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Lemont, Illinois

REACTOR ENGINEERING DIVISION QUARTERLY REPORT
ON THE POWER REACTOR PROGRAM

January 1, 1955 through March 31, 1955

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TABLE OF CONTENTS

	<u>Page</u>
I. EXPERIMENTAL BOILING WATER REACTOR (EBWR)	11
A. General	11
B. Building	11
C. Power Plant	12
1. Pressure Vessel	12
2. Design Evaluation of EBWR in Regard to Internal Explosion	15
3. Steam Pipe Rupture Studies	19
4. Steam By-pass System	21
5. Impurity Carry-over in a Boiling Process	21
6. Local Boiling Pressure Drop in Vertical Tubes	22
D. Reactor Core	23
1. Feasibility Tests on Mock-up of EBWR Core	23
2. Alternate Control Rod Design Studies	28
3. Control Rod Drive Mechanism	28
4. Boric Acid Crystal Injunction Tests	29
5. Zircaloy 2 - Stainless Steel Contact Corrosion	32
E. Fuel Studies	34
1. Stress Analysis of EBWR - Type Fuel Plates and Subassemblies	34
2. Freely Expanding, Guided EBWR Fuel Plate Subassembly	46
3. Corrosion and Irradiation Tests	48
F. Physics Studies	62
1. The Effect of the Spatial Distribution of Voids on Reactivity	62
2. Temperature and Void Coefficients	69
3. EBWR Shielding Calculations	69
4. D ₂ O Boiling Reactor Studies	79
II. FAST POWER BREEDER REACTOR PROGRAM	85
A. The EBR-II Working Model	85
1. EBR-II Working Model	85
2. Performance Tests on EBR-II Working Model Components	85
3. 1000-gpm, d-c Electromagnetic Pump	89

DECLASSIFIED

	<u>Page</u>
B. Coolant Studies	89
1. Low-Frequency Induction Heating of Sodium System	89
2. Large Size Conventionally Packed Valves for Liquid Metal Systems	95
3. Heat Transfer Characteristics of Liquid Metals. . .	100
4. Design Study No. 1 Secondary and Steam System Conditions.	104
C. Fuel Studies	108
1. Thermal Analysis of Radiator Type No. 1 Fuel Element	108
2. Irradiation of EBR-II Pin-Type Fuel Elements . . .	113
3. Thermal Cycling of EBR-II Pin-Type Fuel Elements	119
D. UNIVAC Calculations on EBR-II	119

037128.030

LIST OF FIGURES

<u>Figure</u>	<u>Title</u>	<u>Page</u>
1	Effect of Main Steam Pipe Failure on Pressure and Water Volume in the EBWR. (No Feed Water Added.)	20
1a	Comparison of Test Data with Rohde and Reynolds Correlation for Local Boiling Pressure Drop	24
2	Mock-up EBWR Core Section for Feasibility Tests.	25
3	Fuel Assembly Grappling Mechanism.	27
4	Comparison of Rod Deceleration Effected by Internal and External Dashpot Designs	30
5	Hydraulic Control Rod Drive Schematic Free-Floating Piston Type.	31
6	Boric Acid Concentration in Test Tank as a Function of Time and Nozzle Geometry	33
7	Shear Stress in Pin Connection Due to Relative Thermal Expansion of Fuel Plate and Side Plate.	39
8	Welded EBWR-Type Fuel Plate Subassembly.	40
9	Minimum Creep Rate vs. Stress of Alpha-Annealed Zircaloy 2 at 343 C.	43
10	Freely Expanding, Guided EBWR Fuel Plate Subassembly .	47
11	Unclad Uranium-Niobium-Tin and Uranium-Silicon Alloy Pins Irradiated to 0.03% Burnup in CP-5 Autoclave Test Facility (Run No. 7)	53
12	Unclad Thoria-Urania Body (2.5%-Fully Enriched Urania) Irradiated to 0.8% Burnup of Uranium Atoms in 500F-515F Water (CP-5 Autoclave Test Facility).	57
13	In-Pile Test Samples of Nickel-Plated 2S Aluminum and Aluminum-Nickel Alloy Plates Showing Magnetite Deposited on Plates Exposed for 530 hr to 500F Water and 6×10^{20} nvt (maximum) in ANL Loop at MTR.	61

DECLASSIFIED

LIST OF FIGURES

<u>Figure</u>	<u>Title</u>	<u>Page</u>
14	2S Aluminum-1/2% Nickel Alloy Plate Exposed for 530 hr to 500F Water and 6×10^{20} nvt (maximum) in ANL Loop at MTR (Magnification:7x)	63
15	Reactivity Change vs. Fraction of Core Radius Containing 20% Void.	67
16	Reactivity as a Function of Water Temperature and Density in EBWR. 36.6% of Fuel Elements Enriched.	70
17	Calculated Fast Neutron Flux in EBWR Shielding	71
18	Calculated Thermal Neutron Flux in EBWR Radial Shielding.	72
19	Calculated Thermal Neutron Flux in Water and Iron above the EBWR Core	73
20	Calculated Axial Thermal Neutron Flux below the EBWR Core	74
21	Calculated Gamma-Ray Flux in Radial EBWR Shield.	75
22	Calculated Axial Gamma-Ray Flux in Iron Shield above the EBWR Core	76
23	Calculated Gamma-Ray Flux below the EBWR Core	77
24	Calculated Heat Generation in EBWR Shielding	78
25	Sectional Views of 20-mw D ₂ O Boiling Reactor and of Fuel Tube	80
26	Enrichment Required for Criticality (E_{CR}) vs Concentration (x) of H ₂ O in D ₂ O - Moderated Boiling Reactor.	81
27	Variations in k_{∞} , L, and k_{eff} for Cold, Hot, and Boiling D ₂ O and H ₂ O + D ₂ O Moderated Reactors (without Xenon + Samarium)	84
28	Calrod Heater Units Installed in Outer Tank of EBR-II Working Model.	86

0371228.030

LIST OF FIGURES

<u>Figure</u>	<u>Title</u>	<u>Page</u>
29	EBR-II Working Model Tank Assembly.	87
30	Sodium Storage Tank (6,000-gallon capacity) for EBR-II Working Model.	88
31	Composite of EBR-II Working Model in "Operating Position"	90
32	Sequence of Operation of Fuel Transfer System - EBR-II Working Model.	91
33	Liquid Metal Duct and Magnet Assembly - 1000-gpm, d-c Electromagnetic Pump for EBR-II-Working Model . . .	92
34	Insulated Carbon Steel Pipe Shell (18 in. IPS)	96
35	Test Apparatus Used to Evaluate 14-inch Gate Valve for Liquid Metal Systems	98
36	Sectional View of 14-inch Gate Valve Test Assembly	99
37	Fully Developed Heat Transfer Data for Eutectic NaK Flowing Turbulently in a Copper Tube (0.25 in. ID x 0.55 in. OD x 36 in. long)	102
38	Copper Tube Test Section after Burnout. Burnout Conditions: 320 lb/hr flow; 6×10^6 Btu/(hr)(sq ft) Maximum Flux; 1175F Maximum Fluid Bulk Temperature; 1800F Maximum Tube Temperature.	103
39	Electromagnetic Pump Tube Failure Attributed to Gas Entrapment	105
40	Skeleton Flow Chart - EBR-II Design Study No. 1.	106
41	Temperature-Enthalpy Diagram for Steam Generator - EBR-II Design Study No. 1	107
42	Cross Section of Radiation-Type No. 1 Fuel Element Subassembly	110

DECLASSIFIED

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LIST OF FIGURES

<u>Figure</u>	<u>Title</u>	<u>Page</u>
43	Axial Temperature Distributions at Innermost Edge of Core (Radius = 8.18 cm) using Radiator-Type No. 1 Fuel Element (Uncertainty Factors Included)	112
44	Schematic of Irradiated Pin-Type Fuel Element with Enlarged Views of Tube and Fuel Slug Surfaces (Magnification:4x).	114
45	Pin-Type Fuel Elements Thermal-Cycled 200 Times in NaK from 450F to 1330F	120
46	Vertical Section of Design Study No. 1	121

LIST OF TABLES

<u>Table</u>	<u>Title</u>	<u>Page</u>
I	Maximum Steady-State Thermal Stresses in Natural Uranium Alloy Fuel Plate	36
II	Corrosion of High-Uranium Alloys in 500F and 550F Water	49
III	Effect of NaK Pretreatment on Corrosion of Uranium-1-1/2% Niobium-5% Zirconium in 500F Degassed, Static Water	51
IV	Corrosion-Irradiation of High-Uranium Alloy Pins in CP-5 Autoclave Facility	52
V	Out-of-Pile Water Tests on Thoria-Urania Bodies	56
VI	Weight Change Rates for Aluminum and Aluminum-Nickel Alloy in Static, Degassed Water at High Temperature	59
VII	Perturbation Coefficients for 20% Void	66
VIII	Critical Constants	66
XI	Reactivity Change Due to Voids	68
X	Core Composition	79
XI	Shield Thickness on Core Centerlines	79
XII	Operating Characteristics of 20-mw Unit for Different Concentrations of H ₂ O in D ₂ O	82
XIII	Summary of Packing Gland Tests	100
XIV	Operating Conditions - EBR-II Design Study No. 1	108
XV	Summary of Thermal Analysis Results for "Hottest" Regions of Core and Upper Blanket Using Radiator-Type No. 1 Fuel Element	111
XVI	Fuel Alloy Slug Measurements	118

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REACTOR ENGINEERING DIVISION QUARTERLY REPORT ON THE POWER REACTOR PROGRAM

I. Experimental Boiling Water Reactor (EBWR) - J. M. West

A. General

A hazards evaluation report on the EBWR facility¹ was reviewed by the Advisory Committee on Reactor Safeguards at its February 4 meeting. The Committee concluded that operation of this reactor at 20 mw in a gastight steel building as proposed should be an acceptable low risk. The Atomic Energy Commission thereupon formally approved construction of the EBWR at the DuPage County site of the Laboratory.

The preparation of construction drawings and specifications on the building and power plant, exclusive of the reactor, has been completed by the engineering firm of Sargent and Lundy. Two main construction contracts will be awarded. The steel shell which houses the reactor and the primary steam circuit will be constructed by specialists in steel fabrication. Bids on this job will be opened early in April. The remainder of the building and power plant will be awarded to a general contractor after opening of bids early in May.

The Babcock and Wilcox Company was awarded a contract for supplying the pressure vessel for the reactor. Design detail and material specification questions are now being resolved in meetings between Babcock and Wilcox and Argonne personnel. Delivery of the pressure vessel is scheduled for January 1, 1956.

B. Building - A. H. Heineman

The building specifications and drawings prepared by Sargent & Lundy were reviewed jointly by EBWR staff engineers and representatives of the architectural-engineering firm. The specifications and layouts were revised to incorporate Argonne-designed, pressure-tight doors for the inner and outer bulkheads of the access air-lock through the Power Plant Building Shell. The final specifications were mailed to prospective bidders on March 23, 1955.

The pressure-tight doors (approximately 3 ft wide by 7 ft high) are designed to seal against 15-psi differential pressure on either side. Pressure-equalizing valves on each bulkhead are geared into the operating

¹J. M. West, et al, "Hazard Summary Report on the Boiling Experimental Reactor," ANL-WHZ-390 (January, 1955).

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mechanism of each door to equalize the pressure differentials before the unlatching mechanism will function. The operation of the mechanism is completely manual; however, a mechanical interlock prevents both doors from being opened simultaneously except under emergency conditions. In the event of an emergency, the interlock may be overridden by a lever at each station.

The recent turbine rotor failure at the Ridgeland Power Station (near Chicago) served as a model to develop relations which would afford an evaluation of the destructive potential of a similar incident in the EBWR turbine. The calculations were based on limited factual data and assumptions pertinent to the rotor failure. The close agreement of the calculated effects with the circumstances surrounding the Ridgeland disaster was considered ample justification for these assumptions.

The results of analogous calculations of a hypothetical turbine rotor failure in the EBWR indicated that any and all fragments would be contained by the turbine casing. However, by assuming that, in some extraordinary manner, the rotational energy of the most dangerous fragment (one-half wheel) is converted into linear motion, the fragment could pierce the turbine casing and emerge with considerable velocity. Under these circumstances, the calculations indicate the fragment would be contained by the 12-inch reinforced concrete envelope to be built inside the building shell.

Further study of crane types has shown that a circular crane rotating on a rail supported by the concrete lining of the steel shell is more useful and less expensive than the rectangular crane system previously shown in the design. Therefore, the circular crane has been adopted.

C. Power Plant

Almost all power plant equipment has been ordered or will be included in the general construction contract for the EBWR facility. An exception is the steam dryer-emergency cooler, which is so unconventional that it will be obtained by Argonne under a separate purchase order or contract.

1. Pressure Vessel - N. Balai, A. H. Heineman, L. C. Livesey

A detailed analysis of six vendors' quotations on the EBWR Pressure Vessel showed that the Babcock and Wilcox Co. was the lowest bidder. A vessel contract was signed with an effective date of February 1, 1955 for the delivery of pressure vessel on January 1, 1956.

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In addition to the provisions of the general vessel specifications prepared by Sargent and Lundy, the Babcock and Wilcox Co. recommended a thin ($1/8$ in.), Type 304 stainless steel shock shield in the upper section of the pressure vessel to eliminate an accidental cold water thermal shock hazard to a hot pressure vessel wall. This recommendation has been adopted for the EBWR pressure vessel.

In subsequent conferences with the Babcock and Wilcox Co., the general design involving the strength shell of the pressure vessel has been fixed to expedite procurement of steel plate, forgings and cladding. The cylindrical shell section is to be $2-1/4$ in. thick and clad with 0.109-in. thick Type 304 stainless steel; the lower 2:1 ellipsoidal head is to be $3-9/16$ in. thick clad with 0.109-in. thick Type 304 stainless steel; the upper ring forging is to be a truncated cone with a maximum thickness of approximately $6-1/2$ in. clad with $3/16$ in. of Type 304 stainless steel; and the flat head closure is to be approximately $9-1/2$ in. thick clad with a $3/16$ in. thick Type 304 stainless steel. The strength members are to be fully killed, aluminum-treated carbon steel made to a fine grain practice and are to meet the following specifications:

<u>Components</u>	<u>Material</u>
Shell Plates	SA-212 Firebox Quality, Grade B
Lower Head	SA-212 Firebox Quality, Grade B
Upper Ring Forging	SA-105 Grade II
Flat Head Closure	SA-105 Grade II
Bolting	SA-193 B14

The 6-8 (McQuaid-Ehn grain size) fully killed, aluminum-treated SA-212B steel was selected for the following reasons:

(a) Fine-grained steels are known to be less affected by fast neutron irradiation than the coarser grained steels.

(b) The critical thermal stress problem in the heavy section of the vessel closure during the heating-up cycle requires a maximum assurance of ductility (which is a temperature function) to prevent cracking and to arrest the propagation of cracks.

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Fully killed, ductile carbon steels unfortunately possess the highest temperature nil-ductility properties.²⁻⁶ Treatment of the steel with aluminum in the steel making process reduces the nil-ductility temperatures appreciably (+60F for untreated steels to -20F for aluminum treated are average values).

In the final report to the Safeguard Committee, the Laboratory indicated that the EBWR facility would probably be able to contain a hazard represented by a chemical reaction between water and the metal of the core involving 25% of the core metal. This problem was defined as a high-order TNT detonation and subcontracted to the Armour Research Foundation for evaluation. Preliminary results of the study (see next section, p. 15) indicate that the portion of the vessel in contact with the water phase would be destroyed under the design assumptions. Accordingly, the lower biological shielding plug is designed as a frustrum of a cone to blow out and provide a large relief port area into the basement of the EBWR facility.

The philosophy of a controllable catastrophe of the pressure vessel requires a vessel mounting at the top and a surrounding vessel support and missile-retaining structure which are immune to destruction.

A number of preliminary vessel support structure designs have been made to withstand the core catastrophe. Structural steel shapes, forged alloy tie-rods, and welded plate skirt designs are being evaluated considering the following problems:

(a) Thermal stresses and restraint of the vessel at the mounting point.

(b) Field construction problems associated with mounting the vessel inside the biological shield which will be partially completed for the construction of the EBWR facility building.

²P. P. Puzak, M. E. Schuster and W. S. Pellini, "Applicability of Charpy Test Data," The Welding Journal (September, 1954).

³P. P. Puzak, M. E. Schuster and W. S. Pellini, "Crack-Starter Tests of Ship Fracture and Project Steels," The Welding Journal, (October, 1954).

⁴W. S. Pellini and E. W. Eschbacher, "Performance of Weldments and Prime Plate of ABS-B Steel," The Welding Journal (October, 1954).

⁵W. S. Pellini, "Evaluation of the Significance of Charpy Tests," ASTM Special Technical Publication No. 158 (1954).

⁶Private discussions with W. S. Pellini, Naval Research Laboratory, March 9, 1955.

(c) Hydrostatic testing of vessel and piping after installation, repair of defects, and application of thermal insulation.

(d) Field construction problems associated with the construction of the biological shield after installing the vessel.

A fast heatup and shutdown time of 3 hr has been adopted as an operating philosophy, and this introduces an aggravating thermal stress problem in the closure of the pressure vessel. The problem is particularly acute for the heating-up cycle. A preliminary transient analysis by the Babcock and Wilcox Co. for a constant heat input rate, from 70F to 488F, shows that a 3-hour heating-up cycle is possible with modifications of the ring flange geometry and increased annuli between the upper indexing plugs and vessel flange to insure a uniform distribution of steam and the return of the condensate. Special air venting of the vessel is particularly important, since condenser vacuum is not available until steam pressure is high enough to operate the condenser air ejectors.

Other studies are in progress to determine a vessel design which is flexible enough to permit future conversion of the facility to forced circulation of the coolant and, later, to operation with heavy water. One preliminary design incorporates four additional 6-in. diameter connections in the lower head and four 8-in. diameter connections in the lower section of pressure vessel cylinder. These additional connections can be easily accommodated without major vessel design changes, i.e., only the 8-inch connections will require additional reinforcement of the vessel wall.

For an eventual heavy-water conversion of the EBWR facility, a borated stainless steel thermal shield would seem particularly adaptable, since the slow neutron flux level is higher by a factor of approximately 4. Negotiations are now in progress for the procurement of Type 304 stainless steel containing 1-1/2% boron in 1/8-in. and 1-in. thicknesses for evaluation of welds, physical, radiation and corrosion properties. A preliminary evaluation of a small laboratory heat of alloys containing 1% to 2% boron showed corrosion resistance approximately equal to that of Type 304 stainless steel.

2. Design Evaluation of EBWR in Regard to Internal Explosion - N. Balai, G. A. Freund

The design of the EBWR as presented to the Reactor Safe-guard Committee is predicated upon maintaining the gas-tight integrity of the steel building shell. Probably the most serious test of this integrity would result from a metal-water reaction in the reactor core due to a reactor runaway. From experimental work at du Pont, Aerojet General, North American Aviation, and Mine Safety Appliance, it is apparent that such a reaction takes place explosively only under special conditions (such as exceedingly fine particle division). It is impossible to visualize all of the core reaching this condition simultaneously.

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A design guaranteeing containment for a 10% core reaction was agreed upon with the probability that containment of a 25% reaction might be attainable. Preliminary calculations had indicated that the building shell (designed for 15 psig) could withstand the gradual pressure buildup from the 25% reaction. The equilibrium hydrogen concentration in the building resulting from a 25% reaction is below the lower explosive limit.

With this basis, it was then necessary to establish whether the 25% reaction could be contained without launching fragments to endanger the building integrity. Preliminary analysis clearly showed that it was not feasible to design a vessel which would completely contain this reaction. The best that could be hoped for would be to direct the force of the explosion downward into the building basement and to preserve the integrity of the pressure vessel top structure.

A subcontract was let to the Armour Research Foundation to evaluate the general feasibility of this design approach for the EBWR pressure vessel. The work was originally requested in two phases: (1) a theoretical study of the shock waves, pressures, water velocity, and forces developed in the pressure vessel resulting from an explosive release of 0.8×10^9 calories (equivalent to 25% reaction); and (2) an estimate of the structural damage resulting to the vessel and surroundings. A preliminary report covering phase (1) and comments on phase (2) have been received. Further structural investigations have been deferred until Argonne has reviewed the report and prepared a design in accordance with the results received.

The problem, as considered by Armour, involves a cylindrical vessel 22 ft high and 7 ft in diameter, filled with water to a depth of 12 ft, initially at 486F, with 600-psig saturated steam above it. The point of origin of the energy release was assumed to be the center of the core (4 ft diameter by 4 ft high cylinder located 18 in. from the bottom).

The experimental data on metal-water reactions are so scant that Armour could be given no information with regard to the time over which the energy is released. For a sufficiently long time of energy release, the effect could be no more serious than to distribute the energy throughout the water which would result in a new saturation level at higher temperature and pressure but substantially below a pressure which would destroy the top structure.

From an ultra-conservative viewpoint, the results from a point-source explosion as an upper limit will bracket the problem. The upper limit was calculated by the Armour Research Foundation to be roughly comparable to a high-order chemical explosion of TNT. Incidentally, to give the energy release specified, a TNT charge of about the same volume as the core would be required.

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The general plan of analysis (by Armour) was based on the conservation of mass, energy and momentum for the calculation of incident and reflected shock wave pressures on the bottom, sides and top of the vessel, as well as the pressures occurring across the interface between the water and the steam. These pressures are then compared with estimates of the "long time" pressure at which the vessel might reach "quasi-stability" while intact. (This "long time" pressure is the result of the initial energy division between vapor and liquid phase and does not represent equilibrium between the two phases.)

The calculated transient phenomena are summarized below:

Side walls:

Incident pressure	1.9×10^5 psi
Reflected pressure	6.1×10^5 psi
Incidence to reception	0.5 millisecond

Bottom of vessel:

Incident pressure	0.82×10^5 psi
Reflected pressure	2.5×10^5 psi
Incidence to reception	0.5 millisecond

Vessel cover:

Incident pressure	1600 psi
Reflected pressure	3800 psi
Incidence to reception	5 milliseconds

<u>Stabilized pressure</u> (before vessel evacuates):	3100 psi
--	----------

Transient duration	20 milliseconds (max.)
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Lower shielding plug:

Incident pressure	2×10^5 psi
Incidence to reception	0.5 millisecond
Ejection time	20 milliseconds
Initial velocity	40 meters/second
Clearance velocity	80 meters/second

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Water Slug:

Incident pressure	1570 psi
Reflected pressure	3380 psi
Incidence to reception	0.5 millisecond

The calculated results bring out two significant factors:

(1) the reflected shock wave from the vessel top is more than sufficient to cancel the shock wave emerging from the water-steam interface and, therefore, no "slug" of water is possible, and (2) high steam voids (50% condition) in the water phase will not change appreciably the initial shock wave pressure on the vessel top.

The results indicate that the side walls and bottom of the vessel will fail in such an explosion; however, the vessel cover sees a maximum pressure of 3800 psi. A cover design, with bolting and cover material stressed to values below their ultimate tensile strengths, is entirely feasible for the 25% core reaction. Since the bolting normally has a nominal factor of safety of six, an 800-psi bolting design would suffice. The cover, however, is customarily designed with a factor of safety of four; hence, a 1200-psi design would seem to be indicated. Breaking strength vs loading for these bases of design would be:

$$\text{Bolting } \frac{6 \times 800}{3800} = 1.25$$

$$\text{Cover } \frac{4 \times 1200}{3800} = 1.25$$

The Armour report has further estimated the time of loading on the vessel cover. In the design outline as submitted to them, pressure relief is first obtained by the movement of the concrete shielding plug under the pressure vessel into the control rod room. It is estimated that one millisecond after the shock of the explosion reaches the vessel cover, the cover receives a signal that the bottom plug has started to move; in 20 milliseconds the plug has blown out entirely and the vessel becomes essentially depressurized.

The results of this study indicate that a chemical reaction involving 25% of the core with water (for the most pessimistic assumption of explosion mechanism) can be controlled by directing the force of the explosion downward and maintaining the integrity of pressure vessel top structure.

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3. Steam Pipe Rupture Studies - T. H. Gamon

Tests on the BORAX reactor have shown that a large drop in pressure may cause a power excursion. The fastest drop in pressure visualized for the EBWR would result from a sudden break in the main steam line. Therefore, a study has been made to determine the effect of a main steam line failure on both the reactor pressure and the water remaining in the reactor vessel.

The following general criteria were used in making the calculations:

(a) The 6-inch main steam line was assumed to be broken off clearly just outside the EBWR shield.

(b) Initial conditions:

Pressure 600 psig

Temperature 485 F

(c) Feed-water flow to reactor interrupted during the blowdown period.

(d) Reactor power 6% of the design power (6% of 20 mw). (It was assumed that the increased voids caused by the reduction of pressure, in turn due to the pipe rupture, would result in reactor shutdown except for the 6% decay heat.)

(e) Steam flow through the broken pipe to conform with Grashof's formula for critical flow.

(f) The carry-over of water is negligible.

The calculations indicate an initial steam flow of 750,000 pounds per hour. The upward velocity of the steam in the reactor corresponding to the initial steam flow is 5 fps. At this velocity it is believed that bulk separation of large masses of liquid is ample; therefore, only steam escapes the reactor.

The calculations indicate a pressure drop from 600 to 250 psig in 15 seconds and to essentially atmospheric pressure in 150 sec (Fig. 1). Additional calculations show that the water remaining within the reactor would be sufficient to cover the core.

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RE-7-16208-A
T.H.GAMMON:L.R.D., 6-27-55

FIG. 1
EFFECT OF MAIN STEAM PIPE FAILURE
ON PRESSURE AND WATER VOLUME IN
THE EBWR. (NO FEED WATER ADDED)

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4. Steam By-pass System - A. S. Jameson

In the light of the experience gained in the operation of the Borax Reactor, the control of the EBWR turbine by-pass valve has been modified. Originally, it was intended to make the control of the by-pass valve sensitive to reactor pressure and its function was to automatically maintain the reactor pressure constant. Tests on the Borax Reactor with such a pressure controller indicated that this type of control induced reactor instability due to immeasurable pressure fluctuations which affected the steam voids and thus the power. The type of control found satisfactory for Borax was one of relatively wide proportional band throttling control with manual reset. The same type of control action will be adopted for the EBWR by-pass valve. The main function of the valve will be to maintain the reactor flow constant at any given reactor power level by sensing flow changes to the turbine and accordingly adjusting itself to these flow changes. The control system is essentially one of primarily matching the flow characteristics of the turbine admission valves with secondary manual corrective reset action of pressure.

Askania Regulator Company will furnish all the elements of the control system. In addition to the automatic control of the by-pass flow to match flow or load changes on the turbine, there is overriding control by the closure of the trip-throttle valve, which results in opening of the by-pass valve to a position established by the pressure control portion of the system. Failure of the hydraulic system, such as due to accidental breakage of the oil line to the valve, will result in closure of the valve. The fail-close operation of the valve has been established by the test findings at BORAX to be better than fail-open, since the latter results in pressure blow-down and possible induced power excursions. In the event that mechanical failure of the piping system occurs, overpressure merely initiates the opening of the pressure relief valves.

In addition to the automatic hydraulic control of the valve, remote electric motor positioning and local handwheel control are to be provided. The former enables valve control in case of hydraulic malfunction and the latter permits valve control in case of loss of electric power.

5. Impurity Carry-over in a Boiling Process - T. H. Gamon

The carry-over of irradiated corrosion products in a boiling reactor may impose a contamination problem in the turbine system. This problem is related to the degree of moisture carry-over. Radioactive tracer techniques have been employed by the Chemical Engineering Division to study the carry-over of dissolved solids from a boiling interface at low pressures. In addition, tests have been made jointly by the Chemical Engineering Division and Reactor Engineering Division at higher pressures (up to 600 psi) in a boiling test facility.

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The boiling test facility has been previously described in ANL-5260.⁷ The boiling section consists of a multiple channel configuration containing electrically heated plates. Cs¹³⁷ tracer was added to the water containing approximately 5 ppm of solids. Cs¹³⁷ was selected because of its high solubility in water and its stable properties at the operating conditions, besides being a suitable tracer material at low concentrations. Power densities of 14 and 28 kw/liter were tested (compared to 25 kw/liter for EBWR) at pressures of 150-600 psi. Steam velocities of 1/2 fps, similar to those in EBWR, were obtained. Samples of the steam were taken at 14, 28, and 44 in. above the water level to determine the amount of moisture carry-over and to measure the activity.

The tests have indicated the following:

(a) Decontamination factors of 10^4 to 4×10^4 were obtained. (Decontamination factor equals the ratio of activity in the steam to activity in water.)

(b) Comparable decontamination factors were obtained at various pressures from 150 to 600 psi.

(c) Power densities of 14 and 28 kw/liter produced nearly identical decontamination factors.

(d) The samples taken at 28 in. and 44 in. above the water level showed equal activity. The activity of the samples taken 14 in. above the apparent water level varied widely between samples. The variation is attributed to splashing.

(e) At each new startup of operation of the test facility, 1-1/2 to 2 hr of operation at temperature and pressure were generally required to obtain consistent activity readings in the samples.

6. Local Boiling Pressure Drop in Vertical Tubes - R. J. Weatherhead

Local boiling pressure drop tests have been made to determine whether the local boiling pressure drop correlations developed by Rohde^{7a,b} and Reynolds^{7c} are applicable to EBWR conditions.

The test section consisted of a vertical, d-c resistance-heated, nickel tube (0.180 in. ID), having a heated length of 11.5 in. and a pressure drop length of 14 in., with upward flow of hot, demineralized water.

⁷ Reactor Engineering Division Quarterly Report, (April 15, 1954), p. 154.

^{7a} Naval Reactor Program Quarterly Report, ANL-4842 (June 15, 1952) p. 33.

^{7b} Naval Reactor Program Quarterly Report, ANL-4898 (September 15, 1952) p. 59.

^{7c} J. B. Reynolds, "Local Boiling Pressure Drop," ANL-5178 (March, 1954).

The following ranges of variables were covered:

<u>Pressure,</u> <u>psia</u>	<u>Inlet Subcooling,</u> <u>F</u>	<u>Inlet Velocity,</u> <u>fps</u>	<u>Heat Flux,</u> <u>Btu/(hr)(sq ft)</u>
600	25, 50, 100	1, 3, 5	50,000 - 250,000

The test data was compared with the Rohde local boiling pressure drop correlation developed from UCLA data for a vertical, Type 347 tube (0.226 in. ID) with a 24-in. heated length, and with the Reynolds correlation for a horizontal, Type 347 tube (0.375 in. ID) with a 6-ft heated length. The test data appear to fit either correlation equally well; 80% of the test points fall within the prescribed limits of accuracy (Fig. 1A).

D. Reactor Core

1. Feasibility Tests on Mock-up of EBWR Core - A. B. Schultz, C. K. Soppet

A full-scale mock-up of a small section of the proposed core structure has been built in order to study the practicability of certain features and to establish tolerances for the final design. The core structure includes a small section of the bottom grid to support nine simulated fuel assemblies. A 30-foot high scaffold is mounted over the structure to facilitate performance tests of a fuel-grappling device from an elevation corresponding to the top of the reactor vessel.

The mock-up (Fig. 2) was fabricated of ordinary carbon steel and presented no unusual manufacturing problems. If the mechanical properties and fabricability of Zircaloy 2 and stainless steel are comparable, the basic design can be considered acceptable. The only detail in question is the bending of 1/8-inch thick Zircaloy 2 sheet around a 3/16-in. radius. Westinghouse data⁸ indicate this bend radius to be feasible; however, tests on sample plates will be made to determine whether or not welded corners may be necessary.

Accurate construction was necessary to insure that the cross control rods would operate freely in their guides, which are cross slots 1/2 in. wide and 8-3/4 in. across points. The accuracy achieved in the mock-up indicates that the following tolerance specification for the control rod channels be met:

⁸W. L. Mudge, Jr. and F. Forscher, "Mechanical Properties of Zircaloy 2," WAPD-101 (July 23, 1954).

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FIG. 1A
COMPARISON OF TEST DATA WITH ROHDE AND REYNOLDS
CORRELATION FOR LOCAL BOILING PRESSURE DROP

FIG. 2
MOCK-UP OF EBWR CORE SECTION
FOR FEASIBILITY TESTS

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"Tolerances for twist, warp and off-angle of cross slots to be determined by free fit of master cross having accurate blades $7/16$ in. thick, $8-5/8$ in. across and 80 in. long."

The accuracy needed for the guidance of the fuel assemblies is of secondary importance and will easily be met through the required accuracy of the control rod guides.

It is to be noted that the proposed core has no top grid to hold the fuel assemblies down; the fuel assemblies weigh approximately 170 lb each and no forces are foreseen which could dislodge the fuel assemblies from their sockets. These sockets have two conical surfaces which will provide vertical alignment of the fuel assembly. Since it is physically impractical to machine the pilots and the sockets to a perfect fit, there will be a slight amount of sideways "shake" at the top of each fuel assembly. The natural circulation boiling process results in irregular formation of large steam bubbles which might rattle the fuel assemblies and cause wear in the sockets; therefore, leaf-spring upper end fittings are used to prevent this rattling (Fig. 3). Manipulation of the nine assemblies in the mock-up indicated that the fittings will provide the desired stability of the fuel assemblies.

The grappling tool consists of a special grappling device (Fig. 3) mounted on the bottom end of a 15-foot-long aluminum tube. In the performance tests the tool was suspended from a cable running over a pulley and fastened to a hand-operated winch. The tool was operated from a level corresponding to the top of the pressure vessel. When lowered into a fuel assembly, the self-centering action of the tip was found to be satisfactory. After a few minor corrections the gripper actuating mechanism and the engagement of the gripping pins into the corresponding holes in the top fitting of the fuel assembly also were satisfactory.

Upon lowering the fuel assembly into the core, some difficulty was experienced in having the pilot enter its socket without first "hanging up" on the edge of the hole. Therefore, a conical pilot will be added to the fuel assembly to insure ready entry into the bottom grid, as well as proper entry between the top ends of adjacent fuel assemblies.

The unloading tool and the fuel assembly will be raised into a coffin by means of a special coupling on the end of the cable. This coupling not only provides a positive connection, but also is interlocked with the grappling tool so that the latter cannot be released when suspended by the winch. The unloading tool and coupling are finished articles and can be used as is, provided no changes are made in the unloading procedure.

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FUEL ASSEMBLY

DISENGAGED

GRIPPING
PIN

ENGAGED

FIG. 3
FUEL ASSEMBLY GRAPPLING MECHANISM

2. Alternate EBWR Control Rod Design Studies -
J. H. Monaweck, A. B. Schultz, A. Smaardyk

Alternate control rod design studies are still focused on vessel-type stainless steel blades filled with powdered poison absorbers as previously reported.⁹ The design of the container has been altered slightly. A sheet of stainless steel corrugated with longitudinal flutes will be inserted into the shell and microbrazed. This construction is a substitute for the inside stud structure and is designed to withstand an internal pressure of 1000 psi. The new proposed over-all width of two blades is 10-3/4 in.; there is a 1-inch gap at the center containing no poison material.

Physical calculations have shown that either a 0.150-in. layer of boron carbide powder or a layer of ferro-boron containing 17% boron packed to a density of 70% is sufficient to control the reactor for a period of 10 years. Due to the extremely low density of powder, this same thickness of commercial samarium-gadolinium oxide mixture will control the reactor for a period less than two years.

Heat transfer calculations have shown that no excessive temperatures will be developed within the blades.

Materials have been ordered to make four 1-foot blades, each filled with a different powder. These blades will be welded together to form a cross-type rod for test purposes.

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

a. EBWR Reference Design Control Rod Drive

The EBWR reference design control rod drive mechanism¹⁰ has accumulated a total of 1436 hr of operation in the facility simulating EBWR conditions (600 psi and 485 F). During this time 11,400 full cycles of 42-in. travel and 1090 scrams were completed. The system is operated continuously on full automatic control except for periods of routine inspection and maintenance.

The average rate of leakage through the pressure breakdown seal around the control rod extension shaft is approximately 750 cc/minute. Approximately 100 gm of uranium oxide and chips were introduced into the system to determine the effect of the particles on seal performance; there was no apparent effect.

⁹Reactor Engineering Quarterly Report, ANL-5371 (January 15, 1955) p. 208.

¹⁰Reactor Engineering Quarterly Report, ANL-5371 (January 15, 1955) p. 211.

The relative merits of internal and external dashpots (for stopping the control rod at the end of scram travel) were considered, and a decision was made to incorporate the external type in the final design. The deceleration rates effected by each design are compared in Fig. 4.

b. Hydraulically Operated Control Rod Drive

The hydraulic control rod drive has been in the process of development as a possible alternate to the mechanical drive for the EBWR. Two types of pistons are being studied - the sealed piston and the free-floating piston. The following discussion pertains to the free-floating or "rotameter"-type in which there is a clearance between the piston and the cylinder wall (Fig. 5).

The floating piston-type unit offers possibilities for application on the EBWR since the unit would be located at the bottom of the reactor, where advantage may be taken of the water leg in the thimble. Also, the water flowing past the piston may be allowed to enter the reactor core at the bottom at a rate of approximately 1.5 gpm. The piston clearance of approximately 0.010 in. requires a pressure drop of approximately 23 psi to support a load of 160 lb.

Preliminary tests were conducted at atmospheric pressure; a variety of piston shapes were tested in an effort to eliminate the tendency to "seize" against the cylinder wall due to unequal distribution of hydraulic pressure. A piston 2.998 in. in diameter and 4 in. long with seven circumferential grooves equally spaced appears to provide the correct amount of maneuverability. A stepless speed range from 0.1 in. to 42 in. per minute is attainable and the piston level may be maintained in any location without use of the auxiliary magnetic gripper. Position indication by the differential transformer-servo motor drive may be determined within 0.002 in., if desired.

The next series of tests will be conducted at gradually increased pressures and temperatures (up to 800 psi and 500 F) to determine scram times and other operating characteristics.

4. Boric Acid Crystal Injection Tests - A. L. Lindsay

A system is provided for the automatic spraying of water on the EBWR core to remove decay heat from the fuel elements in the event of simultaneous low pressure and low water level in the reactor pressure vessel. In order to remove the remote possibility that the water addition to the core would cause criticality, provisions have been made to add boric acid to the spray water. A continuous boric acid dissolution method has been selected in order to eliminate the necessity of storing a large supply of boric acid solution.

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FIG. 4
COMPARISON OF ROD DECELERATION EFFECTED
BY INTERNAL AND EXTERNAL DASHPOT DESIGNS

RE-7- 16140-A
C.F.BULLINGER:L.D.,6-10-55

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RE-6-16141-A
C.F. BULLINGER:E.K., 6-10-55

FIG. 5
HYDRAULIC CONTROL ROD DRIVE SCHEMATIC
(FREE-FLOATING PISTON TYPE)

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Tests have been conducted to develop a continuous boric acid dissolution process. The apparatus consisted mainly of a horizontal, 6-in. diameter Pyrex pipe. Boric acid crystals were placed in the bottom half of the pipe and water was passed over the crystals along the length of the pipe. The boric acid concentrations obtained by this method were about one-tenth of the required concentration of 2.4 gm of H_3BO_3 per liter of water (established to overcome 12-1/2% reactivity).

Improved concentrations were obtained by injecting the water into the pipe through a series of holes along the length of the pipe. Two tests were made: the first with ten evenly spaced holes, 1/16 in. in diameter, and the second with three evenly spaced holes, 1/8 in. in diameter. Figure 6 shows the acid concentration at the exit of the pipe as a function of time and nozzle geometry. The curves indicate that the test pipe with the three holes gave the best results, since the boric acid concentration was maintained for approximately 2-1/2 times longer as compared to the test pipe with the ten holes.

Extrapolation from these tests has resulted in the selection of a 12-in. diameter, 10-ft long boric acid tank, fitted with eight evenly spaced injection nozzles, for the EBWR reactor.

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

The EBWR reactor core will be composed of Zircaloy 2 and stainless steel components in contact with each other. Therefore, tests are being made to observe and compare the corrosion behavior of Zircaloy 2 coupled with Type 304 and with Type 347 stainless steels in static and dynamic high-temperature water.

In the static tests the samples are bolted together, end to end, with a 1/4-inch overlap. The bolt and nut material is the same as the stainless steel half of the couple. The couples are being tested in both air-saturated and degassed water at both 500F and 600F.

In the dynamic tests the samples are held butted end to end by two small strips of Zircaloy 2 and stainless steel, respectively, bolted to the two plates. Two coupled assemblies, one of Zircaloy 2 with Type 304 and one of Zircaloy 2 with Type 347 stainless steel, are being tested in 500F degassed, demineralized water flowing at a velocity of about 10 fps.

The samples in the static tests have accumulated a total of 2958 hr at 600F and 3709 hr at 500F. The dynamic test specimens have been exposed for 2774 hr. In both tests, there is no evidence of accelerated corrosion as a result of the crevice or contact between the dissimilar materials.

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FIG. 6
BORIC ACID CONCENTRATION IN TEST TANK AS
A FUNCTION OF TIME AND NOZZLE GEOMETRY

RE-7-16143-A
A.L.LINDSAY:H.S.,6-13-55

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E. Fuel Studies

A number of clad fuel plates have been made to EBWR specifications by the Metallurgy Division. The meat of these plates is an alloy of uranium with 1.5% niobium and 5% zirconium by weight; the cladding is Zircaloy 2 (0.020 in. thick). Corrosion and thermal cycling tests are under way. A high degree of integrity of the cladding is required because efforts to develop uranium alloys which remain dimensionally stable under irradiation and which possess good corrosion resistance in high-temperature water have been largely unsuccessful. There appears to be a basic incompatibility between these two requirements in that the heat treatment required for dimensional stability is different from that which is required for high corrosion resistance.

1. Stress Analyses of EBWR Type Fuel Plates and Sub-assemblies

a. Steady-State Thermal Stresses in Zircaloy 2 Clad, Enriched Uranium and Natural Uranium Alloy Fuel Plates - W. S. Flinn

Calculations were made to estimate the possible maximum steady-state thermal stresses in the present reference design fuel plates under conditions of EBWR operation. The fuel elements are 1/4 in. thick and consist of (1) an enriched uranium plate clad with 0.010 in. of Zircaloy 2, and (2) a natural uranium alloy plate (1-1/2% Nb, 5% Zr, bal. U) clad with 0.020 in. of Zircaloy 2.

The calculations were based upon the following assumptions and constants:

- (1) Both plates were free of any subassembly structure.
- (2) A sound mechanical and thermal bond existed between the core and cladding of each plate.
- (3) The enriched uranium plate had the mechanical and thermal properties of a homogeneous Zircaloy 2 plate. Creep was not considered.
- (4) The properties of the uranium-niobium-zirconium plate were the same as those for natural uranium.
- (5) The total stresses in the natural uranium alloy plate included stresses incurred during fabrication, reactor heat-up period, and while at reactor operating temperatures. The manufacturing and heating stresses were eliminated in the case of the enriched uranium plate since it was considered that the thermal expansion coefficient of the core alloy and Zircaloy cladding were approximately the same.

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(6) A rolling temperature of 1340F was considered the original zero stress level. Possible stresses or phase change strains developed during subsequent heat treatments were considered as relatively minor compared to thermal strains. The strains set up by the carbon steel rolling jacket were not included, since the over-all effect would result in a - 35,400 psi stress in the cladding instead of - 31,200 psi, as shown in Table I.

(7) The reactor operating conditions were:

Maximum heat flux	110,000 Btu/(hr)(sq ft)
Ambient temperature	500 F

(8) Thermal Conductivity

Zircaloy 2	8 Btu/(hr)(ft)(F)
Natural Uranium alloy	10 Btu/(hr)(ft)(F)

(9) Coefficient of Thermal Expansion

Zircaloy 2	3×10^{-6}
Uranium alloy	7×10^{-6}

(The constant for the uranium alloy represents a single average value available in the temperature range of 75-212F.)

(10) Since the stress-strain relationships were not linear, a different modulus of elasticity was used to calculate each of the three stress components for the natural uranium alloy plate.

	Effective Modulus of Elasticity, psi		
	Manuf.	Heating-up	Operating
Zircaloy 2	7×10^6	12×10^6	8×10^6
Uranium alloy	10×10^6	20×10^6	13×10^6

(11) Poissons Ratio

Zircaloy 2	0.35
Uranium alloy	0.25

The maximum steady-state stresses in the enriched plate were calculated to be about 1900 psi.

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Table I

MAXIMUM STEADY-STATE THERMAL STRESSES IN
NATURAL URANIUM ALLOY FUEL PLATE

(in psi)

	<u>Zircaloy 2 Clad</u>		<u>Uranium Alloy</u>	
	<u>Outside</u> <u>Surface</u>	<u>Inside</u> <u>Surface</u>	<u>Outside</u> <u>Surface</u>	<u>Center</u>
Manufacturing Stress	-31,200	-31,200	5,970	5,970
Heating-Up Stress	18,300	18,300	-3,500	-3,500
Operating Stress	2,900	2,100	3,400	-2,400
Maximum Total Stress	-10,800 (Inside)		5,870 (Outside)	

The stresses in the natural uranium alloy plate (Table I) do not appear to be too great; however, considerable information on the physical and mechanical properties (detailed fatigue data) would be necessary before any significant evaluation could be made. The calculations indicate that the maximum fuel plate stresses are likely to exist at room temperature and that, in going up to operating conditions, these stresses will be reduced. The operating thermal stress components themselves are quite small and, unless considerable reactor power cycling exists, should not cause trouble.

The relative free thermal growth of the alloy core and Zircaloy cladding associated with these thermal stresses was only about 0.16%. Although cladding strains are quite high, it is possible that these thermal strains are relatively insignificant when compared to possible growth strains set up by thermal cycling and/or irradiation.

It should be noted that the natural uranium alloy is in tension and the cladding in compression when the fuel plate is at the reactor ambient temperature of 500F. Therefore, it seems possible that the Zircaloy cladding would not tend to restrain the fuel alloy from thermal cycling or irradiation growth, at least not until the core has grown sufficiently to develop restraining tensile forces in the cladding.

b. Shear Stresses in Pin-Connected Fuel Plate Sub-assembly - W. S. Flinn

Except for the effective modulus of elasticity, the thermal and physical properties outlined in the preceding section were also used to calculate the approximate pin stresses developed by relative

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longitudinal thermal expansion of the fuel plates and side plates in a pin-connected, Zircaloy 2 clad, natural uranium alloy plate subassembly.

With reference to the illustration, the pin shear stress (S_s) is given by:

$$S_s = \frac{2}{\pi d^2} \left[\frac{\alpha_U \Delta T_U - \alpha_Z \Delta T_Z}{\frac{1}{bhE_U} + \frac{1}{2taE_Z}} \right]$$

where

a = Fuel plate spacing = 0.67 in. pitch

b = Fuel plate width = 3-1/2 in.

d = Pin diameter, in.

h = Fuel plate thickness = 1/4 in.

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t = Side plate thickness = $1/8$ in.

$\Delta T_U + 70$ = Mean Fuel Plate Temp. = 556F

$\Delta T_Z + 70$ = Mean Side Plate Temp. = 486F

E_U = Effective Modulus of Elasticity of
Fuel Plate = 24×10^6 psi

E_Z = Effective Modulus of Elasticity of Side Plate = 10×10^6 psi

α_U = Thermal Coefficient of Expansion of Fuel Plate = $10 \times 10^{-6}/F$

α_Z = Thermal Coefficient of Expansion of Side Plate = $3.5 \times 10^{-6}/F$

The calculated average pin shear stresses are plotted as a function of pin diameter in Fig. 7; the maximum stresses are 50% greater. The relative free growth of the fuel and side plates was calculated to be about 0.34%. Stresses developed by growth resulting from thermal cycling and/or irradiation could overshadow the thermal expansion stresses.

Assuming values corresponding to one-half the tensile values,¹¹ the shear yield and ultimate strengths of annealed Zircaloy 2 at 500F would be 9,000 and 16,000 psi, respectively. Therefore, the possibility of loosening or failure of the pin connections does exist in this type of fuel assembly.

c. Stress Analysis of a Welded EBWR-Type Fuel Sub-assembly - C. K. Soppet

One of the fuel designs advanced for use in the EBWR is a welded-type subassembly, as shown in Fig. 8. The design offers the rigidity to resist boiling water-induced vibrations and the simplicity of an all-welded grouping of components. The design also poses several problems of stress and strain in the individual components which comprise the welded subassembly. Chief among these problems are:

(1) The ability of the Zircaloy 2 cladding and side plates to withstand and absorb the strains induced by radiation damage, thermal expansion and thermal cycling of the fuel "meat" under anticipated EBWR operating conditions (486F).

(2) The effect of thermal and radiation damage-induced loads, and possible distortion of the components, on weld integrity.

¹¹W. L. Mudge, Jr., "Mechanical Properties of Zircaloy-2" - Progress Report on Mechanical Metallurgy Project MM-1a, WAPD-MM-198 (April 9, 1953).

FIG. 7
SHEAR STRESS IN PIN CONNECTION DUE
TO RELATIVE THERMAL EXPANSION
OF FUEL PLATE AND SIDE PLATE

RE-7-16207-A
W.S.FLINN:L.R.D., 6-27-55

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FIG. 8
WELDED EBWR-TYPE FUEL PLATE SUBASSEMBLY

The following calculations indicate that: (1) the Zircaloy 2 clad probably will be able to absorb the stresses incurred during EBWR operation; (2) properly located plug welds should be able to transmit the expected fuel plate loading to the side plate without damage to the edge bond of the fuel plate or the weld; and (3) with proper design, the side plate may be made to act as an extension of the fuel plate edge clad, subjected to and absorbing the same strains. However, because of the meager amount of available information concerning the materials of construction, and the operational environment, an irradiation experiment is recommended.

The constants used in the analysis are subject to the same limitations imposed by the sources of information.^{12,13}

Mechanical Properties of Zircaloy 2

Elongation (max.)	12%
Creep Stress	27,000 psi at 250C
Tensile Strength	33,000 psi at 250C

Coefficients of Thermal Expansion

α_Z	Zircaloy 2	3×10^{-6} in./in./F
α_U	Natural Uranium Alloy	7.5×10^{-6} in./in./F

Effective Modulus of Elasticity at Operating Temperature (500F)

E_Z	Zircaloy 2	8×10^6 psi
E_U	Natural Uranium Alloy	13×10^6 psi

Endurance Limits

C_e	Fatigue Limit	10^7 cycles at 250C
S_e	Endurance Limit	30,000 psi at 250C

The radiation damage growth was assumed to proceed at a maximum uniform rate of 0.5 inch per plate per year.

(1) Fuel-Plate Clad

The Plate will be subjected to two major strain-producing conditions: (a) long-time radiation damage elongation, and (b) normal operation thermal cycling.

¹²W. L. Mudge, Jr. and F. Forscher, "Mechanical Properties of Zircaloy 2," WAPD-101 (July 23, 1954).

¹³Unpublished Argonne data.

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(a) Radiation Damage

The most extreme case will occur during operation at the end of the expected life of the fuel plate. At this time the maximum radiation damage will all have been absorbed and the thermal stress will be positive and additive.

The elongation, producing strain, is set up by radiation damage growth, ϵ_{RD} , and thermal expansion, ϵ_{ther} .

$$\begin{aligned}\epsilon_{RD} &= t\delta/L \\ &= (5)(0.5)/(48)(8760) \\ &= 5.94 \times 10^{-6} \text{ in./in./hr}\end{aligned}$$

A comparison of this elongation with the creep properties of Zircaloy 2 (Fig. 9) indicates that the radiation damage elongation can be absorbed in this manner.

The thermal expansion contribution is:

$$\begin{aligned}\epsilon_{ther} &= \alpha_U \Delta T \\ &= (7.5 \times 10^{-6})(486F) \\ &= 3.75 \times 10^{-3} \text{ in./in.}\end{aligned}$$

Since this expansion would occur over the time interval required to bring the reactor from the room temperature condition to the operating temperature:

$$\begin{aligned}\epsilon'_{ther} &= \epsilon_{ther}/t_{op} \\ &= (3.75 \times 10^{-3})/(4) \\ &= 9.4 \times 10^{-4} \text{ in./in./hr}\end{aligned}$$

Since this rate is also within the creep limits of the material, normal creep can be expected to relieve the thermal stresses.

The maximum elongation produced is the sum of the total radiation damage and the thermal expansion on the last day of operation.

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FIG. 9
MINIMUM CREEP RATE VS STRESS OF
ALPHA-ANNEALED ZIRCALOY 2 AT 343 C

COURTESY WESTINGHOUSE ATOMIC POWER DIVISION

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$$\begin{aligned}
 \epsilon_t &= [(\delta t) + (\epsilon_{\text{ther}} L)]/L \\
 &= [(0.5)(5) + (3.75 \times 10^{-3})(48)]/48 \\
 &= 0.057 \text{ in./in.}
 \end{aligned}$$

This represents an increase in length of 5.7%, which is about half the maximum elongation of 12% published by Westinghouse.

(b) Thermal Cycling

In addition to thermal cycling growth, which we assume will be small and will be absorbed in the same manner as radiation damage elongation, two other situations must be considered. The first is the normal fatigue condition imposed by the differential thermal expansions of core and clad when operating between 486°F and room temperature. The second is the more severe case of fatigue imposed by cycling between conditions of negative and positive stress.

(1) Normal Stress Cycling Fatigue

Pessimistically assuming that there is no yield of the fertile material, all the differential thermal expansion is resisted by the Zircaloy 2 and produces a maximum stress of 27,000 psi, the stress at which Zircaloy begins to creep. Assuming further that the clad returns to a condition of lower position stress when a reactor scram occurs, the maximum stress range will be 0-27,000 psi. To determine the number of stress cycles during the core life, assume two reactor scrams (to room temperature) each day for ten years. This gives 7300 cycles as the total number to be expected. These values are well below the endurance limits of 10^7 cycles between 0 and 30,000 psi which are attributed to Zircaloy 2.

(2) Stress Reversal Fatigue

Using the clad stress values by Flinn and assuming that the clad constantly absorbs radiation damage strains by elongating, then the time lapse between initial reactor startup and the point at which the clad stress is zero will be:

$$\begin{aligned}
 t_i &= S/EZ \epsilon_{RD} \\
 &= 13 \times 10^3 / (8)(10^6)(6)(10^{-6}) = 13 \times 10^3 / 48 \\
 &= 261 \text{ hr}
 \end{aligned}$$

Subsequent reactor shutdowns to room temperature will result in thermal cycling of the fuel through zero and into a condition of compressive stress.

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This situation will continue until the room temperature stress is also zero. Using the same formulation and substituting room temperature stress for operating stress then:

$$\begin{aligned} t_f &= S/EZ \epsilon_{RD} \\ &= 30 \times 10^3 / (8)(10^6)(6)(10^{-6}) = 35 \times 10^3 / 48 \\ &= 626 \text{ hr} \end{aligned}$$

Summarizing the foregoing, 261 hours after initial reactor startup, a period of 365 hours is encountered during which the fuel will be cycled between conditions of tensile and compressive stress at each reactor scram to room temperature.

(3) Side Plate Action

The fuel plate clad analysis indicated a mechanism whereby the clad could absorb the strains imposed by the operating conditions. Since the side plate is made of the same material as the clad, it is reasonable to assume that the side plate can absorb the transmitted loads by the same method. For the case of perfect clad elasticity and rigid attachment of side plate to fuel plate, the thermal stress produced in the side plate by the fuel plate is given by:

$$\begin{aligned} S_{SP} &= E_Z \Delta T \Delta \alpha \\ &= (8 \times 10^6)(4)(10^{-6})(416) \\ &= 13,312 \text{ psi} \end{aligned}$$

The stress produced by radiation damage for the same conditions will be 27,000 psi, the point at which a reasonable creep rate will be initiated. Assuming that each fuel plate loads a side plate of thickness (t) and width (a+c-D), then

$$\begin{aligned} A_{sp} &= (t)(a+c-D) \\ &= (0.125)(0.25 + 0.416 - 0.375) \\ &= 0.037 \text{ sq in.} \end{aligned}$$

and the produced load which resists side plate motion is:

$$\begin{aligned} P_R &= (S_c)(A_{sp}) \\ &= (27,000)(0.037) \\ &= 999 \text{ lbs} \end{aligned}$$

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(4) Load Transmission

The load, " P_R " induced by the fuel plate elongation, must be transmitted to the side plate by means of the welds which connect the side plate and fuel plate in a manner which assures the integrity of the weld. Considering the shear strength of Zircaloy 2 to be one-half the tensile strength and the weld efficiency to be 80%, the required weld area needed to stretch the side plate is:

$$\begin{aligned} A_W &= 2 A_{sp} / E \\ &= (2)(0.037) / (0.8) \\ &= 0.0925 \text{ sq in.} \end{aligned}$$

A weld, 3/16 in. by 1 in. long, connecting each end of the fuel plate to the side plate should be adequate.

In addition to the structural welds located at either end of the assembly, stay welds will be required along the length to prevent side plate bowing. The welds should be small to insure bond integrity.

Differential growth between adjacent fuel plates is expected to be negligible. However, a sufficient number of longitudinally oriented slotted holes may be placed in the side plate to reduce its total shear area to less than the total weld area.

2. Freely Expanding, Guided EBWR Fuel Plate Subassembly - C. K. Soppet

Figure 10 illustrates a EBWR subassembly design which permits free expansion of the fuel plates. This type of design will be considered in the event that the present subassembly is found to be unacceptable.

The fuel plates are welded to the side plate at only one point which allows the fuel to grow longitudinally. Adjacent fuel plates are spaced and guided by small button rivets which are attached to the side plates. The side plates are perforated to alleviate any pressure build-up which might occur during an excursion. This procedure also reduces the number of stay welds required to hold the side plates to the fuel plate edges.

The most important problems attendant to the design are corrosion and wear. The points of contact between fuel plate clad and spacer buttons may be sources of serious fretting corrosion. These same points of contact present wear and galling problems as the fuel plates move relative to the side plates. These problems must be resolved before the design can be used.

037029.030

FIG. 10
FREELY EXPANDING GUIDED EBWR FUEL PLATE SUBASSEMBLY

3. Corrosion and Irradiation Tests - C. R. Breden,
G. A. Freund, R. A. Granacki, D. R. Patterson,
R. J. Schiltz

The following core and clad materials are being evaluated for possible use in water-cooled reactors:

- a. High-uranium alloys.
- b. Thoria-urania bodies.
- c. Aluminum (nickel-plated or with nickel additions).

Tests are carried out in out-of-pile loops and autoclaves, in autoclaves in the CP-5 reactor, and in the ANL Water Loop at MTR. In general, the CP-5 autoclaves are used for simultaneous corrosion and irradiation of pin samples of clad and bare core materials. The MTR loop is used for plate samples of cladding materials and for fuel plates; one set of samples is irradiated in high-temperature, flowing water, while the other set is exposed to the same water, but in the out-of-flux region.

a. High-Uranium Alloys

It is desired to develop a corrosion-resistant high-uranium alloy which will have dimensional stability and will retain its corrosion resistance under irradiation. Alloys being tested are:

- (1) niobium (3 and 6%)
- (2) niobium-zirconium (primarily 1.5% Nb-5% Zr)
- (3) niobium-tin (3% Nb-0.75% Sn)
- (4) silicon (3.8% Si forming an epsilon phase)

(1) Water Corrosion of Unclad High-Uranium Alloys

The current results of the corrosion tests on unclad high-uranium alloys¹⁴ are summarized in Table II.

The sample of uranium-6% niobium alloy with a defect in the can has outlasted other canned samples. The uranium-3.8% silicon alloy is still good after 6944 hr and the average over-all weight change rate is the lowest of all samples listed. One sample of uranium-1-1/2% niobium-5% zirconium, quenched from 1470F, is still good after 8614 hr, and the over-all weight change rate is not excessive. Most recent test samples show much higher rates.

¹⁴Reactor Engineering Division Quarterly Report, ANL-5371 (January 15, 1955).

037029.030

Table II
CORROSION OF HIGH-URANIUM ALLOYS IN 500F AND 550F WATER

Sample No.	Experiment No.	Alloy Composition, % Uranium Plus				Heat Treatment(2)	Water(3) Composition	Rate of Wt. Change, mg/(cm ²)(mo)		Time to(4) Disintegration, hr		Appearance
		Nb	Zr	Sn	Si			500F	550F	500F	550F	
A-4065 ⁽¹⁾	U-64	6				2012F quenched.	Deg.			>10,088		After 9371 hr three cracks appeared in top edge of can. After 10,088 hr cracks were enlarged and the top end showed a slight bulge.
A-4114a	U-81				3.8%	850C - 7 days	Deg.	-48		>6,944		Weight change rate is for last period.
A-4298f	U-314				3.8%		Deg.	-50.1		>500		Loose black coating, pitting, spalling.
A-4297h	U-313	3		0.75			Deg.	-141.8		>571		Loose smooth black coating
A-4128t	U-192	2	4			1470F quenched.	O ₂		-352	>3423		Adherent rough black coating, pitting, spalling
A-4127r	U-188	1-1/2	5			1470F quenched.	O ₂		-291	6262		Disintegrated.
A-4118	U-85	1-1/2	5			1470F quenched.	Deg.	-96		>8,614		Adherent black coating. No change in pitting.
A-4207a								-91.5				
A-4207c								-94.8				
A-4207d								-96.2				
A-4207e	U-217	1-1/2	5			As cast. Quenched at 850C	Deg.	-89.6		>1,186		Adherent smooth black coating. No change in pitting.
A-4207f								-83.5				
A-4207g								-80.0				
A-4207h								-83.6				
A-4241a	U-249	1-1/2	5			Salt at 1562F.		-352		>3,504		Loose black coating. No change in pitting.
A-4355a	U-327	1-1/2	5					-502				Adherent black coating. Pitting.
A-4355b	U-328	1-1/2	5			As cast. 1000C in vacuum 1 hr, WMQ		-402				Adherent black coating. Slight pitting.
A-4355c	U-329	1-1/2	5			Aged 400C - 2 hr.	Deg.	-325		>320		Adherent black coating. Slight pitting.
A-4355d	U-330	1-1/2	5					-314				Adherent black coating. Slight pitting.
A-4355e	U-331	1-1/2	5					-382				Adherent black coating. Pitting.
A-4355f	U-332	1-1/2	5			As cast. 750C - 1.75 hr, FC. 700C 3 min; 725C - 10 min; WQ. Aged 400C 2 hr.	Deg.	-464		>320		Adherent black coating. Many pits.
A-4355g	U-333	1-1/2	5			As cast. 750C - 1.75 hr; FC. 725C - 10 min, WQ. Aged 400C - 2 hr.	Deg.	-2140		>320		Adherent black coating Pitted and spalled.

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

(2) WMQ = Wood's Metal Quench; FC = Furnace-cooled, WQ = Water Quench.

(3) Water Condition: Deg. = Degassed (1-2 ppm O₂); O₂ = saturated at room temperature.

(4) Symbol (>) denotes accumulated test time, test still in progress.

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Since the Metallurgy Division has been carrying out irradiation tests on uranium-1-2/2% niobium-5% zirconium alloy in contact with NaK, followed by subsequent corrosion tests, it was of interest to determine if prolonged contact with NaK at elevated temperatures affected the subsequent corrosion behavior. The results, summarized in Table III, appear to indicate a slight increase in corrosion rate as a result of such pretreatment. These results, however, cannot be accepted without qualification, since the streaked pitting reported suggests the presence of stringers and segregated impurities in the alloy.

(2) Corrosion-Irradiation of High-Uranium Alloy Pins

The CP-5 autoclave facility¹⁵ has been utilized for the corrosion-irradiation testing of several high-uranium alloy pins made by the Argonne Metallurgy Division. Description of samples, test conditions, and results of post-irradiation examination are given in Table IV.

The pressure buildup during operation may be due to: (1) dissociation of the water as it became contaminated with corrosion products; (2) hydrogen release upon corrosion of the samples; or a combination of both.

In either case, operating experience has verified that the pressure buildup would be directly or indirectly a result of corrosion of the samples. In general, there has been a greater and faster pressure buildup in tests where the samples exhibited pronounced corrosion attack.

Gas removed from the system after three typical runs with high-uranium samples has been analyzed in the mass spectrometer. The results, in per cent of total atoms, are tabulated below. A gas analysis of a test run on a 2S aluminum sample is included for comparison purposes.

Run No.	Sample Condition	H ₂	O ₂	N ₂	He
2*	Disintegrated	44	3.7	23	--
4*	Disintegrated	72.5	20.7	3.4	3.5
8	2S aluminum	0.04	0.12	1.3	98.6
9	See Table IV	0.13	0.06	4.0	95.8

* Previously reported in ANL-5371

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

Table III

EFFECT OF NaK PRETREATMENT ON CORROSION OF URANIUM-1-1/2% NIOBIUM-5% ZIRCONIUM IN 500F DEGASSED, STATIC WATER

(Total Test Time: 386 hr)

Sample No.	Experiment No.	Pretreatment	Test Period, hr	Rate of Weight Change, mg/(cm ²)(mo)	Total Rate of Weight Change, mg/(cm ²)(mo)	Appearance	
A-4279a	U-297	360F in vacuum - 6 weeks	64	-49.6	-55.3	Smooth black coating. Streaked pitting parallel to rolling.	
			160	-37.1		Autoclave dry. Pitting increased.	
			162	-74.4		Sample cracked. Pitting increased.	
A-4279b			64	-53.7	-60.0	Smooth black coating. Streaked pitting parallel to rolling.	
				160		-40.7	Pitting increased.
				162		-81.0	Cracked in three places. Pitting increased.
A-4279c			64	-59.0	-61.2	Smooth black coating. Streaked pitting parallel to rolling.	
				160		-39.6	Pitting increased.
				162		-81.8	Pitting increased.
A-4279d			64	-64.2	-71.4	Smooth black coating. Streaked pitting parallel to rolling.	
				160		-74.8	Raised ridge on one side. Surface cracks.
				162		-50.8	Increased cracking.
A-4279e	U-298	360F in NaK - 3 weeks	64	-63.3	-67.2	Smooth black coating. Streaked pitting parallel to rolling.	
			160	-75.4		Small dark rings on side.	
			162	-60.3		Pitting increased. Rings gone.	
A-4279f			64	-70.1	-73.7	Small cracks. Pitting enlarged.	
				160		-81.0	Smooth black coating. Streaked pitting parallel to rolling.
				162		-67.5	Pitting increased. Raised spots.
						Cracks and pits enlarged.	

Table IV

CORROSION-IRRADIATION OF HIGH-URANIUM ALLOY PINS IN CP-5 AUTOCLAVE FACILITY

Autoclave Run No.	Sample			Time, hr	Burnup, % U atoms	Operating Characteristics	Results
	No.	Description	History*				
5	A-4298c	3.89% Si, bal. U (9.98% enrich.) Unclad. 1/8 in. dia. x 3/8 in. long	A	186	0.037	Pressure rise began after 40-hr operation. Rose 45 psi in 30 hr and remained steady until last 10 hr of operation when it rose 10 psi	All samples disintegrated with only short piece of clad specimen remaining in the holder.
	A-4297c	3% Nb, 0.75% Sn, bal. U (10% enrich.) Unclad. 1/8 in. dia. x 1 in. long	B				
	A-4292c	5% Zr, 1-1/2% Nb, bal. U (10% enrich.) Zircaloy 2 clad, ends open. 1/8 in. dia. x 5/8 in. long	C				
6	A-4292b	5% Zr, 1-1/2% Nb, bal. U (10% enrich.) Zircaloy 2 clad, ends open. 1/8 in. dia. x 5/8 in. long	C	6	---	Erratic thermocouple operation resulting in early removal of sample	Irradiation too short for any significant results.
	A-4297g	3% Nb, 0.75% Sn, bal. U (10% enrich.) Unclad. 1/8 in. dia. x 5/8 in. long	B				
7	A-4298d	3.89% Si, bal. U (9.98% enrich.) Unclad. 1/8 in. dia. x 1/4 in. long	A	169	0.033	Rapid pressure rise beginning after 4-hr operation. Rise of 115 psi in 75-hr operation, then remaining steady to end of test.	Sn specimen largely intact with corners severely attacked. See Fig. 11.
	A-4286e	5% Zr, 1-1/2% Nb, bal. U (10% enrich.) Zircaloy 2 clad, ends open. 1/8 in. dia. x 5/8 in. long	D				
	A-4286d	5% Zr, 1-1/2% Nb, bal. U (10% enrich.) Zircaloy 2 clad, ends open. 1/8 in. dia. x 5/8 in. long	D				
9	A-4285a	5% Zr, 1-1/2% Nb, bal. Nat. U. Zircaloy 2 clad, with niobium barrier, ends open. 1/8 in. dia. x 5/8 in. long	E	333	0.005	Operating temperature approximately 450F, pressure 800-820 psig. Relatively rapid pressure rise (approximately 15 psi per day); believed due to leaking valve.	Badly spalled and cracked to a depth of approximately 1/8 in. at open end. White film on clad. Meat corroded from open end to a depth of perhaps 1/16 in. Clad bright and intact.

*Sample History

A

1. As cast and heat treated for 2 hr at 825C in vacuum capsule.
2. Air cooled inside the capsule.

B

1. Immersed in lead at 800C, water quenched.
2. Annealed in vacuum at 350C for 6 hr.
3. Air cooled.

C

1. Cast into Zircaloy can at 1425C.
2. Rolled out of salt and swaged out of lead at 800-850C.
3. Immersed in Wood's metal at 300C for 3 min.
4. Immersed in lead at 600C for 3 min.
5. Immersed immediately in lead at 850C for 10 min.
6. Immersed in Wood's metal at 300C for 3 min, air cooled.
7. Cleaned in boiling water and sealed in vacuum capsule and annealed for 50 min at 425C, air cooled.

D

1. Cast into Zircaloy 2 can at 1425C.
2. Rolled out of salt and swaged out of lead at 800-850C.
3. Aged at 850C in vacuum, quenched in Wood's metal.
4. Aged at 425C in vacuum, air cooled.

E

1. Hot rolled.
2. Jacketed with 0.006 in. thick niobium barrier by swaging.
3. Co-extruded with Zircaloy 2 clad.
4. Heat treated for 15 min in salt at 850C.
5. Water quenched.
6. Aged 45 min in lead at 425C.

ANL-CRB-15875-A
G.FREUND:L.D., 5-10-55

FIG. 11
UNCLAD URANIUM-NIOBIUM-TIN AND URANIUM-
SILICON ALLOY PINS IRRADIATED TO 0.03%
BURNUP IN CP-5 AUTOCLAVE TEST FACILITY
(RUN NO. 7)

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Based on results of this series of tests, none of the three alloys being considered is very promising as a core material resistant to corrosion-irradiation damage.

Samples tested to date have been heat treated for maximum corrosion resistance. Tests are in progress on samples of the zirconium-niobium alloy which have been given a duplex heat treatment. This heat treatment combines methods used for improving corrosion resistance with those used for improving dimensional stability.

(3) Irradiation of Zircaloy 2 Clad, High-Uranium Alloy Plates

The plates have cores of uranium, alloyed with 3% and 6% niobium, as well as 1.5% niobium-5% zirconium. The plates were fabricated by the modified picture-frame technique which results in a good metallurgical bond along the top and bottom of the plates, but not along the side edges or ends.

Following irradiation in the ANL Water Loop at MTR, the plates are to be drilled with deliberate defects to expose the core alloy during a post-irradiation corrosion test at Argonne. The results are to be compared with deliberate defect tests on similar plates¹⁶ and with tests on samples exposed in the out-of-flux portion of the MTR loop. In addition, long-term corrosion tests are in progress on plates without defects, as reported below.

It was previously reported in ANL-5345, page 93, that the in-pile cartridge of plates was removed for observation because of apparently increased flow resistance through the cartridge. The samples were intact with no visible blistering after an exposure to 0.04% burnup (of all uranium atoms). Subsequent operation of the MTR loop has indicated that the increased pressure drop is due to other causes (as described under Corrosion-Irradiation of Aluminum Plates, p. 60); therefore, the plates have been returned to MTR for further irradiation.

(4) Long-Term Corrosion Tests on Zircaloy 2 Clad, High-Uranium Alloy Plates

Four binary alloy plates are being tested in water at 550F and have accumulated 4600 to 6400 hr in static test (300 to 400 hr at 500F and the remainder at 550F) without indications of failure. Six other binary alloy plates are being tested at 500F and have accumulated 4900 to 6200 hr (all but about 300 hr in dynamic tests) also without indication of failure.

¹⁶Reactor Engineering Division Quarterly Reports: ANL-5345 (October 15, 1954); ANL-5371 (January 15, 1953).

b. Thoria-Urania Bodies

(1) Water Solubility Tests

Exposure of thoria-urania bodies in static, de-gassed, deionized water at 600F has continued with the results indicated in Table V. Since results were last reported, samples Nos. 4311 and 4335c have been started in tests.

The 4282 series of samples was made by dry pressing and firing at 1650C. The constituents for this group were hand mixed, resulting in nonuniformity and areas of high urania concentration. It was in these areas that the samples showed the greatest erosion and pitting. However, none of the samples have fractured or disintegrated in 2085 hr at 600F and are now showing uniform corrosion rates.

The constituents for samples No. 4291 and No. 4311 were mixed by ball-milling, resulting in more homogeneity. Pitting of specific areas did not occur, but only a slight roughening of the entire surface. Both samples were fired at 1830C; sample No. 4291 was compacted by dry pressing, while sample No. 4311 was slip-cast.

Sample No. 4335c (2.5% urania - fully enriched) was from a group prepared for irradiation tests (see below). The constituents for this group were mixed by ball-milling. Samples were dry pressed at 500 lb and fired at 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.

(2) Irradiation

Two samples of thoria-urania (2.5% urania - fully enriched) have been irradiated to 0.8% burnup of the uranium atoms. Exposure was in 500-515F water in a CP-5 autoclave. The cubic heat generation in these samples is estimated as 16 cal/(sec)(cc), resulting in a surface heat flux of 4.7 cal/(sec)(cm²) (6×10^4 Btu/(hr)(sq ft)) for the 1/4 in. diameter by 3/4 in. long samples. One of the samples was bare and the other in a Zircaloy 2 can (15 mil) with a 20-mil hole in one end.

The bare sample was intact after irradiation (see Fig. 12(a)) but had a transverse crack which resulted in a break during post-irradiation handling (see top edge in Fig. 12(d)). The area of the break showed distinct evidence of color gradations (Fig. 12(c)). The outside surface of the sample was in general quite satisfactory (Fig. 12(a)), but at one end there was some evidence of erosion (Fig. 12(d)) and "melting" (Fig. 12(b)).

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Table V

OUT-OF-PILE WATER TESTS ON THORIA-URANIA BODIES

(Water Temperature: 600F unless otherwise noted)

Sample No.	Sample Composition	Time, hr	Rate of Wt. Change, (Last Period) mg/(cm ²)(mo)	Total Wt. Change, gm	Comments
4282-4	5% urania-thoria	2085	-5.5	-0.2400	Pitting; spalling; turned pink.
4282-6	5% urania-thoria (1/2% CaF ₂)	2085	-5.2	-0.2862	Pitting; spalling; turned pink.
4282-10	3% urania-thoria	2085	-4.1	-0.1515	Pitting; spalling; turned pink.
4282-12	3% urania-thoria (1/2% CaF ₂)	2085	-4.2	-0.1585	Pitting; spalling; turned pink.
4282-13	Norton thoria	2085	-0.62	-0.0395	Bleached surface; striations on surface.
4291	10% urania-thoria	1645	-3.3	-0.0771	Surface roughened.
4311	10% urania-thoria	1346	-1.1	-0.0560	Surface roughened.
4335c*	2.5% urania (fully enriched)-thoria	328	-2.1	-0.0038	End was broken; fracture developing.

* Tested at 500F.

(a) INTACT SAMPLE

(c) PLAN VIEW OF TRANSVERSE
CRACK SHOWING COLOR
GRADATIONS

(b) "MELTED" REGION

AREA OF TRANSVERSE CRACK

(d) ERODED REGION

FIG. 12
UNCLAD THORIA-URANIA BODY (2.5%-FULLY
ENRICHED URANIA) IRRADIATED TO 0.8%
BURNUP OF URANIUM ATOMS IN 500 F-515 F
WATER. (CP-5 AUTOCLAVE TEST FACILITY)

ANL-CRB-16134-A
C.R.BREDEN:L.D., 6-10-55

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The Zircaloy 2 can of the second sample was in excellent condition; however, the sample itself tended to crumble when being removed from the can. Long-term irradiation (under the same conditions) of canned samples, one in Zircaloy 2 and two in stainless steel (all with a hole in the can), is in progress.

c. Aluminum and Its Alloys

(1) Corrosion

Nickel-Plated Aluminum: The 4300-H series of 2S aluminum samples, with a thick nickel plate (0.3 mil) applied by the Kanigen process, have accumulated 1564 hr in 600F degassed, deionized water at 18 fps. One of five samples failed after 1250 hr; the others show some pitting and weight changes of +0.1 to 0.4 mg/(cm²)(mo). Another set of four Kanigen-plated samples blistered, warped, or ruptured in a one-week test in static, degassed, deionized water at 600F.

Electro-plated samples (1-mil plate) have been tested for 581 hr in 600F degassed, deionized water at 18 fps. After weight changes of - 0.3 to - 0.4 mg/(cm²)(mo) during the first 260-hour test period, most samples show essentially no weight changes; all samples have a slightly loose black coating.

Aluminum-Nickel Alloy: Four samples of 2S aluminum containing 1/2% nickel have been tested for 314 hr in 600F degassed, deionized water at 18 fps. All samples showed a loose white coating over adherent, gray coating; weight changes of +7 to 8 mg/(cm²)(mo) were found. The alloy was vacuum melted and cast and then cold rolled. Analysis showed 0.48% nickel, 0.47% iron, 0.21% copper and 0.09% silicon, the balance being aluminum. Samples of aluminum-1/2% nickel alloy have satisfactorily withstood 300-400 hr in 500, 550, and 600F static, deionized, degassed water. The weight changes are given in Table VI.

Aluminum: Static corrosion tests are being carried out on 75S aluminum (5.5% tin, 2-5% magnesium, 1-5% copper, 0.3% chromium, and 0.2% manganese), on anodized 2S aluminum, and on 2S aluminum in acidic water.

The 75S aluminum (in both rolled and annealed condition) has been tested for 483 hr in degassed, deionized water at 400 and 500F. Thus far, the samples are intact with weight change rates leveled off at about +0.5 and +2 mg/(cm²)(mo) at 400 and 500F, respectively (see Table VI). The samples show localized black spots.

The 2S aluminum samples, subjected to various anodizing treatments with chromic or sulfuric acid, have been tested for 370 hr in degassed, deionized water at 500F. The samples have not failed, but do show weight change rates up to +20 mg/(cm²)(mo).

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Table VI

WEIGHT CHANGE RATES FOR ALUMINUM AND ALUMINUM-NICKEL
ALLOY IN STATIC, DEGASSED WATER AT HIGH TEMPERATURE

(Test in Progress)

Sample		Temperature, F	Test Period No. 1		Test Period No. 2		Test Period No. 3	
No.	Composition		hr	mg/(cm ²)(mo)	hr	mg/(cm ²)(mo)	hr	mg/(cm ²)(mo)
4336	2S Al-1/2% Ni	500	118	+5.78	165	+1.95	161	+0.65
4340a	2S Al-1/2% Ni	550	160	+10.95	161	+3.12		
4340b	2S Al-1/2% Ni	600	160	+13.7	161	+2.69		
4318a	75S Al	400	162	+2.92	160	+0.49	161	+0.49
4318b*	75S Al	400	162	+3.21	160	+0.49	161	+0.44
4318c	75S Al	500	162	+7.45	160	+2.2	161	+1.7
4318d*	75S Al	500	162	+6.93	160	+2.1	161	+1.7

* Samples annealed before corrosion test:

2 hr at 685F followed by 4 hr at 450F.

Samples of 2S aluminum tested for 325 hr in 500F water (pH = 2.5) showed weight changes of about $+0.8 \text{ mg}/(\text{cm}^2)(\text{mo})$ after a second 160-hour test period. Similar samples in pH = 3.8 water for the same period of time show much higher weight change rates ($+6$ to $8 \text{ mg}/(\text{cm}^2)(\text{mo})$).

(2) Corrosion-Irradiation of Nickel-Plated Aluminum and Aluminum-Nickel Alloy Plates

Sample plates of nickel-plated aluminum and of aluminum-1/2% nickel alloys were exposed to 500F water for 530 hr in the ANL water loop at MTR. During part of the MTR operation and during reactor shutdown the loop was at reduced temperature conditions, accumulating an additional 50 hr at 450F, and 150 hr at 350F. The in-pile samples received a maximum nvt of about 6×10^{20} during about 700 hr of reactor operation, equivalent to about 0.1% burnup of all the core atoms in a natural uranium fuel plate. Deionized, degassed water, with an average resistivity of 2 megohm-cm, flowed over the samples at an estimated velocity of 25 fps; pH measurements of the water were not taken.

The in-pile and out-of-pile cartridges each contained two samples each of the following:

- (1) 2S aluminum alloyed with 0.46 wt-% nickel (vacuum cast and rolled).
- (2) 2S aluminum plated with 0.3-mil nickel (Kanigen Process).
- (3) 2S aluminum electroplated with 0.7-mil nickel.

Post-irradiation examination of in-pile samples was somewhat obscured by the unusual heavy deposit of magnetite (Fe_3O_4) on the samples as shown in Fig. 13. After brushing the samples in acetone (which did not remove all the deposit), the samples were carefully examined and weighed. The following results were obtained:

Sample	In-Pile		Out-of-Pile	
	Sample No.	Weight Loss, gm	Sample No.	Weight Loss, gm
2S Al-1/2% Ni	A-4331	0.014	A-4333	2.727
	A-4332	0.941	A-4334	4.136
Kanigen Plated	A-4321	2.727	A-4320	0.562
	A-4322	2.223	A-4323	0.668
Electroplated	A-4327	1.293	A-4328	2.296
	A-4329	0.718	A-4330	1.436

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A-4321

A-4322

A-4329

A-4327

A-4332

A-4331

KANIGEN PROCESS
(0.3 mil NICKEL)

ELECTROPLATED
(0.7 mil NICKEL)

2S Al-1/2% Ni
(VACUUM CAST AND ROLLED)

NOTE: PLATES NO. A-4322, 4329, AND 4331 BRUSHED IN ACETONE

FIG. 13
IN-PILE TEST SAMPLES OF NICKEL PLATED 2S
ALUMINUM AND ALUMINUM-NICKEL ALLOY PLATES
SHOWING MAGNETITE DEPOSIT. PLATES EXPOSED
TO 500 F WATER AND 6×10^{20} nvt (MAXIMUM)
IN ANL LOOP AT MTR

The alloy samples had generally a smooth surface except for some indication of erosion near the combs holding the samples in the cartridge, where the turbulence of water flow would be the greatest. The in-pile alloy samples also showed some erosion at the leading and trailing edges, again an area of probably high turbulence (Fig. 14).

The out-of-pile weight losses are considerably higher than would be expected from other dynamic corrosion tests at 18 fps and are equivalent to about a 3-mil loss off the sample surface in one month. This surface loss was roughly substantiated by micrometer measurements after irradiation and comparison with nominal sample thicknesses. The weight losses in-pile are less, undoubtedly because magnetite and corrosion product could not be removed completely prior to weighing.

No adverse effect due to irradiation on the alloy samples were shown by the tests, but there is no clear-cut explanation for the high weight losses out-of-pile. It might be that the pH in the loop was slightly basic, which could have a marked effect on corrosion rate, especially at the high velocities. In future tests accurate pH measurements will be taken.

Examination of the Kanigen-plated samples revealed that the plating had come off in large areas of the in-pile samples, but had remained intact almost completely on the out-of-pile samples. This probably accounts for the much greater weight losses in-pile than out-of-pile.

The electroplated samples, both in-and out-of-pile, lost their plating over significant areas; hence, the out-of-pile weight losses are probably greater again because the magnetite deposit was not completely removed from the samples.

During this irradiation, there was definite indication of local boiling at the tip of the in-pile tube with the loop operating at 500F and 1000 psi. When the pressure was increased to 1500 psi, local boiling was suppressed. It now appears that local boiling was at least partly the cause for the increasing pressure drop encountered during the irradiation of cartridge MTR-W-2; however, it is not clear why this phenomenon was not encountered during operation before the second in-pile tube was inserted in September, 1954.

F. Physics Studies

1. The Effect of the Spatial Distribution of Voids on Reactivity - O. A. Schulze

In natural circulation, water-cooled reactors at incipient boiling, the steam voids are produced first in the regions of highest heat generation. The resultant effect on reactivity may be quite different from that predicted from the assumption of a uniform void distribution.

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TYPICAL ALLOY SURFACE (WITH
BLACK SPECKS OF MAGNETITE)

ERODED AREA AT EDGE AND NEAR
COMB (SHOWING BLACK AREA OF
ADHERENT MAGNETITE AND CORROS-
ION PRODUCT)

FIG. 14
2S ALUMINUM-1/2% NICKEL ALLOY PLATE
EXPOSED FOR 530 hr TO 500 F WATER AND
 6×10^{20} nvt (MAXIMUM) IN ANL LOOP
AT MTR (MAGNIFICATION:7X)

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In particular, the two boiling reactors which have been operated to date both had negative void coefficients based on a uniform void distribution. In Borax-I (where 0.060-in. thick plates were separated by 0.117-in. water channels) no evidence exists to indicate a positive change in reactivity as boiling began in a slightly subcooled excursion; however, in Borax-II (0.060-in. thick plates with 0.250-in. water channels) numerous excursions have indicated that as boiling began the reactivity at first increased, then decreased, forming a "hook" in the power curve.*

The local effects of voids can be estimated from perturbation theory. The two group equation for reactivity is¹⁷

$$\rho = \frac{\Delta k_{\text{eff}}}{k_{\text{eff}}} = \int_{\text{Reactor}} \frac{1}{\text{Vol.}} \left[\Delta \left(\frac{k_{\infty}}{p} \Sigma_{\text{as}} \right) \phi_f^* \phi_s + \Delta (p \Sigma_{\text{af}}) \phi_s^* \phi_f - \Delta (\Sigma_{\text{af}}) \phi_f^* \phi_f - \Delta (\Sigma_{\text{as}}) \phi_s^* \phi_s - \Delta (D_s) \nabla \phi_s^* \cdot \nabla \phi_s - \Delta (D_f) \nabla \phi_f^* \cdot \nabla \phi_f \right] dv \div \int_{\text{Core}} \Sigma_{\text{as}} \frac{k_{\infty}}{p} \phi_f^* \phi_s dv$$

where the asterisk indicates the adjoint of the function.

For convenience, assume a bare spherical reactor. Then the following relationships hold everywhere:

$$\frac{\phi_s^*}{\phi_f^*} = \frac{k_{\infty}}{p} \frac{1}{1 + L^2 B^2},$$

$$\frac{\phi_s}{\phi_f} = \frac{p \Sigma_{\text{af}}}{\Sigma_{\text{as}}} \frac{1}{1 + L^2 B^2},$$

and

$$\phi_s^* = \text{constant} \times \phi_s.$$

* These results to be published.

¹⁷H. L. Garabedian, "Theory of Homogeneous Control of a Cylindrical Reactor," WAPD-19 (September 20, 1950).

The reactivity can be rewritten as:

$$\rho = \frac{\int_{\text{Reactor Vol.}} \left[U \phi_s^2 + V \frac{\partial \phi_s^2}{\partial r} \right] dV}{\int_{\text{Core Vol.}} \frac{\Sigma_{as}}{p} k_{\infty} A \phi_s^2 dV} \quad (1)$$

where

$$U = \Delta \left(\frac{k_{\infty}}{p} \Sigma_{as} \right) A + \Delta (p \Sigma_{af}) C - \Delta (\Sigma_{af}) AC - \Delta (\Sigma_{as})$$

$$V = - \Delta (D_s) - \Delta (D_f) AC$$

$$A = \frac{p(1 + L^2 B^2)}{k_{\infty}}$$

$$C = \frac{\Sigma_{as}(1 + L^2 B^2)}{p \Sigma_{af}}$$

For a bare spherical reactor the flux is $\phi_s \sim (\sin Br)/r$. As r approaches zero $(\partial \phi_s / \partial r)^2 \rightarrow 0$, but $\phi_s^2 \rightarrow B^2$. Thus, if U is positive and if voids are produced only near the center of the reactor the reactivity will be positive.

The values of U and V have been calculated for Borax-I, Borax-II, and the present EBWR design. The results for a 20% reduction in water density are listed in Table VII. The reactors were assumed to be bare homogeneous spheres critical with no voids at the indicated temperatures. The critical constants are given in Table VIII.

The calculated value of U was positive for Borax-II but negative for Borax-I and EBWR. It is to be noted that U was obtained by taking the difference between numbers of nearly equal magnitude; hence, relatively small uncertainties in the coefficients can have a large effect on the final result.

The reactivities were determined from Eq. (1) as a function of the fraction of the core radius containing 20% void and are plotted in Fig. 15. For convenience the ordinate is the reactivity change divided by the cube of the radius $((\Delta \rho)/(F \times R_c)^3)$.

The following conclusions can be drawn from Fig. 15.

For Borax-I the reactivity change with 20% void is strongly negative regardless of how the void is distributed. Furthermore, voids produced near the center produce a greater reduction in reactivity than voids distributed uniformly over the core.

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Table VII

PERTURBATION COEFFICIENTS FOR 20% VOID

Coefficient	BORAX-I 200F	BORAX-II 200F	EBWR 486F
$\Delta \left(\frac{k_{\infty}}{p} \Sigma_{as} \right) A$	0	0	+0.000786
$\Delta (p \Sigma_{af}) C$	-0.01178	-0.00904	-0.009078
$-\Delta(\Sigma_{af}) AC$	+0.008126	+0.00694	+0.007017
$-\Delta(\Sigma_{as})$	<u>+0.002195</u>	<u>+0.00250</u>	<u>+0.000868</u>
SUM = U	-0.001459	+0.00040	-0.000407
$-\Delta(D_s)$	-0.06308	-0.0601	-0.08057
$-\Delta(D_f) AC$	<u>-0.4360</u>	<u>-0.329</u>	<u>-0.27901</u>
SUM = V	-0.49908	-0.3891	-0.35958

Table VIII

CRITICAL CONSTANTS

Coefficient	BORAX-I 200F	BORAX-II 200F	EBWR 486F
k_{∞}	1.4947	1.343	1.1222
p	1	1	0.8642
D_s	0.2605	0.246	0.3891
D_f	1.2739	1.237	1.2421
L^2	4.382	5.668	7.710
τ	63.54	53.81	50.65
B^2	0.00708	0.00561	0.002065
(Vol metal/Vol H ₂ O)	0.626	0.424	0.617
Reflector Savings	8.33	7.89	10.17
Core Radius	29.01	34.05	58.97

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FIG. 15
REACTIVITY CHANGE VS FRACTION OF
CORE RADIUS CONTAINING 20% VOID

RE-7-15768-A
O.A.SCHULZE:L.D., 4-27-55

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For Borax-II if voids are produced only near the center, the reactivity change is positive; however, if the voids are uniformly distributed over the core, the reactivity change is negative.

For EBWR the effect of voids is to reduce reactivity. As in Borax-I, voids formed near the center produce a greater reduction in reactivity than uniformly distributed voids.

The above conclusions may be illustrated by taking a specific example in which a central volume, whose radius is one-fourth of the bare reactor radius, is assumed to have a 20% void. (In the case of Borax-II this volume corresponds roughly to the central third of the four innermost fuel elements.) The reactivity changes produced by this central void and by the same total void distributed uniformly ($20\% / (4)^3 = 0.31\%$ void) over the bare reactor volume are indicated in Table IX.

Table IX
REACTIVITY CHANGES DUE TO VOIDS

Coefficient	Borax-I 200F	Borax-II 200F	EBWR 486F
20% void in central volume	- 0.0024	+ 0.00063	- 0.00078
0.31% void uniformly distributed	- 0.0012	- 0.00053	- 0.00033

In addition to the above calculations, two-group reflected sphere problems were performed on the UNIVAC for Borax-II. Using the same set of constants as above, the reactivity was determined for several sizes of a central zone containing 20% void. This is shown as a dashed curve in Fig. 15. Although qualitatively similar, the UNIVAC results are shifted downward. For the most part the difference can be attributed to the change produced in the thermal flux by the voids; this change is ignored in the perturbation method, which deals only with the unperturbed flux.

The calculations confirm what has already been demonstrated experimentally in Borax-I and Borax-II; however, quantitative comparison with experiment would depend upon an accurate knowledge of the amount and distribution of steam in an excursion.

2. Temperature and Void Coefficients - O. A. Schulze

Previous calculations¹⁸ of the resonance escape probability from EBWR were based on inaccurate values for the resonance self-shielding factor. The new values yield a higher resonance escape probability with the increase being greatest for operating conditions. The net result was to decrease the number of enriched elements required for operation from 42 to 40 and also to make the void and temperature coefficients less negative.

In operation with 17.5% average steam voids the reactivity controlled by voids is 1.64% as compared to the 2.23% calculated previously.

Figure 16 indicates the changes in k_{eff} with voids for three different temperatures.

The temperature void coefficients are given in the following table. These coefficients were calculated for the 4-ft diameter reactor with equilibrium concentration of xenon and samarium, but without plutonium, fission products, or control rods; 36.6% of the elements were enriched.

Average Temperature Coefficient		Average Void Coefficient		
		Temp, F	$\frac{\% \Delta (k_{eff}-1)}{\% \text{ Void}}$	
Temp Range, F	$\Delta (k_{eff} - 1)/F$		0-10% void	0-20% void
68-325	-0.57×10^{-4}	68	-0.004	-0.025
325-486	-0.98×10^{-4}	325	-0.041	-0.062
486-520	-1.5×10^{-4}	486	-0.070	-0.104

3. EBWR Shielding Calculations - M. Grotenhuis, R. J. Rickert

The results of final shielding calculations for the Experimental Boiling Water Reactor are presented in graphical form. Figures 17 to 23 represent the centerline fluxes of neutrons and gamma rays in three directions from the core: radial, axial upward, and axial downward. In similar fashion, Fig. 24 represents the heat generation in critical regions of the reactor shield.

The reactor was assumed to be a homogeneous sphere, 70 cm in radius, operating at a power level of 20 mw. The composition of the core is given in Table X.

¹⁸Reactor Engineering Division Quarterly Report, ANL-5371
(January 15, 1955).

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FIG. 16
 REACTIVITY AS A FUNCTION OF WATER
 TEMPERATURE AND DENSITY IN EBWR.
 36.6% OF FUEL ELEMENTS ENRICHED.

ANL-FWT-15769-A
 O.A. SCHULZE:L.D., 4-27-55

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ANL-FWT-15846-B
M.GROTENHUIS:L.D.,5-9-55

FIG. 17
CALCULATED FAST NEUTRON FLUX IN EBWR SHIELDING

FIG. 18
CALCULATED THERMAL NEUTRON
FLUX IN EBWR RADIAL SHIELD

ANL-FWT-15845-A
M.GROTENHUIS:F.S., 5-6-55

FIG. 19
CALCULATED THERMAL NEUTRON FLUX IN
WATER AND IRON ABOVE THE EBWR CORE

RE-7-16212-B
M.GROTENHUIS:L.R.D.,6-28-55

FIG. 20
CALCULATED AXIAL THERMAL NEUTRON
FLUX BELOW THE EBR CORE

FIG. 21
CALCULATED GAMMA-RAY FLUX
IN RADIAL EBWR SHIELD

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FIG. 22
CALCULATED AXIAL GAMMA-RAY FLUX
IN IRON SHIELD ABOVE THE E3WR CORE

ANL-FWT-15843-A
M.GROTHENHUIS:F.S., 5-6-55

FIG. 23
CALCULATED GAMMA-RAY FLUX
BELOW THE EBWR CORE

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RE-7-15850-B

FIG. 24
CALCULATED HEAT GENERATION IN EBWR SHIELDING

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Table X
CORE COMPOSITION
(in volume-per cent)

	Natural Elements (68%)	Enriched Elements (32%)
U ²³⁵	0.00178	0.00178
U ²³⁸	0.24561	0
H ₂ O	0.46318	0.46318
Zr	0.12528	0.37973
Nb	0.00884	0

The materials between the core surface and the outer shield surface are given in Table XI.

Table XI
SHIELD THICKNESS ON CORE CENTERLINES

	Radial, in.	Axial Upward, in.	Axial Downward, in.
Water	12 ($\rho = 0.8$)	54 ($\rho = 0.6$)	41 ($\rho = 0.8$)
Steam	--	115 ($\rho = 0.0$)	--
Iron	2.5	24	2.5
Insulation	3 ($\rho = 0$)	--	3 ($\rho = 0$)
Helium	3 ($\rho = 0$)	--	5.5 ($\rho = 0$)
Lead	3	--	3
Concrete	87	--	54 ($\rho = 2.4$ or 4.6)

4. D₂O Boiling Reactor Studies - H. P. Iskenderian

Calculations have been performed to determine the operating characteristics of 20-mw, D₂O-cooled and moderated, EBWR-type boiling reactors, with and without small additions of H₂O (given in volume-per cent).

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FIG. 25
SECTIONAL VIEWS OF 20-mw D_2O
BOILING REACTOR AND OF FUEL TUBE

The reactor core assembly (Fig. 25) consists of 32 fuel tubes arranged on an 8-inch rectangular lattice. The assembly is considered as a possible alternate replacement of the H₂O-moderated EBWR core for experimental purposes.

The physical dimensions and characteristics of the reactor are given below:

Core height, ft	4.75
Core ID, ft	4.25
Reactor Tank ID, ft	7
Uranium, ton	1.6
No. of fuel tubes	32
Reflector thickness, ft	
Radial	1.25
Axial (upper and lower), ft	1 (each)
Average temp. of moderator, F	486
Operating void density of coolant, %	40

The operating characteristics are given in Table XII.

Figure 26 shows the effect on the enrichment required for criticality (En_{cr}) of small additions of H₂O to the D₂O moderator. Computations based on these data indicate that the addition of 5% H₂O would result in a maximum increase in reactivity of about 0.66%.

FIG. 26
ENRICHMENT REQUIRED FOR CRITICALITY
(En_{cr}) VS CONCENTRATION (x) OF H₂O IN
D₂O - MODERATED BOILING REACTOR

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Table XII

OPERATING CHARACTERISTICS OF 20-MW UNIT FOR
DIFFERENT CONCENTRATIONS OF H₂O IN D₂O

(Equilibrium Xenon + Samarium)

Constant	Moderator			
	All D ₂ O	5% H ₂ O + 95% D ₂ O	10% H ₂ O + 90% D ₂ O	15% H ₂ O + 85% D ₂ O
p	0.844	0.879	0.903	0.923
f	0.943	0.903	0.868	0.832
ε	1.024	1.024	1.024	1.024
η	1.584	1.556	1.564	1.584
k _∞	1.2911	1.2650	1.2553	1.2456
L ²	95	87	75	61
τ	265	220	196	176
En _{cr}	0.0125	0.01175	0.012	0.0125
Initial Conversion Ratio	0.625	0.580	0.527	0.477
Mean Neutron Lifetime (cold)	3.7 x 10 ⁻⁴ sec		4.1 x 10 ⁻⁴ sec	

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The changes in k_{∞} , neutron flux leakage L , and k_{eff} , with the reactors cold clean, hot, and boiling, are shown in Fig. 27. It is of special interest to note that the reactivity change of 1.99% from "hot to boiling" in the D_2O -moderated unit has been reduced by a factor of ~ 2 by the addition of 10% H_2O . This reduction in void coefficient at boiling temperature is due to the different void characteristics of neutron leakage L , and reactivity k_{∞} , in the respective units, as shown on Fig. 27.

In case of an excursion with loss of the coolant in the tubes, a further reduction in reactivity will result due to increased leakage, streaming effect of neutron flux in the fuel tubes, Doppler effect, and a relatively small positive change in thermal utilization, f . It then appears that the steam void coefficient, hence the stability of the reactor, may be controlled by the addition of small amounts of H_2O , at the same time retaining adequate safety in the reactor.

These results were obtained from two-group calculations, using a τ_G value obtained from

$$\tau_G = \frac{\exp(\tau_m B^2) - 1}{B^2}$$

where

$$\tau_m = \tau \text{ of mixture}$$

as calculated from the equation:¹⁹

$$\tau_m = \int \frac{\frac{dE}{E}}{3 (\xi \Sigma_s)_{\text{mix}} (\Sigma_{\text{tr}})_{\text{mix}}}$$

and letting $\tau_{\text{D}_2\text{O}} = 127$; $\tau_{\text{H}_2\text{O}} = 30$ at room temperature.

¹⁹Reactor Engineering Division Quarterly Report, ANL-5260
(April 15, 1954) p. 58.

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FIG. 27
 VARIATIONS IN k_{∞} , LEAKAGE (L), AND k_{eff}
 FOR COLD, HOT, AND BOILING D_2O AND
 $H_2O + D_2O$ - MODERATED REACTORS
 (WITHOUT XENON + SAMARIUM)

ANL-FWT-15809-A
 H.P. ISKENDERIAN: H.S., 5-4-55

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II. FAST POWER BREEDER REACTOR PROGRAM - L. J. Koch

The major effort during the past quarter has been devoted to the construction of the EBR-II Working Model. The primary tank has been received. The heaters and connections have been installed preparatory to the installation of the external insulation. The sodium storage tank, the sodium charging tank, and 100 drums of sodium have also been received. The individual reactor system components, i.e., the dummy reactor, the fuel unloading system, the reactor cover and drive system, and the control rod drive system, have been assembled and are in operation. Appropriate sequence circuits are being added to these components to simulate operation of the equipment in EBR-II.

A. The EBR-II Working Model

1. EBR-II Working Model - M. P. Paleczny, A. R. Jamrog

The Working Model tank has been installed and is being implemented in the Liquid Metals Building. The outer tank was first set up on the supporting sand bed and the calrod heater units installed as shown in Fig. 28. The inner tank was installed and the heater leads brought through appropriate gas-tight electrical connections. The tank assembly prior to the installation of the external calrod units is shown in Fig. 29.

The sodium storage tank (6000-gallon capacity) has been received and is being installed in a temporary shelter immediately adjacent to Liquid Metals Building (Fig. 30). The two tanks will be interconnected by a piping system which will permit the transfer of sodium to either tank and will permit the by-pass of sodium from either tank through a cold trap. The sodium will be charged into the storage tank and will be circulated through the cold trap prior to filling the primary tank. One hundred drums of sodium, argon blanketed, have been delivered, and a special charging tank has been constructed to receive the sodium from the drums. The sodium can be transferred from the charging tank to the storage tank by an electromagnetic pump or by differential pressure in the two tanks.

2. Performance Tests on EBR-II Working Model Components

Pre-installation performance tests were conducted on the reactor components and mechanisms designed for operation in the sodium-filled primary tank of the EBR-II Working Model.

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FIG. 28
CALROD HEATER UNITS INSTALLED IN
OUTER TANK OF EBR-II WORKING MODEL

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FIG. 30
SODIUM STORAGE TANK (6,000-gallon
CAPACITY) FOR EBR-II WORKING MODEL

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Figure 31 is a composite view of the reactor in the "operating position." The drive mechanisms for the "submerged" components are located on the platform which simulates the top structure of the primary system tank. The reactor cover is closed, the control rod drive is engaged, and the fuel transfer system is inoperative. The "reactor" is loaded with a close-packed geometry of dummy fuel elements consisting of hexagonal stainless steel bars with suitable end adapters.

Figure 32 shows the sequence of operation of the fuel transfer system (unloading procedure). The equipment has been made to operate satisfactorily, as described, and has been disassembled for subsequent installation in the sodium tank.

3. 1000-gpm, d-c Electromagnetic Pump for EBR-II Working Model - A. H. Barnes

A 1000-gpm, d-c electromagnetic pump for the EBR-II Working Model has been assembled. Figure 33 shows the liquid duct and magnet components prior to assembly in a test loop for pre-installation performance tests.

B. Coolant Studies

1. Low-Frequency Induction Heating of Sodium Systems - R. A. Jaross

Conventional resistance heaters on high-temperature piping (1000F) tend to deteriorate and burn out after prolonged use, even though the heaters are not in actual operation. A more reliable heating method is desired, since the continuous replacement of resistance heaters is an arduous task. The actual requirements of the heaters in a sodium piping system are quite simple. All that is necessary is that the heaters raise the temperature of solid sodium from room temperature to the molten state (65 to 207F) to permit circulation. Normally, with a system in operation, heating is not required, and the heating system is inoperative.

The Ethyl Corporation has successfully used 60-cycle induction heating of their sodium transfer systems for many years. It is used on 2- and 4-inch carbon steel sodium lines and also on large sodium-mixing tanks (8 ft diameter) and simply consists of winding the insulated sodium vessel with a conventional copper conductor. It is not necessary that the coil of wire be adjacent to the pipe, but it can be wound outside the pipe insulation. For this reason the wire remains relatively cool (140F) even though the pipe temperature may be 1000F. The basic principle

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FIG. 31
COMPOSITE OF EBR-II WORKING
MODEL IN "OPERATING" POSITION

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FIG. 33
LIQUID METAL DUCT AND MAGNET ASSEMBLY -
1000 gpm d-c ELECTROMAGNETIC
PUMP FOR EBR II WORKING MODEL

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involved is the same as used in conventional high-frequency induction heating equipment. The primary sources of heat are the Eddy Current and Hysteresis Losses; however, each does not supply the same proportion of heat in 60-cycle heating as in high-frequency heating. This can be seen by the basic equations for each:

$$P_E = K_1 f^2 B^2$$

$$P_H = K_2 f B^{1.6}$$

where:

P_E = Eddy Current loss

P_H = Hysteresis loss

K_1 and K_2 = Constants dependent on geometry and physical composition

f = Frequency of flux reversal

B = Flux density

In the case of high-frequency induction heating, the Eddy Current Losses are predominant. If 60-cycle current is to be used it is apparent that the flux density (B) must be greatly increased. To achieve this, a high permeability is required in the pipe or vessel to be heated. For this reason a magnetic material must be used.

a. 60-Cycle Induction Heating of Types 405 and 446 Stainless Steel Pipe

The Type 400 series stainless steels have desirable corrosion characteristics and they are ferro-magnetic. For these reasons a test was made to determine the heating effect that could be achieved in a short length of insulated pipe with varying magnetizing forces.

A straight length of Type 405 stainless steel pipe, 4 in. IPS, Schedule 40, and 60 in. long, was insulated with 3-1/2 in. of Superex-85% Magnesia combination insulation. Over this insulation was wound one continuous coil of 8-gage round copper wire, each turn spaced one inch apart. The ends of the pipe were insulated with blanket insulation. The total coil consisted of 60 turns and had a d-c resistance of 0.127 ohm. Thermocouples were placed along the pipe from the center out. It was subsequently found that the center of the pipe was the hottest point due to the heat loss from the ends. An identical test was performed on Type 446 stainless steel pipe of the same dimensions.

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The results of these tests are tabulated below. In each case the voltage source was single-phase, 60 cycle.

Coil Current, amp	Coil Voltage		Ultimate Temp. at Center, F	
	Type 405	Type 446	Type 405	Type 446
15	3.81	3.60	177	160
20	7.75	7.52	259	243
25	9.68	9.51	345	322
30	11.6	11.4	415	398

Since these tests were run continuously, it was difficult to determine the time required to reach the ultimate temperature in each case; a good approximation for the 30-ampere case would be 24 hr to reach 90% of the ultimate temperature. This is meaningless, however, since the pipes contained no sodium.

These two particular Type 400 series stainless steels were selected because Type 405 has the best magnetic properties while Type 446 has the poorest magnetic properties. The other Type 400 series stainless steels lie somewhere between these two.

Although the Type 400 series stainless steels appear to be satisfactory with regard to induction heating, they are not equal to the Type 300 series (austenitic non-magnetic) stainless steels with regard to weldability. To investigate this problem, a 4-inch sodium loop is being constructed for a pump test containing Types 347, 405, and 446 stainless steels, and Type 4135 chrome-moly steel pipe. It is expected that useful information as to welding and corrosion properties of these steels will be obtained.

b. 60-Cycle Induction Heating of a Cold-Rolled, Carbon Steel Pipe Shell

A second method of induction heating is being explored which permits the use of Type 300 series stainless steel pipe. The non-magnetic stainless steel pipe is covered with a shell of magnetic material which is heated by induction heating; the inner pipe is heated by conduction. Since the outer shell is insulated, almost all of the heat produced in it is conducted to the inner pipe.

An outer shell consisting of two semi-circular carbon steel pipe sections, placed over the stainless pipe and welded, is being considered. To test this idea, a cold-rolled, carbon steel pipe shell was made

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in the following manner; two flat sheets were rolled to form a semi-circular section and were tack welded to form a completed pipe shell. This shell was 18 in. OD with a 3/16-in. wall thickness. The pipe shell was insulated with 4 in. of Superex-85% Magnesia combination insulation and the ends blanket-insulated. Over this insulation was wound one continuous coil, 8 ft long, of 10-gage round copper wire, plastic insulated. The coil contained 96 turns (roughly 1 turn per inch) and had a d-c resistance of 0.77 ohm. The completed pipe is shown in Fig. 34. Thermocouples were placed in the following positions:

No. 1 - Center of pipe (axially and radially).

No. 2 - Outside wall of pipe equidistant from tack welds.

No. 3 - Outside wall of pipe on a tack weld.

The results of the test to date are tabulated.

Coil Current, amp	Coil Voltage	Ultimate Temperature, F		
		No. 1	No. 2	No. 3
15.0	32.0	209	208	190
20.0	44.8	322	322	289

It will be noted that the temperature of the tack weld is lower than that of the pipe proper. This indicates that: (1) very little of the magnetic flux traveling axially in the pipe shell is passing through the tack welds; and (2) the paths that the eddy currents take are not circumferentially around the pipe but are probably in small loops inside the pipe wall. This difference in temperature would not be apparent if the pipe contained sodium, due to the thermal conductivity of the sodium.

There are many unknown factors yet to be investigated; among them is the effect of varying the cross-sectional area of the pipe. It is known that more steel will produce more heat, but the relationship is not known; it is evidently not linear. In the near future a test similar to that just described will be made using 12-in., Type 304 stainless steel pipe with a carbon steel outer shell.

2. Large Size Conventionally Packed Valves for Liquid Metal Systems - D. V. Hammond, F. A. Smith

Earlier experiments²⁰ indicated that the packing on a small-size valve can seal liquid sodium at 329F and 75 psig. It was

²⁰ Reactor Engineering Quarterly Report, ANL-5345 (October 15, 1954) p. 110.

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FIG. 34
INSULATED CARBON STEEL PIPE SHELL (18 in. IPS)

considered that a large valve with conventional packing might be used with similar results. This was predicated on the basis that the distance between the body and the stuffing box packing of a large valve would be adequate for maintaining a low stuffing box temperature, sufficient to seal liquid sodium, with the sodium in the valve at a higher temperature. It is believed that the leakage through a packing is very closely dependent upon the viscosity, and thus the temperature of the liquid at the packing (the packing to be of a substance which will not react chemically with the liquid metal).

A 14-in. gate valve, a Crane No. 33-1/2U-O.S.Y. 300-pound gate with Stellite face and seats, is now under test with NaK. The test facility is shown in Fig. 35. Figure 36 is a sectional view of the valve test assembly. The heat is supplied to the NaK in the valve body by electric bent-tube heaters which terminate in the flange. These heaters are controlled to maintain a definite NaK temperature in the valve body. Thermocouples located within 1/4 in. of the packing are used to measure the packing gland temperature. The pressure at the stuffing box is produced by helium pressure on the liquid level in the expansion chambers. An expansion chamber on each side of the gate allows for 16% thermal expansion of the liquid metal. Liquid level in the expansion chamber is measured by means of an inductance bridge-type portable level indicator.

The first packing arrangement to be tested was recommended by representatives of the Crane Company. There is a cast iron stuffing box spacer 3-1/2 in. long at the bottom of the stuffing box. This spacer with the stem hole bushing keeps the NaK approximately 5-1/2 in. from the first packing ring. Above the spacer are two rings of John Crane packing No. 201, consisting of asbestos and graphite with a thin, electrolytic copper foil wrapping which has been de-graphited. The next seven rings are John Crane No. 177-AI, consisting of asbestos and graphite with Inconel wire reinforcements. The next ring is John Crane No. 2 Super Seal, which is a plastic packing made of asbestos and graphite with neoprene rubber binder. The next four rings are successively graphite, plastic, graphite and graphite. The graphite is the same as previously described, and the plastic is the Super Seal-type ring. These rings were packed very carefully, and the packing was subjected to a leak test with, first, a detergent, and then with helium.

During the first month of heating the packing gland and nuts were taken up until the hand wheel was just firm in turning. The hand wheel required approximately 770 in. pound of torque to begin operation. An electric operator was installed after 20 cycles of operation. The valve was then operated continuously at 572F and 5 psig for 550 cycles. (A cycle on this valve is one complete open and closure, or two passes of the steam through the packing.) The temperature and pressure were increased in

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FIG. 35
TEST APPARATUS USED TO EVALUATE 14-inch
GATE VALVE FOR LIQUID METAL SYSTEMS

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FIG. 36

SECTIONAL VIEW OF 14 - INCH
GATE VALVE TEST ASSEMBLY

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increments and the valve was cycled intermittently, usually approximately six cycles each shift, or 18 times each day. The test conditions are summarized in Table XIII; to date there has been no evidence of leakage or sticking.

Table XIII

SUMMARY OF PACKING GLAND TESTS

Date	NaK Temp, F	Packing Gland Temp, F			System Pressure, psig	Valve Cycles
		Bottom	Middle	Top		
Feb. 7	572	225	200	190	5	20
March 7	572	225	200	190	5	550
March 21	572	225	200	190	50	18
March 28	662	230	212	200	35	132
March 29	706	240	230	212	35	21
March 30	752	275	250	235	35	113
April 4	842	315	270	250	35	20
April 5	900	330	285	265	35	21 (to date)

If a satisfactory packing cannot be developed as the primary seal for a valve of this type for high-temperature liquid metal service, it is expected that sufficient information will be obtained to permit the use of packing as a secondary seal. Bellows-sealed valves require a secondary seal in the event of a bellows failure. The secondary seal is only required to prevent a major leak until the necessary repairs can be effected. It is believed that a satisfactory packing can be developed to meet this requirement.

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

Convection heat transfer data on eutectic NaK in an experimental heat transfer loop²¹ have been partially analyzed, and additional data covering wide ranges of flow and heat transfer rates have been accumulated.

²¹ Reactor Engineering Division Quarterly Reports: ANL-5260 (April 15, 1954) p. 153; and ANL-5371 (January 15, 1955)

The ranges of conditions covered were as follows:

Flow:	318-3410 lb/hr
Fluid Bulk Temperature:	100-1175F
Reynolds No:	22,000-214,000
Peclet No:	297-3360
Nusselt No:	1.74-21.25
Heat Flux:	12,000-6,000,000 Btu/(hr) (sq ft)

In Fig. 37 fully developed Nusselt Numbers are plotted against Peclet Numbers for the data analyzed; the well-known Lyon equation²² is also plotted for comparison purposes. The accuracy of the inconsistent experimental values at $N_{Pe} \approx 300$ is considered reasonable since the average tube-to-fluid Δt was approximately 100F. It should be noted the English and Barrett data for mercury, as reported by Macdonald and Quittenton,²³ also exhibit a similar drop at the lowest Peclet numbers attained. Otherwise, the values in Fig. 37 do not disagree radially with the Lyon correlation, especially since the values may be in error from 10 to 20%.

The highest temperatures and fluxes were achieved during a deliberate tube "burnout" which was scheduled as the concluding test. No significant changes in film coefficients were observed. The copper tube, shown in Fig. 38, failed at its hottest region where the temperature was near the melting point of copper.

The effect of gas entrainment on the heat transfer properties of NaK was also investigated. A known amount of helium was injected into the flowing NaK stream (3400 lb/hr). With the injection of 0.05% gas (vol-%), no change in the surface temperatures of the test section occurred; at 0.15% the flow rate became erratic and began to decrease. The initial flow rate could not be restored by increasing the power input to the electromagnetic pump.

²² Liquid Metals Handbook, (2nd Ed. (Revised); Washington D.C: U.S. Gov't. Printing Office, 1954), p. 187.

²³ W. C. Macdonald, R. C. Quittenton, "A Critical Analysis of 'Wetting' and Gas Entrainment in Heat Transfer to Molten Metals." Paper presented at the Heat Transfer Symposium, Annual Meeting, AIChE, St. Louis, Mo., December, 1953.

FIG. 37
FULLY DEVELOPED HEAT TRANSFER DATA FOR EUTECTIC
NaK FLOWING TURBULENTLY IN A COPPER TUBE
(0.25 in. ID x 0.55 in. OD x 36 in. LONG)

TEST SECTION WITH INSULATION REMOVED

ENLARGED VIEW OF BURNOUT REGION

INSULATION AT BURNOUT REGION

FIG. 38
 COPPER TUBE TEST SECTION AFTER BURNOUT.
 BURNOUT CONDITIONS: 3200 lb/hr FLOW;
 6×10^6 Btu/(hr)(sq ft) MAXIMUM FLUX;
 1175 F MAXIMUM FLUID BULK TEMPERATURE;
 1880 F MAXIMUM TUBE TEMPERATURE

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The flow rate was decreased to 900 lb/hr for 15 hr to allow entrained gas to escape into the expansion tank. Assuming most of the entrained gas to be freed, the power input to the electromagnetic pump was increased. The pump tube burned out immediately (Fig. 39), indicating gas entrapment in the tube. Further attempts to measure the effect of gas entrainment on heat transfer were discontinued.

The pump that failed was replaced with another of similar design. However, the flow rate of the second pump was only 20% of that of the original pump. Attempts to increase the flow rate by heating the loop to 250F, or by pumping at a low rate for 48 hr, were unsuccessful.

The NaK in the loop was drained into the dump tank and a vacuum was applied to the dump tank. The loop was refilled under ~ 15 in. Hg vacuum. Upon startup, the pump rate rose immediately to the same value as that for the failed pump, indicating gas entrainment, not "non-wetting," was the cause for the decrease in pumping efficiency.

4. Design Study No. 1 Secondary and Steam System Conditions - M. C. Beekman, H. O. Monson, W. R. Simmons

A description of the EBR-II Design Study No. 1 was reported previously in ANL-5371. However, only a portion of the secondary and steam system conditions were available at that time. All of the major conditions of these systems now have been established and are presented below.

A skeleton flow chart of the entire plant indicating principal components, temperatures, pressures, and flow quantities is shown in Fig. 40. The temperature and flow rates are also listed in Table XIV. A temperature-enthalpy diagram for the water side of the boiler, with corresponding temperature distributions for secondary and primary system sodium superimposed, is shown in Fig 41. For the purposes of Study No. 1, the coolant outlet temperature from the reactor has been arbitrarily limited to 900F, even though thermal analysis of the Radiator Type No. 1 fuel element (see next section) indicates that a somewhat higher temperature (922F) is realizable. Gross net electrical power output is approximately 20,000 kw.

The secondary system flow rate and the steam throttle pressure values employed have been established, in part, on the basis of minimizing the thermal stress conditions in the evaporator section of the boiler. Within this section the heat transfer film coefficients on both sides of the tubes are very high, and the temperature difference across the tube wall represents an unusually large fraction of the total temperature

FIG. 39
ELECTROMAGNETIC PUMP TUBE FAILURE
ATTRIBUTED TO GAS ENTRAPMENT

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FIG. 40
SKELETON FLOW CHART -
EBR-II DESIGN STUDY NO. 1

FIG. 41
TEMPERATURE-ENTHALPY DIAGRAM FOR STEAM GENERATOR -
EBR-II DESIGN STUDY NO. 1

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Table XIV

OPERATING CONDITIONS-EBR-II DESIGN STUDY NO. 1

<u>Flow Rates, lb/hr</u>	
Primary System	5.2×10^6
Secondary System	2.2×10^6
Steam System	210,000
<u>Heat Exchanger Temperatures, F</u>	
Primary Inlet	900
Primary Outlet	770
Secondary Inlet	580
Secondary Outlet	880
<u>Steam Conditions at Turbine Throttle</u>	
Pressure, psig	1250
Temperature, F	850
<u>Feed Water, F</u>	
Inlet Temperature to Boiler	450

difference between the two heat transfer fluids. This, of course, tends to produce abnormally large thermal stresses in the tubes. In order to minimize this condition (through reduction of the total temperature difference), the flow rate of the secondary system sodium has been selected for maximum temperature rise through the heat exchanger consistent with allowable thermal stress in the cold end of that unit. Also, the steam saturation temperature has been established at a high level by selection of as high a throttle pressure as is economically practical, consistent with maximum conventional pressures for standard turbine units utilizing 850F steam.

C. Fuel Studies

1. Thermal Analysis of Radiator-Type No. 1 Fuel Element - H. O. Monson, W. R. Simmons, F. J. Tebo

The initial core loadings of EBR-II probably will employ pin-type fuel elements. For subsequent loadings, use of radiator-type elements is anticipated. A fuel element of the latter type, designated Radiator Type No. 1, recently has been designed, and a thermal analysis has been made to indicate its probable performance. This element is compatible

with the Pin Type No. 1 fuel element of Design Study No. 1,²⁴ that is, the dimensions of the hexagonal subassembly tubing in which the elements are contained, the fraction of core volume devoted to fuel alloy, and the total core volume are the same in both cases. Consequently, replacement in the reactor of a subassembly (or a complete core) of one type element with one of the other type element is readily accomplished.

Figure 42 is a sketch of a cross section through the Radiator Type No. 1 element. Composition of the fuel element subassembly is as follows:

<u>Material</u>	<u>% of Core Volume</u>
Fuel Alloy	32.0
Na Coolant	41.6
Stainless Steel	15.2
Na "Bond"	8.6
Na Clearance between subassemblies	2.6

Thermal analysis of the element is based upon the same conditions as those assumed in the analysis of the Pin Type No. 1 element made in conjunction with Design Study No. 1, as reported in ANL-5371. Also, the same upper and lower blanket sections of the core subassembly are assumed. The results are summarized in Table XV. Axial distributions of coolant temperature, maximum fuel alloy temperature, and maximum blanket uranium temperature are shown in Fig. 43.

It should be noted that the 1000F outlet coolant temperature indicated in Table XV obtains only in the "hottest" channel of the reactor and then only if the effects of all the uncertainty factors are manifested. If these effects of uncertainty factors were not included, the temperature would be about 954F (based on the same inlet temperature). The former value represents the maximum temperature thought possible to occur under normal, full-power operating conditions; the latter value represents the temperature considered most likely to obtain. Further, since perfect metering of coolant flow through all the subassemblies other than the "hottest" is not realizable, the expected average coolant outlet temperature for the entire reactor is somewhat less than 954F.

²⁴Reactor Engineering Division Quarterly Report, ANL-5371
(January 15, 1955)

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FIG. 42
CROSS SECTION OF RADIATOR-TYPE No. 1
FUEL ELEMENT SUBASSEMBLY

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Table XV

SUMMARY OF THERMAL ANALYSIS RESULTS FOR "HOTTEST" REGIONS
OF CORE AND UPPER BLANKET USING RADIATOR TYPE NO. 1
FUEL BLANKET

(With Uncertainty Factors)

<u>Coolant Flow</u>	<u>Core</u>	<u>Upper Blanket</u>
Nominal Coolant Velocity, fps	32	27
Nominal Flow Area per Subassembly, in ² .	1.938	2.285
<u>Heat Flux, Maximum, Btu/(hr)(sq ft)</u>	1,093,000	159,000
<u>Temperature, F</u>		
Uranium, Maximum	1200	1135
Coolant, at Outlet	1000 ⁽¹⁾	-
Coolant, at Inlet	782 ⁽²⁾	-
Coolant Temperature Rise, Inlet to Outlet	218 ⁽³⁾	-
Coolant, at Point of Maximum Uranium Temperature	929	996
<u>Maximum Temperature Differences at Point of Maximum Uranium Temperature, F</u>		
Through Uranium	117	103
Through Na "bond" Layer	13	3
Through Tube Wall	79	20
Through Coolant Film	62	13

(1) At outlet from Core subassembly (outlet from Upper Blanket).

(2) At inlet to Core subassembly (inlet to Lower Blanket).

(3) From inlet to outlet of Core subassembly (total through Lower Blanket, Core, and Upper Blanket).

FIG. 43
AXIAL TEMPERATURE DISTRIBUTIONS AT INNERMOST
EDGE OF CORE (RADIUS = 8.18 cm) USING RADIATOR-TYPE
NO. 1 FUEL ELEMENT. (UNCERTAINTY FACTORS INCLUDED)

114

A comparison of the inlet and outlet coolant temperatures for the Pin Type No. 1 and Radiator Type No. 1 elements under the full-power operating conditions of Design Study No. 1 is given below.

	<u>Pin Type No. 1</u>	<u>Radiator Type No. 1</u>
<u>Maximum Permissible Coolant Inlet Temperature, F</u>		
(with all uncertainty factors considered applied)	574	782
<u>Coolant Outlet Temperature, F</u>		
From "Hottest" Channel (with uncertainty factors)	773	1000
From "Hottest" Channel (without uncertainty factors)	731	954
Estimated Actual Average Reactor Outlet Temperature (assuming imperfect orificing)	702	922

2. Irradiation of EBR-II Pin-Type Fuel Element - E. L. Currier, J. E. Kemme, H. O. Monson, E. S. Sowa

Irradiation of a reference design (pin-type) fuel element has been under way in CP-5 since November 11, 1954. Descriptions of the fuel element, the fintube cooling assembly, and the normal (approximate) temperature distributions under irradiation are given in ANL-5371. During the present period the irradiation was terminated and examination of the fuel element was started. Results of the inspection thus far are described below.

Total irradiation time (at full power) was 1374 hr, corresponding to a calculated average burnup of approximately 0.38 fuel alloy atom per cent (based on fissions only, captures not included). A single measurement of local burnup has been made by radiochemical analysis (by the Cs^{137} method); the indicated burnup was 0.34 atom per cent. This is for the region 11.3 in. from the bottom of the fuel alloy stack (near the top end of fuel slug No. 6, the position of which is shown in Fig. 44). Radiochemical determination of exact burnup of each of the seven slugs will be made after additional decay time has been accumulated.

FIG. 44
SCHEMATIC OF IRRADIATED PIN-TYPE FUEL
ELEMENT WITH ENLARGED VIEWS OF
TUBE AND FUEL SLUG SURFACES. (4X)

The total number of thermal cycles accumulated was:

From normal operating temperatures to room temperature:	117
From transient (high) temperatures to room temperature:	<u>17</u>
Total:	134

This division into two types of thermal cycles based on estimated maximum cycle temperature is the same as that employed in ANL-5371. All of the 17 cycles from "transient temperatures" to room temperature (and possibly, a fraction of the 117 cycles) involved cycling of a portion of the fuel alloy through the alpha-beta transition temperature. Because of the high cooling rate employed, and the low thermal capacity of the test assembly, the rate of decrease of temperature during cycling usually was extremely high (most cycling being occasioned by reactor scram).

The fintube assembly containing the element was removed from the reactor on February 14, 95 days after insertion. The decision to end the test was based primarily upon the loss of thermocouples from the fintube. As the test proceeded, these thermocouples began failing one by one (in each case, during a reactor overpower scram circuit check). After all but one had failed, continuance of the testing was considered to be unsafe.

A cooling period of seven weeks was allowed before examination was started. The first measurement was a determination of the amount of gas in the sodium expansion reservoir inside the fintube. This was done so as to have a measurement of the fission product gas release by the element in event the element tubing had ruptured during irradiation. A capillary tube leading from the reservoir and projecting from the top end of the fintube had been provided for this purpose in the design of the assembly. It was found that the amount of gas in the fintube after irradiation was the same as that before irradiation, indicating a high probability that no fission product gas had escaped from the element. As an additional check, the gas was bottled for subsequent analysis for fission products.

After cutting off both ends of the fintube, the assembly was heated slightly to melt the sodium bond between the element and fintube and the element then pushed out. Residual sodium on the element surface was removed by brushing with water. The element appeared to be intact and in good condition except for an area of bumpy surface. This bumpy area extends from about 0.7 to 5.8 in. from the bottom of the fuel alloy stack. A cursory measurement indicated the maximum local diameter within this area to be between 0.198 and 0.202 in. (0.010-0.014 in. larger than the original diameter). The remainder of the element was smooth and of essentially

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unchanged diameter. Figures 44 (b) and (c) are close-ups of element surface areas centered at points $4\text{-}3/4$ and $6\text{-}1/2$ inches, respectively, from the bottom of the fuel alloy stack. These areas are typical of the bumpy and the smooth regions, respectively. Visual observation under a hand magnifying glass disclosed no holes or fissures in the fuel element tubing. Measurement of the fuel element tube length indicated no change in length due to irradiation.

The amount of gas in the fuel element fission product gas reservoir was next measured (using the capillary tube provided for the purpose at the upper end of the element). Within the accuracy of measurement, the amount of gas after irradiation was found equal to that before irradiation, indicating negligible escape of fission product gas to the reservoir (an increase in the amount of gas equivalent to 5% of the total fission gas generated at the calculated burnups easily could have been detected). As a further check on this measurement, the gas will be analyzed for fission products.

The fuel element internal sodium level was located by inserting a wire probe through the capillary tube. The level was determined to have risen about $1\text{-}3/8$ in. above its original height. This displacement of bond sodium (apparently caused by swelling of the fuel slugs) was volumetrically equal to about 7 to 8% of the original fuel alloy volume.

An attempt was made to remove the fuel slugs from the element by cutting circumferentially through the tube wall with a tube cutter at a point $1\text{-}1/4$ in. from the bottom end and pulling longitudinally from both ends of the element. Upon pulling, the No. 1 fuel slug fractured (at the location of the tube wall cutting) and the element broke into two pieces. This method was therefore abandoned. Longitudinal cuts were milled through the element tube wall from end to end of both pieces (three cuts on each piece, spaced at approximately 120 degrees). Following this the slugs were removed readily by peeling off the three tubing segments.

A portion of the bond sodium recovered was set aside for chemical analysis. Residual bond sodium could be seen adhering to the slug surfaces, more in some areas than in others. The slugs near the top of the element appeared uniformly silvery-white in color (the color of clean sodium). Those near the bottom appeared dark, much like tarnished uranium. Little adherent sodium was apparent on the latter slugs. A rather sharp line of demarcation between the areas of light and dark color existed, occurring at about 8.7 inches from the bottom of the fuel alloy stack (1 in. from the bottom of slug No. 5). Figure 44 (a) shows a portion of slug No. 2, typical of the dark-colored area. Figure 44 (d) shows a portion of slug No. 6, typical of the light-colored area.

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A portion of the slug surface was bumpy (Fig. 44 (a) and matched that of the fuel element tube surface. Although the majority of the bumpy area occurred on a 1.3 wt-% Zr alloy, a portion (about 1-2/3 in. of length) occurred on a 2.0 wt-% Zr alloy. A portion of each alloy (about 2/3 in. of the 1.3 wt-% Zr and 8 in. of the 2.0 wt-% Zr) remained unbumped. As seen from the approximate temperature distributions given in ANL-5371, the zone of bumping coincides with the zone of highest fuel alloy temperature during testing. Consequently, this was the zone subjected to the most severe thermal cycling (in that the rate of temperature change was necessarily highest, the range of cycling temperature the largest, and the likelihood of cycling through the alpha-beta transition temperature the greatest). This zone of bumpiness is thought not to coincide with the zone of highest burnup; however, this cannot be established with certainty until radiochemical burnup analyses become available. Approximate measurement of local diameters of the bumpy area indicated maximum diameters of about 0.174-0.176 in. (0.010-0.012 in. larger than before irradiation).

The density, length, and weight of each slug were measured (with the exception of the length of slug No. 1, which had broken into two pieces as mentioned earlier). These values are shown in Table XVI. The density measurements may not be precise, as it is not certain that the slug surfaces had been sufficiently cleaned. A second set of density determinations is to be made at a later date. On the basis of the present measurements, the average density change (or volume change) of the fuel alloy was about 10% (because densities of individual slugs were not measured before irradiation, the same original density was ascribed to each slug as that measured on representative samples from the respective fuel alloy melts). The average length change was 2.4%, with no clear difference between the two alloys. Since a longitudinal growth gap of length equal to about 3.1% (erroneously reported in ANL-5371 as 2%) of the original fuel alloy length had been incorporated in the element, there apparently existed no opportunity during the test for the element "restrainer" to effect restraint upon fuel alloy longitudinal growth. The post-irradiation weight values shown are subject to some error because the slugs may not have been thoroughly cleaned and because small amounts of fuel alloy were inadvertently removed in the process of milling through the fuel element tubing preparatory to stripping. The total weight change of the slugs indicated was small, although some individual slugs apparently lost weight and others gained.

Samples of alloy have been obtained from each slug for radiochemical determination of burnup.

Changes	
	Density %
	-9.7
	-6.8
	-11.4
	-10.5
	-11.3
	-9.7
	-12.2
sd	Weighted
ie	Average
	-10.2

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FIG. 45
PIN-TYPE FUEL ELEMENTS THERMAL CYCLED
200 TIMES IN NaK FROM 450 F TO 1330 F

119

3. Thermal Cycling of EBR-II Pin-Type Fuel Elements -
E. L. Currier

One 19-element, hexagonal subassembly²⁵ loaded with alpha-rolled uranium was beta thermal cycled in NaK. The individual elements of the subassembly were made of uranium pins (0.164 in. diameter by 14.300 in. long) contained in a Type 304 tube (0.172 in. ID; 0.008 in. wall). The end plugs of the elements were positioned to allow a growth gap of 1/8 in. inside the elements. A hole in both end plugs permitted access of the NaK direct to the uranium metal in order to expedite heat transfer during cycling.

The subassembly was cycled 200 times from 450F to 1330F with a holding time of 80 sec at each temperature and a 25-sec transition time. Figure 45 shows two of the individual elements after test; the elements have twisted, ruptured the cladding, and developed elliptical diameters. The average length decrease of the uranium is 6%. Earlier and similar thermal cycling tests²⁶ on identical, single elements (but without a rib) showed a uranium shrinkage of 5-1/2%.

D. UNIVAC Calculations on EBR-II - H I. Kraig, W. B. Lowenstein,
D. Okrent

A first set of multigroup calculations has been completed at the NYU UNIVAC relative to Design Study No. 1.²⁷ Further calculations are required; however, some results are available and will be reported herein.

The reactor model chosen for physics analysis is given in Fig. 46. The hexagonal-shaped regions of the reactor have been cylindricalized on an equal area basis. The compositions of the various regions (in volume-per cent) are listed on the next page:

²⁵Reactor Engineering Division Quarterly Report, ANL-5371 (January 15, 1955).

²⁶Reactor Engineering Division Quarterly Report, ANL-5345 (October 15, 1954) pp. 104.

²⁷Reactor Engineering Division Quarterly Report, ANL-5371 (January 15, 1955).

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FIG. 45
PIN-TYPE FUEL ELEMENTS THERMAL CYCLED
200 TIMES IN NaK FROM 450 F TO 1330 F

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<u>RADIUS,</u>	<u>cm</u>	<u>HEIGHT,</u>	<u>cm</u>	<u>HEIGHT,</u>	<u>cm</u>
R ₁	8.19	H ₁	365.21	H ₆	60.96
R ₂	18.12	H ₂	178.72	H ₇	13.061
R ₃	24.17	H ₃	60.12	H ₈	36.12
R ₄	34.87	H ₄	71.12	H ₉	7.62
R ₅	78.09	H ₅	55.25	H ₁₀	60.96
R ₆	90.80				
R ₇	151.77				

FIG. 46
VERTICAL SECTION OF DESIGN STUDY NO. 1

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Region	Fuel	Depleted U	Stainless Steel	Na
Core	32	-	17	51
Central blanket	-	55	13	31
Upper blanket	-	32	17	51
Lower blanket	-	32	17	51
Inner blanket	-	55	13	31
Outer blanket	-	80	8	12
Control rod (active)	22	-	18	60
Control rod (inactive)	-	-	13	87
Upper gap	-	-	40	47
Lower gap	-	-	37	57

The term "fuel" is synonymous with "meat" and describes the mixture of U^{235} and U^{238} in the fuel elements. The fuel cladding is stainless steel; however, iron was substituted in these calculations.

A new set of ten-group cross sections was used in these calculations (UNIVAC Problem No. 16). The group structure was revised somewhat from the previous ten-group cross sections,²⁸ the fission cross sections were altered to conform with the recent changes as published in BNL-250,²⁹ and certain transport and capture cross sections were made to conform with recent measurements. The new set will be published in a future quarterly.

The first set of calculations were all made in one-dimensional geometry, i.e., slab or cylinder. It is clear that the multi-region design does not lend itself well to such analyses, and two-dimensional calculations are in the offing. However, much valuable information can be obtained from one-dimensional calculations, and some preliminary results will be reported here.

²⁸Reactor Engineering Division Quarterly Report, ANL-5345, (October 15, 1954)

²⁹"Neutron Cross Sections," Brookhaven National Laboratory, (August 1, 1954)

1. Axial Reflector Savings

In a one-dimensional axial problem, an ambiguity exists in the proper model to use. For the core region, it was decided to use a composition representative of the fuel element itself, rather than an average over central blanket, core and control rod region. For the blanket, again various possibilities exist. In this calculation, an optimistic blanket density, i.e., that of the central and inner blankets, was used. Later calculations will be made using the composition of the end blanket.

With the model chosen, the axial reflector savings, averaged over both ends, were 9.2 cm at each end.

2. Critical Mass

Using this reflector savings, the critical mass of the reactor in its hot, clean condition was computed. Eight of the twelve control rods were taken to be in the pile, and the enrichments of fuel rods and control rods were varied together to obtain criticality. The resulting critical mass was found to be 210 kg of U^{235} .

3. Worth of a Control Rod

A calculation in cylindrical geometry, in which the active section of one control rod is replaced by its inactive section (sodium), gave the change in reactivity as $\Delta k/k = -0.007$. However, this number is an overestimate, and calculations remain to be done to correct for two deviations from fact. First, the active section is only the length of the real core, not the equivalent core. Second, when the active section is removed from the core, it remains of non-zero statistical weight. Both of these effects tend to reduce the calculated value.

If this "full length" control rod had been designed so that meat is replaced, not by sodium, but by an equal volume of depleted uranium, the resulting change in reactivity would be $\Delta k/k = -0.005$ per rod. On the other hand, had the meat been replaced by an equal volume of B^{10} , at normal density the reactivity loss would have increased to $\Delta k/k = -0.012$.

4. Accidents in Assembly

If a fuel rod were accidentally substituted for the central rod of the central blanket, the resulting increase in reactivity would be $\Delta k/k = +0.016$.

If a fuel rod were accidentally substituted for a blanket rod in the second row of the central blanket, the resulting increase in reactivity would be $\Delta k/k = +0.014$.

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If a fuel rod were accidentally substituted for a blanket rod in the innermost row of the inner blanket, the resulting increase in reactivity would be $\Delta k/k = +0.004$.

5. Axial Expansion of Fuel

If the fuel alone expands axially, the resulting loss in reactivity for small expansions is given by $\Delta k/k = -0.5 \Delta L/L$, where L is the original length of the fuel rod.

6. Sodium Density Effect

In cylindrical geometry, the sodium density coefficient was calculated to be $\Delta k/k \approx 0.07 \Delta \rho/\rho$. In slab geometry, using the relatively high-density blanket previously discussed, the coefficient was calculated to be $\Delta k/k \approx 0.10 \Delta \rho/\rho$.

7. Uniform Expansion of Assembly

Again, the results differ in cylindrical and slab geometry. For uniform expansion of the entire reactor, the result is

$$\Delta k/k \approx -0.6 \Delta V/V \text{ in cylindrical geometry}$$

$$\Delta k/k \approx -0.5 \Delta V/V \text{ axial geometry}$$

In cylindrical geometry, if only expansion of the central blanket and core region be allowed, with the inner and outer blankets remaining at original density, the result is $\Delta k/k \approx -0.4 \Delta V/V$.

8. The Cold Clean Critical Condition

If a "cold" temperature of 300F, contrasted to the hot core temperature of 790F, is assumed, and the resultant changes in density and dimensions are accounted for, the gain in reactivity is calculated to be +0.022, ignoring any Doppler effects. This is equivalent to a temperature coefficient of $\Delta k = -4.5 \times 10^{-5}$ per degree Fahrenheit.