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REACTOR ENGINEERING DIVISION

Quarterly Report

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Reactors-Research and Power

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REACTOR ENGINEERING DIVISION

Quarterly Report

PART A REACTOR PROGRAMSI. THERMAL POWER REACTOR PROGRAMA. Boiling Experimental Reactor (BER) - J. M. West1. General

A subcontract was negotiated with Sargent and Lundy, Engineers, to cover design of the building, power plant, and part of the reactor for the BER facility. The reactor core, reactor controls, and nuclear instrumentation are to be designed by Argonne National Laboratory.

An earlier proposal to develop seals for power plant components suitable for ultimate use with a D₂O system by making simulated tests has been modified in favor of testing the seals on the actual components in the BER system. A subcontract was signed with the Allis-Chalmers Manufacturing Company to develop, design, fabricate and install the following components:

- Turbine
- Generator
- Condenser
- Condensate and feed-water pumps
- Circulating water pumps for cooling tower circuit
- Pressure regulating system
- Air drying and vapor recovery system

These components are to be so designed that the combined leakage of system fluid (H₂O or D₂O) does not exceed one pound per day. Delivery to the BER site is to be on or before December 31, 1955.

The design schedule for Sargent and Lundy calls for sufficient detail to permit cost estimating and preparation of a final Safeguard Report before the end of 1954. To avoid delay in the over-all completion date of the BER facility, certain long-term delivery items such as the reactor pressure vessel and the major electrical equipment will be designed and sent out for bids well ahead of the remainder of the design. The building design is to be ready for negotiation with a general construction contractor early in 1955.

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Preliminary designs in considerable quantity have already been prepared by Sargent and Lundy and Allis-Chalmers and reviewed by Argonne National Laboratory. The final versions of these designs will be described as they evolve from cooperative efforts by these concerns.

2. Building

The gastight envelope around the reactor and steam system is to be a vertical steel cylinder about 80 ft in diameter and 120 ft high. A pressure of 10 psi or less would be developed in the 400,000 cu ft volume of this envelope in the event of a sudden accidental blowdown of the 10 tons of water in the pressurized system. The 5/8-in. thick shell is designed for 15 psig internal pressure.

A 15,000-gallon reservoir of water is suspended from the dome of the building. If pressure should be developed in the building due to a blowdown, this water would be released as a spray at a rate of about 1,000 gpm. This would quickly reduce the pressure and temperature as well as wash particulate radioactivity, if any, out of the air. The cooling water also would protect personnel from possible ill effects due to the high-temperature, saturated atmosphere which would otherwise exist.

In order to avoid the difficult problem of closing large dampers quickly and tightly against an internal pressure of 15 psi in case of emergency, the air change rate in the building is to be only that which is required to maintain freshness. The temperature and humidity are to be controlled by recirculating the building air through air conditioning equipment.

3. Reactor

The major features of the reactor pressure vessel are shown in Figure 1. The inside diameter of the vessel is 7 ft. The overall height is 24 ft. The operating and design pressures are 600 psig and 800 psig, respectively. The opening in the top of the vessel is large enough to permit insertion or removal of the core support grid. The 2-ft thick steel plug in the top of the vessel provides adequate shielding when the reactor is shut down and during operation attenuates thermal neutrons to a tolerable level from the radioactivation standpoint. This plug rotates and contains an eccentric rotating plug which permits positioning the fuel removal hole over any fuel element in the core.

The pressure vessel will be lined with stainless steel. A thermal shield inside the vessel may be necessary to reduce the thermal stress in the wall. This will require further study.

FIG. 1
BER PRESSURE VESSEL

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Consideration is being given to the use of moisture separators in the upper part of the pressure vessel. Since the entrained radioactivity is expected to be proportional to the moisture content of the steam, it is advantageous to extract as much moisture as possible before the steam leaves the reactor.

Piping connections to the pressure vessel are shown for feed-water inlet, steam exit, air vent, water level measurement, startup heating and shutdown cooling, and for control rod drive mechanisms.

The present version of the core design is shown in Figures 2, 3, and 4. The reference 4-ft diameter core contains 112 fuel assemblies, but provision is made for using up to 148 assemblies if desired. The grid locations which do not contain fuel will be filled with dummy zirconium elements.

Fuel assemblies are held in a basic 12-in. square grid structure. Nine fuel assemblies occupy each grid cell. Nine cross-type hafnium control rods operate in channels between these cells as shown. Cross-type rods were adopted when physics calculations showed that twelve 8-inch blades would not have enough control strength. The 8-inch spacing of the blades was also cramped, although apparently feasible. The smaller number and wider spacing of the cross rods will make design and maintenance of the rod drives easier as well as reduce the total cost of the drives.

A sheet metal shroud surrounds the upper grid and extends upward to a level $3\frac{1}{2}$ ft above the core. This shroud gives a chimney effect for improved natural circulation and reduces entrainment of steam in the downcomer. The small amount of steam which may be entrained will be condensed by the cold feed-water which is introduced in the downcomer by a distribution ring in a horizontal plane 6 in. above the top of the core.

The still (hot, but not boiling) level of water in the reactor will be $4\frac{1}{2}$ ft above the core and 1 ft above the shroud. At room temperature the water level would be about 2 ft lower. The total amount of water in the pressure vessel is about 10 tons.

The reference design has two kinds of fuel assemblies. The natural uranium assembly is shown in Figure 5. The enriched assembly (not shown) is essentially the same except that zirconium is substituted for U^{238} . The enriched plates are made thicker than would otherwise be necessary so that they would not reach their melting point ahead of the natural plates in case of a radical excursion. By making both types of plates reach their melting points simultaneously, more time is available for steam formation in an excursion, and full advantage is taken of the negative temperature coefficient of the natural uranium.

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FIG. 2
CUTAWAY PERSPECTIVE
OF BER CORE

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FIG.-3

BER CORE (ELEVATION)

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Physics calculations indicate that about one-third of the fuel assemblies in the 4 ft diameter core must be enriched. With such a uniformly spiked loading the reactivity is expected to decrease slowly with steam content of the core at room temperature and rather rapidly at operating temperature (488F). By decreasing the water to U^{238} ratio (about 2.5 in the reference design) this curve can be made steeper. This would make the reactor safer in a cold excursion but would increase the amount of k required to compensate for the steam in the core under normal operating conditions. This would increase the control rod requirements and might cause instability. It appears to be possible to achieve additional safety against cold excursions without increasing the Δk due to steam at operating conditions by making it impossible to withdraw the control rods until the water is hot. The present plan is to use building steam to heat the water to 300F or higher. Thermoelectric power from thermocouples immersed in the reactor water must actuate relays before the rods can be withdrawn.

The heat exchanger used for heating the reactor water prior to startup is also to be used for removing shutdown heat. The valving arrangement is such that either heating steam or cooling water can be passed through the tubes of this exchanger. The exchanger is located above the water level of the reactor, and therefore a pump is required to circulate reactor water through the exchanger for startup heating or for cooling below the boiling point. On loss of electric power, steam from the reactor steam dome still passes into the heat exchanger, is condensed and flows back into the reactor by gravity, thereby providing shutdown cooling. The heat is taken away by secondary water flowing to the exchanger under gravity from an elevated storage tank.

A facility for proof testing the reference mechanical drive for control rods under actual operating conditions is nearing completion. The rod extension shaft passes through a breakdown metallic seal and a packing ring separated by a bleed-off point for drawing off and recovering the leakage.

Although the mechanical type of control rod drive is expected to be satisfactory, particularly for H_2O reactors, alternate design and development efforts are being put into hydraulic and magnetic jack drive mechanisms. If reliable performance can be achieved at reasonable cost in either of these completely sealed types, one of the major problems in D_2O reactor design will have been overcome.

Since the major components of the external system of the BER facility are to be equipped on an experimental basis with special seals to reduce leakage to a level which would be tolerable for a D_2O system, a limited feasibility study is under way to determine whether the reference H_2O reactor could be used to study a D_2O type after an initial period of

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operation with H_2O . At present it looks as if the major change required would be replacement of the H_2O reactor fuel elements and grids with corresponding components for a D_2O lattice. Five of the original nine control rods could be used and would be adequate. A modified feed-water piping system would be required, since about 75% of the core area as well as the reflector acts as downcomer area in the D_2O version whereas only the reflector serves as a downcomer in the H_2O version.

Although the corrosion resistance of the reference fuel alloy (93.5% U, 5% Zr, 1.5% Nb) at operating temperature in the absence of radiation has been good, a very limited number of tests with low burn-ups in reactors indicate considerable loss of corrosion resistance. More tests will be necessary for a reliable evaluation of this effect. Parallel effort continues on the 3.8% epsilonized silicon alloy. Other proportions of zirconium and niobium additions are also being studied further.

4. Power Plant

Preliminary designs by Allis-Chalmers of the power plant components which they are to furnish for the BER facility are being reviewed. The integration of these components into the over-all plant is also being worked out by Sargent and Lundy and Argonne National Laboratory.

The basic philosophy being employed by Allis-Chalmers in meeting the maximum leakage specifications on their equipment is to use breakdown seals with low pressure bleed-off points. For example, on the turbine shafts, dry air is fed under slight pressure into one cell of the breakdown system from which part of the air leaks outward to the atmosphere and part inward to the next low pressure bleed-off cell where it mixes with steam leaking outward through still another breakdown seal from the turbine. The mixture of dry air and steam is piped to a drying system which uses refrigeration followed by chemical dryers to extract the moisture. The recovered moisture is returned to the main system and the dried air is recycled.

The condenser is to be a double-tube, sheet-type with provisions for bleeding off any leakage from the space between. A divided water box will be used to permit isolation of one-half of the condenser if a leaky tube should develop. The hotwell will also be divided so that the portion of the condenser having a leak can be determined more easily. In this case, the side which is at fault could be quickly valved off and drained of H_2O .

Calculations have been made of the amount of radioactivity expected to be deposited in the external steam and water system as a result of corrosion and erosion of the external system followed by activation of the corrosion and erosion products in the reactor. Since copper from

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Admiralty Metal tubes is the most active of these products, consideration is being given to using aluminum tubes in the condenser. The possibility of using tungsten instead of stellite tips on turbine blades is also being considered since cobalt has a long half-life and high activation cross section.

In order to reduce the number of seals required, the condensate and feedwater pumps which normally occur separately in power plants will be combined in the BER facility.

The pressure control system (Figure 6) which is required to maintain the steam pressure in the reactor within $\pm 1\%$ regardless of electric power demand changes is expected to consist of two (coarse and fine) hydraulically operated valves which bypass excess steam directly to the condenser, thereby making the reactor pressure and power level essentially constant for normal load changes. The question of whether the steam surge drum is necessary to attenuate pressure transients requires further study. An experiment will be performed on the Borax-II reactor to determine the value of such a surge drum.

The flow diagrams for the main and auxiliary steam and water systems are receiving concentrated attention and will be frozen soon. The present conceptions are shown in Figure 6.

It will be noted that the reactor relief and safety valves discharge to the condenser rather than to the building atmosphere. By keeping small the pressure drop in the lines between these valves and the condenser, this system should provide as much protection against pressure vessel rupture as the conventional system and at the same time avoid the hazard to personnel resulting from discharge to the room.

B. Boiling Reactor Experiment (Borax)

1. Borax-I

During July, a final runaway experiment was intentionally made on the Borax-I reactor under conditions which led to destruction of the reactor. In this final experiment excess reactivity of 4% was added suddenly so as to produce an exponential power increase with a period of 2.6 milliseconds. The maximum power level was between 13,000 and 20,000 megawatts. The integrated energy release of approximately 135 megawatt-seconds resulted in melting of most of the fuel plates, rupture of the reactor tank, and scattering of fuel element fragments up to a distance of perhaps 300 feet from the reactor. Extrapolation of the results of previous excursions with periods of 5 milliseconds and up would have predicted a nuclear energy release in the final experiment not far from that actually observed. The complexity of the geometry of mechanical components and other conditions prevailing during the burst

FIG. 6
BER FLOW DIAGRAM

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makes it difficult to determine whether the destructive effects observed are consistent with the hypothesis of a steam explosion alone or whether the severity was augmented by partial reaction of molten metal with the water.

The details pertaining to this final excursion as well as other excursion tests of the 1954 series which preceded it have been published in ANL-5323.¹

2. Borax-II

Design of the installation for the Borax-II reactor on a new site about 200 yards east of the Borax-I site was begun about August 1. Construction is proceeding rapidly and the reactor is expected to be made critical in October.

The purpose of the new reactor is to extend the Borax-I experiments on the transient and steady-state operation of boiling reactors (ANL-5211,² ANL-5323) to a design which approximates more closely that of a practical boiling power reactor. The reactor will be operated in steady boiling, as a steam producer, but the tests to date have been concerned with its transient behavior, and particularly with its ability to protect itself against the effects of sudden large reactivity increases by virtue of the reactivity decrease associated with the production of large volumes of steam in the reactor core. Qualitatively, the reactor is similar to Borax-I, but it is larger, is designed for operation at higher pressure, and is protected from the weather by a sheet-metal building.

The reactor tank, 4-1/2 ft diameter by 16 ft high, is fabricated of stainless steel to a design pressure of 300 psig. It is installed completely below ground level in a concrete-lined pit which can be flooded with H₂O for shielding to permit work on the reactor during periods of shutdown. The pit is provided with a removable lid of heavy concrete, 3 ft thick. The control rod mechanism is mounted above the pit, and drives the control rods by extension rods which pass through the concrete pit lid and through seals in the lid of the reactor tank.

The reactor core, which is mounted near the bottom of the reactor tank, consists of an adjustable number of modified MTR-type fuel elements held in a close-packed assembly by an aluminum grid. The core is completely submerged in H₂O, which serves as coolant, moderator, and reflector. H₂O circulation during reactor operation is by natural convection.

¹J. R. Dietrich, et al, "Experimental Investigation of the Self-Limitation of Power During Reactivity Transients in a Subcooled, Water-Moderated Reactor Borax-I Experiments, 1954," ANL-5323.

²J. R. Dietrich, D. Layman, "Transient and Steady State Characteristics of a Boiling Reactor," 1953.

The active sections of the fuel elements have the same external dimensions as standard MTR fuel elements, but they contain only 10 fuel plates, with a total loading of 93 g U^{235} per element. The ends of the elements are modified to eliminate the flow restrictions (ANL-5211). The predicted critical loading is 45 elements.

The reactor core is divided into quadrants by four pairs of flat aluminum-canned, cadmium control rods, 4 in. wide. At the center of the core is an additional flat cadmium rod, 3-3/8 in. wide, which can be ejected rapidly downward from the reactor to apply sudden reactivity increases for experiments on the self-limitation of reactor power. The five control elements are operated independently by individual mechanical-pneumatic drives (ANL-5323).

Most of the equipment outside the reactor tank, e.g., control rod mechanisms, reactor feed pump, reactor preheat system, water treatment system, and instrumentation, was originally used on Borax-I. The equipment was decontaminated and repaired after the final Borax-I experiment. Several new instruments were provided.

Adjacent to the reactor pit, but shielded from it by 5 ft of concrete, is the machinery pit which contains feed pumps, the water preheat circuit, steam controls and related equipment.

The deionizer system for the reactor H_2O is mounted at ground level, as is the nuclear and electronic instrumentation for recording the results of transient experiments. The entire installation is covered by a 40 ft by 60 ft Butler building. The reactor is to be operated remotely from a control trailer 1/2 mile south of the reactor.

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

A. Power Breeder Reactor (PBR)

A series of problems, involving reactors of about 800-liter core volume and consisting of varying reactor compositions, were solved on the UNIVAC to determine the effect of these compositions on reactor performance. Of particular interest was the effect on radial flattening by employing a central blanket of depleted uranium. As described previously,³ this is believed to be a feasible method to achieve radial flattening of a fast reactor. The heat generation per unit volume of uranium in the blanket varies tremendously from the edge of the core to the outside diameter: by a factor of several hundred in a clean blanket. Immediately surrounding the core, the heat generation is comparable to that in the central blanket. The use of two types of blanket elements is, therefore, being considered. An element with

³Reactor Engineering Division Quarterly Report, ANL-5297, July 15, 1954.

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low volume composition uranium and small cross section area is required in the high power density zone, while an element with high volume composition and large cross section can be employed in the low power density zones. The radial heat generation varies continuously across the blanket, and the desire for uniform coolant temperature rise must be accomplished by orificing. The use of two types of blanket elements reduces the range over which orificing must be effective. This approach results in three different blanket zones in the radial composition of the reactor; these have been termed the central blanket, the inner blanket, and the outer blanket. It is planned to adjust the compositions of the inner blanket and central blanket to permit the use of identical subassemblies in each location.

In this series of problems blanket compositions of 45%, 55%, and 65% uranium, by volume, were investigated in the central and inner blanket, the volume composition in the outer blanket being 80% uranium. The maximum to average power generation in the core was not very sensitive to changes in composition of the central and inner blanket; however, it was sensitive to the size of the central blanket. The results of these analyses are reported in detail in Part C, Section I.

The results of these calculations are now being used to provide the basis for an analysis of the PBR to optimize the reactor to achieve the maximum specific power for any given maximum power density attainable with a fuel element. A series of curves are being developed which will permit selecting the optimum reactor configuration for any fuel element geometry, provided the maximum power density has been established. Although this study has not been completed as yet, it is evident that in a large reactor the use of a central blanket results in improved over-all reactor performance. Although this optimum point varies somewhat with reactor size, it occurs in the vicinity of a central blanket radius of approximately 30% of the total radius of the central blanket plus the core. It is planned to continue these analyses and to extend the points under investigation in a future set of problems to be coded for the UNIVAC. In the meantime, present results are being utilized to prepare a reference design for EBR-II.

B. Experimental Breeder Reactor II (EBR-II)

A set of problems similar to those described above were also coded for EBR-II with a reactor core volume of approximately 50 liters. The results have not yet been analyzed for optimum performance; however, it is apparent that the use of a central blanket in a small reactor does not effectively improve performance. In the smaller reactor the unflattened, radial, maximum to average heat generation is smaller, and therefore the contribution from the central blanket is less effective. In the PBR this variation is approximately 1.5 to 1, while for EBR-II it is approximately 1.25 to 1. In the smaller reactor the critical mass is more sensitive to the introduction of the central blanket, and therefore the specific power is not

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improved even though some improvement is achieved in flattening. The decrease in specific power in EBR-II with the employment of a central blanket is small; therefore, a central blanket is being considered in EBR-II for engineering purposes. The use of a central blanket in EBR-II will make it possible to nearly duplicate the maximum to average heat generation which will be found in the PBR, for the flattened radial maximum to average heat generation is approximately 1.15 in each case. It is also believed advantageous from an operational standpoint to duplicate in EBR-II the anticipated type of loading to be used in the PBR. A preliminary design configuration has been established for EBR-II employing a central blanket, inner blanket, and outer blanket (Figures 7 and 8). This results in a central geometry consisting of sixty-one subassemblies including the central blanket, the core, and the control rods.

Work has continued on the design and construction of the scale Working Model of the EBR-II primary system, as described in ANL-5297. The design of the double-walled primary system tank has been completed and let for bids. The various components are in the process of fabrication and will be assembled and tested prior to installation in the tank. A platform is being constructed which will permit the assembly and test of the fuel-handling equipment, the pressure cover equipment, and the control drive unit in a series of dry runs prior to installation. It is anticipated that these units will be in good working order prior to installation in the tank.

A fuel subassembly consisting of 169 reference pin-type fuel elements was constructed for water flow tests. The fuel elements consisted of 0.188-in. OD tubes with a helical rib spiral wound about the OD on a 4-in. pitch. These tube assemblies contained steel pins of the reference fuel pin diameter of 0.164 in. With a water flow rate of 35 fps the pressure drop across the fuel section was approximately 35 psi. The results of this test are reported in detail in Part C, Section IV.

Plans have been made for an irradiation experiment of an EBR-II fuel element in CP-5. The test sample, which is to be a reference design tube assembly with a 14-in. long active fuel section, is now being fabricated. The sample is to be air cooled, and the necessary modifications have been made in CP-5 thimble, V-18. The fuel element assembly is to be contained in a finned capsule to provide the necessary heat transfer surface for cooling. The installation of the necessary equipment and instrumentation is now in progress.

Because of the lower thermal conductivity of uranium-plutonium alloys, attention is being given to other fuel geometries which permit a shorter heat transfer ligament in the fuel alloy. Although the reference design of pin-type element is believed to be near a practical minimum in diameter, the possibility of a smaller pin-type element is being investigated. A pin-type element utilizing a 0.125-in. diameter tube with a 0.101-in. diameter fuel pin is being constructed. The disadvantages of such a small-size fuel

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EBR-II
ELEVATION

FIG. 7

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FIG. 8

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element are rather apparent, and a second effort is being applied to the fabrication of a radiator-type element. Such an element has been analyzed and described previously in ANL-5297. A sample element is being fabricated by drilling machined wafers of brass to permit working on the problems of radiator and header fabrication, and to get a "feel" for this type of element. A test subassembly containing machined uranium wafers will be thermally cycled to determine the geometric effects of uranium distortion on the assembly. A program is under way to determine the feasibility of fabricating wafers by powdered metallurgy, and the Argonne Metallurgy Division is exploring the possibilities of fabricating longitudinal sections of fuel to be employed in a radiator-type assembly.

An irradiated uranium specimen has been inserted in the dynamic sodium corrosion loop to determine the corrosion of an unclad specimen and to determine the distribution of activity in the loop. This loop has been in continuous operation for several weeks; at a temperature of 360C there was no measurable distribution of activity in the system, nor was there any measurable pickup of activity in the sodium, which is sampled regularly for both activity and oxide content. Additional operation at temperatures of 450 and 500C resulted in some activity in the coolant. The results of this test are reported in detail in Part C, Section V. This program is directed toward the determination of fission product distribution in the reactor primary system in the event of a fuel element failure, or to determine the feasibility of relaxing the requirements on the fuel element container.

A 100,000-ampere homopolar generator has been obtained on loan from the Navy. This machine was built by the Allis-Chalmers Manufacturing Company and shipped disassembled to Argonne late in July. The generator had never been run successfully due to mechanical difficulties, and work on this unit at Allis-Chalmers had been stopped due to a change in program. Several modifications have been made in the unit, and it is now ready for test. If this unit can be made to operate satisfactorily, it will be used as the power source for the d-c electromagnetic pump to be used in the Working Model. For this application an output of approximately 50,000 amp is required.

PART B ADVANCED REACTOR DESIGN AND EVALUATIONI. NAVAL REACTORS - L. W. FrommA. Inherent Safety of the Argonne Submarine Advanced Reactor (SAR) Design - R. E. Rice, J. P. Silvers

In the continuing study of the inherent safety and reliability of the Submarine Advanced Reactor as proposed by Argonne,⁴ several possible accidents and malfunctions of reactor components have been considered.

In any examination of the safety of the Argonne SAR power plant, it is important that the control scheme of the plant be thoroughly understood. In the normal operating range of the reactor (above 450F) the balance of reactivity is determined only by the following:

1. Core life (uranium burnup, boron burnup, and fission product buildup).
2. Position of control rods.
3. Amount of xenon and samarium present.
4. Average coolant temperature.

The control plan calls for control rod position adjustment to compensate for changes in core life reactivity, and for average coolant temperature adjustment to compensate for variation in xenon and samarium concentration, both equilibrium and transient.

It should be noted that changes in power demand will affect only the inlet and outlet coolant temperature but will have negligible effect on the average temperature.* This phenomenon may be visualized by consideration of the reactor operating at some steady power level. If the demand for power decreases, the temperature of primary water leaving the boiler will begin to rise; when this water enters the core its higher temperature tends to raise the average core temperature and causes the core to become

⁴J. B. Anderson, "Water-Cooled Submarine Advanced Reactor (SAR) Power Plant Design Study," ANL-5217, June, 1954.

*Average temperature will be affected to the extent that higher power levels require higher fluxes and consequently higher equilibrium xenon and samarium levels. The latter will automatically be balanced by minor changes in average coolant temperature, provided no control rod adjustments are made.

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subcritical. However, with this condition of subcriticality the water, in passing through the core, is not heated to its former extent and the outlet temperature falls. The average temperature therefore tends to recover, and criticality is re-established with a lower core outlet temperature and a higher core inlet temperature than those previously existing. The resulting lower inlet to outlet temperature increase is equivalent to the reduced power demand. Some temperature fluctuations between reactor and boiler will probably occur, but each successive readjustment should be smaller than the previous one, and the total time to regain equilibrium should not be more than a few coolant cycles.

The most serious type of accident studied to date is one in which all available excess reactivity is added slowly to the reactor. Such a situation is possible under the following conditions. Consider that the core has reached maximum excess reactivity (the point in core life at which the difference between the boron burnup and uranium burnup is at a maximum; this occurs at about one-half the design core life). The average reactor temperature has been reduced to override transient xenon buildup. Shim and safety rods are completely withdrawn in order to override at the highest possible temperature, and the reactor is operated at maximum power demand (maximum flux) to burn out the xenon and reach full power in a minimum period of time. For this consideration it is assumed that the boiler feed-water heater is inoperative and that water is entering the secondary side of the boiler at 70F.

Under these conditions the average coolant temperature begins to increase, in order to maintain criticality, as soon as maximum xenon concentration has passed. Since the capacity of the boiler to remove heat is a function of the mean temperature difference between primary and secondary sides, reactor power level rises with the increasing average coolant temperature. When full power and normal average temperature (617F) have been reached, standard procedure calls for adjustment of the shim rod to maintain this average temperature while the remaining xenon burns out. When this is done boiler output will level off and remain at full rated power.

If, for reasons of operator error, sabotage, or malfunction of controls and equipment, no control rod adjustment is made when full power is reached (average temperature 617F), the average temperature will continue to increase as xenon burns out until approximately 665F is reached. At this point the temperature coefficient of reactivity matches the maximum available reactivity from core life and complete rod withdrawal, and operation levels off. Assuming that no reduction in steam demand and/or boiler feed-water flow is made, the rise in average temperature will be accompanied by an increase in reactor power level to about 130% of full rated load. In this case bulk boiling would take place in the region around the

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axial center line* near the exit of the core. The calculated distribution of this steaming region is shown in Figure 9. Actually, the reduction in moderator density resulting from steaming would cause the reactor to level off at an average temperature a few degrees below 665F and at a power slightly less than 130%. However, the effect of this phenomenon on predicted maximum metal surface temperatures would be insignificant.

Coolant temperatures and densities, as well as fuel element surface temperatures, were calculated for the hottest subassembly in the wire core under these maximum reactivity and power conditions. Curves of these values in terms of position along the subassembly are presented in Figures 10 and 11. The data for Figure 10 are based upon radial and axial flux distribution factors only, whereas values in Figure 11 include the hot spot factors for the wire core design estimated in ANL-5217. Both sets of data take into account the reduction in flow resulting from steaming in the outlet end of the subassembly.

In both curves the values of ΔT_{sat} (defined as the difference between the fuel element surface temperature and the saturation temperature of the coolant under boiling conditions) used to calculate surface temperatures were obtained for the local boiling region from the data of Jens and Lottes,⁵ and for the bulk boiling region from an extrapolation of the Rohsenow correlation⁶ from 2465 to 3000 psi.

The maximum surface temperature calculated from the boiling considerations either with or without hot spot factors is 699F. Steam qualities of 27% without and 42% with the hot spot factors are obtained at the subassembly outlet. The maximum theoretical hot spot heat flux is 640,000 Btu/(sq ft)(hr), and it is not felt that the steam-blanketing phenomenon usually associated with burnout would occur with flow across wire elements under these conditions. However, calculations were made to determine the upper limit the fuel element surface temperature would reach if only convection were taking place and neither local nor bulk boiling occurred to provide higher heat transfer coefficients. The results of these calculations

* It should be noted that a reduction in steam demand and/or boiler feed-water flow, in the absence of control rod adjustment, could prevent boiling by reducing the inlet to outlet core temperature rise and consequently the outlet temperature. However, since average core temperature is essentially a function only of reactivity considerations, the continued burnout of xenon and the absence of control rod adjustment would still force the average temperature to 665F to maintain criticality.

⁵ W. H. Jens, and P. A. Lottes, "Analysis of Heat Transfer, Burnout, Pressure Drop, and Density Data for High-Pressure Water" ANL-4627, May 1, 1951.

⁶ W. M. Rohsenow, "A Method of Correlating Heat Transfer Data for Surface Boiling of Liquids," Trans. ASME, 74, 969 (1952)

LEGEND

$\bar{\rho}$ = AVERAGE DENSITY, lb/cu ft

 = STEAMING REGION

CONDITIONS

AVG. CORE TEMP = 665F

POWER = 130% OF RATED LOAD

$$\text{FLOW} = 2.35 \times 10^6 \text{ lb/hr}$$

CONSTANT RADIAL FLOW

AVG. VELOCITY = 9.7 fps

FIG. 9
VOID DISTRIBUTION IN SAR CORE
AT MAXIMUM EXCESS REACTIVITY

FIG. 10
FUEL ELEMENT SURFACE TEMPERATURE, COOLANT
TEMPERATURE AND DENSITY ALONG HOTTEST SUB-
ASSEMBLY IN SAR WIRE CORE AT MAXIMUM EXCESS
REACTIVITY. (WITHOUT HOT SPOT FACTORS)

FIG. 11
COOLANT AND FUEL SURFACE TEMPERATURES ALONG
HOTTEST PATH IN SAR WIRE CORE AT MAXIMUM
EXCESS REACTIVITY. (WITH HOT SPOT FACTORS)

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are shown by the dashed lines in Figures 10 and 11. Under purely convection conditions the maximum surface temperature possible (from Figure 11) is 768F. Since at 3000 psi the convection coefficient for saturated vapor is only 4% lower than the value for saturated liquid, it can be seen that the fuel element surface temperature would not exceed 771F even with complete steam blanketing. This temperature is still far below the danger point for a stainless steel element. It is felt, therefore, that operation of the reactor at maximum steady-state over-power (all available reactivity added slowly, 130% of full rated load) probably would not damage the reactor plant. More concrete evaluation of the problem must await the obtaining of experimental heat transfer and burnout data in this range of temperature and pressure.

Other accidents and maloperations which have been investigated, together with their anticipated effects on the plant, are listed in Table I. In all cases, the reactor is considered to be operating at full power unless otherwise noted, and it is assumed that no automatic reactor scrams occur. It should be noted that in cases in which the steam generator is completely shut down for some reason, so that there is no cooling of the system other than by ambient heat loss, reactor decay heat will cause a slow temperature increase in the system even though the reactor will have become subcritical through temperature adjustment. The rate of temperature increase will depend upon the relative order of magnitude of the decay heat and the ambient heat loss. Thus a high core inlet temperature actuation of emergency cooling, or other auxiliary cooling, is necessary even in cases where primary coolant flow is maintained.

The minor excursions mentioned in the table are all considered to be less serious than the case of bringing the reactor from no load to full load by means of a 33F decrease in core inlet temperature in 1 sec (sine wave input characteristic). The latter case is currently under investigation using advanced computer techniques.

B. Flashing and Supercritical Reactor Studies - W. H. Cook

Studies of flashing type reactors for naval application to operate at 600, 800, and 1000 psia have been reported in ANL-5260.⁷ These studies have been extended to include a flashing reactor operating at 2000 psia and two supercritical reactors operating at 3500 psia. Reactor size, shape, method of control, and plant efficiency have been considered as functions of pressure and temperature. The physics problems associated with flashing and supercritical reactors are discussed in Part C, Section I.

The three reactors previously studied and those reported herein were calculated for 100-mw reactor output. Turbine exhaust conditions

⁷Reactor Engineering Division Quarterly Report, ANL-5260, April 15, 1954, p. 139.

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Table I

EFFECTS OF ACCIDENTS AND MALOPERATIONS IN SAR

<u>Condition</u>	<u>Effect</u>	<u>Conclusion</u>
1. <u>Restoring boiler feed water flow after cessation, or sudden increase in feed water flow.</u> Reactor power equal to ambient heat loss. Maximum steam demand. Average core temperature 617F. Full primary coolant flow. Feed water at 70F (heater inoperative) with feed pumps running. Feed-water valve is opened instantaneously.	1. Feed water flowing into boiler causes 66F drop in primary coolant in not less than 5 sec, as determined by time required for feed water to flow through boiler and time required for primary water to pass from boiler to reactor.	1. Increased reactivity from lower temperature starts minor excursion.
2. <u>Pump Startup during creep condition.</u> Reactor self-sustaining at about 3% power. One coolant pump on at 1/4 flow. Average core temperature 617F. Inlet temperature about 606F. Both pumps suddenly turned on at full flow.	2. Full flow is reached in about 1 sec. Core temperature drops slightly because of sudden surge through core of water at inlet temperature.	2. Increased reactivity from lower temperature starts minor excursion.
3. <u>Loss of Feed-Water Heater.</u> Feed-water heater heat source fails.	3. Surge tank of de-aerating feed-water heater contains about a 2-minute supply of water at full flow. With loss of heat source, entering water at condenser hot well temperature (128F) mixes with already heated water in heater and causes reduction in temperature of water entering boiler, spread over 2-minute period.	3. Two-minute reduction in boiler feed-water temperature may cause very minor excursion as primary water inlet temperature drops.
4. <u>Emergency cooling reserve tank valve opens.</u>		
a. All other valves remain in proper operating positions.	4a. Pressure in boiler prevents water from flowing rapidly into boiler.	4a. No accident.
b. Vent valves also open, and steam valve between boiler and turbine is closed.	b. Boiler is flooded with cold water at rate determined by gravity flow through valve and line from tank to boiler.	b. Minor excursion, not as severe as Case 1 above because of longer time.
c. Vent valves also open, and steam valve between boiler and turbine remains open.	c. Cold water flows by gravity to boiler, and slugs of water are carried into turbine with steam.	c. Possible damage to turbine blades by water slugs. Minor excursion as in b above.
5. <u>Cessation of boiler feed-water flow.</u>	5. Remaining water in boiler boils away within a few seconds, and reactor power drops to level of decay heat. Since decay heat is higher than ambient heat loss, average temperature will slowly rise (reactor goes subcritical) until high core inlet temperature actuates emergency cooling.	5. No accident.

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Table I (Contd.)

6. <u>Sudden steam demand.</u> In order of decreasing severity, caused by (a) full rupture of main steam line; (b) simultaneous opening of turbine throttle and dump valves; (c) blowing of steam relief valves; or (d) opening steam valve to standby condenser.	6. Provided an increase of feed water flow over full power rate is physically impossible, water in boiler flashes and reactor power adjusts to that required to boil water entering. Physical limiting of feed-water flow may be accomplished by proper design.	6. No accident.
7. <u>Emergency Injection System operates.</u>	7. Cold water is injected into the primary water just downstream of the boiler and is thoroughly mixed with hot water from boiler before entering reactor. Relative flow rate from emergency injection system is so small compared to primary flow that temperature decrease is insignificant.	7. No accident.
8. <u>Sudden closing of turbine throttle.</u> Simultaneous closing of boiler feed-water valve.	8. The water in the boiler at time of throttle closing will continue to boil, and its temperature and pressure will rise. The reactor will adjust in power to that being removed in the boiler. If the situation continues until the boiler water reaches the average reactor temperature (617F) and the only heat removed from the system is ambient load, then the reactor decay heat will force the primary system and boiler water temperatures up until the boiler relief valve blows and/or high core inlet temperature initiates emergency cooling. Mechanical design of the throttle valve to prevent full closing eliminates this difficulty.	8. No accident.
9. <u>Primary coolant pump failure.</u>	9.. Emergency cooling is initiated. Reactor reduces to a power equal to that removed by emergency cooling system.	9. No accident.
10. <u>Sudden removal of soluble poison from cold reactor.</u>	10. There is no means for rapid removal of soluble poison except by blowing all coolant, including poison, from the system, or by deliberate chemical precipitation. If all coolant were removed, reactor would remain subcritical because of absence of moderator. However, if this action were taken within a few hours after the reactor had been operating, decay heat would create a possible core melt-down situation similar to primary system rupture, a case still under consideration. Precipitation would occur in the core as well as elsewhere in the system, and the core would still remain subcritical.	10. No accident except as noted.

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common to naval power plants (16% mean stage moisture and 5 in. Hg back pressure) were used. A single turbine expansion with no reheat or separation stage was assumed. The internal turbine efficiency was taken as 77% for all cases.

The data for a flashing reactor operating at 2000 psia with the reactor mean outlet temperature subcooled 20F below saturation is given as Case 1 in Table II. As in the earlier study, a core metal to water ratio of 1:17, a flashing chamber and separator condition of 390 psia saturated steam, and a 100% separation efficiency were used. The plant flow diagram and steam cycle are shown below.

The increase of reactor pressure to 2000 psia results in an over-all efficiency gain of about 2.6%. However, this gain is accompanied by a considerable decrease in coolant flow, so much so that, with a core length/diameter ratio of 1, a three-pass arrangement is required to maintain sufficiently high coolant velocities and heat transfer coefficients to avoid excessive fuel element surface temperatures. It does not appear that the necessary increases in pressure vessel weight to contain the higher pressure, and core complexity to achieve the three-pass flow path, are warranted in view of the small gain in efficiency.

Cases 2 and 3 of Table II show calculated data for supercritical reactors operating at 3500 psia and at two different coolant outlet temperatures. In Case 2 the supercritical water is heated by the reactor only to a temperature at which the enthalpy is equal to that of 390 psia saturated steam, and flashing is accomplished through a throttle valve. The turbine

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used would therefore be identical with that in Case 1. This system results in a still greater reduction in coolant flow, since the total coolant flow is flashed to the turbine. There are several methods by which these low flows may be accepted without sacrificing velocity and heat transfer. The values given for Case 2 in Table II are for a core in which the coolant and moderating functions are separated, with the fuel and coolant clustered in tubes retaining the high operating pressure while the moderator fills the space between tubes. A design of this type offers such attractive characteristics as low fuel inventory and over-power control by moderator boiling and/or change of temperature. However, no increase in over-all plant efficiency over Case 1 is indicated for this type of supercritical system.

Alternative designs such as multi-pass cores and increased volume flow by recirculation of a portion of coolant might also be considered. Schematic flow diagrams for the separated moderator and coolant design and for the recirculating design are shown below.

In Case 3 the reactor outlet temperature is raised to the point where the supercritical water may be expanded directly through the turbine without flashing, and a considerable increase in over-all plant efficiency is effected. The cycle diagram for both Case 2 and Case 3 is shown below.

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Table IIFLASHING AND SUPERCRITICAL REACTOR DATA

Condition	Case		
	1	2	3
Reactor pressure, psia	2000	3500	3500
Pressure at turbine inlet, psia	390	390	3500
Flash fraction (X), lb steam produced/lb coolant circulated	0.319	1.00	-
Reactor coolant flow (W_r), lb/hr $\times 10^{-6}$	1.08	0.310	0.279
Steam flow (XW_r), lb/hr $\times 10^{-6}$	0.346	0.310	0.279
Subcooling at reactor outlet, F	20	-	-
Core length, in.	24.5 or 33.4	27.7	*
Core diameter, in.	27 or 23	25.3	*
Number of passes	3	1	1
Gross thermal efficiency, %	24.7	-	-
Over-all thermal efficiency, %	22.6	21.2	28.8

*Not determined specifically.

In both supercritical cases considered, anticipated temperature coefficients of reactivity are too low to permit full override of transient xenon by temperature control alone. In Case 2 the coefficient is too low to provide sufficient reactivity for override. In Case 3 override by temperature reduction is possible, but the lower temperature supercritical water could not be fed to the turbine because excessive moisture would form early in the passage through the turbine and would probably damage the blades. Therefore, during the override period no shaft power would be available, and dumping condensers would be required to remove the reactor heat from the coolant. A more complete discussion of control problems in flashing and supercritical reactors will be found on page 59.

The size and weight of the over-all plant is of prime importance in evaluating feasibility for naval application. Estimates for the flashing and supercritical reactors reported here indicate that plant components other than the reactor and flashing chamber will be of the same general size as those used in the Argonne Submarine Advanced Reactor (SAR) design.⁸

⁸J. B. Anderson, "Water Cooled Submarine Advanced Reactor (SAR) Power Plant Design Study," ANL-5217, June 1, 1954.

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The reactor for Case 1 will also be similar to the SAR design, but the size of the flashing chamber is not known. If the flashing chamber size can be kept smaller than that of the boiler which it replaces, then an over-all saving in weight and space may be realized. For supercritical Case 2 the flashing chamber consists only of a throttling valve, and Case 3 does not require flashing. The elimination of both boilers and flashing chambers, as well as savings in coolant flow, may result in appreciable weight and space reductions for supercritical plants.

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C. Natural Circulation Primary Coolant System for a Small Submarine Reactor - J. P. Silvers

A brief study was conducted to determine the feasibility of a pressurized water, natural circulation, primary coolant system for application to a small submarine developing 3500 shaft horsepower. Calculations were approximate and only major factors were considered.

The basic small submarine design used in this study assumed a single reactor compartment undivided horizontally, so that the entire hull inner diameter is available for the reactor and associated components. Access from one end of the compartment to the other is achieved by inclusion of a shielded passage through the compartment. For expediency, the STR Mark I boiler tube arrangement and size* were used as a reference, with the steam drum dimensions reduced in proportion to the reduced output of the small submarine. In four of the five cases studied, the core was considered to be identical with an STR Mark I core arranged for single pass operation, and it was assumed that the pressure vessel diameter might be decreased by the incorporation of a steel reflector as in the Argonne SAR design.⁹ In the fifth case the core diameter was increased to provide a heat transfer area twice that of Mark I. Vertical placement of both core and steam drum was judged to be best for this application. Other conditions used in the study were:

1. Primary coolant pressure, 2000 psia.
2. Steam conditions 240-340 psia, saturated.
3. Power generation (reactor heat), 20 mw.
4. Hull inner diameter, 18 ft.

Certain assumptions were made:

1. Minimum reactor outlet subcooling, 50F.
2. Maximum practical coolant temperature increase, 150F.
3. Total required ducting length, 40 ft.
4. Form losses caused by twelve bends, ten enlargements and reductions, and two valves, 7.2 velocity heads.

It was further assumed that placement of the steam drum directly above and resting upon the boiler shell would still permit sufficient boiler circulation for effective operation.

*2616 tubes, 3/8 in. OD on 1-1/2 in. diameter triangular pitch centers

⁹J. B. Anderson, "Water-Cooled Submarine Advanced Reactor (SAR) Power Plant Design Study," ANL-5217, June 1, 1954.

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The data for the five cases studied are presented in Table III.

In Case 1, wherein a 100F core temperature rise and a single boiler and coolant loop of 13.5-inch ducting were assumed, local boiling occurred, and the required head proved excessive to the extent that the plant could not be placed within the specified hull diameter. In Case 1a the same reactor conditions of flow and temperature rise were used, but two boilers (natural circulation type) and coolant loops utilizing 16.5-inch ducting were incorporated in order to reduce pressure drop and the resulting head requirements. The arrangement of components is shown in Figure 12. This case is feasible provided that (1) local boiling is acceptable; (2) the boiler secondary side would still retain sufficient circulation under roll, list pitch, and trim conditions; and (3) roll and list would not seriously affect primary system operation. The effect of roll may be significant in such cases as this where the boilers are low. Small lists will definitely reduce power.

In Case 2, the use of a larger core temperature rise reduced the plant size at the expense of increased local boiling. However, with a single boiler and loop using 13.5-inch ducting, the required head is large enough so that the steam drum could not be placed directly above the boiler; this is objectionable unless a forced circulation boiler is used. With this larger temperature rise, a plant using the double-loop arrangement of Case 1a could be achieved with somewhat smaller ducts and boilers than those shown for Case 1, and would be feasible with the same provisions as given for Case 1a above.

In Case 3 an attempt was made to utilize the components of Case 1a but to avoid local boiling by decreasing the core temperature rise and increasing primary coolant flow. Required head was much too high.

In Case 3a the core heat transfer area was doubled and the large system components of Case 1a (double loop) were used. The resulting low pressure drop, high flow, and low reactor heat flux combined to eliminate local boiling. A circulating head of 10 ft was necessary for this case. It appears that only a once-through boiler, as shown in Figure 13 could achieve that head in the specified hull. The feasibility of this case is contingent only upon the acceptability of the once-through boiler.

Some conclusions may be drawn from this study:

It is very important whether or not local boiling is allowable. With local boiling, low flow rates and corresponding large temperature rises are allowable without extra primary system pressurization, and plant size may be reduced accordingly. Local boiling is probably not objectionable from a reactor physics standpoint, but the surface corrosion-erosion effect of continuous local boiling should be determined.

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Table III

NATURAL CIRCULATION PRIMARY SYSTEM DATA

	Case Number				
	1	1a	2	3	3a
Temperature rise, F	100	100	150	41	50
Primary Coolant Flow (Reactor), lb/hr	569,000	569,000	379,400	1.314×10^{-6}	1.098×10^{-6}
<u>Boiler Data</u>					
Number of Units	1	2	1	2	2
Heat Transfer Area, sq ft (Total)	1236	1656	1495	*	*
Over-all length, ft	10	8.5	11		
Over-all coefficient, Btu/(hr)(sq ft)(F)	560	418	480		
Coolant velocity, fps	2.29	1.15	1.53		
Pressure drop, psi	0.115	0.0224	0.068	0.097	0.07
Boiler Type	---	Natural	Forced	---	Once-through
Steam pressure, psi	340	340	240	Over 340	Over 340
<u>Reactor</u>					
Core diameter, in	36	36	36	36	51
Pressure vessel diameter, ft	5	5	5	5	7
Coolant velocity, fps	1.21	1.21	0.81	2.8	1.17
Pressure drop, psi	0.061	0.061	0.03	0.264	0.0569
Local boiling condition	yes	yes	yes	no	no
Heat transfer area, sq ft	2226	2226	2226	2226	4452
<u>Primary Coolant Loop</u>					
Number of loops	1	2	1	2	2
Duct size (ID), in.	13.5	16.5	13.5	16.5	16.5
Head required, ft	12.9	2.89	4.6	23.2	10
Duct pressure drop, psi	0.019	0.0025	0.0094	0.0108	0.0078
Form losses, psi	0.386	0.0439	0.172	0.19	0.138
Total circuit pressure drop, psi	0.582	0.13	0.279	0.56	0.273
<u>Feasibility to the extent of this study</u>	no	perhaps	perhaps	no	yes

*Units of Case 1a used and were not specifically sized for Cases 3 and 3a. These units are probably somewhat oversized for Cases 3 and 3a because of improved coefficients and increased mean temperature differences.

FIG. 12
PRELIMINARY LAYOUT OF NATURAL CIRCULATION
PRIMARY COOLANT REACTOR FOR SMALL
SUBMARINE APPLICATION
(CASE 1a - NATURAL CIRCULATION BOILER)

FIG. 13
PRELIMINARY LAYOUT OF NATURAL CIRCULATION
PRIMARY COOLANT REACTOR FOR SMALL
SUBMARINE APPLICATION
(CASE 3a - ONCE-THROUGH BOILERS)

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With the specified hull size, two loops (bulky components) and maximum head utilization seem to be necessary to avoid local boiling. With or without local boiling, the plant components will be considerably bulkier than those necessary to provide the same performance with a forced circulation primary system.

Operation nearer the critical temperature and pressure would help reduce plant size by yielding a greater circulating head for a given coolant temperature change and by making greater coolant temperature changes possible for the same steam pressure. Even at lower temperatures, higher pressurization would of course eliminate local boiling without greatly affecting plant size and weight.

Because of the limited head room, a Mark I-type boiler does not look promising. Only about 10 ft of primary loop head is available if the boilers are not placed directly above the reactor (see Figure 13). An integral separator boiler might be used if it can be designed to circulate properly with a head no greater than the boiler shell diameter, and if it can be built so as to be unaffected by roll, list, pitch, and trim. Forced circulation units might be applied. For maximum head and minimum auxiliary equipment, a once-through boiler is indicated.

In transient operation, changes of power result in both changes in flow and core temperature rise. Reactor excursions and loop transients would be somewhat different than in the forced flow case. Intuitively, it seems that the reactor is probably stable against reactivity disturbances from circulation, since the time of flow through the core is about 3 sec, and cold water could not invade the core rapidly. The heat transfer response constant for the coolant temperature is slow (perhaps 2 sec), however, because of the low film coefficient, and significant $\frac{\Delta k}{k}$ change by control rod might give rise to severe excursions before temperature coefficient could correct.

It is not yet obvious whether or not a natural circulation primary system is preferable to forced circulation. It appears that a natural circulation system may be made to work to the extent of those factors investigated herein. Which is best may be answered only by more detailed optimization, comparative plant size and weight studies, and investigation of stability and control feasibility of the natural circulation system.

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PART C SUPPORTING DESIGN RESEARCH AND DEVELOPMENT

I. PHYSICS

A. Calculations on Fast Reactors - R. Avery

Further fast reactor calculations, as part of the EBR-II - PBR development program, have been performed on the UNIVAC at the AEC Computing Facility at New York University. Solutions were obtained for the multigroup diffusion equations in spherical geometry.

The calculations were designed to give information on the following:

Accuracy of diffusion theory for systems under consideration.
Steady-state properties of reference designs.
Neutron lifetime.

EBR-II and PBR are to have the same composition except for fuel enrichment. Many of the results were therefore obtained for this composition with the enrichment a parameter which varied from that corresponding to a system considerably more concentrated than EBR-II to a system corresponding to PBR.

The composition (to be referred to as the "standard composition") used was, by volume fraction,

	Pu^{239}	U^{235}	U^{238}	Ti	Na
Core		0.32		0.14	0.54
Blanket	0	0.0014	0.6986	0.1	0.2

The blanket thickness was in all cases taken to be 45 cm.

In most of the calculations, Pu^{239} was the fuel. The choice of Ti as the structural material was arbitrary and probably does not represent the most likely choice at present.

1. Cross Sections

The multigroup calculations were performed with the ten-group set of cross sections in Table IV. The ten-group set of cross sections incorporates a number of improvements suggested by H. A. Bethe on the previously used seven-group set of cross sections.¹⁰

¹⁰Reactor Engineering Division Quarterly Report, ANL-5260, April 15, 1954, p. 64.

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Table IV

TEN-GROUP CROSS SECTIONS

Group No.	Interval ($\ln 10/E$)	E_L , mev	β Fission Spectrum	Pu^{239} ($\nu = 2.9$)				U^{235} ($\nu = 2.5$)				U^{238} ($\nu = 2.5$)			
				σ_{tr}	σ_f	α	σ_{in}	σ_{tr}	σ_f	α	σ_{in}	σ_{tr}	σ_f	σ_c	σ_{in}
1	0-1.5	2.231	0.346	4.5	2.00	0.03	1.2	4.5	1.25	0.1	2.0	4.5	0.56	0.02	2.7
2	1.5-2	1.353	0.229	5.0	2.00	0.05	1.1	5.0	1.25	0.12	1.8	4.8	0.42	0.055	2.5
3	2-3	0.4979	0.301	5.5	1.95	0.07	0.7	5.5	1.20	0.15	1.0	5.0	0.03	0.12	1.0
4	3-4	0.1832	0.091	7.0	1.90	0.12	0.5	7.0	1.35	0.2	0.5	6.5	0	0.175	0.5
5	4-5	0.0674	0.025	9.5	2.2	0.2	0	9.5	1.90	0.25	0	9.0	0	0.25	0
6	5-6	0.0248	0.006	12.0	2.4	0.3	0	12.0	2.50	0.3	0	12.0	0	0.35	0
7	6-7	0.00912	0.002	13.0	2.7	0.35	0	13.0	3.10	0.32	0	13.0	0	0.5	0
8	7-8	0.00335	0	16.0	3.0	0.4	0	18.0	3.9	0.35	0	13.0	0	0.7	0
9	8-9	0.001234	0	16.0	4.0	0.45	0	23.0	5.5	0.38	0	13.5	0	0.9	0
10	9-	0	0	16.0	6.0	0.5	0	28.0	10.0	0.4	0	13.5	0	1.0	0

				Fe			Na			K			C		B^{10}		Ti		
				σ_{tr}	σ_c	σ_{in}	σ_{tr}	σ_c	σ_{in}	σ_{tr}	σ_c	σ_{in}	σ_{tr}	σ_c	σ_{tr}	σ_c	σ_{tr}	σ_c	σ_{in}
1	0-1.5	2.231	0.346	2.0	0.006	1.0	2.0	0.0002	0.1	2.0	0.02	0.3	1.2	0	2.0	0.2	2.0	0.01	0.8
2	1.5-2	1.353	0.229	2.0	0.006	0.7	2.0	0.0005	0.05	2.0	0.02	0.2	1.5	0	2.0	0.4	2.0	0.01	0.55
3	2-3	0.4979	0.301	2.0	0.006	0.3	3.0	0.001	0	1.5	0.02	0.1	2.0	0	3.0	0.3	2.0	0.01	0.25
4	3-4	0.1832	0.091	3.0	0.006	0	3.0	0.001	0	1.5	0.02	0	3.0	0	4.5	0.9	3.0	0.01	0
5	4-5	0.0674	0.025	3.0	0.006	0	3.0	0.001	0	1.5	0.02	0	4.0	0	4.5	1.9	5.0	0.01	0
6	5-6	0.0248	0.006	3.0	0.006	0	5.0	0.001	0	2.0	0.02	0	4.4	0	5.5	3.1	15.0	0.01	0
7	6-7	0.00912	0.002	3.0	0.006	0	5.0	0.001	0	1.3	0.02	0	4.4	0	8.8	5.2	30.0	0.01	0
8	7-8	0.00335	0	7.0	0.006	0	15.0	0.001	0	1.5	0.02	0	4.4	0	12.1	8.5	10.0	0.01	0
9	8-9	0.001234	0	7.0	0.010	0	10.0	0.002	0	1.5	0.02	0	4.4	0	17.6	14.0	6.0	0.02	0
10	9-	0	0	10.0	0.015	0	4.0	0.003	0	1.5	0.02	0	4.4	0	26.6	23.0	6.0	0.03	0

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The fission spectrum used was Watt's formula,

$$f(E) = \sqrt{\frac{2}{\pi e}} \sinh \sqrt{2E} e^{-E}$$

where E is the energy in mev.

The probability that a neutron is born with energy $> E$ is then given by

$$\begin{aligned} g(E) &= \int_E^{\infty} f(E') dE' \\ &= 1 + \sqrt{\frac{2}{\pi e}} \sinh \sqrt{E} e^{-E} - \frac{1}{\sqrt{\pi}} \left[I\left(\frac{\sqrt{2E}-1}{\sqrt{2}}\right) + I\left(\frac{\sqrt{2E}+1}{\sqrt{2}}\right) \right] \end{aligned}$$

where $I(x) = \int_0^x e^{-t^2} dt$.

The fraction of fission neutrons born in each of the energy groups is included in Table IV.

The inelastic scattering matrix was derived from a differential inelastic scattering law of the form:

$$\sigma(E_0 \rightarrow E) = (\text{Const.}) E \sigma_c(E) e^{-E/T}$$

where $\sigma_c(E)$ is proportional to the cross section for formation of the compound nucleus and was taken to be

$$\begin{aligned} \sigma_c(E) &= 1, \quad E \geq E' \\ &= \frac{\sqrt{E'}}{\sqrt{E}}, \quad E \leq E'. \end{aligned}$$

It was assumed that

$$T = 1 \text{ mev}$$

$$E' = 0.4979 \text{ mev } (u = 3).$$

For $E_2 > E_1 \geq E'$, we obtain

$$\int_{E_1}^{E_2} \sigma(E_0 \rightarrow E) dE = \text{Const.} \left[e^{-E_1} (E_1 + 1) - e^{-E_2} (E_2 + 1) \right]$$

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For $E_1 < E_2 \leq E'$, we obtain

$$\int_{E_1}^{E_2} \sigma(E_0 \rightarrow E) dE = \text{Const.} \sqrt{E'} \left[I(\sqrt{E_2}) - I(\sqrt{E_1}) - \sqrt{E_2} e^{-E_2} + \sqrt{E_1} e^{-E_1} \right]$$

It was assumed that no inelastic scattering occurred to final energies below group 7.

The inelastic scattering matrix for the energy breakdown used is given in Table V.

Table V

INELASTIC SCATTERING MATRIX

<u>j</u>	<u>X_{j→k}</u>					
	<u>k = 2</u>	<u>3</u>	<u>4</u>	<u>5</u>	<u>6</u>	<u>7</u>
1	0.3799	0.4402	0.1318	0.0366	0.0089	0.0026
2		0.7098	0.2126	0.0591	0.0143	0.0042
3			0.7324	0.2037	0.0493	0.0146
4				0.7612	0.1841	0.0547

The cross section for transfer by elastic moderation from group j to j+1 was assumed to be

$$\alpha^j \xi \overline{\Sigma_{el\ tr}^j}$$

where α^j is a constant which gives the ratio of the flux per unit u interval at E_{jL} (low energy end of jth group) to the total flux in the jth energy group; ξ is the average logarithmic loss per (spherically symmetric in center of mass system) elastic collision; and

$$\Sigma_{el\ tr} = \Sigma_{tr} - \Sigma_f - \Sigma_c - \Sigma_{in}.$$

In groups 1 and 2 it was assumed that the flux shape was the same as in a fission spectrum, yielding

$$\alpha^1 = \frac{E_{1L} f(E_{1L})}{g(E_{1L})} = 1.37$$

$$\alpha^2 = \frac{E_{2L} f(E_{2L})}{g(E_{1L}) - g(E_{2L})} = 1.84$$

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where $f(E)$ is the fission spectrum

$$\text{and } g(E) = \int_E^{\infty} f(E') dE'.$$

In the remaining groups it was assumed that the flux per unit u interval was constant over the group. Therefore, $\alpha_j = 1$ for $j = 3, 4, \dots, 9$, since $\Delta\mu = -1$ for these groups.

The multigroup diffusion equations are written in the form

$$\frac{i}{3 \Sigma_{tr}^j} \nabla^2 \phi^j - \Sigma_a^j \phi^j + \sum_{i=1}^{j-1} \frac{i}{\Sigma_j^i} \phi^i + \beta_j \sum_{i=1}^G \frac{i}{\nu \Sigma_f^i} \phi^i = 0$$

The macroscopic cross sections that enter formally into the above equations are given in Table VI.

2. Accuracy of Diffusion Theory

a. Comparison of Diffusion Theory and S_4 Method

Bengt Carlson¹¹ at Los Alamos has developed the " S_n Method" for numerically solving the Boltzmann Equation in a form suitable for high-speed computing machines. Four spherical problems, the solutions to which had been previously obtained by diffusion theory, were sent to Los Alamos and solved by the S_4 method. The ten-group cross sections were used in both sets of calculations.

The four assemblies were each of the standard composition and varied in enrichment from a system more concentrated than EBR-II to one approximating PBR.

The problem parameters and results are given in Table VII and Figure 14.

It is important to distinguish between the error involved if the composition is fixed with the radius being varied for criticality and the error when the radius is fixed and the composition is varied for criticality. The former error is approximately twice the latter.

It would appear that diffusion theory is quite satisfactory for PBR. For EBR-II an overestimate of $\sim 5\%$ in critical mass results from the use of diffusion theory if the radius is fixed in the critical composition then determined.

¹¹ B. C. Carlson, "Solution of the Transport Equation by S_n Approximations," Los Alamos Scientific Laboratory, University of California, LA-1599, October, 1953.

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Table VI

MACROSCOPIC GROUP CONSTANTS

Material	Group	Σ_a	$3 \Sigma_{tr}$	$\nu \Sigma_f$	$\Sigma_j^i + 1$	$\Sigma_j^i + 2$	$\Sigma_j^i + 3$	$\Sigma_j^i + 4$	$\Sigma_j^i + 5$	$\Sigma_j^i + 6$
Pu ²³⁹	1	0.130965	0.54	0.232	0.01880	0.02113	0.006326	0.001757	0.0004272	0.0001248
	2	0.129108	0.6	0.232	0.03234	0.009354	0.002600	0.0006292	0.0001848	0
	3	0.1123728	0.66	0.2262	0.02142	0.005704	0.001380	0.0004088	0	0
	4	0.106586	0.84	0.2204	0.01669	0.003682	0.001094	0	0	0
	5	0.107905	1.14	0.2552	0.002305	0	0	0	0	0
	6	0.127784	1.44	0.2784	0.002984	0	0	0	0	0
	7	0.148943	1.56	0.3132	0.003143	0	0	0	0	0
	8	0.171965	1.92	0.348	0.003965	0	0	0	0	0
	9	0.235427	1.92	0.464	0.003427	0	0	0	0	0
	10	0.36	1.92	0.696	0	0	0	0	0	0
U ²³⁵	1	0.162618	0.648	0.15	0.03709	0.04226	0.01265	0.003514	0.0008544	0.0002496
	2	0.1549349	0.72	0.15	0.06266	0.01837	0.005106	0.001236	0.0003629	0
	3	0.1154948	0.792	0.144	0.03641	0.009778	0.002366	0.0007008	0	0
	4	0.103731	1.008	0.162	0.02024	0.004418	0.001313	0	0	0
	5	0.116873	1.368	0.228	0.002873	0	0	0	0	0
	6	0.159528	1.728	0.300	0.003528	0	0	0	0	0
	7	0.200012	1.872	0.372	0.003592	0	0	0	0	0
	8	0.257855	2.592	0.468	0.005135	0	0	0	0	0
	9	0.370513	3.312	0.660	0.006213	0	0	0	0	0
	10	0.672	4.032	1.2	0	0	0	0	0	0
U ²³⁸	1	0.158113	0.648	0.0672	0.04991	0.05705	0.01708	0.004743	0.001153	0.0003370
	2	0.144152	0.6912	0.0504	0.08653	0.02551	0.007092	0.001716	0.0005040	0
	3	0.0567548	0.72	0.0036	0.03671	0.009778	0.002366	0.0007008	0	0
	4	0.034751	0.936	0	0.02062	0.004418	0.001313	0	0	0
	5	0.015528	1.296	0	0.003528	0	0	0	0	0
	6	0.021497	1.728	0	0.004697	0	0	0	0	0
	7	0.02904	1.872	0	0.00504	0	0	0	0	0
	8	0.038559	1.872	0	0.004959	0	0	0	0	0
	9	0.04828	1.944	0	0.005080	0	0	0	0	0
	10	0.048	1.944	0	0	0	0	0	0	0

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Table VI
(Cont'd.)

Material	Group	Σ_a	$3 \Sigma_{tr}$	$\nu \Sigma_f$	$\Sigma_j^i + 1$	$\Sigma_j^i + 2$	$\Sigma_j^i + 3$	$\Sigma_j^i + 4$	$\Sigma_j^i + 5$	$\Sigma_j^i + 6$
Fe	1	0.00706562	0.132	0	0.005697	0.0009684	0.0002900	0.00008052	0.00001958	0.00000572
	2	0.00783526	0.132		0.007505	0.0002339	0.00006501	0.00001573	0.00000462	0
	3	0.005643	0.198		0.005621	0	0	0	0	0
	4	0.005643	0.198		0.005621	0	0	0	0	0
	5	0.005643	0.198		0.005621	0	0	0	0	0
	6	0.009392	0.330		0.009370	0	0	0	0	0
	7	0.009392	0.330		0.009370	0	0	0	0	0
	8	0.028132	0.99		0.02811	0	0	0	0	0
	9	0.018784	0.66		0.01874	0	0	0	0	0
	10	0.000066	0.264		0	0	0	0	0	0
Na	1	0.0893022	0.5082	0	0.03628	0.03728	0.01116	0.003100	0.0007538	0.0002202
	2	0.0669990	0.5082	0	0.04928	0.01261	0.003504	0.0008478	0.0002490	0
	3	0.0310382	0.5082	0	0.02373	0.005176	0.001253	0.0003710	0	0
	4	0.0095622	0.7623	0	0.009054	0	0	0	0	0
	5	0.0095622	0.7623	0	0.009054	0	0	0	0	0
	6	0.0095622	0.7623	0	0.009054	0	0	0	0	0
	7	0.0095622	0.7623	0	0.009054	0	0	0	0	0
	8	0.0216582	1.7787	0	0.02115	0	0	0	0	0
	9	0.021987	1.7787	0	0.02114	0	0	0	0	0
	10	0.00127	2.541	0	0	0	0	0	0	0
K	1	0.00584613	0.0804	0	0.003085	0.001770	0.0005248	0.0001471	0.00003578	0.00001045
	2	0.00517278	0.0804	0	0.004127	0.0005698	0.0001584	0.00003832	0.00001126	0
	3	0.00254562	0.0603	0	0.001919	0.0002730	0.00006606	0.00001956	0	0
	4	0.001273	0.0603	0	0.001005	0	0	0	0	0
	5	0.001273	0.0603	0	0.001005	0	0	0	0	0
	6	0.001613	0.0804	0	0.001345	0	0	0	0	0
	7	0.0011376	0.05226	0	0.0008696	0	0	0	0	0
	8	0.001273	0.0603	0	0.001005	0	0	0	0	0
	9	0.001273	0.0603	0	0.001005	0	0	0	0	0
	10	0.000268	0.0603	0	0	0	0	0	0	0

Table VI
(Cont'd.)

Material	Group	Σ_a	$3 \Sigma_{tr}$	$\nu \Sigma_f$	$\Sigma_j^j + 1$	$\Sigma_j^j + 2$	$\Sigma_j^j + 3$	$\Sigma_j^j + 4$	$\Sigma_j^j + 5$	$\Sigma_j^j + 6$
C	1	0.02182	0.3018	0	0.02182	0	0	0	0	0
	2	0.03675	0.3771	0	0.03675	0	0	0	0	0
	3	0.02663	0.5028	0	0.02663	0	0	0	0	0
	4	0.03995	0.7542	0	0.03995	0	0	0	0	0
	5	0.05326	1.0056	0	0.05326	0	0	0	0	0
	6	0.05859	1.1061	0	0.05859	0	0	0	0	0
	7	0.05859	1.1061	0	0.05859	0	0	0	0	0
	8	0.05859	1.1061	0	0.05859	0	0	0	0	0
	9	0.05859	1.1061	0	0.05859	0	0	0	0	0
	10	0	1.1061	0	0	0	0	0	0	0
B ¹⁰	1	0.09315	0.8466	0	0.06493	0	0	0	0	0
	2	0.13424	0.8466	0	0.07780	0	0	0	0	0
	3	0.11369	1.2699	0	0.07136	0	0	0	0	0
	4	0.22215	1.905	0	0.09515	0	0	0	0	0
	5	0.33682	1.905	0	0.06872	0	0	0	0	0
	6	0.50082	2.328	0	0.06342	0	0	0	0	0
	7	0.82890	3.726	0	0.09520	0	0	0	0	0
	8	1.29415	5.121	0	0.09515	0	0	0	0	0
	9	2.07015	7.449	0	0.09515	0	0	0	0	0
	10	3.245	11.259	0	0	0	0	0	0	0
Ti	1	0.0496617	0.3396	0	0.02102	0.01993	0.005968	0.001657	0.0004030	0.0001177
	2	0.0379199	0.3396	0	0.02832	0.006618	0.001840	0.0004452	0.0001307	0
	3	0.0188022	0.3396	0	0.01445	0.002882	0.0006975	0.0002066	0	0
	4	0.007589	0.5094	0	0.007023	0	0	0	0	0
	5	0.012286	0.849	0	0.01172	0	0	0	0	0
	6	0.035776	2.547	0	0.03521	0	0	0	0	0
	7	0.071006	5.094	0	0.07044	0	0	0	0	0
	8	0.024036	1.698	0	0.02347	0	0	0	0	0
	9	0.015182	1.0188	0	0.01405	0	0	0	0	0
	10	0.001698	1.0188	0	0	0	0	0	0	0

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The exact agreement for the PBR between the two methods is probably fortuitous. However, the indication of a rapidly decreasing error with increased size for diffusion theory is clear.

Table VII

PROBLEM PARAMETERS AND RESULTS OF DIFFUSION THEORYS₄ METHOD COMPARISON

									Relative Error Due to Diffusion Theory	
Core Volume Fraction				Critical Radius (cm)		Critical Mass (kg Pu ²³⁹)		$\frac{\Delta M}{M_{S_4}}$	$\frac{\Delta M}{M_{S_4}}$	
				Diffusion	S ₄	Diffusion	S ₄	Fixed Composition	Fixed Radius	
	Pu ²³⁹	U ²³⁸	Ti	Na	Theory	Method	Theory	Method		
EBR-II	0.2170	0.1030	0.14	0.54	14.40	13.48	43.1	35.3	0.221	0.12
	0.1043	0.2157	0.14	0.54	22.85	22.18	82.7	75.6	0.094	0.05
	0.0561	0.2639	0.14	0.54	36.28	35.94	178	173	0.029	0.015
PBR	0.0351	0.2849	0.14	0.54	57.59	57.59	446	446	0	0

For all four assemblies:

	U ²³⁵	U ²³⁸	Ti	Na
Blanket Volume Fractions	0.0014	0.6986	0.1	0.2

Blanket Thickness

Diffusion Theory Calculation: 45 cm including extrapolation length

S₄ Calculation: 42.75 cm

3. Steady-State Properties of Reference Design

The results are given in Table VIII for four Pu²³⁹ fuel assemblies and two (corresponding to EBR-II and PBR) U²³⁵ fuel assemblies.

Table VIII

SURVEY OF STEADY-STATE PROPERTIES OF ASSEMBLIES OF EBR-II-PBR
COMPOSITION WITH VARIOUS ENRICHMENTS

For All Assemblies:

	Core Volume Fraction						Blanket Volume Fraction					
	Fuel + U ²³⁸		Ti	Na	U ²³⁵		U ²³⁸	Ti	Na			
	0.32		0.14	0.54	0.0014		0.6986	0.1	0.2			
	Concentrated Pu ²³⁹ Assembly		EBR-II Pu ²³⁹ Fuel		EBR-II U ²³⁵ Fuel		Intermediate Pu ²³⁹ Fuel		PBR Pu ²³⁹ Fuel		PBR U ²³⁵ Fuel	
Core Volume (liters)	12.5		50		50		200		800		800	
Core Radius (cm)	14.4		22.85		22.85		36.28		57.59		57.59	
Blanket Thickness (cm)	45		45		45		45		45		45	
	Pu ²³⁹	U ²³⁸	Pu ²³⁹	U ²³⁸	U ²³⁵	U ²³⁸	Pu ²³⁹	U ²³⁸	Pu ²³⁹	U ²³⁸	U ²³⁵	U ²³⁸
Volume Fractions in Core	0.2170	0.1030	0.1043	0.2157	0.1606	0.1594	0.0561	0.2639	0.0351	0.2849	0.0475	0.2725
Critical Mass	43.1 kg Pu ²³⁹		82.7 kg Pu ²³⁹		150 kg U ²³⁵		178 kg Pu ²³⁹		446 kg Pu ²³⁹		712 kg U ²³⁵	
Flux Integral for:	Core	Blanket	Core	Blanket	Core	Blanket	Core	Blanket	Core	Blanket	Core	Blanket
Group 1	2.60	2.55	3.58	1.78	3.45	1.71	4.40	1.11	4.99	0.62	4.89	0.61
2	2.32	2.91	3.45	2.11	3.34	2.04	4.51	1.36	5.31	0.77	5.23	0.77
3	5.33	13.42	9.43	10.72	8.94	10.19	14.33	7.51	18.84	4.55	18.40	4.48
4	4.11	20.29	8.59	17.34	8.04	16.37	15.25	13.15	22.72	8.54	22.09	8.37
5	3.09	31.03	7.26	27.63	5.95	25.15	14.55	22.23	24.14	15.37	21.84	14.61
6	0.95	11.00	2.41	9.86	1.71	8.81	5.18	8.03	9.08	5.63	7.59	5.26
7	0.34	4.03	0.94	3.65	0.57	3.21	2.20	3.02	4.09	2.16	3.13	1.97
8	0.097	1.38	0.32	1.29	0.15	1.09	0.88	1.11	1.81	0.83	1.21	0.73
9	0.028	0.39	0.12	0.38	0.040	0.31	0.41	0.36	0.99	0.30	0.54	0.24
10	0.005	0.094	0.031	0.099	0.005	0.072	0.15	0.11	0.46	0.11	0.16	0.067
Pu ²³⁹ Fissions	0.3337	0	0.3116	0	0	0	0.2925	0	0.2790	0	0	0
Pu ²³⁹ Captures	0.0399	0	0.0431	0	0	0	0.0463	0	0.0491	0	0	0
U ²³⁵ Fissions	0	0.0106	0	0.0091	0.3724	0.0084	0	0.0073	0	0.0050	0.3282	0.0047
U ²³⁵ Captures	0	0.0026	0	0.0023	0.0748	0.0021	0	0.0019	0	0.0013	0.0758	0.0012
U ²³⁸ Fissions	0.0128	0.1023	0.0387	0.0740	0.0276	0.0711	0.0607	0.0476	0.0764	0.0271	0.0718	0.0268
U ²³⁸ Captures	0.0144	0.6842	0.0662	0.5037	0.0402	0.5472	0.1572	0.4831	0.2805	0.3334	0.2307	0.3127
Na + Ti Captures	0.0017	0.0054	0.0033	0.0044	0.0029	0.0042	0.0072	0.0036	0.0098	0.0024	0.0097	0.0023
Total Fissions	0.3465	0.1129	0.3503	0.0831	0.4	0.0795	0.3532	0.0549	0.3554	0.0321	0.4	0.0315
Internal Breeding Ratio	0.039		0.187		0.090		0.464		0.855		0.571	
Blanket Breeding Ratio	1.831		1.702		1.224		1.426		1.016		0.774	
Breeding Ratio	1.870		1.889		1.314		1.890		1.871		1.345	
$\alpha = \frac{\text{fuel captures}}{\text{fuel fissions}}$	0.120		0.138		0.201		0.158		0.176		0.231	
$f = \frac{\text{U}^{238} \text{ fissions}}{\text{fuel fissions}}$	0.345		0.352		0.265		0.370		0.371		0.300	
Max/Average Core Power	1.27		1.40		1.36		1.61		1.87		1.84	
Escape	0.0746		0.0515		0.0484		0.0318		0.0174		0.0170	
Buckling (cm ⁻²)	0.01190		0.00656		---		0.00346		0.00173		---	
Reflector Savings (cm)	14.4		16.0		---		17.1		17.9		---	

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4. Neutron Lifetime

The neutron lifetime, ℓ , can be evaluated from

$$\ell = \frac{\delta\nu/\nu}{c},$$

where $\delta\nu/\nu$ is the loss of reactivity associated with the uniform insertion of a $1/\nu$ absorber with macroscopic cross section c/ν .

The average neutron velocities for each group are given in Table IX. They were obtained by taking the reciprocal of the average $1/\nu$ of the flux in the group under the assumption that the neutron flux in groups 1 and 2 was that of a fission spectrum while for all lower groups the flux per unit u interval was constant. It was assumed that the lowest group extended from $u = 9$ to 10. The energies corresponding to these velocities are also included in Table IX.

Table IX

NEUTRON VELOCITIES FOR LIFETIME CALCULATIONS

<u>Group</u>	<u>v, cm/10^{-8}sec</u>	<u>E, mev</u>	<u>$1/\nu$</u>
1	25.59	3.42	0.0391
2	18.26	1.74	0.0548
3	12.40	0.804	0.0806
4	7.522	0.296	0.133
5	4.562	0.109	0.219
6	2.772	0.0402	0.361
7	1.677	0.0147	0.596
8	1.018	0.00542	0.982
9	0.6174	0.00199	1.62
10	0.3746	0.000734	2.67

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Figure 15 gives the neutron lifetime as a function of enrichment (and thus of size) for spherical assemblies of the standard composition. The calculated lifetime is 1.7×10^{-7} seconds for PBR, and 0.8×10^{-7} seconds for EBR-II.

In order to determine the influence of the blanket, the lifetimes were also evaluated for the bare cores as a function of enrichment (Figure 15). The effect of the blanket on the lifetime decreases with core size. This result is reasonable, physically, since for the larger core size a smaller fraction of the neutrons causing fissions have spent part of their lifetime diffusing in the blanket.

There are several experimental values of the neutron lifetime for bare uranium assemblies of various dilutions and also for Topsy, the natural uranium reflected, 94 % enriched uranium assembly. These assemblies were evaluated in order to test the method and the cross sections. The curve of calculated lifetime vs enrichment for the bare assemblies is given in Figure 16 along with the experimental values. The agreement is quite good and thus lends some confirmation to the value obtained for EBR-II. The experimental points do not go to sufficiently low enrichments to give a good test of the low energy cross sections and thus of the PBR value.

For Topsy the agreement was (probably fortuitously) very good: 2.03×10^{-8} sec calculated vs 2.05×10^{-8} sec experimentally.

B. Flashing and Supercritical Reactors - G. I. Moller

A preliminary study has been made of the physics problems which are likely to be encountered in the design of a flashing reactor system. The following reactor has been considered:

Pressure	800 psia
Core temperature - inlet	412F
outlet	518F
average	465F
Subcooling at outlet	0F
Core diameter	23 in.
Core height	33 in.
Metal/water ratio	1/1.7

Structure and fuel elements were assumed to be of zirconium since the temperatures indicated are within the corrosion limits of this material. For these conditions the clean critical mass is about 5 kg U^{235} , not including allowances for depletion and for transient xenon override. The depletion allowance for 2000 hr at 100 mw reactor heat is 9.83 kg U^{235} .

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FIG. 16
NEUTRON LIFETIME OF BARE URANIUM
ASSEMBLIES VS U²³⁵ ENRICHMENT

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Since in this reactor at equilibrium the water at the core outlet is at saturation conditions, any increase in power demand by throttle adjustment causes an initial loss in pressure, and a portion of the water in the core flashes to steam in an effort to maintain pressure. The reactor tends to become subcritical because of the increased void content. However, with the reactor subcritical, the feed water which continues to enter the core is not heated to as high an outlet temperature, and the average temperature falls until the temperature coefficient has made up the reactivity loss and criticality is again established. Some fluctuation will probably occur before equilibrium is reached, but the condition is not thought to be unstable.

Results of pressure changes on the primary coolant have been calculated. In the table below, ΔT is the drop in temperature required to overcome the void coefficient of reactivity, based on an assumed temperature coefficient of $-4 \times 10^{-4} \delta k/k$ per degree F and assuming no change in voids as the temperature falls.

Pressure Change, psi	% Void Distributed In Water	% $\delta k/k$	ΔT (F)
0	0	0	0
-20	1	-0.22	- 5.5
-40	2.5	-0.60	-14.8

It is doubtful that a core not specifically designed for a high temperature coefficient would have a coefficient as large as that assumed here.

The production of voids in the core with increases in power demand may result in a difficult problem of reactor control and should be avoided by resorting to subcooling, so that at no time is there appreciable steam formation within the core except at its very outlet, where the effect on reactivity is minimized.

The core considered here is small in diameter and would permit the inclusion of only a relatively small number of absorbing rods to control the excess reactivity required for xenon override, fuel depletion, and cold shutdown. Burnable poisons such as boron-10 and lithium-6 may be relied upon for control of fuel depletion to a certain extent. However, soluble poisons are probably not practical for flashing reactors because of possible deposition in the flashing nozzle.

Since the critical mass is held to a minimum, the operating neutron flux and consequently the transient xenon poisoning are certain to be quite high. It is not likely that override can be achieved by temperature coefficient alone; therefore, it appears that for the reactor considered here the chief reliance must be placed on control by rods.

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A very preliminary investigation of the physics of supercritical water reactors has been made. In the case studied the functions of the water as moderator and as coolant were separated. Such an arrangement permits the volume of moderator to be increased greatly without large increases in core dimensions and without decrease in coolant velocity and heat transfer coefficients. In the tabulation the metal volume was assumed to be the same as that in the Argonne Submarine Advanced Reactor (SAR) design,¹² about 51,000 cu cm. The volume of water acting as coolant was assumed to be 8700 cu cm, and the water moderator volume was allowed to vary with the metal to water ratio. The length to diameter ratio was taken as unity. Fuel and coolant were assumed to be clustered in tubes retaining the supercritical pressure, with the moderator filling the spaces between tubes.

<u>Metal to Water Ratio</u>	<u>Radius of Cylindrical Core, cm</u>
1:1.70	28.02
1:3.24	32.55
1:4.77	36.08
1:6.30	39.03

A criticality calculation was performed for the 1:6.30 ratio, assuming a stainless steel core, a moderator temperature of 500F, a reflector savings of 7 cm, and a value for the age to thermal energy, τ , of 50 sq cm. The critical mass for the equilibrium operating condition was found to be 16.7 kg U²³⁵. Fuel depletion and transient xenon allowances must be added to this figure, but nevertheless it appears that in this type reactor fuel inventories can be kept low.

With large core sizes and low moderator temperatures large temperature coefficients of reactivity are not anticipated. Therefore, the main control of the reactor must come from control rods in the moderator space, although burnable poisons may be relied upon for control of fuel depletion to a certain extent. With the moderator and coolant independently pressurized, there is the possibility of control by adjustment of moderator pressure and void content. However, a much more detailed study of this must be undertaken before any concrete recommendation can be made.

¹² J. B. Anderson, "Water Cooled Submarine Advanced Reactor (SAR) Power Plant Design Study," ANL-5217, June 1, 1954.

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C. The Regeneration Factor η As a Function of Irradiation Time - J. C. Carter

Calculations have been completed, and the results of a theoretical investigation of η as a function of irradiation time in a nuclear reactor using a mixture of Th^{232} - U^{235} as a fuel have been published in ANL-5283.¹³ The report shows the abundance of the significant isotopes in the thorium-uranium cycle of a thermal reactor as a function of irradiation time at constant power. The change in η is given for combinations of enrichment and resonance escape probability considered likely to exist in a thermal reactor. The effect upon η of the absorption cross section of $_{91}\text{Pa}^{233}$ and of the fission products is shown.

II. HEAT TRANSFER

A. 600 psia Multi-Channel Boiling Density Tests - W. H. Cook, R. R. Rohde, C. K. Soppet

Natural circulation boiling density studies are being conducted at 600 psia to provide basic information for the Boiling Reactor Experimental Program. The present objectives are to determine the effects of power input, inlet water temperature, flow rate, and channel geometry upon the density in the channels.

The test section currently being used is a multiple channel configuration containing four stainless steel plates (1/16 in. thick by 4-1/4 in. wide by 60 in. long) housed within a stainless steel tube (Figure 17). The plates are connected in series and electrically heated as described in ANL-5260.¹⁴ The areas between the test section and the tube wall are filled with stagnant water at equilibrium with the system. Heat losses in this direction are negligible.

The test facility previously described in ANL-5260 has been modified to provide for an additional test section inlet thermocouple, a test section outlet thermocouple located in the riser, and an auxiliary pressurizing system which permits the maintenance of pressure and temperature conditions after power shutdown. The modified test facility is shown in Figure 18.

¹³ J. C. Carter, "The Regeneration Factor as a Function of Time in a Th^{232} - U^{235} Thermal Reactor," ANL-5231 September, 1954.

¹⁴ Reactor Engineering Division Quarterly Report, ANL-5260, April 15, 1954, p. 158.

FIG. 17
CROSS SECTION OF 1/4-INCH CHANNEL TEST SECTION

FIG. 18

600-psia NATURAL CIRCULATION
BOILING DENSITY TEST FACILITY

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System performance tests conducted with the 1/4-inch channel test section show that mass velocities of 3×10^5 lb/(hr)(sq ft) have been obtained with heat fluxes of 60,000 Btu/(hr)(sq ft) in the channels.

Density of the steam-water mixture in the channels is obtained by calibration of the attenuation of gamma rays passing through the test section. The density equipment, shown schematically in Figure 19, consists of a small thulium source, a photomultiplier tube pickup, a linear current amplifier and a recorder. The beam from the source is directed between channels of the test section producing a signal at the photomultiplier tube.

A series of tests have been made at 600 psia with the 1/4-inch channels at power densities of 20 and 30 kw/liter of channel volume (corresponding heat fluxes: 23,000 and 34,000 Btu/(hr)(sq ft), respectively). The greater portion of these preliminary runs were made to obtain reproducible data. A great deal of modification of equipment and test techniques were required before reasonable results were obtained. However, 12 runs have been made which gave satisfactory results. Data of these runs have been plotted to show the relationship of fraction steam voids to per cent of channel length for various inlet water temperatures (or inlet subcooling). Figure 20 shows these curves plotted for 20 kw/liter power density; Figure 21 shows similar data for 30 kw/liter power density. Data points do not show unreasonable spreads except at one or two isolated points. The spread in data shown is not due entirely to irreproducibility of data; it is also due to small discrepancies in inlet temperatures from which the average curves were drawn and due to inaccuracies in flow measurement. Comparing Figure 20 and Figure 21 there is, first, a change in the level of inlet voids due to local boiling (local boiling voids are a function of heat flux). Next there is a change in the slope of the curves at the point of inception of boiling (solid data points) producing three distinct curves for each inlet subcooling. The general shape of the curves at the channel outlet is quite different for the two power densities; in addition, there is a higher level of voids in the 30 kw/liter runs than in the 20 kw/liter runs. The difference in the shape of the curves is believed to be the effect of a change of mechanism of flow at the channel outlet of the 30 kw/liter runs. Further analysis of data is being made.

Flow rates through the channels varied as a function of the subcooling and power density. Figure 22 is a plot of flow rate versus inlet subcooling for the 20 kw/liter and 30 kw/liter power densities. The data at 30 kw/liter show good reproducibility; however, no duplicate data were available for the 20 kw/liter runs. The curves for both levels have been extrapolated to zero inlet subcooling, although there is no proof that the mechanism of flow will not change to alter this extrapolation.

A considerable amount of additional data will be required to refine the results reported here.

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FIG. 19
SCHEMATIC DIAGRAM OF DENSITY EQUIPMENT

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PRESSURE : 600 psia
POWER DENSITY : 20 kw/liter
CHANNEL L/D : 157.5

FIG. 20
STEAM VOID FRACTION VS
PER CENT CHANNEL LENGTH
AS A FUNCTION OF POWER
DENSITY AND INLET SUBCOOLING

PRESSURE : 600 psia
 POWER DENSITY : 30 kw/liter
 CHANNEL L/D : 157.5

FIG. 21
 STEAM VOID FRACTION VS
 PER CENT CHANNEL LENGTH
 AS A FUNCTION OF POWER
 DENSITY AND INLET
 SUBCOOLING

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FIG. 22
FLOW RATE VS INLET SUBCOOLING
AS A FUNCTION OF POWER DENSITY

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B. Pressurized Boiling Water Density Studies - H. H. Hooker,
P. A. Lottes, R. J. Schiltz

Additional data were taken on the supercritical loop on density of boiling water during flow upward through a vertical tube. The test conditions were as follows:

Pressure	- 2000 and 600 psia
Inlet velocity	- 5 fps
Heat flux	- 297,000 to 600,000 Btu/(hr)(sq ft)
Steam quality	- 0 to 30
Tube ID	- 0.22 in.
Tube Length	- 24 in.

Previous data¹⁵ indicated a range of slip ratios ($\frac{V_g}{V_f}$) of from 0.8 to 1.2.

These values were in part due to dissolved gas in the system which came out of solution during boiling. The data in Figures 23 and 24 were collected from the system after degassing down to a value less than 1.0 cc gas (at STP) per liter of water. The average value of slip ratio was 1.2 for 600 psia and 1.09 for 2000 psia.

C. Tubular Channel Burnout Tests - Small Scale Heat Through-put Loop - P. A. Lottes, R. J. Weatherhead

Burnout tests for round vertical tubes with net steam generation have been completed. All data taken were for upward flow of water through an electrically-heated tube. The apparatus has been previously described in ANL-4916.¹⁶

Each test was conducted in the following manner. The flow rate, total power, and system pressure were fixed at predetermined values. The heat exchanger was either partially or totally cut out of the flow circuit. Under these conditions, the inlet water temperature at the test section increased slowly with time and the weight flow rate of steam issuing from the test section also increased with time until burnout occurred. The value of inlet temperature at burnout was recorded. The value of exit quality at burnout was then calculated from a heat balance according to the equation

$$x = \frac{Q}{Wh_{fg}} \frac{C_p (T_{sat} - T_{in})}{h_{fg}} \quad (1)$$

¹⁵ Reactor Engineering Division Quarterly Report, ANL-5297, July 15, 1954

¹⁶ D. H. Weiss, "Pressure Drop in Two Phase Flow," ANL-4916, Oct. 20, 1954

FIG. 23
VARIATION OF EXIT STEAM VOIDS WITH EXIT QUALITY IN A
VERTICAL TUBE (0.22 in. ID x 24 in. LONG) AT 600 psia

FIG. 24
 VARIATION OF EXIT STEAM VOIDS WITH EXIT QUALITY IN A
 VERTICAL TUBE (0.22 in. ID x 24 in. LONG) AT 2000 psia

where

- x = exit quality, lb steam/lb moisture, %
 Q = total power input, Btu/hr
 W = mass flow rate, lb/hr
 C_p = specific heat at constant pressure, Btu/(lb)(F)
 T_{sat} = saturation temperature, F
 T_{in} = inlet water temperature, F
 h_{fg} = latent heat of vaporization, Btu/lb

The ranges of variables covered are:

- Pressure, psia: 600, 1,000, 2,000
 Heat Flux, Btu/(hr)(sq ft): 200,000 - 2,000,000
 Exit Steam Quality, lb/lb: 0 - 0.8
 Mass Flow Rate, lb/(hr)(sq ft): 250,000 - 6,000,000
 Tube Diameter, in.: 0.306 - 0.18

It may be seen from Equation (1) that the variables are not independent. Thus it was recognized that the data could not be correlated because the effects of heat flux, flow rate, and inlet subcooling could not be separated. Typical data are presented in Table X.

III. FUEL ELEMENTS

A. Fabrication

1. Stainless Steel Clad Zirconium-Uranium Wire-Type Fuel Elements - J. H. Monaweck

The qualities of wire-type fuel elements containing a core of 80% zirconium-20% uranium and clad with Type 347 stainless steel (0.005 $\pm$ 0.002 in.) were previously reported.¹⁷ The wire could not be formed into the proposed grid shape owing to the brittleness of the core alloy.

A new set of fuel wires was manufactured by Sylvania Electric Products with a core composition of 60% zirconium-40% uranium and clad with Type 347 stainless steel. This composition was to simulate a core composed of 60% zirconium-20% uranium-238 and 20% uranium-235. It was

¹⁷Reactor Engineering Division Quarterly Report, ANL-5208, December 15, 1953, p. 34.

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Table X (cont'd)

Exit Pressure, psia	Inlet Subcooling, F	Inlet Velocity, fps	Mass Velocity, 10 ⁶ lb/(hr)(sq ft)	Heat Flux 10 ⁶ Btu/(hr)(sq ft)	Exit Steam Quality, %
Tube Dimensions: OD = 0.180 in.; ID = 0.250 in.; L = 11-5/8 in.					
2000	0	24.6	3.45	0.745	12.1
2000	1	24.6	3.45	0.737	11.6
2000	23	24.0	3.56	1.00	7.4
2000	55	20.9	3.22	1.15	2.0
2000	56	20.8	3.22	1.16	2.0
Tube Dimensions: ID = 0.306 in.; OD = 0.377 in.; L = 23-1/4 in.					
600	0	2.54	0.455	0.278	25.4
	14	3.10	0.563	0.282	18.6
	24	3.10	0.569	0.280	16.7
	36	2.04	0.381	0.317	29.0
	36	2.98	0.555	0.282	15.4
	37	1.87	0.348	0.274	27.0
	71	3.26	0.625	0.894	48.2
	75	4.02	0.771	0.925	38.2
	101	2.58	0.503	0.915	61.5
	104	2.24	0.438	0.908	70.7
	115	2.20	0.432	0.901	70.5
	9	5.50	0.928	0.822	39.7
1000	36	4.46	0.781	0.824	42.3
	210	8.11	1.64	1.42	4.9
	236	6.03	1.24	1.44	14.8
	260	5.92	1.23	1.44	10.9
	356	5.49	1.19	1.74	10.4
	357	5.07	1.10	1.74	16.0
	361	5.11	1.11	1.74	14.9
Tube Dimensions: OD = 0.306 in.; OD = 0.377 in.; L = 23-1/4 in.					
2000	159	4.24	0.765	0.663	13.4
	197	3.23	0.605	0.663	19.7
	224	2.93	0.560	0.665	19.9

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found that by exercising great care the wires could be cold shaped into a proper grid. However, due to the extreme care necessary for bending, it is recommended that a thicker stainless steel clad be used which will enhance the bending quality of the wire as a unit. A section of a complete end loop of the hairpin is shown in Figure 25(a). A section of a broken loop with an 80% zirconium-20% uranium core is also shown (Figure 25(b)). It can be seen that there is no evidence of an alloy between core and cladding in either core, only mechanical contact. The photomicrographs also reveal better mechanical adhesion between core and clad when the core composition is 60% zirconium-40% uranium.

B. Corrosion

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

Long-term tests on unclad, high-uranium alloy samples¹⁸ have been continued. The samples that have survived in excess of 2000 hr in 500 F and 550F water are listed in Table XI. Excepting the 3.8% silicon alloy (only one sample was tested), the 1-1/2% niobium-5% zirconium alloy appears to be the best from the standpoint of number of samples surviving 2000 hr.

It is interesting to note that of the relatively few samples confined in a can with a known defect, only the 6% niobium sample has lasted over 2000 hr.

2. Water Corrosion of Zirconium- and Titanium-base Alloys - C. R. Breden

Samples of zirconium and titanium alloys, being investigated by the Argonne Metallurgy Division for possible application as fuel element cladding materials, have been subjected to static corrosion tests in degassed, demineralized water at high temperatures. These alloys were prepared by Armour Institute from Grade I crystal bar zirconium, commercial titanium, tantalum, and aluminum. The zirconium alloys were produced as button melts and forged at about 2000F. The titanium alloys were produced as 8-10 lb ingots, remelted by consummable arc, and forged at about 1800F.

Table XII gives the results on a sample of titanium-8% aluminum alloy tested at different temperatures. The corrosion resistance is remarkably good.

¹⁸Reactor Engineering Division Quarterly Report, ANL-5297,
July 15, 1954.

(b) 80% Zr - 20% U CORE

(a) 60% Zr - 40% U CORE

FIG. 25
PHOTOMICROGRAPHS OF TYPE 347 CLAD
ZIRCONIUM-URANIUM ALLOY WIRES. X41

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Table XICORROSION OF URANIUM ALLOYS IN HIGH-TEMPERATURE WATER

(Samples exposed in excess of 2000 hr)

Test No.	Sample No.	Alloy Composition, % Uranium plus			Heat Treatment	Water Cond.*	Rate of Wt. Change mg/(cm ²)(mo)		Time to Disintegration, hr	
		Nb	Si	Zr			500F	550F	500F	550F
27	4064c	3%			Quenched from 2012F	Deg	-246		2260	
40	4067	3%			Quenched from 2012F	O ₂	-189		4332	
28	4065f	6%			Quenched from 1470F	Deg	- 23		6884	
64	4065***	6%			Quenched from 2012F	Deg			6453**	
43	4071b	6%			As rolled (1562F)	O ₂	- 52		3957	
190	4065h	6%			Quenched from 1470F	O ₂		- 47		2262
81	4114a		3.8%		1562F - 7 days	Deg	55		4422**	
99	4127d	1-1/2%		5%	Quenched from 1470F	Deg	-107		2356	
138	4127l	1-1/2%		5%	Quenched from 1490F	Deg	- 93		4669**	
85	4118	1-1/2%		5%	Quenched from 1470F	Deg	-111		5677**	
173	4127m	1-1/2%		5%	Quenched from 1490F	Deg		-705		3589
189	4127q	1-1/2%		5%	Quenched from 1470F	Deg		-317		3374
98	4127c	1-1/2%		5%	Quenched from 1470F	O ₂		-270		3625
188	4127r	1-1/2%		5%	Quenched from 1470F	O ₂		-338		3332
99	4127d	1-1/2%		5%	Quenched from 1490F	Deg	-107		2356	
84	4117	2%		4%	Quenched from 1470F	Deg	-264		3364	

*Water Condition

Deg = Degassed (1-2 ppm oxygen).

O₂ = O₂ saturated at room temperature.

**= accumulated test time; test still in progress.

***Sample loosely fitted into stainless steel capsule perforated with 1/32 in. hole drilled inside.

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Table XII

CORROSION OF TITANIUM-8% ALUMINUM ALLOY IN DEGASSED,
DEMINERALIZED, STATIC WATER AT HIGH TEMPERATURES

(Sample A-4013)

Temp, F	Time, hr	Rate of Weight Change, (mg/(cm ²)(mo))	Appearance After Test
200	146	+1.22	No change.
500	230	-0.621	Lustrous purple-blue coating.
500	712	+0.124	Faint gray coating over lustrous purple-blue tarnish.
500	714	-0.020	No change.
500	1481	-0.060	Adherent dark blue-gray coating.
680	334	+0.263	Adherent dark blue-gray coating.
680	671	0.00	Adherent dark blue-gray coating.
680	1481	+0.047	Adherent smooth, dark blue-gray and iridescent green coating.
680	713	0.00	Adherent smooth, dark blue-gray and iridescent green coating.

Table XIII gives the results on samples tested for two days in 680F water. The more promising materials were tested for an additional two-week period. The samples in the latter group were separated by a stainless steel wire holder and exposed in the same autoclave.

Corrosion-wise, the results are good or promising for the high-titanium alloys, the zirconium-20% tantalum alloy, and the zirconium-tin alloys. The Argonne Metallurgy Division reports that all of these alloys, excepting the zirconium-20% tantalum alloy, have high-strength properties. However, the heat conductivity values of all of the alloys are too low to be attractive.

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Table XIII

CORROSION OF ZIRCONIUM-AND TITANIUM-BASE ALLOYS IN
DEGASSED, DEMINERALIZED, STATIC WATER AT 680F

Sample No.	Nominal Composition, %	Test Period, hr	Rate of Wt. Change mg/(cm ²)(mo)	Appearance After Test
4245-A	Zr-98;Al-2	1) 48	-2110	Heavy, loose, flaking white coating.
4246-B	Zr-97;Al-3	1) 48	+518	Heavy, adherent white coating, partly off on one end.
4247-C	Zr-96;Al-4	1) 48	-30	Heavy, loose, flaking white coating.
4248-D	Zr-95;Al-5	1) 48	+147	Heavy, loose, flaking white coating.
4249-E	Zr-95;Ti-5	1) 48	-657	Heavy, loose gray coating. Sample pitted.
4250-F	Zr-90;Ti-10	1) 48 2)329	+26.5 -22.5	Adherent black coating with gray spots. Heavy, loose, flaking gray coating.
4251-G	Zr-85;Ti-15	1) 48 2)329	+30 -3.09	Adherent lustrous black coating. White spots on each end. Heavy, loose, flaking, gray and black coating.
4252-H	Zr-75;Ti-25	1) 48 2)329	+22.1 +4.64	Adherent gray and black coating. Adherent black coating with gray spots.
4253-I	Zr-55;Ti-45	1) 48 2)329	+5.30 +2.70	Adherent blue-black coating. Adherent dull black coating.
4254-J	Zr-97;Sn-3	1) 48 2)329	0.00 +0.515	Adherent lustrous black coating. Adherent lustrous black coating.
4255-K	Zr-93;Sn-7	1) 48 2)329	-4.42 +0.515	Adherent lustrous black coating. Adherent lustrous black coating.
4256-L	Zr-90;Sn-10	1) 48 2)329	0.00 +1.03	Adherent lustrous black coating. Adherent mottled gray-black coating.
4257-M	Zr-95;Ta-5	1) 48	-2060	Heavy loose white coating.
4258-N	Zr-90;Ta-10	1) 48 2)329	+8.83 +6.44	Adherent lustrous black coating. Small white spot at previous crack on one end. Slightly loose white coating.
4259-O	Zr-85;Ta-15	1) 48	-1030	Heavy, loose, flaking gray coating.
4260-P	Zr-80;Ta-20	1) 48 2)329	0.00 +3.09	Adherent lustrous black coating. Adherent lustrous gray speckled coating.
4261-Q	Ti-92;Al-8	1) 48 2)329	0.00 0.00	Adherent blue, red, and straw tarnish. Adherent, faintly iridescent, dark gray coating.
4262-R	Ti-93;Al-7	1) 48 2)329	+1.36 0.00	Adherent blue and dark straw tarnish. Adherent, faintly iridescent, blue-black coating.
4263-S	Ti-90;Al-7,Mo-3	1) 48 2)329	-2.04 -0.298	Adherent blue and dark straw tarnish. Adherent dark blue tarnish.
4264-T	Ti-92-1/2;Al-7,Si-1/2	1) 48 2)329	-2.04 0.00	Adherent blue and dark straw tarnish. Adherent, faintly iridescent, blue-black coating.

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3. Post-Corrosion Test Examination of High-Uranium Alloys J. H. Monaweck

a. Heat Treated Uranium-Niobium and Uranium-Zirconium-Niobium Alloys

The following alloys were examined microscopically in an effort to interpret their erratic corrosion behavior in high-temperature water.

(1) Two samples of 94% uranium-6% niobium alloy, quenched from 2010F, with high and low weight changes, respectively.

(2) Samples of 94% uranium-4% zirconium-2% niobium alloy, quenched from 1472F, with weight loss rates ranging between 130 and 6000 mg/(cm²)(mo).

(3) Two samples of 93-1/2% uranium-5% zirconium-1-1/2% niobium alloy, quenched from 1472F, with weight loss rates of 125 and 1140 mg/(cm²)(mo), respectively.

(4) Zircaloy-2 clad 93-1/2% uranium-5% zirconium-1-1/2% niobium alloy fuel plates.

The following observations were made:

(1) Quenching the 94% uranium-6% niobium alloy from 2010F caused quenching cracks to cross grains in the alloy (Figure 26(a)). High measured weight loss rates, averaging 700 mg/(cm²)(mo), were obtained for these samples.

(2) One sample (Figure 26(b)) of 94% uranium-4% zirconium-2% niobium alloy, quenched from 1472F, also showed cracks and a high rate of weight loss (6000 mg/(cm²)(mo)).

(3) The sample of 93-1/2% uranium-5% zirconium-1-1/2% niobium alloy, having a high rate of weight loss (1140 mg/(cm²)(mo)), showed quenching cracks across some of the grains (Figure 26(d)). The second sample, with a much lower weight loss (125 mg/cm²)(mo), had fewer cracks (Figure 26(c)). Both samples were oil quenched from 1472F.

(4) In some cases, corrosion of the 94% uranium-6% niobium alloy (Figures 27(a) and 27(b)) has followed along the grain boundaries. Both alloys were quenched from 2210F. The crack shown in Figure 27(b), in general, traveled along the grain boundaries, but the center line of the crack occurred at the crystal boundaries. It is unknown whether the crack existed before corrosion occurred or whether the expanded corrosion products forced a crack between the grains and crystals.

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(a) 94% U - 6% Nb
QUENCHED FROM 2010 F
TESTED 327 hr IN 600 F WATER
WT. LOSS: 700 mg/(cm²)(mo)

(b) 94% U - 4% Zr - 2% Nb
QUENCHED FROM 1472 F
TESTED 1008 hr IN 500 F WATER
WT. LOSS: 6000 mg/(cm²)(mo)

(c) 93 $\frac{1}{2}$ % U - 5% Zr - 1 $\frac{1}{2}$ % Nb
QUENCHED FROM 1472 F
TESTED 2184 hr IN 500 F WATER
WT. LOSS: 125 mg/(cm²)(mo)

(d) 93 $\frac{1}{2}$ % U - 5% Zr - 1 $\frac{1}{2}$ % Nb
QUENCHED FROM 1472 F
TESTED 1512 hr IN 500 F WATER
WT. LOSS: 1140 mg/(cm²)(mo)

FIG. 26
PHOTOMICROGRAPHS SHOWING QUENCHING
CRACKS IN HIGH-URANIUM ALLOYS. X 400

(a) 94% U - 6% Nb
CANNED WITH EXPOSED ENDS
TESTED 375 hr AT 680 F
WT. GAIN: 280 mg/(cm²)(mo)

(b) 94% U - 6% Nb
QUENCHED FROM 2010 F
TESTED 327 hr IN 600 F WATER
(SAMPLE NOT ETCHED)

EXAMPLES OF CRACKING MECHANISM

(c) 94% U - 6% Nb
CANNED WITH EXPOSED ENDS
QUENCHED FROM 2010 F
TESTED 1074 hr IN 600 F WATER
WT. GAIN: 15 mg/(cm²)(mo)
(SAMPLE NOT ETCHED)

(d) ZIRCALOY 2-CLAD
93 $\frac{1}{2}$ % U - 5% Zr - 1 $\frac{1}{2}$ % Nb
FUEL PLATE

CORROSION ALONG CRYSTAL BOUNDARIES

CORROSION IN DELIBERATE DEFECT

FIG. 27
PHOTOMICROGRAPHS SHOWING TYPICAL
CORROSION IN HIGH-URANIUM ALLOYS. 400X

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(5) One sample of 94% uranium-6% niobium alloy, quenched from 2010F, revealed that corrosion followed the crystal boundaries (Figure 27(c)). This sample, examined after the crystal structure was brought out by standing in laboratory atmosphere overnight, showed grain boundaries similar to other metals which are susceptible to microshrinkage. The sample shown in Figure 26(b) revealed the same type of crystal structure before etching. Again, this behavior is similar to that observed in metals showing microshrinkage.

(6) The mechanism of corrosion seems to be the same for 1-1/2% niobium-5% zirconium, 2% niobium-5% zirconium, 2% niobium-4% zirconium, and 6% niobium alloys of uranium.

(7) Examination of samples, corrosion tested at 500F, 600F and 680F, revealed no micrographic evidence of aging. In all cases, only the corroded samples were examined.

It must be said that the above alloys were some of the first prepared. Observations made on these samples indicate that great care must be exercised to make homogeneous melts and that the same care should be taken in the heat treating to prevent cracking.

Microexamination of Zircaloy-2 clad fuel elements with cores of 93-1/2% uranium-5% zirconium-1-1/2% niobium alloy, tested in water at elevated temperatures for long periods of time, led to the following observations:

(1) Surface blisters were caused by the formation of corrosion products under that particular area, subsequent to moisture traveling to that point. Moisture, once it has presumably entered through the end closure, migrated to the affected area through (a) a channel caused by the picture frame not bonding to the edge of the core; (b) areas wherein the top and bottom plates were not bonded to the core; (c) cracks (presumably quench cracks) in the alloy layer or diffusion bond between the core and cladding material. Some cracks were observed to occur between the diffusion layer and cladding; others were between the diffusion layer and the core.

(2) Corroded areas and adjacent areas of the core did not reveal differences in crystal structure sufficient to warrant a conclusion that one certain type of alloy structure corroded more easily than another.

(3) Deliberate holes drilled through the cladding and diffusion layer into the core did not cause any corrosion or blisters to form at or near the exposed bond interface (Figure 27(d)). The diffusion layer, the top plate, and the core are all integral at the hole. There was no chance for corrosion products to form in a split or cracked area and thus allow the corrosion products, by expansion, to pry the cladding from the core.

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b. Epsilonized Uranium-Silicon Alloy

One sample of cast uranium-silicon alloy containing 3.8% by weight silicon, and converted into an epsilon alloy by heating for one week at 1562F, has withstood corrosion testing for six months in 500F water. The average weight change was 60 mg/(cm²)(mo). The microstructure of this alloy is shown in Figure 28. Other samples, with similar microstructures, except that the skeleton dendritic cast structure was missing, have shown destructive corrosion rates.

4. Water Corrosion of Nickel-Plated 2S Aluminum Plates -
C. R. Breden

As part of a program to improve the corrosion resistance of aluminum to high-temperature water, corrosion tests were made on 2S aluminum plates nickel plated by the Kanigen process. The samples, prepared by the Argonne Metallurgy Division, represented two thicknesses of nickel plate: one about 0.0003 in. and the other 0.0001-0.0002 in.

For the first test at 525F the plates were supported by a Teflon-lined holder to eliminate galvanic effects. In the second test at 600F a stainless steel holder was used with no attempt to eliminate galvanic effects between the coated aluminum and the stainless steel. Distilled, degassed water was used with a small portion being continuously bypassed through a mixed-bed demineralizer. The velocity past the samples was about 18 fps.

The weight change rate results are given in Table XIV. Although the results do not show any significant difference between corrosion rates at 525F and 600F, there is a significant difference between the rates in the first period as compared with the rates in subsequent periods at both temperatures. There is also a significant difference between the two thicknesses of coating as evidenced by the appearance of the samples after test; the lighter coat apparently is more porous and usually shows more spots of white oxide. It will be noted that the weight changes during the first period at 525F are all positive with the light coat and all negative with the heavy coat.

Sample 7L failed after 138 hr at 600F (Figure 29). Failure is believed to have been due to a large area of nickel coating which was removed along with the adhesive tape used to bind a group of samples previous to testing.

The exceptionally high rates shown by sample 2H at 525F appear to be associated with its failure when tested at 600F. This is confirmed by the abundance of white oxide spots observed after 431 hr at 525F (Figure 30). Figure 31 shows this sample after 301 hr at 600F.

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FIG. 28
PHOTOMICROGRAPH OF EPSILONIZED URANIUM -
3.8% SILICON ALLOY EXPOSED FOR SIX
MONTHS IN 500 F WATER. X400

ASSEMBLED HOLDER

PLATE 7L REMOVED FROM HOLDER

NICKEL PLATE THICKNESS: 0.0001 - 0.0002 in.

FIG. 29
NICKEL PLATED 2S ALUMINUM PLATES
EXPOSED FOR 138 hr IN 600 F WATER

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1H

2H

3H

4H

5H

NICKEL PLATE THICKNESS: ~ 0.0003 in.

FIG. 30
NICKEL PLATED 2S ALUMINUM PLATES EXPOSED
FOR 431 hr IN 525 F WATER

93

6H

2H

7H

8H

9H

10H

431 hr AT 525 F
301 hr AT 600 F

NICKEL PLATE THICKNESS: ~ 0.0003 in.

FIG. 31
NICKEL PLATED 2S ALUMINUM PLATES EXPOSED
FOR 439 hr IN 600 F WATER

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Omitting the two samples that failed, the corrosion resistance has been much improved by the presence of the nickel film. Dynamic corrosion tests, run in 1952 in 500F water on uncoated 2S aluminum plates, showed weight changes averaging about $+40 \text{ mg}/(\text{cm}^2)(\text{mo})$ with so much attack that the test was discontinued after 316 hr.

Table XIV

CORROSION OF NICKEL-PLATED 2S ALUMINUM IN HIGH-TEMPERATURE WATER

Weight Change Rate, $\text{mg}/(\text{cm}^2)(\text{mo})$						
525F			600F			
Sample No. (3)	1st Period 95 hr	2nd Period 336 hr	Sample No.	1st Period 138 hr	2nd Period 168 hr	3rd Period 133 hr
1L	+1.37	-0.061	6L	+4.50	-0.959	0.00
2L	+5.56	+0.367	7L	Failed(1)	-5.77(2)	+53.6
3L	+6.54	+0.313	8L	+3.38	-0.224	+ 0.204
4L	+7.5	+0.856	9L	+4.10	-0.102	+ 0.11
5L	+4.62	-0.031	10L	+8.05	+0.347	+ 0.516
1H	-1.01	-0.184	6H	-0.423	-0.776	- 0.329
2H	-6.72	+0.876	7H	-0.895	-0.653	- 0.266
3H	-1.01	-0.224	8H	+1.59	-0.224	+ 0.047
4H	-1.48	-0.234	9H	-0.895	-1.16	- 0.532
5H	-4.31	-0.581	10H	+2.06	-1.33	0.00

(1) Before testing this sample had a large spot where the film had been pulled off by adhesive tape used to hold the bundle of samples together prior to testing.

(2) After 138 hr sample 7L failed and was replaced by 2H which had been tested at 525F.

(3) Plate thickness is light (L) or heavy (H).

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5. Corrosion-Irradiation Tests on High-Uranium Alloys for Water Reactors

The corrosion-irradiation program is continuing with the test of Zircaloy-2 clad fuel plates in the Argonne Water Loop at MTR. The plates have cores of uranium alloyed with 3 and 6% niobium as well as 1-1/2 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 of the plates. Following irradiation at MTR the plates will be returned to Argonne for the drilling of deliberate defects to expose the core alloy prior to post-irradiation corrosion test. Similar plates are being corrosion tested at Argonne for long intervals, with and without deliberate (drilled hole) defects exposing the core, to aid in the selection of the deliberate defects to be put in the plates currently under irradiation at MTR. In addition, a new series of tests has been started in an autoclave-type facility, inserted in the CP-5 reactor, where pins of various high uranium alloys are irradiated in water at temperatures about 500F.

a. Irradiation of High-Uranium Alloy Plates at MTR -
C. R. Breden

Insertion of Cartridges MTR-W-2 and MTR-W-2E, containing Zircaloy-2 clad plates of uranium alloyed with 3% niobium, 6% niobium, and 1-1/2% niobium-5% zirconium, respectively, was described in ANL-5260.¹⁹

According to heat balance measurements, the in-pile cartridge generated 26 kw of heat, including both fission and gamma heat. On June 14 water was drained out of the in-pile tube and away from the cartridge in order to replace a blown gasket. On July 1 an increasing leak rate in the system, combined with failure of the emergency power system, resulted in a decision to remove the cartridge. Due to a series of difficulties with the emergency power supply, the cartridge was not reinserted until September 28.

A few days after reinsertion of the cartridge, the flow in the system began to decrease until it had dropped from about 90 gpm to 50 gpm. This decrease is believed to be due to an increased resistance through the cartridge. Since the reason for increase resistance was unknown, the decision was made to remove the cartridge for observation as soon as a cave became available.

During the September 6 shutdown period the in-pile tube was replaced by a new one fabricated at Argonne. The reason for the replacement was that the old one had received a higher fast nvt (estimated to be about 1.5×10^{21} at the time it was removed) than had been received by

¹⁹ Reactor Engineering Division Quarterly Report, ANL-5260, April 15, 1954, p. 195.

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any known sample of Type 316 stainless steel on which correlated physical property data were available. The old tube tip will be cut off about 2 in. back of the weld and returned to Argonne for examination.

In order to provide some degree of monitoring on the effect of irradiation on the new tube tip, each new cartridge will be adapted to carry on the innermost end one of a pair of samples from the same billet of Type 316 stainless steel as the tube tip was made. As each cartridge comes out of irradiation, the monitoring sample will be removed, its hardness measured, and then it will be placed on the next cartridge going in. While one sample is being irradiated, the other will be returned to Argonne for test purposes.

b. Long-Term Corrosion Tests of Zircaloy-2 Clad High-Uranium Alloy Core Plates - C. R. Breden

Without Known Defects: Four binary alloy plates without known defects are being tested at 550F and have accumulated 2400 to 4200 hr (300 to 400 hr at 500F and the remainder at 550F) without indications of failure. Six other binary alloy plates without defects are being tested at 500F and have accumulated 2400 to 3700 hr (all but about 300 hr in dynamic tests) also without indications of failure.

Deliberate (Drilled Hole) Defect Tests: A total of ten plates (representing the three core compositions) were originally selected for this testing as described in ANL-5297.²⁰ Five plates were satisfactorily intact for further testing; the results as shown in Table XV. (In addition, plates 4228 and 4234 were added to the program.) It is significant to note that binary alloy plates with holes drilled into the bonded sides have been exposed up to 2-1/2 months without serious failure.

c. Irradiation-Corrosion Tests of the High-Uranium Alloy Pins - G. A. Freund, R. H. Leyse, D. R. Patterson, R. J. Schiltz

An experiment facility has been designed and operated for in-reactor testing of the high-uranium fuel alloys in boiling water at 500F. The facility which is located in an air-cooled thimble in CP-5 is shown schematically in Figure 32. The test sample (a 1/8-in. dia. pin) is supported in a stainless holder in a finned autoclave connected to an external surge tank by a 3/8 in. OD tube having an inner fill tube (3/16 in. OD).

The autoclave is designed on the basis of local boiling at the surface of the pin sample. Ultimate heat removal is by the CP-5 cooling air from the surface of the autoclave. Temperature gradients due to local

²⁰ Reactor Engineering Division Quarterly Report, ANL-5297, July 15, 1954.

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Table XV

SUMMARY OF LONG-TERM CORROSION TEST ON ZIRCALOY-2 CLAD
HIGH-URANIUM ALLOY CORE PLATES

(With deliberate defects (drilled hole) in clad)

Plate No.	Core Composition, % Uranium plus %		Test time, hr		(1) Location of drilled hole	Appearance
	Nb	Zr	Accum.	With Core Exposed		
4130	3		S-1396 D- 724 2120	S-811 811	edge	No change in appearance in 501 hr. Rupture next to hole after 811 hr. Test discontinued.
4150	6		S-2235 D- 838 3073	S-1785 1785	side	No change in appearance.
4151	6		S-1493 D- 838 2331	S-1043 1043	edge	No change in appearance in 501 hr. Slight bulging in places along bond line after 1043 hr. Test discontinued
4178	3		S-2052 D- 892 2944	S-1785 1785 1785	side	Bulging at hole after 811 hr. Cracks after 1785 hr. Test discontinued.
4228	1.5	5	S-1772 D- 144 1916	S-336 336	side	No change in appearance.
4234	1.5	5	S-1171 D- 144 1315	S-336 336	side	Large bulge over hole after 336 hr. Test discontinued.
4236	1.5	5	S- 670 D- 144 814	S-501 501	side	Bulge at hole after 334 hr. Small rupture at bond line after 501 hr. Test discontinued.

(1) Location of drilled hole:

side - into side of plate where a good metallurgical bond exists between clad and core.

edge - into edgeline interface from side.

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FIG. 32
CP-5 AUTOCLAVE TEST FACILITY

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boiling are relatively independent of heat flux over wide ranges of heat flux; therefore temperature of the sample can be accurately controlled by regulation of external helium pressure to the surge tank and hence temperature of the boiling process.

The water level extends half-way into the surge tank, but during normal operation the water in the surge tank and connecting tube remains relatively cold. The liquid level in the surge tank is measured by two heated thermocouple probes, which give different readings due to the higher conductivity of water as compared to gas.

Pressure is maintained at 700 psi and the temperature at about 500F. Excessive temperature and high or low pressure result in reactor scrams. A rupture disc is provided for backup on high pressure (possibly due to water dissociation in the presence of a disintegrated high-uranium sample).

One run of the autoclave facility has been completed and a second one is in progress. In the first run, an unclad 93-1/2% uranium-5% zirconium-1-1/2% niobium sample was irradiated at 500F for the equivalent of 110 hr at full CP-5 power (thermal flux about 10^{13} nv). With 10% enrichment it is estimated that a burnup of 0.015% was attained. After removal, the sample was visually examined and found to be bent and the surface roughened by corrosion (Figure 33).

The sample was surveyed dimensionally with the following results.

	Length, in.	Diameter, in.	Weight, gm
Pre-Irradiation	0.983	0.125	3.505
Post-Irradiation	1.044 ± 0.002	0.128 ± 0.001	

Before irradiation the sample had been placed in a stainless steel holder by means of a press fit. After irradiation, when an attempt was made to machine the sample away from the holder, the sample broke away from the holder leaving a thick collar of stainless steel on the specimen. Attempts to remove this collar proved futile and consequently an accurate determination of the post-irradiation weight could not be made.

The sample before it was machined to size for the irradiation test had the following history:

- (1) Cast and gamma quenched from 1000C (1832F) into Wood's metal.

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FRONT

BACK

FIG. 33

IRRADIATION-CORROSION TEST SPECIMEN OF UNCLAD $93\frac{1}{2}\%$ U -
5% Zr - $1\frac{1}{2}\%$ Nb ALLOY IRRADIATED TO ESTIMATED BURNUP
OF 0.015% OF TOTAL CORE ATOMS IN 500 F BOILING
WATER IN CP-5 AUTOCLAVE TEST FACILITY

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- (2) Corrosion tested for three days at 570F with a rate of 34 mg/(cm²)(day).
 - (3) Aged at 350C (660F) for 24 hr.
 - (4) Corrosion tested for another three days at 570F with a rate of 21 mg/(cm²)(day).

The facility represents a successful first attempt to carry out boiling corrosion irradiation; a more advanced design on a larger scale and with simplified removal procedures is being prepared.

C. Physical and Mechanical Properties

1. Thermal Cycling of Co-extruded Zirconium Clad, 98% Uranium-2% Zirconium Fuel Rods - J. E. Kemme, J. H. Monaweck

The thermal cycling of co-extruded fuel rods (98% uranium-2% zirconium core clad with 0.015 in. zirconium), approximately 3/8 in. in diameter, was previously described.²¹

The rods were extruded and swaged with a 12% reduction of area and air cooled. The rods were microexamined prior to quenching; all rods showed large, rounded grain structure typical of slowly cooled uranium. The samples were then thermal cycled in NaK from 150C to 625C and 725C. The samples cycled 250 times from 150C to 625C decreased in length (0.8% average) and showed slight marks. The samples cycled 150 times from 150C to 725C showed an average growth of 5.9%; the samples were rough and badly bowed.

The effect of heat treatment on the thermal cycling properties of the extruded fuel rod was studied. One additional extruded fuel rod, received from the Division of Industrial Cooperation of MIT, was cut into samples 4-1/2 in. in length. These samples were cut from a single rod and were not end capped. Five of these pieces were heated to 725C in liquid salt and quenched in water; cross sections from this rod showed perfect bond between the core and cladding material. After quenching from 725C, the core was transformed completely into acicular structure. Two pieces were then thermal cycled between 100C and 258C for 500 cycles. Two other pieces were cycled 500 times between 150C and 625C. The data obtained are shown in Table XVI.

Four other pieces of the extruded rod were heated to 825C and quenched in water. Micrographs showed the quenched structure to be acicular. Two of these samples were thermal cycled from 100C to 358C

²¹ Reactor Engineering Division Quarterly Report, ANL-5260, April 15, 1954, p. 81.

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Table XVI

EFFECT OF THERMAL CYCLING OF EXTRUDED, SWAGED, HEAT TREATED
98% URANIUM-2% ZIRCONIUM ALLOY CLAD WITH ZIRCONIUM

<u>Sample No.</u>	<u>Cycling Temp., C</u>	<u>Length Before Cycling, in.</u>	<u>Length After Cycling, in.</u>	<u>% Growth, Length</u>	<u>Diam. Before Cycling, in.</u>	<u>Diam. After Cycling, in.</u>	<u>% Growth, Diam.</u>	<u>Remarks</u>
<u>Heated to 725C - Quenched in water</u>								
1.	150-358C	4.125	4.125	0	0.365	0.365	0	Smooth and straight.
2.	150-358C	4.125	4.125	0	0.365	0.365	0	Smooth and straight.
3.	150-625C	4.5	3.94	-12.4	3.65	3.93	+7.7	Rough, badly marked.
4.	150-625C	4.5	3.94	-12.4	3.65	3.91	+7.1	Rough, badly marked.
<u>Heated to 825C - Quenched in water</u>								
5.	150-358C	4.5	4.5	0	0.365	0.365	0	Smooth and straight.
6.	150-358C	4.5	4.5	0	0.365	0.365	0	Smooth and straight.
7.	150-625C	4.125	3.75	-9.1	0.365	0.386	+5.75	Rough curved to half moon.
8.	150-625C	4.125	3.81	-7.6	0.365	0.386	+5.75	Rough curved to half moon.

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and two others were cycled from 150C to 625C. The data obtained from these samples are also shown in Table XVI.

Examination of Table XVI and comparison with Table XII, ANL-5297, led to the following observations:

a. Samples quenched in water from 725C and 825C followed by thermal cycling from 100 to 358C showed no dimensional change after 500 cycles.

b. Samples quenched in water from 725C and 825C followed by thermal cycling from 150 to 625C showed marked shrinkage in length and a large increase in diameter. The samples quenched from 825C showed less shrinkage in length with less change in diameter.

c. Extruded, air-cooled samples showed a decrease in length of 0.8% and no increase in diameter when cycled from 150C to 625C for 150 cycles, while quenched samples decreased an average of 10.8% in length, with corresponding increase in diameter when thermal cycled from 150 to 625C for 500 cycles.

d. Quenching extruded swaged fuel rods from 825C and 725C before thermal cycling to 625C is detrimental.

2. Thermal Cycling of Experimental Pin-Type Fuel Elements - E. L. Currier

A number of experimental pin-type fuel elements have been prepared and thermal cycled in NaK. All samples contained 14.3 in. long by 0.164 in. diameter alpha-rolled uranium clad in 0.172 in. ID x 0.0008 in. wall AISI type 304 stainless steel tubing of various lengths. Free expansion lengths of 1/8 in., 1/2 in., and 2-3/4 in. were provided for uranium length changes before the tube would be subjected to longitudinal tensile stress by the expanding uranium. Annular openings in the welded end plugs of the closed samples permitted circulation of NaK directly onto the uranium specimens. The temperature cycle was from 300F to both 1180F and 1330F for two sets of samples. The cycle time was 90 sec at each temperature with 15 sec transition time. Figures 34 and 35 show:

1. The original unclad uranium pin.
2. The clad pin with 2-3/4 in. original free space, after cycling.
3. The clad pin with 1/2 in. original free space, after cycling.
4. The clad pin with 1/8 in. original free space, after cycling.
5. The unclad uranium specimen after cycling.

In the case of the high alpha cycled samples (up to 1180F), the unclad uranium length gain was 9%. Sample 2 cladding did not change length although the uranium grew 9%. Sample 3 cladding lengthened 2.5%, while the restrained uranium grew 7%. Sample 4 cladding lengthened 4% while the restrained uranium grew 5%.

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FIG. 34
TYPE 304 CLAD AND UNCLAD ALPHA-ROLLED
URANIUM ROD THERMAL CYCLED 200 TIMES
IN NaK BETWEEN 300 F AND 1180 F

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FIG. 35
TYPE 304 CLAD AND UNCLAD ALPHA-ROLLED
URANIUM ROD THERMAL CYCLED 200 TIMES
IN NaK BETWEEN 300 F AND 1330 F

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Figure 36 shows typical plots of data from four of the 17 runs made.* The minimum difference between adjacent couples on either side of the water-steam interface in the thermocouple tube was 18F, and in some runs the difference was as high as 121F. The plots amply demonstrate that the difference between couples on the liquid and vapor sides is sufficient for detection on even the crudest sensing devices.

In order to estimate the accuracy of the measurement, the readings from the differential pressure indicator were corrected for the difference in density between the water in the cold standpipe and in the autoclave. The values so obtained were considered to be the true level, and were plotted along with uncorrected readings from the differential pressure indicator, the sight glass, and the thermocouple tube in Figure 37.*

With the autoclave approximately two-thirds full of water, all readings were in close agreement for autoclave temperatures up to 400F. At autoclave temperatures of 445 to 553F, sight glass readings were somewhat lower than true level (since the water in the liquid leg of the sight glass was somewhat cooler and therefore denser than the water in the autoclave), and the readings from the differential pressure cell (uncorrected) and from the thermocouple tube were considerably below the true level. Again the discrepancy was caused by density differences, but in the case of the differential pressure cell and the thermocouple tube the cold liquid legs were longer and the temperature differences greater than for the sight glass. In runs 16 and 17, however, where the level was quite low, the differences diminished because of the shorter and higher temperature cold legs.

In several runs, the behavior of the thermocouple readings with time was studied. Data from a run considered to be typical is plotted in Figure 38. In this run, water was drained approximately 3 in. in the autoclave, and a set of readings was obtained 15 sec after completion of draining and at 3- and 4-minute intervals thereafter. It can be seen from the figure that couple No. 7, which was below the liquid level before draining, had already reached a new equilibrium point at the 15-second reading. Couples further down the tube reached equilibrium more slowly, but the change in the indicating couple, the one nearest the level, was virtually instantaneous.

2. Cold -Water Pressure Drop Tests on Reference Pin-Type Fuel for EBR-II - E. L. Currier

Cold-water pressure drop measurements have been made on the reference fuel element subassembly design for EBR-II reactor.

Cold water was chosen instead of sodium as the hydraulic medium for these tests because of the relative ease and speed of fabrication of the test equipment. Furthermore, pressure drop data obtained could not

*Liquid level heights as indicated on Figures 36 and 37 are measured from the thermocouple tube bottom connection.

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FIG. 36
TYPICAL THERMOCOUPLE READINGS AT VARIOUS
LIQUID LEVELS AND AUTOCLAVE TEMPERATURES

LEGEND

- TRUE LEVEL (CALC. FROM DP CELL)
 - △ SIGHT GLASS READING
 - DP CELL READING
 - LEVEL INDICATED BY THERMOCOUPLES
- } UNCORRECTED

FIG. 37
COMPARISON OF LIQUID LEVEL READINGS

FIG. 38
TYPICAL TIME RESPONSE OF THERMOCOUPLES
UPON RAPID LOWERING OF WATER LEVEL

be projected into a sodium coolant system analysis. In addition, variations in the design of the elements and their holders could be inserted and tested in the experimental section quite simply.

The test apparatus consisted of a loop of 4-in. pipe with water flow powered by a centrifugal pump. The 18-in. long, hexagonal fuel element tube was flanged into an upright section of the loop with 4-ft long entrance and exit sections of pipe. A description of the experimental fuel elements and their method of manufacture has been reported previously.²²

The test section consisted of an hexagonal cluster of 169 ribbed elements resting on a perforated bottom plate of 169 3/16-in. diameter offset holes and restrained above with screening of wire 0.045 in. diameter on 3/16-in. centers. The data were obtained from pressure taps located both 4 in. below and above the flanged faces of the hexagonal tube, as well as an additional pressure tap located 45 in. above the top of the tube. Two additional pressure taps were located 2 in. from each face inside the tube. All data reported relate to the reference flow velocity of 565 gpm or 35 fps.

The over-all pressure drop was 65 psi outside the test section and 35 psi in the 14-in. length centered inside the test section. A test of the hexagonal tube with the top and bottom holders but without the ribbed elements showed an over-all pressure drop of 10 psi. A test of the hexagonal tube with neither holders nor ribbed elements showed a pressure loss of less than one psi.

These tests are continuing with various holders, rib pitches, etc., designed to decrease the pressure drop through the fuel element sub-assembly.

3. High-Temperature d-c Electromagnetic Pump Current Conductor Connection - A. H. Barnes, F. A. Smith

In building some ten small d-c pumps for various liquid metal experimental programs, improvement has been made in the technique of making pumps. In order to eliminate the possibility of remelt of the silver-soldered conductor connections at high temperature, "fabricated-by-welding" bond between the pump wall and the pump bus bar has been made. The welding of the bus assembly is also desirable for large d-c pumps where one would question the reliability of a silver-soldered or microbrazed joint covering a very large area.

²² Reactor Engineering Division Quarterly Report, ANL-5297,
July 15, 1954.

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One fabricated pump (1/4 in. gap x 2-1/8 in. wide), pumping 10 gpm, has operated on a loop at 600C for 150 hr without failure. Another similar pump has operated at 800 C for 10 hr (Figure 39). This pump is currently in continuous 24 hr per day operation at 600C and has completed 150 hr of operation.

The joint between the pump tube and the current conductor is shown in Figure 40. The unique feature of the joint is that NaK is the fluid conductor completing the electrical circuit from the copper bus bar to the pump tube. The procedure in filling the assembly with NaK was as follows: The annulus between the copper and the container around the copper is evacuated. NaK under vacuum is then flowed into the annulus. The NaK level in the expansion tank is adjusted by removing excess NaK out of the expansion chamber with a hypodermic syringe. The expansion tank is then welded tight.

Resistance measurements on the completed joint showed the "canned" NaK bonded joint to be equal to an equivalent length of solid copper. No voids, high resistance, or lack of wetting have been encountered. The weld between the copper and the stainless steel has been subjected to the worst possible shock conditions by withdrawing the entire assembly from an electric oven at 800C and quenching in a container of cold water. There was no weld failure.

4. Conventional Packing for Sodium Valves - D. V. Hammond,
F. A. Smith

The need for 10-inch or larger valves for dynamic Na systems has resulted in a renewed interest in conventionally packed valves, because of the long bellows required in packless valves. In 1947 and 1948 various valve packings were tested using NaK as the fluid at 30 psi internal pressure. It was noted that at 200C all packing materials leaked if there was sufficient clearance between the packing and the stem to allow the stem to move.

Recently, a 1/2-inch valve with conventional packing has operated 107 days at 165C packing temperature with Na at 75 psi. The valve used was a Powell gate valve (18-8 SS, 150-lb body, screwed ends, 0.58Y.) with a graphite-asbestos packing. The only change made to this valve was to replace a paper gasket with a copper gasket between the body and the bonnet. The test apparatus included a small expansion tank, screwed pipe fittings, pressure gage, heating cable and a temperature controller (Fig. 41).

The temperature-controlling thermocouple located on the packing gland regulates the amount of heat to the Na system to maintain the desired packing gland temperature. It should be pointed out that the Na is not much hotter than the packing gland. The test showed only the temperature-pressure characteristic of the packing gland.

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6 110

FIG. 40
SCHEMATIC OF ALL-WELDED d-c ELECTROMAGNETIC
"PUMP TUBE-CONDUCTOR" JOINT

115

FIG. 41
VALVE PACKING TEST APPARATUS
($\frac{1}{2}$ - inch GATE VALVE)

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On a large valve the physical distance between the body and the packing becomes great enough to permit maintaining the proper gland temperature, regardless of the Na system temperature, by not insulating the bonnet of the valve. Data are available on high-temperature steam valves, with uninsulated bonnets, which predict that it will be possible to have a 500C Na temperature in the body of a large valve and still maintain a packing gland temperature of less than 200C without any external cooling apparatus other than ambient air.

The valve in this test was deliberately loaded with high oxide content, dry mold Na in order to enhance the effect of the Na_2O in causing sticking of the gate in its seat or a "freezing" of the stem in the packing. The valve was cycled by hand on the average of once a day. No sticking or freezing has been observed.

In the first days of operation, 75 psi was set as the design pressure. At 200C on the packing, Na was extruded past the packing. The valve was cleaned, re-loaded with Na, and the temperature on the packing was reduced to 165C; the valve has operated successfully for 107 days.

Figure 42 shows the indicated pressure-temperature relationship for successfully operating a valve with conventional packing in a Na system.

5. EBR-II Rod Gripper Test Facility - E. Hutter

The rod gripper head for the EBR-II Working Model unloading mechanism has been tested under simulated operating conditions. (The unloading mechanism is described in Part C, Section VI.)

Figure 43 shows the gripper, which consists of two jaws which fit over the knob-like connector end of the fuel or blanket element. The blade between the gripper jaws fits into a slot in the connector end of the element to provide for angular positioning of the subassembly. The jaws are opened or closed by sliding a sleeve past their fulcrum, and the required motion is obtained by a cam action.

The gripper and a short section of a dummy fuel rod is placed into a sealed sodium-filled tank. The gripper is connected, by rods, to two plates at the upper end of two bellows. The vertical movement of the lower plate lifts or lowers the gripper while a movement of the handle fastened to the upper plate opens or closes the jaws. This arrangement (Fig. 44) allows the rod to be "dropped" about 1-1/2 in. and then to be picked up again.

FIG. 42
TEMPERATURE - PRESSURE
CRITERIA FOR SUCCESSFUL
SODIUM VALVE PACKING

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FIG. 43
ROD GRIPPER ASSEMBLY

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FIG. 44
ROD GRIPPER TEST FACILITY

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The tests were run with the gripper submerged in sodium at 300C. The gripper was operated daily for 5 days and then at 2-day intervals for one week. It operated satisfactorily and smoothly. Two additional tests were made each after a one-week interval of soaking. Very slight sticking was observed at the start of the jaw movement, but the unit operated satisfactorily. The facility was operated for a total of 5 weeks. After disassembly and sodium cleanup, the moveable parts of the gripper were examined and were found to be in good condition. The gripper jaws worked somewhat stiffly due to the absence of any kind of lubricant. The relatively small number of manipulations did not cause any wear of the working parts.

V. COOLANTS

A. Liquid Metal Purification Studies - Description of Sampling and Analytical Method - J. R. Humphreys

The vacuum cup sampler is now being used for all oxide determinations in high-temperature Na and NaK systems requiring such data.

The apparatus (Fig. 45) consists of a nickel sampling cup mounted in an evacuated chamber with an annular space provided to accommodate and drain the overflow from the liquid metal sample. A resistance heater is mounted in an annulus around the sampling cup and is controlled to maintain a pre-set sampling cup temperature. Other heaters are mounted on the sampling line and sampling valve and are set at, or above, system temperature to prevent a possible precipitation of oxide in the sampling lines. For sampling sodium systems an additional heater is required on the drain line to prevent freezing and plugging. Cooling coils are positioned to cool the outer wall of the sampler to provide a condensing surface for the distillate during the distillation operation.

In taking a sample the sampling unit is first evacuated and, after the cup, sampling valve and line, and the overflow drain line reach operational temperature, the sampling valve is opened and approximately ten cup volumes of liquid metal are allowed to flow into the sampling cup. This is visually measured by observing the height of liquid metal in the glass overflow reservoir. At this point, the sample valve is closed and water is introduced into the copper-cooling coils. While maintaining the sample cup temperature with the electrical heaters, the unit is allowed to stand under vacuum until distillation is complete. The system is then pressurized with helium to approximately 2 psig and the bottom flange assembly with the mounted sample cup is removed and quickly replaced with a duplicate assembly containing a clean cup. The exposed sample cup is then washed with distilled, degassed water and the total basicity of the dissolved residue determined by titration. From this and the known cup volume the total weight-per cent of oxygen in the sodium sample is calculated directly.

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B. Transport of Activity in Dynamic Sodium Corrosion Loop -
J. R. Humphreys, E. S. Sowa, A. J. Ulrich

Two pins of irradiated normal uranium ($1/8$ in. dia. x $1-1/4$ in. long) were placed in the test section of the loop on September 13, 1954. The pins were irradiated to a burnup of 0.0056% for pin No. AB-18-3 and 0.0192% for pin No. AB-20-3. The time of loading into the loop was 129 days after irradiation.

The loop was operated for a total of 1,320 hr with conditions of operation as shown in Table XVII.

Table XVII

SODIUM-URANIUM LOOP OPERATION

<u>Operation Time, hr</u>	<u>Flow Rate, fps</u>	<u>Temp, C</u>	<u>O₂, wt-%</u>	<u>Activity</u>
0	1.38	360		None
216	1.38	360		None
504	1.38	360		None
888	2.42	450		None
936	2.91	500		Activity
1,008	2.91	500	0.008	Activity
1,032	2.91	500	0.004	Activity
1,056	2.91	500	0.004	Activity
1,080	2.91	500	0.004	Activity
1,104	2.91	500	0.003	Activity
1,176	2.91	500	0.004	Activity
1,200	2.91	500	0.002	Activity

Sodium samples for oxide determinations were taken using the vacuum cup sampler. The excess sodium in the glass overflow reservoir from each sampling operation was monitored for activity.

For the first 216 hr, samples were taken daily. No activity was detected on the overflow samples up to this time. Subsequently, it was discovered that the sampler was faulty, and this made the initial oxide results unreliable. This defective operation prevented the taking of samples in the period of 216 to 936 hr of operation. After 936 hr, daily samples were again taken for analysis. The data from these analyses are shown in Table XVIII.

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ACTIVITY IN SODIUM-URANIUM CORROSION
LOOP-EXTERNAL SURVEY
 (mr/hr at 2 in., Gamma)

<u>Days of Operation</u>	<u>Flange Before E.M. Pump</u>	<u>Position Calrod Heater 4 in. Section</u>	<u>Bottom of Surge Tank</u>
0	2	2	70
2	3	2	60
4	-	2	35
6	2	-	44
8	2	2	40
10	2	2	-
12	-	2	-
14	2	2	25
16	2	2	24
18	2	2	21
20	3	-	-
22	-	2	12
24	-	-	16
26	2	4	-
28	5	5	14
30	-	5	30
32	5	10	15
34	-	-	-
36	5	5	25
38	6	5	25
40	-	-	-
42	-	5	30
44	10	10	36
46	-	13	-
48	12	15	36
50	14	18	60
52	15	19	80
54	-	-	110

Activity surveys were made over the loop daily; the survey detected gamma activity only. The initial survey made after the samples were loaded and before the startup of the loop showed an appreciable gamma background over all points of the loop. This background was introduced by the samples since it was not possible to completely shield them. The data for these surveys are reported as milliroentgens per hour at 2 in., γ radiation; however, considerable scatter is observed in the data since different instruments and different observers made the measurements over the period of operation.

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The data are plotted in Fig. 46. It can be seen that a significant part of the sample's activity is being transported from the sample and is depositing in various positions over the loop, the flanges and the bottom of the surge tank showing the greatest buildup of activity. The plot of activity in the surge tank exhibited a decrease, initially, to a minimum and then began climbing. This effect is caused by the proximity to the source whose activity is diminishing by both radioactive decay and transport. The rise of activity in the surge tank is caused by actual deposition of active material.

In addition to the external survey, the samples removed for oxide analysis were also counted for activity. Initially, the samples did not show any detectable activity up to 216 hours of operation. When it was again possible to take samples after 936 hr, considerable activity was shown to be present in the sodium. This activity is currently under investigation and the results will be reported in future reports.

VI. DESIGN AND EVALUATION

A. EBR-II Working Model

1. Reactor Vessel Cover Lifting and Rotating Mechanism - D. Nicoll

The cover lifting and rotating mechanism for the EBR-II Working Model (Fig. 47) performs the following functions:

- a. Raises or lowers the cover away from or onto the reactor vessel.
- b. Rotates the cover over or away from the reactor vessel to permit free access to the tank during the loading and unloading operation.
- c. Permits the hold-down mechanism to seal the cover against pressure during simulated reactor operation.
- d. Permits free expansion of the operating members due to thermal expansion.
- e. Holds the fuel elements against their seats by depressing the fuel subassembly holddown springs.
- f. Aligns the control rod guide sleeve.
- g. Provides a bearing for the control drive rod lower section.

The cover is raised and lowered by a rack and pinion drive and is rotated between the open and closed position by a "Geneva" drive. Accurate angular position is insured by a large dowel, located 180 degrees

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FIG. 46
ACTIVITY AT VARIOUS LOCATIONS IN
DYNAMIC SODIUM CORROSION LOOP

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from the actuating shaft. A large-diameter actuating shaft, supported by a bearing secured to the reactor vessel, is provided to limit deflection due to the overhung load of the cover.

Leakage of helium gas and sodium vapors into the mechanism is prevented by a rotary seal within the lift-shaft rack and a nesting-type bellows secured to the lift-shaft rack and housing.

The raising or lowering of the cover is accomplished by an electric motor and reduction unit, coupled directly to the rack pinion through an over-running clutch and pin-type coupling. One-half of the pin coupling is attached to the reduction unit shaft and the other half is secured to the over-running clutch cylindrical outer housing. The circular inner race is attached to the pinion shaft. The two pins in the drive half operate in slots in the driven half of the coupling and interconnect the unit into a single drive.

When the cover is lowered to the reactor vessel, the weight of the cover and actuating shaft transmits a torque to the pinion and over-running clutch. Since the weight of the cover is downward, the drive pins in the coupling will be held in the drive position within the slot and the torque will be transferred to the gear within the reduction unit. Thus, the motion in lowering the cover is controlled by the speed of the electric motor. As soon as the cover rests on the reactor vessel, the torque on the clutch is released and the clutch will continue to overrun if permitted. At this point the two tension springs attached to the drive pins and the driver half of the coupling apply a torque to the over-running clutch, permitting the energizing spring to maintain the sprags in contact with both inner race and outer housing and actuate the clutch in the drive position. Any further rotation of the drive half of the coupling rotates the drive pin within the slot causing no motion to be transmitted to the lift shaft pinion. The rotation of the drive pin will continue until the motor is stopped. Under these conditions the cover rests on the reactor vessel and the lift shaft is free to move in an upward direction since the over-running clutch is now in the free-wheeling position. Thus, thermal expansion can be compensated for without any restraint to the mechanism.

A position indicator secured to the pinion shaft indicates the movement of the lift shaft.

After the cover is raised above the reactor vessel, it can be rotated away by a Geneva drive which performs the operation in two stages. Power for the drive shaft is supplied by an electric motor and reduction unit. An arrangement of micro-switches and a cam control the direction of current to the electric motor.

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2. Loading and Unloading Mechanism - E. Hutter

The loading and unloading mechanism for the EBR-II Working Model has been designed and released for construction. The mechanism consists of four units whose combined manual operation, by means of gears and racks, accomplishes the transfer of a fuel or blanket element between the reactor and the storage rack (Fig. 48). The units extend through the top shield structure and are sealed by bellows or rotating seals.

The unloading procedure is as follows:

After the coolant flow has stopped and the reactor pressure vessel cover has been removed, the seals on the two rotating shield plugs are loosened and the inner eccentrically rotating shield plug and the larger plug are rotated, moving the gripper assembly directly over any desired position in the core lattice. The gripper guide is lowered and the guide tube positioned over the fuel element to be removed. In this position, the guide tube also holds the adjoining elements in their lattice position. The gripper is lowered through the guide tube to grasp and lift the element out of the reactor vessel into the sodium stratum above the reactor.

The two shield plugs are rotated to a position where the gripper releases the element to transfer arm which, in turn, moves the element to a position above the storage rack.

The transfer arm has no movable parts other than the rotating shaft; therefore the storage rack is raised to receive the element in one of several compartments in the rack. The rack can be raised further to permit visual inspection of the tops of the stored elements.

In order to load an element into the reactor vessel, the transfer operation is reversed.

FIG. 48
LOADING AND UNLOADING MECHANISM
EBR II WORKING MODEL