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EXPERIMENTS ON BOILING CORE B-2,
BORAX-V

by

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TABLE OF CONTENTS

	<u>Page</u>
I. INTRODUCTION.	11
II. REACTOR DESCRIPTION	11
III. REACTIVITY MEASUREMENTS	15
A. Zero-power Reactivity Measurements	15
1. Loading Experiments	15
a. Loading	15
b. Spiking Experiment.	19
2. Control-rod Reactivity Worths	21
3. Excess Reactivity and Temperature Effects	21
4. Reactivity Effect of Boric Acid	28
B. High-power Reactivity Measurements	30
1. Power Coefficient of Reactivity.	30
2. Xenon and Samarium Reactivity Effects	31
IV. POWER-DISTRIBUTION MEASUREMENTS	35
A. Coarse Neutron-flux Distributions.	35
1. Measurement Techniques.	35
2. Results.	38
3. Analysis	43
B. Power Mapping with Fuel Rods	43
1. Measurement Techniques.	43
2. Results.	46
C. Comparison of Flux-wire and Fuel-rod Results	49
V. IN-REACTOR-VESSEL INSTRUMENTATION MEASUREMENTS.	51
A. Temperature Measurements.	51
1. Fuel Temperatures.	51
2. Water and Steam Temperatures	53
B. Flow Measurements	54
1. Electrical Devices	54
2. Hydraulic Devices	55
C. Other Downcomer Instruments	56

TABLE OF CONTENTS

	<u>Page</u>
D. Neutron-flux Measurements	56
E. Miscellaneous Measurements	57
VI. WATER CHEMISTRY MEASUREMENTS	58
A. Water Quality	58
B. Suspended Solids	58
C. Corrosion and Deposition.	60
D. Chloride Concentration	61
E. Steam Quality	62
F. Radiolytic Gas Production	64
VII. TRANSFER-FUNCTION MEASUREMENTS AND STABILITY ANALYSIS	66
A. Rod Oscillator Experiments	66
1. Description of Equipment.	66
a. Oscillator and Drive	66
b. Transfer-function Measuring Equipment	68
2. Experimental Procedure	70
a. Oscillator Fully Inserted.	70
b. Oscillator Half Inserted.	71
3. Experimental Results, Comparison with Analytical Model Studies	72
a. Zero-power Transfer Function (G_0)	73
b. Normalization of Measured Reactor Transfer Functions.	73
c. Overall Reactor Transfer Function (G)	75
d. Open-loop Transfer Function (G_0H)	79
e. Feedback Transfer Function (H)	81
f. Large, Driving, Reactivity Effects, Comparison with Describing-function Calculations	84
g. Reactor Stability Results	85
B. Noise Analysis Experiments.	88
C. Conclusions.	89
VIII. PLANT PERFORMANCE AND TESTS	91
A. Reactor Vessel	91

TABLE OF CONTENTS

	<u>Page</u>
1. Radiation Effects	91
2. Temperature Distribution	91
3. Water-level Measurement	91
B. Core Structure.	92
1. Control-rod Shroud.	92
2. Belleville Spring	93
C. Fuel.	93
1. Boiling Fuel Rods.	93
2. Boiling Fuel Assembly Boxes	93
3. Fuel Handling and Storage	94
D. Reactor Control	94
1. Electrical	94
2. Control-rod Drives.	95
3. Control Rods	95
4. Results.	96
a. Thickness	96
b. Angularity.	96
c. Straightness and Twist	96
5. Conclusions.	96
E. Nuclear Instruments	96
1. Power Calibration of Nuclear Instruments	97
F. Water and Steam Systems	97
1. Steam System	97
2. Boron Addition and Batch Feed Systems	98
3. Turbine-generator System	99
4. Feedwater System	99
5. Auxiliary Water System.	100
IX. SPECIAL EXPERIMENTS	101
A. Control-materials Experiments	101
B. Fueled-oscillator Experiment.	105

TABLE OF CONTENTS

Page

APPENDICES

A.	Derivation of Feedback Model	106
1.	Zero-power Reactor Kinetics	106
2.	Boiler Fuel-pin Heat Transfer	113
3.	Steam Void Generation	116
a.	Power-void Transfer Function, Boiling Region Only	119
b.	Power-void Transfer Function, Subcooled Region Only	120
	(1) Power-boiling Boundary Transfer Function . .	120
	(2) Boiling Boundary-void Transfer Function . .	121
4.	Recirculation Flow Hydrodynamics	121
a.	Void Effects on Recirculation Flow	122
b.	Recirculation Flow Acceleration Effects on Void Fraction	124
	(1) Velocity Effect in Boiling Region Only, Constant Boiling Boundary	124
	(2) Velocity Effect in Subcooled Region, Changing Boiling Boundary	125
5.	Total Feedback Transfer Function	126
6.	Nomenclature	130
B.	Reactor Describing-function Derivation (Outline)	134
	ACKNOWLEDGMENTS	138
	REFERENCES	139

LIST OF FIGURES

<u>No.</u>	<u>Title</u>	<u>Page</u>
1.	Boiling Core B-2	12
2.	Fuel Rod and Cutaway Section of Boiling Fuel Assembly	13
3.	Boiling Fuel Rod	13
4.	Boron-stainless-steel Poison Rod	14
5.	Flow Rod	16
6.	Second Loading Step, Boiling Core B-1 to B-2	17
7.	Loading Diagram, Boiling Core B-2	18
8.	Spiking Experiment, Boiling Core B-1 to B-2	20
9.	Differential Reactivity Worth of Nine-control-rod Bank and Control Rod No. 1, Boiling Core B-2	22
10.	Differential Reactivity Worth of Control Rods No. 2 and 6, Boiling Core B-2	23
11.	Differential Reactivity Worth of Intermediate and Outside Control-rod Banks, Boiling Core B-2	24
12.	Integrated Reactivity Worth of Nine-control-rod Bank, Boiling Core B-2	25
13.	Available Excess Reactivity versus Molecular Ratio, H_2O/UO_2	26
14.	Nine-control-rod-bank Critical Positions versus Temperature, Boiling Core B-2	27
15.	Available Excess Reactivity versus Temperature, Boiling Core B-2	27
16.	Nine-control-rod-bank Position versus Boric Acid Concentration, Boiling Core B-2	28
17.	Available Excess Reactivity in Boric Acid, Boiling Core B-2	29
18.	Nine-control-rod-bank Position versus Power, Boiling Core B-2	30
19.	Power Coefficient of Reactivity versus Power Level, Boiling Core B-2	32
20.	Control Rod Positions versus Time, Power Operation of Boiling Core B-2	33

LIST OF FIGURES

<u>No.</u>	<u>Title</u>	<u>Page</u>
21.	Control Rod Positions versus Time, Zero Power Following Power Operation of Boiling Core B-2	34
22.	Flux-wire Holder	36
23.	Flux-wire Holder in Fuel Assembly.	36
24.	Flux-wire Counting Equipment	37
25.	Transverse Flux Tilt due to Oscillator, Boiling Core B-2 . . .	39
26.	Axial Neutron-flux Distributions at Zero Power, Boiling Core B-2	40
27.	Axial Neutron-flux Distributions at 15 MWt, Boiling Core B-2	41
28.	Point-to-average Axial Neutron-flux Distributions, Boiling Core B-2	42
29.	Two-group PDQ Calculation of Effect of Flow Rods on Transverse Thermal Neutron-flux Distribution in Fuel Cell, Boiling Core B-2	44
30.	Apparatus for Gamma Scanning of Fuel Rods.	45
31.	Desired and Actual Locations of Irradiated Fuel Rods, Boiling Core B-2	46
32.	Fuel-rod Point-to-average Power Distribution, Boiling Core B-2	47
33.	Fuel-rod Point-to-average Power Distribution in Core Position 54, Boiling Core B-2.	48
34.	Comparison of Axial Traverses Using Fuel Rod and Zirconium Flux Wire, Reactor Power 15 MWt, Boiling Core B-2	49
35.	Boiling Fuel Temperature versus Cumulative Reactor Energy at Fixed Reactor Power Levels, Boiling Core B-2 . . .	52
36.	Downcomer Drag-disk Flowmeter, Simplified Schematic and Block Diagrams	56
37.	Suspended Solids Filtration System, BORAX-V Sample Station	59
38.	Steam Dome Probe Schematic Diagram, Boiling Core B-2 . . .	63
39.	Steam Quality Profile, Boiling Core B-2.	64

LIST OF FIGURES

<u>No.</u>	<u>Title</u>	<u>Page</u>
40.	Comparison of Radiolytic-gas Production Rates	65
41.	Section through Oscillator Rod, Boiling Core B-2.	67
42.	Oscillator Drive System	68
43.	Block Diagram of Equipment Used to Measure Transfer Function	69
44.	Oscillator Reactivity Worth versus Power, Boiling Core B-2	71
45.	Overall Gain (G) and Phase versus Frequency for Various Power Levels and Oscillator Insertions (Out-of-core Ion Chamber), Boiling Core B-2.	74
46.	Overall Gain and Phase versus Frequency for Various Power Levels (In-core Ion Chamber), Boiling Core B-2	77
47.	Overall Gain (G) and Phase versus Calculated Frequency, Boiling Core B-2	78
48.	Open-loop Response (G_0H) for Various Power Levels and Oscillator Insertions, Boiling Core B-2	79
49.	Nyquist Diagram of Open-loop Transfer Function, G_0H (β/ℓ^* -adjusted), Boiling Core B-2.	80
50.	Nyquist Diagram of Open-loop Transfer Function, G_0H (Calculated), Boiling Core B-2	81
51.	Gain and Phase of Feedback (H) for Various Power Levels and Oscillator Insertions, Boiling Core B-2	82
52.	Gain and Phase of Feedback (H) versus Frequency (Calculated), Boiling Core B-2	83
53.	Kinetics Nonlinearity Effect on Overall Gain Using Describing-function Analysis with Linear Feedback, Boiling Core B-2	84
54.	Gain and Phase Margins of Open-loop Feedback (G_0H) versus Power, Boiling Core B-2	86
55.	Relative rms Noise versus Power and Unstable Power Predicted by rms Noise Analysis, Boiling Core B-2	89
56.	Control-materials Test, Clean Core B-2	101
57.	Samples and Holder for Control-materials Test, Boiling Core B-2	102

LIST OF FIGURES

<u>No.</u>	<u>Title</u>	<u>Page</u>
58.	Axial Neutron Flux Traverses, Mock-up Fueled Oscillator Test, Boiling Core B-2	105
59.	Average Void Volume Fraction versus Power Level (Calculated), Boiling Core B-2	107
60.	Recirculation Flow versus Power (Calculated), Boiling Core B-2	108
61.	Average Slip Ratio versus Power (Calculated), Boiling Core B-2	109
62.	Subcooling versus Power (Calculated), Boiling Core B-2	110
63.	Flow Friction Parameters versus Power Level (Calculated), Boiling Core B-2	111

LIST OF TABLES

<u>No.</u>	<u>Title</u>	<u>Page</u>
I.	Reactivity Results during Change from Core B-1D to Core B-2	19
II.	Results of Analysis of Fuel Rod Gamma-scanning Data	48
III.	Boiling Fuel Rod Center Temperatures, °F	52
IV.	Boiling Fuel Rod Time Constants	53
V.	Core-inlet Subcooling Temperatures	54
VI.	Suspended Insoluble Solids, Cores B-1 and B-2	59
VII.	Emission Spectrographic Analysis of Suspended Solids, Core B-2	60
VIII.	Constituents of Radiolytic Gas	65
IX.	Control Materials Experiments Raw Data	103
X.	Relative Reactivity Worth of Samples of Control Materials	104
XI.	Delayed-Neutron Parameters	112
XII.	Fuel-pin Heat-transfer Constants	116
XIII.	Feedback Transfer Function Parameters versus Power Level	122

EXPERIMENTS ON BOILING CORE B-2, BORAX-V

by

BORAX-V Project Staff

I. INTRODUCTION

Boiling core B-2 was loaded into BORAX-V to complete the initial program of boiling core experiments whose primary purposes were to shake down the reactor and steam plants and to determine certain reactor characteristics before proceeding with the testing of cores with integral nuclear superheaters.

At the conclusion of the zero-power experiments at operating conditions on core B-1D, it was determined that the available excess reactivity (with the oscillator rod installed) at 489°F, 600 psig, was only about 3.2%.⁽¹⁾ Based on the measured reactivity worth of voids at room temperature and the expectation that void worth would be higher at operating temperature, it was estimated that the rated full power of 20 MWt probably could not be achieved. Therefore, the planned power operation of core B-1D was cancelled, and core changes were started for core B-2, designed to increase the available excess reactivity.

Two methods of increasing available excess reactivity were tried. The method selected for core B-2 (that of increasing the water-to-fuel ratio near the center of each boiling fuel assembly) was selected because it flattened the neutron flux distribution within an assembly and was expected to reduce the reactivity effects of temperature and steam voids and thus increase the available excess reactivity.

Attempts to increase reactivity by spiking with 9.9 w/o-enriched fuel rods in the outer row of fuel assemblies were unsatisfactory because of the violation of the single-control-rod criticality criterion on the intermediate control rods for only a very minor gain in reactivity.

Poison rods next to the intermediate and central control rods were used to reduce control-rod reactivity worth and the available excess reactivity in the core so that the reactor could not be made critical on the withdrawal of a single control rod alone.

II. REACTOR DESCRIPTION⁽²⁾

The BORAX-V reactor and power plant equipment are located in two buildings one-half mile from the Control Building. The turbogenerator is located 200 ft from the Reactor Building.

The reactor vessel is a cylinder with ellipsoidal heads $5\frac{1}{2}$ ft ID and 16 ft internal height. The core was centered about 4 ft from the vessel bottom. Three separate core configurations were used: (1) a boiling core without a superheater, (2) a boiling core with a centrally located superheater, and (3) a boiling core with a peripherally located superheater.

The boiling core was 24 in. high and had an effective diameter of 39 in. when containing the maximum of sixty $3\frac{7}{8}$ -in.-sq. fuel assemblies arranged in an 8 x 8 array with corner assemblies missing. Each square cell of four assemblies was surrounded by adjacent control-rod channels. Figure 1 is a photograph of the boiling core.

Fig. 1
Boiling Core B-2

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The boiling assemblies were composed of individually removable fuel rods (the number of rods per assembly could be varied from 0 to 49) made of UO_2 of 4.95 w/o enrichment (design reference was 16.3 g of U^{235} per rod) with 0.015-in., Type 304, stainless-steel cladding. Fuel rods of 9.90 w/o enrichment were used for special experiments. The outside diameter of the rods was 0.375 in. (see Figs. 2 and 3). Poison rods of stainless steel or boron-stainless steel could be substituted for fuel rods for flux flattening and excess reactivity adjustment. The design of the boron-stainless-steel rods is shown in Fig. 4. All the poison rods used as part of the loadings for core B-2 were made of boron-stainless steel.

The nine control rods were made of Boral, canned in stainless steel. 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.

Fig 2. Fuel Rod and Cutaway Section of Boiling Fuel Assembly

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Fig 3. Boiling Fuel Rod

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Fig. 4. Boron-stainless-steel Poison Rod

III. REACTIVITY MEASUREMENTS

A. Zero-power Reactivity Measurements

1. Loading Experiments

a. Loading. The core loading was changed from core B-1D to core B-2 by replacing four 4.95 w/o-enriched boiling fuel rods in each of 59 boiling fuel assemblies with four flow rods or their equivalents. A flow rod, as shown in Fig. 5, has the same outside geometry and dimensions as a boiling fuel rod, but the in-core portion is made of X-8001 aluminum tubing $\frac{3}{8}$ in. OD $\times$ 0.020-in. wall thickness, perforated on each end to ensure flooding with reactor water. (During the initial stages of this loading change, the required number of flow rods was not available. Therefore, two water rods, containing approximately the same amount of aluminum as four flow rods, and two water holes were used temporarily as being equivalent to four flow rods.) The use of flow rods to increase the moderator fraction of the core prevents by-pass of coolant water through a vacant fuel-rod location.

The decision to add four flow rods per assembly was based on experiments in core B-1A⁽¹⁾ in which the water-to-fuel ratio was increased by replacing five central fuel rods with water rods. (Five water rods contain approximately the same amount of water as four flow rods.) These experiments indicated that available excess reactivity would be increased, the void coefficient of reactivity would be decreased, and the maximum-to-average power ratio within a boiling fuel assembly would be reduced.

The change from core B-1D to core B-2 was made in a series of loading steps. After each step, a nine-control-rod-bank critical position was attained, and frequent checks were made for criticality on the withdrawal of a single control rod alone. Two additional poison rods were added, next to each of control rods No. 2 and 4, to correct this situation after the central 16 fuel assemblies had been changed. A diagram of the core loading at this stage is shown in Fig. 6. Throughout the loading change, the oscillator assembly and oscillator rod were located in core position 75, and stainless-steel rods simulating stainless-steel, miniature, ion-chamber thimbles were located as shown in Fig. 7. In the last stage of the loading operation, single-control-rod criticality checks permitted all poison rods next to control rod No. 6 to be replaced with 4.95 w/o-enriched fuel rods. Control rod No. 8 required only five poison rods, while control rods No. 2 and 4 each required six. This reduced effect of withdrawing control rods No. 6 and 8 is explained by their proximity to the oscillator rod and assembly in core position 75.

SECTION THRU ROD

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Fig. 5. Flow Rod

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Fig. 6. Second Loading Step, Boiling Core B-1 to B-2

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Fig. 7. Loading Diagram, Boiling Core B-2

The final loading configuration for core B-2 is shown in Fig. 7. This core contained 33 boron-stainless-steel poison rods, 236 flow rods, five $\frac{3}{8}$ -in.-OD, stainless-steel miniature ion-chamber thimbles, six $\frac{1}{8}$ -in.-OD, stainless-steel flux wire thimbles, one oscillating rod, one instrumented boiling fuel assembly containing five thermocouple fuel rods, and 2667 (4.95 w/o-enriched) boiling fuel rods.

The reactivity conditions before, during, and after the loading change are shown in Table I.

Table I

REACTIVITY RESULTS DURING CHANGE FROM CORE B-1D TO CORE B-2

Loading Condition	Reference	Nine-control-rod Bank Position (in.)	Temp (°F)	Available Excess Reactivity (%)
Core B-1D	1	9.165	78	9.1
Flow rods in center 16 fuel assemblies	Fig. 6	9.054	78	~10.7
Flow rods in 27 fuel assemblies	Fig. 8	8.972	74	~10.8
Spiking experiment No. 1	Fig. 8	8.906	74	~10.9
Spiking experiment No. 2	Fig. 8	8.902	74	~10.91
Core B-2	Fig. 7	8.752	80	11.25

A removable antimony source and source drive were used during both the cold and hot zero-power critical experiments to permit determination of true critical positions. Experience has shown that source removal is of real value in improving the accuracy of period measurements during cold critical work but not during hot criticals, when nuclear instrument chambers are at some distance from the core and reactor power during period measurements is higher.

b. Spiking Experiment. During the change from core B-1 to core B-2, two experiments were performed to investigate the effect of spiking with 9.90 w/o-enriched boiling fuel rods on reactivity and single-control-rod criticality. These experiments were made when four flow rods or their equivalents had been loaded into 27 boiling fuel assemblies located in the core center and east quadrant, as shown in Fig. 8.

In the first spiking experiment, fifty-eight 9.90 w/o-enriched boiling fuel rods were installed in core positions 28 and 38 on the core periphery in place of 4.95 w/o-enriched rods. In the second

spiking experiment, fifty-five 9.90 w/o-enriched fuel rods were installed in core positions 28, 38, and 48. The location of these 9.90 w/o-enriched fuel rods is shown in Fig. 8. The reactivity results of this experiment are shown in Table I.

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Fig. 8. Spiking Experiment, Boiling Core B-1 to B-2

During each spiking experiment, the reactor was made critical by withdrawing No. 4 control rod alone, with all other control rods inserted. Since the gain in available excess reactivity was small and the adverse effect on single-control-rod criticality was large, the decision was made not to spike the core.

2. Control-rod Reactivity Worths

The control-rod reactivity worths were determined, both at room-temperature and zero-power operating conditions of 600 psig, 489°F, by measuring the period produced when a control rod or group of rods was raised slightly above the critical position. Boric-acid solution was added in increments to compensate for adjustment of control-rod critical positions. In this manner, the control rods were calibrated over a range of about 9 to 22 in.

The differential reactivity-worth curves for both room-temperature and operating conditions are shown in Figs. 9, 10, and 11 for the nine-control-rod bank, control rods No. 1, 2, and 6, and the intermediate and outside control-rod banks.

The influence of the oscillator rod and assembly in core position 75 on the reactivity worth of adjacent intermediate control rod No. 6 is illustrated in Fig. 10. Although all poison rods around control rod No. 6 had been removed and additional poison rods installed around control rods No. 2 and 4, as shown in Fig. 7, the differential reactivity worth of control rod No. 6 was less than that of control rod No. 2 in the opposite quadrant of the core. The explanation for this difference lies in the asymmetrical flux shape across the core caused by the oscillator rod. A radial flux traverse at room temperature, taken along a line through core positions 14 to 84 near the reactor centerline, is shown in Fig. 25. The thermal flux level measured in core position 14 was 1.4 times that for symmetrical core position 84 near the oscillator.

Figure 12 shows the integral reactivity worth $[\int_{Z'}^{26} \text{differential worth } (Z')dZ']$, where Z' is the central rod position in inches] of the nine-control-rod bank versus control-rod position measured both at room-temperature and operating conditions.

3. Excess Reactivity and Temperature Effects

From the curves of integral control-rod reactivity worth of Fig. 12, the available excess reactivity for core B-2 was determined to be 11.2% at 80°F and 6.6% at 489°F, 600 psig, for a reactivity loss of 4.6% due to increasing temperature from ambient to operating conditions. The corresponding values for core B-1D⁽¹⁾ were 9.1% at 80°F and 3.2% at 489°F, 600 psig, for a reactivity defect of 5.9%.

Cores B-1D and B-2 may be compared qualitatively by the plot of Fig. 13. The available excess reactivities of the hot and cold cores are compared with their H₂O-to-UO₂ molecular ratios. The reduction in density of water upon heating is the major reason for the reactivity loss with increasing temperature, although such factors as Doppler resonance

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Fig. 9. Differential Reactivity Worth of Nine-control-rod Bank and Control Rod No. 1, Boiling Core B-2

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Fig. 10. Differential Reactivity Worth of Control Rods No. 2 and 6, Boiling Core B-2

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Fig. 11. Differential Reactivity Worth of Intermediate and Outside Control-rod Banks, Boiling Core B-2

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Fig. 12. Integrated Reactivity Worth of Nine-control-rod Bank, Boiling Core B-2

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Fig. 13. Available Excess Reactivity versus Molecular Ratio, H_2O/UO_2

broadening and core expansion are also present. The curve roughly suggests that a further increase of available excess reactivity could be achieved with a higher H_2O -to- UO_2 ratio. The ratios given are based on all-core, averaged volume fractions, ignoring in-core instrumentation thimbles and the oscillator.

A plot of nine-control-rod-bank critical positions at various temperatures, both with and without boric acid, is shown in Fig. 14. Critical positions with no boric acid in the reactor water were measured at several intermediate temperatures. Critical positions with boric acid in the reactor to permit additional control-rod withdrawal were measured only at room and operating temperatures. From Fig. 14, and by linear interpolation from the differential control-rod reactivity worth curves of Fig. 9, the curve of excess available reactivity versus temperature shown in Fig. 15 was derived.

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Fig. 14. Nine-control-rod-bank Critical Positions versus Temperature, Boiling Core B-2

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Fig. 15. Available Excess Reactivity versus Temperature, Boiling Core B-2

4. Reactivity Effect of Boric Acid

All boric-acid concentrations in the reactor water were analyzed at room temperature and pressure, and all concentrations are reported for these conditions. A curve of nine-control-rod-bank position versus boric-acid concentration is shown in Fig. 16 for both room-temperature and operating conditions. A plot of available excess reactivity held down by boric acid versus boric-acid concentration for both conditions is shown in Fig. 17. The differential reactivity worth of boric acid as indicated by the slope of the curves is 0.65% per $\text{g}(\text{H}_3\text{BO}_3)/\text{gal}(\text{H}_2\text{O})$ at 95°F and 0.52% per $\text{g}(\text{H}_3\text{BO}_3)/\text{gal}(\text{H}_2\text{O})$ at 489°F .

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Fig. 16. Nine-control-rod-bank Position versus Boric Acid Concentration, Boiling Core B-2

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Fig. 17. Available Excess Reactivity in Boric Acid, Boiling Core B-2

B. High-power Reactivity Measurements

1. Power Coefficient of Reactivity

The power coefficient described in this section is the "effective power reactivity effect coefficient" as opposed to the "power coefficient of excess reactivity." The former coefficient (hereafter called power coefficient) is a measure of reactivity changes caused by changes in power with a fixed reactor configuration (i.e., constant control rod position), while the latter expresses reactivity changes caused by varying the configuration. This effect has been determined for a series of fixed reactor system conditions, i.e., at various power levels with fixed, but different,

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Fig. 18. Nine-control-rod-bank
Position versus Power,
Boiling Core B-2

control-rod-bank position at each power level. In a boiling-water reactor, a change in power level also changes the steam void content of the core, thus changing the control-rod reactivity worths. Control-rod calibrations at zero-power, operating-temperature conditions cannot be used for accurate determination of the power coefficient of reactivity. Also, the usual static period measurements to establish differential control-rod reactivity worths are not possible at higher powers in a boiling-water reactor, because withdrawal of control rods results in a trivial asymptotic period and merely changes void content and power. Figure 18 shows a graph of nine-control-rod-bank position versus power for core B-2.

The power coefficient was evaluated indirectly, utilizing results of the rod oscillator experiments and the theoretical model studies described later in Section VII. The power coefficient could not be determined directly from the measured transfer functions (by extrapolation to zero frequency) because of inadequate low-frequency data. A theoretical dynamic model was derived with various assumptions, the principal one being the absence of pressure effects above a frequency of 0.5 cps, and adjusted to fit the measured reactor frequency response. The parameter of particular interest here is the power coefficient of reactivity required to fit the measured data at each power level. The general expression defining the power coefficient, as used in the model study, is given on pages 127 and 128 and can also be described as follows:

$$H(s) = P_0 \left(\frac{\partial \rho_C}{\partial P} \right) \cdot T(s) = \frac{1}{G(s)} - \frac{1}{G_0(s)},$$

where

s = Laplace variable,

P_0 = Mean reactor power level,

ρ_C = Reactivity held down by control rods,

$\partial \rho_C / \partial P$ = Power coefficient of reactivity,

$H(s)$ = Theoretical feedback model adjusted to fit the experimental G and G_0 ,

$T(s)$ = Transfer function with unity gain coefficient having the same phase response as $H(s)$,

$G(s)$ = Measured overall reactor transfer function at power,

and

$G_0(s)$ = Measured zero-power reactor transfer function.

Values of the power coefficient were thus obtained at power levels of 5, 10, 15, and 16.5 MWt from the rod oscillator measurements and theoretical model by using the above equation. The point at zero power (hot), however, was determined from the known control-rod calibration, $(\partial \rho_C / \partial h)_{P=0}$, as follows: $\rho_C = \rho_C(h, P)$, where control-rod worth is assumed a function of rod height (h) and power (P). Therefore

$$\frac{d\rho_C}{dP} = \left(\frac{\partial \rho_C}{\partial h} \right)_P \cdot \frac{dh}{dP} + \left(\frac{\partial \rho_C}{\partial P} \right) = \text{power coefficient of excess reactivity, and at any power,}$$

$$\frac{\partial \rho_C}{\partial P} = \left(\frac{\partial \rho_C}{\partial h} \right)_P \cdot \frac{dh}{dP} = \text{power coefficient.}$$

The power coefficient can thus be determined at zero power by obtaining $(\partial \rho_C / \partial h)_{P=0}$ and dh/dP from Figs. 9 and 18, respectively. The resulting variation of power coefficient with power is shown in Fig. 19.

2. Xenon and Samarium Reactivity Effects

Because of the briefness of the experimental program of power operation on core B-2, no sustained power runs were made at various power levels to measure the reactivity effect of equilibrium xenon. Also, no sustained post-shutdown critical experiments were made to determine the

reactivity effect of xenon buildup. The reactivity effect from the samarium in the core resulting from the low, total integrated power of 65 MWd was insignificant. However, the data have been analyzed to glean some limited information about xenon reactivity effects.

ID-103-A3208

Fig. 19. Power Coefficient of Reactivity versus Power Level, Boiling Core B-2

Boiling core B-2 was intermittently operated at power from December 2, 1962, to February 16, 1963. Power operation was therefore subject to considerable variation in level and duration, and is not easily analyzed in terms of reactivity effects.

Significant points concerning the operation are as follows:

(1). The control-rod positions for selected power runs are shown in Fig. 20. The effect of xenon is qualitatively suggested.

On February 4, 1963, 20 MWt was achieved with the nine-control-rod bank at 24 in. On February 7, after sustained operation at various levels (average level, 8.35 MWt for 69 hours), 15 MWt was obtained with the nine-control-rod bank at 24.050 in. Since no control-rod calibration is available at these powers, no reactivity effect of xenon has been deduced.

ID-103-A3209

Fig. 20. Control Rod Positions versus Time,
Power Operation of Boiling Core B-2

(2). The zero-power, nine control-rod position $2\frac{1}{2}$ hours following the period of continuous power operation was 14.420 in., or 1.2% reactivity was held in xenon at this time. The integrated power for the operation to that time was 700 MWh, with an average power of 9.27 MWt. Eight hours later, 10 MWt was obtained with the nine-control-rod bank at

21 in., whereas this level could be achieved at 18.25 in. in the clean reactor. This was near the point where maximum xenon buildup after shutdown would occur. A portion of the curve of control-rod position versus time from zero-power critical experiments after shutdown is shown in Fig. 21. The point of maximum rod withdrawal is assumed to be near the peak of the xenon buildup. This point corresponds to a reactivity value of 1.7% in xenon.

Fig. 21

Control Rod Positions versus Time,
Zero Power Following Power Operation
of Boiling Core B-2

ID-103-A3112

IV. POWER-DISTRIBUTION MEASUREMENTS

Only a few, coarse, neutron-flux-distribution measurements were made on core B-2. A diametral measurement was made at room-temperature conditions to check the effect of the oscillator rod assembly on flux distribution. Measurements were made at several power levels at 600 psig, 489°F.

No fine power-distribution measurements were made with flux wires on core B-2, but gamma scanning of boiling fuel rods irradiated at one power level produced good results.

A. Coarse Neutron-flux Distributions

1. Measurement Techniques

Measurement techniques and counting equipment similar to those used on core B-1⁽¹⁾ were also used on core B-2.

Three different detector materials were used for the coarse flux-mapping measurements. These were:

- a. Gold wire, 0.032-in. diam.
- b. Gold-5 w/o iron alloy, 0.032-in. diam.
- c. Reactor-grade zirconium, 0.032-in. diam.

For the room-temperature irradiations, gold wire segments were $3/8$ in. long and arranged within a wire holder so that their longitudinal centers were at 1-in. intervals, beginning at the base of the fuel and rising vertically to 5 in. above it. Aluminum spacers were placed between wire segments. The flux-wire holders were made of aluminum tubing, 0.125-in. diam. x 0.020-in. wall, with a support and guides for reproducible positioning. An aluminum wire holder and a representative wire segment loading are shown in Fig. 22. The wire holders were inserted into a water channel of the fuel assembly and were positioned by the upper and lower grids. The upper shoulder of the holder controlled the insertion distance by resting on the upper grid of the fuel assembly, as shown in Fig. 23. To normalize counts, a monitor wire of each material used was irradiated at the same radial and axial position in each irradiation for purposes of data comparison. The activity induced in the short flux wires was gamma-counted with sodium iodide scintillation crystals in a standard pig arrangement. Preset count control, using the monitor foil of the material being counted, permitted automatic decay correction. An overall view of the counting equipment is shown in Fig. 24.

ID-103-A3225

Fig. 22. Flux-wire Holder

ID-103-3807

Fig. 23. Flux-wire Holder in Fuel Assembly

ID-103-3622

Fig. 24. Flux-wire Counting Equipment

A rotary scanner was normally used for gamma-counting the wires. The wires were attached to the rim of a large aluminum disk with a lead hub and passed in front of the counting head by an automatic sequencing system which controlled the incremental movement of the wheel, counting time, and data print-out. The same preset count technique used for the segments determined the count time duration.

For the irradiations at operating temperature and pressure, continuous flux wires were inserted in six 1/8-in.-OD x 0.020-in.-wall, stainless-steel flux-wire thimbles located in core coolant channels, as shown in Fig. 7. These pressure-tight thimbles had extensions through the reactor top shield which permitted manual insertion and withdrawal of the flux wires attached to a carrier wire at any reactor power. The wires, during power operation, were inserted in sequence to a point above the core, rapidly inserted into the core, and irradiated for 5 minutes; a reverse procedure was followed in withdrawing.

Gold wires, stiffened by the addition of 5 w/o iron, were used for the zero-power work at 489°F, 600 psig, and reactor-grade zirconium wire

was used for measurements at power. The activity of the zirconium was found to be due primarily to Zr^{97} , with a 17-hr half-life, although Mn^{56} (an impurity) with a 2.56-hr half-life is present. Counting was delayed until the manganese activity had subsided.

While the counting in the B-2 series was done primarily with the rotary counter, delay in processing two of the six 30-in. wires irradiated at zero power and operating temperature resulted in counting rates that were too low for adequate statistical averaging by this means. To salvage the information contained on wires from thimbles W3 and W5, a temporary linear counter was constructed. Two lead bricks were clamped together, and a 3/8-in.-diam. hole was drilled from end to end along the contacting faces. The wires, held in a milled slot, were moved through the hole in 1-in. steps by a lead screw arrangement. Counting was accomplished by mounting a shielded photomultiplier and NaI crystal over a 3/8-in.-diam. hole drilled at right angles to the wire. Thus, counting two of the wires previously processed on the rotary counter resulted in a factor for adjusting the relative magnitudes of activation of the two wires from thimbles W3 and W5.

2. Results

A transverse profile of the thermal-flux distribution, measured at room temperature, with gold wire segments 0.032 in. diam. x 3/8 in. long, mounted in aluminum flux-wire holders, is shown in Fig. 25. The overall flux tilt across the core caused by the oscillator assembly in core position 75 is clearly shown.

Axial traverses measured in the six flux-wire thimbles with Au-5 w/o Fe wire at 600 psig, 489°F, zero-power conditions are shown in Fig. 26. The effect of the adjacent control rod on the flux distribution is illustrated by curve W1, compared to the lack of control-rod influence in the other curves. The curves W3 and W5 were counted on a linear traversing arrangement, as discussed in Sec. IV, A, 1 above.

Figure 27 shows the results of measurements made with zirconium wires in the six flux-wire thimbles at a reactor power of 15 MWt. Note that the axial peak flux location has shifted very little, from about 8 to 8.5 in. at zero power, to 9 in. at 15 MWt, even though the nine-control-rod-bank position has changed from about 13 to about 21 in. Note also that the W1 curve is below the W2 and W3 curves. This differs from the zero-power flux distributions shown in Fig. 26. The flux distributions determined by gamma scanning of boiling fuel rods irradiated at 15 MWt differ, as shown in Fig. 33. Here, rods No. 4 and 7 are in positions similar to flux-wire thimble W1, and rod No. 6 is in a position similar to thimble W2.

Point-to-average values for axial neutron-flux distributions at several power levels measured in flux-wire thimbles W2 and W5 are compared in Fig. 28. These curves show the change of flux distribution with power.

ID-103-A3210

Fig. 25. Transverse Flux Tilt due to Oscillator, Boiling Core B-2

ID-103-A3211

Fig. 26. Axial Neutron-flux Distributions
at Zero Power, Boiling Core B-2

ID-103-A3212

Fig. 27. Axial Neutron-flux Distributions at 15 MWt, Boiling Core B-2

ID-103-A3213

Fig. 28. Point-to-average Axial Neutron-flux Distributions, Boiling Core B-2

3. Analysis

Two-group PDQ cell problems were run to obtain thermal disadvantage factors for a boiler fuel assembly with forty-nine 4.95 w/o-enriched fuel rods and for a boiler assembly containing four flow rods and 45 fuel rods. Thermal neutron-flux distributions obtained for the no-flow-rod case (core B-1D) and for the four-flow-rod case (core B-2) at room and operating temperatures are compared in Fig. 29. These curves are normalized to the average flux in the fuel region and include the cold and operating-temperature flux patterns. The customary zero-current boundary condition was specified on all boundaries. The cell model is depicted in the inset drawings.

Disadvantage factors were obtained by utilizing the region flux integrals ($\bar{\phi}$) and dividing by the areas concerned to obtain a region average; these, in turn, are ratioed to give $(\bar{\phi}_{\text{nonfuel}})/(\bar{\phi}_{\text{fuel}})$. The fuel region is the region containing fuel rods. The disadvantage factors for each case are also listed on Fig. 29.

B. Power Mapping with Fuel Rods

1. Measurement Techniques

The boiling fuel assemblies in BORAX-V had a feature not common to most boiling-water reactors in that the fuel rods are individually removable. As a consequence, the spatial power distribution at high powers was obtained by counting fuel rods rather than flux wires. Such a procedure had the advantage that no extraneous materials, such as flux-wire thimbles, were introduced into the core. In addition, and perhaps more important, the resultant data gave the actual power distribution in fuel, rather than a flux distribution measured by nonfuel material in water channels next to fuel rods.

Apparatus designed and fabricated to permit gamma scanning of irradiated fuel rods is shown in Fig. 30. The procedure in using the apparatus follows: Fuel rods had been removed from preselected positions in the core and brought to the stationary counting equipment in the transport cask. The transport shielding cask consisted of a 5-in.-thick, stainless-steel-clad, lead cylinder within which was located a 5.56-in.-diam. cask which held 15 fuel rods latched into place in 15 numbered holes. The inner cask could be rotated to bring the individual fuel rods successively into proper position for insertion into the stationary casks. The transport shielding cask was mounted on a flat-bed dolly and was manhandled into position in the counting trailer.

ID-103-A3214

Fig. 29. Two-group PDQ Calculation of Effect of Flow Rods on Transverse Thermal Neutron-flux Distribution in Fuel Cell, Boiling Core B-2

ID-103-3953

Fig. 30. Apparatus for Gamma Scanning of Fuel Rods

Two stationary casks were provided, one for use with a fuel rod used as a monitor, and another, longer one in which a fuel rod could be traversed past the collimating hole to produce an axial profile. Both casks had a $5\frac{1}{2}$ -in. thickness of lead contained in carbon steel for shielding, and both had a 0.150-in.(ID)-sq. collimating hole 6 in. long, perpendicular to the axial tube that held the fuel rod.

An irradiated fuel rod was moved from the transport cask into the traversing cask by a manually operated fuel-rod manipulator which extended through the traversing cask, grasped the fuel rod, unlatched it from the transport cask, and drew it into the traversing cask. The fuel-rod manipulator, still grasping the fuel rod, was then latched into place in the traversing mechanism and moved past the collimating hole. Change gears were provided for the traversing mechanism so that speeds of 1, 2, or 4 in./min were available. Before a traverse was made, another fuel rod was moved from the transport cask into the monitor cask, the fuel rod manipulator being used as before.

Sodium iodide scintillation crystals for gamma-ray detection were placed in lead shields above the collimating hole on both the monitor cask and the traversing cask, and the pulses from the two counting heads were taken into a count-rate meter which computed the ratio of the two count rates and transmitted this ratio to a recorder chart. The recorder chart had a potentiometer which allowed the ratio of the counts to be adjusted so as to reach the maximum on the recorder when the most active fuel rod in the traversing pig was used. In addition, the pulses were fed into scalars so that 1-min counts could be taken at intervals along the length of the fuel rod in the traversing cask when the fuel-rod motion stopped. The use of the monitor fuel rod, which was irradiated at the same time as the other fuel rods that were scanned, resulted in automatic decay correction, since the two counting systems were calibrated similarly by using a Cs^{137} source.

After all other experiments at power were completed with the B-2 core, 15 fuel rods were removed from one octant of the core and replaced with 15 unirradiated fuel rods. The fuel rods were replaced by working through two of the 6-in. nozzles with the vessel head in place. The presence of the chimneys and hold-down boxes made it impossible to replace the desired rods in six of the 15 cases. Figure 31 shows the desired and actual locations of the unirradiated rods. The fuel rods in the corners of core positions 63 and 64 were chosen because this was found to be the hottest spot in the core during the cold, fine-flux mapping of core B-1B,⁽¹⁾ and the fact that these rods could not be properly located made them useless during the subsequent analysis.

ID-103-A3037

Fig. 31. Desired and Actual Locations of Irradiated Fuel Rods, Boiling Core B-2

After the unirradiated fuel rods were installed in the reactor, the reactor was taken to operating temperature by using electric heaters and reactor power (less than 1 MWt). The rods were then irradiated at a reactor power of 15 MWt for a sufficient length of time so that 90% of the energy produced by the irradiated fuel rods was produced at steady-state operation of 15 MWt. The reactor was shut down and allowed to cool, and the 15 fuel rods were removed from the reactor and placed in the water storage pit to await subsequent gamma scanning.

2. Results

The results of gamma-scanning the irradiated fuel rods are shown in Figs. 32 and 33. Work on fine-flux mapping of a boiling fuel assembly with four flow rods, carried out on core B-1B,⁽¹⁾ indicated that

the power produced within any one of the five remaining fuel rods in the nine central fuel-rod positions was within $\pm 5\%$ of the power produced in an "average" rod in the assembly. The assumption was therefore made that the power produced by those irradiated fuel rods located at or next to fuel rod location No. 25, in the nine fuel assemblies that constituted an octant of the core, was the average power produced by all the fuel rods in the given assembly. With this assumption, the results of the gamma scanning were normalized to the average power production in the core, with both the axial and transverse variation taken into account. This has been done in Figs. 32 and 33. Figure 32 shows the local-to-core-average power for the nine fuel rods at or next to the center fuel rod in each of the nine fuel assemblies in one octant of the core. Figure 33 shows the local-to-core-average power distribution for the five fuel rods that were irradiated in a central fuel assembly in core position 54. In Fig. 33, note that for fuel rods No. 4 and 7 and No. 5 and 8, which are in symmetrical positions in the core, the power distributions are in good agreement. Also note that at the upper end of the core, the power production falls off faster in fuel rods No. 4 and 7 than in fuel rods No. 5, 6, and 8. This fall-off is reasonable, since fuel rods No. 4 and 7 are next to the control rods, and at 15 MWt the control rods are withdrawn 21 in., and the break in the axial curves is at about $20\frac{1}{2}$ in.

ID-103-A3215

Fig. 32. Fuel-rod Point-to-average Power Distribution, Boiling Core B-2

ID-103-A3216

Fig. 33. Fuel-rod Point-to-average Power Distribution
in Core Position 54, Boiling Core B-2

Results of further analysis of the data are presented in Table II. The shapes of the axial traverse curves for all the fuel rods are similar, and the peak/average power for the individual rods varied from 1.33 to 1.38, the 15 fuel rods having an average value of 1.36.

Table II
RESULTS OF ANALYSIS OF FUEL ROD GAMMA SCANNING DATA

Fuel Rod No	Fuel Assembly Core Position	Fuel Rod Axial Peak/Average	Power in Fuel Assembly/Power in "Average" Fuel Assembly	
			At 15 MWt	At Zero Power*
1	51	1.35	0.89	0.87
2	52	1.33	1.17	1.27
3	53	1.36	1.30	1.40
4	54	1.35		
5	54	1.36		
6	54	1.36	1.31	1.27
7	54	1.38		
8	54	1.36		
9	61	1.37	0.80	0.66
10	62	1.36	1.04	1.06
11	63	1.39	1.20	1.28
12	63	1.36		
13	64	1.37		
14	71	1.35	0.64	0.56
15	72	1.36	0.85	0.80
Avg 1.36				

*From fine-power map of core B-1B a core with no flow rods in the center area of fuel assemblies
See ANL 6689 Fig 34

The ratio of the power produced in the particular assembly to that produced in an "average" assembly was obtained, for the 15-MWt case, by assuming, based on Fig. 30 in ANL-6689,⁽¹⁾ that the power produced in the measured fuel rod was typical of all fuel rods in the assembly, and then comparing the area under the curve for that rod, taken from Fig. 32, to the area for the average rod in the core. The corresponding number for the cold, zero-power case was obtained from Fig. 34 in ANL-6689. Note that the effect of power operation has been to flatten the transverse power distribution, due to the greater presence of steam voids in the higher-power fuel assemblies.

ID-103-A3040

Fig. 34. Comparison of Axial Traverses Using Fuel Rod and Zirconium Flux Wire, Reactor Power 15 MWt, Boiling Core B-2

C. Comparison of Flux-wire and Fuel-rod Results

The results of the axial, neutron-flux-distribution measurements made at 15 MWt, shown in Fig. 27, were compared with the results of the gamma scanning of the fuel rods irradiated at 15 MWt, shown in Fig. 32. The location of the six zirconium flux wires was similar to the location of six of the fuel rods, as can be seen from the respective figures. The comparison was made by normalizing two corresponding curves to a value of 1.00 at the point 9 in. above the bottom of the core and plotting the normalized curves together. A typical result of such a comparison is shown in Fig. 34. In this case, zirconium wire W3 (core position 56) is compared to fuel rod No. 3 (core position 53) located in roughly symmetrical positions in the core. The effect shown in Fig. 34, namely, that the

curve for the fuel rod lies above the curve for the zirconium wire in the lower portion of the core and below the zirconium curve in the upper portion of the core, was evident in all six comparisons. The reason for this difference is believed due to the difference in cross section versus energy for uranium and zirconium. Since the upper portion of the core contains more voids, the neutron spectrum in this area is somewhat hardened, relative to the spectrum in the lower section, and the ratio of the zirconium activation to that of the U^{235} is expected to increase as the spectrum hardens.

V. IN-REACTOR-VESSEL INSTRUMENTATION MEASUREMENTS

During operation of the B-2 core, several groups of instruments were utilized to obtain data. One instrumented boiling fuel assembly (I-2), along with other removable in-core instruments and fixed in-vessel instruments, provided data. The number and type of instruments installed were as follows:

<u>No. of Instruments</u>	<u>Type of Instrument</u>
1	Instrumented boiling fuel assembly I-2, containing fuel and water thermocouples and entrance and exit flowmeters
1	Fixed, miniature ionization chamber
6	Pressurized flux-wire thimbles
5	Pressurized, miniature ionization chamber thimbles
1	Reactor pressure transducer
1	Downcomer drag-disk flowmeter
1	Downcomer differential-temperature thermopile
3	Downcomer density- and velocity-sensing probes
3	Reactor-vessel-steel specimen-capsule-temperature thermocouples
3	Reactor-water-temperature thermocouples
2	Reactor-steam-temperature thermocouples

Measurements obtained from the above instruments are described in detail below.

A. Temperature Measurements

1. Fuel Temperatures

Temperatures in the UO_2 boiler fuel were measured with W-W/26% Re thermocouples insulated with high-purity BeO and sheathed in tantalum. The thermocouples were located in five fuel rods of instrumented boiling fuel assembly I-2 in core position 46, as shown in Fig. 7. Measurements made attempted to correlate steady-state fuel temperature and fuel burnup, and to help determine information leading to a measure of the apparent thermal time constant of the fuel. The data obtained are tabulated in Table III, and a portion of the data is plotted in Fig. 35. The rising temperatures at constant reactor power appear to indicate an effect of small amounts of fuel irradiation. As one attempt at confirming this deduction, the reactor was scrambled from power, and fuel temperatures were recorded with time.

Theory would indicate an increased fuel-rod thermal time constant in the event of fuel fracture or separation from the clad. Data from two representative thermocouples are shown in Table IV.

Table III
BOILING FUEL ROD CENTER TEMPERATURES, °F

Date	Cumulative MWh**	Thermocouple No.					Power (MWt)
		2	3	4	5	6	
12/20/62	18.3	810	890	790	790	780	5
1/8/63	31.5	-*	962	840	840	840	5
1/8/63	31.5	-*	1195	1100	1080	1110	10
1/17/63	86.6	-*	1258	1150	1170	1170	10
1/18/63	117.3	-*	1256	1140	1190	1170	10
1/22/63	267.1	1480	1615	1430	1490	1490	20
1/23/63	418.9	1185	1540	1160	1320	1240	10
1/29/63	637.0	-*	1420	940	1280	1260	5
1/29/63	637.0	-*	1810	1570	1710	1700	19
2/4/63	874.3	-*	1270	1500	-*	1640	20
2/6/63	1237.7	1150	-*	1360	1560	1560	10
2/6/63	1237.7	1310	-*	1550	1635	1670	15
2/7/63	1466.2	820	-*	970	1250	1260	5

*Thermocouple in use for other tests.

**Cumulative energy totals are at day's end.

ID-103-A3217

Fig. 35. Boiling Fuel Temperature versus Cumulative Reactor Energy at Fixed Reactor Power Levels, Boiling Core B-2

Table IV
BOILING FUEL ROD TIME CONSTANTS

Thermocouple No.	63.2% Time Constant, 2/1/63, 763 MWht		63.2% Time Constant, 2/8/63, 1503 MWht
	5-MWt Scram	10-MWt Scram	10-MWt Scram
3	24 sec	15 sec	23 sec
6	21 sec	10.5 sec	17 sec

The data indicated an increase in time response between the two 10-MWt scram cases, but this is not conclusive, since past reactor history can influence the rate at which fuel temperature decreases (decay of fission products) after scram. Another unknown factor was the stability of the W-W/26% Re thermocouple in a high neutron and gamma field. Errors on the order of $\pm 1\%$ of temperature were expected because of thermal aging effects on the thermocouples, but no published data are known regarding their radiation stability. (A calculated major fuel time constant of about 4 sec was used in reactor transfer function models. See Table XII.)

2. Water and Steam Temperatures

Most of the installed thermocouples provided data during test periods. One water-temperature thermocouple in instrumented boiling fuel assembly I-2 was intermittent in operation, while all others operated continuously.

Operating personnel used reactor-vessel-water and -steam temperatures routinely as a measure of reactor operating conditions. The reactor-vessel-material specimen thermocouples showed no measurable (less than 0.5°F) increases in internal temperatures for reactor operation up to the maximum of 20.5 MWt.

The downcomer differential temperature thermopile produced questionable data during its operation, showing downcomer differential temperatures as great as 70°F . Temperature differential was not found to be directly related to power. After reactor shutdown and B-2 core removal, an atmospheric-pressure calibration was run which indicated no serious calibration error in the thermopile. (The thermopile had been originally calibrated by using baths of organic compounds to give temperature differentials up to about 47°F .) Further analysis of temperature records of the thermocouples on the outer wall of the reactor vessel showed good correlation between the thermopile readings and simultaneous maximum vessel-temperature differentials (i.e., upper reactor-vessel-head temperature, minus a minimum lower vessel-wall-region temperature). Agreement, which could be checked in most cases, was within 1 to 2° . Two thermocouples, located approximately

1 ft above the hot and cold junctions of the thermopile, indicated a maximum ΔT of 2° at a reactor power of 20 MWt, compared to the thermopile indication of 70°F differential. It is now evident that the temperature distribution in the downcomer of BORAX-V cannot be simply described, because of inadequate mixing of water from the feedwater sparger, and that only at the core entrance could any degree of mixing be assumed.

Valid subcooling measurements, for the above reasons, could not be obtained from downcomer instruments but were based on water-temperature readings from the instrumented assembly. Data obtained from the instrumented assembly thermocouples indicate the averaged values of subcooling as a function of power, as shown in Table V.

Table V

CORE-INLET SUBCOOLING TEMPERATURES

Reactor Power (MWt)	Subcooling Temp ($^\circ\text{F}$)
1*	3.0
3*	4.0
5	4.0
10	5.9
15	6.0
20	8.0

*Single data points.

Other water temperatures taken along the axis of a coolant channel in the instrumented assembly showed a temperature peak 6 in. above the base of the fuel, but otherwise indicated a constant rate of temperature rise throughout the length of the water channel. The peak may be attributed to the close spacing of that thermocouple to an adjacent fuel rod. Difficulty was experienced in the alignment of these couples.

B. Flow Measurements

1. Electrical Devices

No data were obtained on fluid flow in the reactor vessel, because of electrical failure of both turbine meters on the instrumented assembly and the downcomer drag-disk flowmeter. All failures were due to circuit-continuity failures and not magnetic circuit difficulties. During reactor operation (B-1 core) of instrumented boiling fuel assembly I-2, insulation failure due to leaking weld joints on the cans for the pickup coils

caused coil replacement with modified new units. About two months at zero-power, 600-psig, 489°F conditions (B-2 core) resulted in shorting out and grounding of these new units. Failure of the downcomer flowmeter occurred at about the same time, although it later regained a good portion of its original insulation and coil-resistance characteristics.

In reviewing the characteristics of the two flowmeter designs, the merits and weaknesses should be pointed out. The turbine meters utilized a system of permanent magnets and pickup coils to provide a pulse-rate signal proportional to turbine speed and water velocity. The drag-disk meter utilized a variable-reluctance, ac, bridge circuit whose unbalance was proportional to drag-disk deflection (0.005 to 0.010 in. maximum).

A weakness of the pulse-rate system was that the magnets experienced radiation damage, and the resultant lowered magnetic flux eventually led to an output-voltage pulse height from the coil that was masked in background noise and was therefore not usable. In practice, this point is not usually reached until several months of operation have been accomplished. Conversion of the sensing pickup to a variable-reluctance type device has been reported by General Electric Company⁽³⁾ as a means of removing the problem of magnet deterioration.

The variable-reluctance bridge used in the drag-disk meter has no obvious shortcomings, and the failure experienced was undoubtedly due to some combined radiation and temperature effects on the sensing coils and electrical insulation contained in the meter body.

The basic operation of the drag-disk meter is illustrated in Fig. 36. The meter was built around a standard, high-temperature, "E core" assembly procured from Wiancko Engineering Company, and used the "E core" as two legs of an ac bridge circuit operated at 3 kc. By using matched coils on the "E core" and feedback control of the 3-kc supply voltage, the system was made quite independent of reactor temperature and 60-cycle line-voltage fluctuations. Operation of the device relied on changing the coupling between the two halves of the "E core." This was done by coupling the drag disk to a torsion-rod-mounted, low-reluctance armature. The armature, mounted outside the pressure housing of the coils, rotated through a total angle of about 5° to cover the full range of flow.

2. Hydraulic Devices

In addition to the previous flowmeters, a cylindrical impact-measuring device was installed in the downcomer. This unit failed to produce useful information because of the apparent entrapment of gases in the external piping.

ID-103-A3118

Fig. 36. Downcomer Drag-disk Flowmeter, Simplified Schematic and Block Diagrams

C. Other Downcomer Instruments

The three pairs of density taps intended for use along with the flow probe also failed to operate consistently, apparently because of the gas traps in the lines. A completely new set of larger-diameter probes was later installed in the vessel, and the external instruments were remounted above the reactor building floor to permit adequate venting of the systems.

D. Neutron-flux Measurements

One miniature, neutron-sensitive, ionization chamber was installed in the core throughout the B-2 core operation. This chamber operated successfully the entire time and was used to provide signals for transfer-function studies and stability tests. Chamber output current was linear when plotted against chamber current of the operating linear instrument. Neutron sensitivity appears to be at least twice the published value of 5×10^{-18} Amp/nv, but has not been confirmed.

A special unit containing two, miniature, neutron-sensitive chambers (one cadmium-shielded) was used, along with zirconium wires, to get information on the flux profile and void distribution in the reactor. Performance of the unit was difficult to evaluate. The unshielded chamber gave an easily interpreted output as a function of axial position in the core, but the output of the shielded chamber did not follow any reproducible output. It was later determined that the chambers were incorrectly wired.

E. Miscellaneous Measurements

An unbonded strain-gage type of pressure transducer was mounted in the vessel, under water, for use during transient tests on the reactor. Its first use came during main steam-valve closing tests on the reactor, when it was used to back up the "operations" pressure transmitter for higher speed of response. The unit also operated to show a $\frac{1}{2}$ -psi pressure sine wave during oscillator-rod operation with the steam back-pressure control valve in service. No further use was made of this transducer, although it continued to function. The major problem with the device was excessive 60-cycle pickup, which was due mainly to the low span attempted for most measurements (typically 40 psi or less).

VI. WATER CHEMISTRY MEASUREMENTS

Tests were performed on the boiling cores to outline the general performance of the BORAX-V system and to determine its characteristics, as a basis for later operations with various superheater designs. The purpose of the various tests on core B-2 under the heading of water chemistry was to determine whether the reactor system was clean enough to operate successfully later with a superheater fuel having Type 304 stainless-steel cladding. In examining the chemistry of the system, the following parameters were measured:

- (a) pH
- (b) resistivity
- (c) suspended solids
- (d) corrosion and deposition
- (e) chloride concentration
- (f) steam quality
- (g) radiolytic gas production.

A. Water Quality

During most of the operating time, without boric acid or sodium nitrate in the system, the resistivity of the reactor water was 1 megohm-cm or better and the pH was close to 7. No trouble was experienced in maintaining these conditions, because the makeup water quality was always about 2 megohm-cm and the condensate return was demineralized to a quality of 10 megohm-cm. Feedwater quality was almost always better than 2 megohm-cm. Whenever the reactor water quality approached 1 megohm-cm, a cleanup loop was operated at rates up to 27 gpm to restore the quality quickly. During conditions of high water quality (around 2.5 megohm-cm), the pH cells read around either 6 or 8 and would drift slowly up or down. Checks at these times with a high-impedance laboratory instrument always gave a pH of 7; therefore, a pH of 7 was assumed during high-purity conditions.

B. Suspended Solids

The filtration system used to determine suspended insoluble solids is shown schematically in Fig. 37. A cooled sample stream at about 100-300 cc/min was passed through a millipore filter where all particulate matter down to 0.65 micron 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 get 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.

ID-103-A3049

Fig. 37. Suspended Solids Filtration System,
BORAX-V Sample Station

As can be inferred from the high-quality water in the system, the concentration of suspended solids was quite low during all periods. Table VI shows the results obtained. The average analyses of the ignited material show the relative amounts of the main metals found and are expressed as percent of the metal, not metal oxide; consequently, they do not add to 100%.

Table VI

SUSPENDED INSOLUBLE SOLIDS, CORES B-1 AND B-2

Sample Period	Concentrated Ignited Solids, ppm	Average Analysis, w/o	Remarks
(1-31-62) - (2-5-62)	0.12	Fe: 37.0 Cr: 1.3 Ni: 1.2 Al: 17.6	System cold. Sampled from vessel bottom. Core B-1
(2-5-62) - (2-9-62)	0.03		
(2-13-62) - (2-19-62)	0.04		
(2-19-62) - (2-22-62)	0.08		
(3-19-62) - (3-20-62)	0.03		
(3-20-62) - (3-22-62)	0.04		
	0.05 av.		
(12-13-62) - (12-14-62)	0.12	None taken	System hot and at zero power. Sampled from vessel bottom. Core B-2
(12-18-62) - (12-19-62)	0.27		
(12-19-62) - (12-20-62)	0.27		
(12-20-62) - (12-21-62)	0.28		
	0.23 av.		
(1-4-63) - (1-11-63)	0.40	Fe: 35.8 Cr: 2.3 Ni: 1.2 Al: 25.8	System hot. Various powers from 0 to 20 MWt. Sampled from vessel bottom. Core B-2
(1-11-63) - (1-15-63)	0.1		
(1-15-63) - (1-16-63)	0.6		
(1-18-63) - (1-21-63)	0.16		
(1-23-63) - (1-24-63)	0.03		
(1-28-63) - (1-30-63)	0.23		
	0.25 av.		
(1-30-63) - (2-1-63)	0.02	Fe: 20.4 Cr: 1.2 Ni: 3.7 Al: 41.0	System hot. Various powers from 0 to 20 MWt. Sampled from middle of vessel. Core B-2
(2-6-63) - (2-8-63)	0.08		
(2-8-63) - (2-13-63)	0.08		
	0.06 av.		

During cold operation, as expected, the average concentration of solids was very low. During hot operation, the concentration went up by about a factor of 5. When the sampling point was changed from the bottom of the reactor vessel to the middle of the vessel, the solids concentration dropped

to a value near that with the system cold and became relatively higher in aluminum. This probably happened because the middle vessel sample point is next to the X-8001 Al core shroud and the major part of each sample was taken with the water relatively static, rather than circulating due to steam flow; i.e., steam flow occurred only during a minor part of each sampling period.

The analysis of one unignited sample of filtered material analyzed by emission spectrograph and X-ray spectrograph to identify the elements and compounds is reported in Table VII. Alpha Fe_2O_3 , Fe_3O_4 , and FeC were reported, but most of the material was amorphous and identification was impossible.

Table VII

EMISSION SPECTROGRAPHIC ANALYSIS OF
SUSPENDED SOLIDS, CORE B-2

Sampled 1/16/63 - 1/18/63		
<hr/>		
Major:	5 w/o or over	M
Minor:	0.1-5 w/o	m
Trace:	0.1 w/o	T
<hr/>		
Al: M	Cu: m	Co: T
Fe: M	Mn: m	Mg: T
Cr: m	Pb: m	Mo: T
Ni: M - m		Si: T
		Sn: T
		Ti: T
		Zn: T

C. Corrosion and Deposition

Some of the radioisotopes found in smears from the control-rod wiper housings below the reactor vessel are as follows:

Cr^{51} from Cr^{50} , n, γ , Cr^{51} reaction;
 Fe^{59} from Fe^{58} , n, γ , Fe^{59} reaction;
 Co^{58} from Ni^{58} , n, p, Co^{58} reaction;
 Mn^{54} from Fe^{54} , n, p, Mn^{54} reaction.

Early in the operation, before any sodium had been injected into the reactor water, Na^{24} was found in the mixed-bed, ion-exchange column on the fission-product monitor. This is thought to have been formed by an n, α reaction on aluminum. Also discovered was a low concentration of Cs^{138} , which occasionally caused the fission-product monitor to alarm. This alarm

point was set quite low, and it is most likely that the Cs^{138} came from surface contamination of the fuel. The Cs^{138} was not persistent and never caused a high, steady reading, characteristic of a cladding rupture.

A further back-up to the evidence indicating a clean system is the data obtained on corrosion and deposition specimens and observations on the general condition of the fuel rods and various core structures.

Coupons of Type 304L stainless steel and X-8001 and A-288 aluminum (each with total surface area of $1/5 \text{ dm}^2$) were arranged in two bundles and placed on the outside of the core structure in the downcomer area. The bundles were mounted so that the flow of water was parallel to the face of the coupon in the direction of the major dimension. One bundle of coupons exposed during room-temperature operation saw only cold, pure water and cold, dilute, boric-acid solutions (not over 25 gm/gal). A second bundle was exposed during all of the hot operation time, including the hot, H_3BO_3 tests. The bundle of coupons exposed during cold operation showed no change.

Examination of coupons exposed to hot operation at 600 psig, 489°F revealed that both the aluminum and stainless steel were coated with a thin, nonadherent film of hydrous Fe_2O_3 . This film was easily scrubbed off with a stiff, fiber-bristle brush. Beneath the nonadherent film of ferric oxide on the stainless steel was a thin, adherent, brownish-black temper film, a condition also observed on stainless-steel parts of the core structure. This film was so thin that none of the stainless-steel coupons had any significant weight change. Each stainless-steel coupon had gained perhaps 0.1 to 0.2 mg. The aluminum had gained considerably more weight, due to the formation of adherent aluminum oxide. The weight gain for both aluminum alloys was the same, and amounted to about 430 mg/dm^2 . No pitting corrosion was observed on the coupons nor on any reactor surfaces examined. The nominal composition of A-288 alloy is 1 w/o Ni, 0.5 w/o Fe, 0.1 w/o Ti, balance Al; and of X-8001 alloy is 0.89 w/o Ni, 0.48 w/o Fe, balance Al.

Two boiling fuel rods examined were not coated with the loose Fe_2O_3 film. These had only a thin, hard, adherent, bronze-colored film. No metallurgical examinations were made because of the short operating time of only 65 megawatt-days.

Deposition samples placed in the steam line and turbine casing were not examined until about 24 days after reactor shutdown. No long-lived isotopes were found, and the samples were not active enough to obtain a gamma spectrum. Deposition samples were brass, X-8001 aluminum, Type 304L stainless steel, and chrome-moly steel.

D. Chloride Concentration

Because of the anticipated problem with chloride stress corrosion of the Type 304, stainless-steel, superheater fuel, one of the most important

elements monitored was Cl^- . A Technicon autoanalyzer, which had been specifically designed to measure Cl^- , was obtained. After several weeks of careful testing, it was established that, if the instrument was kept at $70 \pm 1^\circ\text{F}$, it was stable and would show changes in Cl^- concentrations of 0.01 ppm. Repeated standardizations were performed to establish confidence in the response of the instrument in the 0 to 0.1 ppm Cl^- range before it was placed in operation.

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

The $\text{Fe}(\text{CNS})^{++}$ ion in Eq. (2) has a distinctive red color and cannot form unless Cl^- is present. A saturated solution of mercuric thiocyanate is mixed with a solution of ferric nitrate (67.5 gm/l) and with the filtered sample. Air is then brought into the solution for the purpose of mixing, and the color is allowed to develop during the mixing in a helical, hold-up coil. The solution then passes through a colorimetric cell where the color intensity of the $\text{Fe}(\text{CNS})^{++}$ ion is read out as chloride concentration. The instrument performance can be checked at any time by turning a valve to permit either one of two standards to be sampled. Water of 10-megohm-cm quality and 0.1-ppm NaCl solutions were used for standards.

During the entire operation of core B-2, chloride concentration rose above 0.1 ppm during only one period. This was during a 5-MWt power run on December 20, 1962. Condensate was returned to the reactor for the first time through piping that had not previously been used. At this time, the reactor water resistivity dropped to 470,000 ohms, the pH dropped to 6.5, and the chloride concentration rose to 0.3 ppm and remained at that level for about 24 hr, but there was no steam flow during this period. Shortly after this 24-hr period, the chloride concentration was reduced to less than 0.1 ppm, using the purification system, and there were no further incidents or trouble in keeping the concentration below 0.1 ppm. For the remainder of the operating period, the average chloride concentration was between 0.02 to 0.05 ppm, the lower value being more prevalent during the higher steaming rates. The condensate return upstream of the full-flow condensate demineralizer had a similar, very low, chloride reading.

E. Steam Quality

To test the ability of the system to produce dry steam, two tests were performed at 10 and 15 MWt, using the sodium tracer technique, to measure

moisture carryover in steam. To sample the steam dome, a movable sampling probe was constructed and attached to the 6-in. nozzle on the west side of the vessel head. Figure 38 shows schematically the relative positions of the probe and the saturated steam collector opening in the reactor vessel. Figure 38 also shows the bottom sample injection ring and sampling point.

ID-103-A3050

Fig. 38. Steam Dome Probe Schematic Diagram, Boiling Core B-2

Forty grams of NaNO_3 in solution in a 78-ml pressure vessel were used for the 10-MWt test; 20 grams of sodium nitrate were used for the 15-MWt test. NaNO_3 was chosen because its solutions have a pH of 7 and because the NO_3^- ion causes no activation interferences. After the purification demineralizer was by-passed, this solution was injected into the reactor vessel, using feedwater pressure on the sample injection line.

The sodium nitrate solution entered the reactor through a sparger ring under the core and quickly mixed throughout the water in the system, as indicated by conductivity measurements. During these tests, which lasted about 8 hr each, the pH was 7 to 7.4, but the conductivity dropped to between 250,000 and 260,000 ohms. After about 1 hr of radiation time, two simultaneous samples were taken, one from the reactor bottom for reference, and one from the steam dome for the indicator. This was done for vertical 5-in. increments in the probe position. The samples were later counted, using a well-type counter and single-channel analyzer set to count only the 2.75-MeV gamma energy from the Na^{24} .

During these runs, the fission-product and air-ejector-exhaust-gas monitors alarmed often. The effect on the fission-product monitor was quite unexpected.

The parameters of interest in this test were the distance above the boiling surface required for de-entrainment, and how complete the de-entrainment was before the steam entered the steam collector opening. Figure 39 shows graphically the change of water entrainment in the steam at various positions. As can be seen, complete de-entrainment occurs at these power levels within 5 in. from the boiling surface. It is also obvious that the level of the steam-collector opening is well above the de-entrainment level. This was borne out by steam calorimeter tests on the main steam line, which indicated that steam quality was better than 100%.

F. Radiolytic Gas Production

ID-103-A3051

Fig. 39. Steam Quality Profile,
Boiling Core B-2

Some radiolytic gas concentrations were determined. Because of the failure of the continuous dissolved-oxygen analyzer, continuous data were unobtainable and it was necessary to resort to a series of grab samples for all points of interest. This gave some scatter to the data. Figure 40 shows what was found in BORAX-V core B-2 and compares the radiolytic gas production found at the same power levels at BORAX-IV and EBWR. The component gases were determined by an absorption method. A mass spectrographic analysis was performed on the constituents of the radiolytic gas. Table VIII shows the results obtained.

ID-103-A3052

Fig. 40. Comparison of Radiolytic-gas Production Rates

Table VIII

CONSTITUENTS OF
RADIOLYTIC GAS

Gas	Percent by Volume
H ₂	52.8
O ₂	33.4
N ₂	~12.4
CO	~1.0
A	0.27
CO ₂	0.20

VII. TRANSFER-FUNCTION MEASUREMENTS AND STABILITY ANALYSIS

A. Rod Oscillator Experiments

Transfer-function measurements were made during the power operation of core B-2 to estimate the limiting power of stable operation of the core and to check out the equipment for use in later experiments. The overall feedback and open-loop functions were obtained by determining, through a sine-cosine, cross-correlation, analog computer, the response of the reactor power to a sinusoidally varying reactivity input provided by a rotating poison oscillator rod within the core.

1. Description of Equipment

a. Oscillator and Drive. The oscillator used in these experiments consisted of a hollow, water-filled cylinder, $2\frac{11}{16}$ -in. OD, which rotated within a $2\frac{13}{16}$ -in.-ID, stationary, aluminum housing. Graphite bearings were located at the top and bottom of the stationary housing. A 165° arc of Boral, 28 in. long, made up part of the rotating oscillator, the remainder of the circle being aluminum-slotted to balance the assembly. The Boral and aluminum were clad inside and out with Zircaloy-2 for corrosion resistance. The design of the oscillator was thus similar to that used by Wasserman.⁽⁴⁾ Three positions were available for locating the oscillator, namely, thimble C in the reflector and core positions 85 and 75 (see Fig. 7). Investigation during the cold critical experiments indicated that placing the oscillator in core position 75 (in the space left by removing the fuel assembly) resulted in the greatest reactivity worth (3.5% half-peak amplitude); however, this worth was felt to be insufficient for the transfer-function measurements at power. Consequently, various measures were taken to increase the differential worth of the oscillator. The result is shown in Fig. 41, a plan view of the oscillator and its housing, shown in a modified boiling fuel assembly. All fuel rods except the three in each corner were removed, and a 165° arc of boron-stainless steel was attached to the fuel assembly box on the side farthest from the center of the core. This configuration increased the worth of the oscillator to about 7% amplitude in the cold condition. If the oscillator worth became too great at higher temperatures and powers, it could be reduced, if desired, by raising the stationary housing and oscillator section with its $1\frac{1}{2}$ -in.-diam. shaft extending through a seal in the top of the vessel. This seal, originally a carbon-ring mechanical seal, was replaced after its failure by a water-cooled, packing gland-type seal with a special graphite bearing.

ID-103-3041

Fig. 41. Section through Oscillator Rod,
Boiling Core B-2

The oscillator drive, shown schematically in Fig. 42, 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; for a given setting, speed variations as measured by the electronic counting equipment were insignificant.

ID-103-3042

Fig. 42. Oscillator Drive System

It had originally been planned to carry out transfer-function measurements using both null-balance and cross-correlation equipment, but Lipinski's results at EBWR⁽⁵⁾ and preliminary zero-power measurements with null-balance equipment on BORAX-V indicated that the analog computer gave at least as good results in a much shorter time. In addition, the null-balance system accuracy is at its worst in the presence of boiling noise at power.

For the cross-correlation method of determining the transfer function, a sine-cosine, carbon-ring potentiometer was connected to the output drive shaft. The same drive was also designed for the null-balance method, and a mechanical differential was geared off the output drive shaft to drive a second, identical, sine-cosine potentiometer. A hand crank with a rotational counter was connected to the differential so that the null phase angle could be measured.

A photocell picking up a beam of light showing through a 0.013-in.-diam. hole in a gear mounted on the output shaft was used, in conjunction with an electrical timing circuit, to measure the rotational speed of the oscillator.

b. Transfer-function Measuring Equipment. The transfer-function measurements were made on a sine-cosine, cross-correlation, analog computer system. A block diagram of the equipment is shown in Fig. 43. A signal (of the form $E_0 + A \sin \omega t$) from an ion chamber located outside the reactor vessel was brought into a Donner Model 3500 analog computer. A bucking voltage (E_0) was applied to this signal to cancel out the dc component, and the resulting ac signal ($A \sin \omega t$) was amplified and taken to a pair of function multipliers (Donner Model 3731). A signal from the sine tap of the sine-cosine potentiometer was brought to the first function multiplier, and from the cosine tap 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 were

$$E_{01} = K_1 \Delta n \cos \phi, \text{ and } E_{02} = K_2 \Delta n \sin \phi.$$

The phase angle was given by $\phi = \tan^{-1} [(E_{02}K_1)/(E_{01}K_2)]$, and the fractional amplitude of the power oscillations was given by

$$\frac{\Delta n}{n_0} = \frac{K_3}{TE_0} \sqrt{\left(\frac{E_{01}}{K_1}\right)^2 + \left(\frac{E_{02}}{K_2}\right)^2},$$

where the K's were determined by the amplitudes of the sinusoidal signals and the values of the components used in the analog circuitry, and T was the time of integration.

Fig. 43

Block Diagram of Equipment Used to Measure Transfer Function

ID-103-3218

The integral number of cycles was achieved through the revolution indicator attached to the oscillator drive shaft and the preset counter (Beckman Model 7424A). With the oscillator running and the analog computer turned on, the preset counter was set to close the two relays (corresponding to the two integrators) that started the integration at a given cycle; after a preset number of cycles, the relays were opened, thus stopping the integration. The integration was started and stopped at a corresponding point on the first and last cycles because the pulse that actuated the preset counter was generated by the photocell in the revolution sensor. Integration was carried out for at least 100 cycles at each frequency, unless this would result in an integration time in excess of 5 minutes, in which case the inaccuracies due to drift in the integrating circuit necessitated fewer cycles. Because the preset counter occasionally skipped counts due to extraneous noise pickup, the total integration time for each run was measured directly with a CMC Model 225B Universal counter-timer. This timer was also used to determine the oscillator frequency when the oscillator drive was adjusted.

In addition to the components described above, which were part of the computational equipment, an AMPEX CP-100 tape recorder was used to record the incoming, out-of-core, ion chamber signal and the sine signal from the sine-cosine potentiometer. During most of the power transfer-function measurements, signals were also recorded from an in-core ion chamber and a special high-temperature thermocouple located at the center of one of the fuel rods in the instrumented boiling fuel assembly in core position 46, as shown in Fig. 7.

2. Experimental Procedure

The reactor was maintained as near as possible to 489°F and 600 psig during all the transfer-function measurements. Reactor system variables were controlled automatically by the following two major control systems: (1) the reactor pressure-control system, which took the steam-dome pressure signal to a proportional plus reset controller and actuated the main steam back-pressure control valve; and (2) the feedwater reactor-water-level control system, which took the level error plus steam flow minus feedwater flow signals in typical, three-element, control-system fashion. This latter combined signal actuated the main feedwater control valve.

Reactor power was controlled manually by control rods and dissipated to the turbogenerator system, and no boric acid was present in the reactor water for any of the transfer-function measurements.

a. Oscillator Fully Inserted. The first series of measurements was made with the bottom of the poison section of the oscillator 1/2-in. above the bottom of the core, i.e., effectively fully inserted. Runs were then made at zero power (hot), 5, 10, and 16.5 MWt. Results of the 10- and 16.5-MWt runs were satisfactory, but the data for the 5-MWt run were useless because of a malfunction in the function multiplier. The 16.5-MWt run (maximum power level run studied) revealed that the reactivity worth of the oscillator had increased over the zero-power worth by a factor of about 1.13 to approximately 16¢ amplitude, based on the gain normalization to the zero-power gain at the highest frequencies. The resultant power-oscillation amplitude at the resonance peak for the 16.5-MWt run was about 29%. This large power oscillation invalidated the assumption of linearity on which transfer-function analysis was based. It was therefore decided to withdraw the oscillator partially and to repeat the experiment at various power levels with smaller reactivity amplitudes and compare results.

It was reasoned that the oscillator increased its differential worth with power because (1) the control-rod bank gradually unshadowed the oscillator as it was raised with power, and (2) the increase in void volume with power increased the neutron diffusion length, thus increasing the effectiveness of poisonous material in the core.

To move the oscillator axially and decrease its worth, it was necessary to shut down the reactor, cool down to atmospheric pressure, make the oscillator adjustment, and then return the reactor to the 600-psig operating pressure. Since this was a time-consuming operation, the oscillator insertion was never adjusted during any given run, although it would have been theoretically ideal to make frequent adjustments of the oscillator insertion during a run as the power amplitude varied with frequency.

b. Oscillator Half Inserted. The second set of transfer-function measurements was made with the oscillator withdrawn to a position at which the bottom of the poison was 12 in. from the bottom of the fuel, i.e., approximately half inserted. Runs were then made at 5, 10, and 15 MWt and at zero power. The resulting deduced worth of the oscillator decreased from its fully inserted value to 8.6¢ at 15 MWt. The oscillator worth was found to increase monotonically with power (for both levels of oscillator insertion) as shown in Fig. 44.

ID-103-A3219

Fig. 44. Oscillator Reactivity Worth versus Power, Boiling Core B-2

In most of the runs with both oscillator insertions, good reproducibility of the data was obtained; however, whenever the first two runs at a given frequency were not consistent, additional runs were made until the data were in better agreement. Reproducibility was usually better than $\pm 2\%$ in gain and $\pm 2^\circ$ in phase. The computing equipment was found to need frequent balancing; i.e., the function multiplier was balanced before every run, and the computer and tape recorder input amplifiers were balanced at least once a day. The signal-to-noise ratio of the ion chamber signal was usually well in excess of 2-to-1, as determined by reading an oscilloscope, and was always greater than 1-to-1 at power.

3. Experimental Results, Comparison with Analytical Model Studies

The overall stability of BORAX-V with natural-convection flow was studied primarily by (1) measuring the reactor response in flux to a sinusoidal reactivity driving signal, (2) developing a theoretical model to fit the measured response at various power levels and comparing the measured results with the theoretical model, and (3) performing a limited noise analysis of the flux signal at various power levels.

In addition to the above, the kinetics nonlinearity effects of large, driving, reactivity insertions were studied both experimentally and theoretically, assuming a linear feedback model. The effect of approximately doubling the driving reactivity amplitude at various power levels was measured. The results were then compared with a theoretical evaluation of the nonlinearity effect by a describing-function method, using the linear feedback model derived earlier.

The purpose of the stability studies was essentially four-fold: (1) to determine overall reactor stability versus power and the maximum stable reactor power level, or at least to determine if the reactor would be stable at its 40-MWt maximum design limitation; (2) to determine if (or how much) the overall reactor transfer function is "distorted" by large driving reactivities; (3) to evaluate the percentage noise level at various power levels, and to obtain the noise-power spectrum, if possible; and (4) to deduce the values of various basic parameters and their variation with power; i.e., the power coefficient of reactivity, effective thermal time constants or thermal conductivity of the boiler fuel rods, and the ratio $\bar{\beta}/\ell^*$. As stated in the conclusions, the attempts to establish the above were generally successful.

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

$$G = \Delta n / (n_0 \Delta k) = G_0 / (1 + G_0 H),$$

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

Described below are the results of a simplified model study in the 0.20- to 20.0-cps frequency range, where the pressure and Doppler reactivity effects were small compared with void reactivity effects. This comparison was made earlier by performing more complex model studies. This simplified model, with the pressure and Doppler reactivity effects omitted, gave identical results to the original complex model in the 1.2- to 20.0-cps range at the power levels measured (0 to 16.5 MWt). Another reason for this close correspondence is that detailed expressions for important effects, particularly the heat transfer and void dynamics, have been retained. Thus, it was deemed appropriate for this report to present a reactor dynamics study using the simplified model from the standpoint of better understanding and greater ease of computation, and because of inadequate transfer-function data at the lower frequencies.

a. Zero-power Transfer Function (G_0). The zero-power transfer function for the B-2 core at 489°F and 600 psig was measured with the rod oscillator fully inserted at a power level of about 3 kWt. The gain and phase data resulted in smooth curves and, after normalizing at 15 cps (the maximum frequency studied), agreed within 1% in gain and 1° in phase with a computer calculation using an adjusted value of $\bar{\beta}/\ell^*$. Hereafter, the zero-power transfer function is assumed to mean the six-group transfer function calculated by adjusting $\bar{\beta}/\ell^*$ to fit the measured zero-power experimental phase data at 489°F. The resulting ℓ^* was 2.7×10^{-5} sec, assuming a calculated $\bar{\beta}$ of 0.0071. The remaining delayed-neutron parameters are presented in Table XI as taken from the fast-fission data of Keepin⁽⁶⁾ for U^{235} . The zero-power transfer function is shown for reference in the Bodé plots of Fig. 45.

b. Normalization of Measured Reactor Transfer Functions. The differential worth of the rod oscillator at power was determined indirectly by first measuring the flux amplitude at power ($\Delta n/n_0$) at the highest frequency studied, and then arbitrarily equating the gain (G) to the zero-power gain at this frequency. Thus, the deduced worth equals the observed amplitude, divided by the gain of the zero-power transfer function at the highest frequency. The phase, on the other hand, was evaluated at all power levels by equating the phase at the maximum frequency to the zero-power phase. Thus, a constant phase-correction term was added at each frequency. The above normalization procedure is valid if (1) the power feedback is negligible at the maximum frequency (15 cps), and (2) $\bar{\beta}/\ell^*$ does not change with power level. Model studies have verified that feedback is truly negligible (compared with $1/G_0$) at the maximum frequency; however, there is evidence that $\bar{\beta}/\ell^*$ does decrease with power. Variations in $\bar{\beta}/\ell^*$ do not significantly affect the deduced values of the overall reactor gain, but do affect the overall reactor phase at all frequencies at power. Hence, variations in $\bar{\beta}/\ell^*$ would also change the deduced feedback (H) and open-loop (G_0H) transfer functions, especially at the higher frequencies.

ID-103-A3226

Fig. 45. Overall Gain (G) and Phase versus Frequency for Various Power Levels and Oscillator Insertions (Out-of-core Ion Chamber), Boiling Core B-2

The phase normalization problem actually arose because the absolute phase could not be measured; i.e., the sine-cosine potentiometer was not in phase with oscillator position, and the rod oscillator was not rotated in both directions at high frequencies. Hence, only the relative phase angle could actually be determined and the ratio $\bar{\beta}/\ell^*$ versus power could not be obtained directly. The effect of changes in $\bar{\beta}/\ell^*$ versus power was studied by arbitrarily changing ℓ^* over a range and evaluating the experimental G, H, and G_0H transfer functions accordingly. The resulting effects on H and G_0H seem to indicate that the zero-power value of $\bar{\beta}/\ell^*$ should be decreased by a factor of about 1.4 at 15 and 16.5 MWt.

c. Overall Reactor Transfer Function (G). A comparison of the calculated overall response with the experimental results with the rod oscillator half inserted can be made between Figs. 44 and 45. It should be noted that:

- (1) The comparison can be made for 5, 10, and 15 MWt and is satisfactory above 1.0 cps.
- (2) The overall response always returns to the apparent zero-power transfer function at a frequency well under the maximum driving frequency of 15 cps, i.e., at about 10 cps.
- (3) The power coefficient required for the large gains and phase leads in the resonance region must be very large to counterbalance the stabilizing effect of the large, thermal, time constants. The actual, deduced, power coefficient (as it varies with power) is discussed below, and the time constants required to fit the measured data are listed in Tables XII and XIII.
- (4) The peak gain, phase lead, and resonance frequency all increase with power level.

The reactor-transfer-function results determined experimentally with an out-of-core ion chamber are shown in Fig. 45 for the following two oscillator insertions:

- (a) oscillator effectively fully inserted
(1/2 in. withdrawn).
- (b) oscillator half inserted (12 in. withdrawn).

The purpose of this comparison was to study the effect on stability of varying the effective oscillator worth at each power level. As Fig. 45 indicates, a direct comparison can be made only at 10 MWt where differences are noted, especially at the lower frequencies. The reasons for the actual differences found are uncertain, however, since the describing-function analysis (Fig. 53) shows the kinetics nonlinearity effect to be almost negligible from 0 to 15 MWt

with the magnitudes of reactivity amplitudes involved. Further details of the theoretical describing function results are given in Sec. VII, A, 3, f and g (pages 84 and 85, below). The derivation is outlined in Appendix B.

Figure 46 shows gain and phase as a function of frequency taken from a miniature, in-core, ion chamber installed in a stainless-steel thimble in core position 44, as shown in Fig. 7. The oscillator was half inserted at each power, except 16.5 MWt, where it was fully inserted. The curves show a trend similar to that obtained from the out-of-core chamber in Fig. 45. This indicates that spatial effects in the small BORAX-V reactor were negligible.

The data from the in-core, ion chamber were recorded on an AMPEX Model CP-100 tape recorder, digitized on a NAVCOR analog-to-digital converter, and analyzed digitally on an IBM 1620 computer using a Fourier analysis-noise rejection program. Four oscillator cycles were used in analyzing all but the very low frequencies where one or two cycles were used. The recorded temperature data from the boiling fuel thermocouple were not usable because the signal-to-noise ratio was too low.

The theoretical, overall, reactor transfer function, G , shown in Fig. 47, was calculated as a function of power level using the feedback model given above and fixed, zero-power, delayed neutron parameters. The functional dependence upon power of the parameters inherent in H was derived from the initial B-2 core design calculations, since steady-state measurements of these parameters during power operation could not be made due to instrumentation failures. Hence, the deduced variation of these parameters with power finally resulted from a trial-and-error fit of the measured reactor response with the theoretical model. This model fit was initiated by making small deviations in the initial set of design parameters (primarily in slip ratio and power coefficient of reactivity) corresponding to a power level of 15 MWt. This model-fitting procedure was then extended to the other power levels at which the reactor response was measured, assuming meanwhile that the kinetics nonlinearity effects were negligible. In spite of the lack of experimental steady-state information and the complicated model-fitting process that resulted, the final calculated results are encouraging.

The functional relationship of power coefficient and other steady-state functions with power derived in the 5- to 16.5-MWt range was extrapolated to higher powers, thus permitting theoretical calculations to 30 MWt. However, the extrapolations are uncertain, and little emphasis should be placed on the calculated results above 20 MWt. Thus, valid conclusions cannot easily be drawn concerning the unstable power limit of this core, since the all-important power coefficient is difficult to predict with confidence at higher powers. However, the power coefficient does decrease monotonically with power in the 5- to 16.5-MWt range.

ID-103-A3227

Fig. 46. Overall Gain and Phase versus Frequency for Various Power Levels (In-core Ion Chamber), Boiling Core B-2

ID-103-A3116

Fig. 47. Overall Gain (G) and Phase versus Calculated Frequency, Boiling Core B-2

d. Open-loop Transfer Function (G_0H). The experimental results of G_0H were first obtained knowing the overall response (G) and again assuming that G_0 remained constant with power. These results are shown in Fig. 48 in the form of Nyquist diagrams. It is seen that while a wide margin of stability is still present at 15 and 16.5 MWt, the phase margin trend is to decrease with increasing power. The corresponding gain margins are obviously difficult or impossible to determine because of the erratic data at the high frequencies.

ID-103-A3045

Fig. 48. Open-loop Response (G_0H) for Various Power Levels and Oscillator Insertions, Boiling Core B-2

As mentioned earlier, the cause of errors in G_0H at high frequencies is presumed to be due to the ratio $\bar{\beta}/l^*$ changing with power instead of remaining fixed as first assumed. Figure 49 shows the experimental open-loop results at 5, 10, and 15 MWt after a trial-and-error study was made varying $\bar{\beta}/l^*$ with power. The criteria of the best values of $\bar{\beta}/l^*$ were that G_0H (and H) in the form of Bode' and Nyquist plots were similar to the calculated curves at high frequencies. With the corrected $\bar{\beta}/l^*$ values, both the gain and phase margins of G_0H look more reasonable as a function of power level. It should be noted in Fig. 49, that the "adjusted" G_0H curves enter the second quadrant somewhat similar to the results of the model shown in Fig. 50 for all three power levels.

ID-103-A3220

Fig. 49. Nyquist Diagram of Open-loop Transfer Function, G_0H ($\bar{\beta}/l^*$ -adjusted), Boiling Core B-2

Figures 48, 49, and 50 also show that the equal-frequency lines linking the Nyquist plots at various power levels curve somewhat. Since the lines should theoretically be straight for a truly linear system, the curvature suggests that the feedback time constants change somewhat with power level. This effect is clearly demonstrated in Table XIII. The theoretical G_0H Nyquist diagrams in Fig. 50 were calculated using the zero-power (hot) value of $\bar{\beta}/l^*$ for all power levels.

ID-103-A3129

Fig. 50. Nyquist Diagram of Open-loop Transfer Function, G_0H (Calculated), Boiling Core B-2

e. Feedback Transfer Function (H). The measured feedback results and the theoretical (model-fitted) feedback are compared in Figs. 51 and 52, respectively. At high frequencies, the experimental curves become unreasonable for the same $\bar{\beta}/\ell$ * effect described in Sec. d above. As the theoretical model implies, the actual phase of the measured H may well become asymptotic to -180° at the high frequencies, indicating a net second-order feedback system, while at high frequencies the gain should decrease indefinitely at 12 db/octave.

The Bode' plots of H versus power, using the simplified model, show that the time constants change somewhat with power. Evidently the simplified model does not fit the measured results very well below about 0.5 cps, depending on the power level, since pressure effects are omitted and are particularly important at these low frequencies.

However, measurements at the low frequencies are influenced more strongly by the deleterious effects of (a) inherent inaccuracies of the measuring equipment, (b) drifts in power level, and (c) relatively small signal-to-noise ratios

ID-103-A3221

Fig. 51. Gain and Phase of Feedback (H) for Various Power Levels and Oscillator Insertions, Boiling Core B-2

ID-103-A3123

Fig. 52. Gain and Phase of Feedback (H) versus Frequency (Calculated), Boiling Core B-2

A comparison of the reactor response between manual and automatic pressure control systems could not be made, since it was virtually impossible to hold both pressure and average power constant under manual control while oscillating the reactor.

f. Large, Driving, Reactivity Effects, Comparison with Describing-function Calculations. The feedback model used in the describing-function studies was that derived in e above for the linear-stability studies. The range of reactivity amplitudes used in the calculations, 0 to 22.5¢, was suggested by the experimental values shown in Fig. 44.

Two aspects of the kinetics nonlinearity effect are presented: (1) the effect of increasing the Δk amplitude at a fixed power level (or feedback gain); and (2) the effect of increasing the power level at a fixed Δk amplitude. The results of these effects are shown in Fig. 53.

ID-103-A3122

Fig. 53. Kinetics Nonlinearity Effect on Overall Gain Using Describing-function Analysis with Linear Feedback, Boiling Core B-2

For the feedback transfer function involved here, the results show that:

(1) Transfer-function "distortion" occurs only near the resonance frequency (over about a one-decade range) of the overall reactor frequency response.

(2) The general tendency for both increasing reactor power and increasing reactivity amplitude is two-fold:

(a) The resonance peak (gain) decreases slightly at a fixed power with Δk increasing to 22.5¢, while the overall phase decreases negligibly.

(b) Increasing power at a fixed Δk causes the resonance gain to become increasingly distorted, the gain always being less than the transfer function ($\Delta k = 0$) values.

(3) The optimum driving reactivity amplitude, from the standpoint of suitable signal-to-noise ratio and negligible "distortion" of the transfer function, decreases as expected with increasing power level. This relationship is shown in Fig. 44 in terms of an overall response error criterion.

The results in Fig. 53 suggest that the maximum Δk amplitude obtained in the rod oscillator tests apparently caused little distortion even at 16.5 MWt, i.e., less than 2% in gain and 1° in phase. However, this value of reactivity amplitude could have caused errors in excess of 3% in gain and 1.5° in phase on the overall reactor response above 20 MWt, assuming the feedback model extrapolation is valid at the higher power levels.

g. Reactor Stability Results

(1) Results from Linear Reactor Model

The results in general show that the stabilizing effect of the large, fuel, thermal time constants is largely offset by the large power coefficient of reactivity on the all-boiling B-2 core. In addition to the Nyquist plots made for the $\bar{\beta}/\ell^*$ -corrected measured results, shown in Fig. 49, plots of gain and phase margins versus power of the renormalized G_0H transfer functions are shown superimposed in Fig. 54, along with the calculated margins when the theoretical model is used. Simple straight-line extrapolations of the gain and phase margins suggest a power-level instability in the range of 30-40 MWt, but as stated earlier, this stability limit is difficult to extrapolate with confidence. Relatively large gain and phase margins are still indicated at 16.5 MWt, and the maximum steady power level of 20.5 MWt achieved on this core (without forced oscillations) indicated no instability.

Reactor stability could not be measured above 16.5 MWt. However, without these data it can be speculated that one of the following effects should be true: (a) the power coefficient decreases gradually to zero as extrapolated (and then becomes positive), because of increasing carryover with power; or (b) the power coefficient remains essentially constant above some power level, as should theoretically be the case with no carryover.

ID-103-A3114

Fig. 54. Gain and Phase Margins of Open-loop Feed-back (G_0H) versus Power, Boiling Core B-2

The first effect would cause a simple reactor instability at the power level where the power coefficient becomes positive, and would be evidenced by undefined (large) gains at very low frequencies. With a positive power coefficient, reactor power would increase with the addition of negative reactivity.

If the second effect were true, the reactor would become unstable at some high power level, probably in excess of 40 MWt.

Instability would thus result from a gradual increase in the open-loop gain (G_0H) until its Nyquist plot encircled the $(-1, 0)$ point. However, with the absence of additional data, neither of the above conclusions can be drawn.

(2) Results from Reactor Describing-function Analysis

Although the theoretical effect of large reactivity amplitudes on the overall reactor frequency response is readily obtainable, thus permitting a qualitative stability analysis, only an approximate quantitative evaluation of stability "in the large" can be obtained when the describing-function technique is used. That is, the effects of large driving reactivities on reactor stability can be determined only on a relative basis, or at best to the extent of a quantitative approximation. Gyftopoulos⁽⁷⁾ summarized the problem as follows: The linear model (transfer function) is a good approximation for a certain range of small signal amplitudes. However, for very large perturbations, the equations describing the system performance for most practical cases are insolvable. For some intermediate range, an approximate nonlinear model such as the describing function will give more accurate results than the transfer function if enough terms are included in its series expansion (see Appendix B). Thus, the describing-function technique serves as an adequate check on the range of signal amplitudes where nonlinearities are important, and serves to evaluate stability in a range where the perturbations are large but not excessive.

An approximate stability analysis was thus made by evaluating the overall reactor frequency response, or describing function,⁽⁴⁾ by using reactivity amplitudes in the range of 0 to 22.5¢. Figure 53 shows that even the largest oscillator-reactivity values actually imposed do not significantly affect either the overall frequency response or the reactor stability. The most severe test measured was the 16.5-MWt run when the driving Δk amplitude became 15.9¢. This value of reactivity amplitude theoretically caused only a 2% decrease in the height of the gain resonance peak over the transfer-function resonance at this power level. The effect on phase caused a maximum decrease of about 0.7°, again at about 2.0 cps. The describing-function analysis indicates, however, that a 4.5% gain decrease and a 1.5° phase decrease would have resulted from a 22.5¢ oscillator amplitude at this power level. It is important to note that the qualitative effect of large Δk values is stabilizing for all power levels studied; i.e., the resonance peak gain decreases steadily with Δk at a given power level for the B-2 core. It must not be assumed, however, that large Δk values are stabilizing for all reactors, since the effect depends strongly upon the specific feedback processes involved.

A preliminary study indicated that the kinetics non-linearity effect is strongly a function of the power coefficient, or more correctly, the product of power and power coefficient. The effect was

somewhat stabilizing if the product is below a certain value, and very destabilizing if the product exceeds this value. The critical value of this product was found to be about $10\% \Delta k/k$ for this core. This would correspond to a power coefficient of $0.61\% \Delta k/k/\text{MW}$ at 16.5 MWt; however, the actual power coefficient at this power level was about $0.41\% \Delta k/k/\text{MW}$.

B. Noise Analysis Experiments

Only a limited amount of noise analysis has been performed on BORAX-V boiling core B-2. However, the preliminary results are useful since they suggest areas of improvement in the future, both in the manner of recording the signals and in optimizing the analysis of the data. What follows below are the results of measuring noise at power and applying the results to the determination of reactor stability using the root-mean-square (rms)⁽⁸⁾ method as developed by Akcasu. The power spectra and autocorrelation function measurements made of the flux signal at various power levels were also measured in an attempt to verify the results of rod oscillator experiments, but the results are too incomplete to present at this time.

Valid application of the rms method of noise analysis for predicting power instability (as Akcasu had originally intended) is theoretically doubtful for BORAX-V, and probably for most boiling-water reactors, because (1) the "driving boiling noise" has never been found to be white, certainly not to $\bar{\beta}/\ell^*$ rad/sec under power conditions; and (2) the overall reactor transfer function, G , cannot be well-approximated by a quadratic transfer function.

The first condition above has been found to be true from noise analyses on several boiling-water reactors to date. Actually, the deduced boiling-noise power spectrum usually begins falling off drastically between 0.1 and 1.0 rad/sec at power, and is very small where the resonance frequency is located. The second condition is especially true for BORAX-V as found from the model-fitting studies described above. Primarily because of the low thermal conductivity of the UO_2 fuel, a quadratic overall transfer-function description of BORAX-V is inadequate; at least a fourth-order system is required to give a reasonable approximation to the measured feedback function.

High-speed recorder traces of the flux signals from both in-core and out-of-core ion chambers taken in conjunction with the rms measurements at 5, 10, 15, and 20 MWt were found to be quite similar. The boiling-noise amplitude at 20 MWt was found to be about 1.7% rms; and the relative noise variation with power taken from the recorder traces was found to be similar to the rms results.

The rms results and system schematic used are shown in Fig. 55. The relative rms noise reaches a minimum, suprisingly, at about 13 MWt. The

power level at instability is predicted to be about 34 MWt, which is not in disagreement with the results of the rod oscillator studies.

ID-103-A3117

Fig. 55. Relative rms Noise versus Power and Unstable Power Predicted by rms Noise Analysis, Boiling Core B-2

C. Conclusions

1. The analog cross-correlation method of measuring the reactor transfer function proved to be more satisfactory and faster than the null-balance method.

2. Large driving reactivity amplitudes of the rod oscillator caused very little change in the overall reactor frequency response on this core and caused a negligible but stabilizing effect on stability. Although the effect increased with power, a Δk amplitude of 15¢ at 15 MWt theoretically caused an error (reduction) of only 2% in gain and 0.7° in phase.

3. The ratio, $\bar{\beta}/\ell^*$, decreased with power, apparently by as much as a factor of 1.4 at 16.5 MWt, from the zero-power value.

4. A simplified model, excluding pressure effects but retaining a detailed description of the reactor kinetics, fuel pin heat transfer, and void dynamics, was found to fit the measured data very well in the 0.5- to 20.0-cps range. The deduced power coefficient at 16.5 MWt was high: 0.41% $\Delta k/k/MW$.

5. The reactor was still very stable at the maximum power level analyzed. At 16.5 MWt, gain and phase margins were about 12 db and 55°, respectively, the trends suggesting a power level instability in the range of 30 to 40 MWt.

6. The model-fitting process was rather insensitive to the value of the thermal conductivity used for the UO_2 fuel in the range of 1.3 to 1.7 Btu/hr-ft-°F. The final value selected was 1.5, which yielded a major, fuel, thermal time constant of 4.2 sec.

7. The relative noise level on the B-2 core was low at all power levels. The maximum measured amplitude (half-peak) was 1.7% rms at 20 MWt.

VIII. PLANT PERFORMANCE AND TESTS

A. Reactor Vessel

1. Radiation Effects

Upon completion of the B-2 core operations, a flux wire installed close to the reactor vessel wall was removed and analyzed to determine vessel irradiation dose and radiation effect. Calculations indicate that vessel irradiation damage, in terms of change in NDT (Nil-Ductility Temperature), is too small to be measurable. Since the "new vessel" NDT was below -50°F , there is no current cause for concern in this regard. It was noted that calculated radiation dosages to the vessel wall were very close to measured values.

2. Temperature Distribution

Reactor vessel wall temperatures, measured by a system of 18 permanent thermocouples, were satisfactory except in the center of the lower head in the control-rod nozzle area. Here, shell temperatures were as much as 95°F below reactor water temperature. This was judged to be due to the greater-than-estimated need for inflow of cold-control-rod seal water and a less-than-anticipated reactor water turbulence in this section of the head.

This condition was acceptably improved by a piping change which allowed a small amount of heated water from the auxiliary pump discharge to be by-passed to the center portion of the head by means of the vessel blowdown pipe. Temperature differences in the range of $15\text{-}35^{\circ}\text{F}$ were then obtained.

3. Water-level Measurement

The reference design system of reactor water-level measurement, utilizing a pressure-compensated, differential-pressure signal, referred to a heated reference leg, was found to have several operating problems. Maintenance of a full reference leg at reactor saturated-pressure conditions was found to be impractical during periods of reactor blowdown. Such operations created excessive flashing in the reference leg, with attendant erroneous level indications. The system was converted to one using a "cold" reference leg, operated at reactor vessel pressure, appropriate changes being made in the electrical portion of the pressure-compensation system.

The use of heated, reactor-water-level, gage glasses decreased greatly the error in level measurement caused by their mounting on a stand-pipe some 15 ft from the reactor vessel. The glasses, originally supplied

with internally mounted, 1/4-in.-OD, stainless-steel, steam tracing tubes, were found to be inadequately heated. The internal tubes were removed, and a pair of 1/2-in. pipes was strapped and cemented to the outside edges of the gage glass frames. This technique provided gage-glass water temperatures averaging some 100°F below reactor saturation temperature.

The use of out-of-vessel devices to determine internal reactor vessel level is a difficult problem at high reactor powers. The water-steam interface rises considerably above regions of high power density in the core, and in-vessel measurements indicate a serious disagreement with the external-gage-glass and differential-pressure measurement.

A sodium tracer technique was utilized to provide information on actual position of the steam-water interface. This technique is based on the phenomenon that sodium, injected into a boiling-water reactor, associates with the liquid phase and not the gaseous phase. A program of sampling and analyzing for Na^{24} activated by reactor neutrons was used to detect the water level. Sampling was performed by using a movable steam probe installed in a vessel head nozzle, and sodium nitrate was injected into the vessel through a separate line. This method indicated the steam to water-steam mixture "interface" in the reactor vessel to an accuracy of about 1/2 in.

When this technique was used at 1-MWt reactor power, the indicated true interface was at 13 ft, 1 in., with the gage glass at 12 ft, 8 in. At 10 MWt, the true interface was at 13 ft, 11 $\frac{1}{4}$ in.; and at 15 MWt, the level was 14 ft, 4 $\frac{1}{4}$ in. Gage-glass readings for the two latter conditions were 12 ft, 2 $\frac{1}{4}$ in., and 12 ft, 2 in., respectively. These indicated differences of 5, 21, and 26 in. in level occurred for a sample probe position 16 in. radially out from core center and do not necessarily reflect the maximum interface elevation. Since the temperature of the water in the gage glass was not measured for this test, only the change in relative gage-glass levels with power is significant.

B. Core Structure

1. Control-rod Shroud

The control-rod channels in the X-8001 Al core shroud were visually inspected before the control rods were removed, following power operation with core B-2, to determine if deformation had taken place. This examination showed that the five cruciform channels maintained their shape, while the four "tee" channels were bowed-in at the top "tee" junction. Using Micarta gages, it was determined that the thickness of the "tee" control-rod channels had been reduced from 1/2 in. to something less than 3/8 in. at the top of the shroud. A similar deformation was observed earlier during the first reactor heating without reactor fuel installed. To

pull the control rods easily, the channel was expanded to approximately the design width of $1/2$ in., using Micarta wedges. The cruciform channels were also checked at this time, and the worst collapse was a channel width of $7/16$ in. The deformation was similar to what has been reported before, in that the worst reduction in channel thickness was at the top of the shroud. There was no indication of any interference with control-rod motion. Spacer brackets have been installed in the top of the control-rod shroud channels to resist this deformation.

2. Belleville Spring

Before power operation with the B-2 core, the natural-convection Belleville spring was installed on the top hold-down plate of the boiling core structure (see reference 1 for a description of this spring). With the reactor vessel head bolted in place, a cold-core-structure hold-down load of 5,700 lb resulted from an average Belleville spring deflection of 0.083 in.

Following power operation with the B-2 core, the reactor vessel head was removed and a reading taken on the four Belleville spring-deflection jigs. Due to the differential thermal expansion between the reactor vessel and the core structure, it was determined that the spring had deflected an additional 0.081 in. This gave a total average spring deflection of 0.164 in. at operating temperature and pressure, which resulted in a core structure hold-down load of 13,200 lb. This compares with the design conditions of 0.220 in. and 20,000-lb load. The 13,200-lb load was considered adequate.

C. Fuel

1. Boiling Fuel Rods

The performance of the $3/8$ -in.-OD x 0.015-in.-wall, stainless-steel-clad, 4.95 w/o-enriched, UO_2 boiling fuel rods was entirely satisfactory during the operation of core B-2. There was no evidence of failure. Visual inspection of some fuel rods and fuel handling after shutdown revealed no defective or deformed boiling fuel rods.

2. Boiling Fuel Assembly Boxes

Each of the two fuel-positioning grids in a boiling fuel assembly box was held in place by rivets. Originally, there were twelve $1/8$ -in.-diam., X-8001, aluminum rivets in each grid.⁽²⁾ These were replaced with twenty $3/16$ -in.-diam., "A"-nickel rivets.

Early in the course of the hot, zero-power, critical experiments, it was discovered that virtually all the aluminum rivets had failed,

permitting the grids to move out of position. This failure is thought to be due to unexpectedly low creep strength of X-8001 aluminum alloy at operating temperature. The new nickel rivets have performed satisfactorily to date. Furthermore, inspection of a test fuel assembly after 2344 hr of autoclaving at 600 psig and 489°F revealed no evidence of rivet failure or dimensional change in the boiling fuel assembly box.

3. Fuel Handling and Storage

It was demonstrated that large radioactive BORAX reactor components such as core structures and control rods could be transported in open air without expensive shielding devices.

To facilitate this type of handling, the building crane rails and bridge beams were equipped with fixed foot and inch scales, and the bridge and trolley were fitted with pointers which moved past the scales when the crane was moved. An indexing system was thus provided to map the location of the reactor vessel centerline, storage pits, etc. This method permitted positioning with an accuracy of $\pm 1/2$ in. Shield blocks were then built up about the fixed crane operating station, mounted on the west wall of the reactor building, high enough for operator protection, but low enough to permit reading of the indices.

By this means, the control rods and boiling core structure (estimated at 5000 R/hr on contact) were moved from the reactor vessel to storage with only nominal personnel-radiation dosages after the completion of the experiments with core B-2.

D. Reactor Control

1. Electrical

The reactor shutdown circuits all performed satisfactorily during core B-2 operation except the low-steam-flow scram circuit. This device was intended for protection during later superheater core operation but was test-operated during boiling core power operation. Failure to operate was due to unsatisfactory venting of the differential-pressure cell and to dirt in the steam pipe lines. Piping changes were made to insure satisfactory venting.

Two intermediate, two-speed, rod drive motors failed. In each case, it was the motor for control rod No. 8. After considerable inspection and testing, the trouble was diagnosed as either overheating due to high starting current and an excessive number of starts, or unsatisfactory winding during manufacture. No conclusive evidence was found to prove the cause. Because of the large number of repetitive starts and

high starting current, conventional overload protection caused an excessive number of trip-outs. The two-speed motors were rewound with over-temperature protective devices imbedded in the windings.

The reactor control circuits were modified so that when driving the intermediate rods down they would run at high speed, whether running individually or as a group. This permitted a faster shutdown by driving in control rods and resulted in a saving of operating time.

2. Control-rod Drives

The control-rod drive seals were modified by replacing Delrin guide bushings with stainless-steel-backed, Teflon guide bushings to obtain better high-temperature strength and stability of the guide bushings. During operation with boiling core B-2, the control-rod drop times were satisfactory and no scarring or scratching of the extension shafts was noted. After the boiling core experiments were completed, all rod drives were removed (in conjunction with control-rod inspection), and all the extension shafts and components from one seal were carefully inspected. All nine of the extension shafts were in the as-installed condition, with only the normal burnish marks caused from passing through the Stellite seal rings. The Teflon guide bushings showed no signs of wear or deterioration due to heat or gamma radiation. No radioactive crud was found in the seal housings; in fact, only background radiation was detectable, and the only crud found was a very light, nonadherent coating of ferric oxide (Fe_2O_3) which covered the seal rings, the dashpot plunger, and the internal housing cavity.

3. Control Rods

The third inspection of the control rods took place after 65 MWd of boiling core B-2 operation. Since the control rods were highly radioactive (up to 600-700 R/hr on contact), the inspection was carried out in the water storage pit. Three types of gages were used, and their functions are as follows:

a. Thickness gages of a Go/No-go type were used to check blade thickness, both poison and follower, of the control rod. Tapped holes were provided so that a pair of long handles could locate the gages over the full length to be gaged.

b. Angularity gages, machined in the form of a cruciform, were used to check the 90° angle between each blade by slipping the gage over the control rod.

c. A straightness-and-twist fixture, consisting of parallel plates machined to a flat plane, was used in conjunction with a piece of 0.025-in. shim stock attached to a long handle to check straightness and

twist. A control rod was set in the fixture and an attempt made to insert the shim stock between the fixture and the mating surface of the control rod. If the control rod was twisted or bowed more than the permissible tolerance, the shim stock would slip between the control rod and fixture.

4. Results

a. Thickness. All nine control rods passed the 0.330-in., slotted-thickness gage. (The maximum design thickness of the control rods is 0.328 in.) The gage was tight on some of the aluminum followers, but a slight push made the gage go.

b. Angularity. The 0.390-in., slotted-angularity gage passed over the poison section of all control rods, but would pass over only the follower of control rod No. 1 from core control rod position 1. The 0.438-in., slotted-angularity gage passed freely over all the followers except those of T-shaped control rods No. 8 from core-control-rod position 3, and No. 10 from core-control-rod position 9. A slight push on the gage handles made the gage go. (The 0.438-in. gage represents the maximum tolerance for angularity and horizontal bend of control-rod blades.) The designed reverse bend on the wide blade of these T-shaped rods may have affected the measurement. All the T-shaped rods had retained some of their initial reverse bend, although the exact amount could not be determined.

c. Straightness and Twist. All control rods were straight and had no twist greater than 0.025 in., as determined from the straightness-and-twist fixture. (The maximum allowable twist and bow over the full length is 0.032 in.)

5. Conclusions

All control rods passed the dimensional inspection and showed no deviation from allowable tolerances. There was no evidence of blade swelling or leaks in the stainless-steel can. Only superficial, longitudinal, rubbing marks were noted in the Fe_2O_3 crud deposit on the control-rod blades.

E. Nuclear Instruments

To reduce the noise on the period circuits and the resulting excessive number of spurious scrams, the period-meter amplifiers were moved from the control room to the reactor building instrument room. The linear flux amplifier was rebuilt to change the range-switching system. Both changes resulted in reduced noise on the period-meter circuits.

1. Power Calibration of Nuclear Instruments

The reactor power level is normally measured by neutron-detecting, compensated, ionization chamber circuits. These instruments were calibrated during the operation of core B-2 by two methods: (1) operating the reactor to match heating rates using electric preheaters, and (2) using steam and feedwater flow rates to make a heat balance

With the electric preheat system circulating water at 100 gpm, the reactor temperature was raised from 167 to 173°F in 30 min, using 128 kW of electric heat. The same heating rate for the same temperature range was obtained with reactor power. The linear, neutron-flux operating instrument read 0.8×10^{-6} Amp.

As the reactor temperature is raised, the neutron-flux level at the operating instruments is increased for the same reactor power level. This is due primarily to the decrease in attenuation of neutrons in the downcomer water because of the reduced density of water. In the range of 460-489°F, electric heating input of 225 kW produced a temperature rise equal to a reactor power level indication of 0.35×10^{-5} Amp, as indicated on the linear, neutron-flux-level instrument. Thus, the neutron-flux-circuit sensitivity in amperes per kilowatt of reactor power is three times the sensitivity at temperatures of 167-173°F.

With the reactor operating at power and producing steam, a comparison of the level of the linear, neutron-flux circuit with feedwater flow and steam flow was used as the basis for power-level calibration. The feedwater flowmeter was calibrated against measured changes in water level in the known volume of the reactor vessel. At the maximum power of 20.5 MWt, as calculated from feedwater input rate, the linear flux instrument showed a value of 0.46×10^{-3} Amp. This further increase in chamber current for steaming conditions, as compared with a linear extrapolation of power level from the 489°F nonsteaming, is believed to be caused by the increased leakage of neutrons from the core and reflector due to the steam void content.

F. Water and Steam Systems

1. Steam System

A series of tests, using the reactor back-pressure regulating valves, was performed to determine the effect of valve operation on reactor power. These piston-operated valves were originally chosen for their high speed of response to enable performance of transfer-function studies on the reactor with variable steam pressure as the driving function. Because of the possible chance of reactor damage due to a rapid, valve-induced, pressure transient, the controls of all piston-operated valves in the plant were

deliberately modified to increase their stroking time to a minimum of 10 sec. The modification was made on the controls of both back-pressure control valves and consisted of adding a needle valve and volume chamber on the control-signal input of each valve.

After the above modifications, experiments were performed at reactor powers of 3 and 5 MWt to determine the effect on reactor performance of partial closure of the atmospheric-vent, back-pressure, control valve. At 600 psig and an initial reactor power of 3 MWt, with the steam valve 2% open, the valve was moved to its closed position in about 0.7 sec. (Feedwater measurements before the experiment indicated the closed valve was leaking steam equivalent to about 1.7 MWt. Valve leakage was not measured after the test.) Power increased to about 3.5 MWt with an initial period of about 3 sec for about 1 sec, after which the period slowly decreased. The test was terminated after 12 sec at a pressure of 625 psig and power of about 3.8 MWt by insertion of control rods.

At 5 MWt, the valve traveled from 13% open to the closed position in 1.6 sec. The power level rose from 5 to 7 MWt and leveled in 18 sec. The experiment was terminated by inserting the control rods after 20 sec when the reactor pressure approached the scram pressure.

2. Boron Addition and Batch Feed Systems

The boron poisoning system was a continuing source of trouble. The concentration of boric acid required for 100% reactor shutdown with all nine control rods withdrawn necessitates constant heating of the storage tank, tankage, and associated piping. Furthermore, to minimize steam hammer during a dump, the tank is normally maintained at a temperature and pressure equal to that in the reactor (but not less than 175°). Because of the difficulty of maintaining proper control over a wide range of temperatures, under-temperature boric-acid precipitation and pipe-line plugging and over-temperature plateout were experienced. Also, there has been boric-acid attack on asbestos gasketing and valve packing and crystal fouling of the storage-tank relief valve. Teflon bearing gaskets and packing and a rupture disk ahead of the relief valve showed promise of correcting the last two difficulties.

Experience showed that lower boron tank temperatures diminished the problems. Tests showed that the tank could be held at 175°F and atmospheric pressure, and could then be dumped without objectionable hammer when valve operation was programmed so the dump was preceded by a 10-min period of steam admission for pressure equalization.

The pressure-equalizing steam line between the reactor vessel and the pressure tank of the batch emergency feedwater system was revised to feed into the bottom portion of the tank through a sparger-type distributor. Thus, the pressure-equalizing steam also served to heat the

tankage before discharge. This change eliminated the previously observed steam hammer in the pressure tank and discharge piping when the system was test-operated.

3. Turbine-generator System

The turbine-generator unit and steam plant had been through a prolonged period of overhaul, with a few slight modifications, before power operation with the B-2 core. New bearings were installed throughout the turbine generator. The main surface condenser had been cleaned, leak-tested, and repaired. The condensate pumps were overhauled. A completely new, air-ejector, exhaust system had been installed, providing a dilution air blower, a filter, a gas scrubber, a rotameter, and a moisture-removing heat exchanger. Many valves were packed and fittings repaired, etc.

Upon initial power operation with the B-2 core, the turbo-generator unit performed satisfactorily, except for failure of the condensate drain pump, which caused flooding of the air-ejector exhaust filters. Subsequent operation, following repairs to the pump and filters, proved satisfactory; however, slight adjustments to control valve settings, etc. were necessary. The turbine overspeed trip was adjusted and test-operated to trip at about 63.1 cps, or 3786 rpm. The maximum power of 20.5 MWt achieved in the reactor was absorbed by the turbogenerator unit loaded to capacity. The maximum vacuum achieved on the main turbine condenser at high powers was 20-21 in. of Hg vacuum.

The air-ejector exhaust system performed satisfactorily throughout the power operation. The system was slightly modified to provide for insertion of the dilution air upstream of the two filters to assure nonexplosive gas mixtures from that point downstream. During initial startup, the noncondensable-gas flow in the air-ejector exhaust system caused the 10-cfm rotameter to exceed full-scale, due mostly to turbine-gland in-leakage. At high power with good vacuum and gland seal on the turbine, the noncondensable-gas flow rate of the air-ejector exhaust system was approximately 5 cfm.

4. Feedwater System

Each of the reactor feedwater pumps was equipped with a recirculation loop to provide a flow path for pump cooling during periods of low plant demand. Spring-loaded relief valves were originally installed in these loops in an attempt to permit recirculation flow only during low-demand periods (and high pump-discharge pressure). These valves did not perform satisfactorily and required frequent maintenance. They were replaced with breakdown orifices which provided a continuous by-pass of about 10% of pump rated capacity.

When the reactor was at elevated temperatures, it was necessary to run one of the reactor feedwater pumps to supply seal water to the control rods, auxiliary pump, etc. Seal water demand was small, and therefore very little fresh makeup water was supplied to the feedwater storage tank and the feed pumps when the reactor was at zero power. Under these conditions, continuous flow through the pump recirculation loop led to heating of the stored feedwater. Since much of the reactor operation was at operating temperature and at zero power, a coil-type, raw water, heat exchanger was installed in the feedwater storage tank for feedwater cooling. Performance was satisfactory.

The seal-water pressure-control system was modified. A small valve installed in the seal-water pressure-control system improved system performance by damping the oscillations in pressure seen by the controller and thereby reducing control-valve oscillations. Considerable manual adjustment was necessary to obtain the proper flow to each control-rod seal and to the auxiliary water-pump seal.

5. Auxiliary Water System

The auxiliary water system satisfactorily performed all the functions for which it was designed. Before power operation with the B-2 core, the 144 kW of immersion heaters were augmented with an additional 166 kW of strip heaters on the exterior piping surfaces of the system, for a total electric heating capacity of 310 kW. This additional heater capacity approximately doubled the nonnuclear heating rate of reactor water.

The auxiliary water-circulating pump (P-2), with a maximum flow rate of approximately 135 gpm, was used for circulation of reactor water through the heater bank and/or the reactor demineralizer. Difficulties were encountered with the packed-stuffing box-type seals on the auxiliary water pump. Because of the extremely wide range of operational pressures (0 to 600 psig) and the necessity for maintaining a higher pressure on the seal water supply to the gland lantern ring than in the pump casing, movement of packing rings and lantern ring, and subsequent leakage, resulted. Tightening of the packing gland to reduce leakage frequently resulted in binding and freezing of the pump shaft. Addition of a differential-pressure, seal-water, control valve with seal supply-and-return rotameters helped appreciably but failed to solve the problem. Following B-2 core power operation, a carbon ring-type seal with its own cooling system was installed.

The reactor water demineralizer portion of the auxiliary water system was used to maintain reactor water quality at acceptable levels throughout the B-2 core operation. At reactor pressures of approximately 100 psig and below, the auxiliary water pump provided flow rates up to 20 gpm through the clean-up loop. At operating pressures greater than 100 psig, a diaphragm control valve and pressure break-down orifice controlled reactor-demineralizer flow rates in a satisfactory manner up to about 18 gpm.

IX. SPECIAL EXPERIMENTS

A. Control-materials Experiments

Experiments to determine the relative reactivity worth of different thicknesses of various control materials were performed in a "clean" B-2 core, shown in Fig. 56, after the completion of power operation. The primary purpose of these experiments was to determine a suitable control-rod material for BORAX-V in case of failure of the reference-design, stainless-steel-clad, Boral rods. Samples of control materials were

ID-103-A3222

Fig. 56. Control-materials Test, Clean Core B-2

compared in a special fuel assembly which had one row of fuel rods removed to leave a vertical water slot through the core. This slot was positioned as indicated in Fig. 56. The control materials samples, 8 in. long and $3\frac{1}{2}$ in. wide, were held in an aluminum "window-frame" holder, as shown in Fig. 57. The bottom of the sample was $\frac{7}{16}$ in. above the bottom of the active portion of the core. The experiments were performed at room temperature. It is estimated that core temperature varied from about 89°F at the beginning to about 87°F at the end of the experiments.

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Fig. 57. Samples and Holder for Control-materials Test, Boiling Core B-2

The experimental procedure was as follows: (1) With the sample slot containing only water, the nine-control-rod-bank critical position was found to be 8.625 in. (2) With a sample of the reference control-rod material ($\frac{3}{16}$ in., 50 w/o natural B_4C , Boral clad in $\frac{1}{16}$ -in. stainless steel) in position, the reactor was found to be critical with control rod No. 1 at 8.910 in. and all remaining control rods at 8.914 in. Raising control rod No. 2 to 9.914 in. led to a positive period of 13.3 sec. Raising all nine control rods from the critical position by 0.100 in. led to a positive period of 32.8 sec. (3) With a sample of 1100 aluminum, $\frac{5}{16}$ in. thick, in position, simulating a control-rod follower, the nine-rod-bank critical position was found to be 8.640 in. Raising the nine-rod bank to 8.740 in. led to a positive period of 31.0 sec. The reactor was found to be critical with control rod No. 2 at 6.672 in. and all remaining control rods at 8.914 in. Raising control rod No. 2 to 7.422 in. led to a positive period of 38.2 sec. (4) In all the remaining steps of the experiment, all control rods except

control rod No. 2 were held at 8.914 in. A different control-material sample was inserted, and the reactor was made critical by positioning control rod No. 2. Then control rod No. 2 was withdrawn to a new position, and the resulting positive period was observed. The raw data from steps of this type are recorded in chronological order in Table IX.

Table IX
CONTROL MATERIALS EXPERIMENTS
RAW DATA

Material of Sample	Position of Control Rod No. 2*		Period (sec)
	For Criticality (in.)	For Period (in.)	
3/16-in. Boral clad in 1/16-in. SS	8.914	9.914	13.3
5/16-in. 1100 Al	6.672	7.472	38.2
5/16-in. 2.2-w/o natural B-SS	8.830	9.630	21.8
5/16-in. 2.0-w/o-enriched B-SS**	9.244	10.044	19.4
3/16-in. 2.0-w/o-enriched B-SS** clad in 1/16-in. SS	8.970	9.770	22.3
1/16-in. SS clad in 1/8-in. 2.0-w/o-enriched B-SS**	9.145	9.945	20.1
5/16-in. Hf	9.157	9.957	20.6
5/16-in. Cd	8.739	9.589	19.9
1/4-in. Cd-In-Ag [†] clad in 0.025-in. SS	9.020	9.900	17.3
1/4-in. 1100 Al	6.600	7.850	17.7
1/4-in. 2.0-w/o-enriched B-SS**	9.200	10.000	19.6
1/4-in. Hf	9.040	9.900	18.7
1/4-in. Cd	8.614	9.514	18.4
3/16-in. Cd-In-Ag [†] clad in 0.025-in. SS	8.916	9.716	21.8
1/4-in. 2.2-w/o natural B-SS	8.679	9.480	23.4
3/16-in. 1100 Al	6.460	7.260	43.4
3/16-in. 2.0-w/o-enriched B-SS**	9.020	9.820	22.4
3/16-in. Cd	8.510	9.310	24.4
3/16-in. Hf	8.892	9.692	22.3

*All other control rods held at 8.914 in.

**B enriched to 92.4 w/o in B¹⁰.

[†]3.43 w/o Cd, 16.14 w/o In, 80.28 w/o Ag.

The first step in interpreting the data was computing values of reactivity, $\Delta k/k = \rho\beta$, from observed periods by using the expression on p. 27

of ANL-5800,⁽⁹⁾ with $\beta = 0.0071$, $\ell^* = 3 \times 10^{-5}$ sec, and other parameters for U^{235} from Table 1-13 of ANL-5800. In the second step of interpretation, it was estimated from the data of experimental steps (1), (2), and (3) above that the differential reactivity worth of the nine-rod bank was about 1.55%/in. and that the reactivity effect of replacing water by the sample of the present control rods was about $(1.55\%/in.)(8.625 \text{ in.} - 8.914 \text{ in.}) = -0.446\%$. The third step in interpreting the data was computation of the reactivity effect of replacing water by each sample, in turn. This computation was carried out as follows: (a) Use the data of Table IX and the results of the first step of interpretation to compute

$$w = \frac{\Delta k/k}{x - z}, \quad \bar{z} = \frac{x + z}{2}, \quad \text{and} \quad \bar{z}' = \frac{8.914 \text{ in.} + z}{2},$$

where z is the position of control rod No. 2 for criticality, and x is the position of control rod No. 2 for the observed period. (b) Consider only the data for 5/16-in.-thick samples (including the 1/4-in. Cd-In-Ag between 0.025-in. stainless steel) and plot w versus $\bar{z}$. Draw an estimated average straight line through the resulting points. (c) From the average straight line, read $w(\bar{z}')$, or w evaluated at $\bar{z}'$. (d) Compute the reactivity effect of replacing water by each sample, in turn, as $(8.914 \text{ in.} - z) \cdot w(\bar{z}') - 0.446\%$, where the -0.446% is from the second step of interpretation above. (e) Repeat steps (b), (c), and (d) for the 1/4-in. samples (including the 3/16-in. Cd-In-Ag between 0.025-in. stainless steel). (f) Repeat steps (b), (c), and (d) for the 3/16-in. samples. The final step of interpretation was computation of relative worths of samples of control materials, where relative worth = (reactivity effect of replacing water by sample)/(reactivity effect of replacing water by sample of present control rods). Results are displayed in Table X.

Table X
RELATIVE REACTIVITY WORTH* OF SAMPLES OF CONTROL MATERIALS

Material of Sample	Total Thickness of Sample		
	5/16 in	1/4 in	3/16 in
1100 Al	0 01	0 01	-0 02
Cd	0 91	0 85	0 80
2 2 w/o natural B-SS	0 96	0 88	-
2 0 w/o-enriched B-SS**	1 18	1 15	1 05
2 0 w/o-enriched B-SS* clad in 1/16-in SS	1 03	-	-
SS clad in 1/8-in 2 0-w/o-enriched B-SS*	1 12	-	-
Boral clad in 1/16-in SS	1 (Ref)	-	-
Cd-In-Ag [†] clad in 0 025-in SS	1 06 ^{††}	1 00 [§]	-
Hf	1 13	1 06	0 99

Relative worth = $\frac{\text{Reactivity effect of replacing water by sample}}{\text{Reactivity effect of replacing water by sample of reference control materials}}$

[Reference control rod material is 3/16-in Boral (50 w/o natural B₄C) clad in 1/16-in SS]

B enriched to 92.4 w/o in B¹⁰

[†] 3.43 w/o Cd 16.14 w/o In 80.28 w/o Ag

^{††} Actual sample thickness 0.300 in including 1/4 in Cd-In-Ag

[§] Actual sample thickness 0.238 in including 3/16 in Cd-In-Ag

B. Fueled-oscillator Experiment

At the end of the control-materials test, the special, slotted, boiling fuel assembly in core position 65 was replaced with a standard-core B-2 fuel assembly. A mock-up fueled, rotating, oscillating rod was inserted in reflector thimble C, as shown in Fig. 56. The oscillator rotor section which was next to the core was about 2.875 in. OD and 24 in. long, and was made of laminated Al-U fuel plates in a 180° arc containing 126.5 g of U^{235} . On the opposite side of the rotor was a 180° arc of 0.040-in.-thick cadmium. Both the U-Al fuel and the cadmium plates were taped to a thin-wall aluminum tube.

Critical experiments were run at room temperature with control rod No. 7 completely withdrawn to determine the reactivity worths of the oscillator. The differential reactivity worth resulting from rotating the oscillator 180° was measured to be 0.149%.

To determine the neutron-flux level and shape at the oscillator relative to the flux in the core, irradiation was made using gold flux wires 0.032 in. in diameter. The fueled portion of the oscillator was rotated toward the core, and the flux wires were located as shown in Fig. 56. Control rod No. 7 was completely withdrawn. The axial, thermal, neutron-flux distribution on the fuel and cadmium surfaces and near the core center is shown in Fig. 58.

ID-103-A3223

Fig. 58. Axial Neutron Flux Traverses, Mock-up Fueled Oscillator Test, Boiling Core B-2

APPENDIX A

Derivation of Feedback Model

The derivation of a simplified feedback model for the natural-convection operational mode of the B-2 core is outlined below. A much more complex model was first derived and used to study the inherent nuclear-feedback mechanism over the entire frequency range of interest, i.e., 0.01 to 20 cps. However, this study showed that the original complex model could be simplified greatly while sacrificing negligible accuracy in the 0.5- to 20-cps frequency range, which was of particular interest since it contained the resonance frequency for all power levels studied. All the theoretical results given above were calculated using this simplified model.

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⁽¹⁰⁾ code, the feedback model derived incorporates the inherent steady-state relationships shown in Figs. 59 through 63 for average void fraction, natural-convection flow, average slip ratio, subcooling, and flow friction as functions of reactor power level.

The simplified model excludes the reactivity effects due to pressure, Doppler broadening, and subcooling changes because these effects are negligible in the B-2 core above 0.5 cps. Hence, the derivation of those feedback mechanisms is omitted in this report. However, the important aspects of the model are not simplified, namely, the reactor kinetics, the fuel-pin 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:

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

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Fig. 59. Average Void Volume Fraction versus
Power Level (Calculated), Boiling Core B-2

ID-103-A3119

Fig. 60. Recirculation Flow versus Power (Calculated), Boiling Core B-2

ID-103-A3125

Fig. 61. Average Slip Ratio versus Power (Calculated), Boiling Core B-2

ID-103-A3124

Fig. 62. Subcooling versus Power (Calculated), Boiling Core B-2

ID-103-A3127

Fig. 63. Flow Friction Parameters versus Power Level (Calculated), Boiling Core B-2

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 (proportional to neutron population) and its deviation;

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

C_i = Concentration of effective delayed-neutron emitters (ith group);

λ_i = Delayed-neutron decay constants (ith group);

$\bar{\beta}_i$ = Effective yield of delayed neutrons (ith 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 XI.

Table XI
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	0.026

$$\left. \begin{array}{l} \bar{\beta} = 0.0071 \\ \bar{\beta}/\ell^* = 262.96 \\ \ell^* = 2.7 \times 10^{-5} \end{array} \right\} \begin{array}{l} \text{Hot only,} \\ \text{zero power} \\ (489^\circ\text{F, 600 psig}) \end{array}$$

2. Boiler Fuel-pin 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 pin.
- c. The conductances 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

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

where

K = Thermal conductivity of UO_2 fuel,

and

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

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), similar to that of Iriarte,⁽¹¹⁾

$$T_f(r,s) = AI_0(qr) + BK_0(qr) + \frac{\alpha P(s)}{Ks}, \quad (6)$$

where

$$q = \sqrt{\frac{s}{\alpha}},$$

$[I_0, K_0$ are Bessel functions.],

$$s = jw,$$

$$w = \text{rad/sec},$$

and

$$j = \sqrt{-1}.$$

The appropriate boundary and initial conditions are

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

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

and

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

where

T_c = Coolant temperature (assumed constant),

H = Average overall heat-transfer

$$\text{coefficient} = \frac{1}{1/h_F + 1/h_G + d_c/K_c},$$

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

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

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

$$\frac{\Delta Q(s)}{\Delta N} = \frac{2H}{Kq} \left[\frac{I_1(qa)}{qaI_1(qa) + bI_0(qa)} \right] \cdot \frac{1}{1 + \tau_0 s},$$

where

$$b = Ha/K$$

and

$$\tau_0(\text{clad}) = \rho_C C_C V_C / (Ha_C), \text{ cladding time constant.}$$

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 Q/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 are listed in Table XII and were evaluated with the aid of the IBM-1620 computer for a deduced value of 1.5 Btu/hr-ft-°F for the UO_2 thermal conductivity, 3785 Btu/ft²-°F-hr for the effective overall heat-transfer coefficient, and UO_2 pellet diameter of 0.343 in.

Table XII

FUEL-PIN HEAT-TRANSFER
CONSTANTS

n	a_n	τ_n (sec)
0	-	0.08
1	0.7107	4.20
2	0.1319	0.796
3	0.0515	0.324
4	0.0262	0.174
5	0.0152	0.108
6	0.0096	0.074
7	0.0064	0.054
8	0.0044	0.041
9	0.0032	0.032
10	0.0109	0.026
11	0.03	0.00

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

3. Steam Void Generation

The term "void fraction" in this section of the report implies the actual void volume weighted by the assumed axial reactivity importance

function discussed below, whereas the actual (nonweighted) voids are implied in Sec. 4 below. Calculated voids are plotted against power in Fig. 59.

The dynamic relationship between the steam void volume (or average core-void fraction) 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's⁽¹²⁾ 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. 60.

(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 B-2 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 the explicit expressions on pages 127-129.

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

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

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 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

$$\bar{\alpha}(s) = \frac{1}{A_c L} \int_{x_0}^L W(x) A_g(x, s) dx, \quad (12)$$

where

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

L = Length of the active core,

and

x_0 = Average distance of boiling boundary from the bottom of 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,

$$\left. \frac{\Delta \bar{\alpha}(s)}{\Delta Q} \right]_{\text{Boiling Region}} = \frac{A^2}{H_v L^2 \bar{V}_g A_c} \int_{x_0}^L \sin B(x + C) dx \int_{x_0}^x \sin B(x' + C) e^{-(s/\bar{V}_g)(x' - x_0)} dx', \quad (13)$$

where

$\bar{V}_g$ = Average steam velocity in active core.

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:

$$\Delta h_w(x, s) = \frac{A \Delta Q(s)}{L W_0} \int_0^x \sin B(x' + C) e^{-(s/\bar{V}_f)(x' - x)} dx'. \quad (14)$$

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

$$\int_0^{x_0} Q_0(x) dx = (\Delta h W)_0 = \int_0^{x_0} \left[\frac{\partial h_w(x)}{\partial x} \right] dx.$$

Likewise, the relationship between changes in the enthalpy gradient, $\frac{\partial h_w(x, s)}{\partial x}$, and the boiling boundary, $\Delta x_0(s)$, can be expressed approximately as follows for small changes:

$$\int_0^x \frac{\partial h_w(x', s)}{\partial x} dx' = \frac{1}{W_0} \int_0^{x_0 + \Delta x_0(s)} Q_0(x) dx.$$

Expanding the above equation, we get

$$\Delta x_0(s) \cong -\frac{W_0}{Q_0(x_0)} \cdot \Delta h_w(x, s). \quad (15)$$

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

$$\frac{\Delta x_0(s)}{\Delta Q} = \frac{-A}{L Q_0(x_0)} \int_0^{x_0} \sin B(x + C) e^{-(s/\bar{V}_f)(x - x_0)} dx. \quad (16)$$

(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,

$$A_g(x, s) = \frac{1}{\bar{V}_g H_V} \int_{x_0 + \Delta x_0(s)}^x Q_0(x') \cdot e^{-(s/\bar{V}_g)(x' - x_0)} dx'.$$

The boiling boundary-void transfer function is thus evaluated as

$$\frac{\Delta \bar{\alpha}(s)}{\Delta x_0} = \frac{-A Q_0(x_0)}{\bar{V}_g H_V L A_c} \int_{x_0}^L \sin B(x + C) e^{-(s/\bar{V}_g)(x - x_0)} dx. \quad (17)$$

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

$$\left[\frac{\Delta \bar{\alpha}(s)}{\Delta Q} \right]_{\text{Subcooled Region}} = \frac{A^2}{L^2 \bar{V}_g H_V A_c} \left[\int_0^{x_0} \sin B(x + C) e^{-(s/\bar{V}_f)(x - x_0)} dx \right] \left[\int_{x_0}^L \sin B(x + C) e^{-(s/\bar{V}_g)(x - x_0)} dx \right]. \quad (18)$$

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

$$Q_0 \frac{\Delta \bar{\alpha}(s)}{\Delta Q} = \frac{N_0 \partial \bar{\alpha}}{\partial N} \cdot \frac{1 + a_{13}s + a_{14}s^2}{(1 + \tau_{12}s)(1 + \tau_{13}s)(1 + \tau_{14}s)}. \quad (19)$$

Table XIII lists the values of the constants in Eq. (19) as a function of power level.

4. Recirculation Flow Hydrodynamics

The net-void transfer function 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 region are negligible compared with changes in the core and riser void fraction at all power levels.

Table XIII
FEEDBACK TRANSFER FUNCTION PARAMETERS
VERSUS POWER LEVEL

Power Level (MWt)	a_0	τ_{12}	$\frac{a_{13}}{\tau_{13}}$	$\frac{a_{14}}{\tau_{14}}$	$\frac{a_{15}}{\tau_{15}}$	$\frac{a_{16}}{\tau_{16}}$	$\frac{a_{17}}{\tau_{17}}$
5	3.55	0.084	$\frac{0.201}{0.125}$	$\frac{0.006}{0.128}$	$\frac{1.48}{1.03}$	$\frac{0.410}{0.287}$	$\frac{0.022}{0.016}$
10	6.93	0.065	$\frac{0.153}{0.102}$	$\frac{0.005}{0.113}$	$\frac{1.23}{0.951}$	$\frac{0.295}{0.217}$	$\frac{0.014}{0.010}$
15	9.20	0.062	$\frac{0.135}{0.094}$	$\frac{0.004}{0.110}$	$\frac{1.14}{0.768}$	$\frac{0.264}{0.169}$	$\frac{0.012}{0.008}$
20	10.10*	0.062	$\frac{0.124}{0.089}$	$\frac{0.004}{0.110}$	$\frac{1.10}{0.626}$	$\frac{0.250}{0.138}$	$\frac{0.011}{0.006}$
30	11.40*	0.052	$\frac{0.113}{0.082}$	$\frac{0.004}{0.092}$	$\frac{0.966}{0.617}$	$\frac{0.187}{0.106}$	$\frac{0.007}{0.004}$

*Extrapolated from Fig. 19.

a. Void Effects on Recirculation Flow

A momentum balance around the recirculating loop yields

$$I_w \frac{dW}{dt} = \Delta p_v - \Delta p_f, \quad (20)$$

where

I_w = Effective inertia of water and steam in the circuit

$$= \frac{1}{144g_0} \sum_{i=1}^m \left(\frac{L_i}{A_i} \right),$$

W = Recirculation flow around the loop (mass flow rate through active core),

m = Number of various cross-section elements in series in the circuit,

Δp_v = Transient driving head in loop, assumed a function only of void fraction in active core and risers,

and

Δp_f = Transient 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

$$\bar{\alpha}_t(t) = \frac{1}{A_c L} \int_{x_0}^L A_g(x, t) dx. \quad (21)$$

We will approximate the relationship between actual-void and weighted-void fractions by a constant term evaluated as follows:

$$\frac{\bar{\alpha}_t}{\bar{\alpha}} = \frac{\int_{x_0}^L A_g(x) dx}{\int_{x_0}^L W(x) \cdot A_g(x) dx} = \frac{\int_{x_0}^L dx \left[\int_{x_0}^x \sin B(x' + C) dx' \right]}{A \int_{x_0}^L \sin B(x + C) dx \left[\int_{x_0}^x \sin B(x' + C) dx' \right]}$$

Thus, $\bar{\alpha}_t/\bar{\alpha}$ depends only upon power distribution and boiling boundary, x_0 . Power distribution is further assumed to remain constant with power.

Laplace-transforming Eq. (20), we have

$$I_w s W = \Delta p_v(s) - \Delta p_f(s). \quad (22)$$

The transient frictional head can be expressed as

$$\Delta p_f(s) \cong \left(\frac{\bar{\alpha}_t}{\bar{\alpha}} \right) \left(\frac{\partial f}{\partial \bar{\alpha}_t} \right)_W \left(\frac{W}{W_0} \right)^2 \cdot \Delta \bar{\alpha}(s) + 2 \left(\frac{W}{W_0} \right) f \frac{\Delta W(s)}{W_0}, \quad (23)$$

and the transient driving head can be written approximately as

$$\Delta p_V(s) \cong \frac{g}{144g_0} \left\{ (\rho_f - \rho_g) \left[L\bar{\alpha}_t(s) + \frac{(L_1 + L_2 + L_3)\alpha_E(s)}{1 + \tau_r s} \right] - (\Delta\rho_r L_3) \cdot \frac{\alpha_E(s)}{1 + \tau_r s} \right\}, \quad (24)$$

where

$\alpha_E(s)$ = Average void fraction at core exit

$$= \left(\frac{\alpha_E}{\bar{\alpha}_t} \right) \cdot \bar{\alpha}_t(s) = \left(\frac{\alpha_E}{\bar{\alpha}_t} \right) \left(\frac{\bar{\alpha}_t}{\bar{\alpha}} \right) \cdot \bar{\alpha}(s),$$

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. (23) and (24) into (22) and rearranging results in

$$\frac{\Delta W(s)}{\Delta \bar{\alpha} \cdot W_0} = \frac{C_{VW}(1 + \tau_{r2}s)}{(1 + \tau_{r1}s)(1 + \tau_{ws})}, \quad (25)$$

The τ 's and C_{VW} are defined on pages 127-129.

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:

$$\frac{\partial}{\partial x} \left[\bar{V}_g A_g \right] + s \cdot A_g(x, s) \frac{Q(x)}{H_v}. \quad (26)$$

Expanding each term of Eq. (26) 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

$$-\frac{\partial a_g(x, s)}{\partial x} + \frac{s}{\bar{V}_g} a_g(x, s) = - \left[\frac{\partial A_g(x)}{\partial x} \right] \cdot \frac{\Delta \bar{V}_g(s)}{\bar{V}_g}. \quad (27)$$

Now,

$$\bar{V}_g(o) \equiv \bar{V}_g(x_0) \equiv \frac{WS}{\rho_f A_c} = \text{Steam velocity at the boiling boundary,}$$

where

$$\bar{V}_g = \text{Average steam velocity in core,}$$

$$S = \text{Average slip ratio in core,}$$

and

$$\Delta \bar{V}_g(s) = \left(\frac{S}{\rho_f A_c} \right) \frac{\bar{V}_g}{\bar{V}_g(o)} \Delta W(s) = \text{Perturbation of average steam velocity.} \quad (28)$$

Incorporating the void-reactivity weighting function, substituting Eq. (28), and integrating Eq. (27), we have

$$W_0 \left. \frac{\Delta \bar{\alpha}(s)}{\Delta W} \right]_{\text{Boiling Region}} = \frac{Q_0 A^2}{H_v A_c \bar{V}_g L^2} \int_{x_0}^L \sin B(x+C) dx \int_{x_0}^x \sin B(x'+C) e^{-(s/\bar{V}_g)(x'-x)} dx'. \quad (29)$$

(2) Velocity Effect in Subcooled Region, Changing Boiling Boundary

We again use the following form of the void equation:

$$A_g(x, s) = \frac{1}{\bar{V}_g H_v} \int_{x_0 + \Delta x_0(s)}^x Q_0(x') e^{-(s/\bar{V}_g)(x'-x_0)} dx'. \quad (30)$$

Expanding the right-hand side of Eq. (30) in terms of $\Delta x_0(s)$, we get

$$a_g(x, s) = - \frac{Q_0(x_0)}{\bar{V}_g H_v} \cdot e^{-(s/\bar{V}_g)(x_0-x)} \cdot \Delta x_0(s). \quad (31)$$

Solving the energy equation for a variable flow rate and constant power production, we get

$$\Delta h_w(x_0, s) = \frac{-AQ_0 \Delta W(s)}{W_0 L} \int_0^{x_0} \sin B(x+C) e^{-(s/\bar{V}_f)(x-x_0)} dx. \quad (32)$$

Again using Eq. (15), we get

$$\Delta x_0(s) \cong \frac{W_0}{Q_0(x_0)} \cdot \Delta h_w(x_0, s).$$

Incorporating the weighting function and combining Eqs. (15), (17), and (32), we get

$$\begin{aligned} W_0 \frac{\Delta \bar{\alpha}(s)}{\Delta W} \Bigg]_{\substack{\text{Subcooled} \\ \text{Region}}} &= - \left(\frac{Q_0 A^2}{\bar{V}_g H_V L^2 A_C} \right) \left[\int_0^{x_0} \sin B(x+C) e^{-(s/\bar{V}_f)(x-x_0)} dx \right] \\ &\quad \left[\int_{x_0}^L \sin B(x+C) \cdot e^{-(s/\bar{V}_g)(x-x_0)} dx \right]. \end{aligned} \quad (33)$$

Adding Eqs. (29) and (33) and simplifying, we get the total recirculation effect on the weighted voids, as follows:

$$W_0 \frac{\Delta \bar{\alpha}(s)}{\Delta W} = \frac{C_{wv}}{1 + \tau_{wv} \cdot s}. \quad (34)$$

To get the overall effect of the void-recirculation flow interaction, Eqs. (25) and (34) are combined to yield the following void ($\bar{\alpha}$) to net void ($\bar{\alpha}_n$) transfer function:

$$\frac{\Delta \bar{\alpha}_n(s)}{\Delta \bar{\alpha}} = \frac{1 + a_{15}s + a_{16}s^2 + a_{17}s^3}{1 + \tau_{15}s + \tau_{16}s^2 + \tau_{17}s^3}. \quad (35)$$

Table XIII lists the values of the constants in Eq. (35).

5. Total Feedback Transfer Function

The final feedback expression given by Eq. (36) includes all the effects derived above, combined with the power coefficient of reactivity. Since the void coefficient was not determined explicitly for the B-2 core, either analytically or by experiment, the power coefficient (as defined below) instead was the gain parameter adjusted in the model-fitting process. This feedback model is complex enough so that it fits the measured data well in the 1.0- to 10-cps range, but is simplified sufficiently so that physical significance can be attached to the parameters as defined below.

Expressing the heat-transfer dynamics by $T_h(s)$ and the void-reactivity dynamics by $T_v(s)$, we can state the total feedback transfer function as

$$H(s) = T_h(s) \cdot T_v(s),$$

where

$$T_h(s) = \left(\frac{1}{1 + \tau_0 s} \right) \sum_{j=1}^{11} \left(\frac{a_j}{1 + \tau_j s} \right); \quad (36)$$

and

$$T_v(s) = a_0 \left[\frac{1 + a_{13}s + a_{14}s^2}{(1 + \tau_{12}s)(1 + \tau_{13}s)(1 + \tau_{14}s)} \right] \left[\frac{1 + a_{15}s + a_{16}s^2 + a_{17}s^3}{1 + \tau_{15}s + \tau_{16}s^2 + \tau_{17}s^3} \right].$$

The heat-transfer constants in $T_h(s)$ are listed in Table XII for the value deduced for the UO_2 thermal conductivity. The remaining constants in $T_v(s)$ are first defined by the following general expressions written in terms of void fraction, natural-convection flow rate, subcooling, slip ratio, and flow friction, as shown in Figs. 19 and 59-63:

$$a_0 = N_0 \left(\frac{\partial \rho_c}{\partial N} \right) \equiv N_0 \left(\frac{\partial \bar{\alpha}}{\partial N} \right) \left(\frac{\partial \rho_c}{\partial \bar{\alpha}} \right),$$

where

$$\frac{\partial \rho_c}{\partial N} = \text{Power coefficient of reactivity (see Fig. 19),}$$

$$\frac{\partial \bar{\alpha}}{\partial N} = \text{Slope of void fraction versus power (see Fig. 59),}$$

$$\frac{\partial \rho_c}{\partial \bar{\alpha}} = \text{Void coefficient of reactivity,}$$

and

$$N_0 = \text{Mean power level.}$$

The power coefficient and the constants in Eq. (36) are defined as follows:

$$\frac{\partial \rho_c}{\partial N} = \frac{(C_q + C_{qx}C_x)(\partial \rho_c / \partial \bar{\alpha})}{Q(1 - C_{wv} \cdot C_{vw})},$$

where

$$C_q = \left(\frac{A^2 \rho_f}{L^2 B^2 H_V} \right) \left(\frac{Q}{SW} \right) \left\{ \frac{1}{2} \left[\sin^2 B(x_0 + C) - \sin^2 B(L + C) \right] \right. \\ \left. + \cos B(x_0 + C) \left[\cos B(L + C) - \cos B(x_0 + C) \right] \right\},$$

$$C_{qx} = \frac{-AQ}{LBQ(x_0)} \left[\cos(BC) - \cos B(x_0 + C) \right],$$

$$C_x = \frac{\rho_f A Q(x_0)}{B H_V S W L} \left[\cos B(L + C) - \cos B(x_0 + C) \right],$$

$$C_{wv} = \frac{-A \rho_f}{L B H_V} \left\{ \left[\cos B(x_0 + C) \left[\cos B(x_0 + C) - \cos B(L + C) \right] \right. \right. \\ \left. + \frac{1}{4} \left[\cos 2B(L + C) - \cos 2B(x_0 + C) \right] \right] \left(\frac{A}{BL} \cdot \frac{Q}{SW} \right) \\ \left. + \left[\cos B(x_0 + C) - \cos B(L + C) \right] \left(\frac{\Delta h}{S} \right) \right\},$$

$$C_{vw} = \left(\frac{\bar{\alpha}_t}{\alpha} \right) \left[\left(\frac{\rho_f - \rho_g}{144} \right) \frac{g}{g_0} L_E - \left(\frac{\partial f}{\partial \bar{\alpha}_t} \right)_W \left(\frac{W}{W_0} \right)^2 \right] \frac{W_0}{2fW},$$

$$a_{13} = [C_q(\tau_{14} + \tau_{13}) + C_{qx}C_x\tau_{12}]/(C_q + C_{qx}C_x),$$

$$a_{14} = (C_q\tau_{14}\tau_{13})/(C_q + C_{qx}C_x),$$

$$\tau_{12} = \left(\frac{\rho_f A_c}{B^2} \right) \left(\frac{1}{SW} \right) \cdot \left[\frac{1}{L - x_0 + \frac{\sin 2B(x_0 + C) - \sin 2B(L + C)}{2B}} \right] \cdot \\ \left[\sin^2 B(x_0 + C) - \sin^2 B(L + C) + 2 \cos B(x_0 + C) \cdot \right. \\ \left. \left[\cos B(L + C) - \cos B(x_0 + C) \right] \right],$$

$$\tau_{13} = \left(\frac{\rho_f A_c}{B} \right) \left(\frac{1}{SW} \right) \left[\frac{\cos B(x_0 + C) - \cos B(L + C)}{\sin B(x_0 + C)} \right],$$

$$\tau_{14} = \left(\frac{\rho_f A_c}{B} \right) \left(\frac{1}{W} \right) \left[\frac{\cos(BC) - \cos B(x_0 + C)}{\sin B(x_0 + C)} \right],$$

$$a_{15} = \tau_{r_1} + \tau_w + \tau_{wv},$$

$$a_{16} = \tau_{r_1}\tau_w + \tau_w\tau_{wv} + \tau_{wv}\tau_{r_1},$$

$$a_{17} = \tau_{r_1} \tau_w \tau_{wv},$$

$$\tau_{15} = (a_{15} - C_{vw} C_{wv} \tau_{r_2}) / (1 - C_{vw} C_{wv}),$$

$$\tau_{16} = a_{16} / (1 - C_{vw} C_{wv}),$$

$$\tau_{17} = a_{17} / (1 - C_{vw} C_{wv}),$$

$$\tau_{r_2} = \frac{L}{L_E} \left[\frac{\left(\frac{W}{W_0} \right)^2 \left(\frac{\partial f}{\partial \bar{\alpha}_t} \right)_W - 1}{\left(\frac{g}{144 g_0} \right) (\rho_f - \rho_g) L} \cdot \frac{\left(\frac{W}{W_0} \right)^2 \left(\frac{\partial f}{\partial \bar{\alpha}} \right)_W - 1}{\left(\frac{g}{144 g_0} \right) (\rho_f - \rho_g) L_E} \right] \cdot \tau_{r_1},$$

$$\tau_{r_1} = (\rho_f A_c / 2) [(L_1 + L_2 + L_3) / (SW)],$$

$$\tau_w = (I_w / 2) [W_0^2 / (fW)],$$

and

$$\tau_{wv} = \frac{\rho_f A_c}{B \sin B(x_0 + C)} \left[\frac{1}{\Delta h \cdot W + \frac{A}{BL} \cdot Q [\cos B(x_0 + C) - \cos B(L + C)]} \right] \cdot \left[\left[\cos B(x_0 + C) [\cos B(x_0 + C) - \cos B(L + C)] + \frac{1}{4} [\cos 2B(L + C) - \cos 2B(x_0 + C)] \right] \left(\frac{A}{BL} \cdot \frac{Q}{SW} \right) + [\cos B(x_0 + C) - \cos B(L + C)] \left(\frac{\Delta h}{S} \right) \right].$$

The remaining constants are defined and evaluated in the Nomenclature below. Table XIII lists the values of the feedback transfer-function parameters, as calculated from the above expressions, versus power level.

6. NomenclatureCONSTANTS

a	Radius of UO ₂ fuel pin (excluding cladding)	0.0143	ft
A _c	Total cross-sectional area for flow inside boiler assemblies (outside fuel and/or flow rods)	3.63	ft ²
A	Amplitude of sine-fitted axial power distribution (axial peaking factor)	1.34	-
B	Constant in above sine function	1.24	ft ⁻¹
C	Constant in above sine function	0.424	ft
C _c	Cladding specific heat	0.14	Btu/lb _m -°F
d _c	Cladding thickness	0.00125	ft
g	Acceleration due to gravity	32.2	ft/sec ²
g ₀	Gravitational constant	32.2	lb _m -ft/lb _f -sec ²
h _f	Enthalpy of saturated water (600 psig)	475	Btu/lb _m
h _g	Enthalpy of saturated steam (600 psig)	1203	Btu/lb _m
h _F	Average film coefficient in sub-cooled and boiling regions	6825	Btu/hr-°F-ft ²
h _G	Conductance of helium gas space in clad fuel pin	∞	Btu/hr-°F-ft ²
H	Average overall heat transfer coefficient	3785	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	9.37x10 ⁻⁴	lb _f -sec ² /in ² -lb _m
K	Average thermal conductivity of UO ₂	1.5	Btu/ft-°F-hr
K _c	Thermal conductivity of cladding	10.6	Btu/ft-°F-hr

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
T_c	Average temperature of moderator water (coolant)	486	°F
V_c/a_c	Volume of cladding per unit of cross-sectional area	0.0012	ft
α	Thermal diffusivity of UO_2	8.98×10^{-6}	ft ² /sec
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
$\bar{\beta}$	Delayed neutron fraction	0.0071	-
ℓ^*	Prompt neutron lifetime	2.7×10^{-5}	sec
$\lambda_i, \bar{\beta}_i$	Delayed neutron parameters (see Table XI)	-	-

VARIABLE AND PARAMETER SYMBOLS AND STEADY-STATE
VALUES (at 20 MWt)*

$A_f(x)$	Water cross-sectional area in active core	-	ft ²
$A_g(x)$	Steam cross-sectional area in active core	-	ft ²
C_i	Concentration of effective delayed-neutron emitters	-	-
f	Flow-friction coefficient	0.5345	psi
$(\partial f / \partial \bar{\alpha}_t)_W$	Flow-friction derivative	2.5	psi
$G(s)$	Overall reactor transfer function	-	\$^{-1}\$
$G_0(s)$	Zero-power reactor transfer function	-	\$^{-1}\$

*Subscript "0" in text implies values of variables corresponding to design power of 20 MWt.

$G_0H(s)$	Open-loop transfer function	-	-
$h_w(x)$	Recirculating water enthalpy in active core below boiling boundary	-	Btu/lb _m
$H(s)$	Feedback transfer function	-	\$
I_0, I_1, J_0, J_1	Bessel functions	-	-
$k, \Delta k$	Multiplication constant, excessive reactivity	-	-
N	Power generated in fuel	1.90×10^4	Btu/sec
P	Volumetric power generation	5.29×10^3	Btu/sec-ft ³
q	Complex operator, $\sqrt{\frac{s}{\alpha}}$	-	ft ⁻¹
Q	Power transferred to coolant	1.90×10^4	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
s	Laplace variable	-	sec ⁻¹
S	Average slip ratio	1.767	-
t	Time	-	sec
$T_f(r)$	Temperature of fuel	-	°F
$T_h(s)$	Heat-dynamics transfer function	-	-
$T_v(s)$	Void-reactivity transfer function	-	\$
$\bar{V}_f$	Average water velocity in active core	5.1	ft/sec
$\bar{V}_g$	Average steam velocity in active core	10.9	ft/sec
W	Mass flow rate through active core	886.0	lb _m /sec
$W(x)$	Void-reactivity axial weighting function, $A \sin B(x+C)$	-	-
x	Axial distance in active core	-	ft
$\bar{\alpha}$	Average weighted void fraction in active core	0.095	-
$\bar{\alpha}_n$	Net average weighted void fraction	0.095	-

$\bar{\alpha}_t$	True average (unweighted) void fraction in active core	0.118	-
α_E	Void fraction at core exit	0.277	-
Δh	Subcooling of recirculating water	7.62	Btu/lb _m
Δp_f	Frictional head loss in loop	0.5345	psi
Δp_v	Driving head in loop	0.5345	psi
$\Delta \rho_r$	Effective coolant density increase in third riser	1.0	lb _m /ft ³

APPENDIX B

Reactor Describing-function Derivation (Outline)

If a reactor were truly a linear system, as transfer-function studies imply, then determination of stability "in the small" would apply to stability "in the large," since stability would be independent of perturbation amplitude. However, the reactor is truly a nonlinear system, and stability in the small does not necessarily imply stability in the large. Large amplitudes could, for example, lead to "limit cycles," or large, periodic oscillations of varying amplitude. Small-signal, frequency-response measurements alone cannot predict such behavior.

The describing-function concept is an extension of the transfer-function approach that is useful in predicting nonlinear reactor response on the basis of small-signal, frequency-response measurements. Once the inherent, feedback frequency response is deduced from the overall frequency-response measurement, then the effect of large Δk amplitudes (i.e., stability in the large) can readily be obtained. All that remains is to derive the theoretical expressions for calculating the describing function. The derivation outlined below uses the iterative method to derive the Fourier coefficients of the output (power amplitude) signal. The high-power describing function is finally presented as the ratio of the fundamental component of the output to the fundamental component of the input.

For this approach to be valid, the following conditions must be true:

- (1) The characteristics of the nonlinearity must not change with time, and
- (2) the integro-differential equations governing the behavior of each nonlinear element in the system must be known. These conditions are satisfied in the case of the kinetics nonlinearity if the feedback process is linear.

The describing-function method applies to systems of any order and can be used to determine the conditions for any self-sustained oscillations or limit cycles. In addition, the method can be used to evaluate the actual overall frequency response of the system, i.e., as it becomes distorted from the transfer function.

Sandmeier⁽¹³⁾ first applied the describing function to the EBR-I, Mark I reactor whose feedback was represented by a simple linear feedback model derived by Kinchin.⁽¹⁴⁾ The results showed that the relative stability was less with large Δk amplitudes than with small values. Later, Sandmeier applied the analysis to the EBWR⁽¹⁵⁾ reactor, using an experimentally determined, feedback transfer function. The effect of large Δk was again theoretically destabilizing. Later, analog studies of the EBR-I reactor using the Kinchin model gave excellent confirmation of the describing-function results.

The describing function does have limitations. First, from a practical standpoint, the effects of the higher-order space modes can be more important than the kinetics nonlinearity effect, especially in large reactors. Second, the accuracy of the method depends upon the specific feedback transfer function involved, the Δk amplitude, and the number of terms taken in the series expansion [Eq. (37)].

The high-power reactor describing function, $D(\omega, \Delta k, N_0)$ is thus an amplitude-dependent, pseudo-transfer function relating the fundamental components of the output power variation to a sinusoidal input reactivity variation. Thus, the D response function replaces the G transfer function and is a function of frequency (ω), reactivity amplitude (Δk), and mean power level (N_0).

The describing function may be written as an infinite series expansion,

$$D(\omega, \Delta k, N_0) = G_n(\omega) \left[1 + \Delta k^2 d_2(\omega, N_0) + \Delta k^4 d_4(\omega, N_0) + \dots \right], \quad (37)$$

where

$$G_n(\omega) = \left\{ \left[G_{0n}(\omega) \right]^{-1} - N_0 F_n(\omega) \right\}^{-1},$$

$$F_n(\omega) = \frac{1}{N_0} H(\omega), \quad (38)$$

$$G_{0n}(\omega) = \left[jn\omega \left(\frac{\lambda}{\beta} + \sum_{i=1}^n \frac{\bar{\beta}_i / \bar{\beta}}{\lambda_i + jn\omega} \right) \right]^{-1}, \quad (39)$$

$$d_\ell(\omega, n_0) = \sum_{m=0}^{\ell-1} N_0^m \cdot b_{1,\ell,m}, \quad (40)$$

$$b_{n,\ell,m} = G_n \left[\frac{1}{2} (b_{n-1,\ell-1,m} + b_{n+1,\ell-1,m}) + \sum_{q=0}^{\ell} \sum_{u=0}^m c_{qu} b_{n,\ell-q,m-u} \right. \\ \left. + \sum_{\substack{p=-\infty \\ p \neq 0,n}}^{\infty} \frac{F_p}{2} \sum_{q=0}^{\ell} \sum_{u=0}^{m-1} b_{pqu} b_{n-p,\ell-q,m-1-u} \right] \quad (41)$$

$$= 0, \text{ if } \begin{cases} n = 0 \\ \ell = n + 2s \ (s = 0, 1, 2, \dots), \\ m \geq \ell \end{cases} \quad (42)$$

$$c_{\ell,m} = -\frac{1}{2} \operatorname{Re} \left[b_{1,\ell-1,m} + \sum_{p=1}^{\infty} \sum_{q=0}^{\ell} \sum_{u=0}^{m-1} F_p b_{pqu} b_{-p,\ell-q,m-1-u} \right] \quad (43)$$

$$= 0, \text{ if } \begin{cases} \ell = \text{odd} \\ m \geq \ell \end{cases}, \quad (44)$$

and where

$G_{0n}(\omega)$ = The zero-power reactor transfer function evaluated at the n^{th} multiple of the fundamental angular frequency, ω ,

$G_n(\omega)$ = The high-power (overall) reactor transfer function evaluated at the n^{th} multiple of the fundamental angular frequency, ω ,

$F_n(\omega)$ = The specific reactivity-feedback transfer function evaluated at the n^{th} multiple of the fundamental angular frequency, ω ,

N_0 = The mean power level,

λ = The effective prompt-neutron generation time,

$\bar{\beta}$ = The effective delayed-neutron yield fraction,

$\bar{\beta}_i$ = The effective yield of the i^{th} group of delayed neutrons,

λ_i = The decay constant of the i^{th} group of delayed-neutron precursors,

n = Multiple of fundamental frequency,

and

$$j = \sqrt{-1}.$$

A flow sheet for the calculation of the $b_{n\ell m}$, $c_{\ell m}$, and $D^{(u)}(\omega, \Delta k, N_0)$ up to $u = 2$ is given in Reference 3.

At high power levels, it can thus be seen that the expression for D in Eq. (37) reduces to the transfer function, G , for small values of reactivity amplitude, Δk . At low power levels (zero power), the high-power reactor describing-function expression reduces to a low-power reactor describing function, $D(\omega, \Delta k)$, which is independent of $F_n(\omega)$ and N_0 .

The output of the high-power program lists the zero-, first-, and second-order approximations to D at any values of N_0 and Δk desired, along with G_0 and H at each frequency. The describing-function results presented in this report correspond to the more accurate second-order approximation.

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