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IDO-16974
May 1964

SELF-LIMITING EXCURSION TESTS OF A
HIGHLY ENRICHED PLATE-TYPE D₂O-MODERATED REACTOR
PART II. EFFECT OF SYSTEM PRESSURE

R. L. Johnson and J. E. Grund

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NATIONAL REACTOR TESTING STATION
US ATOMIC ENERGY COMMISSION

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PRINTED IN USA

IDO-16974
AEC Research & Development Report
Reactor Technology
TID-4500 (28th Ed.)
Issued: May 1964

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BY

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Contract AT(10-1)-205
Idaho Operations Office
U. S. ATOMIC ENERGY COMMISSION

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PART II. EFFECT OF SYSTEM PRESSURE

R. L. Johnson and J. E. Grund

ABSTRACT

Self-limiting power excursion tests have been conducted to investigate the effects of initial system pressure on the dynamic response of the Spert II BD-22/24 Expanded D₂O Core. This D₂O-moderated core is comprised of 3-inch square x 24-inch long, highly enriched, uranium-aluminum, plate-type fuel assemblies spaced on 6-inch centers. The power excursions were initiated by stepwise reactivity insertions at ambient temperature, with no forced coolant flow, at various initial system pressures from atmospheric to 375 psig, and both with and without an expansion space in the reactor vessel. The reactivity insertions ranged from about 50 cents to 2.6 dollars and resulted in initial asymptotic reactor periods ranging from about 5 sec to 60 msec, respectively. These tests demonstrated that the delay or suppression of boiling brought about by an increase in the system pressure results in higher maximum powers, energy releases, and fuel plate temperatures for those short period tests in which steam formation is an important contributor to shutdown at atmospheric pressure. Limited fuel plate damage occurred for tests with initial periods of approximately 70 msec or less, independent of system pressure. Calculation of the reactivity compensated by known nonboiling mechanisms gave results which were insufficient to account for the experimentally determined compensated reactivity.

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HIGHLY ENRICHED PLATE-TYPE D₂O-MODERATED REACTOR

PART II. EFFECT OF SYSTEM PRESSURE

CONTENTS

ABSTRACT	iii
I. INTRODUCTION	1
II. BRIEF DESCRIPTION OF THE FACILITY	3
1. PLANT DESCRIPTION	3
2. REACTOR DESCRIPTION	3
2.1 Reactor Components	3
2.2 Reactor Control System	7
3. TRANSIENT INSTRUMENTATION	9
III. REVIEW OF PREVIOUS TEST RESULTS	11
1. STATIC CHARACTERISTICS	11
2. ATMOSPHERIC PRESSURE POWER EXCURSION TESTS	12
IV. THE EFFECT OF SYSTEM PRESSURE ON DYNAMIC BEHAVIOR ..	17
V. RESULTS OF NONBOILING REACTIVITY COMPENSATION CALCULATIONS	29
VI. SUMMARY AND CONCLUSIONS	31
VII. REFERENCES	33
APPENDIX A	35
SUMMARY OF EXPERIMENTAL DATA	37
APPENDIX B	39
DETAILS OF NONBOILING REACTIVITY COMPENSATION CALCULATIONS	41
1. CASE I: $\tau_o = 263$ msec	41
2. CASE II: $\tau_o = 73$ msec	50

FIGURES

1. Location of pressurizer with respect to reactor vessel	4
2. Spert II reactor vessel assembly	5

3. Spert II core coordinate system	6
4. Spert type BD fuel assembly with a 22-fuel-plate loading	8
5. Operational core loading configuration of the BD-22/24 expanded D ₂ O core	11
6. Maximum reactor power vs reciprocal period	17
7. Normalized power burst shapes	18
8. Energy release at time of maximum power vs reciprocal period	19
9. Reactivity compensation at time of maximum power vs energy release to time of maximum power	20
10. Maximum measured fuel plate surface temperature at time of maximum power vs reciprocal period	22
11. Maximum measured fuel plate surface temperature vs reciprocal period	23
12. Reactor power and fuel plate surface temperature behavior as a function of time	24
13. Reactor power and transient pressure behavior as a function of time	25
14. Pressure at time of maximum power vs reciprocal period	26
15. Maximum pressure vs reciprocal period	26
16. Maximum transient pressure vs system pressure for 100-msec power excursions	27
17. Spert II lattice divided into radial and axial regions	42
18. Experimental and calculated (Two-Term Model) fuel plate surface temperature behavior for a thermocouple in the W1-2 assembly and an α_0 of 3.86 sec ⁻¹	45
19. Experimental and calculated (Two-Term Model) fuel plate surface temperature behavior for a thermocouple in the N1-2 assembly and an α_0 of 3.86 sec ⁻¹	45
20. Relative temperature distribution in the water adjacent to fuel surfaces as a function of the dimensionless parameter u for the Two-Term Model	46
21. Experimental and calculated (Exponential Model) fuel plate surface temperature behavior for a thermocouple in the N1-2 assembly and an α_0 of 13.7 sec ⁻¹	51

22. Experimental and calculated (Exponential Model) fuel plate surface temperature behavior for a thermocouple in the W1-2 assembly and an α_0 of 13.7 sec^{-1}	51
23. Relative temperature distribution in the water adjacent to fuel plate surfaces as a function of the dimensionless parameter u for the Exponential Model	52

TABLES

I. Static Characteristics of the Spert II BD-22/24 Expanded D_2O Core ..	13
A-I. Summary of Experimental Data	38
B-I. Comparison of Measured and Calculated Fuel Plate Service Temperatures - Case I	44
B-2. Comparison of Measured and Calculated Fuel Plate Surface Temperatures - Case II	51

SELF-LIMITING EXCURSION TESTS OF A HIGHLY ENRICHED PLATE-TYPE D₂O-MODERATED REACTOR

PART II. EFFECT OF SYSTEM PRESSURE

I. INTRODUCTION

A primary purpose of the overall Spert program is to determine, both experimentally and/or analytically, the dynamic behavior as well as the consequences of extreme power-excursion tests conducted under typical accident conditions on various classes of reactor systems. The Spert II reactor, operated by Phillips Petroleum Company as a part of the Atomic Energy Commission's Reactor Safety Program, is one of the facilities available for tests of this nature and is located in the Spert reactor area of the National Reactor Testing Station in Idaho. Previous dynamic tests in the Spert program have been conducted with light-water-moderated, -reflected, and -cooled reactor cores of various types [1, 2, 3, 4] and over a wide range of initial parametric conditions. These systems, although differing somewhat in many static characteristics, have all had prompt neutron lifetimes typical of light-water-moderated systems (≈ 100 to $20 \mu\text{sec}$). In order to study the effects of a longer prompt neutron lifetime on dynamic behavior, the Spert II reactor was designed to operate with heavy water as a moderator-coolant and reflector. This report is concerned with the effect of system pressure on the dynamic behavior and safety of a D₂O-moderated, -cooled, and -reflected core and represents an extension of the results previously reported for tests at atmospheric pressure [5].

Increasing the system pressure with a constant initial temperature will delay or in some cases entirely suppress boiling as a contributory reactivity compensation mechanism for a self-limiting power excursion. Clearly, if boiling were the only effective shutdown mechanism, the delay or complete suppression of it by increased system pressure could result in a more hazardous set of circumstances in an accident situation. Nonboiling shutdown mechanisms (ie, thermal expansion of water, fuel plate expansion, etc) also perform an important role in shutdown, and the delay or suppression of boiling by increasing the system pressure may allow the less effective nonboiling mechanisms to quench the power excursion. Previous tests [5] with the Spert II expanded D₂O core at ambient temperature and atmospheric pressure have indicated that steam formation performs an important part in self-limiting super-prompt critical power ex-

cursions. To determine the effects of suppressing the contribution of boiling to shutdown, a series of tests was performed with this core at ambient temperature and various system pressures to 375 psig.

This report describes the changes in kinetic behavior observed during this test series. A brief description of the core, plant facilities, instrumentation, and a review of the results from static measurements and the initial atmospheric pressure tests are given to serve as a basis for discussion and for purposes of comparison. A representative portion of the experimental data is presented, the salient features of these data are discussed, and pertinent conclusions are drawn concerning the kinetic behavior and safety of the system. Calculations to determine the amount of reactivity compensated by several postulated non-boiling shutdown mechanisms are performed. A summary of experimental data for each test in this series is presented in tabular form in Appendix A and details of the nonboiling reactivity compensation calculations are presented in Appendix B.

II. BRIEF DESCRIPTION OF THE FACILITY

1. PLANT DESCRIPTION

The Spert II facility consists of the reactor building and its control center which are separated from each other by a distance of approximately 1/2 mile. The reactor building encloses the reactor vessel, coolant flow system, heavy water system, and the necessary auxiliary equipment for operating a reactor of this type. The control center building houses the controls necessary for remote operation of the reactor and plant from a distance of approximately 1/2 mile during nuclear operation.

The reactor vessel and coolant flow systems are designed for operation at pressures up to 375 psig, temperatures up to 400°F, and coolant flow rates to 20,000 gpm, either upward or downward. The reactor system is pressurized by means of a high pressure helium gas supply. The pressurizer vessel is directly connected to the reactor vessel by a 6-inch line (Figure 1). The system is normally operated with a gas dome in the pressurizer and with the reactor vessel liquid-full in order to conserve the amount of helium required for pressurization. Operation also is possible, however, with a gas dome in the reactor vessel as well as in the pressurizer.

A detailed description of the plant is given elsewhere [6].

2. REACTOR DESCRIPTION

The Spert II reactor, shown in Figure 2, consists of the reactor core with the control rods and drives, a transient rod with its drive, the reactor core structure, and the reactor pressure vessel with a full diameter flanged top head. Inlet and exit coolant nozzles, which were blind-flanged for the tests discussed in this report, penetrate the reactor vessel bottom head.

2.1 Reactor Components

The reactor vessel is a stainless steel-clad, carbon steel vessel, 10-foot ID by about 16-feet high. Flanged nozzles for the egress of instrument leads are provided in the top head and at several elevations around the vessel shell. The reactor core is supported by an internal structure fastened to the bottom of the vessel. An aluminum flow skirt, 42-inch ID with 1/2-inch wall thickness, surrounds the core to a height of approximately 12 inches above the top of

Fig. 1 Location of pressurizer with respect to reactor vessel.

Fig. 2 Spert II reactor vessel assembly.

the fuel. The core support grid is divided into four symmetrical quadrants by a 3/4-inch thick aluminum rod guide cross.

The grid has 88 lattice positions available for the loading of fuel assemblies. The coordinate numbering system for these lattice positions is shown in Figure 3. The numbering system is based on a division of the core into four quadrants (N, E, S, and W) by the cross. Numbers are assigned to each lattice position

Fig. 3 Spert II core coordinate system.

by considering each individual quadrant separately as the first quadrant of a normal cartesian coordinate system with the origin at the center of the core. A given position is described by quadrant and by X- and Y-coordinates within the quadrant; eg, W 4-2. Thus, like numbered positions have quadrant symmetry.

The reactor is controlled by means of eight control rods which operate in the positions shown in Figure 3. In the center of the core is a blade-type transient rod used for initiation of step-transient tests.

The fuel assembly utilized in this core is the Spert BD-type assembly shown in Figure 4. Basically, the fuel assembly consists of a 3-inch square by 27-5/8 inch long aluminum fuel assembly can, an aluminum lower end box with locking lugs, two grooved side plates, a lifting bail, and 22 fuel plates. The lifting bail and the lower end box axially constrain the fuel plates in the fuel assembly can.

The fuel is contained in flat plates, each consisting of seven grams of U-235 alloyed with aluminum melting stock to produce a "meat" section 0.020 inch thick by 2.45 inches wide by 24 inches long. This fuel section is clad with 6061 aluminum alloy to produce a plate with overall dimensions of 0.060 x 2.704 x 25-1/8 inches. A fuel assembly, when fully loaded with 22 of these plates, contains 154 grams of U-235. The nominal water channel thickness is 0.065 inch.

2.2 Reactor Control System

The reactor control instrumentation basically consists of two B¹⁰-lined ionization chambers for reactor power operation above 0.1 watt, and two B¹⁰-lined neutron pulse chambers for low-level neutron measurements (≤ 1 watt). The outputs of the ionization chambers are displayed on two neutron level recorders (one linear and one logarithmic) mounted in the reactor console. The outputs of the pulse chambers are connected to scalers and to a count rate meter.

Due to the nature of the tests conducted, the control system contains no automatic safety circuits such as power level scram or period scram circuits; the control circuitry is designed to be fail-safe, and numerous interlocks, both electrical and mechanical, are provided. Thus, the control rods are automatically scrammed on loss of control system power and/or interruption of other interlocks. A manual scram, power failure, loss of control rod air pressure, or loss of contact between any rod and its drive unit will cause an automatic fast rundown of all four control rod drives. A 15 kVA, gasoline-powered, electric standby generator is provided to supply power to the control rod drives for insertion of the control rod drives in the event of commercial power failure.

Fig. 4 Spert type BD fuel assembly with a 22-fuel-plate loading.

3. TRANSIENT INSTRUMENTATION

The transient instrumentation is utilized to measure variables associated with the study of reactor behavior under dynamic conditions. The variables include neutron level, fuel plate surface temperature, pressure, moderator flow, etc. This instrumentation is not used for operational purposes or as a safety control for any given test, (ie, scram circuitry); however, the data obtained therefrom are extrapolated to establish specific limits for the overall test series. The data obtained from each test are recorded in analog form as a function of time on oscillograph records for reduction and analysis following each test. A description of the measuring system is given in Reference 5, and only those features unique to the pressurized tests are included in this report.

Reactor power over as much as 10 decades was measured by use of uncompensated B^{10} -lined ion chambers which are located at different distances from the core. Seven power level channels were used, five linear and two logarithmic. The electronic circuitry associated with each ion chamber was calibrated prior to the tests and weekly thereafter, and the current output of each ionization chamber as a function of the neutron power level was established by means of gold-foil activation measurements.

Fuel plate surface temperatures were measured by means of about 60 chromel-aluminum-alumel thermocouple junctions located throughout the core. Each thermocouple junction consisted of a 5 mil Alumel wire and a 5 mil chromel wire spot-welded to the aluminum fuel plate surface. Several ungrounded, chromel-alumel junctions were positioned in the moderator to measure bulk water temperatures during the course of the power excursions.

Transient pressure measurements were made using strain-gage-type pressure transducers located at the vertical core centerline in various positions in and around the core. Two of the pressure transducers were mounted on the side of a fuel can to measure the local pressure generated within the fuel assembly. Another transducer was located in a water space near the center of the core and two others were positioned near the flow skirt just outside the core. In addition, other transducers were located at the vessel wall and in the pressurizer for plant safety purposes.

Differential transformer devices for the measurement of transient flow were located at the top and bottom of the fuel assembly containing the side-mounted pressure transducers. Although the flow transducers were still developmental in

nature, qualitative correlations of pressure and flow were obtained from these measurements.

III. REVIEW OF PREVIOUS TEST RESULTS

1. STATIC CHARACTERISTICS

The Spert II BD-22/24 expanded D_2O core consists of 24, 3-inch square by 24-inch long, high-enriched, plate-type fuel assemblies spaced on 6-inch centers as shown in plan-view in Figure 5. This type of loading provides an expanded configuration which is typical of some heavy water research reactor designs.

Fig. 5 Operational core loading configuration of the BD-22/24 expanded D_2O core.

A summary of important core properties is presented in Table I. The details of the experiments performed to determine these static characteristics are given in Reference 7. The operational fuel loading of 3.7 kg of U-235 for the core provides a cold clean excess reactivity of approximately 8\$. Because of the long prompt neutron lifetime for this core ($\approx 750 \mu\text{sec}$), at least this amount of excess reactivity must be available to perform the required short-period tests at elevated system temperature.

Isothermal moderator heating provided a measure of the temperature coefficients of reactivity for uniform changes in density of the moderator-coolant throughout the core and reflector region and allowed calculation of density coefficients of reactivity. Since the greatest fraction of the moderator density change during a power excursion occurs within the fuel assemblies (measurements having indicated that inter-element heating is negligible for the majority of the tests performed), the localized reactivity coefficient, within the fuel assemblies, is of more importance for self-shutdown analysis of power excursions. The local void coefficient was determined to be negative within all the fuel assemblies in the core, with an average value of $-0.02\phi/\text{cm}^3$.

The thermal neutron flux is relatively uniform throughout the core and the reflector, however, a flux depression within the fuel assemblies (edge to center ratio ≈ 1.4) indicates that hot spots could occur near the assembly edges during power excursions.

The reduced prompt neutron lifetime, ℓ/β_{eff} , was determined by analysis of fifteen step-transient tests performed in the initial test series at atmospheric pressure [8]. These data provided a value of $\ell/\beta_{\text{eff}} = 0.10 \pm 0.01 \text{ sec}$.

2. ATMOSPHERIC PRESSURE POWER EXCURSION TESTS

The initial self-limiting power excursions performed in the Spert II reactor were step reactivity additions to the BD-22/24 expanded D_2O core with the system at ambient temperature, at atmospheric pressure, with no forced coolant flow, and a water head of approximately 50 inches above the core [5]. These were the first self-limiting power excursions of this type conducted on a heavy-water-moderated and -reflected core and constitute a base to which data from additional tests at different initial conditions may be compared in order to evaluate the effects of parametric changes.

TABLE I
 STATIC CHARACTERISTICS OF THE SPERT II BD-22/24 EXPANDED D₂O CORE

Fuel Assembly Type	Spert BD
Number of Plates Per Fuel Assembly	22
Number of Fuel Assemblies	24
Core Configuration	"Expanded" (see Figure 5)
Mass of U-235	
Initial Critical Loading	2.8 kg
Operational Loading	3.7 kg
Average Thermal Flux Per Watt	$\approx 6 \times 10^6$ nv/watt
Experimental Maximum-to-Average Core Flux Ratio	1.5
Isothermal Temperature Coefficient of Reactivity at a Constant System Pressure of 260 psig	$-3.3\phi/^{\circ}\text{C}$ @ 20°C $-4.4\phi/^{\circ}\text{C}$ @ 50°C $-6.2\phi/^{\circ}\text{C}$ @ 100°C $-8.0\phi/^{\circ}\text{C}$ @ 150°C
Temperature Defect (@ 260 psig)	
22 to 96°C	3.5\$
22 to 152°C	7.4\$
Available Excess Reactivity	
22°C, atmospheric pressure	8.10\$
96°C, 260 psig	4.65\$
152°C, 260 psig	0.60\$
Average Uniform Density Coefficient of Reactivity (per percent decrease in moderator density, determined from isothermal temperature changes)	$-94\phi/\%$
Average Local Density Coefficient of Reactivity (per percent decrease of moderator density within the fuel assemblies, determined from void measurements)	$-7\phi/\%$
Average Void Coefficient of Reactivity Within the Fuel Assemblies	$-0.02\phi/\text{cm}^3$
Maximum Measured Local Void Coefficient of Reactivity Within the Fuel Assemblies	$-0.03\phi/\text{cm}^3$
Reduced Prompt Neutron Lifetime, ℓ/β_{eff}	± 0.01 sec

Step reactivity insertions ranging from about 40 cents to 3 dollars resulted in initial asymptotic reactor periods ranging from about 10 sec to 50 msec. The major shutdown mechanisms were believed to be thermal in nature, both non-boiling shutdown such as fuel plate expansion and moderator heating and/or boiling shutdown caused by steam void formation. Utilizing nonboiling and boiling mechanisms as a criteria, the atmospheric pressure tests may be categorized into three general groups as follows:

- (1) Nonboiling mechanisms provided the necessary reactivity compensation required to quench the initial power burst for reactivity additions of about 1\$ or less, which corresponded to initial periods of about 600 msec or longer.
- (2) A combination of nonboiling and boiling shutdown mechanisms provided sufficient compensated reactivity to quench power bursts for reactivity additions of from about 1 to 2\$, corresponding to initial periods of from about 600 to about 100 msec.
- (3) Boiling was apparently the predominant shutdown mechanism for power excursions with initial periods less than about 100 msec, corresponding to reactivity additions greater than about 2\$.

In the region of nonboiling shutdown ($\tau_0 \gtrsim 600$ msec), the reactivity compensation was provided at the rate of about 4¢/MW-sec of energy released during the power excursion. The power burst shape was broad and symmetrical as would be predicted by analytical models in which the reactivity feedback is linearly dependent on the energy release.

In the transition region between that in which nonboiling shutdown predominates and that in which boiling shutdown predominates ($600 \text{ msec} \gtrsim \tau_0 \gtrsim 100 \text{ msec}$), the power burst shape became sharper and larger increments of reactivity compensation were obtained with smaller increments of energy release. Fuel plate surface temperatures in the central region of the core exceeded the saturation temperature at time of maximum power and the time behavior of the temperature indicated boiling within the core.

In the region of predominant boiling shutdown ($\tau_0 \lesssim 100$ msec), the power burst shape approached a nearly constant form and almost all of the fuel plate surfaces were above saturation temperature before time of maximum power. The contribution of boiling to shutdown was reflected in the power and energy data because larger reactivity compensation was obtained with only nominal additional energy release. This appeared to be a direct consequence of the existence of nucleate

boiling in a large portion of the core. The temperature data indicated that although a major portion of the core was experiencing nucleate boiling, the central or maximum power generation region was beginning to experience partial film boiling. The onset of this type of boiling-heat transfer in any portion of the core implies that extrapolation of power or energy data will not provide a true indication of the safety of the system. The onset of partial film boiling, with its steam blanketing of fuel plate surfaces, caused rapid increases in temperature with only small increases in the total energy release. The sizeable amounts of steam generated to quench short period power excursions also resulted in transient pressure generation. The rate of void generation in the fuel assemblies was relatively slow and the pressures observed during the time of steam formation were not large, even though as much as 15 to 25 percent of the moderator volume may have been voided during some tests. The more damaging pressures occurred when the steam voids collapsed, allowing water to return to the core very rapidly. This rapid return of water to the core resulted in pressures to about 30 psig with rise rates to about 4000 psig/sec.

The onset of limited local core damage, such as fuel plate deformation, occurred for tests with initial periods of about 70 msec (reactivity additions of about 2.4\$). Extrapolation of the maximum fuel plate surface temperature data indicated that localized melting of fuel plates might be expected for step insertions of about 3.5\$ reactivity, corresponding to initial periods of about 30 msec.

Comparison of the results of tests with this heavy-water-moderated core with the results of similar tests on light-water-moderated cores indicates that the heavy-water-moderated core experienced limited fuel plate damage for tests with periods five to ten times longer and for reactivity additions approximately twice as large. These differences may be attributed to the differences in prompt neutron lifetime and the magnitude of the shutdown coefficients in the two types of reactors. Although the self-shutdown mechanisms for the heavy-water-moderated core are basically the same as for highly enriched, plate-type, light-water systems, a larger dependence on steam formation for shutdown of sub-cooled power excursions was observed for the D_2O core because of the larger reactivity insertions required for corresponding periods. While steam is a highly efficient mechanism in terms of total energy release, the onset of film boiling can lead to dangerous burnout or meltdown conditions quickly. Extrapolation of the data to the shorter-period, larger reactivity insertion region indicates that while only a nominal increase in energy release would result, changes

in heat transfer mechanisms could be expected to result in a higher probability of obtaining a partial meltdown of the core.

IV. THE EFFECT OF SYSTEM PRESSURE ON DYNAMIC BEHAVIOR

The power excursion tests to be discussed in this section were initiated from criticality at a power level of about five watts, at ambient temperature ($\approx 25^\circ\text{C}$), with no forced coolant flow, and with system pressures in the range from atmospheric to 375 psig. The reactivity was inserted essentially stepwise by rapid ejection of the transient rod from the core. Reactivity insertions varied from about 50 cents to 2.6 dollars, resulting in initial asymptotic periods from 4.5 sec to 61 msec. The majority of the tests were conducted with the reactor vessel liquid-full and with an expansion space in the pressurizer vessel; however, several tests were initiated with an expansion space in both the reactor and pressurizer vessels.

Reactor power, fuel plate surface temperature, and pressure as functions of time were recorded for each test. These measurements are discussed below to aid in understanding the effect of system pressure on the kinetic behavior of the system. The results of the initial atmospheric pressure tests are utilized for purposes of comparison.

The maximum reactor power, $\phi(t_m)$, achieved during the initial burst of a self-limiting power excursion is a function of the initial period or reactivity insertion as well as the nature and magnitude of the intrinsic mechanisms operating to quench the power rise. Previous tests at atmospheric pressure have indicated that the shutdown mechanisms for the BD-22/24 expanded D_2O core are thermal in nature with a large dependence on moderator boiling for the shorter period tests. Increasing the system pressure for power excursions at ambient temperature delays and in some cases suppresses boiling entirely as a reactivity compensation mechanism. The effect of system pressure on the power behavior is illustrated in Figure 6 in which the maximum reactor power of the excursion, $\phi(t_m)$, is plotted as a function of the reciprocal of the initial asymptotic period, α_0 , for tests with initial system pressures of 0, 100, and 375 psig. The line shown on Figure 6

Fig. 6 Maximum reactor power vs reciprocal period.

represents a fit of a linear energy model for self-shutdown to the data for small reciprocal periods [9].

For periods longer than about 600 msec ($\alpha_0 \lesssim 1.7 \text{ sec}^{-1}$), where the fuel plate surface does not exceed the saturation temperature prior to the power peak at atmospheric pressure, elevation of the initial pressure has no measurable effect on the maximum power which is observed to follow the linear energy model prediction reasonably well. For $\alpha_0 \gtrsim 1.7 \text{ sec}^{-1}$, the fuel plate surface temperatures exceed the saturation point at or before peak power for tests at atmospheric pressure and boiling must now be considered as a reactivity feedback mechanism in addition to the existing nonboiling shutdown mechanisms. In the region, $1.7 \text{ sec}^{-1} \lesssim \alpha_0 \lesssim 4 \text{ sec}^{-1}$, the amount of boiling in the core is relatively small and does not significantly affect the maximum power of the initial burst. However, for $\alpha_0 > 4 \text{ sec}^{-1}$, the data at atmospheric pressure begins to fall below the line with slope 2. This is indicative of a change from nonboiling shutdown to the case where steam formation is an important contributor to quenching the power rise. In contrast, the tests performed with the system pressure increased must rely more heavily on nonboiling shutdown mechanisms for $\alpha_0 > 1.7 \text{ sec}^{-1}$ than the tests at atmospheric pressure, because of the increased saturation temperature. The region of nonboiling shutdown is thus extended to larger values of α_0 with a resulting increase in the maximum power for the pressurized tests. Although the magnitude of the power, $\phi(t_m)$, is clearly dependent on the system pressure, the suppression or delay of boiling does not grossly change the magnitude or slope of $\phi(t_m)$ as a function of α_0 .

The power burst shape or time history of the power is perhaps a better example of the effect of system pressure on power behavior. Shown in Figure 7 are the power burst shapes for a 261 msec, 375 psig, nonboiling shutdown test and for a 215 msec atmospheric pressure, boiling shutdown test. For convenience of comparison these curves are normalized to maximum power with the time scale in units of reactor periods. The pressurized test has a slightly broader power burst, with the occurrence

Fig. 7 Normalized power burst shapes.

of the peak being slightly delayed as compared with the atmospheric pressure test. The major change occurs after peak power, where for the atmospheric pressure test a rapid shutdown and recovery to an equilibrium power is obtained, in contrast to the 375 psig test where the power decreases monotonically throughout this region. This power undershoot in the atmospheric pressure test may be attributed to the growth and collapse of voids in the core due to boiling, whereas little, if any, boiling occurred in the pressurized test. This change in behavior is very similar to that observed in the Spert III light-water tests [10, 11], except that in Spert III the effects occur at periods which are about a factor of ten shorter, and for reactivity insertions about one-half as large as in Spert II.

The slight increase in the magnitude of the power accompanied by the broadening of the initial burst associated with the short period pressurized tests results in an increase in the energy release compared to the tests at atmospheric pressure. This change in behavior is illustrated in Figure 8 in which the energy release to time of maximum power, $E(t_m)$, is plotted as

Fig. 8 Energy release at time of maximum power vs reciprocal period.

a function of α_0 for tests with initial system pressure of 0, 100, and 375 psig. The energy release, $E(t_m)$, is the maximum amount of energy available to provide the temperature, void or other effects that supply the negative reactivity required to turn the power burst. These data indicate that a much higher energy release is needed to self-limit short period power excursions with the system pressure increased. For example, the energy release to time of maximum power is about a factor of two higher for a 73 msec test at 375 psig compared to the same period atmospheric pressure test. The higher energy release is due to a larger dependence on the nonboiling shutdown mechanisms, which are less efficient than steam formation in terms of reactivity compensation per megawatt-second of energy release.

Shown in Figure 9 is the reactivity compensated to time of maximum power, $\rho_c(t_m)$, as a function of $E(t_m)$ with system pressure the parameter. When less than fifty cents is needed to quench the power rise, ie, reactivity insertions of less than a dollar and a half, the compensated reactivity increases linearly with the energy at the rate of about three cents per megawatt-second, regardless of system pressure. When more than fifty cents is needed for shutdown, it is apparent that the initial system pressure is affecting the nature of the shutdown process. At this point, the fuel plate surface temperatures exceed the saturation

Fig. 9 Reactivity compensation at time of maximum power vs energy release to time of maximum power.

temperature at atmospheric pressure, and the amount of reactivity compensated per unit energy release increases to about twenty cents per megawatt-second. The addition of boiling to the nonboiling reactivity feedback mechanisms results in a net shutdown mechanism which is highly efficient compared to the non-boiling mechanisms alone. As the system pressure is increased, boiling is suppressed, and the reactivity compensated per unit energy release continues to increase at a linear rate of about three cents per megawatt-second until the energy release is sufficient to attain moderator boiling at the new saturation temperature. This is shown in the data at 100 and 375 psig in which the compensated reactivity increases rapidly as a function of energy release once boiling becomes important as a shutdown mechanism.

The power and energy behavior as a function of system pressure and reactor period represent average measurements which are typical of the reactor as a whole. These measurements are important with regard to the safety and kinetic behavior of the system but do not offer any information about the localized behavior within the core. The local measurements are more sensitive to changes in behavior than the average measurements and consequently, in some cases, provide a better indication of the safe limits of operation.

An example of an important local measurement made during the course of these power excursion tests is that of fuel plate surface temperature which provides an indication of the local power or energy generation. The magnitude and time behavior of the temperature at the surface of the fuel plates also indicates when steam formation is taking place and the nature of the boiling heat transfer process.

The temperatures of the fuel plate surfaces at peak power are of importance in determining the shutdown process. Figure 10 shows the effect of initial system pressure on the maximum measured fuel plate surface temperature, $\theta(t_m)$, at time of maximum power as a function of the reciprocal of the initial asymptotic period. Data are shown for tests with initial system pressures of 0, 100, and 375 psig. For $\alpha_0 \lesssim 1.4 \text{ sec}^{-1}$, $\theta(t_m)$ does not exceed the saturation temperature at atmospheric pressure and increasing the initial system pressure has no effect on the measured temperatures. For $\alpha_0 \gtrsim 1.4 \text{ sec}^{-1}$, the fuel plate surface temperature in the region of maximum power generation exceeds the saturation temperature at atmospheric pressure. This indicates that steam formation must be considered as a possible contributing mechanism to the self-limitation of all tests at atmospheric pressure with $\alpha_0 \gtrsim 1.4 \text{ sec}^{-1}$. When

Fig. 10 Maximum measured fuel plate surface temperature at time of maximum power vs reciprocal period.

the system pressure is increased, however, boiling is suppressed and the fuel plate surface temperatures do not exceed the respective saturation temperatures prior to time of peak power until $\alpha_o \gtrsim 3 \text{ sec}^{-1}$ at 100 psig and until $\alpha_o \gtrsim 4 \text{ sec}^{-1}$ at 375 psig. The magnitude of the temperature as a function of α_o is observed to be larger as the system pressure is increased for $\alpha_o \gtrsim 1.4 \text{ sec}^{-1}$. This is because boiling is suppressed and shutdown is more dependent on the nonboiling shutdown mechanisms. Consequently, higher fuel plate temperatures are necessary to attain the additional reactivity compensation from the nonboiling mechanisms.

The temperature, $\theta(t_m)$, for $1.4 \text{ sec}^{-1} \lesssim \alpha_o \lesssim 10 \text{ sec}^{-1}$ at atmospheric pressure, for $3 \text{ sec}^{-1} \lesssim \alpha_o \lesssim 10 \text{ sec}^{-1}$ at 100 psig, and for $4 \text{ sec}^{-1} \lesssim \alpha_o \lesssim 10 \text{ sec}^{-1}$ at 375 psig is observed to be increasing relatively slowly as a function of α_o . This behavior is intimately associated with the type of boiling heat transfer that is taking place on the surface of the fuel plate -- the heat flux, temperature and initial period apparently being such that nucleate boiling may be maintained.

This form of energy transfer is very advantageous for the safe quenching of a power excursion as it provides large reactivity compensation with smaller incremental energy release and temperature rise.

For $\alpha_o \gtrsim 10 \text{ sec}^{-1}$, however, conditions in the high power density regions of the core are such that the boiling heat transfer may change from nucleate boiling to partial film boiling as indicated by the temperature data. In this case, a steam blanket may form to essentially insulate the fuel plate so that energy cannot be transferred rapidly to the moderator and the fuel plate temperature will rise very rapidly. This appearance of partial film blanketing accounts for the rapid rise of $\theta(t_m)$ vs α_o for $\alpha_o > 10 \text{ sec}^{-1}$ (see Figure 10). This transition in temperature behavior is not grossly affecting the average shutdown characteristics of the reactor however, as it is restricted to only a small central portion of the core in the period range investigated. The larger portion of the boiling in the core is still nucleate which provides the major part of the boiling shutdown contribution. The appearance of partial film boiling with its associated temperature behavior, although not important for shutdown in these cases, does have important safety considerations as it may result in localized meltdown.

In terms of meltdown considerations, it is not $\theta(t_m)$ that is important, but rather the maximum fuel plate surface temperature, $\theta(\text{max})$. The $\theta(\text{max})$ vs α_o data are shown in Figure 11 with initial system pressure again the variable parameter. The data at atmospheric pressure are well behaved as a function of α_o , but the elevated pressure data show a large amount of scatter. This scatter is attributed to the presence in the reactor core of a number of deformed fuel plates which reduced the width of the adjacent water channels, resulting in localized hot spots. The fuel plates were deformed initially during the short period atmospheric pressure tests and, even though the more severely damaged plates were replaced, a sufficient number of replacements was not available for the tests at elevated pressure which followed. Consequently, the

Fig. 11 Maximum measured fuel plate surface temperature vs reciprocal period.

maximum temperature data at elevated pressure may be somewhat high in terms of an undamaged core and should represent an upper bound over the period range investigated. Extrapolation of the data at atmospheric pressure has indicated that the maximum fuel plate temperature could exceed the melting point for $\alpha_0 \gtrsim 30 \text{ sec}^{-1}$, which corresponds to reactivity additions $\gtrsim 3.5\%$. The data at elevated pressure indicated that fuel plate melting temperatures would be attained at longer periods and for less reactivity insertion than at atmospheric pressure; however, the scatter in the data makes extrapolation uncertain.

Examples of the power and fuel plate surface temperatures as functions of time during a transient are shown in Figure 12 for tests with initial periods of about 88 msec at 375 psig and atmospheric pressure. The temperature measurements shown were obtained from a thermocouple located in the central region of the core. These data again show the somewhat higher fuel plate temperatures required for shutdown with the system pressure elevated.

A consequence of the shutdown process is the generation of sizeable transient pressures during short period power excursions. The pressure data to be discussed were measured by a transducer located in a water gap near the center of the core (E 1-1 lattice position, vertical core center line). Several other

Fig. 12 Reactor power and fuel plate surface temperature behavior as a function of time.

pressure transducers positioned both in the core and in the reflector measured pressures very similar in time history and magnitude to the transducer located in E 1-1. An example of this similarity in behavior is displayed in Figure 13, in which the time histories of the reactor power and pressure during a 69-msec power excursion are shown for three different pressure transducer locations. The curve designated "pressure, N3-2", was obtained from a transducer mounted on the side of a fuel can in the N3-2 lattice position and shows the local pressure generated within the fuel assembly. The local pressure behavior is observed to show more detail, a result of local void generation in the fuel assembly, but to have the same gross behavior as the other transducers. The relatively small spacial dependence of pressure in the system is a consequence of the slow rate of change of pressure with respect to time, the example shown being typical of the pressure behavior observed for all tests conducted with the vessel liquid-full. This behavior is somewhat different from that observed in the initial test series [5] in which the pressure burst was sharper and of lower magnitude for comparable initial periods. The initial tests, however, were performed with a large expansion space in the reactor vessel, thus allowing rapid dissipation of the transient pressure. With the system liquid-full, the

Fig. 13 Reactor power and transient pressure behavior as a function of time.

pressure is relieved for the most part by expansion of the reactor vessel, which presents a strong constraining force and causes the pressure to rise to higher magnitudes. The sharp, secondary pressure pulses observed in the initial tests do not appear with the vessel liquid-full however, since the vessel apparently constrains significant amounts of water from being expelled from the core during the initial pressure burst.

Figures 14 and 15 present pressure data measured at the time of maximum power, $P(t_m)$, and the maximum pressure measured, $P(max)$, respectively, both as functions of the reciprocal period for initial system pressures of 100 and 375 psig and the vessel liquid-full. Over the range of periods investigated, the pressure at time of maximum power is observed to increase quite rapidly as the reactor period is decreased. It also is noted that the transient pressures are greater for the 100 psig system pressure tests than for the tests at 375 psig system pressure. This is probably a consequence of the higher steam generation rate prior to the time of maximum power at 100 psig than at 375 psig. The maximum measured transient pressures, $P(max)$, are observed to be of higher magnitude and to show somewhat similar behavior as that of $P(t_m)$ as a function of α_o in the range $\alpha_o \lesssim 10 \text{ sec}^{-1}$. For $\alpha_o \gtrsim 10 \text{ sec}^{-1}$, the $P(max)$ data at 100 psig system pressure begin to increase more slowly as a function of α_o . For excursions with $\alpha_o > 10 \text{ sec}^{-1}$ at 100 psig, the decrease in the rate of change of $P(max)$ as a function of α_o may be attributed to the appearance of partial film boiling in the central region of the core. A plausible explanation of the observed $P(max)$ data for $\alpha_o \gtrsim 10 \text{ sec}^{-1}$ is the presence of partial film blanketing which

Fig. 14 Pressure at time of maximum power vs reciprocal period.

Fig. 15 Maximum pressure vs reciprocal period.

would reduce the relative rate of energy transfer from the fuel plates to the water. This also is borne out by the 375 psig data in which the rate of change of $P(\max)$ vs α_0 appears to decrease for $\alpha_0 \gtrsim 12.5 \text{ sec}^{-1}$, although sufficient data are not available to establish this fact conclusively. It should be noted that the $P(\max)$ data for all tests with the system liquid-full were associated with the initial pressure pulse, the maximum pressure occurring slightly after the peak of the power burst. This is in contrast to the pressure behavior observed in the atmospheric pressure series in which $P(\max)$ occurred after the initial pressure pulse for $\alpha_0 \gtrsim 10 \text{ sec}^{-1}$ as a result of steam void collapse which created a water-hammer effect.

The preceding examples of transient behavior have presented data acquired with the reactor vessel liquid-full and with an expansion space in the pressurizer vessel. The presence of an expansion space in the reactor vessel has little or no effect on most of the variables measured during the initial power burst (eg, $\phi(t_m)$, $\theta(t_m)$, $E(t_m)$, etc) over the range of periods or reactivity insertions investigated. A pronounced effect is observed in the transient pressure behavior, however.

An example of the effect of an expansion space in the reactor vessel on the transient pressure is shown in Figure 16 in which $P(\max)$ is plotted as a function of initial system pressure for a series of nominal 100 msec tests both with and without an expansion space in the reactor vessel. The transient pressures observed were three to five times higher with the reactor vessel liquid-full compared to the case with an expansion space of several cubic feet available within the vessel. This difference is important with regard to plant safety since the integrity of the vessel and other structural components may be affected by the large magnitude of the transient pressure in the liquid-full case. On the other hand, the first evidence of limited core damage in the form of permanent deformation of fuel plates was observed for tests with initial periods of about 70 msec independent of both liquid level and system pressure. This would indicate that fuel plate deformation in the threshold region is primarily a consequence of the thermal

Fig. 16 Maximum transient pressure vs system pressure for 100-msec power excursions.

stresses caused by the temperature differentials associated with the onset of nucleate and film boiling.

The transient pressure peak in the liquid-full curve at about 75 psig system pressure, shown in Figure 16, is thought to be associated with the nature of the energy transfer from the fuel plates to the water. This curve would then indicate optimum transfer rates in the region of the peak ie, widespread nucleate boiling in the core. On the basis of this and the data from Figure 15, the peak would be expected to shift towards higher system pressures as the reactor period is decreased. Although sufficient data is not available for verification of this shift, the data shown in Figure 15 lend support to this argument since the 100 psig and 375 psig data converge at the shorter periods.

V. RESULTS OF NONBOILING REACTIVITY COMPENSATION CALCULATIONS

Calculations to determine the amount of reactivity compensated by non-boiling shutdown mechanisms have been performed on two of the elevated pressure Spert II transient tests. The results of these calculations are summarized and discussed in this section; Appendix B contains the details of the calculations. The calculational technique is similar to that applied by Garner [12] to tests in the Spert III reactor.

The effect of system pressure on the overall dynamic behavior of the reactor has been discussed in Section IV. These data indicated that the primary shutdown mechanisms were thermal in nature and that nonboiling reactivity feedback mechanisms contributed a significant amount to the self-limitation of super prompt critical power excursions, especially those conducted with maximum system pressure. The nonboiling shutdown mechanisms postulated include fuel plate expansion, conduction moderator heating, direct radiation heating, and axial fuel plate growth. The compensated reactivity due to each mechanism has been calculated by the use of static void and flux measurements in conjunction with dynamic temperature measurements and the total compared to the compensated reactivity determined by analysis of the power burst shape via the reactor kinetics equations [13].

The initial calculation was for a 375 psig, 263 msec test in which the inserted reactivity was 1.31\$. Saturation temperature for the moderator-coolant ($\approx 227^{\circ}\text{C}$) did not occur until well after the initial power peak, thus the reactivity compensated due to the postulated nonboiling mechanisms was calculated up to the time of peak power. The negative reactivity to time of peak power calculated by each of the postulated mechanisms is as follows: (a) fuel plate expansion, 4.94¢, (b) conduction moderator heating, 9.94¢, (c) direct radiation heating, $\approx 1\text{¢}$, (d) axial expansion of fuel plates, $\approx 1\text{¢}$. The total compensated reactivity calculated is then approximately 16¢. This may be compared to the compensated reactivity determined by analysis of the power burst shape to time of peak power of 43¢. In order to assure that the temperature distribution in the core was not underestimated, the energy release to time of maximum power was calculated using the temperature data and compared with the energy release determined by integration of the power burst. The total calculated energy release was 15.1 MW-sec which compares favorably with the energy release determined by integration of the power of 14.9 MW-sec.

The second calculation was for a 375 psig, 73 msec test in which the inserted reactivity was 2.36\$. In this case, saturation temperature for the moderator-coolant occurred in a portion of the core prior to the time of the initial power peak. In order to stay in a nonboiling region of the power excursion, the compensated reactivity was calculated to a point 85 msec before peak power. The reactivity compensation values obtained were: (a) fuel plate expansion, 5.37¢, (b) conduction moderator heating, 3.90¢, (c) direct radiation heating, ≈ 1 ¢, (d) axial expansion of fuel plates, ≈ 1 ¢. The total is approximately 11.3¢. Analysis of the power burst shape to 85 msec prior to peak power gave 25¢ compensated reactivity. The energy balance for this test gave 13 MW-sec from a temperature calculation compared to 12.6 MW-sec from integration of the power.

An attempt was made in the calculations for the two tests to arrive at results which were not based on any gross overestimates of the variables. The energy balance results indicate that the temperature data used were not unreasonable. Since vertical void importance data were not available, average void coefficients were used for the axial calculations. The vertical flux is, however, relatively flat in this core ^[7] and the use of local axial void coefficients would not be expected to significantly affect the results. The reactivity compensation calculated from the reactor power behavior has an uncertainty the order of 10% due to the uncertainty associated with the value of ℓ/β_{eff} for the system.

A comparison of the results of this analysis with experimental results indicates that calculations of the postulated reactivity compensation mechanisms account for less than one-half of the compensated reactivity determined by analysis of the power burst time history. Either additional physical processes must contribute to the self-limitation of power excursions in this system or the method of analysis must be somewhat inadequate. The in-pile temperature measurements have indicated that boiling did not occur prior to the time at which the calculations were made and even if a small amount of boiling did occur, it seems unlikely that a sufficient amount of steam formation could be present to account for the observed discrepancy. The discrepancy between experiment and calculation is similar to that observed by Garner ^[12] for the Spert III system, however, the difference is somewhat larger in the case of Spert II. Other reactivity compensating mechanisms have been postulated ^[14, 15]; however, experimental data are not available at this time to provide a quantitative estimate of the reactivity compensated by these mechanisms.

VI. SUMMARY AND CONCLUSIONS

The effect of system pressure on the kinetic behavior of a D_2O -moderated core in Spert II has been discussed in this report. As in the initial tests at atmospheric pressure, the basic mechanisms for shutdown appear to be thermal in nature. The primary effect of increasing the system pressure is a delay or suppression of boiling which causes noticeable changes in kinetic behavior for tests with initial asymptotic periods less than about 600 msec. For long period tests where steam does not contribute to shutdown at atmospheric pressure, increasing the system pressure has no effect on test results.

When boiling is delayed or suppressed by a higher system pressure, the shorter period tests exhibit a slight increase in peak power accompanied by a broadening of the burst and an increase in the energy release to the time of maximum power. For example, a 65-msec period test at 375 psig exhibits a maximum power about 50 percent higher and an energy release to time of maximum power about 100 percent higher than at atmospheric pressure. Fuel plate surface temperatures also are higher for the short period, pressurized tests with the data indicating that boiling does not contribute to self-limitation for periods longer than about 240 msec at 375 psig or for periods longer than about 350 msec to 100 psig. A similar dependence of the above mentioned variables on initial system pressure has been observed in the Spert III light-water tests; however, because of the long prompt-neutron lifetime of the D_2O system, the changes in kinetic behavior occur at periods nearly ten times longer and for reactivity insertions about twice as large as for the light-water tests.

Sizeable transient pressure pulses were observed for tests with initial periods shorter than about 200 msec and the reactor vessel liquid-full. The magnitude and time behavior of the pressure pulses generated were significantly affected by the presence of an expansion space in the reactor vessel; the magnitude being three to five times smaller and the rise time faster for tests with a gas dome in the reactor vessel as compared to tests with the vessel liquid-full. The first evidence of limited core damage was observed for tests with initial periods of about 70 msec independent of either liquid level or system pressure. The core damage, in the form of deformed fuel plates, is thought to be a consequence of thermal stresses caused by localized temperature differentials.

The nonboiling reactivity compensation calculations have indicated that the postulated nonboiling shutdown mechanisms, primarily thermal expansion of the fuel plates and water, account for less than one-half of the experimentally determined compensated reactivity. This analysis indicates either that additional physical processes must contribute significantly to self-limitation in the nonboiling region of power excursions, and/or that the analysis is inadequate. In any case, additional analytical and experimental work is needed to fully understand and isolate the individual shutdown mechanisms.

These tests have shown, however, that the delay or suppression of boiling by increasing the system pressure did not grossly impair the safety of this heavy-water system even though steam formation accounts for a sizeable fraction of the shutdown for periods less than about 600 msec at atmospheric pressure. Although boiling is important and perhaps necessary at very short periods, the elimination or reduction of steam formation does not necessarily result in a more dangerous condition since the nonboiling shutdown mechanisms are capable of quenching rather severe power excursions without serious damage to the core.

VII. REFERENCES

1. S. G. Forbes et al, Analysis of Self-Shutdown Behavior in the Spert I Reactor, IDO-16528 (July 23, 1959).
2. Spert Project Quarterly Technical Report, 2nd Qtr 1960, IDO-16640 (1961) pp 20-21.
3. L. A. Stephan and F. L. Bentzen, Transient Tests of the BSR-II Reactor in the Spert I Facility, IDO-16768 (April 1963).
4. A. H. Spano et al, Self-Limiting Power Excursion Tests of a Water-Moderated Low-Enrichment UO₂ Core in Spert I, IDO-16751 (February 28, 1962).
5. J. E. Grund, Self-Limiting Excursion Tests of a Highly Enriched Plate-Type D₂O-Moderated Reactor, Part I. Initial Test Series, IDO-16891 (July 1963).
6. C. R. Montgomery et al, Spert II Reactor Facility, IDO-16888 (August 3, 1963).
7. J. E. Grund et al, Nuclear Start-Up of the Spert II Reactor with Heavy-Water Moderator, IDO-16762 (April 20, 1962).
8. Quarterly Technical Report, Spert Project, 4th Qtr. 1961, IDO-16750 (1962) pp 8-11.
9. R. W. Garner, Review of Several Models for Power Behavior and Reactivity Feedback in Reactor Excursion Studies, IDO-16636 (February 6, 1962).
10. F. Schroeder and C. R. Toole, "The Effect of System Pressure on Room Temperature Power Excursions in Spert III", Trans. Am. Nuclear Soc., 3, 2(1960) p 434.
11. Quarterly Technical Report, Spert Project, 4th Qtr 1960, IDO-16687 (1961) pp 9-14.
12. R. W. Garner, An Analysis of Nonboiling Reactivity Feedback Mechanisms in Pressurized Power Excursions in the Spert III Reactor, IDO-16819 (January 18, 1963).
13. R. W. Miller, Calculations of Reactivity Behavior During Spert I Transients, IDO-16317 (June 1, 1957).
14. S. M. Zivi et al, Kinetic Studies of Heterogeneous Water Reactors, Quarterly Report for Period Ending 30 June 1961, Space Technology Laboratories, Inc., 8977-008-SU-000 (August 1, 1961) pp 1-16.

15. S. M. Zivi, "Gas Microbubble Dilation - A Reactor Shutdown Mechanism", Trans. Am. Nuclear Soc., 5, 1(1962) pp 161-162.
16. Reactor Projects Branch Quarterly Progress Report, 3rd Qtr 1958, IDO-16512 (May 6, 1959) pp 28-48.

APPENDIX A
SUMMARY OF EXPERIMENTAL DATA

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SUMMARY OF EXPERIMENTAL DATA

A summary of experimental data obtained during the self-limiting transient tests at elevated system pressure on the Spert II Expanded D₂O Core is given in Table A-1. The symbols used in Table A-1 have the following meaning:

<u>Symbol</u>	<u>Unit</u>	
No.	- - - -	Oscillograph record or transient number
P_i	psig	Initial system pressure
τ_o	sec	Initial asymptotic reactor period
α_o	sec ⁻¹	Reciprocal of the initial asymptotic reactor period
$\phi(t_m)$	MW	Maximum reactor power (initial power burst)
$E(t_m)$	MW-sec	Energy release to time of maximum power
ρ_o	\$	Reactivity insertion
$\rho_c(t_m)$	\$	Amount of reactivity compensation at time of maximum power
$\theta(t_m)$	°C	Maximum recorded fuel plate surface temperature at time of maximum power
$\theta(\max)$	°C	Maximum recorded fuel plate surface temperature
$P(t_m)$	psig	Transient pressure in central region of core (lattice position E 1-1) at time of maximum power
$P(\max)$	psig	Maximum transient pressure in central region of core (lattice position E 1-1)

TABLE A-I

SUMMARY OF EXPERIMENTAL DATA

No.	P _i (psig)	τ_o (sec)	$\alpha_o - 1$ (sec ⁻¹)	$\phi(t_m)$ (MW)	E(t _m) (MW-sec)	ρ_o ($\frac{lb}{ft^3}$)	$\rho_c(t_m)$ ($\frac{lb}{ft^3}$)	$\theta(t_m)$ (°C)	$\theta(max)$ (°C)	P(t _m) (psig)	P(max) (psig)
Reactor Vessel Liquid-Full; Expansion Space in Pressurizer											
02378	336	4.5	0.22	0.542	6.65	0.54	0.27	67	77	--	--
02407	373	1.3	0.77	2.40	6.88	0.83	0.26	82	128	--	--
02423	375	0.819	1.22	4.35	7.50	0.96	0.26	96	140	--	--
02429	374	0.399	2.51	13.4	10.0	1.10	0.33	239	260	--	--
02437	375	0.263	3.80	30.8	14.9	1.28	0.45	201	240	--	--
02500	373	1.51	6.62	76.7	22.5	1.62	0.73	229	256	5	12
02514	374	0.108	9.26	157	28.1	1.84	1.03	254	342	10	51
03077	375	0.104	9.62	164	30.3	1.93	0.96	240	329	4	46
02537	374	0.088	11.4	214	29.3	2.12	1.15	276	340	15	85
02599	374	0.088	11.4	227	30.4	2.12	1.20	284	364	12	80
02531	374	0.081	12.4	259	30.8	2.23	1.35	257	290	22	109
02525	374	0.073	13.7	302	32.9	2.36	1.43	311	311	24	113
03180	251	0.104	9.62	164	30.3	1.92	1.05	214	276	15	63
02672	100	0.504	1.98	10.6	10.7	1.04	0.30	153	224	--	--
02677	100	0.295	3.39	23.6	13.7	1.20	0.40	174	181	--	--
02735	101	0.197	5.08	51.7	18.7	1.38	0.56	194	215	5	17
02743	100	0.150	6.67	87.5	22.8	1.62	0.73	193	244	6	35
03189	100	0.106	9.43	159	26.1	1.86	0.94	251	267	13	68
02759	100	0.100	10.0	164	26.3	1.97	1.04	222	262	15	75
02782	100	0.091	11.0	198	27.9	2.08	1.10	280	314	22	87
02843	100	0.078	12.8	247	29.0	2.26	1.24	233	322	25	104
02881	100	0.069	14.5	297	29.4	2.44	1.41	230	350	38	120
02892	100	0.061	16.3	322	29.5	2.63	1.51	231	384	45	127
03230	75	0.103	9.71	170	29.5	1.95	1.02	240	280	13	71
03257	50	0.103	9.71	164	29.0	1.95	0.98	223	292	10	67
03263	25	0.098	10.2	153	21.4	1.95	0.91	237	287	15	63
03339	0	0.102	9.8	128	18.6	1.96	1.11	180	310	14	--
03322	0	0.096	10.4	140	20.2	1.96	1.05	181	308	13	49
03393	0	0.100	10.0	132	19.6	1.97	1.02	150	350	14	82
Expansion Space in Reactor Vessel and Pressurizer											
03536	375	0.107	9.35	162	27.9	1.90	0.92	249	300	1	16
03467	100	0.106	9.43	154	25.7	1.91	0.97	200	290	2	16
03462	50	0.102	9.8	159	24.9	1.96	1.01	180	244	4	11
03328	0	0.100	10.0	124	17.8	1.95	0.97	173	245	3	4
03333	0	0.102	9.8	112	15.9	1.95	1.09	175	359	3	5
03456	0	0.102	9.8	123	18.3	1.97	1.02	135	202	--	--
Expansion Space in Reactor; Pressurizer Liquid-Full											
03620	370	0.105	9.52	166	27.1	1.93	0.93	229	323	--	--
03565	50	0.107	9.34	161	25.3	1.91	0.98	187	232	--	--
03626	25	0.103	9.71	150	23.0	1.93	1.01	201	260	--	--
03546	0	0.101	9.9	128	19.1	1.97	1.04	152	179	--	--
Reactor Vessel and Pressurizer Liquid-Full											
03632	2	0.151	6.62	77	17.8	1.62	0.73	144	210	--	--
03638	2	0.101	9.9	139	20.6	1.97	1.13	193	236	--	--

APPENDIX B
DETAILS OF NONBOILING REACTIVITY COMPENSATION CALCULATIONS

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DETAILS OF NONBOILING REACTIVITY COMPENSATION CALCULATIONS

1. CASE I: $\tau_o = 263$ msec

This test had the following constants associated with it:

- $\rho_o = 1.31\%$ = inserted reactivity
- $\tau_o = 263$ msec = initial asymptotic period
- $\theta_i = 24^\circ\text{C}$ = initial system temperature
- $P_i = 375$ psig = initial system pressure
- $E(t_m) = 14.9$ MW-sec = energy release to $\phi(t_m)$
- $\rho_c(t_m) = 43\phi$ = compensated reactivity to $\phi(t_m)$.

Based on fuel plate surface temperature measurements at various points throughout the core, saturation temperature for the moderator-coolant ($\approx 227^\circ\text{C}$) did not occur until well after peak power in this test. This allows calculation of the amount of reactivity compensated up to time of peak power due to non-boiling mechanisms. Flow-meter and bulk-water temperature measurements indicate that the energy was contained within the fuel elements until well after peak power.

Four nonboiling reactivity compensating mechanisms have been postulated. These are (a) fuel plate expansion, (b) conduction moderator heating, (c) direct radiation moderator heating, and (d) axial growth of the fuel plates into the control rods. Spatial variations in the void coefficient of reactivity and in the fuel plate surface temperature have been taken into account by sectionalizing the core both radially and axially. The core was divided into six radial regions and twelve axial regions for a total of 72 volume elements. Figure 17 shows the radial regions ($r = 1, 2, \dots, 6$) and the axial regions ($i = 1, 2, \dots, 12$). In regions close to the fuel plate surface, where large temperature gradients exist, the nonlinear expansion of water with temperature has been included. A brief description of the calculations for each of the postulated mechanisms follows.

(1) Fuel Plate Expansion

The total reactivity compensated by fuel plate expansion as a function of time throughout the whole core may be expressed as:

$$\Delta\rho_p(t) = \sum_{r,i} C_{r,i} (V_o)_{r,i} \left[3 a \overline{\Delta\theta}(t) \right]_{r,i} \quad (1)$$

Fig. 17 Spert II lattice divided into radial and axial regions.

where

$C_{r,i}$ = local void coefficient for region (r,i)

$(V_o)_{r,i}$ = initial volume of the fuel plates in region (r,i)

$\overline{\Delta\theta(t)}_{r,i}$ = average temperature rise of the fuel plates at time t over region (r,i) of the fuel plates

a = linear thermal expansion coefficient of the fuel plates.

The void coefficient for this core has been measured radially but not axially. However, the axial flux is relatively flat (maximum to average ratio ≈ 1.2 , see Reference 7) compared to previous light water cores. An average void coefficient was therefore used axially along with the measured radial importance. The following coefficients were used.

Region (r)	$C_{r,i} (\phi/\text{cm}^3)$	Region (r)	$C_{r,i} (\phi/\text{cm}^3)$
1	-0.025	4	-0.017
2	-0.025	5	-0.019
3	-0.021	6	-0.012

All volume elements, $(V_0)_{r,i}$, have the same initial volume, ie

$$\begin{aligned}(V_0)_{r,i} &= V_0 = (2) (.060) (2.454) (16.39) (22) \\ &= 106.3 \text{ cm}^3.\end{aligned}$$

The linear thermal expansion coefficient (assumed constant) for the fuel plates is,

$$a = 24 \times 10^{-6}/^{\circ}\text{C}.$$

The average temperature rise at time of peak power over region r,i of the fuel plates, $\overline{\Delta\theta}(t_m)_{r,i}$, was calculated by fitting a maximum measured value of fuel plate surface temperature to the thermal neutron flux and extrapolating to all regions, (r,i) , of the core. A comparison of the calculated temperatures with some twenty measured temperatures throughout the core shows that the temperature follows the flux reasonably well aside from local fluctuations. Table B-I shows this comparison between calculated and measured temperatures where the maximum core temperature at time of peak power was measured to be 175°C .

Combining and summing the appropriate terms of Equation (1), the compensated reactivity over the core due to plate expansion is,

$$\Delta\rho_p(t_m) = 4.94\phi.$$

(2) Conduction Moderator Heating

The total reactivity compensated by conduction heating of the moderator-coolant as a function of time throughout the core may be expressed as,

$$\Delta\rho_w(t) = \sum_{r,i} C_{r,i} \left[1 - \frac{\rho(\theta_2)}{\rho(\theta_1)} \right]_{r,i,x} (V_{r,i,x}) \quad (2)$$

where

$C_{r,i}$ = local void coefficient for region (r,i)

$V_{r,i,x}$ = volume of water in region (r,i,x)

θ_2 = temperature of water at time t in region (r,i,x)

θ_1 = initial temperature of water in region (r,i,x)

$\rho(\theta_2)$ = density of water at temperature θ_2

$\rho(\theta_1)$ = density of water at temperature θ_1

TABLE B-I

COMPARISON OF MEASURED AND CALCULATED
FUEL PLATE SURFACE TEMPERATURES - CASE I

Thermocouple Location	$\Delta\theta(t_{m,r,i})$ ($^{\circ}\text{C}$)	
	Calculated	Measured
N3-2, -3E, #12	119	114
N3-2, 0E, #12	118	113
N3-2, +6E, #12	94	94
N3-2, -3E, #20	124	120
N3-2, -6E, #20	118	119
W1-2, -6N, #1	160	169
W3-2, 0N, #12	118	112
W3-4, 0N, #12	129	117
N1-2, -6E, #1	160	170
N1-2, +6E, #1	135	118
W1-2, 0S, #12	127	136
W1-2, -6S, #12	117	119
W1-4, -3N, #12	128	116
W1-4, 0N, #12	129	112
W1-4, -6N, #12	116	106
W1-6, 0N, #12	96	90
W5-2, -3N, #12	113	109
W5-2, -6N, #12	106	101
W3-4, -6N, #12 peen	117	117
W3-4, -6N, #12 weld	117	127

This calculation is based on the conduction of heat from the fuel plates into the adjacent water channels which results in a decrease in the density of the water. The spatial dependence of the temperature in the moderator-coolant may be obtained by solving the one-dimensional thermal diffusion equation. In order to develop a closed form solution, a closed form analytical expression for the fuel plate surface temperature as a function of time is needed as a boundary condition. A reasonably good fit to the experimental data is obtained by using the Two-Term Model ^[16] with r very large, ie,

$r \rightarrow \infty$. This results in an expression for the fuel plate surface temperature rise at or before the peak power of the form,

$$\Delta\theta(t) = \frac{e^{\alpha_0 t}}{2} (2 - \alpha_0 t) \Delta\theta(t_m) \quad (3)$$

where α_0 is the reciprocal of the initial reactor period. Figures 18 and 19 show experimental time behavior of two surface temperature measurements compared with that calculated by the Two-Term Model.

Fig. 18 Experimental and calculated (Two-Term Model) fuel plate surface temperature behavior for a thermocouple in the W1-2 assembly and an α_0 of 3.86 sec^{-1} .

Fig. 19 Experimental and calculated (Two-Term Model) fuel plate surface temperature behavior for a thermocouple in the N1-2 assembly and an α_0 of 3.86 sec^{-1} .

Solution of the one-dimensional thermal diffusion equation using Equation (3) as a boundary condition gives,

$$\Delta\theta(u, t_m) = \Delta\theta(0, t_m) e^{-u} \left(1 + \frac{u}{4}\right) \quad (4)$$

where

$$u = x \sqrt{\frac{\alpha_0}{d}}$$

x = distance from plate surface into water (cm)

α_0 = reciprocal period (sec^{-1})

d = thermal diffusivity of D_2O (cm^2/sec)

$\Delta\theta(0, t_m)$ = fuel plate surface temperature at time of maximum power ($^{\circ}\text{C}$)

$\Delta\theta(u, t_m)$ = temperature of D_2O at distance x at time of maximum power ($^{\circ}\text{C}$).

A plot of the function $\Delta\theta(u, t_m)/\Delta\theta(0, t_m)$ versus u is shown in Figure 20. This curve is indicative of the temperature distribution in the water. The region from $u = 0$ to $u = 6$ is broken up into 33 regions and the relative density change of each region calculated. The width of each region is,

$u = 0$ to 2 , $\Delta u = 0.1$ for $x = 1 - 20$,

$u = 2$ to 4 , $\Delta u = 0.2$ for $x = 21 - 30$,

$u = 4$ to 4.8 , $\Delta u = 0.4$ for $x = 31 - 33$,

$u = 4.8$ to 6.0 , $\Delta u = 1.2$ for $x = 33$.

The volume of each region, $V_{r,i,x}$, is then calculated as,

$$\begin{aligned} V_{r,1,x} &= 2.71 \text{ cm}^3 \text{ for } x = 1 \text{ through } 20, \\ &= 5.32 \text{ cm}^3 \text{ for } x = 21 \text{ through } 30, \\ &= 10.83 \text{ cm}^3 \text{ for } x = 31 \text{ through } 32, \\ &= 32.50 \text{ cm}^3 \text{ for } x = 33, \end{aligned}$$

with

$$\begin{aligned} d &= 1.46 \times 10^{-3} \text{ cm}^2/\text{sec}, \\ \alpha &= 3.86 \text{ sec}^{-1} \end{aligned}$$

$$\sqrt{\frac{d}{\alpha_0}} = 1.94 \times 10^{-2} \text{ cm}.$$

Fig. 20 Relative temperature distribution in the water adjacent to fuel plate surfaces as a function of the dimensionless parameter u for the Two-Term Model.

Summing over the respective products of Equation (2), the compensated reactivity over the core due to conduction heating of the moderator-coolant to time of peak power is,

$$\Delta\rho_w(t_m) = 8.94\phi.$$

(3) Direct Radiation Heating

Assuming the energy available from fission for direct radiation heating of the moderator to be 6 MeV from neutrons and 6 MeV from prompt gammas and assuming all the neutron energy goes into direct heating and that four-tenths of the gamma energy contributes to direct heating (a reasonable estimate based on electron densities), then the percentage of fission energy which goes into direct heating would be,

$$\begin{aligned} \% E(t_m)_{\text{direct heating}} &\approx \left[\frac{6}{174} + (.4) \frac{6}{174} \right] \times 100 \\ &\approx 4.8\% \\ &\approx 5\%. \end{aligned}$$

The energy release to time of maximum power, $E(t_m)$, obtained by integration of the reactor power for this test was 14.9 MW-sec. Therefore, the energy which goes into direct heating is approximately,

$$\begin{aligned} E(t_m)_{\text{direct heating}} &\approx (0.05) (14.9) \\ &\approx 0.75 \text{ MW-sec.} \end{aligned}$$

The energy coefficient for the core region based on uniform heating has been calculated to be approximately 1.2¢/MW-sec. The reactivity compensated by direct radiation heating of the moderator-coolant is then approximately,

$$\begin{aligned} \Delta\rho_w(t_m)_{\text{direct heating}} &\approx (0.75) (1.2) \\ &\approx 0.9\phi \\ &\approx 1\phi. \end{aligned}$$

(4) Axial Expansion of Fuel Plates

The fuel plates in a BD-22/24 fuel assembly normally rest on the lower end box and are not rigidly attached to the side plates. This means that thermal expansion of the fuel plates upward into the control rods must be considered as a possible reactivity compensating mechanism.

The average temperature rise of the fuel plates, $\overline{\Delta\theta}$, is approximately 125°C (slight overestimation). If α is the linear thermal expansion coefficient for the plates and l is the plate length, the maximum extension, δx , assuming the plates were free to expand in the upward direction with no constraint would be,

$$\begin{aligned}
 \delta x &= a \overline{\Delta \theta} \ell \\
 &= (24 \times 10^{-6}) (125) (24) \\
 &= 0.072 \text{ inch.}
 \end{aligned} \tag{5}$$

The fuel plates, however, are not free to expand without limit in the upward direction since they are constrained by the bail which is attached to the fuel can. A number of laboratory measurements using different fuel plates, bails, and fuel cans indicate that the free growth of the fuel plates is limited to about 0.002 to 0.003 inch. Any more extension than this amount would require stretching of the fuel can. What probably happens is that the fuel plates bend once they are constrained by the bail. Assuming a liberal estimate of 0.010 inch extension, the reactivity compensated would be approximately,

$$\begin{aligned}
 \Delta \rho_p(t_m)_{\text{axial exp.}} &\approx (0.010 \text{ inch})(100\phi/\text{inch}) \\
 &\approx 1\phi
 \end{aligned}$$

where the rod worth has been taken to be approximately 1.00\$/inch.

The total compensated reactivity calculated by the four postulated mechanisms is,

$$\begin{aligned}
 \Delta \rho_c(t_m)_{\text{calc.}} &= 11.95 + 8.94 + 1 + 1 \\
 &\approx 16\phi.
 \end{aligned}$$

This may be compared to the experimentally determined value of compensated reactivity of,

$$\Delta \rho_c(t_m)_{\text{exp.}} = 43\phi$$

which was determined by solving the reactor kinetics equations using experimental power data [13].

In order to assure that the temperature distribution in the core was not underestimated, the energy in the fuel plates and water at time of maximum power has been calculated for comparison with the energy obtained by integration of the power burst. Using the calculated fuel plate temperatures for each region, (r,i), the energy in the plates at time of maximum power is given by,

$$E_p(t_m) = \sum_{r,i} \overline{\Delta \theta}(t_m)_{r,i} (V_o)_{r,i} \rho(\theta)_{r,i} c_p(\theta)_{r,i} \tag{6}$$

where,

$\overline{\Delta\theta}(t_m)_{r,i}$ = average temperature rise of fuel plates at time of maximum power in region (r,i)

$(V_o)_{r,i}$ = initial volume of fuel plates in region (r,i)

$\rho(\theta)_{r,i}$ = temperature dependent density of fuel plates in region (r,i)

$C_p(\theta)_{r,i}$ = temperature dependent heat capacity of fuel plates in region (r,i).

Utilizing the best available data for the density and heat capacity, the energy in the plates is,

$$E_p(t_m) = 9.32 \text{ MW-sec.}$$

The energy in the water was determined by the relation,

$$E_w(t_m)_{r,i} = \sum_{r,i} \overline{\Delta\theta}(x, t_m)_{r,i} V_{r,i} \rho(\theta)_{r,i} C_p(\theta)_{r,i} \quad (7)$$

where,

$\overline{\Delta\theta}(x, t_m)_{r,i}$ = average temperature rise of water adjacent to fuel plates at time of maximum power in region (r,i)

$$= \frac{1}{6} \int_0^6 \Delta\theta(u, t_m) du$$

$V_{r,i}$ = volume of water in region (r,i) which is commensurate with that volume over which the average temperature was calculated

$\rho(\theta)_{r,i}$ = temperature dependent density

$C_p(\theta)_{r,i}$ = temperature dependent heat capacity.

Substituting the appropriate values into Equation (7) gives,

$$E_w(t_m) = 5.05 \text{ MW-sec.}$$

The total calculated energy release including the contribution from direct radiation heating is then,

$$E_T(t_m) = 9.32 + 5.05 + 0.75$$

$$= 15.1 \text{ MW-sec.}$$

This may be compared to the $E(t_m)$ value determined by integration of the power of,

$$E(t_m) = 14.9 \text{ MW-sec.}$$

2. CASE II: $\tau_o = 73 \text{ msec}$

This test had the following constants associated with it:

$$\rho_o = 2.36\%$$

$$\tau_o = 73 \text{ msec}$$

$$\theta_i = 25^\circ\text{C}$$

$$P_i = 375 \text{ psig}$$

$$E(-85) = 12.6 \text{ MW-sec}$$

$$\rho_c(-85) = 25\phi.$$

The fuel plate surface temperature measurements indicate that boiling did occur prior to the time of peak power in this test. In order to stay in a nonboiling region of the power excursion, the compensated reactivity was calculated up to a point 85 msec before peak power (hence the notation, (-85), rather than (t_m)). The calculations for each of the postulated mechanisms are described below.

(1) Fuel Plate Expansion

This calculation is similar to that of Case I, part 1. In this case, the maximum measured fuel plate surface temperature 85 msec prior to peak power was 190°C . A comparison of calculated temperatures with several measured temperatures is given in Table B-2, reasonably good agreement being attained.

Using the same $\bar{C}_{r,i}$, $(V_o)_{r,i}$, and a , Equation (1) gives,

$$\Delta\rho_p(-85) = 5.37\phi.$$

(2) Conduction Moderator Heating

This calculation is similar to Case I, part 2. In this case, however, the fuel plate surface temperature as a function of time is better described as a pure exponential rather than by the Two Term Model, or,

$$\Delta\theta(t) = \Delta\theta(-85) e^{\alpha_o t} \quad (8)$$

where t is negative.

TABLE B-2

COMPARISON OF MEASURED AND CALCULATED
FUEL PLATE SURFACE TEMPERATURES - CASE II

Thermocouple Location	$\Delta\theta(-85)_{r,i} (^{\circ}\text{C})$	
	Calculated	Measured
W1-2, -6N, #1	174	164
W3-4, ON, #12	139	126
W1-4, -3N, #12	139	132
W1-4, ON, #12	139	150
W1-4, -6N, #12	126	120
W1-6, ON, #12	104	103
W5-2, -3N, #12	127	128
W5-2, -6N, #12	114	122

Figures 21 and 22 show experimental time behavior of the surface temperature compared with that calculated by this expression.

Solution of the one-dimensional thermal diffusion equation using Equation (8) as a boundary condition gives,

$$\Delta\theta(u, -85) = \Delta\theta(0, -85) e^{-u}. \quad (9)$$

Fig. 21 Experimental and calculated (Exponential Model) fuel plate surface temperature behavior for a thermocouple in the N1-2 assembly and an α_0 of 13.7 sec^{-1} .

Fig. 22 Experimental and calculated (Exponential Model) fuel plate surface temperature behavior for a thermocouple in the W1-2 assembly and an α_0 of 13.7 sec^{-1} .

A plot of the function $\Delta\theta(u, -85)/\Delta\theta(0, -85)$ versus u is shown in Figure 23. Using the same relative width for each region as before, the volume of each region, $V_{r,i,x}$, is

$$\begin{aligned} V_{r,i,x} &= 1.44 \text{ cm}^3 \text{ for } x = 1 \text{ through } 20, \\ &= 2.99 \text{ cm}^3 \text{ for } x = 21 \text{ through } 30, \\ &= 5.76 \text{ cm}^3 \text{ for } x = 31 \text{ through } 32, \\ &= 17.27 \text{ cm}^3 \text{ for } x = 33. \end{aligned}$$

The other constants needed are,

$$d = 1.46 \times 10^{-3} \text{ cm}^2/\text{sec}$$

$$\alpha_0 = 13.7 \text{ sec}^{-1}$$

$$\sqrt{\frac{d}{\alpha_0}} = 1.031 \times 10^{-2} \text{ cm.}$$

Using the same $\bar{C}_{r,i}$ and summing over the respective products of Equation (2) gives,

$$\Delta\rho_w(-85) = 3.90\phi.$$

(3) Direct Radiation Heating

The energy release up to a time 85 msec before peak power was 12.6 MW-sec. Therefore, the energy which goes into direct heating is approximately,

$$\begin{aligned} E(-85)_{\text{direct heating}} &\approx (.05) (12.6) \\ &\approx 0.63 \text{ MW-sec.} \end{aligned}$$

then, the compensated reactivity as a result of direct radiation heating is approximately,

$$\begin{aligned} \Delta\rho_w(-85)_{\text{direct heating}} &\approx (0.63) (1.2) \\ &\approx 0.76\phi \\ &\approx 1\phi. \end{aligned}$$

(4) Axial Expansion of Fuel Plates

Fig. 23 Relative temperature distribution in the water adjacent to fuel plate surfaces as a function of the dimensionless parameter u for the Exponential Model.

This calculation is similar to that presented in Case I, part 4, or,

$$\Delta\rho_p(-85)_{\text{axial exp.}} \approx 1\phi.$$

The total reactivity calculated by the four postulated mechanisms is,

$$\begin{aligned}\Delta\rho_c(-85)_{\text{calc.}} &= 5.37 + 3.90 + 1 + 1 \\ &\approx 11.3\phi.\end{aligned}$$

This may be compared to the experimental value of,

$$\Delta\rho_c(-85)_{\text{exp.}} = 25\phi.$$

The energy balance for this test has been calculated using the same procedure as outlined in Case I, with (t_m) replaced by (-85) , and the two term prediction for surface temperature replaced by the exponential result. The energy in the plates and water is calculated to be,

$$E_p(-85) = 10.07 \text{ MW-sec}$$

$$E_w(-85) = 2.30 \text{ MW-sec}$$

with a total energy including direct radiation heating of,

$$\begin{aligned}E_T(-85) &= 10.07 + 2.30 + 0.63 \\ &= 13.0 \text{ MW-sec.}\end{aligned}$$

This may be compared to the $E(-85)$ value determined by integration of the power of,

$$E(-85) = 12.6 \text{ MW-sec.}$$

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