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Argonne National Laboratory

EXPERIMENTS WITH PERIPHERAL SUPERHEATER CORE PSH-1, BORAX-V

by

BORAX-V Project Staff

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by

BORAX-V Project Staff

Idaho Division

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EXPERIMENTS WITH PERIPHERAL SUPERHEATER CORE PSH-1, BORAX-V

by

BORAX-V Project Staff

I. INTRODUCTION

After power operation of central superheater core CSH-1 was suspended because of superheater fuel failures due to fabrication and repair difficulties, the central superheater core and core structure were removed from the BORAX-V reactor vessel and replaced with the peripheral superheater core and structure.¹ To prevent reoccurrence of the difficulties with the central superheater assemblies, the peripheral superheater fuel assemblies were repaired, modified, inspected, and tested before installation in the reactor.

The BORAX-V reactor, containing a core with a peripherally located superheater, PSH-1, was successfully operated from April through August 1964. This marked the first prolonged operation of an integral boiling water-nuclear superheating reactor in the United States. Both boiler and superheater fuel performance in this core was excellent.

A minimum program of zero-power and power experiments was carried out with the core with the peripheral superheater. The zero-power experiments included control-rod calibrations, measurement of superheater flooding reactivity worth, and available excess reactivity at room and operating temperatures. In addition, the temperature coefficient of reactivity, boric acid reactivity worth, power distribution, cadmium ratios, etc. were measured.

The experimental program at power included the achievement of power and steam temperature in excess of the original design values; the successful test operation and demonstration of the special reactor and steam systems that are required for an integral boiling-nuclear superheating reactor; power distribution, in-core instrumentation, and transfer-function measurements; and power split, water chemistry, and prepared, defective-fuel experiments. Natural recirculation of coolant water was used throughout these experiments.

The BORAX-V Project was terminated at the end of August 1964. Because of this early ending, several portions of the originally planned experimental program were not carried out. These included achievement of high

powers and power densities with both central superheater and pure boiling cores, operation with forced circulation, fabrication and testing of advanced-design superheater fuel elements, investigation into the causes of instability in high-power density boiling and integral boiling-nuclear superheating reactors, and certain special reactor safety experiments such as flooding the superheater at power, and loss of coolant to the superheater.

However, the major objectives of the BORAX-V program were achieved. The feasibility and safety of integral boiling water-nuclear superheating reactors were demonstrated, and the problems pertinent to design and operation of this type of reactor were investigated theoretically and experimentally.

II. REACTOR DESCRIPTION²

The BORAX-V reactor and power plant were located in two buildings one-half mile from the control building at the National Reactor Testing Station in Idaho.

Three separate core-structure configurations were tested in this flexible experimental facility: a pure boiling-water core, a boiling-water core with a centrally located superheater, and a boiling-water core with a peripherally located superheater. In all cases, it was possible to operate with either forced or natural circulation of water through the boiler zone of the core, but only natural circulation was used. Water served as moderator and reflector in both the boiling and superheating regions of the core. The principal design parameters of BORAX-V at the rated power are listed in Table VIII (p. 102).

The peripheral superheater core contained 44 boiling fuel assemblies in the core center and 16 superheater fuel assemblies on the core periphery. The peripheral superheater core structure was similar in concept to the central superheater core structure shown in the cutaway view of the reactor vessel in Fig. 1. All BORAX-V cores had an equivalent diameter of 39 in. and a height of 24 in., with the bottom of the active core positioned 3 ft above the bottom of the reactor vessel. An X-8001 aluminum-nickel alloy shroud formed the control-rod channels and fuel cells.

The poison section of the nine control rods was made of Boral canned in stainless steel, and the follower section was made of X-8001 aluminum alloy. The central and four intermediate control rods were cruciform, and the four outer rods were T-shaped. The control rods were driven from below the reactor, through a linear seal, by motor-driven, lead screw-and-nut mechanisms with solenoid scram latches.

The superheater fuel assemblies, with their long risers, were held down by means of the reactor vessel head and a hold-down plate and individual coil springs. Steam from the reactor vessel steam dome made two passes through the superheater. Figure 2 is a bottom view of the peripheral superheater core structure, the interpass plenum chambers, core support plate, and superheater flood-and-drain manifold. Eighty-one percent of the reactor power was required in the boiler to convert feedwater into saturated steam; 17% of the total power was used in the superheater to superheat the steam from 489 to 850°F, and an additional 2% was lost from the superheater to the moderator water.

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Fig. 1. Reactor with Central Superheater

Fig. 2
Peripheral Superheater
Core Structure,
Bottom View

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The boiling-zone fuel assemblies for BORAX-V, shown in Fig. 3, contained fuel rods, 0.375 in. OD, clad with Type 304 stainless steel 0.015 in. thick and containing UO_2 pellets of 4.95 w/o enrichment. Each rod was filled with helium and had a fission gas expansion space that also contained a spring of Inconel-X to retain the pellets while allowing for the axial differential expansion between the UO_2 and the cladding.

A fully loaded boiling fuel assembly contained 49 fuel rods in a 1/2-in.-square lattice, as shown in Fig. 4. The rods were individually latched in so that the water-to-fuel ratio or enrichment could be varied, or poison rods inserted to adjust reactivity or power distribution. The water-to-fuel ratio was varied by changing the number and arrangement of water-filled flow rods in the assemblies. A flow rod, as shown in Fig. 5, had the same outside geometry and dimensions as a boiling fuel rod, but the in-core portion was made of X-8001 aluminum tubing, 3/8 in. OD x 0.020 in. wall thickness, perforated on each end to insure flooding with reactor water. Flow rods were used to increase the moderator fraction of the core and to prevent bypass of coolant water through a vacant fuel-rod location. The design of a boron-stainless steel poison rod is shown in Fig. 6. The boiling fuel-rod loading used for all three cores was designed for the boiler of the central superheater core, since this zone required the highest fuel enrichment.

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Fig. 3. Boiling Fuel Rod

ID-103-3466

Fig. 4. Fuel Rod and Cutaway Section of Boiling Fuel Assembly

SECTION THRU ROD

ID-103-A3324

Fig. 5. Flow Rod

ID-103-3793

Fig. 6. Boron-Stainless Steel Poison Rod

The design of the welded superheater fuel assembly, made up of five fuel elements with intervening moderator water gaps, is shown in Figs. 7 and 8. Each fuel element was contained in a rectangular stainless-steel tube, which formed a static-steam insulating gap around the element to minimize heat loss to the moderator water. The upper riser and lower nozzle in the fuel assembly also had double-walled, static-steam insulating gaps. The four-plate fuel elements were assembled by vacuum-brazing at 2150°F by means of Coast Metals 60, a chrome-nickel brazing alloy. Each peripheral superheater fuel assembly contained 680 g of U^{235} in the form of 93 w/o-enriched UO_2 particles dispersed in the cermet fuel plate matrix of Type 304-B stainless steel. Since coolant steam flowed past one side only, the two outside fuel plates contained 53.6% as much U^{235} as the two center plates. The outside plates contained 17.7 w/o UO_2 in the matrix, and the central plates contained 31.8 w/o. The peripheral superheater fuel elements were modified from the original design by the installation of **spacer combs** at each end of each coolant channel.

The cladding material used on the superheater fuel plates was Type 304-L stainless steel, which is susceptible to chloride stress corrosion. This type of corrosion was inhibited by maintaining less than 0.04 ppm of Cl^- in the reactor water and steam by means of the makeup water, reactor water, and condensate demineralizer systems, and by continuous chloride monitoring.

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Fig. 7. Cross Section, Peripheral Superheater Fuel Assembly

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The superheater fuel element had good shutdown radiation and conductive cooling properties across the static-steam insulating gap to the moderator water. Even with no steam flow and a total reactor power of 0.8 MWt, the maximum design fuel surface temperature of 1200°F could not be exceeded. In addition to these design features of the superheater fuel element, BORAX-V had a superheater vent valve system which opened automatically on any normal shutdown or scram to assure continued flow of cooling steam produced by decay heat in the boiling zone. A schematic diagram of the BORAX-V process systems is shown in Fig. 9.

Steam-line radioactivity scrams and alarms, and sensitive fission-product monitors, sampling both saturated and superheated steam, were provided in BORAX-V because of the postulated high rate of fission-product carryover in the steam from a ruptured superheater fuel element. The condenser air-ejector exhaust system had a special after-cooler and demister to remove moisture from the gas. The exhaust gases were then diluted with clean air and passed through an AEC-type, high-efficiency filter and an activated-charcoal gas scrubber before being monitored, and ejected up a stack.

Fig. 8

Peripheral Superheater Fuel Assembly

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Fig. 9. Flow Diagram

III. REACTIVITY MEASUREMENTS

A. Minimum Core Experiments

Before the peripheral superheater core PSH-1 was loaded, two minimum-critical-mass experiments and experiments with a 16-boiling-fuel-assembly core were performed at room temperature and zero power.

1. Minimum Critical Mass with Peripheral Superheater Fuel

A minimum-critical-mass experiment was attempted by means of standard peripheral superheater fuel assemblies containing 680 g of U^{235} per assembly. However, a critical mass was not achieved, since only 16 assemblies were available and a 4 x 4 array was not critical. Figure 10 shows

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Fig. 10. Sixteen-680-g-superheater-fuel-assembly Array, Minimum Critical-mass Experiment

the final configuration in this experiment. A plot of inverse count rate vs number of assemblies is shown in Fig. 11. Extrapolation of these plots indicates that about 17-18 assemblies would have been a critical array.

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Fig. 11. Inverse Count Rate vs Number of 680 g Superheater Fuel Assemblies, Minimum-critical-mass Experiment

2. Minimum Critical Mass with Boiler Fuel

A minimum-critical-mass experiment was performed by means of boiling fuel assemblies containing thirty-seven 4.95 w/o-enriched boiling fuel rods and 12 flow rods. Figure 12 shows the sequence in which these assemblies were loaded to obtain the initial 14-assembly array. The nine-control-rod-bank critical position with 14 assemblies and no poison rods was 21.06 in. A plot of inverse count rate vs number of assemblies is shown in Fig. 13.

The fuel and flow rods were then rearranged to a slab array, shown in Fig. 14. This arrangement was critical at a nine-control-rod-bank position of 22.125 in. Periods were measured with the nine-control-rod bank, and the k_{eff} for this slab was calculated to be 1.0043.

Axial neutron-flux distributions were measured in the minimum critical slab by irradiating U^{235} -Al wire segments, 3/8 in. long and 0.032 in. in diameter, in the locations shown in Fig. 14. The flux wires were held on 1-in. centers in an 0.125-in.-diam by 0.020-in.-wall aluminum tube with a support and guides for proper positioning in the moderator. The data are plotted in Fig. 15.

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Fig. 12. Fourteen-boiling-fuel-assembly Array, Minimum-critical-mass Experiment

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Fig. 13. Inverse Count Rate vs Number of 12-flow-rod Boiling Fuel Assemblies, Minimum-critical-mass Experiment

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Fig. 14. Minimum Critical Slab with Boiling Fuel, 12 Flow Rods per Assembly

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Fig. 15. Axial Neutron-flux Distributions in Minimum Critical Slab

In Fig. 15 a least-squares cosine fit was applied to the data, which were not influenced by either control-rod height or reflector, to infer values for the axial buckling. The results are summarized in Table I.

TABLE I. Experimental Axial Buckling in Minimum Critical Slab of Boiler Fuel

Wire No.	B^2 , cm^{-2}	Nine-control-rod-bank Position, in.
W-1	0.00158	23.30
W-2	0.00155	23.30
W-3	0.00155	23.30

3. Experiments with 16 Boiling Fuel Assemblies

After the minimum critical-mass experiments had been performed, an array of 16 twelve-flow-rod boiling fuel assemblies was constructed, as shown in Fig. 16.

The relative effectiveness of eight boron-stainless steel poison rods, either replacing flow rods, as shown in Fig. 16A, or replacing fuel rods, as shown in Fig. 16B, was determined.

With no poison rods, the critical nine-control-rod-bank position was 17.208 in., and the bank differential reactivity worth at 17.358 in. was 0.636%/in. This compares well with the nine-control-rod-bank calibration curve measured later with the full core. Assuming that the control-rod-worth curve in Fig. 21 is applicable, the available excess reactivity of this 16-assembly array with no poison rods was about 2.65%.

When eight poison rods were loaded in place of flow rods, as shown in Fig. 16A, the nine-control-rod-bank critical position was 20.485 in. The bank differential reactivity worth at 20.785 in. was 0.371%/in., which again compares well with Fig. 21. The available excess reactivity for this array was 0.95%, and each poison rod had an average worth of -0.212%.

The final poison rod arrangement before proceeding to the full core loading is shown in Fig. 16B. In this arrangement, eight fuel rods were replaced by poison rods. This arrangement was slightly subcritical; and from the negative period obtained with the source removed, the reactivity was estimated to be about -0.03%. Thus, replacing eight fuel rods with poison rods in the configuration in Fig. 16B decreased the available excess reactivity by 2.68%, or 0.34% per poison rod.

B. Zero-power Reactivity Measurements

1. Loading to Full Core

After the small-core experiments were completed, the loading to the full 60-assembly PSH-1 core was started. Starting with the 16-boiling-fuel assembly core, fuel assemblies were added in the sequence shown in Fig. 17. The boiling fuel assemblies contained 12 flow rods and

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Fig. 16. Sixteen-boiling-fuel-assembly Core

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Fig. 17. Loading Sequence, Core PSH-1

thirty-seven 4.95 w/o-enriched fuel rods. During the loading operation, boron-stainless steel poison rods were substituted for fuel rods and arranged to prevent criticality on the withdrawal of a single control rod alone. Figure 18 gives the nine-control-rod-bank critical positions and the number of poison rods at the various loading stages. After the loading was completed, the poison rods were rearranged to approximately equalize the shutdown margin on each of the central and intermediate control rods and still prevent criticality on the withdrawal of a single control rod alone. Also, five boiling fuel rods were replaced with hollow, X-8001 aluminum void rods, which had 0.028-in.-thick walls. The void rods were used to simulate Zircaloy-2 miniature ion-chamber thimbles that were installed

ID-103-A3419

Fig. 18. Nine-control-rod-bank Critical Position vs Number of Fuel Assemblies, Core PSH-1

later in core PSH-1A before operation at higher temperatures. The core PSH-1 loading used for the room-temperature, zero-power experiments is shown in Fig. 17. A photograph of the core at this stage is shown in Fig. 19.

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Fig. 19. Zero-power, Room-temperature Loading, Core PSH-1

After completion of the room-temperature experiments, the reactor was loaded for power operation. The five void rods were replaced with Zircaloy-2 miniature ion-chamber thimbles; six stainless-steel flux-wire thimbles, two instrumented superheater fuel assemblies, two instrumented boiling fuel assemblies, and the fueled oscillator rod were installed; and the neutron source drive was removed. This core loading, shown in Fig. 20 and designated core PSH-1A, was used during the zero-power, operating-temperature (489°F, 600 psig) experiments, as well as the initial power experiments.

Fig. 20. Loading Diagram, Peripheral Superheater Core, PSH-1A

2. Control-rod Calibrations

The control rods in the PSH-1 core were calibrated primarily with the superheater drained and at room temperature only. A few control-rod worth measurements were taken while heating the reactor and at operating temperature, and some with the superheater flooded. The control-rod differential-reactivity worths were obtained by using boric acid solution in the moderator water, thus permitting the control rods to be withdrawn

incrementally from the core. This method of control-rod calibration yields an underestimate of the true rod worth and of the total available excess reactivity, but the error is considered small and it had been ignored here. The magnitude of the error involved in boric acid calibrations is discussed in ANL-6964.³ All boric concentrations in the reactor water were analyzed at room temperature and pressure, and all concentrations are reported for these conditions.

A source drive was used to withdraw the antimony-beryllium neutron source after reactor startup throughout all the loading and room-temperature experiments.

a. Calibrations at Room Temperature

The measured differential-reactivity control-rod worths for various control-rod configurations are shown in Figs. 21, 22, and 23.

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Fig. 21. Differential-reactivity Worths of Control-rod Groups, Core PSH-1

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Fig. 22. Differential-reactivity Worths of Single Control Rods, Core PSH-1

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Fig. 23. Differential-reactivity Worth of Five-control-rod Bank (Nos. 1, 2, 4, 6, and 8) with 4-T-rod Bank at Various Positions, Core PSH-1

The reactivity worths of grouped control rods withdrawn from the nine-control-rod bank are given in Fig. 21, single control-rod worths from the nine-control-rod bank are shown in Fig. 22, and the five-control-rod-bank (No. 1, 2, 4, 6, 8) worths with the four T-rods at various positions are given in Fig. 23.

Nine-control-rod-bank reactivity worth measured during the loading sequence is compared with that measured using boric acid in Fig. 24. The agreement between the two methods was good, considering that poison rods were continually being shifted to various positions during the loading. Of interest is the fact that until the core loading was nearly complete, some of the control rods were outside a fuel region and contributed very little to the nine-control-rod bank worth. For example, when there were 16 assemblies or less (see Fig. 17 for loading order), control rods No. 3, 4, and 5 were virtually worthless and rods No. 2, 6, 7, and 9 were worth very little, leaving only rods No. 1 and 4 in positions to have a significant effect on overall reactivity. Some of this agreement can be attributed to a radial shift of reactivity importance as the reactor was

ID-103-A3429

Fig. 24. Differential-reactivity Worth of Nine-control-rod Bank, Comparison of Measurements Made with Boric Acid and during Core Loading, Core PSH-1

loaded. The magnitude of the error involved in calibrating control rods with boric acid may be of the same order as the error in calibrating control rods by loading fuel assemblies. The similarity between these two methods of measurement has been observed before in SPERT-I⁴ and SPERT-IV.⁵

Table II compares the relative reactivity worths of individual control rods and groups of rods.

TABLE II. Comparison of Relative Reactivity Worths of Individual Control Rods and Groups of Rods, Core PSH-1

Ratio	68°F Superheater Flooded, No Boric Acid ^a	68°F Superheater Drained, No Boric Acid ^b	68°F Superheater Drained, 8.95 g (H ₃ BO ₃)/ gal (H ₂ O) ^c
$\frac{(\Delta\rho/\Delta h) \text{ No. 1 rod}}{(\Delta\rho/\Delta h) \text{ No. 2 rod}}$	1.74	1.97	1.93
$\frac{(\Delta\rho/\Delta h) \text{ No. 1 rod}}{(\Delta\rho/\Delta h) \text{ 9-rod bank}}$	0.26	0.29	0.27
$\frac{(\Delta\rho/\Delta h) \text{ 4 T-rods}}{(\Delta\rho/\Delta h) \text{ 9-rod bank}}$	0.16	0.14	0.14
$\frac{(\Delta\rho/\Delta h) \text{ 4 T-rods}}{(\Delta\rho/\Delta h) \text{ No. 2 rod}}$	1.09	0.94	0.99

^aNine-control-rod-bank position: 8.65 in.

^bNine-control-rod-bank position: 9.63-9.67 in.

^cNine-control-rod-bank position: 16.26-16.29 in.

b. Calibrations above Room Temperature

For these experiments in core PSH-1A, the antimony-beryllium neutron source was in a fixed location and the fueled section of the oscillator was turned toward the core. The reactor system was heated with electric heaters in steps, from room temperature up to ~489°F, 600 psig. Control-rod differential-reactivity worths measured at various temperatures are shown in Figs. 25 and 26. These measurements are considered inaccurate for several reasons. First, the neutron source could not be removed; therefore a critical position obtained with the linear neutron-flux channel at less than the 10^{-8} range was affected by the source neutrons. Most of the criticality measurements were made with the linear channel on the 10^{-9} scale. Second, some moderator boiling seemed to occur as the power increased in period measurements, in some cases into the 10^{-7} range of the linear channel. These two effects tended to cause underestimation of the differential control-rod worth. Third, the electric heaters were frequently turned on and off to maintain a fairly constant temperature. The effect of the temperature oscillations on the control-rod worth measurements could have been either positive or negative.

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Fig. 25. Differential-reactivity Worths of Control-rod Groups at Various Temperatures, Core PSH-1A

ID-103-A3430

Fig. 26. Differential-reactivity Worths of Control Rods Nos. 1 and 2 at Various Temperatures, Core PSH-1A

The room-temperature control-rod calibration curves are also shown in Figs. 25 and 26 for comparison. Since the spread of data points was so great in these measurements, it was assumed that the room-temperature calibration curves would hold for all temperatures.

c. Shutdown Margin

The control-rod shutdown reactivity margin at room temperature with superheater flooded and no boric acid in the reactor water was estimated by assuming that the nine-control-rod-bank differential-reactivity worth was linear from 0 to 8.66 in., having values of 0.0 and 1.52%/in., respectively. The average differential-reactivity worth was then 0.76%/in., yielding a shutdown margin of reactivity of ~6.7%. The shutdown margin at room temperature with superheater drained, obtained by a similar process, was ~8.0%.

3. Available Excess Reactivity

a. Cores PSH-1 and PSH-1A

The nine-control-rod-bank differential-worth curve shown in Fig. 21 was integrated to obtain the reactivity held in control rods as a function of control-rod position. The integration included the data taken with superheater coolant channels flooded, as indicated by the dashed line. As discussed in the preceding section, this integrated rod worth, shown in Fig. 27, is assumed to hold for all temperatures, which results in an underestimation of the available excess reactivity at higher temperatures.

ID-103-A3304

Fig. 27. Integrated Reactivity Worth of Nine-control-rod Bank, Core PSH-1

Some detailed calculations of available excess reactivity have been performed on core PSH-1.³ The results of these calculations, normalized to the measured excess reactivity at room temperature with superheater drained, are given in Table III for comparison with the measured values.

TABLE III. Available Excess Reactivity, Cores PSH-1 and PSH-1A

Core	Temp, °F	Superheater Condition	Nine-rod-bank Critical Position, in.	Available Excess Reactivity, %	
				Measured	Calculated ^a
PSH-1	70	Flooded	8.66	12.14	11.43
	70	Drained	9.66	10.48	10.48
PSH-1A	300	Flooded	10.00	10.00	10.10
	300	Drained	11.10	8.80	-
PSH-1A	485	Flooded	12.75	6.50	7.83
	485	Drained	14.00	5.20	6.73

^aCalculated reactivity is defined as $(k_{\text{calc}} - 1)/k_{\text{calc}}$.

b. Core PSH-1B

As part of the defective boiler fuel tests, ten fresh, prepared defective, boiling fuel rods were installed in the reactor. Two of these 4.95 w/o-enriched fuel rods replaced boron-stainless steel poison rods in core position 55, rod position 33, and in core position 54, rod position 31. (See Fig. 20 for original poison-rod locations.) This new core configuration with 38 poison rods was called core PSH-1B.

The reactivity effect of this change was estimated by comparison with a critical experiment on core PSH-1A. The nine-control-rod-bank critical position at 120°F, with superheater flooded, was 8.924 in. For core PSH-1B, the critical position for the same conditions was 8.77 in. Since the average nine-control-rod-bank reactivity worth in this range was 1.50%/in., the reactivity was increased by ~0.23%.

c. Core PSH-1C

When the prepared, defective, superheater fuel sample was installed, five additional boron-stainless steel poison rods were removed, reducing the total number of poison rods to 33. Some of the remaining poison rods were relocated. In addition, two 9.9 w/o-enriched boiling fuel thermocouple rods were installed in place of 4.95 w/o rods. The fueled oscillator in the reflector, which had a broken drive shaft connector, was removed. This revised loading (shown in Fig. 28) was designated core PSH-1C.

ID-103-A3412

Fig. 28. Loading Diagram, Peripheral Superheater Core, PSH-1C

The reactivity gain of core PSH-1C, compared to core PSH-1A with 40 poison rods, was measured to be about 0.5% at 120°F. This value does not account for the oscillator orientation, which was unknown during the last critical experiments on core PSH-1A because of the broken drive shaft. However, the reactivity worth of the oscillator at room temperature had been determined to be only +0.009% or -0.015% with fuel section or poison section, respectively, facing the core.

4. Temperature Coefficient of Reactivity

The nine-control-rod-bank critical position as a function of temperature is plotted in Fig. 29.

ID-103-A3431

Fig. 29. Nine-control-rod-bank Critical Position vs Temperature, Core PSH-1

The temperature coefficient of reactivity as a function of temperature was determined from the data shown on Fig. 29 and the reactivity held in control rods at that rod position shown in Fig. 27. The temperature

coefficient as a function of temperature thus obtained is shown in Fig. 30. The average temperature coefficient from room temperature to operating temperature is $-0.0126\%/^{\circ}\text{F}$ with superheater drained, and $-0.0134\%/^{\circ}\text{F}$ with superheater flooded.

ID-103-A3310

Fig. 30. Isothermal Temperature Coefficient of Reactivity, Core PSH-1A

5. Boric Acid Effects

Nine-control-rod-bank critical positions at room temperature as a function of boric acid concentration (C_B) in the reactor moderator water have been plotted in Fig. 31. A least-squares fit to the data yields the following relationship:

$$C_B = -26.103 + 3.672 h - 0.111 h^2 + 0.001 h^3, \quad (1)$$

where h is the nine-control-rod-bank height in inches. As shown in Fig. 31, a boric acid concentration of 13.5 g (H_3BO_3)/gal (H_2O) was required to maintain criticality with all control rods completely withdrawn to ~25 in.

ID-103-A3432

Fig. 31. Nine-control-rod-bank Critical Position vs Boric Acid Concentration, Core PSH-1

During the control-rod calibration at room temperature, several measurements were made in which the boric acid was diluted slightly and the nine-control-rod bank was raised to the critical position of the previous boric acid concentration. This, in effect, was the same as leaving the control rods stationary at a critical position and decreasing the boric acid concentration slightly to go on a positive period. Theoretically, this type of measurement should yield the boric acid reactivity worth directly, assuming that control-rod position does not affect the boric acid worth. However, the values of boric acid reactivity worth obtained in this manner were consistently too low, ranging from ~ 0.65 to $0.60\%/g/gal$. These measurements are believed to be low because the integral of the nine-control-rod-bank differential-worth curve, which is known to underestimate available excess reactivity, indicated 10.5% available excess; if the boric acid worth were $0.65\%/g/gal$, and $13.5 \text{ g}(\text{H}_3\text{BO}_3)/\text{gal}(\text{H}_2\text{O})$ allowed complete withdrawal of the control rods, the available excess reactivity would have been predicted

to be 8.8%. Apparently, a consistent error was involved in the dilution measurements, possibly because the boric acid was not well mixed in the system.

If the boric acid concentration for a particular critical control-rod height is read off the least-squares-fit curve of the nine-control-rod-bank critical position vs boric acid concentration shown in Fig. 31, the boric acid differential-reactivity worth decreases from 0.82%/g (H_3BO_3)/gal (H_2O) at low concentrations to about 0.74%/g (H_3BO_3)/gal (H_2O) at high concentrations. These points are plotted in Fig. 32. Integration under this curve yields a value of excess reactivity of 10.7%, which agrees well with the value of 10.5% obtained by integrating the differential control-rod worth curve.

ID-103-A3433

Fig. 32. Differential-reactivity Worth of Boric Acid vs Concentration, Core PSH-1

6. Superheater Flooding Measurements

One of the unique potential hazards of a small, integral, boiling-nuclear superheating reactor such as BORAX-V is the accidental flooding

of the superheater during operation. In a core with the size and composition of BORAX-V, superheater flooding yielded a positive reactivity addition. Two modes of superheater flooding were possible: (1) opening a superheater flood valve and flooding from below; and (2) raising reactor water level and flooding over the top of the inlet superheater fuel assembly risers.

To add reactivity by flooding the superheater from below during operation required the lifting of a switch guard on the control to the solenoid-tripped flood valve, or the removal of a padlock on the remote operator for the manual flood valve. The "flood-valve-open" scram interlocks would have had to be bypassed. To prevent an accident, even if these precautions failed, a flow-restricting orifice was installed in the superheater flood line to slow the flooding time.

Flooding the superheater from above was potentially more hazardous because of the possible higher flooding rate. To add reactivity by raising the reactor water level rapidly and flooding over the top of the inlet superheater fuel assembly risers would have required maloperation of the feedwater pumps, ignoring the high-reactor-water-level alarm, and failure of two independent high-reactor-water-level scram circuits. To lengthen the flooding time, the riser on one of the eight inlet superheater fuel assemblies was made 2 in. shorter than the remaining risers. The postulated sequence in flooding the peripheral superheater from above was as follows: Rising reactor water first flooded over the short riser inlet superheater fuel assembly, flowed down through this assembly, and filled the interpass plenum and parallel-connected lower superheated steam main before it began to flood the remaining 15 assemblies from below. Before the flooding level reached the top of the core, the rising reactor water level reached the top of the other seven standard inlet assembly risers, flooded down through these assemblies, and speeded up the remaining bottom flooding of the eight outlet assemblies.

Only static superheater flooding worth measurements were made on core PSH-1 and core PSH-1A because the room-temperature flooding time measurements on central superheater core CHS-1¹ were felt to be applicable. In fact, the flooding time of the peripheral superheater should have been longer than that of the central superheater because of the larger superheater coolant channel volume.

The static reactivity effects of flooding the peripheral superheater coolant channels with demineralized water in a core containing no boric acid were measured at both room and operating temperatures. To measure the reactivity effects, control-rod critical positions were determined both with all nine control rods banked, and with the four outer T-shaped control rods completely withdrawn and the remaining five control rods banked. The results of the measurements are shown in Table IV.

TABLE IV. Superheater Flooding Reactivity Worths,
Cores PSH-1 and PSH-1A

Core	Temp, °F	Control-rod Configuration	Flooding Reactivity Worth, %
PSH-1	68	Nine rods banked	1.66
PSH-1	68	Four T-rods withdrawn	1.86
PSH-1A	489	Nine rods banked	1.30
PSH-1A	489	Four T-rods withdrawn	1.50

The values of superheater flooding reactivity worths were higher in the peripheral superheater core than in the central superheater core.¹ However, based on the flooding time measurements made on core CHS-1, the maximum reactivity addition rate when flooding the peripheral superheater at room temperature was estimated to be only about 0.25%/sec. The resulting excursion would have been slow enough to be safely terminated by the automatic period and power level trip scram circuits. Because of the flashing of flooding water to steam on hot fuel plates, flooding of the superheater at power would have been even slower.

7. Other Reactivity Measurements

A measurement was made at room temperature in core PSH-1 to compare the reactivity worths of various fuel assemblies in core position 35. Compared to water in this location, a boiling fuel assembly containing 37 fuel rods and 12 flow rods was worth 1.33% reactivity, a peripheral superheater assembly containing 680 g of U^{235} was worth 1.25%, and a central superheater assembly with 430 g of U^{235} was worth 0.84%.

The static reactivity worth of the fueled rotating oscillator described in Section VIII-A-1 was measured at room temperature to be 0.009% with the fuel section toward the core, and -0.015% with the poison section toward the core. This yielded a total 180° differential oscillator reactivity worth of 0.024%.

8. Experimental Techniques and Errors

The boric acid method of calibrating control rods used in core PSH-1 was the same as that used in all previous BORAX-V cores.^{1,6,7} Qualitative discussion of the error in control-rod worth caused by boric acid is given in ANL-6961,¹ in which a maximum of 15% error in total control-rod worth was suggested. A more quantitative analysis of the effect of boric acid on control-rod worth is given in ANL-6964.³

The accuracy of the chemical analyses of boric acid concentration appeared to be good during the control-rod calibrations on core PSH-1. At the higher boric acid concentrations, the concentration was observed to decrease over a 24-hr period, as was observed in the CSH-1 core.¹ Because of these variations in concentration, a least-squares fit to the curve of control-rod critical position vs boric acid concentration was made.

The effect of the neutron source on control-rod critical position was examined for the CSH-1 core,¹ and was again investigated to a certain extent for the PSH-1 core. The difference in critical position observed at powers of 10^{-9} or 10^{-8} Amp on the linear neutron-flux channel recorder is given in Table V at two temperatures. Assuming that the neutron source effect was negligible in the range of 10^{-8} Amp, the error in the nine-control-rod-bank critical position varied from 0.005 to 0.036 in. when in the range of 10^{-9} Amp. For instance, when periods were taken by raising the nine-control-rod bank in increments of about 0.15 in., the source effect might have introduced a 25% error in the differential control-rod worth. The other errors mentioned in the discussion of control-rod calibration in Section III-A-2 are difficult to evaluate; but, qualitatively, the control-rod worth measurements above room temperature probably yielded values of differential-reactivity worths significantly lower (up to 30%) than actual.

TABLE V. Effect of Neutron Source and Flux Level on Control-rod Critical Position, Core PSH-1

Linear Neutron Flux Recorder Range, Amp	Nine-control-rod-bank Position, in.	
	At 200°F	At 338°F
10^{-9}	9.362	10.414
10^{-8}	9.367	10.450

C. Reactivity Measurements at Power

After the zero-power measurements at operating temperature were completed, the reactor with core PSH-1A was brought to full power in a series of steps. Figure 33 shows the nine-control-rod-bank position as a function of power before fission-product poisons and fuel burnup had a significant effect. Assuming that the room-temperature, zero-power, control-rod worth curve still held, the average power coefficient of reactivity was estimated to be 0.27%/MWt. The control-rod reactivity worth at 489°F has been shown by calculation to be higher than the worth at room temperature. At power, the control-rod worth was even higher. Consequently, the available excess reactivity is underestimated when the

room-temperature control-rod worth curves are used. The power coefficient based on the calculated available excess reactivity, discussed in Section III-B-3, averaged 0.38%/MWt from 0 to 18.5 MWt.

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Fig. 33. Nine-control-rod-bank Position vs Reactor Power, Core PSH-1A

The effect of the buildup of xenon and other fission products during power operation of core PSH-1C is shown in a typical curve of maximum power vs time in Fig. 34. From this curve, the maximum power with all control rods withdrawn and essentially no fission-product poisons was about 20.5 MWt; after xenon had built up to equilibrium concentration, the maximum power was about 12.5 MWt, or a decrease of 8 MWt. The calculated reactivity worth of equilibrium xenon at 12.5 MWt was ~2%.⁸ The average power coefficient of reactivity from 12.5 to 20.5 MWt is thus 0.25%/MWt.

ID-103-A3435

Fig. 34. Reactor Power vs Time, Core PSH-1C

The power coefficient of reactivity from 5 to 18.1 MWt deduced from the dynamic measurements is plotted in Fig. 139 and discussed in Section 11 of the Appendix.

IV. POWER-DISTRIBUTION MEASUREMENTS

Core-physics calculations made during the design of BORAX-V produced smooth flux plots for the peripheral superheater core by using homogenized number densities in various computer codes.² Flux peaking factors within fuel assemblies were obtained from PDQ cell calculations for homogenized media within control cells. However, since the peripheral superheater core of BORAX-V was far from homogeneous, it was desirable to determine experimentally the local axial and radial variations in the neutron flux within a fuel assembly, from assembly to assembly, and between the boiler and superheater zones. This provided information on power distribution for the heat engineering analysis, peak-to-average power ratios, fractional power production in the boiler and superheater zones, and verification of the calculational techniques used.

Neutron flux measurements included coarse flux distributions (both radial and axial), cadmium ratio measurements, and fine power-distribution measurements. Data were obtained from both flux-wire activation and miniature fission chambers.

A. Coarse-neutron-flux Distributions

1. Measurement Techniques

Five different detector materials were used in the coarse-neutron-flux mapping experiments:

- a. Uranium-aluminum wire, 7.78 w/o uranium, 93.1 w/o enrichment, 0.032-in. diam.
- b. Uranium-zirconium wire, 3.379 w/o uranium, 93.13 w/o enrichment, 0.031-in. diam.
- c. Copper wire, 0.032-in. diam.
- d. Aluminum--1 w/o copper wire, 0.032-in. diam.
- e. Miniature fission-type ionization chambers loaded with 0.005 g of 93 w/o-enriched uranium.

The flux wires were irradiated in both long-wire and segmented-wire arrangements. Short-wire segments were used for all radial, and for some axial, measurements, and long wires and miniature ion chambers were used for the remainder of the axial traverses.

For the room-temperature irradiations, the flux wires in the boiler region were held in special wire holders that were inserted into coolant channels of the boiling fuel assemblies. These holders were made of

aluminum tubing, 0.125-in. diam x 0.020-in. wall, with a support and guides for reproducible positioning. The reproducibility of vertical wire positioning was assured by the upper shoulder of the holder, which rested on the upper grid of the fuel assembly, as shown in Fig. 35. Both long and segmented wires could be loaded into these holders. Aluminum spacer wires were used to position the flux-wire segments axially.

Room-temperature irradiations in the superheater region were made by using the equipment shown in Fig. 36. Teflon strips, 0.050 in.

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Fig. 35. Flux-wire Holder in Boiling Fuel Assembly

ID-103-A3066

Fig. 36. Flux-wire Holders in Superheater Fuel Assembly

thick, were cut to the width of the coolant channels in a superheater assembly, and a $1/32$ -in. $\times$ $1/32$ -in. groove was cut axially on each strip to hold the flux wires. The strips were inserted into the bottom ends of the coolant passages of special superheater fuel assemblies with removable bottom nozzles and no bottom spacer combs. Axial positioning was accomplished by pushing the strips into the coolant channels until their ends were at the same elevation as the bottom of the lower flow vanes.

With the reactor at operating temperature and pressure, data were obtained from long flux-wire irradiations and miniature ion-chamber traverses. The flux wires were fastened to stainless-steel carrier wires and inserted into six $5/32$ -in.-OD $\times$ 0.020-in.-wall stainless-steel pressure thimbles, and the miniature ion chamber was inserted into five $3/8$ -in.-OD $\times$ 0.022-in.-wall Zircaloy-2 pressure thimbles located in place of flow rods. The location of these thimbles is shown in Fig. 20. Both flux-wire thimbles and ion-chamber thimbles extended through the reactor top shield. This permitted manual insertion and withdrawal of the measuring device during power operation.

The induced radioactivity in the flux wires was gamma-counted by means of sodium iodide scintillation crystals and standard associated electronics as shown in the block diagram of Fig. 37. Count normalization

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Fig. 37. Block Diagram of Flux-wire Counting System

for data comparison was accomplished by irradiating a monitor of each wire material being used at the same axial and radial position in each irradiation. By counting to a preset number, using these monitor wires, automatic normalization and decay correction were provided.

The wire segments were counted in a standard lead-pig arrangement, and the long wires were counted in a linear flux-wire scanner. The long wires were fastened in a groove in an aluminum rod and pulled under a vertical, collimated scintillation head. Counts were made at 1-in. intervals.

A schematic diagram of the dual-purpose, miniature, ion-chamber circuit is shown in Fig. 38. The unit consisted of a detector capsule, a cable, and a readout system. The detector capsule was 0.25 in. in diameter and 3 in. long, and contained two subminiature fission chambers. Each fission chamber was 0.090 in. in diameter and 0.421 in. long. The neutron sensitivity of each fission chamber was provided by an enriched-uranium-alloy sleeve that was pressed into the chamber shell to form the outer electrode. One fission chamber was completely shielded by cadmium to make it nearly insensitive to neutrons of energies less than 0.6 eV; the second chamber was unshielded. An applied emf of 100 V d.c. assured saturation.

ID-103-A3436

Fig. 38. Schematic Diagram of Miniature Fission-type Ionization Chamber

The cable was a guarded, twin-axial type, with Type 304 stainless-steel conductors and high-purity aluminum oxide insulation. The insulation resistance between cable conductors was greater than 10^{10} ohms at 70°F. Other operating characteristics of the unit were:

Neutron sensitivity (thermal): 10^{-18} Amp/nv
Gamma sensitivity: 8×10^{-15} Amp/R/hr,
based on a Co^{60} source.

The unit was designed to operate at a maximum temperature of 650°F and a maximum pressure of 2000 psi.

The readout system consisted of two $100\text{ k}\Omega \pm 1\%$ resistors and two Keithley Model 151R microvoltmeters. The voltage drop produced by the ionization current flowing through the series resistor was measured by the microvoltmeters with a sensitivity of $1 \pm 0.1 \times 10^{13}\text{ nv/V}$.

2. Radial Neutron-flux Distributions

Radial flux measurements were made to determine the effect of control-rod configuration on the power split between the boiler and superheater regions. Figures 39 and 40 show plots, obtained from uranium-aluminum flux-wire activation, of the radial flux shapes measured in transverse and diagonal directions, respectively. The data were obtained 6 in. above the bottom of the fuel, at room temperature and zero power, with no boric acid in the system. It is apparent that the power split between the boiler and superheater regions could be at least partially controlled by control-rod configuration.

ID-103-A3437

Fig. 39. Transverse Radial Neutron-flux Distribution, Core PSH-1

ID-103-A3438

Fig. 40. Diagonal Radial Neutron-flux Distributions, Core PSH-1

3. Axial Neutron-flux Distributions

Axial neutron-flux distribution measurements were made in the PSH-1 core at zero power, 5, 10, 15, and 20 MWt. From these measurements, axial neutron-flux shapes and axial peak-to-average ratios were obtained.

The zero-power data are shown in Figs. 41, 42, 43, and 44. Comparison of the curves for flux-wire locations W-1, W-4, and W-6 of Figs. 41 and 43 shows the effect of increasing reactor temperature on axial flux shape; comparison of Figs. 43 and 44 shows the effect of draining the superheater coolant channels; and comparison of Figs. 42 and 43 shows the change in the flux shape due to the addition of boric acid to the system. Note that in Figs. 42, 43, and 44, the values from W-5 are greater than those from W-6. This was believed to be due to the boron-stainless steel poison rods in core position 44. The maximum axial experimental values for each curve, compared with results obtained from radial PDQ-calculated flux at the same points, showed excellent agreement.³

Axial neutron-flux traverses at powers of 5, 10, 15, and 20 MWt, measured with Al-1 w/o Cu wire at 600 psig and 489°F, are shown in Figs. 45, 46, 47, and 48. The data in Figs. 46-48 were taken with control rods withdrawn while building up xenon. The wires were counted on the linear traversing equipment, as discussed in Section IV-A-1. At these powers, the activation of the flux wires in location W-6 was greater than the activation in W-5. This reversal could be because the wire holders were made of aluminum for the zero-power irradiations and of stainless steel for the power irradiations. The stainless steel would absorb a significant portion of the thermal flux, resulting in considerable epithermal activation of the copper. PDQ calculations indicated that, without stainless-steel thimbles, the neutron-flux level at location W-5 was greater than the level at W-6.

Figures 49 and 50 show axial neutron-flux distributions for power levels of 0.5, 5, 10, 15, and 20 MWt in positions W-3 and W-5, respectively. Note that the axial position of the peak activation shifted downward with increasing power level in both figures, but the shift was much less in position W-3 (boiler-superheater interface) than in W-5 (boiler region). Figure 51 shows the data presented in Fig. 50, but plotted on a point-to-average basis.

Neutron-flux measurements made with the miniature ionization chambers are shown in Figs. 52 through 55. The data are plotted from the output voltage, which was proportional to neutron-flux level, vs distance from the bottom of the fuel. Power level, rather than core position, is used as the parameter in presenting the miniature ion-chamber data. The data in Figs. 52 and 54 are incomplete because of leaks which developed in the pressure thimbles after a reactor vessel head removal. Because the miniature ion chamber could not be fully inserted, no data were obtained from thimble C-3.

ID-103-A3439

Fig. 41. Axial Neutron-flux Distributions, Zero Power, Room Temperature, Core PSH-1

ID-103-A3499

Fig. 42. Axial Neutron-flux Distributions, Zero Power, Operating Temperature, Core PSH-1A

ID-103-A3440

Fig. 43. Axial Neutron-flux Distributions, Zero Power, Operating Temperature, Superheater Drained, Core PSH-1A

ID-103-A3500

Fig. 44. Axial Neutron-flux Distributions, Zero Power, Operating Temperature, Superheated Flooded, Core PSH-1A

ID-103-A3441

Fig. 45. Axial Neutron-flux Distributions at 5 MWt, Core PSH-1A

ID-103-A3442

Fig. 46. Axial Neutron-flux Distributions at 10.3 MWt, Core PSH-1A

ID-103-A3443

Fig. 47. Axial Neutron-flux Distributions at 15 MWt, Core PSH-1A

ID-103-A3444

Fig. 48. Axial Neutron-flux Distributions at 20 MWt, Core PSH-1A

ID-103-A3445

Fig. 49. Axial Neutron-flux Distributions at Various Power Levels, Core Position 41, Core PSH-1A

ID-103-A3415

Fig. 50. Axial Neutron-flux Distributions at Various Power Levels,
Core Position 43, Core PSH-1A

ID-103-A3446

Fig. 51. Point-to-average Axial Neutron-flux Distributions at Various Power Levels, Core Position 43, Core PSH-1A

ID-103-A3447

Fig. 52. Axial Neutron-flux Distributions at Various Power Levels, Using Miniature Ion Chamber, Thimble C-1, Cores PSH-1A and C

ID-103-A3448

Fig. 53. Axial Neutron-flux Distributions at Various Power Levels, Using Miniature Ion Chamber, Thimble C-2, Cores PSH-1A and C

ID-103-A3449

Fig. 54. Axial Neutron-flux Distributions at Various Power Levels, Using Miniature Ion Chamber, Thimble C-4, Cores PSH-1A and C

ID-103-A3450

Fig. 55. Axial Neutron-flux Distributions at Various Power Levels, Using Miniature Ion Chamber, Thimble C-5, Cores PSH-1A and C

Using the miniature ion-chamber sensitivity reported in Section IV-A-1 and the data obtained from Fig. 54, a maximum flux level measured with U^{235} in thimble C-4 (18.5 MWt) of $2.0 \pm 0.2 \times 10^{13}$ n/cm²/sec was obtained. This value was not corrected for the neutron-flux depression due to the void caused by the thimble, or the local depression caused by the poison in the miniature ion chamber. However, it compared favorably with the value of 2.97×10^{13} n/cm²/sec obtained from PDQ cell calculations.³

Figure 56 shows a comparison of data obtained from the bare ion chamber at a power level of about 4.5 MWt with data obtained from an irradiation of Al-1 w/o Cu flux wires at 5 MWt. The two sets of ion-chamber data were normalized at a point 10 in. above the bottom of the fuel on flux wire W-5. The shapes of the two sets of data compare well.

ID-103-A3451

Fig. 56. Axial Neutron-flux Distributions, Comparison of Data from Flux Wires and Miniature Ion Chambers, Core PSH-1A

4. Cadmium Ratio Measurements

Cadmium ratio measurements were made in the peripheral superheater core to obtain an indication of the neutron-flux spectrum at various core locations and to continue investigation into the use of cadmium ratios for in-core void determinations.

The results of an irradiation of both bare and cadmium-(0.020-in. thick) covered uranium-aluminum flux wires are shown in Fig. 57 for room-temperature, zero-power conditions. Bare and cadmium-covered uranium-aluminum wires 3/8 in. long were irradiated, in the positions shown, in two separate irradiations. Normalization between the two irradiations was by monitor wires, as discussed in Section IV-A-1. The results indicate a much harder neutron spectrum in the superheater region than in the boiler region, and a certain degree of coupling between the two regions. A more even distribution of flow rods in the boiler region in the peripheral superheater core resulted in a more even radial cadmium ratio distribution than was measured in the central superheater core.¹

ID-103-A3413

Fig. 57. Cadmium Ratios at Zero Power, Core PSH-1A

Figures 58 through 61 show the axial variation in the cadmium ratio in the pressure thimbles for power levels of 5, 10, 15, and 20 MWt. In these figures, cadmium ratios from miniature ion chambers can be compared with cadmium ratios from flux wires. Both sets of data show similar trends, with a much smaller axial variation than was anticipated. The data indicate that approximately the same degree of thermalization occurred throughout an axial traverse of the core; however, the value of the cadmium ratio did decrease with increasing power level.

B. Fine Power Distributions

The original physics and heat-transfer calculations for the peripheral superheater core were based on a boiling fuel assembly that contained 49 fuel rods and no flow rods. To determine the fine power distribution, a determination desirable from the standpoint of heat transfer, measurements were necessary in the boiling fuel assemblies with flow rods and in the superheater fuel assemblies. Because the techniques used in the measurements blocked the flow passages and utilized materials that were temperature-limited, the fine power-distribution measurements could be made only at zero-power, room-temperature conditions.

1. Measurement Techniques

The fine radial power-distribution measurements in the boiler region were obtained by means of the equipment shown in Fig. 62. Three boiling-fuel-assembly boxes were modified by removing a replaceable section of the box side. A polyethylene block $3\frac{3}{4}$ in. sq x $\frac{1}{2}$ in. thick was prepared with forty-nine $\frac{3}{8}$ -in. holes on $\frac{1}{2}$ -in. centers so that fuel rods could pass through the block. Two holes, $\frac{1}{32}$ in. in diameter x $\frac{1}{4}$ in. deep, were drilled diagonally across, and as close as possible (about 10-15 mils apart) to, each fuel-rod hole. These small holes were used to position and hold securely the $\frac{3}{8}$ -in.-long x 0.031-in.-diam Al-7.78 w/o U or Zr-3.379 w/o U (93 w/o U^{235}) flux wires. Each polyethylene tray was loaded with 74 flux wires, two each adjacent to each of the 37 fuel-rod holes. The flux-wire trays were positioned 6 in. above the bottom of the fuel.

The fine power-distribution measurements in the superheater fuel assemblies were made in a somewhat similar manner, using the equipment shown in Fig. 36. Teflon strips 0.050 in. thick were cut to the width of the steam coolant channels in the superheater fuel element, and a groove $\frac{1}{32}$ x $\frac{1}{32}$ in. was cut across the strip at a height corresponding to the location of the flux wires in the boiler region. Zirconium-uranium flux wires similar to those used in the boiler, but $\frac{1}{8}$ in. long, were then spaced evenly in the groove (five in each of the two edge coolant channels, and three in the middle one) so that an effective traverse parallel to the fuel plates could be obtained. Corresponding coolant channels in each of the four fuel elements in one fuel assembly were so equipped in each irradiation, together with the three edge, or center, channels in the fifth element, that a total of 15 Teflon strips were inserted into an assembly for one irradiation.

ID-103-A3414

Fig. 58. Cadmium-ratio Distributions at 5 MWt, Cores PSH-1A and C

ID-103-A3452

Fig. 59. Cadmium-ratio Distributions at 10 MWt, Cores PSH-1A and C

ID-103-A3453

Fig. 60. Cadmium-ratio Distributions at 15 MWt, Cores PSH-1A and C

Fig. 61. Cadmium-ratio Distributions at 20 MWt, Cores PSH-1A and C

ID-103-3668

Fig. 62. Modified Boiling Fuel Assembly for Fine-flux Mapping

15,000) to be obtained on the monitor wire. The results were thus automatically decay-corrected. The wires from each day's irradiation in one fuel assembly were counted using the same counting channel, and each wire was counted twice.

2. Results

Data from the three irradiations are presented in Figs. 63 through 65. The boiler zone results are shown in Fig. 63; the results for superheater core position 58 are shown in Fig. 64, and for superheater core position 68 in Fig. 65.

Measurements were made in one octant of the core, assuming that symmetry existed. Since there were only three modified boiling-fuel-assembly boxes, three separate irradiations were required to cover the seven boiling-fuel-assembly core positions contained in one octant. Three boiling fuel assemblies and two superheater fuel assemblies contained flux wires in each irradiation; the same superheater core positions were used each time, but the choice of superheater coolant channels varied. The results of the three irradiations were normalized through the use of Zr-U and Al-U monitor wires located in the same positions in the core on each irradiation run.

After each day's irradiation, the wires were counted in the equipment shown in Fig. 37. The gamma radioactivity induced in the flux wires was measured with sodium iodide scintillation crystals and recorded on scalers. Three wires (the monitor wire and two others) were counted simultaneously by using three counting heads, and the counting time was determined by the time required for a preset number of counts (e.g.,

ID-103-A3455

Fig. 63. Normalized Boiler-region Fine-flux Map, Core PSH-1

NOTES: ROOM TEMPERATURE
 SUPERHEATER DRAINED
 NO BORIC ACID
 ZERO POWER
 NUMBERS ARE RELATIVE POWER

ID-103-A3192

Fig. 64. Normalized Fine-flux Map, Superheater Fuel Assembly in Core Position 58, Core PSH-1

NOTES: ROOM TEMPERATURE
 SUPERHEATER DRAINED
 NO BORIC ACID
 ZERO POWER
 NUMBERS ARE RELATIVE POWER

ID-103-A3191

Fig. 65. Normalized Fine-flux Map, Superheater Fuel Assembly in Core Position 68, Core PSH-1

The relative power numbers given in the three figures were obtained in the following manner:

a. Measurements in Boiling-fuel-rod Locations

The counts for the two wires adjacent to each fuel rod were averaged, corrected for varying flux-wire weights and material (all wire weights were adjusted to 100 mg of aluminum-uranium wire), corrected for the difference between the two counting channels, and normalized to a preset count of 15,000 on a standard monitor to give a relative power in that rod.

In certain fuel assemblies, one or more fuel rods were replaced with boron-stainless steel poison rods to insure that the reactor could not become critical on the withdrawal of any single control rod alone. Flux wires were placed next to these poison rods, also, and the number obtained, representing relative power, has been placed in these poison-rod locations in the figures. Similarly, for core positions 55, 56, and 57, flux wires were placed adjacent to the 12 flow rods, and corresponding numbers appear in the flow-rod locations. From these numbers, it is possible to observe the fine variation in power within a boiling assembly as well as the effect of the flow rods on flux level.

b. Average Relative Power in Boiling Fuel Assemblies

In the various figures, the number in the upper right corner of the boiling fuel assemblies represents an average of all the relative power numbers appearing within the fuel rods in an assembly, and as such is the relative power in an "average" rod in the assembly. The local-to-average power distribution is thus readily obtainable.

c. Measurements in Superheater Coolant Channels

The two counts for each superheater flux wire were averaged, normalized to 100 mg of aluminum-uranium wire, corrected for the difference between the two counting channels, and normalized to a preset count of 15,000 on a standard monitor to give the relative power distributions shown in Figs. 64 and 65.

Superheater coolant channel local-to-average flux ratios were obtained in the direction parallel to the fuel plates for core positions 58 and 68. The profiles for 12 coolant channels in each of the two superheater fuel assemblies were averaged, and the results are shown in Fig. 66. The two curves are similarly shaped but slightly different. The flux was highest in the region of the superheater adjacent to the boiler.

ID-103-A3456

Fig. 66. Average Fission Profiles Parallel to Fuel Plates in Peripheral Superheater Fuel Assembly, Core PSH-1

3. Analysis

In an earlier exponential experiment with BORAX-V boiling fuel rods, Kirn and Hagen⁹ found that the average fission rate across a boiling fuel rod was 91% of the edge fission rate. As discussed in ANL-6961,¹ detectors in the outer coolant channels of superheater fuel elements measured the average fission rate of all the fuel in the element. To determine the relative power production in a fuel assembly, the fuel loading as well as the fission flux had to be considered. For a boiler fuel assembly containing 37 fuel rods, each rod with 16.3 g of U^{235} , a normalized power production term was given by $(37)(16.3)(0.91) = 549$ (averaged normalized flux-wire activity in the assembly). Replacing some fuel rods with poison rods reduced the assembly-normalized power-production term proportionately. A superheater fuel assembly power-production term was arrived at similarly. Since an average flux-wire activity had been obtained for each superheater coolant channel, the average of the flux-wire activities in the ten outside coolant channels in an assembly was readily obtainable, and this was the normalized average fission flux for the assembly. Since each superheater fuel assembly contained 680 g of U^{235} , a normalized power production term was given by (680) (average flux measured by fissions in the assembly). Table VI indicates the procedure used in determining the power produced in the various core positions and the superheater region. The local/average power ratios for the individual fuel assemblies are shown in Fig. 67 for both peripheral and central superheater cores. At room temperature, the power split between the boiler and superheater regions is such that 16.7% of the total core power is produced in the peripheral superheater and 17% in the central superheater, compared with the design point of 19% for a power of 20 MWt.

The presence of the superheater on the periphery in the PSH-1 core, with the resultant smaller central boiler region, increased the radial peak-to-average ratio of power production to 1.68 as compared to 1.37 for the CSH core.

TABLE VI. Individual/Average Fuel Assembly Power Production, Core PSH-1

Core Position	Average Relative Power in Assembly	Number of Fuel Rods per Assembly	Normalized Power Production in Assembly	Number of Symmetrical Assemblies in Core	Normalized Power Production in Symmetrical Assemblies	Individual/Average Power Production
55	353	33	17,900	4	71,800	1.68
56	314	37	17,200	8	137,600	1.61
57	240	37	13,200	8	105,600	1.24
58	102		6,900	8	55,200	0.65
66	284	34	14,300	4	57,200	1.34
67	199	36	10,700	8	85,600	1.00
68	95		6,400	8	51,200	0.60
77	149	36	8,000	4	32,000	0.75
78	100	37	5,500	8	44,000	0.51

Central Superheater Core

Notes:

Room Temperature
Superheater Drained
Boric Acid Concentration:
10.8 g(H_3BO_3)/gal(H_2O)

Legend:

- Poison Rods
- ⊗ Flow Rods

Peripheral Superheater Core

Notes:

Room Temperature
Superheater Drained
No Boric Acid in Core

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Fig. 67. Local-to-core Average Ratio of Power Produced in Individual Fuel Assemblies, Superheater Cores

V. PLANT PERFORMANCE AND OPERATING EXPERIENCE

A. General Performance

Preparations for operation of the core with peripheral superheater included unloading the reactor fuel used during operation with the central superheater core. The control rods were removed, inspected, and repaired, and the central-superheater core structure was replaced with the peripheral-superheater core structure. The control rods were then reinstalled and the control-rod system tested.

The loading of fuel into the peripheral superheater core and most of the experiments at zero power are reported in Sections III and IV. To save time, a minimum number of zero-power experiments were performed at 600 psig, 489°F operating conditions. Since all in-core instrumentation for power operation was installed before these experiments, it was not necessary to remove the reactor vessel head after these experiments. Figure 20 is a diagram, and Fig. 68 a photograph, of the power loading for core PSH-1A.

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Fig. 68. Power Loading, Core PSH-1A

Power operation of peripheral superheater core PSH-1A began on May 18 and continued through August 14, 1964. The approach to maximum power is summarized in Table VII. The effect of the reactor-water-demineralizer flow on coolant subcooling temperature and maximum power

level is illustrated in the steam-to-atmosphere experiments. In these experiments, the power was raised rapidly to maximum so that xenon buildup in the core was negligible. The maximum power of 22.4 MWt was achieved late in the program on core PSH-1C with seven of the original 40 boron-stainless steel poison rods removed.

TABLE VII. Approach to Full Power, Core PSH-1

Power, MWt	Nine-control- rod Bank Position, in.	Feed- water Temp, °F	Superheater Steam Temp, Entrance-to- second Pass, °F	Superheater Exit Steam Temp, °F	Steam Line Temp, °F	Exit Boiler Void Fraction (Core Position 56), v/o	Coolant Subcooling, °F
<u>Core PSH-1A</u>							
<u>Steam-to-turbine-condenser Experiments</u>							
2.8	15.89	95	592	690	572	28	1.0
4.8	17.00	87	497	760	660	37	2.0
7.6	18.12	109	635	777	704	42	2.5
10.0	19.30	115	641	795	720	49	3.5
12.2	20.39	122	640	790	722	54	4.0
14.3	21.66	116	652	812	727	56	4.5
18.4	25.00	120	650	810	720	63	6.5
<u>Steam-to-atmosphere Experiments</u>							
19.3	25.00	78	673	840	760	65	7.0
21.4 ^a	25.00	76	693	895	812	66	10.0
<u>Core PSH-1C</u>							
22.4 ^a	25.00	82	695	895	875	-	-

^a40 gpm of reactor water flowing through reactor-water-demineralizer (DM-1) heat exchangers.
Returned to feedwater tank.

The total integrated power accumulated by the peripheral-superheater core was 266 MWd. The superheater was flooded and drained 31 times, and 15 thermal cycles, raising the temperature from below 200°F to the operating temperature of 490°F and returning to below 200°F, were performed during this period. Figure 69 shows the times at which thermal cycling and flooding and draining of the superheater were accomplished. Figure 70 shows the buildup of integrated power. Table VIII contains data obtained during power operation of core PSH-1, such as power level, temperatures, power density, reactivity effects, etc., and for comparison, the initial design objectives for both superheater cores.

During the last weeks of power operation, experiments were conducted with prepared defective fuel samples.

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Fig. 69. Reactor Temperature vs Time, Cores PSH-1A, B, and C

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Fig. 70. Integrated Thermal Power, Cores PSH-1A, B, and C

TABLE VIII. Basic Design and Experimental Parameters, Superheater Cores

	Design Objectives		Experimental Results, PSH
	CSH	PSH	
Total Power, MWt	20	20	22.4
Power Fraction in Superheater at Full Power, %	19	19	18.5
Pressure, psig	600	600	600
Moderator and Boiler Coolant Temp, °F	489	489	489
Superheater Exit Steam Temp, °F	850	850	895
Maximum Boiling-fuel-rod Center Temp, °F	3,100	3,050	2,420
Maximum Superheater-fuel-plate Temp, °F	1,130	1,080	940 ^a
Average Heat Flux, Btu/hr-ft ²			
Boiler	123,000	174,000	184,000
Superheater	55,000	41,200	43,600
Power Density, kW/liter			
Core Average	42.5	42.5	47.6
Boiler	43.5	47.0	52.8
Superheater	41.5	30.2	33.0

^aThermocouple-caused perturbation estimated to be about -100°F

B. Reactor Instrumentation and Control

1. Nuclear Instruments

The neutron-flux detectors were mounted in the same positions for pure boiling, central superheat, and peripheral superheat operation. Figure 71 shows the positions of the ion chambers relative to the reactor core. The elevation of the linear neutron flux and safety (power level trip) circuit chambers was 7 in. above the core horizontal midplane. Not shown on this figure are the log neutron flux/period circuit detectors, which were mounted in similar thimbles 7 in. below core midplane but against the core end of the instrument holes. The startup counters were located on the core midplane.

The ion chambers used for linear, high-flux safety, and log-period circuits were ANL Model IC-15B, 4-in.-diam, parallel-plate, B¹⁰-coated, gamma-compensated ion chambers, having a rated sensitivity of 4×10^{-4} Amp/nv. The startup circuit detectors were Reuter-Stokes Type RSN-44A, BF₃ proportional counters, having a sensitivity of 40 counts/neutron/cm². The detectors were adjusted in their shield-hole positions to provide satisfactory counting rates.

During fuel loading operations, fission counters were connected to the startup counting circuits and the counters were mounted in water-tight thimbles adjacent to the reactor core inside the pressure vessel. These Westinghouse Type WL6376 counters had a sensitivity of 0.7 count/sec/neutron/cm².

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Fig. 71. Location of Some Nuclear Operating Instruments, Cores PSH-1A, B, and C

As reported in ANL-6849,⁷ the current from each ionization chamber for a given reactor power level when the reactor was at room temperature was one-third of that with the reactor at operating temperature of 489°F. Figure 72 shows that for core PSH-1 the deviation from linearity increased as the reactor power level was increased; that is, the increase in current per megawatt of power level increase was greater for higher powers. The line on the graph is a linear extrapolation from the low-power calibration by means of electric preheaters. This calibration was accomplished by comparing the linear neutron-flux measurement of reactor power level required to just maintain the reactor at 600 psig, with the electric heater capacity required to maintain the reactor at 600 psig. At higher powers, the power was computed from feedwater flow, considering the temperatures of feedwater and steam.

ID-103-A3461

Fig. 72. Reactor Power vs Linear-neutron-flux-circuit Current, Core PSH-1A

The current on the linear-flux circuit was about the same at 10 MWt for both central and peripheral superheat power operation. For boiling core B-2 power operation, the current was 1.7 times as high. In the peripheral superheater core, as the outside control rods were lowered to 16 in., with the remaining control rods fully withdrawn, the neutron flux at the ion chambers was reduced by 5.7%, as compared with the condition when the reactor was operating at the same power level with all control rods banked at 21.2 in.

As the flow rate through the reactor-water-demineralizer (DM-1) was increased during operation at a constant power level of about 10 MWt, the neutron-flux level at the chambers was decreased. For a flow rate through the demineralizer of 36 gpm, the neutron flux impinging on the ion chambers was reduced by 8.8% compared with zero flow through the demineralizer. This reduction was believed to be due to the reduced voids in the boiler region because of increased feedwater flow made necessary by the use of the demineralizer.

Two reactor scram devices for high-steam-line activity were added following operation with the central superheater core to provide rapid reactor shutdown in the event of gross fuel failure during power operation. Each circuit consisted of a one-liter, ANL Model IC-33, gamma-sensitive ion chamber mounted directly on the steam line and connected to a conventional linear amplifier and trip circuit. These chambers had a sensitivity of 10^{-10} Amp/R/hr. The operation of these devices was tested by means of a portable gamma source. There were no reactor shutdowns caused by high-steam-line gamma-activity levels.

All the nuclear instrument scram circuits were tested for speed of response. An electronic step-function generator was used as the input signal in place of the neutron-flux-detecting chambers. This step-function generator could be adjusted to produce current increases with various e-folding times, or periods. A light-beam galvanometer-type oscillograph recorded the elapsed times, the current signal to the safety circuits, and voltage changes due to relay or limit switch operation. The measured times for operation of the nuclear instruments are shown in Table IX. The safety circuit times were the intervals from the time that the input current to the circuit amplifier reached the trip value to the time of operation of the output relay in that amplifier. For the period circuits, the time interval was from initiation of the input current at the given period to the operation of the output relay. The maximum time for scram-relay operation and control-rod latch release was 0.105 sec. Therefore, the total time for initiation of reactor shutdown in the case of Safety Circuit No. 2 was $0.021 + 0.105 = 0.126$ sec.

TABLE IX. Nuclear Instrument Operating Times, Core PSH-1

Instrument	Input Signal	Operating Time, sec	
		Run No. 1	Run No. 2
Safety Circuit No. 1	Step	0.031	0.032
Safety Circuit No. 2	Step	0.020	0.021
Period Circuit No. 1	0.1-sec period	0.510	0.510
	1.0-sec period	0.665	0.650
	5.0-sec period	1.934	1.850
Period Circuit No. 2	0.1-sec period	0.450	0.450
	1.0-sec period	0.600	0.600
	5.0-sec period	1.309	1.309

2. Reactor Control Rods

Control-rod and control-rod-drive mechanism performance throughout the peripheral superheater operation was within the prescribed operating limits. Control-rod drop times for a 20-in. stroke, measured biweekly, were less than 1 sec when the reactor pressure was at atmospheric conditions, and less than 1/2 sec when the reactor was at the operating pressure of 600 psig.

Prior to loading of the reactor, one of the control rods did not meet the requirement for drop time at atmospheric pressure. The floating ring labyrinth seal and extension shaft for this rod were disassembled and reinstalled without discovering any defect. Operation was satisfactory thereafter, and the difficulty was presumed to have been a slight misalignment on installation. Although they had been operating satisfactorily, the scram-latch assemblies on all rod drives were inspected at this time. The inspection revealed broken bearings on the scram-latch rollers on control-rod drives Nos. 6 and 7 and a cracked roller retainer plate on drive No. 1. Repairs were accomplished by installing new parts.

Following the final shutdown from power operation, the drop time on the central control rod exceeded the prescribed maximum, but after the control-rod full stroke was cycled three times, the operation was satisfactory. It is believed that high temperatures on the rubber O-ring secondary seal caused hardening of the rubber. The high temperatures resulted when the seal water that normally cools the control-rod seals was shut off during a 3-day reactor shutdown period after extended power operation. Residual heat from the reactor caused the increase in temperature.

Because the reactor operation was terminated, the seal was not disassembled, and no further inspection of control rods was made.

C. Reactor Vessel

1. Radiation Effects

Because of the early termination of the BORAX-V experimental program, the reactor vessel absorbed only a small amount of radiation. For this reason, the several fast-neutron flux-wire monitors in the thimble attached to the interior of the reactor vessel wall and the reactor-vessel-material sample specimens irradiated on the vessel wall were not removed for examination.

2. Temperature Distribution

Continued satisfactory temperature distributions were observed on the outer reactor vessel wall throughout power operation of the peripheral superheater core. The maximum allowable temperature rate of change of 200°F/hr was adhered to during all startup and shutdown operations.

3. Reactor-vessel-head Removal and Replacement

The reactor vessel head was installed and removed twice during peripheral superheater core operation. The two concentric head gaskets, of spiral-wound, plated, cold-rolled steel with Teflon filler, were installed in a satisfactory manner. On each occasion, the 40 reactor-vessel-head stud bolts were strained 0.016 in. to provide a stress of 33,000 lb/in.² The reactor vessel and associated piping systems were hydrostatically leak-tested at 900 lb/in.² following each head installation. Figure 73 is a photograph of the reactor vessel head installed with in-core instrument-lead terminal boxes, flux-wire and miniature ion-chamber thimbles, steam sampling probe, oscillator, traversing mechanism, etc.

Before the second head installation, one corner of the internal southwest core-hold-down gusset in the head was removed. This was necessary to remove an interference with one of the flexible superheated-steam outlet hoses and to allow installation of the steam sampling probe on an adjacent head nozzle.

4. Belleville Spring²

Following installation of the peripheral superheater core structure in the reactor vessel, 1/8-in. shims were installed on top of each of the core structure's eight struts to achieve the correct Belleville spring deflection. A test fit of the vessel head at room temperature compressed the Belleville spring an average of 0.135 in., which applied a core structure hold-down load of 11,300 lb.

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Fig. 73. Reactor-vessel-head Installation,
Core PSH-1A

After the first period of power operation, the reactor vessel head was removed and the Belleville spring deflection-jig measurements were taken. These indicated a hold-down load of 14,800 lb had been applied at operating temperature to the peripheral superheater core structure as a result of an average Belleville spring deflection of 0.178 in. It was also noted that shortening of the feedwater sparger hold-down bracket had allowed the core structure struts to deflect uniformly. This resulted in a uniform deflection of the Belleville spring, thus alleviating the problem experienced during the core CSH-1 experiments.¹

D. Water and Steam Systems

1. Steam Systems

With the exception of a few minor packing-gland and gasket leaks, the steam system performed in a satisfactory manner. The main

steam-back-pressure control valve continued to leak considerable quantities of steam to the turbine during startup operations. Because of its large size, this valve also caused pressure surges back to the reactor core while regulating steam flow at very low power levels. The resulting reactivity surges occasionally caused a period-circuit scram.

The steam desuperheating equipment continued to be a problem. Because there was little control at low power levels, a smaller, low-flow, linear-contoured plug and seat was installed in the feedwater control valve for the desuperheater. The low-power problem was solved, but feedwater flow was insufficient at higher power levels. Performance improved, following enlargement of the hole in the valve seat. Operation of the automatic control for the valve supplying feedwater to the desuperheater also gave considerable difficulty. The difficulty was found to be excessive vibration in the valve operator, causing a change in the calibration for the electro-pneumatic transducer. During the final week of power operation, satisfactory performance of the desuperheater control system was obtained in both the manual and automatic modes.

2. Boron-addition and Batch-feed Systems

No need arose for dumping the boric acid solution to the reactor, but the boron-addition system remained operative and available for use throughout the peripheral-superheater experiments. Maintaining the boric acid tank at atmospheric pressure and at about 200°F alleviated problems of failure previously experienced with the packing glands, gauge glasses, and gaskets. A minimum of 44 kg of boric acid was maintained in the 200 gal of water in the boron-addition tank. This amount would produce a concentration of 15.65 g (H_3BO_3)/gal (H_2O) in the reactor. At this concentration, the reactor was subcritical at room temperature with all control rods fully withdrawn.

The batch-emergency-feedwater tank was operated once during the period of this report. It performed satisfactorily, but required approximately 45 min for heating and dumping a 150-gal batch of water into the reactor vessel. Attempts to increase the rate of emergency feedwater flow resulted in excessive steam-water hammer in the feedwater piping.

3. Turbogenerator System

The BORAX-V turbine plant continued to provide the heat sink necessary for dissipation of reactor power. Several problems were encountered in this system during experiments with core PSH-1. Improper drainage from the air-ejector exhaust-system piping resulted in wetting of both the absolute and charcoal filters. Tests performed on the piping between filters indicated that the vacuum induced by the dilution air blower was several inches of water greater than anticipated. Insufficient head had

been allowed for draining condensate through the loop seal at this point. Repiping of the drain alleviated the problem. A new charcoal filter cartridge was installed.

Drastically reduced vacuum in the main condenser and increased air-ejector exhaust flow led to the discovery of a major leak in the stainless-steel bellows expansion joint on the steam bypass line close to the condenser. The leak was repaired by welding, and an improved condenser vacuum of about 21 in. Hg was achieved during the remainder of the power operation.

4. Feedwater System

The feedwater valves and piping system performed satisfactorily throughout power operation. Some difficulty was experienced with the automatic water-level control system. Erratic operation was caused by a poor electrical contact in the retransmitting slide wire from the reactor water-level recorder. The wetting of the superheater, described in Section VI-E, was caused by the sticking of this water-level recorder. The balancing motor was replaced, and no further difficulty of this type was experienced.

A thorough evaluation of performance of the three-element reactor-water-level control system was not possible because of limited operating time. However, the control parameters were adjusted so that the reactor water level was maintained within 1 in. of the desired set point when the reactor power level was changed by 50%. This power level change from 12 to 6 MWt was accomplished in 1 min by lowering control rods at the maximum rate of motion (6 in./min).

5. Auxiliary Water System

Performance of the auxiliary water system continued to be excellent. Investigation with the seal supplier of the reasons for mechanical-seal failures on the auxiliary water pump during operation of core CSH-1¹ proved fruitless. The packing gland on the pump performed well, and no repacking was necessary during the entire core PSH-1 program.

6. Superheater Flood-and-drain System

The flood-and-drain extension pipe was installed with the peripheral superheater core structure, and the D.S.D. coupling* was leak-tested satisfactorily. Figure 2 is a bottom view of the core structure. The external superheater flood-and-drain system was welded to the extension pipe. The system still retained the low-rate flood valve, flow orifices, and regular flood valves utilized during the core CSH-1 program. Because of

*D.S.D. Manufacturing Company, Hamden, Connecticut.

the shortened power program on core PSH-1, with minimum burnup and decay heat, it was never necessary to use the low-rate flood valve. All flooding of the superheater was performed with the regular solenoid flood valve immediately after insertion of the control rods. About 1 min, 30 sec was required from the opening of the valve for the water to reach the bottom of the superheater fuel plates, and another 30 sec for the water to cover the fuel plates.

During startup operations, the superheater was drained by holding the superheater drain control valve open for 3 min, although actual draining was completed in 2 min. The manual superheater drain valve in this system was routinely left open during power operation to allow the slight leakage through the control valve to go to the condenser and insure that the superheater remained drained.

7. Superheater-steam-outlet manifolds and Couplings

Installation of the four flexible-metal-hose, superheater-steam-outlet manifolds required for peripheral superheater operation was accomplished with no difficulty. The manifolds were coupled to the reactor vessel internal nozzles with new, hollow, metal O-ring seals by using remotely operated special extension socket tools. The other end of the manifolds was attached to the tops of the eight outlet superheater assemblies, as shown in Fig. 68. Several of the manifolds, which had a slight interference with the reactor-vessel-head internal hold-down gussets, were pulled to one side and fastened to alleviate the interference.

Removal of one of the manifolds for fission-product deposition analysis following PSH-1 power operation was routinely accomplished, since radiation levels were low. (See Section X-B-6.) The manifolds were all in excellent condition, and three were left attached to nozzles in the reactor vessel for storage.

E. Fuel

1. Boiling Fuel Rods

The integrity of all boiling fuel rods continued to be satisfactory. Considerable manipulation of individual rods while making core-loading changes indicated that no significant swelling, bulging, etc. of fuel rods had taken place. There was no evidence of boiling fuel failure of any type.

2. Boiling-fuel-assembly Boxes

Following installation of A-nickel grid-holding rivets during initial BORAX-V operation,⁷ the boiling-fuel boxes performed as designed. One instrumented boiling-fuel-assembly box installed in the air-water test

loop for flowmeter calibration had bulged side plates, which required straightening before installation in the reactor vessel. The box had been exposed to higher flowrates and pressure drops than were experienced in the reactor vessel.

3. Superheater Fuel Assemblies

As a result of the central superheater fuel difficulties experienced during the operation of core CSH-1, all the peripheral superheater fuel assemblies were repaired and modified before installation in the reactor. Leak-repair fillet welds had also been made between insulating tubes and flow vanes on all the peripheral superheater fuel assemblies, and weld burn-through into the bottom of fuel plates was found.

The top riser and bottom nozzle were removed from each standard fuel assembly. To provide access to the top of the fuel, windows were cut in the sides of the top riser transition piece on the instrumented assemblies. All the burned-through leak-repair welds that tacked the fuel-plate-bottom dead ends to the insulating tubes were removed to permit the designed expansion of the fuel element inside the insulating tube. To further assure proper coolant channel size, all channels were gauged and three spacer combs were installed in the channels at the top and bottom of each four-plate fuel element. A welding fixture was then used to reweld the top riser and bottom nozzle on each assembly. The welds were dye-checked and the renovated fuel assemblies were straightened, dimensionally inspected, and leak-tested. Figure 74 is a photograph of a modified peripheral superheater fuel assembly with bottom nozzle detached.

The peripheral superheater fuel performed well, and there was no evidence of failure.

4. Fuel Handling and Storage

All fuel and core structure handling and storage were conducted on a routine basis with no problems encountered. Following removal of the radioactive central superheater core structure, the peripheral superheater core structure was installed in the reactor vessel. This was done by utilizing the remote, shielded, crane-operating station and crane-indexing system. The core structure flood-and-drain extension pipe was attached by remote means from behind temporary shielding slabs. Upon completion of the experiments with core PSH-1, the core structure with control rods was left in the reactor vessel as a storage facility for some of the peripheral superheater fuel assemblies.

Considerable manipulation of individual boiling fuel rods in the core, through reactor head nozzles as well as with the head removed, was routinely accomplished.

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Fig. 74. Modified Peripheral Superheater Fuel Assembly

5. Superheater Fuel Seals

The superheater fuel assembly hollow metal O-ring seals, shown in Fig. 74, performed very well during peripheral superheater core operation. Leakage rates of moderator water into the superheater were slightly reduced relative to central superheater core operation, averaging about 1 gal/hr at room temperature for the combined 16 peripheral superheater fuel assemblies. After completion of the room-temperature, zero-power experiments on the PSH-1 core, the used seals were replaced with new seals prior to power operation. This was successfully accomplished under shielding water by utilizing the sliding-door seal removal and installation jig.

F. Plant Radiation Levels

During all BORAX-V operation, the individual radiation exposure to operating personnel was kept well below the recommended level of 3 Rem/quarter. The average weekly operator radiation exposure during BORAX-V operation with the peripheral superheater core was approximately 25 mRem/week. Only on rare occasions during fuel handling did any operator receive in excess of 150 mRem/week.

The installed radioactivity monitoring system included continuous air monitors in both reactor and turbine buildings, area radiation monitors in pertinent locations, and the air-ejector-exhaust-stack monitor. In addition, film badges, dosimeters, hand and foot monitors, and portable radiation monitors were used.

1. Area Radiation Levels

Throughout the operation of the peripheral superheater core, radiation levels were within design limits of 7.5 mRem/hr on the main floor of the reactor and turbine buildings. Plant radiation levels during normal power operation are shown in Table X. During operation with prepared fuel defects, the levels of radiation increased as shown in Table XXVIII (p. 228).

TABLE X. Normal Radiation Levels during Power Operation,
Core PSH-1A

Power Level, MWt	Radiation Level, mR/hr			
	5	10	15	20
Main steam line, 50 ft from reactor ^a	20-30	50-90	85-100	100
Turbine building main floor, 300 ft from reactor ^b	1-3	7-18.5	14-27	35-43
Condenser hotwell	1-5	10-35	23-75	105-110
Air-ejector-exhaust absolute filter	1-5	4-28	28-48	27-110
Air-ejector-exhaust charcoal filter	1-5	5-22	12-31	17-155
Water line to reactor-water demineralizer	8-26	23-80	34-90	95-100

^aMonitor located about 5 ft from main steam pipe.

^bMonitor located about 10 ft from main steam pipe.

On August 12, 1964, a steam system valve bonnet gasket failed. To repair the steam leak, the reactor vessel was blown down to atmospheric pressure. A small amount of water was blown from the reactor vent stack on the west side of the reactor building. A survey of the immediate ground area showed a radiation level of 5 mR/hr at 2 in. Ground samples analyzed

by gamma-ray spectrometry indicated that the contaminant was Na^{24} . This was the only case of contamination detected outside the building during the core PSH-1 experiments.

2. Continuous Air Monitor

The only indication of an increase in airborne radioactivity greater than 1% MPC(40) for unknown beta-gamma emitters in the reactor building was on August 12, 1964. This increase was indicated on the continuous air monitor at approximately 2000 hr, at which time the reactor was shut down. The air sample was removed for laboratory counting and indicated an airborne concentration of 30% MPC(40) for unknown beta-gamma emitters after a 10-min decay. The airborne contamination was attributed to a steam leak in the west trench area near the monitor.

On occasions, a special charcoal filter on the air monitors was used for the collection of iodine samples. This was used in conjunction with a conventional filter paper acting as a prefilter in all cases. Analysis of the charcoal filter indicated the airborne activity to be always less than 1% MPC(40) for I^{131} .

Essentially the same air-monitoring program was conducted in the turbine building, with the air monitor located in close proximity to the turbine-generator. No radioactive concentration in excess of 2% MPC(40) was observed on either the conventional or charcoal filters.

3. Downwind Air Monitoring

A mobile air-sampling unit consisting of a trailer-mounted power supply and high-volume air samplers was used in the field. During power operations and particularly during steam-venting conditions, the unit was located in a downwind position from the air-ejector exhaust stack and steam vents at distances of from 100 ft to 500 yards. All samples from this unit showed airborne radioactive concentrations to be less than 1% MPC(40) for unknown emitters.

4. Air-ejector Exhaust-stack Monitor

To observe the radioactivity being exhausted to the atmosphere via the air-ejector exhaust system, a two-channel Tracerlab Model MAP-1/MGP-1 Continuous Air Particulate and Gas Monitoring system was used. The unit contained a beta-gamma GM detector, observing particulate collected on a filter paper, and a scintillation detector, observing a fixed volume of gas. The rated sensitivity of this unit was such that 25 counts/min above background was produced by a Co^{60} source of $8.0 \times 10^{-12} \mu\text{Ci/cc}$ for the particulate channel, and 250 counts/min above background was produced by an I^{131} source of $1.7 \times 10^{-6} \mu\text{Ci/cc}$ for the gas channel.

The air-ejector exhaust-gas flowrate was normally in the range of 5-10 ft³/min. Dilution air at 120-140 cfm was added to this gas, and the mixture passed through an absolute filter and a charcoal filter in series. The effluent from these filters was then sampled by the stack monitor at a flowrate of 5 ft³/min. Activity levels observed at the start of steam flow were as much as a factor of 10 above the normal level observed, perhaps due to sweeping out of collected gases and contamination in the steam system during startup.

The activity levels for power operation in the range of 10-12 MWt were from 1.4 to 5×10^4 counts/min for the gas monitor during operation without fuel defects. With ten defective boiler fuel rods, the levels were in the range of 4.8 to 25×10^4 counts/min. With the prepared superheater fuel defect, levels from 3.3 to 5.5×10^4 counts/min were observed. Data from the particulate monitor were somewhat erratic, possibly due to moisture in the filters. In general, the levels observed were lower than those for the gas channel.

During operation with the prepared superheater fuel defect, a sample for the stack monitor was taken ahead of the charcoal filter and after the filter while operating at the same power level. The effectiveness of the charcoal filter was demonstrated by the fact that the gas monitor indicated 200,000 counts/min before the filter and 85,000 counts/min after the filter. A similar ratio was observed by comparing gross specific gamma activity of samples taken from the gas stream and counted in the laboratory.

G. Operating Procedures

The operating procedures for startup, shutdown, and continuous power operation were performed essentially as originally designed. Draining, flooding, and venting of the superheater worked very satisfactorily.

As a result of operating experience, a few operating instructions were added to those of the initial design:

1. The steam traps in the steam-line drains had to be placed in operation before control rods were raised to make the reactor critical, when the superheater was drained and the reactor was at operating temperature and pressure. (This was necessary to prevent accumulated condensate at low points in the steam line from entering the superheater while the reactor was being operated.)
2. The manual stop valve in the superheater drain line was left open. (This was done so that condensate collected in the superheater flood-and-drain piping would be automatically drained by leakage through the control valve, and thereby prevent condensate from rising to the elevation of the superheater interpass plenum chambers.)

3. Reactor-water-demineralizer flow was maintained at low values during low-power operation. (This was necessary to assure sufficient steam flow for superheater fuel-element cooling. An excessive demineralizer flow in proportion to reactor power would have caused a large amount of power to be required to raise feedwater temperature to reactor temperature, with the result that insufficient steam would have been produced to cool the superheater fuel plates adequately.)

4. The high rate of superheater flooding was used exclusively. (This was possible since the superheater fuel temperatures were reduced to saturated steam temperatures within a short time (minutes) after reactor shutdown.)

VI. SPECIAL POWER EXPERIMENTS

Operation of the reactor for fuel burnup and defective fuel tests provided opportunities for planned experiments concerned with plant performance and operating parameters. In addition, certain characteristics of the reactor were manifested during the approach to operating conditions, and an unplanned superheater wetting experiment also occurred.

A. Power-split Experiments

The reactor design criteria specified that 19% of the core power be produced in the superheater, the remaining 81% being produced in the boiler at the design full power of 20 MWt. Because of heat losses from the superheater assemblies to the moderator water, and because part of the power produced by fissions in the superheater was released in the moderator water by the absorption of gamma rays and the slowing down of neutrons, only 17% of the reactor power was calculated to be used for superheating the steam from saturation temperature (489°F) to 850°F.

1. Effect of Control-rod Configuration

The fraction of the core power produced in the peripheral superheater was measured by flux-wire irradiations to be about 17% with all nine control rods banked, at zero power and room temperature. (See Section IV-B-3.) Some control of this power split was expected by changing the position of the control rods in the superheating region in relation to the position of the control rods in the boiling region. To determine the extent to which the power split could be controlled, the reactor power level was increased by maneuvering the control rods in two different modes. In the first mode, power was increased by withdrawing the nine control rods in a uniform bank, with the power level increasing as the bank height increased up to the maximum of 25 in. In the second mode, power was increased by movement of only the central control rod, No. 1, with the remaining eight control rods fully withdrawn to 25 in. throughout the power change. The result is shown in Fig. 75, which shows that at 5 MWt the percentage of power developed in the superheater increased from about 15 to 17%. As the power level increased, the difference between the two curves approached zero because control rod No. 1 was increasingly withdrawn, until at 17.4 MWt it was completely removed and all nine rods were banked. Note that with all nine control rods banked, the superheater power fraction increased as the power level rose, due to the increase of voids in the boiler region with increased power.

Table XI illustrates the results of additional power-split experiments. Here, the power level was maintained approximately constant, and superheater exit steam temperature and fraction of core power produced in the superheater were measured while control rods were

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Fig. 75. Effect of Control-rod Configuration on Power Split, Core PSH-1A

manipulated. By insertion or removal of the outer gang of control rods located in the superheater zone, with corresponding movement of the central rod and intermediate gang to maintain approximately level power, the superheater exit-steam temperature was varied from 765 to 830°F and the superheater power fraction was varied from 14.9 to 17.0%.

TABLE XI. Change in Superheater-boiler Power Split by Control-rod Manipulation, Core PSH-1A

Power Level, MWt	Superheater Exit Steam Temp, °F	Control-rod Positions, in.			Power Fraction in Superheater, %
		Central	Intermediate Gang	Outside Gang	
8.1	820	19.80	19.80	19.80	16.6
7.6	830	17.50	19.80	~25	17.0
7.4	790	~25	19.80	16	15.7
7.3	765	~25	~25	12	14.9

2. Effect of Flow through Reactor-water Demineralizer

Another variable, the flow of water from the reactor vessel through the reactor-water demineralizer, DM-1, and heat exchangers to the feedwater tank, was investigated to determine its effect on power split. In general, the investigation was made by comparing the power split at various power levels with no flow through the demineralizer and with full flow of about 36 gpm. Curves of power split vs power level for the two cases are shown in Fig. 76. On first glance, it appears that operation with full flow through the demineralizer reduced the percentage of power generated in the superheater by about 2% of the total power near 6 MWt, with the difference decreasing as the power level increased until it disappeared at about 17 MWt. This is true, but the more important effect of increasing the flow through the demineralizer is shown by the circled data points superimposed on the two curves from 8.7 to 10.5 MWt. These points resulted from an experiment in which the reactor was at full power, with all control rods fully withdrawn and equilibrium xenon, but with no flow through the demineralizer. The flow was then increased in 10-gpm increments. As the flow increased, the reactor power increased, with almost a 2-MWt increase at full flow. The control rods remained fully withdrawn during this time, and the increase in power was due to the increase in feedwater flow rate caused by the flow through the demineralizer. The result was that the inlet-water subcooling was increased, and a higher reactor power was required to generate the same voids within the core. During this experiment, however, the power split remained virtually constant. To summarize: At a given control-rod position, the use of the demineralizer resulted in about a 2-MWt increase in reactor power without changing the power split significantly.

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Fig. 76. Effect of Reactor-water-demineralizer Flow on Power Split, Core PSH-1A

Use of the demineralizer had another significant effect, for as the flow through the demineralizer increased, the superheater outlet-steam temperature increased markedly. This was not due to a change in power split, but rather to the fact that about 2.1 MWt of boiler power was used in sensible heating of the flow (the 36 gpm that was heated from feedwater temperature of about 100°F to saturation at 489°F). Consequently, the steam-production rate in the boiler was reduced. This lesser amount of steam was, therefore, heated to a higher temperature in its passage through the superheater because the superheater was producing the same percentage of power but had less coolant to heat.

B. Reactor-water-level Experiments

The core shroud and chimneys for BORAX-V extended 5 ft above the core, or 10 ft above the bottom of the reactor vessel. The elevation of the lowest superheater inlet was 14 ft 5½ in. The water level in the reactor had to be maintained within limits set by these conditions and was further restricted by the low-level and high-water-level scram settings. There was, then, an apparent allowable water-level operating range of about 4 ft, but the realistic range was less, since a water level of 10 ft 6 in. at operating pressure with zero power could increase by a foot or two as the power increased to maximum and the steam void in the core and riser increased.

1. Reactor-water-level Measurement

Three types of instruments were included in the initial design of BORAX-V for measurement of reactor-vessel water level: (1) a conventional gauge glass, observed remotely by a closed-circuit television system; (2) a differential-pressure measurement, comparing the pressure of the column of water of known height against the head of water in the reactor vessel; and (3) a pressure-compensated, differential-pressure-measuring instrument with circular chart recorder, LR-1.

The pressure-compensated instrument corrected for the changes in reactor-water density as the temperature was increased from room temperature to the operating temperature of 489°F. During power operation, boiling produced a change in effective water level. During operation of boiling core B-2, a steam-sampling probe was used to measure the reactor water level by the change in content of radioactive sodium in the sample as the probe was raised from the water into the steam region.⁷ During central superheater core operation, the same steam probe was used, but the oxygen concentration of the sample was used to obtain water level rather than the radioactive sodium content. At the same time, an acoustic water-level probe was also used through another nozzle. The acoustic probe is described in ANL-6961.¹

During most of the peripheral superheater core operation, the steam-sampling probe was initially installed on the central reactor-vessel-head nozzle, and two means of analyzing its sample were used. The oxygen concentration was again observed, and a sample steam-flow measurement was also used to measure reactor water level. The flow measurement consisted of observing the change in sample flow rate on a conventional rotameter as the probe height was varied. The flow rate was, of course, higher when the probe was in water, where the density was high, and lower when the probe was in steam, where the density was low. For the final two weeks of operation with core PSH-1C, the acoustic probe was mounted on the central head nozzle and was also used to measure the water level (the level at which water just begins to strike the detector plate). Table XII summarizes the differences between the LR-1 water-level recorder (pressure-compensated, differential-pressure measurement) reading and the data from the three different probe measurements.

TABLE XII. Comparison of Reactor-water-level Measuring Systems, Cores PSH-1A, B, and C

Power Level, MWt	Reactor Water Level from LR-1, ft, in.	Steam-Water Mixture Thickness, in., Difference between Measurements by LR-1 and:		
		Steam Probe, Oxygen	Steam Probe, Flow	Acoustic Probe
5	11,9	11.5	-	-
6.3	11,10	-	7	-
7.17-7.74	11,6-12,10	-	-	3-8
10.0	11,9 $\frac{3}{4}$	14.5	-	-
10.8	11,10	-	9	-
12.8-15.5	11,8-13,2	-	-	6-14
15.0	11,9 $\frac{3}{4}$	26.5	-	-
15.0	11,11	-	13	-

The basic differences in measuring technique are probably the cause of the differences in level indicated by these probe devices. The oxygen-analysis method yielded the greatest difference from the differential-pressure method, because the steam quality had to be greater than 99% in order to have a water-level indication from oxygen content. The flow-rate method produced an intermediate value of difference from the LR-1 reading, because a higher percentage of moisture was required in the steam to produce an effective change in flow rate. The acoustic probe required that liquid water droplets strike the plate before the presence of water could be detected.

The reactor scram devices which would cause reactor shut-down in the event of excessive reactor-vessel water level were uncompensated differential-pressure measuring devices. The scram level setting on these devices was established on the basis of steam-probe observations.

2. Effect of Reactor Water Level on Steam Temperature

As the reactor water level was increased in the BORAX pressure vessel, the outlet superheated steam lines became partially covered with water. The second-pass superheater fuel assemblies were connected to the steam-line outlet nozzles in the reactor vessel wall by flexible manifolds. The flexible-metal-hose portion of these manifolds had a single wall; the riser on the superheater second-pass assemblies and a portion of the manifold that connected to the steam line were made of double-walled piping with a static-steam insulating layer between the two walls. The single-walled portion of the flexible manifold began at a level of 12 ft $6\frac{9}{16}$ in. above the bottom of the reactor vessel. As the reactor water level was increased, so that water or the steam-water mixture began to cover this single-walled pipe, heat transfer from the superheated steam to the steam-water mixture increased due to boiling at the outer surface.

Figure 77 shows the change in steam-line temperature (T-1) as the water level was increased. The steam temperatures at the outlet of the second pass (T-2) and at the inlet of the second pass (T-3) remained essentially constant until the water level reached about 12 ft 6 in., as measured on the pressure-compensated differential-pressure measuring instrument, LR-1. As the reactor water level was increased further, the second-pass superheater steam temperatures T-2 and T-3 decreased and steam-line temperature T-1 decreased at an increasing rate. This indicates that moisture was being carried with the steam into the superheater. The actual level of the shortest inlet-superheater assembly riser was 14 ft $5\frac{1}{2}$ in., and the remaining inlet risers were 2 in. higher. It is possible that a mist, or fog, extended for some distance above the height at which the acoustic level probe could detect any water, and that this fog was drawn into the superheater first-pass assembly risers.

There was an opening into the risers of the instrumented superheater fuel assemblies at 13 ft 10 in., at the point where the thermocouple leads were brought through the outer riser wall, and this opening was not leaktight. The acoustic water-level probe, mounted on the central nozzle of the reactor vessel top head, indicated that water droplets were reaching from 6 to 14 in. above the level reported by instrument LR-1. For the LR-1-indicated level of 12 ft 10 in., the acoustic probe indicated a maximum height of steam-water mixture of 13 ft 10 in., the height at which the thermocouple leads are brought out of the superheater fuel-element risers. It is also possible that moisture from the steam-water mixture was entering the superheater steam coolant region through this leak.

An increase in steam-line temperature, T-1, for water levels below the LR-1-indicated level of 11 ft 6 in. indicates the reduced heat-exchanger effect when the water level or steam-water mixture level was below an actual elevation of about 12 ft $6\frac{9}{16}$ in.

Fig. 77. Steam Temperature vs Reactor Water Level, Core PSH-1A

During these water-level experiments, the control rods were fully withdrawn and the flow rate through the reactor-water-demineralizer, DM-1, was at a maximum. The reactor power level was in the range of 11.6 to 14 MWt, with power level increasing as the reactor water level increased. An indication of the variation in steam-line temperature due to changes in power level is shown on curve T-1, at a water level of 12 ft 10 in. This change in power level was caused by the buildup of xenon poison in the reactor.

3. Effect of Reactor Water Level on Power Level

An experiment was performed to determine the effect of reactor water level on reactor power. The reactor was operated at

maximum power, with control rods fully withdrawn, until equilibrium-xenon conditions were reached. Then the reactor water level was increased from 11 ft 0 in. to 12 ft 10 in. (as measured by the level recorder, LR-1, a pressure-compensated, differential-pressure measurement of water level), and the power level (as computed from feedwater flow measurements) increased from 11.8 to 13.7 MWt. Data are plotted in Fig. 78. This increase in power is believed to be due to increased natural recirculation rate resulting from the increased water level above the top of the chimneys and/or the reduced carry-under of steam with the recirculation water.

A second run of the same test was made, extending the water level to 13 ft 2 in. to see whether the maximum power level due to this effect had been reached. Because of limited time, equilibrium-xenon conditions could not be reached. The highest power level (14 MWt) was reached at a water level of 12 ft 10 in. As the water level was increased to 13 ft and 13 ft 2 in., the power level decreased. Reduced steam temperatures at the interpass plenum, superheater outlet, and

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Fig. 78. Power Level vs Water Level at Equilibrium-Xenon Conditions, Core PSH-1A

steam line indicated that increased moisture was being carried into the superheater.

The acoustic water-level probe, mounted on the central nozzle of the reactor vessel top head, indicated that occasional splashes of steam-water foam were reaching a height about 11 in. above the level indicated by LR-1. The lowest superheater inlet was $4\frac{1}{2}$ in. above the height of 14 ft 1 in. indicated by the acoustic probe.

C. Optimization of Reactor Pressure-control System

The operating stability of the reactor and steam systems and optimization of the reactor pressure controller settings were studied both experimentally and analytically with the PSH-1 core. Experiments consisted primarily of imposing a step-change on the control-point setting of the main-steam back-pressure-regulating valve after adjusting the three pressure-controller settings (proportional band P, reset rate R, and rate time T) and observing the resulting transients of superheater fuel-plate temperature, reactor pressure, and reactor power. A corresponding analytical study was made by using a model of the reactor system, and the results of the experiment and analysis agreed in regard to the proper settings of the three pressure-controller settings.

Results showed that once optimized settings had been established, a stabilizing effect on the control system was provided by (1) increasing the proportional bandwidth with constant reset rate and rate time; (2) decreasing reset rate at constant proportional band and rate time; (3) decreasing rate time with constant proportional band and reset rate; and (4) increasing the reactor power with the three controller settings held constant.

A typical result of one set of experiments is shown in Fig. 79, in which the superheater fuel-plate temperature, measured by a thermocouple in a fuel plate 4 in. above the inlet to the second pass, is plotted against time for three values of the proportional band setting in response to a 15-psi step increase in the reactor pressure-control point. The final settings of P, R, and T were 50%, 0.2 repeat/min, and 0.05 min, respectively.

D. Low-pressure Oscillations

On several occasions, while a combination of electric and low-power nuclear heating was being used to raise the reactor to operating temperature and pressure, the usually random boiling noise on a neutron-flux trace exhibited cyclic characteristics. This phenomenon occurred only at low pressures in a range of 150-220 psig with reactor powers of 50-100 kWt and the external electric preheat system furnishing an additional 254 kW. During this heating operation, the coolant channels in the

superheater were flooded and the superheater interpass plenum chambers interconnected to the steam dome and steam line via the pressure-equalizing valves. All steam-line valves were closed, and the feedwater was being supplied only by in-leakage of control-rod drive and auxiliary-pump seal water. The seal-water-system flowmeters indicated a steady flow rate.

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Fig. 79. Superheater Fuel Temperature Response to Pressure-set-point Step Changes, Core PSH-1A

The oscillations, as observed on the linear neutron-flux recorder, had a cycle time of about 10 sec. The period meter indicated periods of about ± 10 sec. Maximum amplitude observed was $\pm 17\%$ of average reactor power. The amplitude decreased as the pressure increased for a given power, but the amplitude increased when power was raised. Above about

220 psig, the linear flux trace reverted to random boiling noise. Figure 80 shows a typical portion of the linear flux trace during the oscillations.

The oscillations observed in the central superheater core, CSH-1, at zero-power operating-temperature conditions had about the same frequency (~ 10 sec) as those in the peripheral superheater core.¹ However, on core CSH-1, the oscillations occurred only when the superheater was partially flooded.

E. Superheater Wetting Experiment

Flooding of an integral nuclear superheater while the reactor was operating at power was expected to be retarded by the flashing of the water to steam on the high-temperature superheater fuel elements, and the resulting change in reactivity was expected to be slow. This theory has been at least partially confirmed by an unplanned experiment on the core with peripheral superheater, PSH-1A, which occurred as follows:

During operation on June 8, 1964, the reactor power had been leveled off at 20 MWt at 1030 hours. At about 1034, the steam-line and superheater-fuel-plate temperatures began to decrease rapidly, as shown in Fig. 81. At about 1035, the reactor operator started inserting control rods slowly to maintain a constant power. At about 1039 and 50 sec, the operator started inserting control rods rapidly to reduce power, and at about 1040, the reactor was scrammed by a high-reactor-water-level signal. The incident was caused by the sticking of the reactor water-level recorder, LR-1, which controlled the feedwater flow. The reactor water-level recorder indication rose from 11 ft to 13 ft 2 in. immediately after the scram. The feedwater flow rate increased steadily throughout the experiment and went off scale on the recorder at 150 gpm about 2 min before the scram. Reactor pressure remained constant at 600 psig, but the indicated steam flow rate increased from 45,000 to about 50,000 lb/hr. The pressure drop through the superheater increased from 15 to 24 psi during the experiment.

The reduction in steam-line and superheater-fuel temperatures shown in Fig. 81 is believed to have been caused by carryover of steam-water foam into the superheater due to the high reactor water level and the extreme turbulence of the steam-water mixture at 20 MWt. The steam-foam interface at an indicated level of 13 ft 2 in. was estimated to be actually about 1 to 2 ft higher; the height of the superheater inlets was 14 ft $5\frac{1}{2}$ in. to 14 ft $7\frac{1}{2}$ in. The minimum amount of moisture carried over into the superheater required to cause the observed reduction in steam-line temperature from 830°F to saturation at 489°F was calculated to be about 9000 lb/hr.

172 psig

185 psig

Notes:

Reactor Power, ~ 85 kW

Superheater Flooded

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Fig. 80. Neutron-flux Oscillations at Low Reactor Pressure, Core PSH-1A

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Fig. 81. Superheater Fuel and Steam-line Temperatures vs Time, Superheater Wetting Experiment, Core PSH-1A

The effect of the moisture carryover into the superheater on the boiling noise can be seen on the linear neutron-flux trace shown in Fig. 82, where the noise begins to increase at about 1036 hours.

Because control-rod positions were not recorded during the experiment and because of the uncertainty of control-rod differential-reactivity worth at high powers, the actual reactivity addition or rate of addition, due both to moisture in the superheater coolant channels and higher inlet subcooling resulting from the excessive feedwater flow, could not be estimated accurately. However, the observed reactivity changes were small and the addition rate slow. During this operation, the reactor power level and the 5-sec-period reactor-scram circuits were in effect.

The reactor was scrammed by the uncompensated, differential-pressure-measuring, high-water-level scram system. At the time, the reactor was at maximum power and feedwater was being added at the maximum rate with one feed-water pump. Subsequently, the high-water-level scram setting was changed to give a greater margin of protection against this type of transient.

Note:

Reactor Power, 20 MWt

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Fig. 82. Linear Neutron Flux, Superheater Wetting Experiment, Core PSH-1A

VII. IN-REACTOR-VESSEL INSTRUMENTATION MEASUREMENTS AND ANALYSIS

A. Summary

Measurements were taken during power operation with core PSH-1 by means of two instrumented boiling fuel assemblies and two instrumented superheater fuel assemblies, plus in-vessel thermocouples, flux-mapping thimbles, and hydraulic probes. Locations of in-core instruments are shown in Fig. 20. With the exception of the hydraulic probes, intended for down-comer density and flow measurements, useful information was obtained from all in-vessel instruments.

During the early phases of reactor operation, thermocouples in the instrumented superheater fuel assemblies were very useful to the reactor operators because they helped to insure that safe superheater fuel temperatures were not exceeded and because they indicated the magnitude of temperature fluctuations (and inherent thermal cycling) during reactor-vessel transient-pressure changes induced by load changes. Thermocouples in all four instrumented fuel assemblies were calibrated before use at temperatures up to 490°F in a 600-psig autoclave. The initial settings of the pressure-control system were later modified to give more stable operation.

Temperatures measured in the UO_2 fuel of the boiling fuel rods showed the influence of burnup and reactor operating power to be complex factors affecting the thermal conductance of the fuel rods. Fuel-rod conductance values at the end of the operating period were found to be about half the initial values at the same power level. Conductance values increased with power level at a given integrated power.

Determination of small temperature differences in the steam and water environment of the reactor vessel remained a difficult problem because of the calibration changes in individual thermocouples. It was necessary to perform periodic intercalibrations at zero power to determine these calibration changes.

B. Superheater Fuel Plate and Steam Temperatures

Each instrumented superheater fuel assembly was equipped with 24 Chromel-Alumel* thermocouples, 20 of which were mounted directly in the fuel portion of the fuel plates to record the plate temperatures. The other thermocouples measured the steam temperature at the inlet and outlet of the fueled region of the assemblies, with two thermocouples at each location. A selected signal from the fuel-plate thermocouples was displayed in the control room on an indicator-recorder. Signals were selected by

*Hoskins Manufacturing Company trade name.

means of a 40-position selector switch. The steam temperatures were normally recorded in the reactor-building instrument room; but since several of the fuel-plate thermocouples failed before operation of the peripheral superheater core, one thermocouple from each of the four steam-temperature locations was connected to the 40-position selector switch so that steam temperatures could be monitored in the control room.

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Fig. 83. Numbered Thermocouple Locations in Instrumented Superheater Fuel Assemblies

The location of the thermocouples in the superheater fuel assemblies is shown schematically in Fig. 83. Three fuel elements in each assembly contained thermocouples as indicated. The 0.040-in.-diam, flattened-tip thermocouples were brazed into holes machined through the fuel-element side plate and through the dead edge of the fuel plate into the cermet fuel portion of the plate, as shown in Fig. 84. The leads passed up within the stagnant-steam insulating gap between the side plate of the element and the surrounding insulating tube and then into the insulating steam space between the double walls of the upper riser.

The 20 numbered fuel-plate thermocouples from the first-pass superheater fuel assembly (located in core position 14) were connected to corresponding positions on the selector switch. The 20 thermocouples from the second-pass assembly (core position 13) were connected to positions 21 through 40 on the selector switch; each switch number was simply 20 greater than the thermocouple number shown in the schematic drawing in Fig. 83. The orientation of the instrumented superheater fuel assemblies in the PSH-1A core is shown in Fig. 20.

After installation of the two instrumented superheater fuel assemblies and during final installation of core components, a core-structure chimney was accidentally dropped on the leads from the instruments of one fuel assembly.

This assembly was relocated to make it the first-pass assembly (since temperature readings from the first pass were considered less important than those from the second pass), and indications at that time were that thermocouples No. 4, 6, 7, 9, 12, 13, and 17 were faulty. Thermocouples No. 1, 2, 3, 5, 8, 11, 18, and 19 appeared to be satisfactory, based on continuity and resistance checks, but in operation the readings never varied by more than 10°F from the saturation temperature. It is believed that the leads to these were also damaged when the chimney was dropped. Consequently, only five of the 20 thermocouples in the fuel plates of the first-pass instrumented fuel assembly were serviceable, namely, No. 10, 14, 15, 16, and 20.

106-7172

Fig. 84. Instrumented Superheater Fuel Element

All 20 thermocouples in the second-pass assembly were believed to be operating when operation at high temperature began, but at the end of the 266-MWd operating period only nine were still serviceable. The remaining 11 couples had failed, generally with an indication of an open circuit, perhaps caused by thermal expansion or stress.

Recordings from the thermocouples in the instrumented fuel assemblies are summarized in Table XIII. In the table, readings are given for all the thermocouples that appeared to have been operative for the entire

core life, for various power levels from 5 to 20 MWt. At each power level, readings are given over a 2- to 3-month period, to show the effect of time at power on the response of the thermocouples. The table shows that neither power level nor time had a significant effect on the temperature readings, and that the most important effect was exerted when water was flowing through the reactor-water demineralizer, DM-1.

TABLE XIII. Superheater Fuel-plate Temperatures as a Function of Power Level, Time, and Reactor-water-demineralizer (DM-1) Flow; Cores PSH-1A, B, and C

Power Level, MWt	Date, 1964	DM-1 Flow, gpm	STEAM TEMPERATURE, °F				FUEL TEMPERATURE, °F													
			First Pass		Second Pass		First Pass					Second Pass								
			Inlet	Outlet	Inlet	Outlet	10 ^a	14	15	16	20	22	23	24	25	31	33	34	36	40
6.3	May 26	10	500	665	665	800	505	600	580	515	575	680	680	730	655	660	720	710	810	755
5.6	June 9	9	500	660	660	790	510	610	580	520	575	680	700	740	660	660	725	730	790	760
5.1	July 10	17	507	680	680	810	520	630	605	535	610	715	715	765	680	680	730	750	850	790
10.0	June 4	5	500	660	660	820	515	605	580	530	565	690	690	775	665	675	710	715	840	760
10.0	June 23	30	500	690	700	870	520	640	615	555	600	740	735	845	700	700	765	775	910	825
10.5	July 9	25	500	680	685	855	530	635	600	555	590	730	735	840	695	700	750	760	900	805
10.1	Aug 7	0	500	650	655	790	525	615	590	555	575	700	705	790	660	665	710	720	835	750
12.0	June 17	36	500	700	710	880	520	650	610	560	600	750	750	870	710	710	780	790	940	830
12.1	July 7	36	500	700	700	880	520	640	600	550	590	750	750	860	705	710	775	785	930	835
12.6	Aug 6	36	500	695	700	880	520	640	605	560	590	740	740	860	700	700	760	770	930	820
15.2	June 11	37	500	695	700	890	540	640	600	560	580	740	740	875	700	705	760	770	905	810
14.6	July 14	38	500	690	695	880	535	640	605	565	585	735	740	870	695	700	760	770	935	820
15.0	Aug 11	38	500	690	695	880	525	640	605	565	590	740	740	870	700	710	760	770	935	815
20.0	May 22	0	490	660	670	850	510	600	560	550	550	705	700	860	675	690	730	730	860	760
20.5	July 14	40	495	690	695	885	525	630	590	560	570	730	735	885	695	700	765	765	940	810
20.1	Aug 4	39	500	685	695	880	530	630	595	565	555	725	730	885	645	700	760	760	940	805

^aThermocouple numbers as in Fig. 83.

The data from two lines of Table XIII (dates Aug 7 and May 22 for 10.1- and 20-MWt operation with no flow through the demineralizer) have been plotted in Fig. 85, together with values of the superheater fuel-plate temperature calculated according to the method presented in ANL-6302.² The calculated and experimental points are not in good agreement. Much of the disagreement may be because fuel material had been removed from the slot into which the thermocouple was brazed in the fuel plate. An unheated strip of fuel-plate surface was thereby produced which was cooled by the flowing steam, the heat that was removed being supplied by conduction from surrounding fuel-plate areas. A complete analysis of the problem is difficult, due to the unknown thermal conductivity of the braze material and to the slight nonuniformity of all of the thermocouple installations, but a discrepancy of 50-100°F between the thermocouple reading and the undisturbed fuel-plate temperature is estimated. More difficult to explain, on the basis of heat transfer, were the numerous thermocouple readings below the temperature of the steam itself. These low readings, which may have been due to breaks in the thermocouple sheaths or to high-resistance shorts in the leads somewhere along their length, are not shown in Table XIII.

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Fig. 85. Calculated and Measured Temperature Distribution in Superheater Fuel Assemblies, Core PSH-1A

C. Central Temperatures in Boiling Fuel Rods

1. Measurements Obtained

Temperatures in the UO_2 fuel of the boiling fuel rods were measured by W-W/26 w/o Re thermocouples in instrumented boiling fuel assemblies No. I-1 and I-3 and in two standard fuel assemblies. The design of a boiling fuel thermocouple rod is shown in Fig. 86. Fourteen thermocouple fuel rods were installed during operation of core PSH-1. Twelve of these thermocouples performed continuously, one failed at the beginning of operation, and one gave intermittent service. Thermocouple fuel rods of 4.95 w/o enrichment were used in the instrumented fuel assemblies and in one of the standard boiling fuel assemblies. During the defective superheater fuel tests in core PSH-1C, two 9.9 w/o-enriched thermocouple fuel rods were installed in a second standard fuel assembly at locations symmetrical with two of the 4.95 w/o-enriched fuel rods in assembly I-1, located as shown in Fig. 28.

The primary objectives of the thermocouple fuel rods were to obtain fuel-temperature measurements as functions of reactor power, fuel burnup, core location, and fuel enrichment. Data from these 13 operating thermocouples are presented in Figs. 87 through 99. Figure 87 shows the type of operation to be expected from a thermocouple exhibiting an intermittent open circuit; i.e., the indicated temperature stayed approximately constant at about 900°F , with power, for a number of data points. Data presented in this one case should be considered questionable as to absolute accuracy. Figure 88 indicates similar performance at later burnup on fuel rod YA-14.

2. Analysis of Data

a. Thermal Conductance of Boiling Fuel Rods

The data presented in Figs. 87 through 99 indicate that the thermal conductance of the fuel rod changed quite drastically with fuel burnup and in an accelerated manner during initial irradiation. Because a relatively new type of thermocouple (W-W/26 w/o Re) was used to obtain the data, thermocouple calibration stability was investigated to determine the accuracy of the temperature measurements. Accuracy of measurement was verified in a series of in-core and out-of-core tests on an irradiated thermocouple fuel rod. These tests are described in detail in Section VII-C-2-c. Values of the thermal conductance of a typical 4.95 w/o-enriched boiling fuel rod, ZA-19 (from core position 56, fuel-rod location 49), have been calculated from fuel-thermocouple readings and power-distribution measurements. Table XIV shows the results of these calculations.

ROD TYPE	DIM A
V	12
W	11
X	10
Y	9
Z	8

SECTION THRU ROD

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Fig. 86. Boiling Fuel Thermocouple Rod

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Fig. 87. Boiling Fuel Rod ZA-18, Central Fuel Temperature vs Reactor Thermal Power and Burnup, Cores PSH-1A, B, and C

ID-103-A3469

Fig. 88. Boiling Fuel Rod YA-14, Central Fuel Temperature vs Reactor Thermal Power and Burnup, Cores PSH-1A, B, and C

ID-103-A3470

Fig. 89. Boiling Fuel Rod ZA-19, Central Fuel Temperature vs Reactor Thermal Power and Burnup, Cores PSH-1A, B, and C

ID-103-A3471

Fig. 90. Boiling Fuel Rod VA-2, Central Fuel Temperature vs Reactor Thermal Power and Burnup, Cores PSH-1A, B, and C

ID-103-A3472

Fig. 91. Boiling Fuel Rod ZA-20, Central Fuel Temperature vs Reactor Thermal Power and Burnup, Cores PSH-1A, B, and C

ID-103-A3473

Fig. 92. Boiling Fuel Rod ZA-17, Central Fuel Temperature vs Reactor Thermal Power and Burnup, Cores PSH-1A, B, and C

ID-103-A3474

Fig. 93. Boiling Fuel Rod YA-15, Central Fuel Temperature vs Reactor Thermal Power and Burnup, Cores PSH-1A, B, and C

ID-103-A3475

Fig. 94. Boiling Fuel Rod WA-6, Central Fuel Temperature vs Reactor Thermal Power and Burnup, Cores PSH-1A, B, and C

ID-103-A3476

Fig. 95. Boiling Fuel Rod WA-7, Central Fuel Temperature vs Reactor Thermal Power and Burnup, Cores PSH-1A, B, and C

ID-103-A3477

Fig. 96. Boiling Fuel Rod YA-16, Central Fuel Temperature vs Reactor Thermal Power and Burnup, Cores PSH-1A, B, and C

ID-103-A3478

Fig. 97. Boiling Fuel Rod WA-5, Central Fuel Temperature vs Reactor Thermal Power and Burnup, Cores PSH-1A, B, and C

ID-103-A3479

Fig. 98. Comparison of Boiling-fuel-rod Central Temperatures for 4.95 w/o-enrichment Rod ZA-20 vs 9.9 w/o-enrichment Rod ZB-9, Cores PSH-1A, B, and C

ID-103-A3480

Fig. 99. Comparison of Boiling-fuel-rod Central Temperatures for 4.95 w/o-enrichment Rod ZA-19 vs 9.9 w/o-enrichment Rod ZB-10, Cores PSH-1A, B, and C

TABLE XIV. Overall Boiling-fuel-rod Thermal Conductance,
Btu/hr-ft-°F, Cores PSH-1A, B, and C

Reactor Power, MWt	Integrated Reactor Power, MWht							
	101	740	1578	2702	3498	4474	5280	6384
5	1.38	0.86	0.86	-	0.58	0.47	0.57	0.66
10	1.59	1.24	1.17	0.97	0.85	0.76	0.70	0.73
15	1.87	1.60	1.49	1.23	1.11	1.02	1.04	1.08
20	2.20	1.92	1.78	1.50	1.39 ^a	1.30	1.28	1.34

^aObtained by extrapolation.

The values of the boiling-fuel-rod conductance were obtained for the UO_2 , helium, and fission gases within the fuel-rod jacket by considering (1) the volumetric heat generation within the fuel at the point where the thermocouple was located and (2) the temperature drop between the thermocouple in the center of the fuel and the calculated temperature at the inside of the stainless-steel cladding. The thermocouple temperatures used in calculating the conductances were taken from the curves of Fig. 89 rather than specific thermocouple readings. The decreased conductance with increased burnup may be due to cracking or disintegration of the fuel; the increase of conductance with power may be due to narrowing of the clearance gap between fuel pellets and the cladding tube, and because the conductivity of UO_2 might be increasing at the high fuel temperatures. The maximum specified radial clearance between UO_2 pellets and cladding was 0.002 in. Table XV and Fig. 100 illustrate the pattern of change in thermal conductance as a function of burnup (integrated power). Average burnup for this rod was approximately 0.064 a/o, expressed as percentage of the total uranium loading.

TABLE XV. Change in Thermal Conductance as a Function of Integrated Power at 20-MWt Operation, Core PSH-1

Integrated Power, MWht	Incremental Change in Integrated Power, MWht	Change in Conductance, % of Initial Value	Incremental Change in Conductance, %	Incremental Conductance Change/Increase of Integrated Power, $10^{-2}\%$
101	0	0	0	0
740	639	-12.7	-12.7	-1.98
1578	838	-19.1	-6.4	-0.76
2702	1124	-31.7	-12.6	-1.12
3498	796	-36.8	-5.1	-0.64
4474	976	-40.8	-4.0	-0.41
5280	806	-41.7	-0.9	-0.11
6384	1104	-39.0	+2.7	+0.24

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Fig. 100. Fuel-rod Conductance Change vs Integrated Reactor Power, Cores PSH-1A, B, and C

Additional temperature data from operation with core PSH-1 are shown in Fig. 90. This boiling thermocouple fuel rod, serial VA-2, core position 45, fuel-rod location 17, containing a W-W/26 w/o Re thermocouple in the radial and axial center of the UO_2 pellet fuel, was one that had been subjected to thermal transient tests when new¹⁰ and then irradiated during BORAX-V operation with cores CSH-1 and PSH-1 for an average burnup of 0.074 a/o of the total uranium in the 4.95 w/o-enriched UO_2 fuel. (Only 55 MWht of total integrated power accrued during operation with core CSH-1.) The indicated fuel temperatures for this rod show the same general pattern as that reported for other 4.95 w/o-enriched rods: increased burnup led to increased fuel temperatures up to a point where temperatures first reached the region of about 2000°F. Beyond this point, the pattern became erratic and unpredictable.

b. Effect of Fuel Enrichment on Temperature

Figures 98 and 99 indicate the effect of fuel enrichment on operating temperature. Figure 98 indicates the increase in fuel temperature due to increased enrichment, which was in agreement with calculation. The measurements in Fig. 98 were obtained in two different fuel rods at symmetrical core locations, and in Fig. 99 in two additional fuel rods located symmetrically. Based on the temperature data of Figs. 89 and 91, these fuel rods operated in approximately the same neutron flux. The relatively straight-line curve shown in Fig. 99 for 9.9 w/o-enriched fuel rod ZB-10 in core position 35, rod location 49, differs greatly from the curve in Fig. 98 for fuel rod ZB-9 in core position 35, rod location 43, both in curvature and indicated temperatures. The 9.9 w/o fuel-rod data in Fig. 99 do indicate some of the characteristics exhibited by data for 4.95 w/o-enriched fuel rods having higher burnup, but 4.95 w/o-enriched rods also appeared to have much higher operating temperatures when the high-burnup condition was reached. The linear curve in Fig. 99 might therefore be attributed to physical characteristics of the fuel in rod ZB-10.

Data obtained from Fig. 98 were used to calculate the approximate power-production ratios for the two fuel rods of differing enrichment. Similar calculations for the other pair of fuel rods shown in Fig. 99, installed for this purpose, were not made because of the questionable performance of the thermocouple in the 9.9 w/o-enriched fuel rod. The calculations were based on the assumptions that both the 4.95 w/o- and 9.9 w/o-enriched fuel rods had similar initial thermal conductances, and that both had a cladding inner-surface temperature of 550°F when power was being produced. Power production was then proportional to the difference between fuel and cladding inner-surface temperatures. The curve obtained is shown in Fig. 101.

c. Special Thermocouple Testing

To verify that the indicated temperatures described above were not in error, transient and static postirradiation calibration tests were performed.

(1) Thermal Time Constant

The first tests measured the overall thermal time constant of the irradiated fuel rod and W-W/26 w/o Re thermocouple, and duplicated tests previously made on some of the fuel rods in a new condition.¹⁰ The tests on the new rods included measurement of the overall time constant of fuel rod and thermocouple, as well as determination of the thermocouple time constant in its environment. The tests were made by inserting two thermocouple fuel rods, from room-temperature conditions, into NaK at 500°F and recording the output from the fuel thermocouples. Total fuel-rod immersion time was measured by using a continuously

indicating resistance-level detector, and was found to require 0.7 to 1.0 sec from the time of initial NaK contact. The 63.2% thermal time constants determined in this manner were found to average 13.1 sec for the irradiated rod VA-2, with an average burnup of 0.074 a/o of the total uranium, and 7.8 sec for rods VA-1 and VA-2 when new.

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Fig. 101. Ratio of Power Production in 4.95 w/o- to That in 9.9 w/o-enriched Fuel Rods, Core PSH-1C

In an attempt to simulate the transient test findings, a thermal analog model was set up and tested by means of an analog computer. Typical values of thermal resistances and heat capacities were tried in various combinations to get approximations of the measured

thermocouple-fuel-rod time response. The circuit elements were manipulated to study the relative effects of changes in resistance of the cladding and "film" (lack of wetting), thermal resistance of fuel, and the thermocouple time constant. Scaling of boiling fuel rods was insignificant in BORAX-V, and the thermal resistance of scale was not considered in this model. Since the steady-state, in-reactor fuel temperature was not governed by heat capacity and thermal resistance values associated with the fuel-rod thermocouple, the primary effects studied with the analog were those of variations in the fuel and cladding parameters.

The analog circuit representing the thermocouple fuel rod is shown in Fig. 102. Some results of the analog study are shown in Fig. 103 compared with actual tests on the fuel rods. The relatively large initial delay in the fuel thermocouple response (curves 1 and 2) is felt to be due primarily to a "film" resistance resulting from a lack of wetting of the stainless-steel fuel cladding by the NaK. Time zero is taken as the point of initial contact of the fuel rod bottom with the NaK. Other studies made with the model are summarized in Fig. 104 and Table XVI.

R_1 = Thermal Resistance of Cladding

R_2 = Total Thermal Resistance of Fuel and Fuel-to-Cladding Gap

R_3 = Effective Combined Thermal Resistance of Fuel-to-Thermocouple Gap and Thermocouple

C_1 = Total Heat Capacity of Cladding

C_2 = Total Heat Capacity of Fuel

C_3 = Total Heat Capacity of Thermocouple

E = Initial Temperature Differential Between Thermocouple and Heat Source. (Model Assumes a Step Increase in Temperature)

V_{out} = Temperature of Thermocouple

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Fig. 102. Analog Thermal Model for Boiling Thermocouple Fuel Rod

ID-103-A3484

Fig. 103. Computed and Measured Thermal Time Response of Boiling Fuel Thermocouple Rods

ID-103-3485

Fig. 104. Computed Thermal Time Response of Boiling Fuel Thermocouple Rods

TABLE XVI. Constants Used in Analog Thermal Model Studies of Boiling Thermocouple Fuel Rods

Run No.	R_1	C_2	R_2	C_2	R_3	C_3	$\frac{R_2 C_2}{R_1 C_1}$	$\frac{R_2}{R_1}$	$\frac{C_2}{C_1}$	τ , sec
A	5	0.2	10	1	10	0.1	10	2	5	12.8
B	5	0.2	5	1	10	0.1	5	1	5	7.6
C	2	0.2	5	1	10	0.1	12.5	2.5	5	6.9
D	10	0.2	10	1	5	0.2	5	1	5	13.5
E	0.1	0.2	10	1	5	0.2	500	100	5	12.1

NOTES:

1. Resistances in $M\Omega$, capacitances in μF .
2. Time constant τ is time required to reach 63.2% of total change in temperature from time zero.
3. Based on original fuel design, the constants were calculated to be related by:

$$\frac{R_2 C_2}{R_1 C_1} \equiv \frac{RC(\text{fuel})}{RC(\text{clad})} = 1050, \quad \frac{R_2}{R_1} \equiv \frac{R(\text{fuel})}{R(\text{clad})} = 244, \quad \frac{C_2}{C_1} \equiv \frac{C(\text{fuel})}{C(\text{clad})} = 4.3.$$

The effective time constant of the thermocouple is of the order of 0.5 to 1 sec (in the preirradiated condition).

Transient tests on fuel rod VA-2 confirmed the in-reactor, static-temperature indications of an approximate 50% reduction in the thermal conductance of the fuel rod due to fuel burnup and operation at elevated temperatures. The analog studies were limited, and exact matching of the analog results and measurements was not obtained. It is concluded that very similar time-response curves and 63.2% time-constant values were obtained with the analog. Furthermore, changes in the thermal resistance of the fuel are much more important to the overall time constant than changes in fuel cladding or film resistances because of the small value of the ratio of cladding-heat capacity to fuel-heat capacity. (The overall time constant is directly related to the RC time-constant values of the sections of the fuel rod, where R is the thermal resistance and C is heat capacity.)

(2) Thermocouple Calibrations

The accuracy of the thermocouple in fuel rod VA-2 was confirmed by preoperational, in-reactor, zero-power calibrations and by a postirradiation furnace calibration. This last test was conducted by

placing the irradiated boiling fuel thermocouple rod VA-2, together with a new thermocouple fuel rod (VA-3) and a Chromel-Alumel test thermocouple, in an argon-purged electric furnace located in a hot cell. A maximum temperature of 1696°F was reached with this arrangement. As shown in Table XVII, there is no obvious error pattern followed by either fuel-rod thermocouple. The only known errors worth considering are the manufacturer's standards for the thermocouples. For the Chromel-Alumel thermocouple, this is $\pm 4^\circ\text{F}$ for temperatures to 530°F, and $\pm 3/4\%$ of temperature for values greater than 530°F. Errors on the W-W/26 w/o Re thermocouple

TABLE XVII. Temperature Intercalibration between W-W/26 w/o Re Thermocouple Rods and Chromel-Alumel Thermocouple

Temperature, °F		
Chromel-Alumel Thermocouple	Irradiated Fuel Rod VA-2	Nonirradiated Fuel Rod VA-3
75	87	85
202	87	85
297	305	283
420	438	426
512	530	511
589	610	595
693	708	695
800	815	808
915	925	920
1002	1011	1010
594	604	598
991	1002	998
1102	1103	1105
1192	1191	1195
1301	1300	1310
1400	1400	1405
1511	1505	1515
1600	1600	1610
1696	-	1705
1499	1498	1512
1301	1298	1315
1096	1088	1105
900	893	910
695	692	704
519	518	525

are stated as $\pm 8^\circ\text{F}$ for temperatures up to 800°F , and $\pm 1\%$ of temperature for values above 800°F . Consideration of these errors alone leads to the conclusion that no measurable calibration change was produced in the W-W/26 w/o Re thermocouple after a neutron-flux exposure of approximately 2×10^{18} nvt in BORAX-V. Earlier calibration work related to core B-2 operation applies also and was as follows:

One new fuel-rod thermocouple and two Chromel-Alumel reference thermocouples in the EBR-I were irradiated to help explain the boiler fuel-temperature changes of several hundred degrees F observed during operation of BORAX-V core B-2.⁷ The three thermocouples were exposed to six cycles of reactor startup and shutdown at a maximum temperature of 690°F in a fast flux of 10^{14} nv. Total exposure of approximately 2×10^{18} nvt (greater than the calculated B-2 integrated dosage) was reached with no apparent shift in calibration. A subsequent furnace calibration to a maximum temperature of 1997°F , with three cycles from room temperature to maximum, showed no measurable errors. It has been concluded that the core B-2 fuel-temperature measurements are valid, with a predicted accuracy of $\pm 1\%$ of true temperature. The 1% accuracy figure is based primarily on the guarantee of the wire manufacturer. Calibration errors due to neutron transmutation of the thermocouple metals might be expected at dosages approaching 10^{21} or 10^{22} nvt if one considers the calculated composition changes presented by Browning and Miller.¹¹ While their data were prepared for periods of time extending to 20 years (at flux levels of 10^{14} nv), the curves presented indicate an initial apparent composition change at about 10^{21} nvt. Calibration curves for the W-Re-Os alloys produced by irradiation are not known, and the above threshold level of appreciable calibration change should be considered as an estimate.

D. Reactor-subcooling Measurements

Figure 105 summarizes the subcooling temperature measurements made on core PSH-1. Seven 1/8-in.-OD, stainless-steel-sheathed, Chromel-Alumel thermocouples were used to obtain the curve. Six thermocouples at the inlets to core positions 12, 21, and 22 measured water-inlet temperature, and the seventh measured the temperature of steam in the steam dome. Periodic zero-power calibrations were made between the steam thermocouple and the six inlet thermocouples to account for random calibration changes, and all readings were then corrected on the basis of these calibrations. The six corrected readings were then averaged for each datum point and subtracted from the steam temperature at that time. The resulting average subcooling values were then plotted.

The unusual shape of the lower curve is attributed to the effect at low powers of the reactor-water-demineralizer return-water flow and feedwater injection through the control-rod-drive seals, downcomer-probe cooling system, and the circulating-pump seal. These sources injected

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Fig. 105. Reactor Water Subcooling vs Thermal Power, Cores PSH-1A, B, and C

relatively large quantities of cool water into the vessel. At higher powers, the relative volume of water injected became small compared to the recirculated water in the downcomer.

Typical correction values ranged from +1 to -1°F and point up the difficulty experienced in making the determinations of subcooling. Accurately measuring small temperature differentials remains one of the problem regions of in-reactor-vessel measurements.

E. Boiler Inlet Velocity and Exit Void-fraction Measurements

The inlet velocity and exit void fraction were measured with turbine-type flowmeters at the entrances and exits of instrumented boiling fuel assemblies I-1 and I-3. Figures 106 and 107 show inlet velocity as a function of reactor power for assemblies I-1 and I-3, respectively. Velocity is reported at the fuel-assembly inlet nozzle, which had an effective cross-sectional area of $4.69 \times 10^{-2} \text{ ft}^2$ at the turbine. Values of velocity were determined from the volumetric readings of the meters by applying the above area factor. No other correction factors were applied. The dual curves for each figure cannot be explained except by some abrupt change in each turbine which might be associated with the reactor shutdown. (A change in the friction of the turbine bearings is a possible explanation.) A postoperation calibration of the recorders associated with the meters showed no calibration errors greater than 1/2 to 1% of full scale. This would account for about 5% of actual velocity change at the flow range in question. No changes in reactor operation that might account for the dual curves are known at this time.

Exit void-fraction measurements for the two instrumented boiling fuel assemblies are shown in Figs. 108 and 109. The results shown were determined by entrance and exit turbine meter measurements and are void-fraction values at the exit meters. Due to fluid expansion and contraction effects between the fuel exit and the meter, the actual fuel-exit void-fraction values might be expected to differ from the reported values by about 10% of value. (All exit void-fraction values reported during BORAX-V operation are designated as volumetric percentage of steam in the exit mixture, measured at the exit turbine meter.)

F. Superheater Fuel-assembly Flow Measurement

Steam-flow venturis and steam-temperature thermocouples were used in two instrumented superheater fuel assemblies to measure steam mass flow rates during power operation with core PSH-1. Since flow in the two instrumented fuel assemblies was in series, both venturis were used. Flow was assumed to be split into eight equal paths through the two instrumented and each of the remaining seven pairs of standard fuel assemblies. Orifices were installed in the inlet risers of the first-pass standard superheater fuel assemblies and the exit risers of the second-pass standard

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Fig. 106. Water Inlet Velocity to Instrumented Fuel Assembly I-1, from Turbine Meter, Cores PSH-1A, B, and C

ID-103-A3487

Fig. 107. Water Inlet Velocity to Instrumental Fuel Assembly I-3, from Turbine Meter, Cores PSH-1A, B, and C

ID-103-A3488

Fig. 108. Exit Void Fraction vs Reactor Power, Fuel Assembly I-1, from Turbine Meters, Cores PSH-1A, B, and C

ID-103-A3489

Fig. 109. Exit Void Fraction vs Reactor Power, Fuel Assembly I-3, from Turbine Meters, Cores PSH-1A, B, and C

superheater fuel assemblies to help maintain this equal-flow distribution. The orifice designs were based on matching the added pressure drop produced by the venturis in each pass.

Table XVIII illustrates the measured performance of the two instrumented superheater fuel assemblies. Because of minor inequality in flow distribution and recognized inaccuracies in the measurements (i.e., manual temperature compensation of recorded differential-pressure signal, possible moisture carryover into the first-pass flowmeter, and about 1% of full-scale accuracy in measuring differential pressure and temperature), the information is perhaps of value only for its academic interest. Mass flow rate was derived from temperature-corrected flow records from the first- and second-pass superheater assembly flowmeters. Flow rates equal to eight times the corrected flow are shown for both flowmeters. This would correspond to the total flow if all assemblies had carried equal steam flow rates. The last column in the table shows total reactor steam flow calculated on the basis of reactor power and superheated-steam exit temperature measured in the second-pass instrumented superheater fuel assembly.

TABLE XVIII. Flow Measurements with Instrumented Superheater Fuel Assembly, Cores PSH-1A, B, and C

Power Level, MWt	Steam Flow, 10 ⁻³ lb/hr				Reactor Water Demineralizer (DM=1) Flow, gpm	Total Steam Flow (Calculated from Net Power), 10 ⁻³ lb/hr
	Instrumented Assembly		Eight Times One Assembly			
	First Pass	Second Pass	First Pass	Second Pass		
	First Pass	Second Pass	First Pass	Second Pass		
6.3	2.11	1.08	16.88	8.64	6.0	15.41
10.0	2.81	2.8	22.5	22.4	34.0	19.9
10.9	2.0	2.2	16.0	17.6	36.0	21.73
12.0	3.45	2.6	27.6	20.8	36.5	25.0
14.6	3.98	3.21	31.9	25.7	38.5	30.65
17.9	5.44	5.05	43.5	40.4	34.5	40.1

G. Downcomer Measurements

Thermocouples (1/8-in. OD, stainless-steel-sheathed, Chromel-Alumel) in the downcomer were routinely used to provide reactor operators with steam- and water-temperature information. No failures were experienced with these thermocouples throughout the entire operation of BORAX-V.

Unsuccessful attempts at hydraulic measurements (density and velocity) in the downcomer were made again. These measurements were attempted with water-cooled, jacketed sensing tubes inserted into the

downcomer region. Differential pressures sensed by these tubes were measured by bellows-type differential-pressure transmitters and recorded. The variations in density and velocity in instrument readings showed no recognizable pattern as functions of reactor power level. The instrument systems were tested on several occasions to verify operation, and the conclusion was reached that the parameters to be measured produced signals below the sensitivity and repeatability of the instruments available for such application. The reasoning is as follows: Downcomer velocity was calculated to be in the range of 1 ft/sec at reactor powers near 20 MWt. At such a velocity, the head developed by the downcomer-velocity-measuring Stauscheibe tube would be about 0.4 in. of H_2O at 70°F. This signal represented 10% of the full-scale range of the measuring instrument, and was equal to the magnitude of random signal variations experienced during reactor operation. Similar problems existed in the density (downcomer void fraction) measuring equipment where expected signal variations were of the same order of magnitude as the random noise. On the basis of the subcooling measurements and the lack of indications of high-velocity and void-fraction signals, it is concluded that no appreciable carry-under of steam occurred.

VIII. TRANSFER-FUNCTION MEASUREMENTS AND STABILITY ANALYSIS

Reactor transfer-function measurements were made on core PSH-1A at powers of 0, 5, 10, 15, and 18.1 MWt with an integrated reactor power of ~1000 MWh. The purpose of these measurements was to estimate the limiting power for stable reactor operation and to determine any special feedback effects associated with an integral boiling-nuclear superheating reactor having a peripherally located superheater. The overall, feedback, and open-loop transfer functions were obtained by determining, through Fourier analysis with an analog computer, the response of the reactor power to a sinusoidally varying reactivity input provided by a rotating fuel-poison oscillator rod in the reflector.

A. Equipment Description

1. Oscillator

For the transfer-function measurements, the oscillator rod was located in reflector thimble C, as shown in Fig. 20. The cylindrical oscillator rotor was made of a 170° arc of Zr-U²³⁵ alloy, Zircaloy-2-clad fuel containing 100 g of U²³⁵ on one side, and a 180° arc of hafnium on the opposite side. (See Fig. 110.) The fuel and poison were 25 in. high. The rotor housing, made of Zircaloy-2, contained 114 5/8-in.-diam holes that aligned with holes in thimble C for cooling of the fuel at power. The rotor had water-lubricated bearings with Type 304 stainless-steel journals and silver-impregnated graphite sleeves. The oscillator drive shaft and upper housing were made of Type 304 stainless steel. A packing gland-type seal, mounted on a reactor vessel head nozzle, had a cooling water supply, leakoff line, and silver-impregnated graphite guide bushing.

The differential-reactivity worth of the oscillator could be varied by means of a traversing mechanism mounted on the reactor vessel head (see Fig. 73) and was manually actuated from above the top reactor shield. The vertical height of the oscillator rotor and housing could be adjusted in 3-in. increments to a maximum withdrawal of 15 in. Because of the low differential-reactivity worth of the oscillator in the PSH-1A core, the traversing feature was not utilized; all experiments were run with the oscillator fully inserted.

Near the start of final transfer-function measurements at 20 MWt, rotation of the oscillator failed to produce a change in reactivity. The oscillator was later removed from the reactor and examined. The oscillator housing was disassembled under water, revealing that the drive-shaft-to-rotor connection was broken. The 17-4 Ph stainless-steel, 1/2-in. male thread of the screwed connection was broken across the weakest section at a locking roll pin, apparently due to a brittle fracture. Because of the high radioactivity of the rotor (about 400 R/hr), no repair was made.

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Fig. 110. Fueled Oscillator

2. Oscillator Drive

The oscillator drive, shown schematically in Fig. 111, consisted of a 1-hp synchronous motor driving a 3:1 infinitely variable Link Belt P.I.V. speed reducer through a flexible coupling. The speed reducer, in turn, drove a gear box in which eight sets of change gears were available, to give an output speed range of 1200 to 1/2 rpm. The output shaft drove a pair of spiral miter gears, which drove the oscillator rod. Torque overload protection was provided by a brass shear pin in the spiral gear, mounted on the output shaft. The stability of the rotating speed of the drive was excellent; in fact, for a given setting, speed variations as measured by the electronic counting equipment were insignificant.

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Fig. 111. Oscillator Drive System

3. Transfer-function Measuring Equipment

The transfer-function measurements were made on a sine-cosine cross-correlation analog computer system. A block diagram of the

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Fig. 112. Block Diagram of Equipment Used to Measure Transfer Function

new equipment, an improvement over that used on core B-2,⁷ is shown in Fig. 112. The measuring equipment consisted of: an out-of-core, B¹⁰-coated, parallel-plate, compensated ion chamber (ANL Model IC-15-B-3-2); an analog computer (Donner Model 3200); a dual electronic multiplier (Donner Model 3731); a preset counter (Beckman Model 7424A); a counter-timer (CMC Model 225B Universal); a deposited-carbon, sine-cosine

potentiometer (CIC Model 206); and a digital voltmeter (Beckman Model 4011). A mobile, forced-air-cooled rack housed all measuring equipment except the ion chamber and the digital voltmeter.

The following nomenclature will apply to this section:

E_0 : d.c. component of ion-chamber output, V;

A: amplitude of a.c. component of ion-chamber output, V;

ω : rotational speed of oscillator, rad/sec;

B: amplitude of signal from the sine-cosine potentiometer, V;

E_{O1} : output from integrator No. 1, V;

E_{O2} : output from integrator No. 2, V;

ϕ : phase angle between input and output signals, rad;

n_0 : average neutron density, n/cm^3 ;

Δn : deviation from average neutron density, n/cm^3 ;

t: time, sec;

T: time of integration, sec.

The out-of-core ion-chamber signal (of the form $E_0 + A \sin \omega t$) was brought into the analog computer, and the d.c. component was cancelled out by applying a bucking voltage to the signal. The resulting a.c. signal ($A \sin \omega t$) was amplified and sent to the function multipliers. A signal from the sine tap of the sine-cosine potentiometer was brought to the first function multiplier, and a signal from the cosine tap was brought to the second function multiplier. The two resulting signals, of the form

$$AB \sin \omega t \cdot \sin(\omega t + \phi),$$

and

$$AB \sin \omega t \cdot \cos(\omega t + \phi),$$

were then integrated over an integral number of cycles. The resulting integrator output voltages, E_{O1} and E_{O2} , were

$$E_{O1} = K_1 \cos \phi,$$

and

$$E_{O2} = K_2 \sin \phi.$$

The phase angle was given by

$$\phi = \tan^{-1} \frac{E_{O2} K_1}{E_{O1} K_2},$$

and the fractional amplitude of the power oscillations was given by

$$\frac{\Delta n}{n_0} = \frac{K_3}{T E_0} \sqrt{\left(\frac{E_{O1}}{K_1}\right)^2 + \left(\frac{E_{O2}}{K_2}\right)^2},$$

where the K's were constants determined by the amplitudes of the sinusoidal signals and the values of the components used in the analog circuitry.

4. Data Reduction, Analysis, and Recording Techniques

Three IBM-1620 programs were written for the purpose of reducing data. One program evaluated the theoretical zero-power transfer function for six groups of delayed-neutron fractions and decay constants. By varying β/ℓ^* as input, the program was used to fit: (1) the measured zero-power transfer function at all frequencies; and (2) the absolute phase of the overall power transfer function measured at the maximum frequency.

The second program reduced the data described above from the transfer-function analyzer and calculated the overall reactor transfer function, power oscillation amplitude, and the oscillator dynamic worth. The third program then used the outputs of these two codes as input and calculated the feedback and open-loop transfer functions vs frequency.

An Ampex Model CP-100 FM tape recorder was used for recording the following signals: Sine, cosine, out-of-core chamber, in-core chamber, boiling-fuel-rod central temperature and superheater-fuel-plate temperature.

B. Experimental Procedure

The general procedure used for obtaining data at each frequency consisted of: (1) setting the desired oscillator speed (using the counter-timer); (2) establishing steady power level; (3) adjusting the bucking voltage; (4) turning on the tape recorder; (5) turning on the analog computer; and (6) taking the required data. In addition to the above, the multipliers were balanced before almost every observation, and the computer summers and integrators were balanced daily. The voltage supplied to the sine-cosine potentiometer was checked frequently, as was the continuity (noise caused by wear of the deposited carbon brush) of the sine-cosine potentiometer. If operating conditions were satisfactory, approximately 8 hr were required to obtain the desired 22 frequency points for each power level. This included an average of three observations at each frequency and both forward and reverse rotations of the oscillator rod at the maximum frequency of 15 cps.

The reactor was maintained very near the saturated conditions of 489°F and 600 psig for all the transfer-function measurements. The reactor system was controlled by the following:

1. Reactor pressure-control system, which took the pressure signal to a proportional-plus-reset-plus-rate controller, which, in turn, actuated the main steam back-pressure control valve.

2. Feedwater and reactor water-level control system, which took the level error signal only to a proportional-plus-reset controller, which, in turn, actuated the main feedwater control valve.

3. Control rods, which were operated manually to regulate reactor power level.

The turbine was operating, with generator output dissipated through the water rheostat. No boric acid was present in the reactor water.

The measured data were, in general, reproducible at each frequency. While the data at zero power were actually the least reproducible, the majority of these points fell within 1% in gain and 1° in phase of the calculated curves. The transfer-function data obtained on core PSH-1A were, in general, more reproducible than the data obtained during the experiments on the all-boiling core B-2, in spite of a greatly reduced oscillator worth in core PSH-1 and approximately equal boiling noise levels in the two cores. During most of the runs at power, reproducibility of data was excellent, averaging better than $\pm 1.5\%$ in gain and $\pm 1.5^\circ$ in phase. At least three observations were made at each frequency point, although additional observations were made if the first three runs were inconsistent. It is suspected that improvements in the measuring equipment after the B-2 measurements resulted in more consistent data. Secondly, the signal-to-noise ratio was significantly improved for power measurements over the B-2 experiment by routing the ion-chamber power supply and output signal cables together in a single trench. This change resulted in signal-to-noise ratios as high as 3 to 1 at the resonance frequency and usually better than 1.5 to 1 at all other frequencies. The measurement of feedback transfer functions was made more reliable by determining absolute phase by both forward and reverse rotations of the oscillator rod at the maximum frequency.

C. Experimental Results

This section describes the results obtained from rod oscillator tests at various reactor power levels. The nomenclature used corresponds to the following definition of the overall reactor transfer function:

$$G = \frac{\Delta n}{n_0 \Delta k} = \frac{G_0}{1 + G_0 H}$$

where Δk is the oscillator reactivity worth in dollars, G_0 is the zero-power transfer function, and H is the feedback transfer function.

1. Zero-power Transfer Function (G_0)

The zero-power transfer function (G_0) at operating temperature was determined first in core PSH-1A by taking measurements at 22 frequencies from 0.036 to 15.0 cps at a power level of about 10 kWt. The results are shown along with the power transfer functions in Fig. 113 and are superimposed on a calculated zero-power curve ($\beta = 0.0071$, $\ell^* = 3.22 \times 10^{-5}$ sec). All gain and phase points fell within $\pm 2.0\%$ and $\pm 2.5^\circ$, respectively, of the calculated curve, with the majority of them within $\pm 1\%$ and $\pm 1^\circ$.

The scatter of the zero-power data probably is due to one or both of the following reasons:

- a. Drifts in average reactor power during measurements.
- b. Very low signal-to-noise ratio, about 1.5/1, due to excessive 60-cps pickup, and a low oscillator differential reactivity worth (2.8¢ half peak). The drifts in reactor power level at zero power (and subsequent correction by the reactor operator) during measurements had deleterious effects on the data obtained, since reactor level sometimes drifted as much as 13% in $1\frac{1}{2}$ min. Adverse effects due to noise are probably minimal by comparison, since the analog analyzer inherently rejects extraneous noise if it has a different frequency from the one being analyzed.

2. Power Transfer Function (G)

A comparison of Figs. 113 and 114 shows that the overall response in core PSH-1A has striking similarities to the transfer-function results on the boiling core B-2.⁷ The resonance peaks have similar values vs power, and the trend of the resonance frequency with power is almost identical. The only exception to the similarity is the magnitude of the phase at the major resonance. PSH-1A showed a considerably lesser phase-resonance peak. Likewise, a comparison of Figs. 119 and 120 shows that the PSH-1A feedback gain is consistently lower than that of the B-2 core in the 0.5- to 1.0-cps range, while the feedback phase trends are almost identical in this frequency range. A plausible explanation for this difference is that the presence of the peripheral superheater causes a sufficient change in the radial void distribution to subsequently change the shape of the power-void transfer function.

The data obtained on core PSH-1A were least reproducible at 18.1 MWt. During this run, the reactor power was allowed to drift steadily downward (by a maximum of $\sim 8\%$) because of xenon buildup, but this gradual drift should not have caused the observed scatter in the data. The three

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Fig. 113. Overall Gain and Phase vs Frequency for Various Power Levels, Core PSH-1A.

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Fig. 114. Gain and Phase vs Frequency for Various Power Levels and Oscillator Insertions, Core B-2

highest- and several low-frequency points were not obtained during this initial maximum power run due to an unscheduled reactor shutdown. A point at 15 cps, including both forward and reverse rotation, was obtained several days later at a power level of 19 MWt.

3. Differential-reactivity Worth of Oscillator

As mentioned in Section III-B-7, the half-peak amplitude static differential-reactivity worth of the fueled oscillator in core PSH-1 was measured statically to be 1.7¢ at room temperature and zero power with all control rods banked. This compares with a half-peak value of 3.2¢ on central superheater core CSH-1¹ measured under similar conditions. The half-peak differential reactivity worth of the oscillator measured dynamically at zero power, 489°F, was 2.8¢ on core PSH-1A.

A special transfer-function experiment was run at zero power, 489°F, to determine the increase in oscillator worth with the four outer T-shaped control rods completely withdrawn and the remaining five inner control rods banked. The data, plotted as a single point in Fig. 115, showed a half-peak reactivity worth of 4¢. This change in oscillator worth is believed to be due to reduction in control-rod shadowing effects.

The change in oscillator worth with power was about twice as great in core PSH-1A as for the fully inserted oscillator in core B-2.⁷ The increase in worth probably is determined largely by the unshadowing associated with raising the control-rod bank with power, plus the effect of neutron leakage as the void volume changes with power. Changes in oscillator worth with power for cores PSH-1A and B-2 are shown in Figs. 115 and 116.

4. Neutron Lifetime

The measured mean prompt-neutron lifetime for core PSH-1 was longer than that measured for core B-2 by 10 to 20%, depending upon the power level. In core B-2, the lifetime increased by 40% from zero power to 16.5 MWt, compared with an increase by 30% in core PSH-1A from 0 to 18.1 MWt. Prompt-neutron lifetime was evaluated by a trial-and-error method with the aid of the IBM-1620 Zero-Power Kinetics program described in Section VIII-A-4. First, it was assumed that the exact zero-power phase was determined at each power by the forward and reverse oscillation procedure at 15 cps. Thus, it is implied that negligible feedback existed at this frequency. Since the zero-power break frequency occurs at $\bar{\beta}/l^*$ rad/sec, this ratio could then be theoretically determined by evaluating either the gain or phase of $G_0(s)$ vs frequency. It should be pointed out that $\bar{\beta}/l^*$ could be deduced in this manner because it did not exceed the maximum measured frequency by more than a factor of 3. Since the program could

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Fig. 115. Oscillator-reactivity Worth and Prompt-neutron Lifetime vs Power, Core PSH-1A.

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Fig. 116. Oscillator-reactivity Worth vs Power, Boiling Core B-2

accept any number of values of ℓ^* as input, a series of values was inserted and the proper value of $\bar{\beta}/\ell^*$ was deduced by interpolation, equating the calculated phase lag at the maximum frequency with the measured phase. It was further assumed that the delayed neutron fraction ($\bar{\beta}$) stayed constant with power, thus permitting a direct evaluation of ℓ^* . Prompt neutron life-time vs power is plotted in Fig. 115.

5. Open-loop (G_0H) Response and Stability Determinations

The open-loop (G_0H) Nyquist diagram (Fig. 117) shows that the 180° line is crossed (the loci all pass into the second quadrant), but the loci do not come close to encircling the $(-1,0)$ point. This shows that a wide margin of stability is still present at 18.1 MWt.

To obtain an approximate unstable power level, a linear plot of $|G_0H|$ at $<180^\circ$ was plotted as a function of reactor power and extrapolated linearly to the value of one, as shown in Fig. 118. Phase extrapolation is also shown. This extrapolation assumes a linear system, and although feedback is not completely linear, it is probably the best assumption since the increase in loop gain is proportional to reactor power with secondary effects caused by nonlinearities.

The extrapolation of the experimental data in Fig. 118 suggests an unstable power level of about 80 MWt. However, it should be noted that this required a four-times extrapolation of power level to unity gain, and the greater the extrapolation, the less reliable the result.

6. Feedback (H) Response

The deduced feedback response is shown in Fig. 119 in the form of Bode plots for various power levels. These results are again similar to those for core B-2, shown in Fig. 120, and seem to exhibit a net second-order lag. The slight resonance near 0.6 is due to resonance effects in the power-void transfer function (found by theoretical analysis described in Section VIII-D). The decrease in phase lag at the high frequencies probably is due to a combination of the increasing importance of the prompt gamma heat (about 3% of total) transferred directly to the coolant and also the power-void transfer function. The phase lags are nearly identical at the various power levels, suggesting that the important feedback time constants are nearly invariant with power level.

It is interesting to note that the thermal conductivity of UO_2 , deduced for the PSH-1 boiling core, varies significantly with power, as seen in Table XIV and Fig. 134. Analysis of a theoretical model has shown that the large (2x) thermal-conductivity variations with power have a small effect on feedback phase lag at frequencies above 0.05 cps. However, an increasing conductivity causes a direct and nearly proportional increase in the feedback

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Fig. 117. Open-loop Response (G_0H) for Various Power Levels, Core PSH-1A.

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Fig. 118. Gain and Phase Margins of Open-loop Feedback

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Fig. 119. Gain and Phase of Feedback (H) vs Frequency for Various Power Levels, Core PSH-1A.

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Fig. 120. Gain and Phase of Feedback (H) for Various Power Levels and Oscillator Insertions, Boiling Core B-2

gain in the frequency range of interest. At frequencies below 0.05 cps, increased thermal conductivity causes a reduction in the feedback phase lag.

7. Other Transfer-function Measurements

In addition to the data analysis described above on the out-of-core chamber, a small amount of analysis was made on the boiling-fuel-rod temperature (recorded during rod oscillator experiments) and a noise analysis on the out-of-core chamber signal (recorded during steady-power runs). Due to an insufficient signal-to-noise ratio, the two fuel-temperature signals did not yield usable results. It was calculated that the maximum superheater temperature oscillation was about 3°F peak-to-peak and occurred below 0.05 cps. The maximum boiling-fuel temperature oscillation was also low, reaching a maximum of about 15°F peak-to-peak below 0.03 cps.

The signal-to-noise ratio of the in-core chamber was also too low for analysis on the PSH-1 core. However, the overall transfer function at power measured with the out-of-core and in-core chambers was compared on the B-2 core. Results showed the transfer functions to be similar in shape, and no spatial effects could be detected.

Power noise analog data from the out-of-core chamber was digitized at 0.1-sec intervals from continuous noise signals that had been recorded on magnetic tape. At each power level (5, 10, 15, and 20 MWt), a raw noise and a filtered noise recording were analyzed. The filtered data recording consisted of raw noise that had been passed through a band-pass filter with a low-frequency cut-off of 0.02 cps and a high-frequency cut-off of 15 cps.

The autocorrelation function and power spectrum were calculated for each record. However, examination of the power spectra did not reveal any resonance effects such as had been observed by rod oscillator measurements. Thus, it was concluded that the BORAX-V noise analysis was essentially of no value since it revealed no information on reactor stability or values of feedback parameters.

D. Theoretical-model Studies

A substantial effort was put forth in deriving a theoretical model to describe the measured feedback transfer functions at the various power levels. The results of this model study are described below, and a detailed derivation is given in the Appendix. While a greatly simplified model was used to analyze B-2 core feedback, the analysis for the PSH-1 core included pressure and Doppler effects. It was found, however, that pressure effects were significant only below 0.03 cps. The Doppler effect was also small

compared with the void feedback (less by an order of magnitude), but persisted throughout the frequency range of interest. The theoretical feedback transfer functions are shown in Fig. 121. The feedback was a function of essentially seven parameters, each varying significantly with power level. They consisted of power coefficient, thermal conductivity of UO_2 , natural convection flow rate, slip ratio, subcooling, core flow friction, and steam flow rate. Since many of these characteristics were not measured with a large degree of accuracy or reproducibility, many calculated characteristics were employed: i.e., power coefficient, natural convection flow, slip ratio, subcooling, and core flow friction.

The feedback transfer function behaved about as expected as a function of power level, but somewhat unexpectedly at a given power level. A comparison of Figs. 121 and 119 shows that agreement is generally good at the low frequencies, but that the gain and phase of the measured feedback for frequencies above 5 cps remains substantially higher than calculated. The only plausible explanation for this effect is (1) that the power-void transfer function is not well enough understood at the higher frequencies; or (2) that a systematic error developed at the higher frequencies. Since the boiler region of PSH-1 was an extremely small core, the effects of tuned node points at particular frequencies would be especially significant as revealed by a separate analysis of a more exact power-void transfer function.

However, at frequencies below 5.0 cps, the fit of H to the measured results was sufficient to yield a good comparison between the calculated and measured G transfer functions, shown in Figs. 122 and 113, respectively. Likewise, the calculated and measured Nyquist plots of G_0H in Figs. 123 and 117, respectively, show similar trends with power level.

E. Conclusions

The peripheral superheater apparently has no significant effect on feedback. The transfer-function results for core PSH-1A at power are very similar to those for boiling core B-2, even though the boiler compositions were different. The resonance peaks have similar values vs power, and the trend of resonance frequency with power is almost identical. A great margin of stability is again still present at the maximum power analyzed on core PSH-1A (18.1 MWt). Valid extrapolation of the gain and phase margins to such high powers is questionable, but a power level instability of about 80 MWt is suggested.

The deduced mean prompt-neutron lifetime for core PSH-1A at zero power was 3.2×10^{-5} sec compared with 2.7×10^{-5} sec for the boiling core B-2. An effective delayed-neutron fraction of $\beta = 0.0071$ was assumed in both cases. The power coefficient varied from 0.61 to 0.30% $\Delta k/k/\text{MWt}$ between 5 and 20 MW, respectively. Major fuel time constant decreased

with power from 8.1 to 4.1 sec, as deduced from boiling-fuel-rod center temperature and power-distribution data.

Rod oscillator experiments and stability analysis on both cores B-2 and PSH-1 established it to be a very stable reactor in spite of the high power coefficient. The transfer-function measurements and model-fitting analysis indicated that a high degree of hydraulic stability was present at all power levels. The analysis also revealed that with stable hydraulics the important parameters in the feedback model are power coefficient and fuel thermal conductivity. It was shown that good estimates of these parameters are the essential requirements for predicting reactor stability.

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Fig. 121. Calculated Gain and Phase of Feedback (H)
vs Frequency, Core PSH-1

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Fig. 122. Calculated Overall Gain and Phase (G) vs Frequency, Core PSH-1.

ID-103-A3386

Fig. 123. Calculated Open-loop Response (G_0H) for Various Power Levels, Core PSH-1.

IX. WATER-CHEMISTRY MEASUREMENTS

The purpose of the water-chemistry experimental program, conducted during the operation of the superheating cores in BORAX-V, was to measure and investigate the water-chemistry phenomena associated with an integral boiling water-nuclear superheating reactor. In examining the chemistry of the reactor system during the experiments with core PSH-1, the following parameters were measured:

- a. pH
- b. Resistivity
- c. Suspended and dissolved solids
- d. Corrosion and deposition
- e. Chloride concentration
- f. Steam quality
- g. Radiolytic gas production
- h. Radioisotope concentration
- i. Gross specific gamma activities
- j. Radiation-induced reactions.

A. Water and Steam Purity

Contributing to the successful operation of core PSH-1 was the purity of the makeup water supplied by the makeup-water purification system described in ANL-6302.² The makeup water was passed through a secondary polishing demineralizer, which insured that malfunction or improper operation of the water softener-demineralizer system did not allow chlorides or other undesirable ions to enter the makeup-water storage tank. During the 3-month period of power operation on core PSH-1, 217,000 gallons of water were produced with an average quality of 4 megohm-cm or better. This required ten regenerations of the softener-demineralizer system.

This pure makeup water and the extensive use of the bypass reactor-water demineralizer, DM-1, and the full-flow condensate demineralizer, DM-2, yielded an excellent record of reactor water purity. Except during one special test, the resistivity of the reactor water ranged between 0.5 and 4.8 megohm-cm.

Water resistivity throughout the BORAX-V system was monitored with six conductivity cells, which were continuously recorded on a 12-point recorder, and three cells, which were read directly from a meter. Problems were encountered with the long-range operation of the conductivity cells when the ceramic-type insulator that housed the electrodes slowly

became conductive due to diffusion of electrolytes into the ceramic. Eventually, the cell would read the conductivity of the ceramic, not the water. This problem was overcome by the replacement of the ceramic-type insulator with a machined Teflon insulator. The resistance of the Teflon insulations was approximately 10^5 megohms across the electrodes.

Because of the presence of carbon dioxide generated in the reactor, probably by the decomposition of resin fines and oxidation of graphite lubricant, the purity of the condensed samples of saturated and superheated steam, as measured by resistivity, was much lower than that of the reactor water during short periods of operation. After extended periods of reactor operation, the purity of both the saturated and superheated steam was better than that of reactor water. Figures 124-128 show how the purity of reactor water sampled at the DM-1 inlet, saturated steam from the top of the reactor vessel, superheated steam from the steam line, and condensate at the condensate demineralizer inlet varied during operation. Since CO_2 in small concentrations causes only minor, short-term (months) corrosion problems on aluminum-stainless steel systems, this lower water and steam purity was acceptable.

The performance of the reactor-water-bypass demineralizer, DM-1, and the full-flow condensate demineralizer, DM-2, is indicated by Fig. 129. Generally, a higher flow rate gave a lower resistivity. The resin in each demineralizer was ILCO NR-6 mixed bed.

The flow through DM-1 varied between 0 and 40 gpm, giving a flow loading on the resin of 0 to 4.8 gpm/ft³. The condensate flow through DM-2 varied between 0 and ~95 gpm, giving a flow loading on the resin of 0 to 2.5 gpm/ft³. This low flow loading should have resulted in a higher effluent quality from the condensate demineralizer than from the reactor-water demineralizer. The effluent quality was not higher from DM-2, however, probably because of the much lower quality of the influent to the condensate demineralizer and because of less frequent use. It can be noted in Fig. 129 that at the end of operation the effluent quality from each demineralizer was approaching the same value. During operation, it was observed that as DM-1 was turned on after an idle period, the effluent water was initially much poorer than after a prolonged period of operation. This behavior caused many of the sudden drops in water quality and pH and an increase in radiolytic gas production rate.

The pH of the reactor water during operation varied between 6 and 7, rising during periods of zero power and falling during power operation. Figures 130-133 show some of the variations observed, and also show the pH changes of condensed samples of saturated and superheated steam. Generally, the pH of the steam was between 5 and 6, due to the concentration of CO_2 .

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Fig. 124. Resistivity of Reactor Water, Steam, and Condensate Correlated with Reactor Power vs Time, Core PSH-1A; May 18-27, 1964

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Fig. 125. Resistivity of Reactor Water, Steam, and Condensate Correlated with Reactor Power vs Time, Core PSH-1A; June 1-12, 1964

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Fig. 126. Resistivity of Reactor Water, Steam, and Condensate Correlated with Reactor Power vs Time, Core PSH-1A; June 15-26, 1964

ID-103-A3390

Fig. 127. Resistivity of Reactor Water, Steam, and Condensate Correlated with Reactor Power vs Time, Cores PSH-1A and B; July 6-20, 1964

ID-103-A3391

Fig. 128. Resistivity of Reactor Water, Steam, and Condensate Correlated with Reactor Power vs Time, Core PSH-1C; August 4-14, 1964

ID-103-A3392

Fig. 129. Daily Average Effluent Resistivity from Reactor-Water (DM-1) and Condensate (DM-2) Demineralizers, Cores PSH-1A, B, and C

ID-103-A3393

Fig. 130. pH of Reactor Water and Steam Correlated with Reactor Power vs Time, Core PSH-1A; May 18-June 4, 1964

ID-103-A3394

Fig. 131. pH of Reactor Water and Steam Correlated with Reactor Power vs Time, Core PSH-1A; June 5-18, 1964

ID-103-A3395

Fig. 132. pH of Reactor Water and Steam Correlated with Reactor Power vs Time, Cores PSH-1A and B; June 19-July 10, 1964

ID-103-A3396

Fig. 133. pH of Reactor Water and Steam Correlated with Reactor Power vs Time, Core PSH-1C; August 4-14, 1964

B. Chloride and Ammonia Concentration

Because of anticipated problems with chloride stress corrosion of the Type 304 stainless-steel superheater fuel, one of the most important elements monitored was Cl^- . A Technicon autoanalyzer specifically designed to measure Cl^- was used for continuous monitoring. When the instrument was kept at $70 \pm 1^\circ\text{F}$, it was stable and showed changes in Cl^- concentrations of 0.01 ppm.

Equations (1) and (2) show the chemical principle of the analytical method used in the autoanalyzer.

The Fe(CNS)^{++} ion in Eq. (2) had a distinctive red color and could not form unless Cl^- was present. A saturated solution of mercuric thiocyanate was mixed with a solution of ferric nitrate (67.5 g/liter) and then the filtered sample. Air was added to the solution for mixing, and the color was allowed to develop during the mixing in a helical holdup coil. The solution was then passed through a colorimetric cell, where the color intensity of the Fe(CNS)^{++} ion was read out as chloride concentration. Repeated standardizations were performed to establish confidence in the response of the instrument in the range of 0 to 0.1 ppm Cl^- . This was done by turning a valve to allow either one of two standards to be sampled. Water of 10-megohm-cm quality and 0.1-ppm NaCl solutions were used for standards. The chloride concentration in reactor water or steam was always less than 0.04 ppm and usually was less than the sensitivity of the analyzer.

Determination of ammonia yielded an average of 12 parts per billion in the reactor water and less than 10 parts per billion in the superheated steam. All of the higher concentrations were noted during the first week of operation.

C. Corrosion and Deposition

Several aluminum-alloy corrosion samples were exposed in the reactor during high-temperature and power operation with both cores CSH-1 and PSH-1. The samples were in the form of $1\frac{1}{4}$ -in.-sq plates, 1/16 in. thick. The plates were made into bundles, held together by stainless-steel rods and spacers. Each bundle contained four samples each of X-8001 and A-288 aluminum alloy. The sample bundles were mounted on the outside of the core structure shroud in two locations: the core midplane, and 36 in. above the core midplane.

The aluminum-alloy corrosion samples were removed after reactor shutdown and examined, weighed, and electrolytically defilmed. The outer corrosion product was a dark-brown to dark-gray color. The brown coloration suggested that some steel corrosion product had been deposited on the outer surface.

Four sample bundles were exposed for the same amount of time. All specimens of the same alloy within a single bundle were corroded approximately the same ($\pm 5\%$). The exposure and corrosion data for these samples are summarized in Groups 1-4, Table XIX. It is apparent that the samples exposed on the core midplane suffered more corrosion (10-15%, depending on alloy) than those exposed near the top of the shroud. This may indicate an increase of corrosion with radiation. From out-of-reactor tests in refreshed autoclaves, a metal loss of about 4 mg/cm² would have been predicted from just the high-temperature exposure.

TABLE XIX. Corrosion of Aluminum Specimens, Core PSH-1

Group No.	Samples ^a	Exposure Time, days				Reactor Location	Corrosion, mg/cm ²		
		Temp >204°C	In Air	In Water (cold)	In H ₃ BO ₃ (cold)	H ₃ BO ₃ >204°C	Metal ^b Loss, Average	Actual Coating	Calculated Coating
1	A-288 X-8001	51	26	501	33	5	Shroud top, N.W.	4.19	8.7
								4.60	9.5
2	A-288 X-8001	51	26	501	33	5	Shroud top, S.E.	4.34	8.6
								4.43	9.3
3	A-288 X-8001	51	26	501	33	5	Core midplane, N.W.	4.57	9.6
								5.08	10.5
4	A-288 X-8001	51	26	501	33	5	Core midplane, S.W.	4.65	9.6
								5.22	10.4
5	A-288 X-8001	82	26	649	49	10	Core midplane, S.E.	6.24	11.3
								6.64	11.9
6	A-288 X-8001	84	26	795	151	11	Shroud top, N.E.	8.54	12.7
								8.51	13.0

^aA-288 composition: 0.93 w/o Ni, 0.53 w/o Fe, 0.11 w/o Ti, 0.003 w/o Si.

X-8001 composition: 1.10 w/o Ni, 0.55 w/o Fe, 0.01 w/o Ti, 0.12 w/o Si.

^b7 mg/cm² metal loss is equivalent to a uniform penetration of 0.001 in.

Two other sample bundles were exposed for longer periods (Groups 5 and 6, Table XIX). Since the two sets did not differ substantially in the time at temperatures over 204°C, the increased corrosion of Group 6 compared with Group 5 was probably a result of corrosive attack in the room-temperature reactor water and boric acid solutions.

In the last two columns of Table XIX, the actual weight of corrosion product is compared with that calculated from the metal loss figures. The difference is the amount of corrosion product that was lost to the reactor system. A significant amount of the coating was released after longer exposures.

Based on visual examination of X-8001 aluminum and stainless-steel core structure components after shutdown, the corrosion rate appeared to

be low. No pitting, cracking, or other signs of serious corrosion were noted. The metal surfaces were all covered fairly uniformly with a thin, powdery coating of reddish-brown ferric oxide; under this, on the stainless steel, was the dark-gray-to-black, very thin, hard film noted previously.⁷ The X-8001 aluminum core structure components were coated with Al_2O_3 mixed with the red ferric oxide. Next to the metal, a harder, very thin, black substance was observed.

Deposition samples, $1\frac{1}{4}$ in. sq, of the various alloys used in BORAX-V were placed within the turbine exit casing to determine what isotopes were deposited and how much activity each accumulated, and to note any variations between them. Before the defective-fuel-element tests, these samples were removed, counted, and gamma-scanned after about $1\frac{1}{2}$ months of operation. All samples gave the same gamma spectrum, and the activity counted 10 days after shutdown was as follows:

Alloy	Relative Count Rate, counts/min
Chrome-Moly steel	500
Condenser tube brass	400
X-8001 aluminum	277
Type 304 stainless steel	150

The activity on these deposition samples was due primarily to Co^{58} . Additional deposition sample data obtained during the defective fuel tests are reported in Sections X-A-5. and X-B-6.

D. Suspended and Dissolved Solids

Some idea of the corrosion of materials in the reactor system was also obtained during part of the power operation by measuring the concentration of suspended insoluble solids in the reactor water.

A filtration system was used to determine the concentration of suspended insoluble solids. A cooled sample stream at about 100-300 cc/min was passed through a millipore filter where all particulate matter down to $0.65\ \mu$ was removed. The sample stream then passed through a differential-pressure cell from which a flow-rate signal was sent to the flow-rate indicator and integrator. An average sample taken for one determination was about 195 liters (51.5 gal), and the average weight of an ignited sample was around 25 mg. To obtain the concentration of suspended solids, the number of grams of ignited material was divided by the number of grams of water taken as a sample and the results expressed as ppm.

Equipment failure prevented the daily recording of suspended solids concentration. Typical suspended insoluble solids measured in boiling core B-2 and cores PSH-1A and B are compared in Table XX. Since the values for the cores PSH-1A, PSH-1B, and B-2 are low, the overall corrosion rate was assumed to be low.

TABLE XX. Suspended Insoluble Solids, Comparison of Core B-2 with Cores PSH-1A and PSH-1B

Sample Period	Concentrated Ignited Solids, ppm	Average Analysis, w/o	Remarks
1963		Core B-2	
Jan 30 to Feb 1	0.02	Fe: 20.4	System hot. Various powers from 0 to 20 MWt. Sampled with midvessel probe.
Feb 6 to Feb 8	0.08	Cr: 1.2	
Feb 8 to Feb 13	0.08	Ni: 3.7	
		Al: 41.0	
Date and Time, 1964		Cores PSH-1A and PSH-1B	
June 22 (1000) to June 23 (0830)	0.09		DM-1 off from 1352 to 1418. Al(NO ₃) ₃ injected at 1410.
June 23 (0850) to June 23 (1303)	0.03		
June 23 (1355) to June 23 (1500)	0.83		
June 23 (1510) to June 23 (1840)	2.5		
June 24 (0850) to June 24 (1500)	0.009	Fe: 20.0	Average suspended solids analysis includes Al(NO ₃) ₃ injection.
June 24 (1509) to June 26 (1100)	0.008	Cr: 1.9	
		Ni: 1.3	
		Al: 32.0	
June 26 (1115) to June 26 (1600)	0.002		
July 6 (1600) to July 10 (0930)	0.006		
July 10 (1020) to July 10 (1240)	0.001		
July 10 (1245) to July 10 (1545)	0.001		
July 13 (1400) to July 15 (0900)	0.080		
July 15 (0900) to July 16 (0900)	0.003		

Further information on the major corrosion products formed and collected from the boiling zone of the reactor during this operating period was derived from the analysis of a sample of material taken from the cellulose filters upstream of the reactor-water demineralizer after about 30 days of operation. Table XXI shows the major components present in this material.

TABLE XXI. Analysis of Solids from Cellulose Filter Upstream of Reactor-Water Demineralizer, Core PSH-1A

Compounds (mostly amorphous):	Elements, w/o:	
Major ones identified:	Al: 15	Cu: 0.1
Bayerite - $\beta \text{Al}(\text{OH})_3$	Fe: 10	Zn: 0.1
Silica - SiO_2	Ni: 3	V: 0.05
Iron Oxide - Fe_2O_3 (hydrated)	Cr: 1	Sn: 0.05
	Si: 1	Zr: 0.02
	Pb: 1	Ti: 0.02
	Mn: 0.5	B: 0.01
	Mg: 0.3	Ag: 0.001

The determination of soluble iron in water and steam during operation showed the following concentrations in ppm:

	Reactor Water	Saturated Steam	Superheated Steam	Condensate
Min-Max	0-0.260	0.005-0.200	0-0.600	0.01-0.04
Average	0.046	0.036	0.067	0.023

The higher concentrations at all points generally occurred during the first week of power operation.

E. Radiolytic Gas Production

Prior to operation with core PSH-1, new continuous dissolved-oxygen analysis equipment was developed. A polarographic instrument, developed by Beckman, proved to be suitable both in range and rate of detection of concentration changes. Figure 134 is a schematic diagram of the oxygen-sampling equipment used with this instrument (Beckmann Model 778 Process Oxygen Analyzer).

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Fig. 134. Schematic Diagram, Continuous Dissolved Oxygen Analysis System, Core PSH-1

A sample of steam or water at 600 psig and 480-850°F was led through 1/4-in.-OD Type 304 stainless-steel tubing to the sampling station, where it was cooled to ~25°C and reduced in pressure to ~100 psig. The

sample passed through a special holder where it was directed against the Teflon membrane of the sensor. The sample stream then passed through the back-pressure regulator to waste. The sampling system was maintained at 100 psig, whereby all the oxygen and other gases from the reactor remained dissolved in the water. This not only made it possible for the oxygen sensor to work properly but also allowed the accurate use of the flow- and pressure-regulating equipment. Bubbles in the sample stream could cause all components to malfunction. Accurate measurement of sample stream flowrate was important. If a high sample velocity was not maintained against the Teflon membrane in the sensor, the oxygen concentration read low. A minimum sample flow rate of 150 cc/min was mandatory. A change in flowrate above 150 cc/min did not affect the measurement.

Oxygen concentrations in saturated steam and superheated steam are summarized in Tables XXII and XXIII. Analyses of the data in these tables revealed that the oxygen concentration changed rapidly and unpredictably during unsteady operation and that it held the same value for many hours, if all operating parameters were steady. An interesting fact shown was that the oxygen concentration was independent of reactor power.

TABLE XXII. Oxygen Concentrations in Saturated Steam, Core PSH-1A

Power Level, MWt	Date and Time, 1964	O ₂ Concentration in Saturated Steam, ppm	Resistivity of Reactor Water, MΩ-cm	Reactor Pressure, psig
4.5	May 27 1210	22.5	2.2	600
4.5	1220	24.3	2.2	600
4.5	1230	24.1	2.2	600
4.5	1300	24.8	2.1	600
4.5	1310	26.0	2.1	600
4.5	May 27 1320	27.0	2.1	600
4.5	1330	26.6	2.1	600
4.5	1400	25.1	2.1	600
4.5	1420	25.1	2.09	600
4.5	1440	25.2	2.09	600
5.0	May 27 1150	24.6	2.2	600
5.0	June 9 1900	27.6	2.2	600
5.0	June 24 1120	31.4	2.6	600
5.0	1150	30.3	2.6	600
5.6	May 27 1545	25.2	2.00	600
5.6	May 27 1120	24.5	2.30	600
5.6	1130	25.0	2.35	600
9.1	June 24 1040	30.8	2.6	600
9.1	1100	30.7	2.6	600
9.2	June 10 1130	27.3	2.3	600
9.2	June 10 1170	26.7	2.3	600
9.2	1150	26.7	2.3	600
9.2	1200	26.9	2.3	600
9.2	1210	26.6	2.3	600
9.2	1220	26.5	2.3	600

TABLE XXII (Contd)

Power Level, MWt	Date and Time, 1964		O ₂ Concentration in Saturated Steam, ppm	Resistivity of Reactor Water, M Ω -cm	Reactor Pressure, psig
9.2	June 10	1230	26.6	2.3	600
9.2		1240	26.6	2.3	600
9.2		1320	25.9	2.3	600
9.2		1330	26.6	2.3	600
10.0	June 23	1420	Over 50	0.05	600
10.0	June 23	1440	Over 50	0.05	600
10.0		1500	Over 50	0.05	600
10.0		1520	Over 50	0.05	600
10.0		1532	49.0	0.05	600
10.0		1540	47.8	0.05	600
10.0	June 23	1550	46.3	0.55	600
10.0		1600	44.7	0.62	600
10.0		1720	33.0	0.40	600
10.0		1740	32.4	0.59	600
10.0	June 23	1800	32.0	0.90	600
10.0		1810	31.5	1.11	600
10.0		1822	31.2	1.18	600
10.0	June 24	1200	30.0	2.7	600
10.0		1220	29.6	2.5	600
10.0	June 24	1240	29.6	2.6	600
10.0		1300	29.8	2.5	600
10.0		1320	29.5	2.5	600
10.0		1336	28.5	2.45	600
10.0		1339	35.1	2.45	585
10.0	June 24	1341	28.6	2.45	600
10.0		1400	29.4	2.60	600
10.0		1413	29.2	2.7	600
11.2	June 19	1350	30.0	3.0	600
11.2		1400	30.3	3.0	600
11.2	June 19	1410	30.5	3.0	600
11.2		1424	29.0	3.0	600
11.2		1440	29.4	3.0	600
11.2		1500	30.0	3.0	600
12.0	June 17	1350	27.8	1.90	600
12.0	June 17	1400	27.8	2.05	600
12.0		1410	27.7	2.20	600
12.0		1420	27.5	2.20	600
12.0		1430	28.5	2.20	600
12.0		1300	22.4	3.3	600
12.0	June 17	1320	23.0	3.3	600
12.0		1340	23.0	3.2	600
12.1		1200	22.6	3.0	600
12.1		1230	22.4	3.2	600
12.2		1150	22.6	3.0	600
12.3	June 17	1140	22.3	2.9	600
12.6		1110	22.6	2.9	600
12.8		1100	23.0	2.9	600
12.9		1056	23.0	2.9	600
13.0		1040	26.6	2.1	600
13.2	June 17	1030	27.3	2.1	600
15.4	June 11	1344	30.2	2.05	600
15.4		1350	29.0	2.05	600
15.4		1400	29.0	2.08	600

TABLE XXII (Contd)

Power Level, MWt	Date and Time, 1964		O ₂ Concentration in Saturated Steam, ppm	Resistivity of Reactor Water, MΩ-cm	Reactor Pressure, psig
15.4	June 11	1408	29.5	2.1	600
18.1	June 9	1912	27.3	2.5	600
18.1		1942	27.3	2.5	600
18.1		1950	28.0	2.5	600
18.1		2000	27.7	2.0	600
18.3	June 15	1540	29.0	2.1	600
18.3		1550	30.5	2.1	600
18.3		1600	30.6	2.1	600
18.7		1520	29.0	2.1	600
18.7		1530	29.0	2.1	600

TABLE XXIII. Oxygen Concentrations in Superheated Steam, Core PSII-1A

Power Level, MWt	Date and Time, 1964		O ₂ Concentration in Superheated Steam, ppm	Resistivity of Reactor Water, MΩ-cm	Reactor Pressure, psig
3.4	June 18	1240	17.7	4.0	585
5.0	June 8	1512	17.7	1.4	600
5.0		1514	33.0	1.4	565
5.0		1518	15.5	1.4	613
5.0		1520	26.0	1.4	600
5.0	June 9	1530	25.8	3.0	600
5.0		1532	26.5	3.0	600
5.0		1534	23.5	3.0	605
5.0		1536	26.0	3.0	600
5.0		1538	26.5	3.0	600
5.0	June 18	1340	22.5	4.0	600
5.0		1350	22.8	3.5	600
5.0		1400	23.0	3.0	600
5.0		1500	22.0	3.3	600
5.0		1506	23.7	3.3	600
5.0	June 18	1510	23.7	3.3	600
5.0		1520	22.0	3.2	600
5.0		1530	25.6	3.1	605
5.0	June 18	1536	20.6	3.1	602
5.0		1540	24.0	3.0	600
5.0		1548	21.0	3.0	602
5.0		1553	25.4	3.0	600
9.2	June 10	1056	25.0	2.3	600
9.2	June 10	1110	24.6	2.3	600
9.2		1120	24.0	2.3	600
9.2		1250	24.4	2.3	600
9.2		1300	24.0	2.3	600
9.2		1310	23.4	2.3	600
9.2	June 10	1316	24.8	2.3	600
9.2		1340	23.0	2.3	600
9.2		1350	24.6	2.3	600
9.2		1430	24.0	2.3	600
9.2		1440	24.0	2.3	600
9.2	June 10	1450	25.3	2.3	600
9.2		1500	24.2	2.3	600
9.2		1540	24.6	2.3	600
10.0	May 22	1450	23.0	2.9	608
10.0		1500	26.5	2.9	590

TABLE XXIII (Contd)

Power Level, MWt	Date and Time, 1964		O ₂ Concentration in Superheated Steam, ppm	Resistivity of Reactor Water, MΩ-cm	Reactor Pressure, psig
10.0	May 22	1550	31.0	1.81	585
10.0	June 23	1200	28.0	2.7	600
10.0		1220	28.0	2.7	600
10.0		1240	27.7	2.7	600
10.0		1300	28.5	2.7	600
10.0	June 23	1310	27.0	2.7	600
10.0		1320	26.4	2.7	600
10.0		1330	27.7	2.7	600
10.0		1340	27.8	2.7	600
10.0		1350	27.3	2.9	600
10.0	June 23	1420	Over 50	0.05	600
10.0		1440	Over 50	0.05	600
10.0		1500	Over 50	0.05	600
10.0		1520	Over 50	0.05	600
10.1		1130	29.3	2.7	600
10.1	June 23	1150	28.4	2.7	600
10.2		1120	28.4	2.7	600
10.1	June 17	1510	24.8	2.30	600
10.3		1530	25.4	2.30	600
10.3	June 17	1540	25.6	2.30	600
10.2		1600	25.0	2.35	600
12.0		1440	25.0	2.20	600
12.0		1448	25.6	2.30	600
12.0	June 18	1480	21.4	3.7	600
12.4	June 17	1130	20.0	2.90	600
12.5		1120	19.7	2.90	600
14.0	June 18	1420	26.4	2.2	600
14.0		1430	25.9	2.3	600
14.0		1440	25.7	2.4	600
15.0	May 22	1510	27.0	1.84	595
15.4	June 11	1418	26.5	2.19	600
15.4		1430	26.3	2.10	600
15.4		1450	26.0	2.1	600
15.4		1510	26.2	2.1	600
15.4	June 11	1530	26.3	2.1	600
15.4		1600	26.1	2.1	600
18.1	June 9	1915	25.0	2.5	600
18.1		1920	24.8	2.5	600
18.1		1930	25.0	2.5	600
18.1		1940	25.3	2.0	600
19.4	June 15	1100	26.0	1.81	600
19.4		1110	25.0	1.98	600
19.4		1125	24.4	2.0	600
19.4		1140	23.7	2.0	600
19.4		1150	23.6	2.0	600
19.4	June 15	1200	23.9	2.0	600
19.4		1220	24.0	2.0	600
19.4		1240	24.0	2.0	600
20.0	May 22	1520	27.5	2.90	592
20.0		1530	30.0	1.92	592
20.0		1540	30.8	1.84	592

Expected dependence of O_2 concentration on reactor vessel pressure was confirmed. The process instruments recording reactor water pressure usually were not sensitive enough, nor were the O_2 concentration and pressure recorders time-coordinated well enough, to obtain continuous quantitative data relating these parameters. However, in a few cases, the data were time-coordinated sufficiently and the variations large enough in magnitude to indicate the change in oxygen concentration caused by a given change in pressure or resistivity. On June 8, a variation in reactor pressure occurred over a 6-min period from 600 to 565 to 613 psig and thence to 600 psig. This gave a maximum change of 48 psig and caused a change of O_2 concentration of 17.5 ppm. On June 9, a variation of 5 psig occurred over a 4-min period, causing a change of 3 ppm in O_2 concentration. A third excursion occurred on June 24 when the pressure cycled once from 600 to 585 psig and back, giving an oxygen concentration rise of 6.6 ppm for a pressure decrease of 15 psig. The first and third excursions, considered most reliable because of good data coordination and larger magnitude, gave an inverse oxygen concentration change of 0.36 and 0.44 ppm, respectively, for each psig change near 600 psig.

The changes in oxygen concentration caused by changes in reactor-water resistivity were few, also, but were easily distinguished from those caused by pressure changes. Any change in O_2 due to a resistivity change usually went in one direction only and leveled off in a short time to a steady value. The addition of fresh makeup water to the reactor vessel always caused the O_2 concentration to increase abnormally, but it would later return to the original concentration. Any change in O_2 due to pressure invariably cycled with the pressure. Any general drift in O_2 concentration was due to slow resistivity changes. However, an attempted correlation, at constant pressure, between the day-to-day steady oxygen concentrations and day-to-day reactor water resistivities shown in Tables XXII and XXIII did not show a definite relationship.

To determine the rate of oxygen production in the reactor, a period was selected during which the pressure change was known and the resistivity was fairly constant. The amount of oxygen remaining in the reactor water and the amount entering with the feedwater were determined. Using these data, the curve in Fig. 135, in which oxygen production rate is plotted against power level, was obtained. These production rate data were determined at the following conditions:

Pressure:	600 psig
Temperature:	489°F
Reactor Water:	2 to 3 $M\Omega$ -cm
Sample Point:	In steam dome at 14 ft, $5\frac{1}{2}$ in. (i.e., at same level as lowest inlet to superheater first pass).

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Fig. 135. Oxygen Production Rate vs Power, Core PSH-1A

The production rate for these same parameters could be different at some other time because of the existence of unknown effects, perhaps the concentration of very fine suspended particulate matter of some varying condition on the metal surfaces in contact with the water-dissolved gas phase.

During most of the operating period, the oxygen concentration measured in superheated steam was lower by an average value of 3 ppm than the concentration in saturated steam and was again independent of power level. Table XXIV gives some typical data selected for gas composition of samples of saturated and superheated steam for the same power level. As noted, the ratio of hydrogen to oxygen is essentially the same from samples taken before and after the superheater, with a slight excess of oxygen in both cases. This is due to the average 5.5 ppm of O₂ fed into the system with the feedwater.

TABLE XXIV. Gas Composition in Steam at 5 MWt, Core PSH-1A

Date and Time, 1964		H ₂ , v/o	O ₂ , v/o	N ₂ , v/o	Ratio H ₂ :O ₂
<u>Saturated Steam</u>					
June 18	1510	54.95	30.79	14.26	1.78
	1532	53.55	30.83	15.61	1.73
	1554	52.92	31.30	15.77	1.69
	1617	53.27	31.38	15.37	1.69
<u>Superheated Steam</u>					
June 19	1132	51.90	31.18	16.87	1.66
	1154	51.90	31.52	16.48	1.65
	1216	62.87	37.12	18.97	1.69
	1239	53.08	31.46	15.47	1.68

Initially, i.e., during the first 8-16 hr of operation, while the metal surfaces of the superheater cladding were still relatively corrosion-free, the oxygen concentration in steam from the superheater was approximately 30% of the inlet concentration. Thereafter, the concentrations were as noted above. Analysis of the composition of the gas before and after the superheater shows that this difference was due primarily to recombination and not to corrosion.

Experiments were run at several powers to measure the variation of oxygen concentration with height in the reactor-vessel steam dome. Figure 136 gives a graphic illustration of the data obtained in this investigation. These data were obtained by sampling steam with the movable sample

probe (described in Reference 1) at vertical intervals from below the boiling-water surface to the top of the reactor vessel. The sample was fed into the continuous oxygen-analysis system.

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Fig. 136. Saturated Steam Oxygen Concentrations in Steam Dome, Core PSH-1A

Because of the absence of definite knowledge of the level of the steam-boiling water interface, it is difficult to interpret these data. The indicated reactor-water level shown on Fig. 136 is based on differential-pressure measurements recorded on LR-1. However, these data do raise

doubts as to exactly which spot within the steam dome gave representative oxygen concentrations. Because of these findings, the oxygen concentrations arbitrarily chosen as representative in this report were those given at the same approximate level as the inlets to the first-pass superheater fuel assemblies, i.e., 14 ft, $5\frac{1}{2}$ in. above the bottom of the reactor vessel.

Hydrogen-addition and oxygen-addition tests were also attempted and gave results similar to those found in BORAX-IV.¹² When the hydrogen concentration in the gas stripped from the condensed steam reached 88%, the oxygen concentration in this gas was just under 1%. Adding oxygen did not substantially reduce the hydrogen concentration. Experimental conditions were not good during these two tests, and malfunction of analysis equipment resulted in poor data.

F. Gross Specific Activity

During operation of core PSH-1A before the defective-fuel tests, the gross specific activities of reactor water, condensed saturated and superheated steam, and air-ejector exhaust gas were measured at various power levels. Gamma-ray spectrometry revealed the identity of some of the isotopes responsible for the activity. Table XXV gives the gross specific activities observed for reactor water and steam. The isotopes responsible for most of the activity were Na^{24} in the reactor water and Xe^{133} , Xe^{135} , Kr^{87} , Kr^{88} , and their daughters in the condensed-steam samples and air-ejector exhaust samples. The reactor-water activity increased as expected with power, but was always reduced at any power level if the reactor-water demineralizer, DM-1, was operated.

TABLE XXV. Gross Specific Gamma Activity of
Water and Steam Samples, Core PSH-1A

Power Level, MWT	Date and Time, 1964	DM-1 Flow, gpm	Reactor Water from Mid-downcomer, counts/min/ml	Saturated Steam from Steam Dome, counts/min/ml	Superheated Steam from Line 30 ft from Reactor, counts/min/ml
1	May 19 1145	6.5	8,000	400	400
1	May 22 1245	0	7,400	3,500	3,000
1-2	May 18 1615	0	4,300	1,100	1,300
3	May 22 1445	0	18,000	2,500	3,100
5	May 19 1445	6.5	39,500	2,000	4,700
5	May 20 1145	0	57,200	2,400	3,600
5	June 9 1715	9	91,000	3,300	6,900
6	May 25 1325	0	12,400	1,500	9,900
10	May 20 1525	0	178,000	6,200	9,100
10	June 23 1535	32	145,000	3,600	4,000
15	May 20 1650	0	268,000	-	9,700
15	May 20 1720	0	306,000	6,200	9,300
19	June 8 1450	30	146,000	7,600	4,700
19-20	May 20 1800	0	394,000	13,400	13,400
20	May 22 1530	14.5	78,700	6,800	5,500

The gross gamma activity of grab samples of the condenser-air ejector exhaust gases, measured at several reactor powers, is shown in Table XXVI. These activities did not follow power-level changes consistently. This may have been caused by the air leaks into the main condenser discussed in Section V-D-3.

TABLE XXVI. Gross Specific Gamma Activity of
Air-ejector Exhaust, Core PSH-1A

Power Level, MWt	Date and Time, 1964		Activity, counts/min/ml
2.5	June 3	1350	125
2.5		1358	175
3.0	June 5	1512	3,000
8.0		1417	550
8-9	June 4	1325	1,625
10.0	June 5	1040	3,500
14.0	June 4	1355	26,250
14.0		1427	10,000
14.2	June 5	1445	27,500
18.2		1110	41,750

The greatest activity in the reactor water after the decay of N^{16} from the $[O^{16}(n, p) N^{16}]$ reaction was Na^{24} . The origin of the large amount of Na^{24} from nonaluminum-clad fuel was of interest, and several experiments were run in an attempt to determine the source of this activity. Sodium was suspected to have come from the sodium hydroxide used for resin regeneration, leaching through the makeup-water demineralizer to the reactor. However, water samples taken from the effluent of this demineralizer and irradiated in the thermal column of the Argonne Fast Source Reactor yielded no activated sodium. Similarly, water samples taken from the reactor vessel and irradiated showed Na^{24} from the $[Na^{23}(n, \gamma) Na^{24}]$ reaction. However, the Na^{24} was not quantitatively determined.

A capsule containing aluminum wire (99.999% pure) irradiated in BORAX-V for 10 min at 10 MWt yielded Mg^{27} from the $[Al^{27}(n, p) Mg^{27}]$ reaction and Na^{24} from the $[Al^{27}(n, \alpha) Na^{24}]$ reaction, after the decay of the short-lived Al^{28} from the $[Al^{27}(n, \gamma) Al^{28}]$ reaction. Thresholds for the first two reactions are 2.1 and 6 MeV, respectively. In addition, another test was made in which aluminum nitrate was injected into the reactor water and the activity of the water checked for a period before and after injection. There was no observable increase in Na^{24} , but early operation of the reactor-water demineralizer interfered with the experiment.

The Na^{24} production can be attributed to both the neutron bombardment of aluminum that was dissolved in the water and bombardment of the core shroud, fuel-assembly boxes, and flow rods, all of which were of X-8001 aluminum alloy. The fast neutrons necessary for this reaction were not seriously attenuated before contacting the aluminum. Because the core was slightly undermoderated, the reaction probability was enhanced due to the higher neutron energies. The aluminum structures could have yielded Na^{24} from the sweeping action of the water on Na^{24} produced on the aluminum surfaces. Also, Na^{24} could have been released by recoiling into the water through a thin layer of aluminum. The recoiling Na^{24} nucleus was produced from the alpha-particle decay of the Al^{28} compound nucleus. The Na^{24} activity in the water varied with operation of the reactor-water demineralizer and rose to 5×10^5 counts/min/ml at 15 MWt after 2 hr of zero flow through the demineralizer.

A determination for tritium was performed on several samples taken before the defective boiler fuel tests and during the tests. The tritium contents of reactor water and saturated and superheated steam ranged from 1.390×10^{-3} to 2.770×10^{-3} $\mu\text{Ci}/\text{ml}$. The highest values obtained were from the samples taken at a power of 10 MWt before the defective fuel tests. The tritium activity was approximately the same for reactor water and saturated and superheated steam for each set of samples.

The low values of tritium concentration were expected, since venting steam to atmosphere and frequent blowdowns, as well as constant sampling, would prevent any large buildup. It is theorized that tritium is produced by a ternary fission mechanism and escapes through thinly clad fuels. This does not preclude the possibility that the tritium is from other sources. The applicable safety limit for tritium release to the atmosphere is 3×10^{-2} $\mu\text{Ci}/\text{ml}$ based on a continuous release rate for 160 hr/week.

G. Fission-product Monitor

The fission-product monitoring system for BORAX-V was a dual-channel modified Tracerlab, Inc., Type MWP-1A, Process Water Fission Products Monitor. One channel detected gamma rays from radioactive cesium in a sample stream of either reactor water or condensed saturated steam; the other channel detected gamma rays from a mixture of radioactive iodine isotopes in a sample stream of condensed superheated steam.

A schematic diagram of the fission-product monitoring system is shown in Fig. 137. In the channel used to detect radioactive cesium, a sample stream of saturated steam or reactor water at 600 psig was passed through a condenser that cooled the sample to approximately 20°C , then through a flow-control valve before passing through a mixed-bed resin column. All the cationic and anionic substances remained in the resin,

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Fig. 137. Schematic Diagram of Fission-product Monitors

while the noble fission gases were carried through. The noble-fission-gas isotopes of xenon and krypton were allowed to decay to cesium and rubidium in a delay tank. The cesium and rubidium were then concentrated on a cation column contained in a lead shield.

In the channel for detecting radioactive iodine, the sample stream of superheated steam was passed through a condenser, a flow-control valve, and a cation column. The noble fission gases and anionic substances, including radioactive iodine, passed through the resin column. The anionic substances and radioactive iodine were then concentrated on a shielded anion resin column.

In the system for monitoring saturated steam or reactor water, the gamma-ray spectrometer log-count-rate meter was discriminated for the energies of the gamma rays from Cs^{138} ; in the system for monitoring superheated steam, the spectrometer was discriminated for the energies of gamma rays from the isotopes I^{131} , I^{133} , I^{135} , etc.

Back-pressure regulators maintained 80 psig back pressure on both systems. The back pressure aided in maintaining constant flowrates through the resin columns and inhibited the formation of air bubbles.

The fission-product monitor proved to be an effective and stable instrument throughout the operation of core PSH-1. Any increases in the established level of fission-product activity were indicative of fuel-cladding failure or defects. Values obtained during normal operation and during the defective fuel tests showed that during extended power operation the readings tended to follow reactor power. However, because of the very low activities and count rates, the readings sometimes wandered. This was caused by activity buildup and decay on the resin columns. Resin was changed every 2 to 5 days during power operation.

Table XXVII illustrates the fission-product monitor readings during normal operation with no known fuel defects. The monitor readings during the fuel defect tests are reported in Section X.

TABLE XXVII. Activity from Fission-product Monitors During Normal Power Operation, Core PSH-1

Power Level, MWt	Date, June 1964	Saturated Steam Activity, counts/min	Superheated Steam Activity, counts/min	Power Level, MWt	Date, June 1964	Saturated Steam Activity, counts/min	Superheated Steam Activity, counts/min
2.5	8	300	300	11.5	24	425	1,450
5.0	26	200	500	12.0	23	400	1,500
6.0	8	250	600	14.5	17	350	1,400
9.0	3	600	1,000	14.5	18	400	1,550
10.0	23	360	1,200	15.0	22	450	1,700
11.0	24	400	1,600	17.6	22	400	1,850
11.3	24	400	1,400	18.0	5	2,000	2,000

The readings obtained from the fission-product monitors remained quite stable during increase in reactor-water activity. The activity readings were not affected by the oxygen and hydrogen addition tests; during the aluminum nitrate test, the saturated-steam-monitor reading did not change, and the superheated-steam-monitor reading increased by only 300 counts/min.

Half-life measurements were obtained from recorder charts during normal power operation by shutting off sample flow at 11.2 MWt and leaving the recorder on. The activity on the superheated-steam resin column had a 160-min half-life, while the activity on the saturated-steam resin column decayed with a half-life of 28 min for the first 1.5 hr. The superheated-steam resin activity then leveled off with an average 3.6-hr half-life; the saturated-steam resin activity decayed completely within 3 hr. The primary factor that increased half-life was the presence of long-lived activation products that leached through the first resin column and slowly built up on the second resin column, which was being monitored.

During short, high-power runs of less than 30 min, activities measured by the fission-product monitor were slightly lower than expected, since the resin did not have sufficient time to equilibrate the activity build-up. Another case of unlikely readings occurred during an atmospheric inversion period when increased radon activity in the atmosphere was measured on the superheated-steam monitor.

A series of readings obtained immediately after the reactor was shut down showed an activity increase, and then eventually the normal decay was measured. One such event occurred during the superheater fuel defect test in which the activity measured on the superheated-steam monitor just before shutdown was 16,000 counts/min, after which the activity increased to 150,000 counts/min 1 hr after shutdown. The activity then decayed in the normal manner. A possible explanation for this event is that the back pressure maintained in the resin column was manually released after shutdown, and a siphoning effect drew water from the resin column, thereby removing shielding to the counter and giving a higher activity reading.

H. Steam Quality

The sodium-tracer technique was used to measure the amount of moisture in saturated steam sampled from the steam dome of the reactor vessel. It was unnecessary to add Na^{23} to the reactor water for this experiment because of the presence of sufficient Na^{24} formed by the n, α reaction on aluminum. (See Section IX-F.) Counts on Na^{24} in reactor-water samples, taken from the sampling probe at the midvessel location in the downcomer, were compared with counts on saturated steam samples taken at various levels from 12 to 15 ft by means of the movable steam-sampling probe.

The data obtained at 10 MWt are plotted in Fig. 138. Although the curve shows zero moisture at 13 ft above the bottom of the reactor vessel and higher, some slight moisture entrainment occurred. This was discovered when deposition samples in the steam line, removed shortly after a reactor shutdown, proved to have a strong spectrum of Na^{24} . Counting statistics on this experiment were such that a moisture content of less than 0.5 w/o could not be distinguished from zero.

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Fig. 138. Steam Quality Profile in Steam Dome, Core PSH-1A

X. DEFECTIVE-FUEL TESTS

Due to the unexpectedly high release of radioactive iodine (I^{131}) from a cladding failure in the annular UO_2 fuel element SH-2 in the Superheat Advanced Demonstration Experiment (SADE) loop in the VBWR, it was postulated that a superheater fuel failure would be a serious problem in a nuclear superheat reactor. Preliminary data from SADE indicated that 10^6 times as much iodine would be carried over with the steam in a superheating system as compared with the steam released from a boiling-water reactor.¹³ It was considered conceivable that a defect in superheater fuel could release enough fission-product activity into the steam system to make long-term system contamination and personnel exposure a major problem. If this were the case, the economic hazard of steam system cleanup, replacement of contaminated components, and personnel protection in the event of superheater fuel defects might preclude the use of nuclear superheat in a boiling-water reactor.

The BORAX-V reactor with the peripheral superheater core was operated with prepared defective fuel in both the boiler region and the superheater region. The principal objective of this experiment was to compare the seriousness of operating the reactor with defective superheater fuel to that of operating with defective boiler fuel. Operating a boiling-water reactor with defective clad oxide fuel had been shown to present no serious hazards.¹⁴ Defect sizes were considered typical of what might be expected in an operating reactor.

Measurements made during the reactor operation included the following:

- a. Radioactivity levels of the steam plant equipment.
- b. Isotopic identification of the fission products in the steam system.
- c. Analyses of gamma counts and gamma spectra samples from reactor water, condensed steam before the turbine, and condensed steam after the turbine.
- d. Airborne radioactivity downwind from the reactor.
- e. Determination of the half-life of deposited fission products.

The results showed that operating with defective, stainless-steel-clad, UO_2 -stainless steel cermet superheater fuel does not present a hazard substantially greater than operating with defective stainless-steel-clad UO_2 boiler fuel. Activities in the steam system from the defective superheater fuel sample were of the same order of magnitude as those from ten defective boiler fuel rods, but the defective superheater fuel resulted in more system contamination than the defective boiler fuel. However, in either case the reactor could have been operated for extended periods of time at maximum

power without creating either a serious radiation hazard to personnel or a long-term contamination problem.

A. Defective-boiler-fuel Test

The defective-boiler-fuel experiment in core PSH-1 was run in two stages: First, a single boiler fuel rod containing partially enriched UO_2 of about 91% theoretical density was defected and placed in the reactor. The rod was prepared by drilling a 1/8-in.-diam hole through the 0.015-in. Type 304 stainless-steel cladding at the vertical center of the fuel. After a week's operation, 33 MWdt, the fission-product levels in the steam system were barely detectable; therefore an additional nine fuel rods were made defective and installed in the boiler region of the core. Each of these nine rods contained two 1/8-in.-diam holes in the cladding, 180° apart, located 1 in. above the bottom of the fuel, and two 1/8-in.-diam holes in the gas space above the fuel. The reactor was operated at power levels of 10 to 14 MWt for most of the experiments. Essentially all of the steam was routed through the turbocondenser system.

1. Radiation Surveys

During the defective-fuel tests, all personnel in the reactor building and the turbine building area were under control of a health physics representative. Radiation surveys in the reactor and turbine buildings were carried out on a scheduled basis, and a high-volume air-sampling station was set up about 150 ft downwind from the reactor area. All persons in the reactor area were required to wear film badges and dosimeters. Dosimeter readings were reported and recorded on a daily basis, and film badges were read on a biweekly basis unless dosimeter readings dictated a special reading.

Radiation levels in various parts of the plant system were measured with gamma chambers mounted directly on the pipe or equipment. The installation of defective boiling fuel rods in the reactor resulted in slightly increased radiation levels throughout most of the plant, as shown in Table XXVIII. The exception to this was the charcoal filter in the air-ejector exhaust system. No increase in radioactivity was observed at the air monitoring system downwind of the reactor area.

2. Isotopic Analysis

Analyses were made to identify the isotopes responsible for the radioactivity in the reactor water and steam.

In the saturated and superheated steam, most of the radioactivity was found to come from the rare gases and their daughter products and some radioiodine. Examination of the reactor water showed that most of the

activity was from Na^{24} from the $[\text{Al}^{27}(\text{n}, \alpha) \text{Na}^{24}]$ reaction; which was so intense that nearly all other radioactive isotopes were masked out. Radioiodine was identified in the reactor water, but its specific activity was very low compared to that of the Na^{24} .

TABLE XXVIII. Plant Radiation Levels with Prepared Defective Fuel, Cores PSH-1B and C

	Defective Boiler Fuel Test			Defective Superheater Fuel Test	
	0	1	10	1	1
Number of Defective Units	0	1	10	1	1
Power Level, MWt	11.3	11-12	11-12	12.3	18.9
Sampling Location	Radiation Level, mR/hr				
Main steam line in reactor bldg., 42 ft from reactor vessel	65	80-85	90-100	95-100	150
Main steam line in turbine bldg., 200 ft from reactor vessel	40	30-40	45-52	40-50	90
Condenser hotwell	26	13-33	60-80	40-45	110
Air-ejector exhaust charcoal filters	45	45-70	2000-2900	130-150	210
Line to reactor water demineralizer, DM-1	47	28-45	33-85	60-85	75
Condensate return line	8	3-8	30-40	12-15	20

Special analyses were performed to detect Mo^{99} , U, and Cs. Mo^{99} was present in trace amounts, but no uranium was found in the coolant. No cesium was detected by the time the analysis was complete (~16-hr decay time).

To gain some indication of the fission-product release, I^{131} determinations were made on reactor water, saturated steam, and superheated steam. These values are absolute in terms of dis/min/ml of cooled sample. Table XXIX gives the results of these determinations. All values were corrected to sampling time. Before the defect tests, I^{131} determinations yielded negative results. The following information is deduced from this table: (1) The fission product iodine remained mostly in the reactor water; (2) there was a fairly low level of iodine in the steam; (3) the superheater removed up to 92% of the iodine in the saturated steam when the inlet concentration was high; and (4) the iodine concentration in the reactor water was mainly dependent on flow through the demineralizer.

TABLE XXIX. I^{131} Activity, Defective Boiling-fuel-rod Tests,
Core PSH-1B

Power Level, MWt	Date and Time, 1964	Demineralizer DM-1 Flow, gpm	^I ¹³¹ Activity, dis/min/ml		
			Reactor Water	Saturated Steam	Superheated Steam
<u>Single Defective Rod</u>					
0.0	July 7 1016	0	16	4	4
5.0	1258	13.5	8	4	4
11.8	1421	24.5	5	4	4
11.5	July 8 0850	36	13	5	6
10.0	July 9 1415	25	36	5	5
5.0	July 10 1125	17	13	5	5
<u>Ten Defective Rods</u>					
4.5	July 14 1030	0	180	20	10
20.0	1230	39	925	22	9
20.0	1343	39	550	13	6
18.0	1525	39	188	6	6
10.5	July 15 0843	35	136	10	5
10.5	1045	35	115	8	6
9.8	1330	0	240	10	4
9.5	1540	0	515	16	4
9.5	July 16 0855	0	371	26	5
9.5	1052	0	742	24	5
9.5	1318	0	1110	52	18
9.5	1534	0	1590	70	27
11.6	July 17 0854	35	438	26	5
18.8	July 20 1518	35	1920	122	98

Decontamination factors for I^{131} (i.e., ratio of activity from I^{131} in a sample of reactor water to the I^{131} activity in an equal-volume sample of condensed superheated steam) are shown in Table XXX.

TABLE XXX. Decontamination Factors for I^{131} , Defective Boiling-fuel-rod Tests, Core PSH-1B

Power Level, MWt	Date and Time, 1964	Demineralizer DM-1 Flow, gpm	Decontamination Factor		Reactor Water Conditions		
			Saturated Steam	Superheated Steam	pH	Resistivity, MΩ-cm	
<u>Single Defective Rod</u>							
0.0	July 7 1016	0	4	-	6.4	2.9	
5.0	1258	13.5	2	2	6.3	2.5	
11.8	1421	24.5	1.2	1.2	6.3	2.5	
11.5	July 8 0850	36	2.6	2	6.3	2.6	
10.0	July 9 1415	25	7	7	6.2	1.9	
5.0	July 10 1125	17	2.6	2.6	6.2	2.1	
<u>Ten Defective Rods</u>							
10.5	July 14 1030	0	9	18	-	1.6	
10.5	1230	39	42	103	-	1.6	
9.8	1343	39	42	91	-	1.7	
9.5	1525	39	31	31	-	1.7	
10.5	July 15 0843	35	13	27	-	1.8	
10.5	1045	35	14	19	-	1.8	
9.8	1330	0	24	60	-	1.9	
9.5	1540	0	32	128	-	1.9	
9.5	July 16 0855	0	14	72	-	2.2	
9.5	1052	0	31	148	-	2.4	
9.5	1318	0	32	62	-	2.8	
9.5	1534	0	23	59	-	2.4	
11.6	July 17 0854	35	17	87	-	2.3	
18.8	July 20 1518	35	16	20	-	2.4	

3. Specific Gamma Activity of Water and Steam

Gross specific gamma activities of the reactor water and steam were determined during the defective boiler-fuel test. Table XXXI gives the results of these measurements. For comparison, activities are also given for periods when no defective fuel was in the reactor. Each value in this table is corrected to 20 min after sampling time.

TABLE XXXI. Gross Specific Gamma Activity Comparisons for Defective Boiling-fuel-rod Tests, Core PSH-1B

Power Level, MWt	Date and Time, 1964	Demineralizer DM-1 Flow, gpm	Reactor Water S-4	Activity, counts/min/ml		
				Saturated Steam S-14	Superheated Steam S-13A	Condensate S-7
<u>No Defective Fuel</u>						
10.0	May 20 1530	0	178,000	6,200	9,100	3,650
19.0	June 8 1520	30	147,000	7,600	4,700	-
5.0	June 9 1717	0	91,000	3,300	6,900	-
12.0	June 17 1319	0	383,000	4,900	24,000	-
10.0	June 23 1250	0	85,000	2,400	3,500	-
<u>Single Defective Rod</u>						
0.0	July 7 1105	0	6,000	730	-	-
5.0	1330	13.5	53,000	2,400	3,000	-
11.8	1445	24.5	89,000	2,800	3,400	1,700
11.5	July 8 0910	36	98,000	3,600	3,600	2,000
10.0	July 9 1433	25	134,000	3,200	4,600	-
5.0	July 10 1145	17	113,000	3,800	4,100	-
<u>Ten Defective Rods</u>						
4.5	July 14 1050	0	79,000	146,000	71,000	-
20.0	1253	39	144,000	283,000	78,000	-
20.0	1409	39	162,000	248,000	146,000	-
18.8	1545	39	174,000	309,000	112,000	-
10.5	July 15 0915	35	109,000	296,000	159,000	8,600
10.5	1117	35	111,000	284,000	71,000	5,500
9.8	1345	0	219,000	307,000	70,000	8,700
9.5	1600	0	428,000	261,000	60,000	-
9.5	July 16 0915	0	201,000	297,000	76,000	6,900
9.5	1115	0	412,000	256,000	64,000	11,000
9.5	1335	0	581,000	310,000	150,000	9,500
9.5	1555	0	732,000	301,000	157,000	9,200
11.6	July 17 0915	35	131,000	371,000	187,000	7,700
18.8	July 20 1600	35	142,000	182,000	123,000	-

Activities were hardly changed with the introduction of one defective fuel rod into the reactor. However, with ten defective rods, the intensity of activities increased by two orders of magnitude in the steam. Only a slight increase was observed in the activity in reactor water.

Activity in the reactor water was strongly dependent on the flow rate through the reactor-water demineralizer, DM-1, and reactor power level; activity in the steam showed a strong dependence on power level only. For example, during the first day of the test with ten defective rods (see Table XXXI, July 14), the saturated-steam activity had increased by a factor of 77, making it 70% higher than the activity of the reactor water, which showed only a moderate increase. By July 15, the average activity of the reactor water had built up, possibly by an increase of iodine concentration and nonvolatile daughters (e.g., Ba^{140} , La^{140}). On this date, the averages show the saturated-steam activity to be only 30% higher than the activity of the reactor water. On July 16, the reactor-water demineralizer was turned off and the activity changes noted. The reactor water activity increased by 120%, and the saturated-steam activity remained essentially unchanged.

Decontamination factors for gross specific gamma activity (i.e., the ratio of gross activity from a sample of reactor water to the gross activity of an equal-volume sample of superheated steam) are summarized in Table XXXII.

The decay data taken on samples of water and steam in the reactor system are shown below, presented in approximate effective half-lives for each point sampled. Note that the decay of the reactor water was essentially that of the 15-hr Na^{24} .

Time after Sampling, hr	Approximate Half-life, hr		
	Reactor Water	Saturated Steam	Superheated Steam
1-2½	15	1½	1½
2½-6	15	2½	2½
6	15	9	9

Considerable decay occurred during the first hour after sampling, but no quantitative data were available.

TABLE XXXII. Gross Specific Gamma-activity Decontamination Factors,
Defective Boiling-fuel-rod Tests, Core PSH-1B

Power Level, MWt	Date and Time, 1964	Demineralizer DM-1 Flow, gpm	Decontamination Factor		pH	Resistivity, MΩ-cm
			Saturated Steam	Superheated Steam		
<u>No Defective Fuel</u>						
10.0	May 20 1530	0	28.7	19.6	6.6	-
19.0	June 8 1520	30	19.3	31.3	6.7	-
5.0	June 9 1717	0	27.6	13.2	6.6	-
12.0	June 17 1319	0	78.2	16.0	6.4	2.4
10.0	June 23 1250	0	35.4	24.3	5.8	2.6
<u>One Defective Rod</u>						
0.0	July 7 1105	0	8.2	-	6.4	2.9
5.0	1130	13.5	22.1	17.7	6.3	2.5
11.8	1445	24.5	31.8	26.2	6.3	2.5
11.5	July 8 0910	36	27.2	27.2	6.3	2.6
10.0	July 9 1433	25	41.9	29.1	6.2	1.9
5.0	July 10 1145	17	29.7	27.6	6.2	2.1
<u>Ten Defective Rods</u>						
4.5	July 14 1050	0	0.5	1.1	-	1.6
20.0	1253	39	0.5	1.8	-	1.6
20.0	1409	39	0.6	1.1	-	1.7
18.0	1545	39	0.6	1.6	-	1.7
10.5	July 15 0915	35	0.4	0.7	-	1.8
10.5	1117	35	0.4	1.6	-	1.8
9.8	1345	0	0.7	3.1	-	1.9
9.5	1600	0	2.0	7.1	-	1.9
9.5	July 16 0915	0	0.7	2.6	-	2.2
9.5	1115	0	1.6	6.5	-	2.4
9.5	1335	0	1.9	3.9	-	2.8
9.5	1555	0	2.4	4.7	-	2.4
11.6	July 17 0912	35	0.4	0.7	-	2.3
18.8	July 20 1600	35	0.9	1.2	-	2.4

4. Activities from Fission-product Monitors

The count rates recorded by the fission-product monitors during the defective boiling-fuel-rod tests are summarized in Table XXXIII. The details of the fission-product monitors and readings during normal operation are presented in Section IX-G. During the single-defective-rod test, the readings on the superheated-steam monitor were about the same as for normal operation, but the readings increased on the saturated-steam monitor. The fission-product monitor gave fairly steady readings for saturated steam and for superheated steam during the test with ten defective rods.

TABLE XXXIII. Activities from Fission-product Monitors, Defective Boiling-fuel-rod Tests, Core PSH-1B

Power Level, MWt	Date, 1964	Saturated-steam Activity, counts/min	Superheated-steam Activity, counts/min
<u>One Defective Rod</u>			
3.0	July 7	400	700
10.0	7	700	1,800
10.4	9	850	1,600
10.6	10	800	1,400
11.0	9	850	1,500
11.8	8	1,000	2,000
<u>Ten Defective Rods</u>			
9.4	July 15	120,000	26,000
11.3	16	155,000	28,000
12.0	15	160,000	40,000
12.0	17	165,000	31,000
15.0	14	150,000	42,000
22.0	14	170,000	60,000

A comparison of the half-lives of fission-product monitor resin activity measured during the superheater fuel defect test showed the saturated steam with a $5\frac{1}{2}$ -hr half-life and the superheated steam with a half-life of 3.45 hr for the first 5 hr and half-lives of $15\frac{1}{2}$ and $14\frac{1}{2}$ hr, respectively, for the next 60 hr.

5. Steam-system Contamination

Deposition samples were placed in the steam system at the following points:

Location	Pipe-line Distance from Center Line of Reactor Vessel, ft	Material	Geometry
a. Superheated steam manifold	24	Type 304 stainless steel	$\frac{1}{2}$ -in.-diam rod
b. High-pressure steam line	46	Carbon steel	$\frac{1}{2}$ -in.-diam rod
c. Low-pressure steam line	69	Carbon steel	$\frac{1}{2}$ -in.-diam rod
d. Turbine exit	300	Type 304 stainless steel $1\frac{1}{2}$ Cr- $\frac{1}{2}$ Mo steel Admiralty brass Carbon steel	$1\frac{1}{4}$ -in.-sq wafers

The samples were constructed of material similar to that of the system wall, with the exception of the turbine exit where several materials were used. In each case, the sample was placed near the system wall, thus giving an approximation of the radioactivity being deposited on the walls.

The predominant isotopes deposited on the samples, as determined by gamma-ray spectrometry, were Ba^{140} and La^{140} . In addition, the following isotopes were identified in small amounts on the deposition samples: Sr^{91} , Sr^{92} , Rb^{89} , Rb^{90} , Cs^{138} , and Co^{58} .

Deposition activity of Ba^{140} on the sample rods placed in the steam line was as follows:

Stainless-steel rod in manifold, 24 ft: $7.3 \times 10^{-4} \mu\text{Ci}/\text{cm}^2$;

Carbon-steel rod in high-pressure line, 46 ft: $4.2 \times 10^{-3} \mu\text{Ci}/\text{cm}^2$;

Carbon-steel rod in low-pressure line, 69 ft: $4.6 \times 10^{-4} \mu\text{Ci}/\text{cm}^2$.

These values are corrected to the time of shutdown.

The large area of the superheater, coupled with a high steam-flow Reynolds number, resulted in considerable deposition in the superheater assemblies. The data in Table XXXII indicate that approximately one-half of the specific gamma activity in the saturated steam is plated out during the superheating process. No other quantitative data are available.

B. Defective-superheater-fuel Test

The prepared sample of defective superheater fuel was a 1-in. x 1-in. x 0.020-in. piece of fully loaded, peripheral superheater fuel plate.

The Type 304-I, stainless-steel-clad cermet plate contained 31.8 w/o highly enriched UO_2 dispersed in a matrix of Type 304-B stainless steel. The cladding was removed from one side of the sample by selective removal of the stainless steel with aqua regia, thus exposing the imbedded UO_2 particles. The presence of the particles was confirmed by microscopic examination.

The sample was installed in the 0.060-in.-thick central steam coolant channel of a fresh second-pass superheater fuel assembly located in core position 16 of core PSH-1C (see Fig. 28). The sample was located 8 to 9 in. above the bottom of the fuel and was held by a support strip and rod with a bayonet latch, as shown in Fig. 139. An 0.018-in.-diam stainless-steel spacer wire was electron-beam-welded to each side of the two edges of the sample parallel to the steam flow path.

For this test, special steam-sampling lines were connected to the top of the riser on the superheater fuel assembly containing the defective sample and to the top of the riser of a normal second-pass assembly. This allowed comparison of samples of steam from both the defective superheater assembly and

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Fig. 139. Defective Superheater-fuel-plate Sample and Superheater Assembly, Core PSH-1C

a normal superheater assembly. The reactor was operated for a total of 78 MWd with the defective superheater fuel, and essentially all of the steam was routed through the turbocondenser system.

1. Surface Concentration of UO_2 in the Defective Sample

Two sections of a UO_2 -stainless steel cermet sample similar to the one installed in the reactor were metallographically examined to determine the UO_2 surface concentration. Visual observation indicated that the concentration of UO_2 at the exposed surface of the plate was not as high as the average concentration in the fuel-bearing section of the plate. This visual observation was confirmed by a linear-intercept analysis.

The samples, which had had one cladding face removed by chemical dissolution, were iron-plated to preserve the exposed surface. Stainless-steel backup plates were placed over both surfaces and the compact mounted in Bakelite. Two samples were cut from the Bakelite mount (sample cuts were at 90° to each other). These samples were, in turn, mounted in Bakelite and prepared for observation under the microscope.

Initial observations at magnifications up to 500 times, made to determine if the chemical removal of the cladding had proceeded in a uniform fashion or if any preferential dissolution had taken place, indicated that the chemical dissolution had proceeded at a rather uniform rate. Figure 140 is typical of the sections in the two samples observed.

Since the basis for the inspection was to determine whether or not the exposed surface was a representative surface, a linear-intercept analysis was made on two arbitrarily selected areas. A linear-intercept analysis determines the ratio of fuel to stainless steel, based on the measurement of the length of interception with the fuel and with the stainless steel of a line parallel to the surface. The surface line was compared to the average of three arbitrarily drawn lines through the fuel section.

The results of the linear-intercept analysis on the two samples tested are shown in Table XXXIV. Apparently the chemically treated surface was not as rich in UO_2 as the average of the rest of the sample. Examination of the microstructure indicated no preferential attack on the UO_2 , but rather that a gradation of UO_2 concentration existed across the sample. Had the chemical clad-removal operation been continued until 2 to 4 mils of additional meat had been removed, the ratio of exposed UO_2 to stainless steel would have been more representative. Sample FCF 14 showed a fuel-section thickness of 13.4 mils; sample FCF 13 showed a fuel thickness of 12.1 mils. The specified nominal meat thickness was 14 mils.

Note:

As Polished Surface
Magnification: 225X

Fig. 140. Photomicrograph of Section through Defective Superheater-fuel Sample FCF 13, Core PSH-1C

TABLE XXXIV. Results of Linear-intercept
Analysis of Defective Superheater-fuel
Samples FCF 13 and 14

Sample No.	Surface, % UO_2	Average of Three Intercepts in Fuel Section, % UO_2
FCF 13	26.2	32.5
FCF 14	19.7	32.0

The results of the linear-intercept analysis indicate that the surface concentration of UO_2 on the defective superheater sample could have been as low as 60% of the average UO_2 concentration. This would have reduced the effective area of the defective sample somewhat below the actual exposed area of 1 sq in. In fact, the effective area could well have been near $1/2$ sq in., since some fuel was covered by the spacer wires.

2. Radiation Surveys

Special precautions and radiation procedures were the same as for the defective-boiler-fuel test outlined in Section X-A-1.

During the 2 weeks of operation with the prepared defective superheater fuel, radiation levels throughout the plant remained low and operators were not restricted in their normal work of reactor and steam-power-plant operation. Radiation levels in the plant systems are given in Table XXVIII. Again, no significant dose rate was detected by the down-wind monitor.

3. Isotopic Analysis

Isotopes identified in the steam from the defective superheater fuel assembly were I^{131} , Sr^{91} , Ba^{140} , and La^{140} . The presence of these isotopes indicated the presence of the gases krypton and xenon. No Mo^{99} was found.

I^{131} activities were determined for all sampling points except the condensate. Table XXXV shows the results of these determinations. The iodine levels were low even in the steam from the fuel assembly containing the defective sample. Several unexplained results are shown, such as: The highest I^{131} concentration was observed during a low-power run; more I^{131} was noted in the steam than in the reactor water initially; and there were higher iodine levels from the normal superheater assembly than from the main steam line.

Decontamination factors for I^{131} are shown in Table XXXVI.

TABLE XXXV. I^{131} Activity, Defective Superheater-fuel Tests,
Core PSH-1C

Power Level, MWt	Date and Time, 1964	Demineralizer DM-1 Flow, gpm	I^{131} Activity, dis/min/ml						
			Reactor Water S-4	Saturated Steam S-14	Defective Super-heater Assembly S-16	Normal Super-heater Assembly S-17	Super-heated Steam S-13A	Steam to Turbine S-12	
6.2	Aug 4	1320	0	94	280	50	84	5	85
10.0		1458	35	69	416	86	95	12	25
20.0		1624	39	57	79	44	58	9	10
13.0	Aug 5	0905	38	23	< 5	110	20	7	12
13.0		1100	38	40	< 5	41	< 5	< 5	11
13.0		1253	38	31	< 5	33	< 5	18	< 5
12.6		1353	37	23	30	35	< 5	22	< 5
12.2		1534	37	19	6	23	7	< 5	< 5
12.2	Aug 6	0947	36	13	< 5	83	< 5	9	< 5
12.4		1300	36	11	7	40	< 5	5	< 5
12.2		1450	36	11	5	29	< 5	< 5	8
4.5	Aug 7	0935	10	24	20	299	< 5	46	13
22.0	Aug 10	1535	40	-	< 5	119	26	22	14
15.6		1740	0	19	< 5	162	35	13	17
15.6		1845	0	27	< 5	65	13	14	15
15.0		2000	39	15	< 5	41	16	9	8

TABLE XXXVI. Decontamination Factors for I^{131} , Defective Superheater-fuel Test, Core PSH-1C

Power Level, MWt	Date and Time, 1964	Demineralizer DM-1 Flow, gpm	Decontamination Factors			Water Condition Resistivity $M\Omega$ -cm
			Saturated Steam S-14	Defective Superheater Assembly S-16	Superheater Steam S-13A	
6.2	Aug 4 1320	0	0.33	1.88	18.80	2.4
10.0	1458	35	0.17	0.80	5.75	2.4
20.0	1624	39	0.72	1.30	6.33	2.5
13.0	Aug 5 0905	38	-	0.21	3.29	2.9
13.0	1100	38	-	1.0	-	2.9
13.0	1253	38	-	0.94	1.72	2.9
12.6	1353	37	0.77	0.66	1.0	2.9
12.2	1534	37	3.17	0.83	-	3.0
12.2	Aug 6 0947	36	-	0.16	1.44	3.1
12.4	1300	36	1.57	0.27	1.1	3.1
12.2	1450	36	2.20	0.38	-	3.1
4.5	Aug 7 0935	10	1.20	0.08	0.52	4.1
15.6	Aug 10 1740	0	-	0.12	1.46	4.4
15.6	1845	0	-	0.42	1.93	4.6
15.0	2000	39	-	0.37	1.67	4.1

4. Specific Gamma Activity of Coolant

Gross specific activities during this test were determined on steam and water samples from the sampling points listed in Table XXXVII. The table shows that, as expected, the activity buildup in the reactor water was slowed down by operation of the reactor-water demineralizer, DM-1. With the demineralizer off, the activity (i.e., Na^{24}) built up quite rapidly. Activities in the steam system from the defective superheater-fuel sample were of the same order of magnitude as those from the ten defective boiler rods.

TABLE XXXVII. Gross Specific Gamma-activity Comparisons for Defective Superheater-fuel Test, Core PSH-1C

Power Level, MWt	Date and Time, 1964	Demineralizer DM-1 Flow, gpm	Reactor Water S-4	Saturated Steam S-14	Gross Activity, counts/min/ml				Condensate S-7
					Defective Super-heater Assembly S-16	Normal Super-heater Assembly S-17	Super-heated Steam S-13A		
6.2	Aug 4	1320	0	39,800	2,300	17,500	1,500	4,100	950
10.0		1458	35	32,700	1,968	18,800	2,300	6,500	2,900
15.0		1540	38	71,700	2,500	21,600	2,900	6,200	2,700
20.0		1624	39	112,000	1,700	18,000	2,300	6,700	4,500
13.0	Aug 5	0905	38	113,000	2,400	17,000	2,200	6,100	2,100
13.0		1100	38	106,000	2,200	20,400	2,700	5,300	2,300
13.0		1253	38	110,000	5,700	20,000	2,300	10,400	1,800
12.6		1353	37	109,000	2,200	19,500	2,400	3,600	2,200
12.2		1534	37	111,000	2,000	20,300	2,400	5,900	1,300
12.2	Aug 6	0947	36	118,000	2,400	20,500	2,900	5,300	1,400
12.4		1300	36	125,000	2,500	15,600	2,500	5,900	2,700
12.2		1450	36	111,000	3,800	14,000	2,400	3,500	2,500
4.5	Aug 7	0935	10	252,000	2,400	8,100	2,400	5,800	2,400
22.0	Aug 10	1535	40	130,000	4,300	82,000	4,800	6,800	-
4.0		1625	0	178,000	-	45,000	6,200	7,800	-
15.0		1740	0	347,000	3,000	30,000	5,600	8,400	-
15.6		1845	0	466,000	4,900	21,000	3,300	7,900	2,800
15.0		2000	39	251,000	8,500	25,000	3,200	7,600	1,700
12.2	Aug 12	0930	36	126,000	1,500	22,000	2,900	7,600	1,500
13.5		1545	39	127,000	7,200	25,600	3,700	8,100	-
13.5	Aug 13	1447	36	88,600	1,300	86,000	3,000	10,000	6,200
16.4		1610	37	111,000	1,500	87,000	3,100	7,100	3,600
14.0	Aug 14	0910	39	124,000	7,600	36,000	3,100	8,600	3,100
13.6		1118	39	119,000	3,500	22,500	2,800	4,000	2,500
7.7		1315	38	125,000	4,900	27,000	5,600	5,100	2,800
13.0		1540	38	110,000	-	37,000	3,400	7,100	4,000

Decontamination factors for gross specific gamma activity in saturated and superheated steam are summarized in Table XXXVIII.

TABLE XXXVIII. Gross Specific Gamma-activity Decontamination Factors, Defective Superheater-fuel Test, Core PSH-1C

Power Level, MWt	Date and Time, 1964	Demineralizer DM-1 Flow, gpm	Decontamination Factors			Water Condition Resistivity, M Ω -cm
			Saturated Steam S-14	Defective Superheater Assembly S-16	Superheater Steam S-13A	
6.2	Aug 4 1320	0	17.3	2.3	9.7	2.4
10.0	1458	35	16.6	1.7	5.0	2.4
15.0	1540	38	28.7	3.3	11.6	2.4
20.0	1624	39	65.9	6.2	16.7	2.5
13.0	Aug 5 0905	38	47.1	6.6	18.5	2.9
13.0	1100	38	48.2	5.2	20.0	2.9
13.0	1253	38	19.3	5.5	11.0	2.9
12.6	1353	37	49.5	5.6	30.2	2.9
12.2	1534	37	55.5	5.6	18.8	3.0
12.2	Aug 6 0947	36	49.2	5.8	22.3	3.1
12.4	1300	36	50.0	8.0	21.2	3.1
12.2	1450	36	29.2	7.9	31.7	3.1
22.0	Aug 10 1535	40	30.2	1.6	19.1	4.4
15.0	1740	0	115.7	11.6	41.3	4.4
15.6	1845	0	95.1	22.2	58.9	4.6
15.0	2000	39	29.5	10.0	33.0	4.1
12.2	Aug 12 0930	36	84.0	5.7	16.6	3.1
13.5	1545	39	17.6	4.9	15.7	3.9
13.5	Aug 13 1447	36	68.2	1.1	8.9	3.9
16.4	1610	37	74.0	1.3	15.6	3.0
14.0	Aug 14 0910	39	16.3	3.4	14.4	3.1
13.6	1118	39	34.0	5.3	29.8	3.1
7.7	1315	38	25.5	4.6	24.5	3.3

Samples of gas were taken at equal volume and pressure from the air-ejector system both before and after the charcoal filter. Results indicated that the charcoal filter reduced the gas activity by about 180%.

The stack monitor sampling the air-ejector exhaust-system effluent had two sections: a gas and a particulate monitor. At equilibrium-xenon conditions of 12.3 MWt, values of 6.6 to 7.2×10^3 counts/min/ft³ for the gas monitor and 2.2 to 7.0×10^3 counts/min/ft³ for the particulate monitor were observed. The stack effluent consisted of 6 to 10 cfm of

air-ejector exhaust flow, and 120 to 140 cfm of dilution air flow. The gas-monitor readings with and without defective superheater fuel were similar, but values two to ten times as high were observed during operation with the ten defective boiling fuel rods. The particulate monitor read highest during the operation with the superheater fuel defect. Air monitoring 100 to 200 ft downwind of the air-ejector exhaust stack and the atmospheric steam vents throughout the defective-fuel tests yielded no results.

5. Activities from Fission-product Monitors

As indicated in Table XXXIX, the activities recorded by the fission-product monitors were nearly constant throughout the superheater-fuel defect test. The values for superheated steam were much lower than those recorded during the test with ten defective boiling fuel rods.

TABLE XXXIX: Activities from Fission-product Monitors,
Superheater-fuel Defect Test, Core PSH-1C

Power Level, MWt	Date, 1964	Saturated Steam Activity, counts/min	Superheated Steam Activity, counts/min
10.0	Aug 4	70	12,000
12.0	8	4,000	16,000
12.5	6	1,100	16,000
12.5	12	1,800	15,000
13.0	12	1,000	16,000
13.5	12	1,800	16,000
15.0	14	2,900	22,000
17.0	13	3,000	32,000

6. Steam-system Contamination

A new set of deposition samples of the same shape and material was placed in the same locations in the steam system as the deposition samples during the tests with defective boiler fuel (see Section X-A-5). In addition, a sample in the form of a 1/2-in.-diam stainless-steel rod was placed in the riser of the defective superheater-fuel assembly.

The deposits on the deposition samples all gave the same gamma spectrum, consisting primarily of peak energies from Ba¹⁴⁰, La¹⁴⁰, and Co⁵⁸ isotopes. Activity from deposits on the samples is shown below. All values

are corrected to the time of reactor shutdown. Distances from the reactor vessel are also shown.

	Activity, $\mu\text{Ci}/\text{cm}^2$		
	Ba ¹⁴⁰	Co ⁵⁸	I ¹³¹
Stainless-steel rod in manifold, 24 ft	1.9×10^{-4}	1.4×10^{-4}	5.6×10^{-4}
Carbon-steel rod in high-pressure line, 46 ft	5×10^{-3}	2×10^{-3}	2.7×10^{-3}
Carbon-steel rod in low-pressure line, 69 ft	8.5×10^{-4}	$<10^{-5}$	2.2×10^{-4}

One-inch slugs cut from each deposition-sample rod and the wafers in the turbine exit were counted for specific gamma activity. The results are shown in Table XL, expressed as counts/min/cm² of sample surface. The values are comparable, since the same counting geometry, count time, elapsed time after shutdown (as nearly as possible), and surface area were used, and corrections were made for the neutron-induced activity in the lower end of the deposition rod in the defective superheater assembly. The values in Table XL correspond to the time approximately 137 hr after the terminal reactor shutdown.

TABLE XL. Steam-system Deposition Activity, Defective Superheater-fuel Tests, Core PSH-1C

Rod in Defective Assembly Riser, Stainless Steel, 3-9 ft above Fuel		Rod in Steam-line Manifold, Stainless Steel, 24 ft ^b		Rod in High-pressure Steam Line, ^c Carbon Steel, 46 ft ^b		Rod in Low-pressure Steam Line, Carbon Steel, 69 ft ^b		Wafers at Turbine Exit, 300 ft ^b	
Sample Position, in. to tip ^a	Activity, counts/min/cm ²	Sample Position, in. to tip ^a	Activity, counts/min/cm ²	Sample Position, in. to tip	Activity, counts/min/cm ²	Sample Position, in. to tip ^a	Activity, counts/min/cm ²	Sample Material	Activity, counts/min/cm ²
Tip	249,000	Tip	59,500	Tip	32,000	Tip	27,000	$\frac{1}{4}\text{Cr}-\frac{1}{2}\text{Mo}$	1,516
1	243,000	1	59,400	1	43,000	1	26,600		
2	239,000	10	50,200	2	54,800	7	26,500	Type 304 SS	628
3	234,000	21	56,600	6	30,500	13	23,500		
4	231,000	33	59,800			19	32,500	Admiralty Brass	1,261
5	228,000	45	89,200						
6	223,000							Carbon Steel	2,682
7	220,000								
8	217,000								
9	212,000								
10	209,000								
16	185,000								
29	143,000								
32	162,000								
45	133,000								
58	91,000								

^aThese rods mounted axially. The tip is defined as the unmounted, upstream end.

^bPipeline distance from center of reactor.

^cRod in high-pressure steam line was located transversely.

Decreasing axial distribution of deposited activity along the steam line is obvious from Table XL. A plot of deposition vs distance from reactor vessel revealed that deposition was very nearly exponential for the first 65 ft only. Radial distribution was examined on the deposition rods but showed nothing definite.

The flexible stainless-steel hose exit manifold that carried the steam from the defective superheater assembly was destructively tested to determine whether or not more deposition occurred on the corrugated interior surface than on a smooth surface. Both gross specific activity and gamma spectrographic analysis indicated that the corrugations did not enhance deposition. The activity per unit area of flexible hose was almost the same as the activity per unit area of deposition sample from the top of the riser on the defective superheater assembly. The flexible hose had the same isotopes deposited on its surface as the deposition samples, and neutron activation of the flexible hose was negligible. No uranium was found in the corrugations of the flexible hose.

The decay of the gross activity deposited in the main steam line was followed after the last reactor shutdown, and the half-life of the gross activity was determined. The half-life was approximated as follows:

<u>Time after Shutdown</u>	<u>Half-life</u>
30 min	7 min
$\frac{1}{2}$ -34 hr	6.2 hr
34-70 hr	23 hr
Over 70 hr	84 hr

APPENDIX

Derivation of Feedback Model

The derivation of a theoretical feedback model for the natural-convection operational mode of the PSH-1 core is outlined below. This model was derived and used to study the inherent nuclear-feedback mechanism over the entire frequency range of interest, i.e., 0.01 to 15 cps. While a similar study on the B-2 core⁷ showed that this complex model could be simplified greatly while sacrificing negligible accuracy in the 0.5- to 20-cps frequency range, substantial effort was expended in the stability analysis of PSH-1 to retain the various feedback effects. All the theoretical results given above were calculated by means of the model derived below.

The linear feedback model is finally presented in terms of Laplace-transform expressions following from linearization of the nonlinear differential equations describing the reactor system. While the model is thus valid only for small perturbations of the reactor feedback parameters about their steady-state values, it was found theoretically and experimentally that the largest perturbations obtained during forced oscillations apparently were not large enough to cause significant errors due to nonlinearities. Thus, the individual feedback mechanisms can validly be added, since the superposition principle applies to linear, or linearized, equations.

From the calculated results obtained previously with the RECHOP code,¹⁶ the feedback model derived incorporates the inherent steady-state relationships shown in Figs. 141 through 146 for average thermal conductivity of UO₂, average void volume fraction, steam flow and back-pressure control-valve capacity, flow-friction parameters, average slip ratio, natural-convection flow, and subcooling as functions of reactor power level.

The model includes the reactivity effects due to pressure, Doppler broadening, and subcooling changes, although these effects are small in the PSH-1 core above 0.5 cps. Particular attention was devoted to the important aspects of the model: namely, the reactor kinetics, the fuel-rod heat transfer, the void formation, and the effect of recirculation flow dynamics on the average void fraction.

1. Zero-power Reactor Kinetics

The standard equations describing the spatially independent thermal-reactor kinetics with six delayed-neutron groups are as follows:

$$\frac{dN}{dt} = \frac{(k - \bar{\beta}) N}{\ell^*} + \sum_{i=1}^6 \lambda_i C_i; \quad \frac{dC_i}{dt} = \frac{\bar{\beta}_i N}{\ell^*} - \lambda_i C_i, \quad i = 1, \dots, 6. \quad (1)$$

ID-103-A3402

Fig. 141. Average Thermal Conductivity of UO_2 vs
Power Level (Measured), Core PSH-1A

ID-103-A3403

Fig. 142. Average Void Volume Fraction in Boiler vs Power Level (Calculated), Core PSH-1

ID-103-A3404

Fig. 143. Total Steam Flow and Main Steam Back-pressure-control-valve Capacity vs Power Level (Measured), Core PSH-1A

ID-103-A3405

Fig. 144. Flow-friction Parameters for Boiler vs Power Level (Calculated), Core PSH-1

ID-103-A3406

Fig. 145. Natural-convection Flow, Average Slip Ratio, and Subcooling vs Power Level (Calculated), Core PSH-1

ID-103-A3407

Fig. 146. Power Coefficient of Reactivity
vs Power Level, Core PSH-1

Linearizing the variables (i.e., expanding as follows and dropping the product $\Delta N \cdot \Delta k$)

$$N = N_0 + \Delta N,$$

and

$$k = k_0 + \Delta k,$$

and Laplace-transforming the final result, we get

$$\frac{\Delta N(s)}{N_0 \Delta k} = G(s) = \frac{1}{\ell^* s \left[1 + \sum_{i=1}^6 \frac{\bar{\beta}_i}{s + \lambda_i} \right]}, \quad (2)$$

where

$N, \Delta N$ = Reactor power generated (proportional to neutron population) and its deviation;

$k, \Delta k$ = Multiplication constant, excessive reactivity;

C_i = Concentration of effective delayed-neutron emitters (i^{th} group);

λ_i = Delayed-neutron decay constants (i^{th} group);

$\bar{\beta}_i$ = Effective yield of delayed neutrons (i^{th} group);

ℓ^* = Prompt-neutron lifetime;

$G(s)$ = Overall reactor transfer function (at power);

t = Time;

and

s = Laplace variable.

The delayed-neutron parameters are listed in Table XLI.

TABLE XLI. Delayed-neutron Parameters

i	λ_i	$\bar{\beta}_i/\bar{\beta}$
1	0.0127	0.038
2	0.0317	0.213
3	0.115	0.188
4	0.311	0.407
5	1.40	0.128
6	3.87	0.206
$\bar{\beta} = 0.0071$ $\bar{\beta}/\ell^* = 220.4$ $\ell^* = 3.22 \times 10^{-5}$		
Zero power, 489°F, 600 psig conditions		

2. Boiling-fuel-rod Heat Transfer

The general heat-diffusion equation is solved for cylindrical geometry and the following assumptions:

a. All properties (i.e., thermal conductivity of UO_2 and Type 304 stainless-steel cladding, overall heat-transfer coefficient, and thermal diffusivity of UO_2) are constant in both space and time.

b. Coolant temperature changes within the core are small compared with the temperature drop across the fuel rod.

c. The conductivities of the film, cladding, and helium gap (or interior cladding surface contact) are combined to yield a single, overall heat-transfer coefficient.

d. Radial power distribution is constant.

e. No heat conduction occurs in the axial direction.

f. The cladding heat storage effect can be treated as a separate region.

The diffusion equation for the fuel region is

$$\nabla^2 T_f + \frac{P(t)}{K} = \frac{1}{\alpha} \frac{\partial T_f}{\partial t}, \quad (3)$$

where

K = Thermal conductivity of UO_2 fuel,

and

α = Thermal diffusivity = $K/(3600\rho c_p)$.

Since $\frac{\partial T_f}{\partial \theta} = \frac{\partial T_f}{\partial z} = 0$, we get the following:

$$\frac{\partial^2 T_f(r, t)}{\partial r^2} + \frac{1}{r} \frac{\partial T_f(r, t)}{\partial r} - \frac{1}{\alpha} \frac{\partial T_f(r, t)}{\partial t} = -\frac{P(t)}{K} = \frac{-N(t)}{3600 \cdot V_f \cdot K}, \quad (4)$$

where

V_f = Total volume of UO_2 fuel.

The steady-state temperature distribution across the fuel pin is given by

$$T_f(r) = T_c + \frac{Pa^2}{4K} \left(1 - \frac{r^2}{a^2} + \frac{2K}{Ha} \right), \quad (5)$$

where

a = Radius of UO_2 fuel pellet.

Using the Laplace-transform method, we obtain the following general solution to Eq. (4):

$$T_f(r, s) = A'I_0(qr) + B'K_0(qr) + \frac{\alpha P(s)}{Ks}, \quad (6)$$

where

$q = \sqrt{\frac{s}{\alpha}}$, I_0 and K_0 are Bessel functions,

and

$s = j\omega$, where $\omega = \text{rad/sec}$, and $j = \sqrt{-1}$.

The appropriate boundary and initial conditions are

$$(1) \quad - \left. \frac{\partial T_f}{\partial r} \right|_{r=a} \cdot K = \bar{H} [T_f(a, s) - T_c],$$

$$(2) \quad \left. \frac{\partial T_f}{\partial r} \right|_{r=0} = 0,$$

and

$$(3) \quad T_f \neq \infty, \text{ at } r = 0,$$

where

T_c = Coolant temperature (assumed constant),

$\bar{H}$ = Average overall heat transfer coefficient = $\frac{1}{1/h_F + 1/h_G + d_c/K_c}$,

h_F = Film coefficient averaged over the subcooled and boiling regions,

h_G = Conductance of helium gas,

K_c = Cladding thermal conductivity,

and

d_c = Cladding thickness.

When the above three conditions are applied to Eq. (6), the fuel temperature can be expressed as follows:¹⁷

$$T_f(r, s) = \left\{ \frac{1}{Kq^2} - \frac{(\bar{H}/K^2) I_0(qr)}{q^2 [qI_1(qa) + (\bar{H}/K) I_0(qa)]} \right\} N(s). \quad (7)$$

Approximating the cladding heat capacity effect by a simple lag in series with the fuel, and solving Eq. (7) for the heat-transfer rate into the coolant, we get

$$\frac{\Delta Q(s)}{\Delta N} = \frac{2\bar{H}}{Kq} \left[\frac{I_1(qa)}{qaI_1(qa) + (\bar{H}a/K) I_0(qa)} \right] \cdot \frac{1}{1 + \tau_0 s},$$

where

$$\tau_0 = \frac{\rho_c C_c}{\bar{H}} \left(\frac{V_c}{a_c} \right) = \text{cladding time constant.}$$

Since the right-hand side of Eq. (7) is a single-valued function, we can use the Laplace Inversion Theorem to write the following equation:

$$\frac{\Delta Q(s)}{\Delta N} = \sum_{n=0}^{\infty} \frac{A_n(s)}{s - s_n} \cdot \frac{1}{1 + \tau_0 s},$$

where A_n represents the residues of $\Delta Q/\Delta N$ at the poles, s_n . Evaluating the poles and residues, rearranging the above equation, and keeping M terms of the resulting infinite-series expansion, we get finally

$$\frac{\Delta Q(s)}{\Delta N} = \frac{1}{1 + \tau_0 s} \sum_{n=1}^M \frac{a_n}{1 + \tau_n s}, \quad (8)$$

where

Q = Rate of heat transfer into coolant,

N = Rate of heat generation,

$$a_n = -A_n/s_n,$$

and

$$\tau_n = -1/s_n.$$

The values of a_n and τ_n vs power level are listed in Table XLII; the average thermal conductivity of UO_2 in the boiler region is plotted in Fig. 141. The average thermal conductivity was taken from the experimental

TABLE XLII. Boiling-fuel-rod Heat-transfer Constants

Power Level (MWt)		5	10	15	18.1
n	a_n	$\tau_n(\text{sec})$			
0	-	0.1	0.1	0.1	0.1
1	0.71953	8.085	6.080	4.875	4.360
2	0.13513	1.533	1.154	0.926	0.827
3	0.05397	0.623	0.470	0.376	0.336
4	0.02832	0.336	0.254	0.203	0.181
5	0.01708	0.209	0.158	0.126	0.113
6	0.0112	0.143	0.108	0.0873	0.0772
7	0.00777	0.1037	0.0781	0.0626	0.0558
8	0.00560	0.0788	0.0593	0.0475	0.0424
9	0.00416	0.0616	0.0464	0.0372	0.0332
10	0.00317	0.0496	0.0373	0.0299	0.0267
11	0.01406	0.0220	0.0165	0.0133	0.0118

Note: $\sum_{n=1}^{11} a_n = 1.0.$

results shown in Fig. 67 and Table XIV for local-to-average boiler power production and boiling-fuel-rod conductance, respectively. It is assumed here that the fuel-rod gap resistance was negligible compared to that of the UO_2 ; thus, Table XIV represents effective UO_2 conductivity. As Table XIV shows, the UO_2 conductivity increases with power level at a given burnup.

With the aid of an IBM-1620 computer program, a_n and τ_n were evaluated for an effective overall heat-transfer coefficient of 3333 Btu/ft²-°F-hr, a UO_2 pellet diameter of 0.343 in., and the conductivity values shown in Fig. 141.

3. Steam Void Generation

The term "void fraction" here implies the actual void volume weighted by the assumed axial-reactivity importance function discussed below, whereas the actual (nonweighted) voids are implied in Section 8 below. Calculated voids are plotted against power in Fig. 142.

The dynamic relationship between the steam void volume (or average core-void fraction in boiler region) and rate of heat transfer (hereafter called power) into the coolant is derived to yield the total power-void transfer function. The following steps used, in general, to derive each feedback mechanism in this section are similar to Akcasu's¹⁸ analysis:

(1) The basic equations are written in differential form in terms of the Laplace variable.

(2) The equations are integrated, and the integrals are evaluated with the following basic assumptions:

(a) Steam and water velocities in the active core can be approximated by average axial-velocity values, which are assumed to vary with power level in the manner prescribed by Fig. 145.

(b) The axial power distribution is not a function of power level but remains identical to that calculated for the reactor operating at 15% exit-void fraction (average) and all control rods out of core. This axial power distribution was within 5% at all points of that measured in the PSH-1 core at 20 MWt (all rods out) and can be approximated by a chopped sinusoid.

(c) The axial power distribution is weighted by a reactivity-importance function that is approximated by the first power of the axial power distribution to obtain weighted voids. However, no reactivity weighting function is considered in the radial direction since an average channel is analyzed.

(d) Variation in potential energy of the water and steam is negligible within the active core.

(e) Pressure-gradient changes along the channel are negligible, compared with the operating pressure of the reactor.

(f) Saturation enthalpies and latent heat depend on pressure only.

(g) Steam and water phases are in thermodynamic equilibrium (saturated) at all times.

(3) With the above assumptions, the integrals can be evaluated in closed form and are simplified to yield expressions in terms of single or multiple time constants. The resulting gains and time constants are generally functions of the reactor operating characteristics and are defined by explicit expressions in Section 11 of this appendix.

The continuity and conservation of energy equations can then be written as follows for the two phases:

$$\left. \begin{aligned} \frac{\partial(A_g \rho_g \bar{V}_g + A_f \rho_f \bar{V}_f)}{\partial x} + \frac{\partial(A_g \rho_g + A_f \rho_f)}{\partial t} &= 0, \\ \text{and} \\ \frac{\partial}{\partial x} (\rho_g A_g \bar{V}_g h_g + \rho_f A_f \bar{V}_f h_f) + \frac{\partial}{\partial t} (A_g \rho_g h_g + A_f \rho_f h_f) &= Q(x, t); \end{aligned} \right\} \quad (9)$$

where

x = Axial distance within the active core;

A_g, A_f = Steam, water cross-sectional areas in active core as a function of x and t ;

ρ_g, ρ_f = Steam, water densities;

$\bar{V}_g, \bar{V}_f$ = Steam, water average velocities;

h_g, h_f = Steam, water enthalpies;

t = Time;

and

$Q(x, t)$ = Power transferred per unit distance.

Combining Eqs. (9), we get the void equation,

$$\frac{\partial}{\partial x} (A_g \bar{V}_g) + \frac{\partial A_g}{\partial t} = \frac{Q(x, t)}{H_v}, \quad (10)$$

where

$$H_v = (h_g - h_f) \rho_g = h_{fg} \rho_g.$$

Equation (10) will first be solved for transient changes in the voided region of the core (i.e., above the boiling boundary), assuming for the time being that the boundary position remains constant. The same equation will next be solved for the average void response to power changes in the sub-cooled portion of the core causing the boiling boundary to change, but meanwhile assuming the power transfer rate is constant in the voided region. The two effects will then be added to yield the total power-void transfer function.

First, the power distribution is approximated as

$$Q(x, s) = Q_0(x) + \Delta Q(x, s) \approx \frac{Q(s)}{L} A \sin B(x+C). \quad (11)$$

The reactivity weighting function is likewise expressed as

$$W(x) = A \sin B(x+C).$$

The average weighted void fraction is written as

$$\bar{\alpha}(s) = \frac{1}{A_c L} \int_{x_b}^L W(x) A_g(x, s) dx, \quad (12)$$

where

A_c = Flow cross-sectional area of the active core,

L = Height of the active core,

and

x_b = Average distance of boiling boundary from the bottom of the active core.

a. Power-Void Transfer Function, Boiling Region Only

Equation (10) is integrated and substituted along with the weighting function into Eq. (12) with the result,

$$\left. \frac{\Delta \bar{\alpha}(s)}{\Delta Q} \right]_{\text{Boiling Region}} = \frac{A^2}{H_v L^2 \bar{V}_g A_c} \int_{x_b}^L \sin B(x+C) dx \int_{x_b}^x \sin B(x'+C) e^{-(s/\bar{V}_g)(x'-x_b)} dx'. \quad (13)$$

b. Power-Void Transfer Function, Subcooled Region Only

Next, the void response to power changes in the subcooled region of the core will be obtained. Transient void volume changes due to subcooled boiling are assumed to be negligible.

(1) Power-Boiling Boundary Transfer Function

The energy equation is solved as follows, assuming for the time being that the core flow (W) and enthalpy of subcooling (Δh) remain constant:

$$\Delta h_w(x, s) = \frac{A \Delta Q(s)}{L W_o} \int_0^x \sin B(x' + C) e^{-(s/\bar{V}_f)(x' - x)} dx'. \quad (14)$$

The steady-state power necessary to raise the inlet-flow enthalpy to saturation can be expressed as follows:

$$\int_0^{x_b} Q_o(x) dx = (\Delta h W)_o = W_o \int_0^{x_b} \left[\frac{\partial h_w(x)}{\partial x} \right] dx.$$

Likewise, the relationship between changes in the enthalpy gradient, $\partial h_w(x, s)/\partial x$, and the boiling boundary, $\Delta x_b(s)$, can be expressed approximately as follows for small changes:

$$\int_0^x \frac{\partial h_w(x', s)}{\partial x} dx' = \frac{1}{W_o} \int_0^{x_b + \Delta x_b(s)} Q_o(x) dx.$$

Expanding the above equation, we get

$$\Delta x_b(s) \approx - \frac{W_o}{Q_o(x_b)} \cdot \Delta h_w(x, s). \quad (15)$$

Combining Eq. (14) and Eq. (15), we get the following power-boiling boundary transfer function:

$$\frac{\Delta x_b(s)}{\Delta Q} = \frac{-A}{L Q_o(x_b)} \int_0^{x_b} \sin B(x + C) e^{-(s/\bar{V}_f)(x - x_b)} dx. \quad (16)$$

(2) Boiling Boundary-Void Transfer Function

Next, the boiling boundary-void transfer function will be derived, again utilizing Eq. (10), but this time with a variable lower limit of integration and constant power production. Hence,

$$A_g(x, s) = \frac{1}{\bar{V}_g H_v} \int_{x_b + \Delta x_b(s)}^x Q_o(x') \cdot e^{-(s/\bar{V}_g)(x' - x_b)} dx'.$$

The boiling boundary-void transfer function is thus evaluated as

$$\frac{\Delta \bar{\alpha}(s)}{\Delta x_b} = \frac{-A Q_o(x_b)}{\bar{V}_g H_v L A_c} \int_{x_b}^L \sin B(x+C) e^{-(s/\bar{V}_g)(x-x_b)} dx. \quad (17)$$

From the product of Eqs. (16) and (17), the power-void transfer function for the subcooled region results in

$$\left. \frac{\Delta \bar{\alpha}(s)}{\Delta Q} \right]_{\text{Subcooled Region}} = \frac{A^2}{L^2 \bar{V}_g H_v A_c} \left[\int_0^{x_b} \sin B(x+C) e^{-(s/\bar{V}_f)(x-x_b)} dx \right] \cdot \left[\int_{x_b}^L \sin B(x+C) e^{-(s/\bar{V}_g)(x-x_b)} dx \right]. \quad (18)$$

Finally, by adding Eqs. (13) and (18), we get the complete power-void transfer function. Simplifying the resulting expression to a simple ratio of polynomials in the Laplace variable, and normalizing to rated power production (20 MWt), we get

$$Q_o \frac{\Delta \bar{\alpha}(s)}{\Delta Q} = \frac{N_o \partial \bar{\alpha}}{\partial N} \cdot \frac{1 + a_{13}s + a_{14}s^2}{(1 + \tau_{12}s)(1 + \tau_{13}s)(1 + \tau_{14}s)}. \quad (19)$$

Table XLIII lists the values of the constants in Eq. (19) as functions of power level; definitions are given in Section 11 of this appendix.

TABLE XLIII. Feedback-transfer-function Parameters vs Power Level

Power	a_0^*	a_{14}	a_{17}	a_{20}	a_{23}	τ_{14}	τ_{17}	τ_{20}	τ_{23}
Level		a_{15}	a_{18}	a_{21}	τ_{12}	τ_{15}	τ_{18}	τ_{21}	τ_{24}
(MWt)	a_{13}	a_{16}	a_{19}	a_{22}	τ_{13}	τ_{16}	τ_{19}	τ_{22}	τ_{25}
5.0	4.31	0.0016	0.0152	2.9×10^{-5}	0.0163	0.114	0.0100	0.8167	0.00025
		1.808	0.0778	18.059	0.0528	1.209	2.000	0.1045	14.12
		0.110	0.4446	0.1433	2.8248	0.0746	0.294	14.12	0.0045
10.0	7.02	0.0010	0.0075	4.8×10^{-5}	0.0075	0.089	0.0055	0.8641	0.00032
		1.324	0.1554	8.868	0.0442	0.945	2.000	0.1208	11.04
		0.090	0.2655	0.3659	2.2075	0.0624	0.194	11.04	0.0040
15.0	8.02	0.0009	0.0055	6.9×10^{-5}	0.0055	0.081	0.0040	0.8882	0.00035
		1.107	0.2333	5.776	0.0422	0.777	2.000	0.1312	9.99
		0.085	0.2060	0.6069	1.9980	0.0596	0.150	9.99	0.0038
18.1	8.32	0.0009	0.0049	8.4×10^{-5}	0.0049	0.078	0.0034	0.8981	0.00036
		1.029	0.2816	4.710	0.0421	0.692	2.000	0.1357	9.62
		0.084	0.1879	0.7611	1.9231	0.0595	0.132	9.61	0.0037

*See Eq. (67), p. 279 for definition of a_0 .

4. Heat-Pressure Transfer Function

Pressure changes in the reactor vessel depend principally upon the rate of heat transferred and the steam flow leaving the vessel. Also, the temperature and rate of natural-convection coolant flow entering the bottom of the boiler core have an appreciable effect on the core dynamics. The coolant-water temperature depends upon the temperatures of feedwater and saturated water just above the feedwater sparger, as well as the rate of feedwater flow into the vessel. However, temperature changes occurring at this mixing point must be delayed several seconds due to transport time between the sparger and the core inlet.

Expressions are thus generated in terms of boiler power transferred (Q), steam flow rate leaving the vessel (W_s), subcooling (Δh), flow rate entering the active core (W), and reactor pressure (P_1). The derivation includes: energy, volume, mass balance; natural convection delay effect; steam system flow dynamics; and the three-action automatic pressure-control system..

The following simplifying assumptions have been made:

(1) The control volume consists of all the reactor vessel space available to water and steam, except the steam space in the superheater coolant channels.

(2) The volumes of subcooled water and saturated water and steam remain essentially constant during transients; i.e., the water and steam initially saturated remain saturated.

(3) All thermodynamic properties of saturated water and steam follow the saturation line during pressure transients.

(4) Subcooled water is essentially incompressible.

(5) Changes in pressure at any point in the fluid within the vessel are equal to changes in steam dome pressure.

(6) Natural-convection transport time is constant during transients but changes with mean flow rate.

(7) The temperature and rate of demineralizer flow, downcomer instrument flow, and net control-rod seal in-flow remain essentially constant during transients.

a. Vessel Energy, Volume, and Mass Balance

An energy balance on the vessel yields, excluding the superheater region:

$$Q + W_{fw}h_{fw} + W_{di}h_{di} + W_{crs}h_{crs} - h_g W_s - h_{dm} W_{dm} = \frac{d}{dt} (uM). \quad (20)$$

A volume balance yields

$$V \cong M_f v_f + M_g v_g + M_r v_r. \quad (21)$$

A mass balance on the vessel can be written in terms of the rates of change of the saturated water and steam masses:

$$\dot{M}_m = \dot{M}_f + \dot{M}_g = W_{fw} + W_{di} + W_{crs} - W_s - W_{dm}. \quad (22)$$

In reducing Eqs. (20), (21), and (22) to a single equation, we make several substitutions and simplifying assumptions: First, the $\dot{M}_f$ and $\dot{M}_g$ terms can be written separately in terms of the total steam generation rate (W_{fg}) as follows:

$$\begin{aligned} \dot{M}_f &= W_{fw} + W_{di} + W_{crs} - W_{dm} - W_{fg}; \\ \dot{M}_g &= W_{fg} - W_s. \end{aligned} \quad (23)$$

Also, the total natural-convection flow can be written as follows:

$$W_v = W_d + W_{fw} - W_{dm}.$$

The active core flow rate is approximately proportional to the total flow:

$$W = (W_o/W_{vo}) W_v.$$

Because of assumption (4) above, $M_r \cdot v_f = 0$, and Eq. (21) can be expanded as follows:

$$\left(M_f \frac{\partial v_f}{\partial P} + M_g \frac{\partial v_g}{\partial P} \right) \frac{dP_1}{dt} + v_f \dot{M}_f + v_g \dot{M}_g = 0. \quad (24)$$

The right-hand side of Eq. (20) can be expanded as follows:

$$\frac{d}{dt} (u M_m) = \frac{d}{dt} \left[h_f M_f + h_g M_g - \frac{P_v}{J} \right] + M_r \frac{d\bar{h}_r}{dt}, \quad (25)$$

where the thermodynamic properties are related by

$$u = h - \frac{P_1 v}{J}.$$

b. Natural-convection Delay Effect on Subcooling

The average enthalpy of the subcooled water, $\bar{h}_r$ (between the sparger and core inlet), is a function of transport time. First, the enthalpy of the mixed water at the sparger outlet can be written:

$$h_r = \frac{h_f(W_v + W_{dm} - W_{fw}) + h_{fw}W_{fw}}{W_v + W_{dm}}. \quad (26)$$

Since perturbations in h_r are transported through the subcooled region having a given mass (M_r), perturbations in the average enthalpy ($\bar{h}_r$) will be delayed according to the differential equation,

$$\frac{\partial h_r(y, t)}{\partial t} = -\bar{V} \frac{\partial h_r(y, t)}{\partial y}, \quad (27)$$

where

$\bar{V}$ = average velocity of subcooled fluid,

and

y = distance along recirculation path.

Taking the Laplace transform of Eq. (27) with respect to time and integrating with respect to distance to obtain the average enthalpy, we get

$$\bar{h}_r(s) = h_r(s) \frac{(1 - e^{-T_w s})}{T_w s},$$

where

$$T_w \cong M_r / (W_v + W_{dm}), \text{ transport time.} \quad (28)$$

Now, substitute Eqs. (23), (24), (25), (26), and (28) into Eq. (20), taking appropriate Laplace transforms and linearizing terms. After sufficient rearrangement, we get:

$$\begin{aligned} Q + W_{di}h_{di} + h_{crs}W_{crs} - [h_f(W_v + W_{dm} - W_{fw})](1 - e^{-T_w s}) \\ - h_f(W_{fw} + W_{di} + W_{crs}) \\ - \frac{h_{fg}}{v_{fg}} [v_g W_s + v_f(W_{dm} - W_{fw} - W_{di} - W_{crs})] = DsP_1. \end{aligned} \quad (29)$$

Now,

$$\Delta h(s) = h_f(s) - h_c(s), \quad (30)$$

and

$$\begin{aligned} [(W_v + W_{dm}) h_c](s) &= [h_f(W_v + W_{dm} - W_{fw}) + h_{fw}W_{fw}] e^{-T_{ws}}; \\ \Delta h(W_v + W_{dm}) &= h_f(W_v + W_{dm}) \\ &\quad - [h_f(W_v + W_{dm} - W_{fw}) + h_{fw}W_{fw}] e^{-T_{ws}}. \end{aligned} \quad (31)$$

Combining Eqs. (19) and (31), we get the final pressure rate-of-change equation,

$$\begin{aligned} D \frac{dP_1}{dt} &= Q - \Delta h W_v - \left(1 + \frac{v_f}{v_{fg}}\right) h_{fg} W_s - \frac{v_f h_{fg}}{v_{fg}} (W_{dm} - W_{fw} - W_{di} - W_{crs}) \\ &\quad - (h_f - h_{di}) W_{di} - (h_f - h_{crs}) W_{crs}; \\ D &= M_f \left(\frac{\partial h_f}{\partial P} - \frac{h_{fg}}{v_{fg}} \frac{\partial v_f}{\partial P} \right) + M_g \left(\frac{\partial h_g}{\partial P} - \frac{h_{fg}}{v_{fg}} \frac{\partial v_g}{\partial P} \right) - \frac{V}{J}. \end{aligned} \quad (32)$$

The subcooled energy rate ($\Delta h W_v$) in Eq. (32) can be expressed approximately as follows, according to Eq. (31):

$$\Delta h W_v \cong h_f W_v - [h_f(W_v - W_{fw}) + h_{fw}W_{fw}] e^{-T_{ws}}, \quad (33)$$

since

$$W_{dm} \ll W_v.$$

Now we further assume

(1) Changes in W_v in Eq. (33) are negligible; i.e., $W_v \ll W_{vo}$.

$$(2) \quad e^{-T_{ws}} \cong \frac{1}{1 + T_{ws}}.$$

$$(3) \quad v_f \ll v_{fg}.$$

Thus,

$$\Delta h W_v \cong \left[\frac{W_{vo}(\partial h_f / \partial P) T_{ws} + W_{fwo}(\partial h_f / \partial P)}{1 + T_{ws}} \right] \Delta P_1. \quad (34)$$

Substituting Eq. (34) into (32) results in

$$\Delta P_1 = \frac{\Delta Q - h_{fg} \Delta W_s}{\left(\frac{W_{vo} T_{ws}}{1 + T_{ws}} \right) (\partial h_f / \partial P) + Ds}. \quad (35)$$

c. Steam-system Dynamics

The steam-line pressure drops included are: (1) back-pressure control valve, Δp_V ; (2) the steam turbine, Δp_t ; and (3) the steam line itself, Δp_L . We can write

$$\left. \begin{aligned} \Delta p_L &= P_1 - P_2 = \Delta p_{Lo} \left(\frac{W_s}{W_{so}} \right)^2; \\ \Delta p_V &= P_2 - P_3 = \left(\frac{W_s}{W_{so}} \right)^2; \\ \Delta p_t &= \Delta p_B = P_3 = \frac{W_t}{C_t} = \frac{W_B}{C_B} \text{ (sonic flow)...} \end{aligned} \right\} \quad (36)$$

Linearizing Eqs. (36) and retaining only the transient parts results in

$$\left. \begin{aligned} \Delta P_1 &= \Delta P_3 + 2\Delta p_{Vo} \left(\frac{\Delta W_s}{W_{so}} - \frac{\Delta C_v}{C_{vo}} \right); \\ \Delta P_3 &= P_{3o} \Delta W_s / W_{so} - \left(\frac{P_{3o}}{C_{Bo} + C_{to}} \right) \Delta C_B; \\ \Delta W_s &= \Delta W_B. \end{aligned} \right\} \quad (37)$$

d. Three-action Controller Dynamics

The pressure controller (PC-1 or PC-2) includes the following control actions: proportional band (P), reset (R), and rate time (T). The input signal to PC-1 is the output of the reactor pressure transducer (PR-2T), and the output of PC-1 in turn becomes the capacity-demand signal to the main steam back-pressure control valve (MS-18) or main steam-bypass valve (MS-14).

The relationship between controller input and output can thus be expressed:

$$\frac{\Delta C_v}{C_{vo}} = G \cdot \left(1 + \frac{R}{60s} + 60Ts \right) \frac{\Delta P_1}{1 + \tau_p s},$$

where

τ_p = pressure transducer lag,

$$G = \frac{100 C_{vm}}{\Delta P \cdot C_{vo} \cdot P},$$

and

ΔP = reactor pressure control band (800 psi).

After further manipulation, we can write

$$\left. \begin{aligned} \frac{\Delta C_v}{C_{vo}} &= \frac{7.5 C_{vm} T}{C_{vo} P s} (s+b)(s+c) \frac{\Delta P_1}{1 + \tau_p s}, \\ b &= \frac{1}{120T} \left[1 + \sqrt{1 - 4RT} \right], \\ \text{and} \\ c &= \frac{1}{120T} \left[1 - \sqrt{1 - 4RT} \right], \\ \text{if} \\ RT &\leq 1/4. \end{aligned} \right\} \quad (38)$$

Combining Eqs. (35), (37), and (38), we get finally the total heat-pressure transfer function,

$$\frac{Q_o \Delta P_1}{\Delta Q} = a_{19} \frac{s(1 + \tau_{18}s)(1 + \tau_{19}s)}{s^4 + \tau_{20}s^3 + \tau_{21}s^2 + \tau_{22}s + \tau_{23}}, \quad (39)$$

where the constants are given vs power in Table XLIII, and definitions are given in Section 11 of this appendix.

5. Pressure-Void Transfer Function

The pressure-void transfer function derivation stems from the basic continuity and conservation of energy equations (9) and is similar to the power-void transfer-function derivation. In this case, however, the pressure terms are retained, while power and flow are momentarily assumed constant. After combining and Laplace-transforming Eq. (9), we get

$$\frac{\partial}{\partial x} (A_g \bar{V}_g) + s A_g(x, s) = - \left[K(x) s + \frac{Q_o(x)}{H_v^2} \frac{\partial H_v}{\partial P} \right] P_1(s), \quad (40)$$

where

P_1 = the reactor pressure variable,

$K(x)$ = function of pressure and distance along core and is defined in Section 11 of this appendix.

The expressions given in Section 3 above are used to define the void-reactivity weighting function and the average void fraction. Substituting the above expressions into Eq. (40), the latter will be integrated, assuming for the time being that power and natural convection flow are constant. Therefore,

$$\frac{\Delta \bar{\alpha}}{\Delta P}(s) = -\frac{A}{H_v L \bar{V}_g A_c} \left\{ \int_{x_b}^L \sin B(x+C) e^{-(s/\bar{V}_g)(x-x_b)} dx \right. \\ \left. + \int_{x_b}^x \left[K_1 s \left[\cos B(x'+C) - \cos B(x_b+C) \right] + K_2 s \right. \right. \\ \left. \left. + K_3 \sin B(x'+C) \right] e^{(s/\bar{V}_g)(x'-x_b)} dx' \right\}, \quad (41)$$

where K_1 , K_2 , and K_3 are defined in Section 11 of this appendix.

After integrating the right-hand side of Eq. (41), the resulting expression is simplified to the following:

$$\frac{\Delta \bar{\alpha}}{\Delta P_1}(s) = -a_{20}(1 + a_{21}s), \quad (42)$$

where the constants vs power are given in Table XLIII and definitions are given in Section 11.

6. Pressure-Subcooling Transfer Function

The pressure-subcooling transfer function is essentially derived in Section 4 of this appendix where the following expression is given:

$$\Delta h W_v = \left[\frac{W_{vo}(\partial h_f / \partial P) T_{ws} + W_{fwo}(\partial h_f / \partial P)}{1 + T_{ws}} \right] \Delta P_1. \quad (34)$$

Since it is assumed (for the time being) that the flow rate (W_v) is constant, the above expression can be written as

$$\frac{\Delta h}{\Delta P_1}(s) = \left(\frac{\partial h_f}{\partial P} \right) \left(\frac{T_{ws} + W_{fwo}/W_{vo}}{1 + T_{ws}} \right).$$

However, at frequencies above 0.001 cps, the W_{fwo}/W_{vo} term in the above expression is negligible. The final pressure-subcooling transfer function is thus given by

$$\frac{\Delta h}{\Delta P_1}(s) = a_{22} \left(\frac{T_{ws}}{1 + T_{ws}} \right), \quad (43)$$

where a_{22} is given vs power level in Table XLIII.

7. Subcooling-Void Transfer Function

The subcooling-void transfer function is derived from the basic equation for void continuity and conservation of energy, Eq. (9), assuming

momentarily that the power, pressure, and velocity terms in Eq. (9) remain constant while the boiling boundary (x_b) can change.

To obtain the desired result, two separate transfer functions must be derived and combined; i.e., the subcooling-boiling boundary transfer function and the boiling boundary-void transfer function.

a. Subcooling-Boiling Boundary Relationship

To obtain the expression relating the core inlet subcooling and the boiling boundary at constant coolant flow and power, the following energy equation must be solved for the subcooled region of the core:

$$s \frac{h_w}{\bar{V}_f}(x, s) + \frac{\partial h_w}{\partial x}(x, s) = \frac{Q(x)}{\bar{V}_f A_c \rho_f}. \quad (44)$$

The following two boundary conditions must be satisfied by the solution to Eq. (44):

- (1) $h_w(0, s) = h_c(s)$;
- (2) $h_w(x_b + \Delta x_b, s) = h_f(s)$.

Solving Eq. (44) and applying the first boundary condition, we get

$$h_w(x, s) = \frac{1}{\bar{V}_f A_c \rho_f} \int_0^x Q(x') \cdot e^{-(s/\bar{V}_f)(x-x')} dx' + h_c(s) e^{-(s/\bar{V}_f)x}.$$

Next, the solution will be written in terms of $h_f(s)$ and $\Delta x_b(s)$ by applying the second boundary condition and Leibnitz's Rule for Derivatives on the right-hand side of the above equation. After rearrangement of the resulting equation, $\Delta x_b(s)$ can be written simply as

$$\Delta x_b(s) = \frac{\bar{V}_f A_c \rho_f}{Q(x_b)} \left[\Delta h_f(s) - \Delta h_c(s) e^{-(s/\bar{V}_f)x_b} \right]. \quad (45)$$

Subcooling is affected primarily through h_f due to pressure changes, and

- (1) $x_b/\bar{V}_f \ll T_w$ [see Eq. (28)];

and

- (2) $W = \rho_f A_c \bar{V}_f$.

Equation (45) can now be reduced to yield the desired subcooling-boiling boundary relationship,

$$\frac{\Delta x_b}{\Delta h} = \frac{W}{Q(x_b)}. \quad (46)$$

b. Boiling Boundary-Void Transfer Function

The relationship between boiling-boundary and void-fraction changes was derived earlier in Section 3-b-(2) of this appendix, and only the final result is repeated below:

$$\frac{\Delta \bar{\alpha}}{\Delta x_b}(s) = \frac{-AQ_0(x_b)}{\bar{V}_g H_v L A_c} \int_{x_b}^L \sin B(x+C) e^{-(s/\bar{V}_g)(x-x_b)} dx. \quad (17)$$

Combining Eqs. (17) and (46) and simplifying the resulting expression to a simple time-constant form, we get finally:

$$\frac{\Delta \bar{\alpha}}{\Delta h}(s) = -\frac{a_{23}}{1 + \tau_{25}s}, \quad (47)$$

where the constants are given in Table XLIII and definitions are given in Section 11 of this appendix.

8. Recirculation Flow Hydrodynamics

The net-void transfer function to be derived in this section combines the complex effects due to the recirculation rate response to void changes in the core and risers, and the countereffect, in turn, on voids due to the recirculation changes. The following assumptions have been made in this derivation:

(1) The steady-state (low-frequency) effect, only, is important in deriving the void-recirculation flow transfer function.

(2) Velocity (or flow) changes in the recirculation loop are small compared with their initial steady-state values.

(3) The ratio of steam and water velocity changes is proportional to the ratio of their initial steady-state values.

(4) Changes in the overall loop friction coefficient can be expressed in terms of its first derivative.

(5) Void-fraction changes in the downcomer and superheater moderator region are negligible compared with changes in the boiler core and riser void fraction at all power levels.

a. Void Effects on Recirculation Flow

A momentum balance around the recirculating loop yields

$$I_w \frac{dW}{dt} = \Delta p_v - \Delta p_f, \quad (48)$$

where

I_w = Effective inertia of water and steam in the circuit

$$= \frac{1}{144g_o} \sum_{i=1}^m \left(\frac{L_i}{A_i} \right),$$

W = Mass flow rate through core,

m = Number of various cross-section elements in series in the circuit,

L_i, A_i = Length and cross-sectional area of various elements in the circuit,

Δp_v = Driving head in loop, assumed a function only of void fraction in active core and risers,

and

Δp_f = Frictional head loss in loop, assumed a function only of void fraction in active core and flow rate.

Since the actual (nonweighted) void fraction ($\bar{\alpha}_t$) provides the driving force for flow, we write

$$\bar{\alpha}_t(t) = \frac{1}{A_c L} \int_{x_b}^L A_g(x, t) dx. \quad (49)$$

We will approximate the relationship between actual-void and weighted-void fractions by a constant term evaluated as follows:

$$\frac{\bar{\alpha}_t}{\bar{\alpha}} = \frac{\int_{x_b}^L A_g(x) dx}{\int_{x_b}^L W(x) \cdot A_g(x) dx} = \frac{\int_{x_b}^L \left[\int_{x_b}^x \sin B(x' + C) dx' \right] dx}{A \int_{x_b}^L \sin B(x + C) \left[\int_{x_b}^x \sin B(x' + C) dx' \right] dx}.$$

Thus, $\bar{\alpha}_t/\bar{\alpha}$ depends only upon power distribution and boiling boundary, x_b . Power distribution is further assumed to remain constant with power.

Laplace-transforming Eq. (48), we have

$$I_w s W = \Delta p_v(s) - \Delta p_f(s). \quad (50)$$

The transient frictional head can be expressed as

$$\Delta p_f(s) \cong \left(\frac{\bar{\alpha}_t}{\bar{\alpha}} \right) \left(\frac{\partial f}{\partial \bar{\alpha}_t} \right)_W \left(\frac{W}{W_o} \right)^2 \Delta \bar{\alpha}(s) + 2 \left(\frac{W}{W_o} \right) f \frac{\Delta W(s)}{W_o}, \quad (51)$$

and the transient driving head can be written approximately as

$$\Delta p_v(s) \cong \frac{g}{144 g_o} \left\{ (\rho_f - \rho_g) \left[L \bar{\alpha}_t(s) + \frac{(L_1 + L_2 + L_3) \alpha_E(s)}{1 + \tau_r s} \right] - (\Delta \rho_r L_3) \frac{\alpha_E(s)}{1 + \tau_r s} \right\}, \quad (52)$$

where

$\alpha_E(s)$ = Average void fraction at core exit

$$= \left(\frac{\alpha_E}{\bar{\alpha}_t} \right) \bar{\alpha}_t(s) = \left(\frac{\alpha_E}{\bar{\alpha}_t} \right) \left(\frac{\bar{\alpha}_t}{\bar{\alpha}} \right) \bar{\alpha}(s),$$

L = Core height,

L_1, L_2, L_3 = Riser heights,

and

τ_r = Effective transit time from core exit to riser section.

Substituting Eqs. (51) and (52) into (50) and rearranging, results in

$$\frac{\Delta W(s)}{\Delta \bar{\alpha} W_o} = \frac{C_{vw}(1 + \tau_{r2} s)}{(1 + \tau_{r1} s)(1 + \tau_{ws} s)}. \quad (53)$$

The τ 's and C_{vw} are defined in Section 11.

b. Recirculation Flow Acceleration Effects on Void Fraction

(1) Velocity Effect in Boiling Region Only, Constant Boiling Boundary

Assuming transient changes occur in the average steam velocity at constant power production, we write the basic void differential Eq. (10) as follows:

$$\frac{\partial}{\partial x} [\bar{v}_g A_g] + s A_g(x, s) = \frac{Q(x)}{H_v}. \quad (54)$$

Expanding each term of Eq. (54) into steady-state plus transient parts, as follows:

$$A_g(x, s) = A_g(x) + a_g(x, s),$$

and similarly for the other variables, we get

$$\frac{\partial a_g(x, s)}{\partial x} + \frac{s}{\bar{V}_g} a_g(x, s) = - \left[\frac{\partial A_g(s)}{\partial x} \right] \cdot \frac{\Delta \bar{V}_g(s)}{\bar{V}_g}. \quad (55)$$

Now,

$$\bar{V}_g(x_b) \equiv \frac{WS}{\rho_f A_c} = \text{Steam velocity at the boiling boundary,}$$

where

$$\bar{V}_g = \text{Average steam velocity in boiler core,}$$

$$S = \text{Average slip ratio in core,}$$

and

$$\Delta \bar{V}_g(s) = \left(\frac{S}{\rho_f A_c} \right) \frac{\bar{V}_g}{\bar{V}_g(x_b)} \Delta W(s) = \text{Perturbation of average steam velocity.} \quad (56)$$

Incorporating the void-reactivity weighting function, substituting Eq. (56) and integrating Eq. (55), we have

$$W_o \left[\frac{\Delta \tilde{a}(s)}{\Delta W} \right]_{\text{Boiling Region}} = \frac{Q_o A^2}{H_v A_c \bar{V}_g L^2} \int_{x_b}^L \sin B(x+C) dx \int_{x_b}^L \sin B(x'+C) e^{-(s/\bar{V}_g)(x'-x)} dx'. \quad (57)$$

(2) Velocity Effect in Subcooled Region, Changing Boiling Boundary

We again use the following form of the void equation:

$$A_g(x, s) = \frac{1}{\bar{V}_g H_v} \int_{x_b + \Delta x_b(s)}^x Q_o(x') e^{-(s/\bar{V}_g)(x'-x_b)} dx'. \quad (58)$$

Expanding the right-hand side of Eq. (58) in terms of $\Delta x_b(s)$, we get

$$a_g(x, s) = - \frac{Q_o(x_b)}{\bar{V}_g H_v} e^{-(s/\bar{V}_g)(x-x_b)} \cdot \Delta x_b(s). \quad (59)$$

Solving the energy equation for a variable flow rate and constant power production, we get

$$\Delta h_w(x_b, s) = \frac{-AQ_o \Delta W(s)}{W_o L} \int_0^{x_b} \sin B(x+C) e^{-(s/\bar{V}_f)(x-x_b)} dx. \quad (60)$$

Again using Eq. (15), we get

$$\Delta x_b(s) \cong \frac{-W_o}{Q_o(x_b)} \Delta h_w(x_b, s).$$

Incorporating the weighting function and combining Eqs. (15), (17), and (60), we get

$$W_o \frac{\Delta \bar{\alpha}(s)}{\Delta W} \Big|_{\text{Subcooled Region}} = - \left(\frac{Q_o A^2}{\bar{V}_g H_v L^2 A_c} \right) \left[\int_0^{x_b} \sin B(x+C) e^{-(s/\bar{V}_f)(x-x_b)} dx \right] \cdot \left[\int_{x_b}^L \sin B(x+C) e^{-(s/\bar{V}_g)(x-x_b)} dx \right]. \quad (61)$$

Adding Eqs. (57) and (61) and simplifying, we get the total recirculation effect on the weighted voids, as follows:

$$W_o \frac{\Delta \bar{\alpha}(s)}{\Delta W} = \frac{C_{wv}}{1 + \tau_{wv}s}, \quad (62)$$

where C_{wv} and τ_{wv} are defined in Section 11.

To get the overall effect of the void-recirculation flow interaction, Eqs. (53) and (62) are combined to yield the following void ($\bar{\alpha}$) to net-void ($\bar{\alpha}_n$) transfer function:

$$\frac{\Delta \bar{\alpha}_n(s)}{\Delta \bar{\alpha}} = \frac{1 + a_{15}s + a_{16}s^2 + a_{17}s^3}{1 + \tau_{15}s + \tau_{16}s^2 + \tau_{17}s^3}. \quad (63)$$

Table XLIII lists the values of the constants in Eq. (63). The constants are defined in Section 11.

9. Doppler Reactivity Feedback

The Doppler effect is defined as the broadening of the resonance peaks in the energy-dependent U^{238} cross section, resulting in more neutron captures during the slowing-down process. The attendant reactivity changes can be written explicitly in terms of the average fuel temperature, which is considered independent of the coolant-moderator condition.

The following assumptions were made in the derivation:

- (1) Boiling conditions always prevail in the coolant-moderator, and the average fuel temperature is always above 489°F.
- (2) The only significant Doppler-effect contribution is from the boiler region. (Reactivity feedback effects due to the peripheral superheater region are discussed below.)
- (3) The average boiler-fuel temperature is adequately determined by using the same thermal properties as in the Boiling-fuel-rod Heat Transfer (Section 2).
- (4) The Doppler coefficient is independent of power level in the range of 0 to 18.1 MWt, while the proper value to use is that given in ANL-6302 for the zero-power, hot condition.² (Two qualifications associated with the coefficient value used should be stated here: (a) All Doppler coefficients were calculated for isothermal temperature conditions across the core, thus tending to underpredict the actual value due to neglect of axial and radial importance; (b) the zero-power coefficient is theoretically greater than at-power values. Thus, a reasonable value at power is the zero-power coefficient, the magnitude of which tends to offset the neglect of spatial importance in its calculation.)

The average temperature ($\bar{T}$) of all fuel in the boiler region can be expressed in terms of the power-generation rate (N) minus the heat-transfer rate into the cladding (Q_c):

$$\frac{d\bar{T}}{dt} = \frac{1}{C_b} \left[\left(\frac{Q_o}{N_o} \right) N(t) - Q_c(t) \right], \quad (64)$$

where C_b is the total boiler heat capacity (UO_2 only). Also, the heat-transfer rate can be written in terms of an infinite series of Laplace transforms:

$$Q_c(s) = \left(\frac{Q_o}{N_o} \right) N(s) \sum_{n=1}^{M-1} \frac{a_n}{1 + \tau_n s} \quad (\text{see Table XLII}). \quad (65)$$

Laplace-transforming Eq. (64) and combining it with Eq. (65), we obtain the desired power-Doppler reactivity transfer function:

$$N_o \frac{\Delta k_d}{\Delta N}(s) = \frac{a_{18}}{s} \left[1 - \sum_{n=1}^{M-1} \frac{a_n}{1 + \tau_n s} \right],$$

$$a_{18} = \frac{Q(\partial k_d / \partial \bar{T})}{C_b}, \quad (66)$$

where

$$\frac{\partial k_d}{\partial T} = \text{Doppler coefficient,}$$

and a_{18} vs power level is shown in Table XLIII and defined in Section 11.

10. Other Reactivity Feedback Effects

Additional reactivity effects have been considered, but only in a qualitative manner. They can be grouped as follows:

a. Effect of Peripheral Superheater Region

Because of the small size of the BORAX-V cores (40 in. in equivalent diameter by 24 in. in height), it had been assumed that the boiler and superheater regions were extremely close-coupled, with the boiler region driving the superheater. Furthermore, feedback effects from the superheater region were calculated to be at least two orders of magnitude less than feedback from the boiler under normal power conditions. The principal effect from the superheater would be the Doppler effect, but due to the highly enriched U^{235} and the relatively low importance of the peripheral zone, the superheater Doppler effect was estimated to be at least an order of magnitude less than the boiler Doppler contribution, and hence was omitted in the analysis.

Thus, the PSH-1 core was assumed to behave essentially as a boiler-driven core with the superheater closely coupled. Probably the major effect of the superheater region was that it altered the importance of void and Doppler effects in the boiler, although no clear evidence of this effect could be verified by measurements, due to differences between the B-2 and PSH-1 cores in the boiler region composition, i.e., volume ratio of fuel and poison rods to water.

b. Effect of Moderator Temperature Changes

Reactivity effects due to moderator temperature changes were omitted because of their (calculated and measured) small magnitude at power conditions and because of their relatively long time scale (present only at very low frequencies; i.e., less than 0.01 cps).

c. Spatial Flux-shape Variations

Due to the compactness of the BORAX-V cores, flux-shape variations were estimated to be negligible in the frequency range of interest (0.01 to 15 cps) and to contribute negligibly to reactor instability at power. An additional factor minimizing any flux-shape instability was the high negative void coefficient exhibited in each BORAX-V core.

d. Hydraulic Stability

The term "hydraulic stability" here refers primarily to the character of core flow response to power and subcooling changes. The hydraulics transfer function described above in Section 4 was derived assuming "stable hydraulics," and hence was not used to investigate the ranges of parameters required for hydraulic stability. The transfer-function measurements and subsequent model-fitting analysis indicated that a high degree of hydraulic stability was present at all power levels and that the core would have exhibited even greater stability had the reactor been operated in the forced convection mode. Neither were any oscillatory tendencies indicated in continuous recordings of void fraction and coolant flow rate through instrumented boiler assemblies.

e. Xenon and Samarium Buildup

Xenon and samarium reactivity effects were omitted from this analysis because of their extremely long time scale (5 to 40 hr, depending upon power level and initial conditions). As described above in Section VIII, the time required to measure a reactor transfer function was about 8 hr so that an attempt was made to reach equilibrium xenon concentration before oscillating the reactor.

11. Total Feedback Transfer Function

The total reactivity-feedback transfer function is comprised of all the effects derived in Sections 1 through 9 above and is shown in block-diagram form in Fig. 147. Since the void coefficient was not determined explicitly for the PSH-1 core, either analytically or by experiment, the power coefficient* was the gain parameter adjusted in the feedback model-fitting process. If the value of the Doppler coefficient used is reasonably correct, then the measurements revealed that the void feedback effects are at least an order of magnitude greater than Doppler effects. It is felt that the feedback model fits the measured data reasonably well in the range of 0.01 to 15 cps, although several of the individual transfer functions employed are greatly simplified from their original form.

All parameters (gains and time constants) in the feedback transfer functions that vary with power level are given in Tables XLII and XLIII. In addition, measured values of average UO_2 thermal conductivity, steam flow, and main steam back-pressure control-valve opening are shown in Figs. 141 and 143. Since measured steady-state characteristics of many desired quantities were not available, calculated curves of void fraction, natural convection flow rate, subcooling, slip ratio, and flow friction were used as shown in Figs. 142, 144, 145, and 146.

*Power coefficient here excludes Doppler reactivity effects (see p. 279).

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Fig. 147. Theoretical Model Block Diagram, Core PSH-1

In addition to the tabulated values, general expressions defining the essential parameters in Table XLIII are listed below:

$$a_0 = N \left(\frac{\partial \rho}{\partial N} \right) \equiv N \left(\frac{\partial \bar{\alpha}}{\partial N} \right) \left(\frac{\partial \rho}{\partial \bar{\alpha}} \right), \quad (67)$$

where

$$\frac{\partial \rho}{\partial N} = \text{Power coefficient of reactivity (see Fig. 146),}$$

$$\frac{\partial \bar{\alpha}}{\partial N} = \text{Slope of void fraction vs power (see Fig. 142),}$$

$$\frac{\partial \rho}{\partial \bar{\alpha}} = \text{Void coefficient of reactivity,}$$

and

N - Power level.

The power coefficient is defined as follows:

$$\frac{\partial \rho}{\partial N} = \frac{(C_q + C_{qx} C_x) (\partial \rho / \partial \bar{\alpha})}{\Omega (1 - C_{wv} C_{vw})},$$

where

$$C_q = \left(\frac{A^2 \rho_f}{L^2 B^2 H_v} \right) \left(\frac{Q}{SW} \right) \left\{ \frac{1}{2} [\sin^2 B(x_b + C) - \sin^2 B(L + C)] \right. \\ \left. + \cos B(x_b + C) [\cos B(L + C) - \cos B(x_b + C)] \right\},$$

$$C_{qx} = \frac{-AQ}{LBQ(x_b)} [\cos(BC) - \cos B(x_b + C)],$$

$$C_x = \frac{\rho_f AQ(x_b)}{BH_v SW L} [\cos B(L + C) - \cos B(x_b + C)],$$

$$C_{wv} = \frac{-A \rho_f}{LBH_v} \left\{ \left[\cos B(x_b + C) [\cos B(x_b + C) - \cos B(L + C)] \right. \right. \\ \left. \left. + \frac{1}{4} [\cos 2B(L + C) - \cos 2B(x_b + C)] \right] \left(\frac{A}{BL} \cdot \frac{Q}{SW} \right) \right. \\ \left. + [\cos B(x_b + C) - \cos B(L + C)] \left(\frac{\Delta h}{S} \right) \right\},$$

$$C_{vw} = \left(\frac{\bar{\alpha}_t}{\bar{\alpha}} \right) \left[\left(\frac{\rho_f - \rho_g}{144} \right) \frac{g}{g_o} L_E - \left(\frac{\partial f}{\partial \bar{\alpha}_t} \right)_W \left(\frac{W}{W_o} \right)^2 \right] \frac{W_o}{2fW},$$

$$a_{13} = [C_q(\tau_{14} + \tau_{13}) + C_{qx}C_x \tau_{12}] / (C_q + C_{qx}C_x),$$

$$a_{14} = (C_q \tau_{14} \tau_{13}) / (C_q + C_{qx}C_x),$$

$$\tau_{12} = \left(\frac{\rho_f A_c}{B^2} \right) \left(\frac{1}{SW} \right) \left[\frac{1}{L - x_b + \frac{\sin 2B(x_b + C) - \sin 2B(L + C)}{2B}} \right]$$

$$: [\sin^2 B(x_b + C) - \sin^2 B(L + C) + 2 \cos B(x_b + C)$$

$$+ [\cos B(L + C) - \cos B(x_b + C)]],$$

$$\tau_{13} = \left(\frac{\rho_f A_c}{B} \right) \left(\frac{1}{SW} \right) \left[\frac{\cos B(x_b + C) - \cos B(L + C)}{\sin B(x_b + C)} \right],$$

$$\tau_{14} = \left(\frac{\rho_f A_c}{B} \right) \left(\frac{1}{W} \right) \left[\frac{\cos(BC) - \cos B(x_b + C)}{\sin B(x_b + C)} \right],$$

$$a_{15} = \tau_{r1} + \tau_w + \tau_{wv},$$

$$a_{16} = \tau_{r1} \tau_w + \tau_w \tau_{wv} + \tau_{wv} \tau_{r1},$$

$$a_{17} = \tau_{r1} \tau_w \tau_{wv},$$

$$\tau_{15} = (a_{15} - C_{vw}C_{wv} \tau_{r2}) / (1 - C_{vw}C_{wv}),$$

$$\tau_{16} = a_{16} / (1 - C_{vw}C_{wv}),$$

$$\tau_{17} = a_{17} / (1 - C_{vw}C_{wv}),$$

$$\tau_{r2} = \frac{L}{L_E} \left[\frac{\left(\frac{W}{W_o} \right)^2 \left(\frac{\partial f}{\partial \bar{\alpha}_t} \right)_W - 1}{\left(\frac{g}{144g_o} \right) \left(\rho_f - \rho_g \right) L} \right] \tau_{r1},$$

$$\tau_{r1} = (\rho_f A_c / 2) [(L_1 + L_2 + L_3) / (SW)],$$

$$\tau_w = (L_w / 2) [W_o^2 / (fW)],$$

$$\tau_{wy} = \frac{\rho_f A_c}{B \sin B(x_b + C)} \left[\frac{1}{\Delta h \cdot W + \frac{A}{BL} \cdot Q [\cos B(x_b + C) - \cos B(L + C)]} \right] \\ \cdot \left[\left[\cos B(x_b + C) [\cos B(x_b + C) - \cos B(L + C)] + \frac{1}{4} [\cos 2B(L + C) - \cos 2B(x_b + C)] \right] \left(\frac{A}{BL} \cdot \frac{Q}{SW} \right) + [\cos B(x_b + C) - \cos B(L + C)] \left(\frac{\Delta h}{S} \right) \right],$$

$$a_{18} = \frac{Q(\partial k_d / \partial \bar{T})}{C_b},$$

$$a_{19} = Q / \tau_2 \tau_p D_e,$$

$$a_{20} = \frac{A \rho_f}{LB^2 H_v SW} \left\{ \frac{K_3}{2} [\sin^2 B(x_b + C) - \sin^2 B(L + C)] \right. \\ \left. + K_3 \cos B(x_b + C) [\cos B(x_b + C) - \cos B(L + C)] \right. \\ \left. - \frac{SWB}{\rho_f A_c} [K_2 - K_1 \cos B(x_b + C)] [\cos B(x_b + C) - \cos B(L + C)] \right\} \\ + \frac{A}{BL A_c} [\cos B(L + C) - \cos B(x_b + C)] [K_2 - K_1 \cos B(x_b + C)],$$

$$a_{21} = \frac{A \rho_f}{a_{20} LB^2 H_v SW} \left\{ \frac{1}{2} \left(K_1 B + \frac{K_3 \rho_f A_c}{SW} \right) \left[L - x_b - \frac{\sin 2B(L + C) - \sin 2B(x_b + C)}{2B} \right] \right. \\ \left. + \left[K_1 \sin B(x_b + C) + \frac{K_3 \rho_f A_c}{BSW} \sin B(x_b + C) \right] [\cos B(x_b + C) - \cos B(L + C)] \right. \\ \left. + \left[\frac{K_3 \rho_f A_c}{BSW} \cos B(x_b + C) \right] [\sin B(L + C) - \sin B(x_b + C)] \right. \\ \left. + [K_1 \cos B(x_b + C) - K_3] [\sin B(L + C) - \sin B(x_b + C)] \right\},$$

$$a_{22} = \frac{\partial h_f}{\partial P},$$

$$K(x) = \left(\frac{1}{h_{fg}} \frac{\partial h_g}{\partial P} + \frac{1}{\rho_g} \frac{\partial \rho_g}{\partial P} - \frac{\rho_f}{H_v} \frac{\partial h_f}{\partial P} \right) A_g(x) + \frac{A_c}{H_v} \left(\rho_f \frac{\partial h_f}{\partial P} - \frac{1}{J} \right),$$

$$H_v = \rho_g h_{fg},$$

$$K_1 = \left(\frac{1}{h_{fg}} \frac{\partial h_g}{\partial P} - \frac{1}{v_g} \frac{\partial v_g}{\partial P} - \frac{\rho_f}{H_v} \frac{\partial h_f}{\partial P} \right) \frac{Q_0 A}{BL \bar{V}_{g0}},$$

$$K_2 = A_c \left(\frac{1}{v_f} \frac{\partial h_f}{\partial P} - \frac{1}{J} \right),$$

$$K_3 = \frac{Q_o}{H_v} \frac{\partial H_v}{\partial P} \frac{A}{L},$$

$$\tau_{18} = \tau_p,$$

$$\tau_{19} = T_w = T_{wo} \left(\frac{W_o}{W} \right),$$

$$\tau_{20} = \frac{1}{\tau_2 \tau_p} \left[\tau_2 + \tau_p + A_1 G T_w \right],$$

$$\tau_{21} = \frac{1}{\tau_2 \tau_p} \left[1 + A_1 (G + F T_w) \right],$$

$$\tau_{22} = \frac{A_1 (F + E T_w)}{\tau_2 \tau_p},$$

$$\tau_{23} = A_1 E / \tau_2 \tau_p,$$

$$A_1 = \frac{h_{fg} W_s}{D_e (P_{30} + 2 \Delta P_{vo})},$$

$$E = C b c,$$

$$C = 15 \Delta P_{vo} T,$$

$$\tau_2 = (D/D_e) T_w,$$

$$D_e = D + M_f (\partial h_f / \partial P),$$

$$F = E C (b + c),$$

$$G_1 = \tau_p + C,$$

$$a_{23} = \frac{A \rho_f}{L B S H_v} \left[\cos B(x_b + C) - \cos B(L + C) \right],$$

$$\tau_{25} = \frac{\rho_f A_c [\cos B(x_b + C) - \cos B(L + C)]}{S W B \sin B(x_b + C)}$$

The remaining constants and rated values of variables (at 20 MWt) used in the analysis are defined and evaluated in the Nomenclature.

12. Nomenclature

<u>Constants</u>			
a	Radius of UO ₂ fuel pin (excluding cladding)	0.0143	ft
a _n	-A _n /s _n	-	-
A	Amplitude of sine functions W(x) and Q(x) (axial peaking factor)	1.34	-
A _c	Total cross-sectional area for flow inside boiler assemblies (outside fuel and/or flow rods)	2.66	ft ²
A _n	Residues at the poles, s _n , in the heat-transfer function, ΔQ/ΔN	-	-
b, c	Roots of pressure-controller transfer function	-	-
B	Constant in sine functions W(x) and Q(x)	1.24	ft ⁻¹
c _p	Specific heat of UO ₂	0.073	Btu/lb _m -°F
C	Constant in sine functions W(x) and Q(x)	0.424	ft
C _b	Boiler fuel heat capacity (UO ₂)	145	Btu/°F
C _c	Cladding specific heat	0.14	Btu/lb _m -°F
d _c	Cladding thickness	0.00125	ft
D	Pressurization constant [see Eq. (32)]	930	Btu/psi
g	Acceleration due to gravity	32.2	ft/sec ²
g ₀	Gravitational constant	32.2	lb _m -ft/lb _f -sec ²
G	Gain of pressure controller	-	psi ⁻¹
h _{fg}	Heat of vaporization	728	Btu/lb _m
h _F	Average film coefficient in subcooled and boiling regions	6825	Btu/hr-°F-ft ²
h _g	Enthalpy of saturated steam (600 psig)	1203	Btu/lb _m
h _G	Conductance of helium gas space in clad fuel pin	∞	Btu/hr-°F-ft ²
$\bar{H}$	Average overall heat-transfer coefficient	3333	Btu/hr-°F-ft ²
H _v	Volumetric enthalpy of vaporization	969	Btu/ft ³
I _w	Effective inertia of water and steam in recirculating loop circuit	9.37 x 10 ⁻⁴	lb _f -sec ² /in. ² -lb _m
j	$\sqrt{-1}$	-	-
J	Energy conversion factor	5.4	ft ³ -lb _m /Btu-in. ²
K	Average thermal conductivity of UO ₂	See Table XLII	
K _c	Thermal conductivity of cladding	10.6	Btu/ft-°F-hr
ℓ*	Prompt-neutron lifetime (at zero power)	3.22 x 10 ⁻⁵	sec
L	Height of active core	2.0	ft
L ₁	Height of first riser above core	0.33	ft
L ₂	Height of second riser above core	0.33	ft
L ₃	Height of third riser above core	4.408	ft
L _E	Equivalent length of voided region	13.75	ft
m	Number of various cross-section elements in recirculation path	6	-

Constants (Contd.)

M_f	Mass of saturated water in vessel	4230	lb _m
M_g	Mass of saturated steam in vessel	125	lb _m
M_m	Total mass of fluid in reactor vessel	10,075	lb _m
M_r	Mass of subcooled water in vessel	5720	lb _m
P	Proportional band setting in PC-1	50	%
R	Reset rate setting in PC-1	0.2	Repeats/min
s_n	Poles in the heat-transfer function	-	-
T	Rate time setting in PC-1	0.05	min
T_c	Average temperature of moderator water (coolant)	486	°F
T_w	Effective recirculation delay (transit) time	9.49	sec
v_f	Specific volume of saturated water	0.0201	ft ³ /lb _m
v_g	Specific volume of saturated steam	0.750	ft ³ /lb _m
v_{fg}	$v_g - v_f$	0.730	ft ³ /lb _m
V	Total volume of water and steam inside reactor vessel (excluding superheater steam coolant space)	294	ft ³
V_c/a_c	Volume of cladding per unit of cross-sectional area	0.0012	ft
V_f	Total volume of UO ₂ fuel	2.04	ft ³
x_b	Average distance of boiling boundary from bottom of active core	0.70	ft
α	Thermal diffusivity of UO ₂	8.98×10^{-6}	ft ² /sec
$\bar{\beta}$	Delayed-neutron fraction	0.0071	-
β_i, λ_i	Delayed-neutron parameters	See Table XLI	-
$\partial h_f / \partial P$	Partial derivative in D	0.20	Btu/lb _m -psi
$\partial h_g / \partial P$	Partial derivative in D	-0.017	Btu/lb _m -psi
$\partial k_d / \partial \bar{T}$	Doppler coefficient of reactivity	2.14×10^{-3}	%Δk/°F
$\partial v_f / \partial P$	Partial derivative in D	3.75×10^{-6}	ft ³ /lb _m -psi
$\partial v_g / \partial P$	Partial derivative in D	-1.25×10^{-3}	ft ³ /lb _m -psi
ΔP	Reactor pressure control band	800	psi
$\Delta \rho_r$	Effective coolant density increase in third riser	1.0	lb _m /ft ³
ρ	Density of UO ₂	636	lb _m /ft ³
ρ_c	Density of cladding	400	lb _m /ft ³
ρ_f	Density of saturated water (600 psig)	49.6	lb _m /ft ³
ρ_g	Density of saturated steam (600 psig)	1.33	lb _m /ft ³
$\partial \rho / \partial N$	Power coefficient of reactivity (at 20 MWt)	0.3	% Δk/k/MWt
$\partial \bar{\alpha} / \partial N$	Slope of void fraction vs power	-	-
$\partial \rho / \partial \bar{\alpha}$	Void coefficient of reactivity	-	-
τ_o	Cladding time constant ($\rho_c C_c V_c / \bar{h} a_c$)	1.1	sec
τ_n	$-1/s_n$	See Table XLII	-
τ_p	Effective back-pressure control-valve lag	2.0	sec

Variable and Parameter Symbols and
Steady-state Values (at 20 MWt)*

a_g	Transient steam cross-sectional area in active core	-	ft ²
$A_f(x)$	Water cross-sectional area in active core	-	ft ²
$A_g(x)$	Steam cross-sectional area in active core	-	ft ²
C_B	Turbine bypass valve capacity	0.0019	lb _m /sec-psi
C_i	Concentration of effective delayed-neutron emitters	-	-
C_t	Turbine inlet valve capacity	0.0427	lb _m /sec-psi
C_v	Main steam back-pressure control-valve capacity	0.722	lb _m /sec-psi ^{1/2}
C_{vm}	Main steam back-pressure control-valve maximum capacity	1.85	lb _m /sec-psi ^{1/2}
f	Flow-friction coefficient	0.510	psi
$(\partial f / \partial \bar{a}_t)_W$	Flow-friction derivative	1.6	psi
G	Overall reactor transfer function	-	\$^{-1}\$
G_0	Zero-power reactor transfer function	-	\$^{-1}\$
G_0H	Open-loop transfer function	-	-
h_c	Coolant enthalpy at core inlet	469.2	Btu/lb _m
h_{crs}	Enthalpy of control-rod seal water	86	Btu/lb _m
h_{di}	Enthalpy of downcomer instrument flow	86	Btu/lb _m
h_{dm}	Enthalpy of demineralizer flow	86	Btu/lb _m
h_f	Enthalpy of saturated water (600 psig)	475	Btu/lb _m
h_{fw}	Feedwater inlet enthalpy	86	Btu/lb _m
h_r	Enthalpy of mixed water at sparger outlet	469.2	Btu/lb _m
$\bar{h}_r$	Average enthalpy of subcooled water	469.2	Btu/lb _m
$h_w(x)$	Recirculating water enthalpy in active core below boiling boundary	-	Btu/lb _m
H	Feedback transfer function	-	\$
I_0, I_1, K_0	Bessel functions	-	-
$k, \Delta k$	Multiplication constant, excessive reactivity	-	-
M	Number of terms in heat-transfer function	11	-
N	Power level	1.90×10^4	Btu/sec
P_1	Reactor pressure	600	psig
P_2	Superheater exit pressure	585	psig
P_3	Turbine inlet pressure	575	psig
$P(t)$	Volumetric power generation in boiler	5.48×10^3	Btu/sec-ft ³
q	Complex operator, $\sqrt{s/a}$	-	ft ⁻¹
Q	Power transferred to coolant in boiler	1.575×10^4	Btu/sec
$Q(x)$	Power transferred per unit distance, $A \sin B(x+C) \cdot Q_0/L$	-	Btu/sec-ft
r	Radial distance in fuel	-	ft

Variable and Parameter Symbols and
Steady-state Values (at 20 MWt)* (Contd.)

s	Laplace variable	-	sec ⁻¹
S	Average slip ratio	1.84	-
t	Time	-	sec
$\bar{T}$	Average boiler fuel temperature (UO ₂)	945	°F
T _{f(r)}	Temperature of fuel	-	°F
$\bar{V}$	Average velocity of subcooled fluid in loop	-	ft/sec
$\bar{V}_f$	Average water velocity in active core	7.1	ft/sec
$\bar{V}_g$	Average steam velocity in active core	13.1	ft/sec
W	Mass flow rate through active core	935	lb _m /sec
W _B	Turbine bypass flow	0.55	lb _m /sec
W _{crs}	Net control-rod seal in-flow leakage	0.55	lb _m /sec
W _d	Downcomer flow	950	lb _m /sec
W _{di}	Downcomer instrument flow	0.55	lb _m /sec
W _{dm}	Demineralizer flow	3.65	lb _m /sec
W _{fg}	Total steam generation rate	12.5	lb _m /sec
W _{fw}	Feedwater flow rate	16.7	lb _m /sec
W _s	Total steam flow	12.5	lb _m /sec
W _t	Turbine steam flow	11.94	lb _m /sec
W _v	Total natural-convection flow	963	lb _m /sec
W(x)	Void-reactivity axial weighting function A sin B(x+C)	-	-
x	Axial distance in active core	-	ft
y	Distance along recirculation path	-	ft
z	Axial position in fuel pin	-	ft
α	Average weighted void fraction in active boiler zone	0.106	-
$\bar{\alpha}_E$	Void fraction at boiler zone exit	0.310	-
$\bar{\alpha}_n$	Net average weighted void fraction	0.106	-
$\bar{\alpha}_t$	True average (unweighted) void fraction in active boiler zone	0.132	-
Δh	Subcooling of recirculating water	5.25	Btu/lb _m
Δp _B	Pressure drop across turbine bypass valve	300	psi
Δp _f	Frictional head loss in loop	0.510	psi
Δp _L	Steam-line pressure drop	10	psi
Δp _t	Turbine pressure drop (including desuperheater)	300	psi
Δp _v	Driving head in loop	0.510	psi
Δp _v	Pressure drop across main steam back-pressure control valve	285	psi
θ	Angular position in fuel pin	-	rad
ω	Angular velocity	-	rad/sec

*Subscript "o" in text implies values of variables corresponding to design power of 20 MWt.

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