1.5	-1.4	-0.9	-1.3
WEIGHT CHANGE, %	0	+0.1	+0.2	-0.7	+0.2

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CANNED AND Na BONDED

1.4 Mo-U	1.4 Mo-U	2 Zr-U	2 Zr-U	2 Zr-U
CAST	← SINTERED POWDER →	CAST	CAST	CAST
← WROUGHT AND GAMMA HEATED →		→		
+0.1	-0.1	+0.2	+0.3	-0.8
0	+0.1	+0.1	0	+0.1
0	0	0	0	0

FIG. 72
HIGH-URANIUM ALLOY FUEL PINS CYCLED
200 TIMES FROM 150 F TO 1150 F.
HOLDING TIME: 3 min. TRANSITION TIME: 10 sec.

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BARE IN NaK

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CANNED AND Na BONDED

FULL SIZE VIEWS
SHOWING OVER-
ALL DISTORTION

MAGNIFIED VIEWS
SHOWING SURFACE
CONDITION (4X)

COMPOSITION	1.4 Mo-U	1.4 Mo-U	2 Zr-U	2 Zr-U	2 Zr-U	1.4 Mo-U	1.4 Mo-U	2 Zr-U	2 Zr-U	2 Zr-U
FABRICATION	CAST	← SINTERED POWDER →		CAST	CAST	CAST	← SINTERED POWDER →		CAST	CAST
HEAT TREATMENT	← WROUGHT AND GAMMA HEATED →				NONE	← WROUGHT AND GAMMA HEATED →				NONE
LENGTH CHANGE, %	+0.6	+0.5	-3.0	+4.3	+5.0	0	0	-1.2	+5	+11.5
DENSITY CHANGE, %	-1.7	-1.3	-1.3	-0.8	-1.1	-0.2	-0.3	-0.9	0	-0.6
WEIGHT CHANGE, %	+0.1	+0.1	-1.1	-0.9	-1.0	0	0	0	0	0

FIG. 73
HIGH-URANIUM ALLOY FUEL PINS CYCLED
200 TIMES FROM 200 F TO 1320 F.
HOLDING TIME: 3 min. TRANSITION TIME: 10 sec.

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b. Density changes of the bare samples were approximately 1% as compared to 0.1% in the canned samples.

c. The distortion of the canned samples was less.

d. The surfaces of the canned samples were smoother.

e. The cast, wrought, gamma-treated, 2% zirconium-uranium alloy (No. 4) developed the roughest surface in both the bare and canned condition and showed the greatest weight loss among the bare samples.

2. Samples Cycled Between 200F and 1320F (Groups C and D)

a. The beta-cycled zirconium-uranium alloy specimens were rougher, more warped, and had greater changes in length than the alpha-cycled specimens.

b. The beta-cycled molybdenum-uranium alloy specimens exhibited about the same roughness and warpage as the alpha-cycled specimens.

c. Of the three types of zirconium alloys, the wrought and gamma-treated alloy (No. 4) had the roughest surface. All three samples were noticeably roughened.

d. Comparing the bare and canned molybdenum samples, the canned samples exhibited a zero change in length compared to 0.5% increase in length for the bare specimens. The canned samples also showed less weight and density change.

The following general conclusions may be drawn from these data:

1. The molybdenum-uranium specimens were relatively unaffected by cycling through the alpha-beta transformation.

2. Canning of the molybdenum-uranium samples reduced minor distortion present in both alpha and beta-cycled bare specimens. Surface condition was particularly improved by canning; density and length changes were reduced. Canning also eliminated weight changes in all specimens whether molybdenum or zirconium alloys and whether alpha or beta cycled.

3. Weight changes for the 1.4% molybdenum-uranium heat-treated samples were considerably less than those reported for the 1.4% molybdenum-uranium "as sintered" specimens reported in ANL-5511.

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4. Whenever surface roughening occurred (especially with the zirconium-uranium alloys), it was accompanied by a weight loss of the bare specimens. Since no weight change was found in the respective canned specimens, it is concluded that weight loss is the result of, rather than the cause of, surface roughening.

5. Changes in length, volume, and weight as well as the amount of distortion of the zirconium-uranium samples, were increased by cycling across the alpha-beta transition range. Canning of the specimens reduced but did not eliminate some of these effects. Surface roughening and elongation was particularly noticeable.

B. 800C d-c Electromagnetic Pump Na Test Loop - A. H. Barnes,
R. A. Stella, F. A. Smith

The primary objective of this test program is the development of d-c electromagnetic pumps which will operate satisfactorily at high temperatures.²⁹

Three pump tubes have been tested. The first tube failed after 150 hr at 600C. The second tube failure occurred after 160 hr at 800C. The third and most recent failure occurred after 3,254 hr of continuous operation at 800C (1472F). The failure did not occur at a weld joint but in the pump tube wall 3 in. from the region of high d-c current [transition of the flattened to round section of the pump tube on the downstream end (see Fig. 74)].

Samples cut from the tube indicate that the breaking of the wall occurred where forming operations in manufacture had produced a thin cross section. The Nichrome pump tube material appears to have undergone a general corrosive attack, corrosion products being deposited predominantly at the sides of the tube that are integral with the current conductors. This can be described as a mass transport phenomenon where corrosion products deposit in the relatively cooler region of the d-c current electrodes.

The test temperature is in the hot or short brittleness range (1200-1660F) of Nichrome. For a design stress of 1660 psi, the allowable internal pressure at the unsupported flattened section of the pump tube is about 10 psi. This value suggests the significance of a small change in wall thickness. Examination of the piping loop after failure has pointed out that a condition of stress greater than 10 psi may have been imposed upon the pump tube. The pipe support roller connecting the loop to its dump tank had "frozen" due to oxidation of steel at high temperature in air. This support was near the inlet to the pump tube and thermal expansion against the non-movable roller could have contributed to tube failure.

²⁹Reactor Engineering Division Quarterly Report, ANL-5345,
(October 15, 1954) p. 109.

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FIG. 74
HIGH-TEMPERATURE SODIUM TEST LOOP
WITH d-c ELECTROMAGNETIC PUMP

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Significant experience has been gained in simply having a Na loop fail severely at 800C. The high-temperature test facility is shown in Fig. 74. Anticipating the consequences of failure of an 800C Na loop, the loop is contained on all sides by a sheet metal box. The bottom of the box is "curbed" to contain more liquid than the contents of the loop. The sheet metal sides are screwed to an angle iron frame and connections to power sources, gas line, etc. are only relatively airtight.

Within several minutes of failure the dump valve was opened. Later observations showed this action was unnecessary because the pump tube failed over a length of several inches and the Na had spilled into the "curbed" bottom tray before the dump valve was opened. The smoke resulting from Na confined in a relatively airtight box was not too severe. Two men disconnected all leads and pushed the loop, mounted on casters, onto an outdoor liquid metal disposal dock. The process of smothering the fire and containing the smoke which occurred in approximately 20 min was aided by plugging all small cracks and lead holes with bits of Banroc mineral wool insulation. The Na remained very hot in the bottom tray and inspections 8 hr later after cooling showed that the entire contents of the loop (about 5 gal) were in the bottom tray. It was apparent that, by excluding air, the Na surface was blanketed by Na_2O which inhibited further combustion.

Another such loop, equipped with a "cold finger" and an oxide-sampling facility, will be placed in operation upon completion of metallurgical examinations of the pump tube and loop welds.

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