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**KINETIC STUDIES OF
HETEROGENEOUS WATER REACTORS**

AEC Research and Development Report
Annual Summary Report 1966

CONTRACT NO. AT(04-3)-372

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AEC RESEARCH AND DEVELOPMENT REPORT
UC-80, Reactor Technology

Annual Summary Report - 1966

KINETIC STUDIES OF
HETEROGENEOUS WATER REACTORS

December, 1966

by

Technical Staff

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1. INTRODUCTION AND SUMMARY

This is the final annual summary report of this project. It covers the reactor safety research work performed during 1966. This work falls into three general categories, first, destructive thermal pressure generation under reactor accident conditions, second, transient steam void growth and collapse, and third, power reactor instability and associated two-phase flow phenomena.

The work on thermal generation of destructive pressures under reactor accident conditions is presented in Section 2. This investigation was originally started to learn the mechanisms of the pressure pulses that destroyed the aluminum-plate reactors BORAX-I, SL-1, and SPERT ID during meltdown power excursions, and to learn the conditions under which similar pressures might occur in other reactors.^{1,2,3,4} It was shown in laboratory experiments that dispersal of molten aluminum into water can generate such pressures.⁵ It is known that partial fuel vaporization caused the high dispersal in BORAX-I and SL-1.^{4,5} SPERT ID did not reach vaporization temperatures, however, but our experiments have shown that the water impact pressure from vapor blanket collapse can produce the delayed dispersal and high pressures that occurred in that reactor.⁵ An alternative dispersal trigger mechanism has also been advanced.³ Current work has included investigation of whether similar in-core destructive pressures could result from the interaction of water and molten oxide fuel in a power reactor accident.

The first part of Section 2 on destructive pressures is a summary of present knowledge of the process of generation of transient thermal pressures by the interaction of water and hot solid or molten material. Pressures corresponding to the coolant vapor pressure at the temperature of a heated solid surface are generated in rapid thermal transients only under very special conditions. A large bubble or other free surface for pressure relief terminates thermal pressure generation after a short time by vapor

blanketing. Experiments have shown that, in the geometries of water reactors, multi-thousand psi destructive pressure pulses cannot be generated by rapid thermal transients in solid surfaces. On the other hand, impact of water upon molten aluminum in a laboratory shock tube experiment has produced multi-thousand psi pressures by violent dispersal of the molten metal into the coolant. Calculations from the measured pressure histories showed that about 10% of the total thermal energy of the molten aluminum (above 20°C) was converted into high pressure steam. In contrast only about 0.1% of the thermal energy in a thin solid surface went into high pressure steam in our millisecond transient boiling experiments. These results indicate that the thermal energy transfer and pressure generation in water from solid surfaces and from dispersed liquid surfaces are fundamentally different. Little is currently known about the pressure generation process with dispersed molten material.

Analysis has shown that the low thermal conductivity of UO_2 does not prevent transient heat fluxes from dispersal of molten oxide fuel into water that are sufficiently high to generate multi-thousand psi pressure pulses like those that occurred in our water-molten aluminum shock tube experiments and in BORAX-I, SL-1, and SPERT ID. The transient heat fluxes that can be obtained from molten UO_2 and from molten aluminum in the same geometry are comparable when account is taken of the large difference in the respective melting points.

The second part of the section on destructive pressure generation reports new results obtained in 1966 in our shock tube experiment. Previous results are given in our 1965 annual summary report, STL 372-30.⁵ In one shot, water impact upon 1000°C aluminum produced a pressure pulse that rose to 5800 psia before rupture of the crucible. This and other shots have shown that the water critical pressure of 3200 psi is not an upper limit to the pressure that may be generated thermally by the sudden dispersal of molten material into water. The present measurements confirm the previous result that the peak pressure from water impact upon molten aluminum increases very rapidly with increases in the aluminum pool temperature above melting.

An experiment was also performed with water impact upon molten silver that definitely eliminated the possibility of a significant chemical contribution to the observed pressure generation with molten aluminum. The silver results were similar to those with molten aluminum. This shows that the thermal generation of steam can produce the pressures observed with molten aluminum in the shock tube, and also the initial pressure spike in the SPERT ID destructive test when account is taken of pressure amplification.

Water impact upon powdered UO_2 at 800°C and 1200°C in the shock tube dispersed the UO_2 into the water, but produced pressures of only 240 psia and 320 psia, respectively. The transient heat flux obtainable from UO_2 is about one-fourth as great as that from aluminum at the same temperature. These very low pressures, therefore, are consistent with previous experiments that indicate that the heat flux into coolant from a liquid material is much greater than from a solid material and probably involves different mechanisms.⁵ It appears, therefore, that the low pressures observed here might well not occur in the interaction of water with very much hotter molten UO_2 .

The third part of Section 2 is an evaluation, on the basis of present knowledge, of the possibility that in-core destructive pressures might be generated in a power reactor by the interaction of water and molten oxide fuel during an accident involving rapid fuel melting. Comparable transient heat fluxes can be obtained from molten UO_2 and from molten aluminum, and definitive experiments on pressure generation from the interaction of water and molten UO_2 have not been performed. It appears at present, therefore, that one cannot safely conclude that destructive in-core pressures similar to those in BORAX-I, SL-1, and SPERT ID could not occur in power reactor accidents that involve rapid and extensive in-core melting. Such an accident might arise from loss of flow, a blocked channel, or from a large very rapid reactivity insertion, but not from loss of coolant with slow core meltdown where the molten oxide fuel might fall into an open pool of water.

We have been performing an in-pile capsule experiment in the KEWB reactor on transient steam void growth and collapse during millisecond heating transients.⁵ The 1966 experiments, described in Section 3, were

performed at elevated initial pressures in steps up to 1500 psig. The steam void behavior was qualitatively the same over this range of initial pressure. An initial nucleate boiling phase occurs followed by vapor blanketing with a large decrease in pressure below the initial value. A sharp water hammer impact pressure pulse occurs upon vapor blanket collapse, and this void growth and collapse cycle is repeated many times. At high initial pressure the period of each void growth and collapse cycle was considerably reduced from that at atmospheric pressure. The impact pressure from vapor-blanket collapse increased with initial pressure to a maximum at about 400 psia and then decreased at higher pressure. Water-hammer impact pressures up to 1000 psia were observed, but the pressures during the initial nucleate boiling phase were always less than 150 psia.

Section 4 of the report concerns power reactor instability and associated two-phase flow phenomena. The first part contains an analysis of the approach to instability in 100 Mw operation of the EBWR. This analysis is based on the hydrodynamics of A. B. Jones' STABLE-3 model which agrees well with the experimental data on hydrodynamic instability.⁵ It was found that the cause of the EBWR instability was a hydrodynamic resonance between the core's plate-type and rod-bundle fuel assemblies that produced a void reactivity feedback instability. The reactor would have been much more stable without the rod-bundle assemblies. This type of instability can also occur in boiling water reactors that have identical fuel assemblies if there is a sizable difference in the void response to power modulation in different regions of the core.

The results are given in Part 2 of our measurements and analysis of power-void transfer functions and steam void distributions in subcooled boiling at 300 and 700 psia. A description of the compact high-pressure loop, the measurement techniques, and the initial results of the experiment were given in STL 372-30.⁵ The principal conclusions from the experiment are as follows:

1. The void fraction increased linearly along the test section and was constant in the riser. This means that the bubble collapse time was long compared with the typical transport time of about 0.1 sec. The transfer function measurements confirm this.
2. Under typical operating conditions with 8°C inlet subcooling, about one-third of the power went into steam and the steam-water slip ratio was 0.8.
3. The power-void transfer function appears to be determined by void transport effects (with no bubble collapse) and the wall-coolant heat transfer time constant.

Part 3 of this section contains the experimental details and results of simultaneous measurements of the exit momentum flux from an atmospheric pressure boiling channel and void fraction distributions within that channel. Analysis of the experimental results led to a new model of the annular flow regime that incorporates velocity distributional effects required by the measured exit momentum flux from the channel. The flow model assumes that the annular liquid film is of uniform thickness, the liquid velocity in the film varies linearly with distance from the wall, the vapor velocity is uniform, and a fraction of the liquid is entrained in the vapor and travels at the vapor velocity. In applying the model to experimental measurements of void fraction, mass and momentum flow rates, power input, and subcooling, it was found within the experimental accuracy that the entrainment fraction was zero.

Several interesting conclusions were drawn from this analysis when applied to our data and that from ten other laboratories. This annular flow model predicts the momentum flow from the exit of the channel for all cases within experimental accuracy. It is shown that the flow parameter in the variable density formulation is a better correlating parameter than slip ratio for characterizing two phase flows. In annular flow, a linear relationship was observed between the flow parameter and void fraction.

2. DESTRUCTIVE PRESSURE GENERATION UNDER REACTOR ACCIDENT CONDITIONS

2.1 SUMMARY OF THE PROCESS OF TRANSIENT THERMAL PRESSURE GENERATION - R. W. Wright and L. B. Wentz

2.1.1 Pressures In Rapid Transient Boiling With Solid Surfaces

Under isothermal conditions in a one component two-phase fluid, such as steam and water, the system pressure is the vapor pressure of the liquid. For temperatures above the critical temperature, the pressure is determined by the single phase equation of state of the fluid. When there are temperature differences within a two phase system, the pressure in steady state is determined by equilibrium between evaporation and condensation along the vapor-liquid interface. In this case the system pressure will be intermediate between the vapor pressures of the hottest and coldest points of the interface.

When a fluid undergoes transient heating by another substance, the situation is much more complex. Only under certain special conditions is the transient system pressure approximately equal to the vapor pressure of the liquid at the temperature of the heated surface. These conditions were investigated in two experiments at TRW Systems in which a heated surface underwent large rapid thermal transients in a rigidly enclosed fixed volume of water that was initially at atmospheric pressure.^{5,6} In one of the experiments, the heated surface was a thin aluminum clad uranium disc that was contained in the reflector of the KEWB reactor and was fission heated by millisecond period power excursions. Pressure generation occurred with compression of the water in a small fixed volume, and steam generation was suppressed. The water volume was 1 cm^3 per cm^2 of the heated surface area. Thermal transients up to 600°C were applied to the surface in about 10 milliseconds. As shown in Figure 1, the pressure approximately followed the water vapor pressure corresponding to the disc surface temperature up to about 300°C .⁵ The pressure at this temperature was about 1400 psi, or about half the water critical pressure of 3200 psi. When the disc surface temperature rose on above the 374°C water critical temperature during a transient, the pressure actually decreased somewhat. This result indicates

Figure 1. Pressure Vs. Temperature in Fixed-Volume Capsule in KEWB Reactor Transients.
At Left - 3.9 msec Transient, 335°C Maximum Temperature.
At Right - 2.8 msec Transient, 490°C Maximum Temperature.

that, at supercritical temperatures, the heat transfer through the H_2O adjacent to the disc surface was sufficiently reduced that the conduction heat loss into the cold water exceeded the heat flux from the disc. The critical pressure of water could not be reached in these experiments. Similar results were achieved in an electrically heated experiment involving $700^\circ C$ transients in 120 microseconds.

Experiments were also performed in both these systems with an inertial load upon the capsule that represented about a three foot water head. In this configuration, the water could move away from the heated surface under the pressure developed in boiling as in most systems that have an accessible free surface. In this system, as would be expected, the pressures produced by transient boiling were much reduced. In the KEWB reactor experiment with the 10 millisecond heating period, the initial nucleate boiling pressures from a $500^\circ C$ transient were only about 50 psi, although in the 120 μsec electrically heated experiment they reached about 800 psi.^{5,6,7} In both cases the pressure peak was terminated by film blanketing of the heated surface as the water moved away from that surface.

As an example of the energy transfer cut-off caused by the occurrence of film blanketing, an oscillograph trace of a $450^\circ C$ transient in the KEWB capsule experiment with an inertial load is shown in Fig. 2. It is seen that, after a few milliseconds, the high heat transfer of the nucleate boiling phase is terminated and the modest pressure falls to a vacuum as the water moves away.^{5,6} While insulated by the vapor blanket, the plate temperature increased over $200^\circ C$. The water impact pressure pulses that occur upon the vapor-blanket collapse are also apparent. We have hypothesized that such a delayed water impact pressure pulse from vapor-blanket collapse in one of the hot channels in SPERT ID triggered the dispersal of the molten core aluminum into the core water. This dispersed molten aluminum produced the pressure pulse that destroyed the reactor.

When a sizeable air bubble was added to the electrically heated fixed volume capsule experiment, the water rapidly moved away from the heated surface into the bubble, so that the heated surface was vapor-

Figure 2. Sketch from Oscillograph Record of Temperature, Pressure, Void Volume, and Reactor Power for 5 msec Transient in KEWB Capsule with 30 gm/cm² Loading.

blanketed, very much as illustrated in Fig. 2.^{5,6} Thus, with an accessible pressure relief volume near the surface, the heat transfer was cut off almost immediately by vapor blanketing, and the sustained pressure resulting from the 700°C thermal transient was only a few psi.

It is known from the reactor power trace that about 12 liters of steam void were present in the SPERT ID core at the time the destructive pressure pulse occurred.^{3,5} However, this void volume did not cut off the heat transfer and pressure generation in SPERT ID as is attested to by the destructive pressure. Furthermore, pressurization of this large void volume to the pressures that occurred during the destructive test required that a major fraction of the thermal energy available in the core went into high pressure steam. In contrast, our experiments on transient heating of thin solid surfaces have shown that only of the order of 10^{-3} of the total thermal energy goes into high pressure steam.⁵

2.1.2 Effects Of Fuel Melting And Dispersal

The surfaces that produced the destructive pressure pulse in SPERT ID were not solid, and during the pressure generation process the molten core aluminum was highly dispersed. The hypothesis that a dispersed molten system is fundamentally different in transient heat transfer behavior from a system with solid surfaces was confirmed by the results of our shock-tube experiment. In this experiment, a water column impacted upon a pool of molten aluminum in simulation of the local vapor-blanket collapse in the SPERT ID core. The experiment showed that water impact violently disrupts molten aluminum and produces transient pressures that can account for the destructive pressure pulse in SPERT ID.^{5,8,9} An oscilloscope picture is shown in Fig. 3 of a 2900 psi pressure pulse obtained in the shock tube from the impact of water upon 950°C aluminum, and a graph of it is shown in Fig. 4. The pressure pulse had an 80 microsecond risetime, and a decay time constant of about 3 milliseconds. The dip after 1.2 milliseconds was caused by the arrival of the unloading wave from the top of the water column.

Figure 3. Oscilloscope Picture of Pressure History from Water Impact upon Molten Aluminum at 950°C. Bottom Sweep, 10 msec/Major Division, 3150 psi/Major Division. Upper Sweeps, 2 msec/Major Division, 840 psi/Major Division and 3150 psi/Major Division.

Figure 4. Graph of 2900 psia Pulse from Water Impact upon 950°C Aluminum with Crucible Intact

These past results and their application to SPERT ID are described in detail in STL 372-30, our 1965 Annual Summary Report.⁵

It is also known that dispersal of molten aluminum into the core water by partial vaporization of the fuel generated the destructive pressures in the BORAX I and SL-1 cores.^{1,2,4,5}

2.1.3 Physical Mechanisms Of Transient Pressure Generation

A volume expansion must occur in order to produce transient pressures in a fluid system (initially at rest), and the pressure increase produced by this volume expansion is determined by the back reaction of the fluid against the expanding volume. The nature of this back reaction is different depending on whether the volume expansion is fast or slow in comparison with the acoustic length of the system divided by the velocity of sound.

These considerations are most easily demonstrated by expansion in a one dimensional (slab) system. Let us consider the system of Figure 5, where pressure is generated by transient heating of the "fuel" on the left, which is connected to the "coolant" on the right by a "two phase" region of arbitrary thickness. Transient pressure is produced by the expansion of the two-phase region that results from transient heat transfer from the fuel. This expansion is measured in the figure by the coordinate x of the arbitrary interface between the "two-phase" region and the "coolant." (The arbitrary "two-phase" region must include the entire volume which

Figure 5. Drawing of One Dimensional Thermal Transient Pressure Generation Process. Heat from the Fuel Expands Two Phase Region Against Coolant to Produce Pressure.

undergoes expansion). The zero position of x is determined from the initial conditions preceding the thermal transient.

Consider first the case where the heating period τ is long compared with the acoustic period of the system $\frac{2L}{c}$, $\left(\tau \gg \frac{2L}{c}\right)$, where L is the distance to the nearest free surface and c and ρ are the sonic velocity and density of the coolant. The pressure change δP is then determined by inertial acceleration of the coolant load per unit area, ρL , so that:

$$\delta P = \rho L \ddot{x} \quad (\text{inertial case}). \quad (1)$$

If, on the other hand, $\tau \ll \frac{2L}{c}$, then the expansion is acoustic and¹⁰

$$\delta P = \rho c \dot{x} \quad (\text{acoustic case}). \quad (2)$$

A third case of interest is pressure generation in a rigid one dimensional "box" or fixed volume. Then

$$\delta P = \frac{x}{\beta L} = \rho c^2 \frac{x}{L} \quad (\text{compressible case}) \quad (3)$$

where β is the coolant compressibility. Our molten aluminum shock tube experiment has pressure pulse risetimes of about 100 microseconds.⁵ This would indicate that pressure generation by dispersal of fuel into coolant ordinarily would occur in the acoustic regime.

In order to maintain the pressure, sufficient energy flux must be supplied to maintain the rate of expansion of the two phase region (the rigid box case excepted). This net energy flux is the difference between

the energy flux into the two phase region from the hot fuel and the flux conducted out into the coolant. If the net energy flux into the two phase region cannot be maintained, the pressure will fall, and some of the hottest liquid near the fuel interface will flash into vapor. If this flashing reduces the energy flux into the two phase region, the pressure will drop further with further flashing in a degenerate process ending rapidly in vapor-blanketing. This may be the process that cuts off the thermal energy transfer in the KEWB capsule oscillograph trace of Figure 2. Whether it is the process that cuts off the thermal energy transfer and limits transient pressure generation by dispersed molten reactor fuel in coolant is not presently known. Another possibility is that freezing of the interface of the molten fuel droplets with the coolant materially reduces the heat transfer.

It is apparent that any effect that reduces the energy flux from the hot fuel into the two phase region will reduce the transient pressure, other things being equal. Such an effect is low thermal conductivity of the fuel, as occurs with UO_2 . It is also apparent that any effect which increases the energy flux out of the two phase region into the coolant will also reduce the transient pressure. The high thermal conductivity of sodium coolant should thus decrease transient pressures. High coolant subcooling will also have this effect, unless countered by some other change of mechanism in the two phase region.

The governing equations for transient pressure generation in spherical geometry for given volume expansion histories are somewhat different from the equations presented here for the plane wave case because of the divergent $\frac{1}{r}$ (radius) pressure propagation loss. The pressure generation process is qualitatively the same, however, and the derivations for the spherical case are given in Cole's book.¹⁰

The growth history of the vapor volume in the two-phase region can be calculated from the measured pressure history of a shot in our shock tube experiment, or from the pressure history in a reactor accident if it is known with sufficient detail. From the calculated volume history and

the measured pressure, the energy deposited in high pressure vapor during the transient can be calculated. For the shock tube pressure pulse shown in Figs. 3 and 4, the energy in high pressure steam was 10% of the total thermal energy in the molten aluminum in the crucible. This energy was calculated as of one millisecond after the start of the pressure pulse, because this is the estimated time available for the formation of the destructive pressure pulse in SPERT ID.⁵ This figure of 10% energy conversion into high pressure steam is to be compared with the fraction 10^{-3} which was obtained for pulsed heating of thin solid surfaces in our KEWB capsule experiment on transient steam void growth.⁶ These numbers illustrate the great difference between solid surfaces and dispersed liquid surfaces in the generation of destructively high transient pressures.

In a reactor accident (and in experiments with dispersed hot particles in coolant), more than one particle or surface is generating vapor, of course, and the source of the expansion against the surrounding coolant load (including some reactor structure) is the entire region containing the molten fuel and/or clad in the coolant. During the period of generation of the destructive pressure pulse, this region, which we call the pressurized volume, is a mixture of dispersed molten and frozen fuel and clad, liquid coolant, coolant vapor, and solid structure. Any pre-existing void volume is "pumped up" by vapor generation in the process of pressurizing the pressurized volume, and this void greatly reduces the pressure generated by a given mass of vapor. The pressurized volume expands as a "spherical piston" against the surrounding coolant load, which in general contains some reactor structure. Normally the expansion will be so rapid that the coolant load will be acoustic, and the pressure will be given, as in Equation 1, by¹⁰

$$\delta P = \rho c \dot{r} \quad (4)$$

where r is the radius of the pressurized volume which is taken to be

spherical. Very slow expansion will produce inertial loading as in Equation 2, but with modification for the spherical geometry.¹⁰ Until reflection occurs, the pressure decreases as $\frac{1}{r}$ as it propagates spherically outward into the coolant. This situation is illustrated in Figure 6 where a pressurized volume from melting in part of a reactor core is expanding into the coolant in the remainder of the core.

This general picture was applied to the analysis of the destructive pressure pulse in SPERT ID and the energy required to produce it.^{2,5,6} The SPERT case was complicated by the existence of peripheral voids in the core outside the initial pressurized volume which was the molten portion of the core. These peripheral voids isolated the tank water from the expanding pressurized volume until they were collapsed. The water-hammer impact from collapse of these outermost voids by the expanding pressurized volume produced in the tank water the sharp initial pressure spike of the destructive pressure pulse. This process is called pressure amplification.¹¹ The initial spike was followed by propagation of the actual pressures from the pressurized volume which lasted for many milliseconds.

Thus far, the only experiment on the pressures that can be generated by the dispersal of molten reactor fuel material into coolant has been in the TRW shock tube with molten aluminum and water. Experiments in which hot particles are injected into coolant through a free surface do not generate appreciable pressure because of the pressure relief afforded by that free surface. The shock tube experiment was undertaken in an attempt to understand what happened in SPERT ID, and it has shown that dispersal of molten aluminum fuel material into water can produce destructively high pressures. Whether or not this could also occur with low conductivity oxide fuel is considered next.

Figure 6. Schematic Drawing of Reactor Melting Accident with Transient Pressure Generation.

2.1.4 The Effect Of Low Conductivity Oxide Fuel

If it could be shown that the low conductivity of oxide fuel precluded sufficient heat flux into the two-phase region to produce destructively high pressures even with dispersal, then further experiments would not be required to determine whether destructively high pressures could be generated by the interaction of water and molten oxide fuel in credible power reactor meltdown accidents. The transient heat flux per unit area, $\frac{q(t)}{A}$, from a semi-infinite slab brought into sudden contact with a perfect conductor with a temperature difference ΔT is:

$$\frac{q(t)}{A} = \sqrt{\frac{k\rho c}{\pi t}} \Delta T \quad (5)$$

where k , ρ , and c are, respectively, the thermal conductivity, density, and specific heat. The total amount of heat transferred per unit area up to time t , $\frac{Q(t)}{A}$, is

$$\frac{Q(t)}{A} = 2\sqrt{\frac{k\rho c t}{\pi}} \Delta T \quad (6)$$

Both these quantities are plotted as a function of time in Figures 7 and 8 with the initial temperature difference ΔT as a parameter. The use of slab geometry is an approximation to the actual heat flux from UO_2 spheres. For 10 micron diameter spheres and a time of 100 microseconds, however, the overestimate is only a factor of 2, and this decreases rapidly with increasing particle size. There is also a possibility that convection within liquid drops of UO_2 could materially increase the heat flux from the drop.

Figure 7. Time Dependence of Heat Flux in Sudden Contact of UO_2 with a Perfect Heat Sink (One-dimensional). Initial Temperature Difference a parameter.

Figure 8. Time Dependence of Total Heat Transferred in Sudden Contact of UO_2 with Perfect Heat Sink (One-dimensional). Initial Temperature Difference a Parameter.

Figures 7 and 8 show that sufficient heat can be removed from molten drops of UO_2 to produce destructively high pressures when these drops are dispersed into water if a significant fraction of this heat goes into high pressure steam. For example, the 14 liter volume corresponding to a 100 microsecond acoustic path length in water could be pressurized to about 4000 psi by 1000 particles 1 millimeter in diameter (5gms) if all the heat removed from the UO_2 went into high pressure steam.

It has been demonstrated that the interaction of water and molten aluminum produces extensive dispersal and destructively high pressures. $\sqrt{k\rho c}$ is the governing parameter for transient heat transfer from a material, as shown in Equation (5), and this is about 1/4 as great for molten UO_2 as it is for molten aluminum. The very much higher temperatures of molten UO_2 can overcome this difference in thermal properties to give a transient heat flux from molten UO_2 that is comparable with that from molten aluminum. This comparison with the aluminum results shows directly that the UO_2 thermal properties themselves will not prevent the generation of destructively high pressures if molten UO_2 is widely dispersed into reactor core water. Determination of whether or not such destructive pressures would actually occur with molten oxide fuels awaits the performance of definitive experiments.

2.2 SHOCK TUBE EXPERIMENTS ON PRESSURE GENERATION - R. W. Wright, A. F. Firstenberg, and G. H. Humberstone

2.2.1 Introduction

Shock tube experiments have been continued on the generation of high pressures by the impact of water upon molten materials. Previous results on pressure generation from the interaction of water and molten aluminum were reported in our 1965 Annual Summary Report, STL 372-30.^{5,8} These experiments were performed in an attempt to understand the cause of the delayed pressure pulse that destroyed the SPERT ID (aluminum core) reactor, and to learn the conditions under which a similar event might occur.³ Experiments on rapid transient boiling had shown that vapor blanket collapse occurs about 10 milliseconds after the initial nucleate boiling phase, and that this collapse produces a water-hammer impact pressure pulse of about 100 psi under ambient conditions.^{5,12,13} We had hypothesized that this delayed mechanism dispersed the molten core aluminum into the water in SPERT ID, and a shock tube experiment was devised to simulate this process and to see whether this mechanism would disperse the molten aluminum into the impacting water and generate sufficient pressure to account for the SPERT ID results. The experimental results were affirmative. The continued experiments reported here are an attempt to understand the mechanism of pressure generation by the dispersal of molten reactor fuel into coolant sufficiently well to be able to predict the conditions under which destructive pressures might occur and the possible magnitude of these pressures. Experiments have been performed with hot UO_2 powder as well as with molten metal.

2.2.2 Experimental Details

The experimental apparatus used was developed from that described previously in STL 372-30, and is shown diagrammatically in Figure 9. The molten aluminum (or other material) is held in a 1 inch i.d. tapered crucible of molybdenum alloy (TZM) that is attached to a heavy water-cooled base plate. The crucible is heated by an oxygen-acetylene flame in a flowing nitrogen

Figure 9. Drawing of the Shock Tube Experiment

atmosphere. The pool temperature is measured by a platinum-rhodium or a tungsten-rhenium thermocouple. A 1 inch i.d. vertical water-filled pipe is clamped to the top of the base plate. A thin stretched rubber diaphragm initially separates the water column from the crucible. A Kistler 603A quartz pressure transducer with a 1.5 microsecond risetime is flush mounted in the side of the pipe near the diaphragm. The volume inside the crucible and the pipe volume on top of the water column are evacuated by a vacuum pump. The inside of the crucible is also connected to an evacuated water-filled flask at room temperature, so that the crucible volume contains about 1 cm Hg of water vapor which condenses on the advancing water column before impact, as in vapor blanket collapse. The water vapor pressure suppress boiling of the molten pool material or impurities. The presence of a non-condensable gas in the crucible, as occurs in the presence of a leak, produces a soft bounce rather than a sharp impact.

When the pool in the crucible has reached the desired temperature, the rubber diaphragm is ruptured, and the pressure history resulting from the water impact upon the pool is recorded on an oscilloscope. Unlike operations reported in STL 372-30, the rubber diaphragm is now ruptured by opening a large solenoid valve to the atmosphere. This produces a supersonic shock wave which is reflected from the top of the water column as a 50 psia pressure spike about 1 millisecond wide under typical operating conditions. The momentum imparted to the water column by this pressure spike ruptures the rubber diaphragm and produces a water impact pressure upon the pool of typically 200 psia. This pressure rupture of the diaphragm produces much more consistent results than the needle rupture previously used.⁵

2.2.3 Molten Aluminum Results

The results reported in STL 372-30 showed that the peak pressure generated by water impact upon molten aluminum increased very rapidly with aluminum pool temperature above the melting point from about 400 psia at 700°C to 2900 psia at 950°C.⁵ These results led to a hypothesis that only the sensible heat of the molten aluminum, but not the latent heat or solid-aluminum sensible heat are available to make high pressure steam and generate pressure.

Attempts to investigate the effects of higher aluminum pool temperatures have not been generally successful because of premature crucible rupture that has truncated the pressure rise. The highest recorded pressure was with 1000°C aluminum, and, at the time of crucible rupture, the pressure was at least 5800 psia. The charge amplifier for the quartz pressure transducer was saturated at this reading. The oscilloscope record of this shot is shown in Figure 10. The second from the top trace with a sensitivity of 2500 psi/major division shows the pressure pulse. The small dot on the spike in this trace is the amplifier saturation level. The sweep speed of this trace is 2 milliseconds/major division, so that the pressure risetime is about 100 microseconds. This particular shot shows that the water critical pressure of 3200 psia is not an upper limit to the pressures that can be generated thermally by the dispersal of molten material into water.

A picture of the ruptured crucible from this 5800 psia shot is shown in Figure 11, with most of the aluminum residue inside. The crucible is resting upon the water-cooled base plate to which the upper lip of the crucible was brazed and is still attached. The gross failure of the crucible under this sharp high pressure pulse is apparent. The failure region was the thinned-down neck of the crucible which furnished the thermal barrier between the hot crucible base and the water-cooled base plate. The simple hoop stress in the .040" thick wall here was 140,000 psi at the peak pressure, and there was additional bending stress.

The shape of a high pressure pulse resulting from the impact of water upon molten aluminum when the pulse is not prematurely truncated by crucible failure was shown by the graph of Figure 4 of part 2.1. This 2900 psia pressure pulse, which resulted from water impact upon 950°C aluminum had an 80 microsecond risetime and a time constant for pressure decay of about 3 milliseconds. The notch after about one millisecond was the passage past the pressure transducer of the unloading wave from the top of the water column.

Figure 10. Oscilloscope Picture of the 5800 psia Pressure Pulse from Water Impact upon 1000°C Molten Aluminum with Crucible Failure. Calibrations are, from the top, 500 and 2500 psi/major division at 2 m sec/major division, and 25 and 5000 psi/major division at 10 m sec/major division

Figure 11. Picture of Ruptured Crucible, Residue,
and Water-Cooled Base Plate after 5800 psia
Shot

A few shots were made that gave extraneously low values of the peak pressure. In many of these shots, one oscilloscope trace monitored the diaphragm rupture and pre-impact period with a 25 psia/major division sensitivity. In all these "anomalously low pressure" shots in which the impact was thus monitored, there was a "precursor" pressure before the impact. Our interpretation of this phenomena is that a slow diaphragm rupture occurred in these cases such that a sizable jet or stream of water sprayed the molten aluminum pool before impact and made sufficient steam to produce a soft impact, low dispersal and low pressure. The presence of this jet as evidenced by extraneous pressure pulses is shown in the third from the top trace in Figure 12, which is a shot in which impact upon 1000°C aluminum produced only 400 psia. A normal shot is illustrated by the 1000°C, 5800 psia trace of Figure 10, in which the approach to impact in the third trace from the top is quite clean and free of pressure spikes from the extraneous jet effect. (The sharp pressure fall to vacuum after the initial slow pressure buildup is due to diaphragm rupture.) Shots with diaphragm rupture by a magnetically driven needle, as described in STL-372-30, were much more subject to irregularities from this jet effect than those with the pressure rupture now used.

The experimental data on peak pressure versus aluminum pool temperature are shown in Figure 13. The extraneously low pressure shots are not included in Figure 13. Pressures that occurred in shots with crucible failure are marked by an $\uparrow$, and these points are a lower limit to the pressure that would have been generated had crucible failure not occurred. There is considerable scatter in the data. The additional new data do verify the general result reported in STL 372-30 that the peak pressure produced by water impact upon molten aluminum increases very rapidly with aluminum pool temperature above melting. It appears from this experiment that only the sensible heat of the molten aluminum but not the latent heat of fusion or the sensible heat of the solid aluminum are available to generate high pressure steam and pressure. It is not known why this should be so. It is not known whether this rapid increase of peak pressure with molten aluminum temperature continues on above 5800 psia.

Figure 12. Oscilloscope Picture of Pressure History from Water Impact upon 1000°C Aluminum with a Large Precursor Water Jet. Calibrations are, from the top, 250 and 1000 psi/major division at 2 m sec/major division, and 25 and 2500 psi/major division at 10 m sec/major division. The Third Trace Shows the Water Jet Precursor Pressure Pulses

(Picture backwards. Time goes from right to left.)

Figure 13. Temperature Dependence of the Peak Pressure from Water Impact upon Aluminum or Silver. Arrows Indicate Shots where Pressure Rise was Terminated by Crucible Failure

2.2.4 The Possible Contribution of Chemical Energy Release to Pressure Generation

Chemical analysis of the SPERT ID core debris for Al_2O_3 showed that about 2% of the molten core aluminum had reacted chemically with the water.³ The chemical energy release was about 15% of the nuclear energy release in SPERT ID and also in SL-1.^{2,3} On the basis of the experiments of his group at ANL, however, Baker has concluded that the chemical reaction rates are too low to have contributed significantly to the destructive pressure pulse in SPERT ID.¹⁴

From the pressure history measured in the shock tube (as shown in Figure 4, for example), one can calculate the steam volume history and therefore the energy in high pressure steam required to produce that measured pressure history. This required energy is a function of time, and for a 3000 psi pressure pulse in the shock tube it is about 10% of the total thermal energy in the molten aluminum after one millisecond.⁵ This is the calculated magnitude of the time required for the destructive pressurization of the partially voided SPERT ID core.⁵ This 10% energy transfer into high pressure steam is very high, about two orders of magnitude greater than the fraction of the thermal energy transferred into high pressure steam in our millisecond period transient boiling experiments with thin solid surfaces. Thus there has been some question as to the relative contribution of thermal and chemical energy in generation of the destructive pressure pulses in the shock tube experiment and in SPERT ID.

Hydrogen from the aluminum-water chemical reaction was collected above the water column in the shock tube after several shots. There was some uncertainty about the collection efficiency, but the hydrogen measurements showed that the fraction of the aluminum undergoing chemical reaction increased with aluminum pool temperature (and hence pressure) to 0.25% at 850°C in a 2200 psia pulse. The volume of hydrogen produced was an order of magnitude too low to account for the volume expansion required to produce the measured pressure history. (In a steam-aluminum chemical reaction, hydrogen replaces steam mole for mole for no net volume increase.) However, the chemical energy released was sufficient to account for the measured pressure history if a major fraction of it produced high pressure steam

and if most of the chemical reaction in this dispersed system with much fresh molten aluminum surface occurred within about a millisecond.

In order to establish definitely whether or not chemical energy release was contributing significantly to pressure generation with molten aluminum, an experiment was performed in the shock tube with molten silver which is chemically non-reactive with water. Water impact produced high dispersal of the molten silver into the water and gave pressures comparable with those from aluminum. With the silver at 1130°C, 170°C above melting, the pressure rose to 3200 psia in about 100 microseconds before crucible failure terminated the pressure pulse. This data point is shown in Figure 12 for comparison with aluminum on the basis of the temperature increment above melting.

The pressure generation process with silver was definitely thermal and not chemical. This shows that thermal processes can account for the comparable pressures generated by water impact upon molten aluminum and also for the initial sharp pressure spike in SPERT ID when account is taken of pressure amplification. This result does not mean that chemical energy release did not contribute significantly to the sustained pressurization of the SPERT ID core which lasted for at least 8 milliseconds.^{3,5}

2.2.5 UO₂ Powder Results

Several runs were made in the shock tube in which the water column impacted upon hot powdered UO₂. The 180 psia water impact dispersed the UO₂ powder into the water, but peak pressures of only 240 psia and 320 psia were produced at UO₂ temperatures of 800°C and 1200°C, respectively.

Oscilloscope records of these runs are shown in Figure 14, with the 800°C shot at the top and the 1200°C shot at the bottom. The top oscilloscope traces which show the resultant pressure pulses best have a sensitivity of 250 psia/major division and a sweep speed of 2 milliseconds/major division. Besides the order of magnitude lower peak pressures than those obtained with molten metal, these pressure peaks have an order of magnitude slower risetime.

Figure 14. Oscilloscope Pictures of Pressures from Water Impact upon Hot UO_2 Powder. Top Picture, 800°C UO_2 . Bottom Picture, 1200°C UO_2 . Calibrations for both are, from the top, 250 and 1000 psi/major division at 2 msec/major division, and 25 and 2500 psi/major division at 10 msec/major division

Several experimental effects make these powdered UO_2 shots less clear cut than the molten metal ones. The UO_2 powder had only about one-third of theoretical density. Following initial contact by the water column, a period of "compaction" of the UO_2 powder occurred at a pressure of a few psia that lasted for several milliseconds before the real impact pressure and dispersal began. This is shown by the third from the top traces in both pictures which have sensitivities of 25 psia/major division and sweep speeds of 10 milliseconds/major division. Shots with cold powdered UO_2 , not shown here, illustrated this phenomenon more clearly. Compliant open space may well still exist in the UO_2 powder after the compaction process, and this would decrease the pressure generated by a given mass of steam. The other uncertainty is the actual temperature profile across the UO_2 powder. The thermocouple measured the temperature near the crucible bottom. Observation during heating with a quartz plate across the crucible mouth showed that the powder surface was definitely darker (and cooler) than the crucible bottom.

Even with the above experimental uncertainties, the pressures obtained from water impact upon the UO_2 powder are very low in comparison with the results with molten metal. However, as discussed previously, the transient heat flux obtainable from UO_2 is about one-fourth that obtainable from aluminum at the same temperature. The UO_2 powder used here is also much finer than the dispersed aluminum or silver residue from a shock tube shot (or from SPERT ID). This would indicate that considerably greater heat fluxes and hence transient pressures can be obtained from molten material dispersed into coolant than from a powder. This appears to be a fruitful area for further investigation of the process of destructive pressure generation. In addition, it does not appear that one can safely conclude at present that the very low pressures obtained here from water impact upon hot powdered UO_2 and also in the SPERT I Oxide Core destructive tests would occur if the UO_2 were molten.

2.3 THE POSSIBILITY OF DESTRUCTIVE IN-CORE PRESSURES IN POWER REACTOR MELTDOWN ACCIDENTS - L. B. Wentz, R. W. Wright, and S. M. Zivi

An important area of uncertainty in reactor safety is the possible consequence of a water reactor accident in which substantial amounts of molten fuel come into contact with reactor coolant. The possibility that in-core destructive pressures may be thus generated is examined here on the basis of presently available knowledge. If large in-core pressures do occur, the resulting extensive core damage might prevent control rod insertion and might also prevent subsequent effective operation of emergency core cooling systems. The integrity of the primary coolant system might also be breached. Although the definitive experiments have not been performed, the available information summarized here indicates a strong possibility that large in-core pressures would be generated by core melting when the primary coolant system is intact.

Accidents which can give rise to in-core destructive pressures are to be distinguished from accidents with slow meltdown from decay heat following loss of coolant. In the latter case, one relevant question is whether molten fuel will fall into a pool of water and generate sufficient steam pressure to breach the containment. In the accidents considered here, however, the reactor vessel and piping are intact when the fuel melting occurs. Such fuel melting accidents might be caused by control rod ejection or other large rapid reactivity insertion, or possibly by loss of coolant flow or a blocked coolant assembly.

The possibility of in-core destructive pressures from the interaction of molten reactor fuel and water in these accidents is indicated by a considerable amount of evidence which may be summarized as follows:

1. All three water reactor events in which extensive core melting occurred have produced multi-thousand psi pressure pulses. These events occurred in BORAX I, SL-1, and SPERT ID, with resulting pressures as shown in Figure 15.^{1,2,3,4,5} Each event involved an aluminum-uranium plate core in water which was at ambient temperature and pressure at the time of the incident.

Figure 15. Magnitudes of Pressure Pulses Observed in Water Reactor Incidents and in Laboratory Experiments

2. In each of the three cases the experimental or accidental event was initiated by a power burst on a millisecond time scale resulting from a reactivity increase to above prompt critical.
3. In the SL-1 accident the major damage was produced by water-hammer impact upon the head of the pressure vessel, not by the actual pressure pulse generated in the core by meltdown.^{2, 4}
4. After each event the fuel which had been melted was highly fragmented and dispersed. In BORAX I and SL-1, partial fuel vaporization aided the dispersal.^{1, 2}
5. Chemical analysis of the core debris from SL-1 and SPERT ID showed that the aluminum-water chemical reactor augmented the nuclear energy release by about 15%.^{2, 3} (The BORAX I debris was not analyzed.) It is generally accepted, however, that chemical reaction rates are too low to have contributed significantly to generation of the destructive pressure pulses.¹⁴
6. In the absence of a rapid and energetic chemical reaction, the mechanism of destructive pressure generation is steam generation by rapid heat transfer.
7. Experiments have demonstrated that sudden contact between molten aluminum and ambient temperature water in reactor geometries can generate multi-thousand psi pressure pulses.^{5, 8} These high pressures occur if and only if the aluminum is molten. The pressures generated in these experiments are consistent with those observed in the SPERT ID destructive test, and resulted from violent dispersal of molten aluminum into water. Similar results have also been obtained with molten silver which is chemically inert in water.
8. Pre-existing void in the core at the time of dispersal and destructive pressure generation introduces compliance into the system so that much greater energy is required to generate a given pressure.⁵ On the other hand, collapse of peripheral voids

under a central driving pressure gives a water-hammer type pressure amplification. In SPERT ID this process produced the initial large pressure spike from a lower and more slowly rising pressure generated in the molten region of the core, as indicated in Figure 15.⁵

With regard to the oxide fueled power reactor, no positive conclusion can be stated at present because the results obtained with metal cores are not directly applicable and because definitive experiments have not been performed. However, the following evidence suggests that a power reactor accident with rapid meltdown may produce results similar to the aluminum core meltdown events:

1. There is ample energy available in a power reactor meltdown accident. Multi-thousand psi destructive pressure pulses are to be expected if dispersal of the oxide fuel into the core water converts thermal energy into mechanical energy with an efficiency comparable with that for molten aluminum.
2. Fragmentation of molten oxide fuel in TREAT capsule meltdown experiments has demonstrated that dispersal of molten oxide fuel into water can occur.¹⁶ The fragmentation was greater in runs that exceeded the vaporization threshold than in less energetic ones.¹⁷ Since the capsules used in these experiments contained a sizeable gas pocket which furnished pressure relief, sharp destructive pressure pulses could not be generated.
3. The possible transient thermal energy flux from molten oxide fuel is comparable with that from molten uranium-aluminum fuel, as discussed previously in Section 2.1.

The two SPERT I oxide core destructive tests did not melt the fuel and did not produce dangerous pressures.¹⁵ In each of these events, powdered UO_2 at about 600°C was dispersed into water by the rupture of two water-logged fuel pins. A power reactor accident with extensive fuel melting could present a much more dangerous situation by virtue of (1) the higher temperature fuel, since 2800°C is the melting point of UO_2 , (2) the molten state of the fuel, and (3) the larger quantities of fuel potentially available in a large meltdown accident.

The following uncertainties exist in a current evaluation of the pressure generating potential of the in-core interaction of water with molten oxide fuel.

1. Can large quantities of molten oxide fuel contact water quickly in credible power reactor accidents? The possibility must be considered of a local in-core pressure disturbance that can trigger large scale dispersal and contact of molten fuel and water, as in SPERT ID.
2. What is the range of conditions under which the contact of molten oxide fuel (and clad) with water results in extensive dispersal and rapid enough steam generation to cause in-core destructive pressures.

In summary, destructive pressure pulses were produced by the dispersal of molten reactor fuel into coolant in all three water reactor meltdown events. Molten oxide fuel can fragment and disperse into water. Energy transfer rates from oxide fuels can be comparable with those from molten aluminum. This suggests that in-core destructive pressures may be generated in power reactor accidents that produce extensive core melting.

3. IN-PILE CAPSULE MEASUREMENTS OF TRANSIENT STEAM-VOID FORMATION - L. B. Wentz

The capsule experiment has been described in detail in our 1963 Annual Summary Report (STL-6312)⁹ and considerable data were presented in our 1964 and 1965 Annual Summary Reports (STL 372-13 and STL 372-30, respectively).^{5,6} The purpose of the experiment is to obtain transient steam-void formation and pressure pulse data which are applicable to short-period water reactor power excursions.

The capsule contains an aluminum-clad uranium - 10 w/o molybdenum fuel disc which is immersed in water. A sketch of the capsule and appurtenances is shown in Figure 16 along with a table of important dimensions. The capsule is placed in the reflector of the KEWB reactor and the disc is fission heated by neutron bursts with reactor periods as short as one millisecond. The reactor power (proportional to power in the fuel disc), fuel disc surface temperature, pressure, and steam-void volume are measured as a function of time. High speed motion pictures are taken of one face of the disc during each transient. The experiment modeling considerations are described in earlier reports.^{6,18}

The experimental parameters are fuel enrichment, reactor period, piston inertial loading (representing hydrostatic head above a reactor core), initial temperature (subcooling), and initial pressure.^{5,6,18} Data have been obtained for a range of reactor periods between 1 and 80 milliseconds with most of the data between 2 and 6 millisecond periods. The effects of hydrostatic loading have been investigated up to 230 gm/cm² and subcooling has been investigated in the range of 15°C to 80°C.

Previous to this year the investigation of the effects of initial pressurization has been limited to a maximum of 100 psig due to inadequacies in the O-ring piston seal. A double rolling diaphragm piston seal system was developed to give a positive seal at all pressures above about 50 psia. This seal system is illustrated in Figure 17 and replaces the area around the piston shown in Figure 16. The 100 psi relief valve maintains the

Figure 16. In-Pile Capsule Configuration, Longitudinal Section View

Figure 17. Sketch of Rolling Diaphragm Piston Seal System

pressure between the two rolling diaphragms at 100 psi less than in the pressurization and fuel chambers in order to prevent diaphragm reversal and binding. It was found that at least 50 psi pressure drop across the diaphragms was necessary for free piston movement. The effects of initial pressurization up to 1500 psia are described in this report.

Temperature, pressure, steam-void volume (dilation), and power histories which are typical of those observed during a 5 millisecond period transient with ambient initial temperature and atmospheric initial pressure and with a 30 gm/cm^2 piston loading are shown in Figure 18. As the disc temperature rises during the reactor power excursion, the pressure rises, first due to thermal expansion and then due to nucleate boiling (Pressure 1).^{*} When nucleate boiling progresses into film boiling (Pressure 2) and a vapor blanket forms, the pressure drops to a constant partial vacuum (Pressure 3) (about 4 psia). The void volume, after the initial pressure pulse, follows a nearly parabolic history (Dilation 1,2) due to the constant deceleration pressure upon the inertial load. When the void collapses (Dilation 3) a water-hammer impact pressure pulse occurs (Pressure 4). The water then rebounds, a vapor blanket forms, the pressure drops again to 4 psia and the parabolic history repeats through several impacts until the energy is dissipated. The void history following the initial pressure pulse is determined by the piston loading, the impulse it has received, and the pressure difference across the piston during the period of vapor blanketing. Thus, the impulse, peak pressure, and rate of void growth immediately following the initial pressure pulse (which are not independent) appear to be the important phenomena.

* (Pressure 1) refers to the number 1 on the pressure history in Figure 18 and indicates the time of occurrence of the phenomenon which is being described

Figure 18. Sketch of Temperature, Pressure, Dilation, and Reactor Power Histories for a 5 ms Transient with 30 gm/cm² Piston Loading and Atmospheric Initial Pressure

Temperature, pressure, steam-void volume, and power histories measured during a 3 millisecond transient with 120 psia initial pressure, ambient initial temperature, and 36 gm/cm^2 piston loading are shown in Figure 19. Comparison of these data with those of Figure 18 reveals the general trends due to initial pressurization.* Qualitatively, the behavior is quite similar, with the same void formation, growth, and collapse process at all initial pressures. In the pressurized run the period of each growth and collapse cycle is shorter and the maximum void volume is lower due to the increased deceleration pressure. The peak temperatures are lower due to increased heat transfer to the water prior to film boiling. The water hammer impact pressures are higher due to higher impact velocities which result from the increased initial impulse. There are more growth and collapse cycles in Figure 19 than in Figure 18, but this is primarily due to the higher total heat input and lower heat removed per cycle in the pressurized run.

Data are shown in Table 1 for runs at various initial pressures with reactor periods of 5, 4, and 3 milliseconds, and piston loadings of 230, 120, 36, and 30 grams/cm². The data are shown in two groups because the two different discs gave rise to different heat source strengths, and there are small differences in friction between the O-ring seal and the diaphragm seal.

The magnitude of the first pressure pulse increases with loading and decreases with reactor period. It also increases with initial pressure to a maximum at about 380 psia initial pressure and then appears to decrease slowly. The duration of the first pressure pulse is nearly independent of initial pressure, so that the impulse given to the

*The numbers on the two figures refer to the occurrence of similar phenomena

Figure 19. Sketch of Temperature, Pressure, Dilation and Reactor Power-Histories for a 3 ms Transient with 36 gram/cm² Piston Loading. and 120 psia Initial Pressure

TABLE 1. PRESSURE AND VOID DATA FOR RUNS AT VARIOUS
INITIAL PRESSURES AND AMBIENT INITIAL TEMPERATURE

INITIAL PRESSURE	REACTOR PERIOD	PISTON LOADING	1st PEAK PRESSURE	1st PRESSURE MINIMUM	MAGNITUDE OF WATER HAMMER PRESSURE				1st VOID MAXIMUM	MAX. VOID GROWTH RATE	ADIABATIC PEAK TEMPERATURE
					PULSES	psi					
psia	ms	gm/cm ²	psia	psia	1st	2nd	3rd	4th	cm	cm/sec	°C
O-RING SEAL			DISC A								
15	5.0	30	45	5	100	100	100	100	.008	2.1	500
15	4.9	120	65	5	180	190	190	180	.013	1.7	500
15	4.9	230	75	5	150	160	120	70	>.03	2.8	500
110	5.0	30	170	50	170	160	160	160	.007	3.4	500
100	5.0	120	190	20	370	370	370	360	.012	3.2	500
100	5.0	230	200	30	940	750	590	460	>.03	4.7	500
DIAPHRAGM SEAL			DISC B								
100	4.9	36	150	30	180	230	140	90	.007	4.4	500
120	4.0	36	190	50	340	370	380	340	.008	6.1	570
120	3.0	36	210	50	220	310	310	370	.006	5.4	660
190	5.2	36	250	100	380	560	490	450	.002	1.8	490
310	5.0	36	380	190	330	420	340	280	.008	4.1	500
380	5.2	36	450	200	630	590	620	400	-----	-----	490
380	4.2	36	480	170	720	650	510	400	.004	7.5	550
380	3.0	36	520	130	740	560	450	400	.024	8.3	660
1000	4.2	36	1080	690	670	560	490	410	.011	6.4	550
1000	3.0	36	1130	700	570	490	420	390	.008	8.3	660
1000	2.9	36	1120	670	600	560	520	500	.009	6.4	670
1500	3.0	36	1580	1340	280	220	210	200	.004	4.7	660

piston and therefore also the maximum void growth rate have about the same dependence on initial pressure as does the magnitude of the first pressure pulse. The deceleration pressure (given by the difference between the initial pressure and the pressure minimum) varies in much the same way as the first pressure pulse. The maximum void volume, which is determined by the impulse given to the piston and the deceleration pressure does not show a clear trend. The magnitudes of the water-hammer pressure pulses vary in much the same manner as the first pressure pulse. The adiabatic peak temperature is a measure of the total heat deposited in the disc and varies with the reactor period.

The initial pressure pulses in these transient heating experiments have not reached destructive magnitudes. The water hammer impact pressures under these highly subcooled conditions^{*} do approach 1000 psi and could have dangerous consequences. There appear to be competing processes in the pressure production, one of which predominates at lower initial pressures. The second process evidently becomes dominant at higher initial pressures and causes a decrease in initial pressure pulse magnitude. The effects of piston loading and reactor period (total heat source and heating rate) are essentially monotonic in nature.

* As reported in our 1965 Annual Report, the water-hammer pressure pulses diminish rapidly with decreased subcooling

4. POWER REACTOR INSTABILITY AND ASSOCIATED TWO-PHASE FLOW PHENOMENA

4.1 AN ANALYSIS OF THE EBWR INSTABILITY WITH JONES' HYDRODYNAMIC MODEL - S. M. Zivi

The cause of the instability approached in 100 Mw operation of the Experimental Boiling Water Reactor is investigated here using the hydrodynamics of Jones' STABLE-3 model of hydrodynamic instability.¹⁹ The instability calculations were actually performed using Jones' FABLE program which incorporates the STABLE-3 hydrodynamics in a multi-region reactor stability program which includes reactivity feedback effects.²⁰ The cause of the instability was found to be a hydrodynamic interaction between dissimilar regions of the reactor core. Such interactions can occur in other boiling water reactors when various fuel assemblies have different steam void and flow responses to a power disturbance. The results of this analysis were presented at the June, 1966 meeting of the American Nuclear Society in Denver in a paper entitled "An Analysis of the EBWR Instability by the FABLE Program" by S. M. Zivi with A. B. Jones of the General Electric Company, San Jose.²¹

The STABLE-3 model of Jones' has been shown to be a reliable predictor of hydrodynamic loop instabilities for a given steady-state flow rate, and this raises the question of its validity for predicting the threshold power for the instability of an entire boiling water reactor. The basic hydrodynamic assumptions of STABLE-3 are embodied in Jones' FABLE model, which also includes the reactor kinetics equations and hydrodynamic interaction between numerous regions of the core. This hydrodynamic interaction or coupling exists through pressure variations in the inlet plenum, such as can be caused by disturbances in the flow distribution between the various regions or channels. FABLE computes the stability of a forced flow boiling water reactor for a given total reactor flow rate, total power, and power distribution. It calculates the steady-state void-fraction distributions and the pressure drop. It determines the hydrodynamic stability of each channel type (as does STABLE), and then it calculates for each frequency of interest:

- 1) The reactivity feedback response of each channel type for 100% power modulation with fixed flow to each channel;
- 2) The reactivity response of each channel type for 100% inlet flow modulation;
- 3) The total reactivity response of the entire core for 100% power modulation, including effects of hydrodynamic coupling between regions;
- 4) The pressure response in the inlet plenum for 100% power modulation (this is the coupling mechanism for multi-region effects);
- 5) The overall reactor stability by combining with the reactivity response, the reactor kinetics equations (zero-power transfer function).

FABLE can accommodate up to 25 different core regions in which power, flow, and void coefficient of reactivity are different. It includes longitudinal distribution effects of power and reactivity coefficients (void coefficients) while solving the equations of mass, momentum, and energy conservation coupled with the reactor's neutronics. Although the hydraulic radii and friction factors of the various zones may differ, the fuel and clad thicknesses must be uniform throughout the core.

The 100 Mw EBWR was chosen as a test case because of the availability of extensive and unique experimental data near an instability.^{22,23} Since the EBWR is a natural circulation reactor, an approximation was required in applying FABLE, as will be discussed later. The EBWR was operated at power levels up to 100 Mw. Transfer functions were measured in rod oscillator tests at powers up to 71 Mw. These measurements were extrapolated to predict an instability at about 120 Mw, where the precision of this extrapolation appears no better than about ± 25 Mw. The dominant frequency in the response at 71 Mw was 10 radians/sec. During high power operation, there was vapor entrainment in the downcomer (carry-under) and liquid entrainment in the steam (carry-over), as well as apparently violent sloshing and motion of the water above the core. These factors tended to obscure the cause of the impending instability.

Figure 20 shows a plan view of the reactor as it was represented in the calculations. The use of only 3 different zones is somewhat of a simplification. The estimated flow rates and exit qualities shown in

ZONE	FUEL TYPE	FLOW AREA	EQUIV. DIAMETER	POWER	MASS VELOCITY	EXIT QUALITY
1	METAL PLATE	2.5 FT ²	0.78 IN.	31 %	0.81×10^6 LB/HR FT ²	0.089
2	UO ₂ RODS	1.6 FT ²	0.42 IN.	23 %	0.46×10^6 LB/HR FT ²	0.200
3	METAL PLATE	5.0 FT ²	0.63 IN.	46 %	0.79×10^6 LB/HR FT ²	0.053

Figure 20. Plan View of EBWR Core as Represented in Stability Calculations

Figure 20 are for the reactor at 120 Mw. Zones 1 and 3 are metal plate-type fuel assemblies, whereas zone 2 consists of UO_2 fueled rod-bundle assemblies that have a smaller hydraulic diameter and a lower steady-state flow velocity. The power density in zone 2 is about 20% higher than zone 1, and this combined with the lower coolant velocity gives it a relatively high exit quality. Although zone 2 accounts for only 23% of the total reactor power, it has a very large effect on the system stability.

The FABLE calculations indicate that all three zones are hydrodynamically stable at a reactor power of 120 Mw. Zone 2 would become hydrodynamically unstable at about 155 Mw. Figure 21 shows the reactivity feedback of each zone for 100% power modulation with fixed flow to each zone. Zones 1 and 3 have responses of very similar shape, whereas zone 2 has distinctly greater phase lags. In this computation, the thermal time constant of the rod bundles (zone 2) is taken to be much smaller than the real one, since the present form of the FABLE program requires all zones to have the same fuel thickness and thermal conductivity. The thermal time constant of the zone 2 fuel was taken to be the same as that of zones 1 and 3, while the correct hydraulic properties of zone 2 are preserved. If the actual long thermal time constant of zone 2 were included, it would cause both the response amplitude and the phase lag to drop further below those of zones 1 and 3. The direct reactivity response of zone 2 to power modulation is not important, however, because it will be shown that it is the hydrodynamic response rather than the direct reactivity response of zone 2 to power disturbances which causes its large effect. Therefore, the use of the incorrect thermal time constant for the rod bundle assemblies is inconsequential.

Figure 22 contains three vector diagrams of the reactivity response to 100% power modulation at 10 radians/sec and at a reactor power of 120 Mw. Figure 22(a) shows the reactivity response of each zone and the total response for fixed inlet flow to each zone. The response of zone 2 is seen to be small. Figure 22(b) shows the reactivity response of the three zones for the case where the total inlet flow is fixed but where flow variations to each zone may occur due to coupling through the inlet plenum pressure response.

Figure 21. FABLE Computations of Reactivity Response of Zones 1, 2, and 3 for 100% Power Modulation at 160 Mw with Constant Inlet Flow to Each Zone

Figure 22. Calculated EBWR Reactivity Response at 120 Mw for 100% Power Modulation at $\omega = 10$ Rad/Sec. Zone 2 is the Rod-Bundle Zone.

The hydrodynamic coupling between zones is seen to have a dramatic effect, with the response of zone 2 now dominant. At frequencies other than 10 radians/sec, the effect of hydrodynamic coupling is much weaker, which indicates the presence of a resonance at about 10 radians/sec. In Figure 22(b), the reactivity response of zones 1 and 3 have been reduced from those in Figure 4(a) because the effect of these zones' inlet flow response partly negates their direct response to power modulation. Figure 22(c) includes an approximate upper bound to the effect of the total inlet flow response in natural circulation in the EBWR. This is obtained as follows.

The inlet plenum pressure response calculated by FABLE for the fixed total flow conditions shown in Figure 22(b) was divided by the calculated EBWR downcomer impedance. As this impedance is primarily inertial, it was calculated from

$$Z = \frac{\delta P}{\delta W} = -j\omega \frac{L_D}{A_D} \quad (7)$$

where

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

$$L_D = \text{effective length of downcomer (approximately 270 cm for EBWR)}$$

$$A_D = \text{effective cross sectional area of downcomer (approximately } 15 \times 10^3 \text{ cm}^2\text{)}$$

$$Z = \text{downcomer impedance}$$

The technique of using the forced flow inlet plenum pressure response to calculate an upper-bound to the inlet flow response in natural circulation is an approximation which must be used with care. If the downcomer impedance were to approach zero, the calculation would yield an upper-bound to the inlet flow response which approached infinity, and the calculation would be meaningless. In the case of the EBWR, however, the downcomer impedance was large enough that the upper-bound to the inlet flow response was only

about 20% of the mean flow rate when the reactor power modulation was 100%. Using the flow response calculated in this manner along with the FABLE computation of the reactivity response to inlet flow modulation, we obtain the upper-bound to the reactivity effect of the downcomer flow response, and thus approximately account for natural circulation effects in the EBWR. This reactivity response is shown in Figure 22(c). The actual EBWR reactivity response to 100% power modulation is bracketed between the reactivity feedback for fixed inlet flow and that for the upper-bound to the flow response. The total feedback transfer function at a frequency of 10 radians/sec is obtained by multiplying this response by the "zero-power" reactor kinetics transfer function. At 10 radians/sec this transfer function has a gain of about 1 and a phase lag of about 7° . The effect of introducing the reactor kinetics is therefore to rotate the vectors in Figure 22 clockwise by 7° . The locus of the reactor feedback transfer function as frequency varies from zero to infinity is such that the Nyquist criterion for system stability reduces to the simple requirement that the feedback transfer function have a magnitude less than 1 when the phase lag is 180° . When the upper-bound to the reactivity response for the natural circulation EBWR in Figure 22(c) is rotated 7° clockwise to account for the neutronics, a system instability is indicated. Even the case for fixed reactor inlet flow is near an instability, so the actual EBWR (bracketed by these two cases) is shown to be unstable at 120 Mw by these calculations. This indicates that the experimentally observed approach to reactor instability can be explained by reactivity feedback which is strongly influenced by the hydrodynamic interaction between the plate-type fuel and the rod-bundle fuel. The destabilizing effect of the rod-bundle zone is further illustrated by the following calculation.

Consider a hypothetical EBWR in which the rod-bundle zone is replaced by an equal flow area of the plate-type fuel which comprises zone 1. The reactor now consists of two zones which have similar thermal and hydraulic characteristics. The reactivity response to 100% power modulation is shown in Figure 23, where zone 1 is the inner region and zone 2 the outer region

2 PLATE-TYPE ZONES

Figure 23. Calculated Reactivity Response at $\omega = 10$ Rad/Sec of the EBWR with the Rod-Bundle Zone Replaced with Zone 1 Plate-Type Fuel Assemblies

(which has been designated zone 3 previously). Comparing Figure 23 with Figure 22, we see that the reactivity response with the rod-bundle zone replaced by plate fuel assemblies has a much smaller phase lag, indicating significantly greater stability. Virtually no hydrodynamic resonance occurs between the two plate-type zones, and the upper-bound to the flow effect is smaller.

In summary, the approach to instability in 100 Mw operation of the EBWR appears to be well explained by the FABLE program of Jones. This instability is caused by power-void reactivity feedback greatly augmented by an interzone hydrodynamic resonance. In general, regions within a boiling water reactor that have different thermal and hydrodynamic responses can introduce destabilizing effects through the hydrodynamic coupling mechanism shown here for the EBWR.

4.2 POWER-VOID TRANSFER FUNCTION MEASUREMENTS IN SUBCOOLED BOILING - A. F. Firstenberg and L. G. Neal

4.2.1 Introduction

Subcooled boiling can occur in both pressurized and boiling water reactors. A knowledge of subcooled boiling dynamics is important in the calculation of reactor stability and response because subcooled voids usually occur in the region of the core with the highest void coefficient of reactivity. The response of subcooled voids to changes in power is not presently calculable, and mathematical models of the dynamic behavior of boiling systems usually neglect the effect of subcooled voids. Furthermore, there appear to be no experimental measurements of subcooled boiling response available.

We have undertaken the measurement of power-void transfer functions in subcooled boiling at pressures, subcoolings, and heat fluxes in the range of commercial nuclear power practice. Experiments were confined to the subcooled boiling region in order to gain a number of economies and simplifications. The experimental loop is relatively small, and no condenser is needed. Power requirements for the production of a net efflux of steam is avoided, and a high heat flux in the subcooled boiling region is obtained with only modest power source facilities.

4.2.2 Experimental Apparatus

The apparatus was described in some detail in a previous report, and therefore will only be briefly discussed here.⁵ The high pressure loop was designed for a study of the subcooled boiling void response to power modulation for test conditions which include pressures up to 700 psi, subcoolings to 100°F, liquid velocities to 10 ft/sec, and heat fluxes to 500,000 BTU/hr ft²°F.

A schematic diagram of the loop is shown in Figure 24. The three expansion joints and a short length at the pump inlet are 1-1/4 inches i.d. The remainder of the flow channel is nominal 1 inch pipe (1.097 inch i.d.). All parts of the loop, with the exception of the test section and riser, are constructed of schedule 10S stainless steel pipe. The test section and riser are constructed of titanium alloy 662. Thermal expansion of the loop is accommodated by the three flexible sections (the pump is a fixed point). Water expansion and void generation are absorbed by the bellows chamber. The pressure on the external side of the bellows is maintained constant at the system operating pressure. A pressure relief valve prevents sudden pressurization of the system.

The test section length (heated length) is 6 inches. The total heat transfer area is 20.68 square inches. The test section wall thickness of 15 mils provides the required test section strength with a minimum attenuation of a soft x-ray beam. A schematic drawing of the test section is shown in Figure 25.

The riser is dimensionally identical to the test section, but is unheated. Its purpose is to allow us to observe the void decay rate. The variables measured are as follows: the pressure drop across the test section and the absolute pressure at the test section exit; the temperature of the water at the test section entrance, the test section exit, and the riser exit, the temperature profile along the test section wall; the total power input, both vertical and horizontal void volume profiles; and the inlet velocity.

4.2.3 Steady State Results

The measurements of the loop variables under steady state operating conditions are of importance in the dynamic analysis of the system. Void profile measurements, both horizontal and vertical, were obtained in the test section and the riser. Temperature measurements made possible the calculation of the fraction of the input power which resulted in steam generation, the quality in the system, and the slip ratio.

Figure 24. Schematic Diagram of the High Pressure Loop

Figure 25. Drawing of Thin Wall Test Section

4.2.3.1 Void Fraction Profiles

In Figure 26 are shown vertical void profiles in both the test section and the riser. At an operating pressure of 300 psia, a subcooling of 14°F and a power input of 15.2 kwatts, the void profiles were obtained for inlet velocities of 2.76 ft/sec, 5.12 ft/sec, and 7.88 ft/sec.

The void fraction increases linearly along the length of the test section, except near the entrance, and is a constant in the riser. The latter result is important because it indicates that the vapor condensation rate is slow compared to the transport time through the riser, and therefore we can conclude that bubble collapse rates are not an important factor in the subcooled dynamic process.

Near the entrance to the channel, the temperature profile is being established along with the void production rate. The established void production rate per unit length appears to be a constant along the test section.

The water temperature measurements, for the same experimental conditions as given in Figure 26 with an inlet velocity of 5.12 ft/sec, showed that the water temperature increased 5.3°F in passing through the test section. From an energy balance on the water, it was found that approximately 30% of the input power went into steam generation. The mass quality is defined in subcooled flow as the mass flow rate of steam divided by the total mass flow rate. At these conditions, the mass quality was calculated to be 0.3%. The slip ratio equation,

$$S \left(\frac{1}{X} - 1 \right) = \frac{\rho_w}{\rho_s} \left(\frac{1}{\alpha} - 1 \right) \quad (8)$$

was used with the calculated quality and the measured void fraction to calculate the slip ratio, S. The slip was found to be 0.82. This value less than unity is due to the fact that the void is concentrated

Figure 26. Longitudinal Void-Fraction Profiles as a Function of Inlet Velocity

near the wall where the stream velocity is low. Figure 27, a horizontal void profile taken 5.6 inches above the test section inlet, shows that this is the case. It is interesting to note that this type of distribution does not exist farther downstream in the riser. From Figure 28, a horizontal void profile taken 6.6 inches downstream from the test section exit, we observe that void is concentrated near the center of the channel.

From the outside wall temperature measurements for the operating conditions of pressure = 300 psia, subcooling = 14°F, power = 15.2 kwatts, and inlet velocity = 5.12 ft/sec, it was found that the inside wall surface temperature was 58°F above the saturation temperature. This value is considerably greater than the value of 33°F predicted by the empirical correlation of Jens and Lottes.²⁵ It is probably that the relatively smooth heat transfer surface in this experiment that gives less active nucleation sites is responsible for this difference.

4.2.3.2 Summary of Steady State Results

The steady state void characteristics for the system may be summarized as follows. The void volume fraction increases linearly except near the entrance in the test section and it is constant in the riser. The radial void distributions have a minimum at the center of the test section which is the inverse of what is usual for bulk boiling. The calculated slip ratios are less than, but close to, unity. The wall superheat is about 50% greater than the value predicted by the Jens-Lottes correlation.

4.2.4 Dynamic Results

Amplitude and phase responses of void fraction to power modulation over a frequency range from 0.1 to 5.0 cycles/sec were obtained for the conditions summarized in Table 2.

Figure 27. Transverse Chordal Void-Fraction Profile in the Test Section

Figure 28. Transverse Chordal Void-Fraction Profile in the Riser

TABLE 2
POWER-VOID TRANSFER FUNCTION OPERATING CONDITIONS

TRANSFER FUNCTION #	PRESSURE (psia)	SUBCOOLING (°F)	INLET VELOCITY (ft/sec)	MEAN POWER (kwatts)	POWER MODULATION AMPLITUDE (kwatts)	DISTANCE ABOVE TEST SECTION INLET (inches)
1	300	14	5.12	15.2	1.0	5.5
2	300	14	5.12	15.2	1.0	12.7
3	300	22	1.18	13.2	1.0	5.5
4	300	22	1.18	13.2	1.0	12.7
5	700	17	5.12	15.0	0.5	9.3

4.2.4.1 System Linearity

The extent to which system response is linear is of importance in a transfer function analysis. The linearity of void response to test section power modulation was investigated by measuring the amplitude and phase response of void fraction to power modulations of various amplitudes at frequencies of 1 and 5 cycles/sec. A systems's response may be considered linear for those conditions in which the amplitude response is proportional and the phase response is independent of the applied modulation amplitude.

The experimental results are shown in Figure 29. The void response is linear over the $\pm 20\%$ power amplitude range tested. The linearity check was performed at a number of power levels and subcoolings, and in every case the response was found to be linear. However, in all cases the subcooled boiling was well established. We have not made linearity checks near the inception of boiling.

Figure 29. Linearity of Void Response to Power Modulation.
Power = 15.2 kw, Pressure = 300 psia, Inlet
Velocity = 5.12 ft/sec, Subcooling = 48°F

4.2.4.2 Power-Void Transfer Functions

There appear to be two transport processes which are important in determining the dynamics of subcooled boiling systems. The first is the energy storage in the test section wall and transport to the liquid, and the second is the void transport from its point of generation to the point of measurement. The actual power-void transfer function may be written as the product of the transfer functions for each of these processes.

$$\frac{\delta\alpha}{\delta Q} = \left(\frac{\delta q}{\delta Q} \right) \left(\frac{\delta\alpha}{\delta q} \right) \quad (9)$$

where

Q = the electrical power per unit of heat transfer area

q = heat flux into the water

and

α = void fraction.

The void response to heat flux modulation, $(\delta\alpha/\delta q)$, may be obtained from a solution of the conservation of mass and energy equations for the liquid. With the assumptions of a constant void transport velocity in the test section and a constant fraction of the input energy contributing to the generation of steam, this transfer function has the form of a distributed source dominated by a pure transportation delay,

$$\frac{\delta\alpha}{\delta q} = K \frac{\sin \frac{\omega\tau_1}{2}}{\frac{\omega\tau_1}{2}} e^{-j \frac{\omega\tau_1}{2}} \quad (10)$$

where K is the zero frequency response of the system and τ_1 is the time of transport along the length of the source.

The heat flux response to an electrical power input change, $(\delta q/\delta Q)$, may be determined by solving the energy equation for the test section wall. As an approximate solution to the problem, a lumped parameter representation of the test section wall was used to derive the following

$$\frac{\delta q}{\delta Q} = \frac{1 - j\omega\tau_2}{1 + (\omega\tau_2)^2} \quad (11)$$

where τ_2 is the time constant associated with the test section wall and is equal to the heat capacity of the wall per unit area of heat transfer surface divided by the heat transfer coefficient. τ_2 was evaluated from the properties of the titanium wall and the heat transfer coefficients calculated from the wall temperature measurements at the conditions of transfer function No. 1. For these conditions the value of τ_2 was calculated to be .039 seconds.

Substituting Equations (10) and (11) into Equation (9) yields the desired transfer function, $\delta\alpha/\delta Q$,

$$\frac{\delta\alpha}{\delta Q} = \left[\frac{1 - j\omega\tau_2}{1 + (\omega\tau_2)^2} \right] \left[\frac{K \sin \frac{\omega\tau_1}{2}}{\frac{\omega\tau_1}{2}} e^{-\frac{j\omega\tau_1}{2}} \right] \quad (12)$$

Solving for the phase lag of the void fraction to power modulation, we obtain

$$\phi = \tan^{-1} \frac{\sin \frac{\omega\tau_1}{2} + \omega\tau_2 \cos \frac{\omega\tau_1}{2}}{\cos \frac{\omega\tau_1}{2} + \omega\tau_2 \sin \frac{\omega\tau_1}{2}} \quad (13)$$

At the conditions of transfer function No. 1, namely pressure = 300 psia, subcooling = 14°F, inlet velocity = 5.12 ft/sec, and a mean power = 15.2 kwatts,

the phase response was measured at various positions along the test section. From these measurements, the total delay time as a function of the vertical position in the test section was obtained and is shown in Figure 30. At the inlet to the test section, the delay time is due solely to the energy storage in the wall and transport to the liquid. The intercept of Figure 30 therefore furnishes an experimental value of τ_2 equal to .031 seconds. This value of τ_2 represents a 20% difference from the value obtained from the lumped parameter approximation. The slope of the delay time versus the test section position furnishes a constant void propagation velocity of 6.4 ft/sec.

Figures 31 and 32 show the amplitude and phase response, respectively, of transfer function No. 1. The theoretical curves were calculated from the measured values of τ_1 and τ_2 . In Figures 33 and 40, the response of transfer functions 2 through 5 are presented.

4.2.4.3 Conclusions

We have formulated a simple model based upon the idea that there are two transport processes which are important in determining the dynamics of a subcooled boiling system. The theoretical predictions of the model agree quite well with the data for frequencies below about 2 cycles/sec. At frequencies above this value, where the wall thickness is appreciable compared to a temperature oscillation wavelength, the lumped parameter representation of the test section wall is no longer adequate and the heat flux response to an electrical power modulation must be obtained by solving the energy equation for the test section wall.

The experimental results and some of the analysis given here were presented at the November, 1966 meeting of the American Nuclear Society in Pittsburgh. The paper, by L. G. Neal, N. C. Pate, and A. F. Firstenberg, was entitled, "Power-Void Transfer Function Measurements in Subcooled Boiling."²⁶

Figure 30. Time Delay of Void Fraction Response to Power Modulation as a Function of Test Section Position

Figure 31. Amplitude of Transfer Function No. 1

Figure 32. Phase of Transfer Function No. 1

Figure 33. Amplitude of Transfer Function No. 2

Figure 34. Phase of Transfer Function No. 2

Figure 35. Amplitude of Transfer Function No. 3

Figure 36. Phase of Transfer Function No. 3

Figure 37. Amplitude of Transfer Function No. 4

Figure 38. Phase of Transfer Function No. 4

Figure 39. Amplitude of Transfer Function No. 5

Figure 40. Phase of Transfer Function No. 5

4.2.5 Nomenclature for Section 4.2

K	zero frequency reponse of the void transportation transfer function
Q	electrical power input per unit of heat transfer area
q	heat flux into the water
S	slip ratio
X	mass quality
α	void volume fraction
ϕ	phase lag of void fraction to electrical power modulation
ρ	density
τ_1	time of transport along the length of the source
τ_2	wall-coolant heat transfer time constant

Subscripts

s	steam
w	water

4.3 EXPERIMENTS AND ANALYSIS ON DISTRIBUTIONAL EFFECTS IN TWO-PHASE FLOW - L. G. Neal and A. F. Firstenberg

4.3.1 Introduction

Studies of two-phase hydrodynamics have been hampered by the inability of present technology to measure many of the important variables. As an example, it has not generally been possible to measure the vapor phase volume fraction distributions or the velocity profiles across a two phase stream. It has therefore been customary to assume that the vapor phase is uniformly distributed with a single velocity associated with each phase.* These velocities are chosen so that continuity is satisfied. Thus, an erroneous result is obtained when they are used to calculate the momentum flux terms in the equations of motion. This is because the squares of the average velocities are not equivalent to the average of the velocities squared when the vapor phase and stream velocities are non-uniformly distributed over the flow cross sectional area (as they usually are).

We have performed a laboratory experiment on these distributional effects in two phase flow. The experiment consisted of simultaneously measuring the power, inlet flow rate, inlet subcooling, exit void fraction, and exit momentum flux in the geometry of a simulated SPERT IA moderator-coolant channel at atmospheric pressure. The measurement of both mass flux and momentum flux allows us to define and evaluate a single parameter chosen to characterize the distributional effects.

Based upon the results of this experiment, an annular flow model was developed that incorporates a liquid velocity distribution as well as liquid entrainment in the vapor stream. The entrainment fraction was found to be zero within the experimental accuracy. A generalized distributional flow parameter of the Bankoff type was found to characterize this model. When applied to void fraction measurements from ten different laboratories, this flow parameter correlated data in bubble and slug flow as well as in annular flow better than did the conventional slip ratio.

*The distributions have been measured for a few specific cases as reported in References 27 and 28. Likewise, some analyses have considered distributional effects.^{27,29,30} However, these analyses have had limited application because of the inability to evaluate them experimentally.

4.3.2 Experimental Apparatus

4.3.2.1 Experimental Loop

The boiling loop used in this study is the atmospheric loop used previously in our laboratory to study the void dynamics and stability of SPERT 1A channels. The loop is described as used for that purpose in Reference 31.

The loop was modified for the purpose of measuring the momentum flow by eliminating the riser and replacing it with a catch bucket and drain line to a storage tank. The water was returned from the storage tank to the downcomer by a pump. Other than these two modifications, elimination of the riser and the pump, the channel and downcomer plumbing were unchanged.

A schematic diagram of the modified loop showing the test section, catch bucket, reservoir and downcomer is shown in Figure 41. The downcomer is heated to maintain a constant inlet subcooling. The loop is thermally insulated to reduce heat losses.

The test section is a simulated SPERT 1A moderator coolant channel. The rectangular channel has the following inside dimensions:

length:	62.5 centimeters
width:	2.1 centimeters
thickness:	0.30 centimeters

The aluminum walls are .076 centimeters thick and are electrically heated. The heating of the constantan edges is negligible because of their relatively high electrical resistivity.

4.3.2.2 Loop Instrumentation

The loop variables measured were the water flow rate into the channel, the heating power, the inlet water temperature, and the vapor volume fraction in the channel. A brief description of the instrumentation used to measure these parameters is given below.

Figure 41. Schematic Diagram Of Boiling Loop

The liquid flow rate is measured using a specially built 12-blade turbine flowmeter which utilizes jeweled bearings and a corrosion resistant platinum alloy shaft. Rotation velocity is measured by the frequency of interruption of a beam of light by the turbine blades. The output photo-cell pulses are converted into uniform pulses by a frequency meter. The calibration of the meter gives the liquid volume flow rate versus the pulse frequency. The flowmeter response is linear above about $4 \text{ cm}^3/\text{sec}$.

The channel heating current is measured by the voltage induced in an air core magnetic pick up loop of about a hundred turns which is fastened rigidly to the upper bus bar lead to the channel. The heating current, I , and the known channel resistance, R , determine the heating power by the equation

$$P = I^2 R \quad (\text{watts}) - \text{heat losses} \quad (14)$$

where the heat losses were calculated to be approximately 25 watts.

The inlet water temperature is measured 6 inches below the channel entrance by a copper-constantan thermocouple. The thermocouple signal is taken relative to an hypsometer* reference and is read out on a strip recorder. The temperature difference is read to within the nearest 0.1°C .

The void fraction is measured using x-ray attenuation. The attenuated x-ray photons are detected by a sodium iodide crystal viewed by a photomultiplier. The photomultiplier tube output current is used as the detected signal and is recorded on a chart recorder. The void fraction measurements are accurate to about 2%.

* An hypsometer is a heated vessel containing steam and water at atmospheric pressure which is designed to provide a reliable reference temperature.

4.3.2.3 Momentum Measurements

The two-phase momentum flow measurements are obtained by measuring the force necessary to change the direction of the stream by 90°. A flat plate is placed directly above the exit of the channel thereby altering the direction of the oncoming stream upon impact. A schematic diagram of the momentum flow measurement apparatus is shown in Figure 42. The vertical force exerted by the stream on the impulse plate is measured by a strain gauge force transducer.

The validity of the momentum flow measurements was checked by comparing experimental and calculated values of momentum flow for single phase steady state flow. The method developed by Deissler and Taylor³² was used to obtain velocity profiles in the noncircular passage. From the velocity profile, the momentum flow was calculated numerically from

$$M = \int_A \rho V^2 dA \quad (15)$$

The single phase comparison between experimental and calculated momentum flow for inlet temperatures of 25°C and 90°C are shown in Figures 43 and 44, respectively.

The potential energy loss of the stream from the channel exit to the momentum plate is calculated and added to the momentum flow measurement. The potential energy loss per unit area is calculated from the equation,

$$P. E. Loss = \left[(1 - \alpha_e) \rho_w + \alpha_e \rho_s \right] h_o \quad (16)$$

Other factors affecting the momentum measurements, such as surface tension, were shown to be unimportant.

The "escape" flow area (between the plate and the periphery of the channel exit) of the stream must be carefully monitored and set equal to the flow area of the test section. If the flow area of the exit or "escape"

MOMENTUM FLOW MEASUREMENT APPARATUS

Figure 42. Schematic Diagram of Momentum Flow Measurement Apparatus

Figure 43. Comparison Between Experimental and Theoretical Single Phase Momentum Flow. Inlet Temperature = 25°C

Figure 44. Comparison Between Experimental And Theoretical Single Phase Momentum Flow. Inlet Temperature = 90°C.

opening between the channel and the plate were less than the channel flow area, a horizontal acceleration of the flow would occur in the exit region. This would have associated with it a pressure gradient in the horizontal direction, beneath the plate. The net upward force due to this pressure distribution would be an unwanted artifact in the experiment, since its magnitude would not be proportional to the upward momentum flow in the channel. The stream escape area is determined by the height, h , of the momentum plate above the channel exit. To equalize flow areas,

$$P_o h_o = A \quad (17)$$

for our channel,

$$P_o = 4.8 \text{ cm}$$

and

$$A = 0.62 \text{ cm}^2$$

so that

$$h_o = \frac{0.62}{4.8} = .13 \text{ cm}$$

where h_o is height of momentum plate above channel exit for equal flow areas, P_o is the channel perimeter and A is the channel cross sectional area.

The sensitivity of measured momentum flow to h was determined by the experimental results shown in Figure 45. At operating conditions, a 10 percent error in h resulted in 16 percent error in the momentum flow. Consequently, a good deal of effort was taken to insure that the value of h was accurate. One problem in this respect was the thermal expansion of the channel during heat up. The test section was rigidly constrained at the entrance and allowed to expand from that point. Figure 46 is a plot of the change in height of the

SENSITIVITY OF MOMENTUM FLOW MEASUREMENT TO h

Figure 45. Sensitivity of Momentum Flow Measurements To h .

Figure 46. Temperature Dependence Of Channel Length

channel with temperature change. The proper value of h_0 was obtained using the following procedure:

- a. With no flow, a weight was placed on the "momentum arm" directly above the force transducer.
- b. The height h was set taking into account the expected thermal expansion of the channel for a particular operating condition.
- c. The force transducer reading was noted and zeroed at this position.
- d. During operating conditions, the weight on the momentum arm is replaced with a smaller weight whose value is such that the "zero" of the force transducer is re-established. The force transducer is thus used as a positioning device while the difference in weights on the arm and its associated moments are used to measure momentum flow.

Early experiments utilized a momentum bucket designed to completely reverse the direction of the flow; the resulting vertical force being twice the momentum flow. However, close investigation of the difference between experimental and calculated results for single phase flow led to the conclusion that the stream exerted a non-negligible drag force on the momentum bucket. The difficulties involved in calculating the drag force accurately forced a design change to the impulse plate.

4.3.3 Results and Analysis

We have measured one more flow variable than is usual in two phase flow studies, and therefore we have one further degree of freedom in characterizing the flow. The simultaneous measurement of flow rate and momentum flow have shown that, when the usual analytical approach of assigning a single velocity to each phase is used so that continuity is satisfied, the calculated momentum flow is considerably smaller than that measured. This difference is due to the fact that velocity distribution effects are important. This distributional effect may be evaluated by an analysis of the experimental measurements.

Transverse void fraction profiles and void histories indicate that the flow regime at the exit of the channel is annular. The assumptions made in analyzing the steady state behavior of the system are based upon this observation.

An annular flow model is proposed which is based upon the assumption that the liquid film is of uniform thickness, the liquid velocity in the film varies linearly from the wall, the vapor velocity is uniform, and a fraction of the liquid is entrained in the vapor stream and travels at the vapor velocity. Within the framework of this flow model, the experimental measurements show that the entrainment fraction is zero. This makes possible an accurate prediction of momentum flow from the model with measurements of flow rate and void fraction.

The proposed annular flow model implies a slip model of the variable density type where the generalized distributional flow parameter is used. The flow parameter is not only a function of pressure, as suggested by Bankoff, but is also a function of void fraction. The observed relation between the flow parameter and void fraction was linear. In the work to be reported here, it is shown, with the aid of data from other laboratories, that the slope of the linear relationship is a function of pressure and that it approaches zero at the critical pressure. It was also observed that the linear relation to void fraction is valid only for annular flow.

In slug flow, and presumably bubble flow, the flow parameter is independent of void fraction.

The annular flow analysis is presented following a presentation of the longitudinal void profiles for the modified loop. A comparison is made between the modified and the unmodified systems.

4.3.3.1 Longitudinal Void Profiles

The steady state natural circulation flow and void characteristics of the SPERT 1A coolant channel in the unmodified loop were measured and are reported in Reference 31. Similar measurements were made with the modified loop for comparison, and these results are shown in the following section.

The principal differences between the two loops are as follows: (a) the pressure at the exit of the channel is one atmosphere for the modified loop compared to 61 cm of water for the unmodified loop, and (b) the flow is forced for the modified loop compared to natural circulation for the unmodified loop. The first difference made it necessary to reproduce the same subcooling by altering the inlet temperature. The water temperature at the channel entrance in experiments with the unmodified loop was 100°C which represents 3.3°C subcooling due to the 122 cm hydrostatic pressure (61 cm above the channel exit). In our experiments, the inlet temperature was 98.4°C which represents 3.4°C subcooling.

The flow chosen for operation in our forced circulation experiment was 29.3 cc/sec which corresponds to the natural circulation flow rate that existed in the unmodified loop at 500 watts total power.

Figure 47 shows the longitudinal void profiles for a flow rate of 29.3 cc/sec, and for steady power levels of 500 and 700 watts. The two sets of data at 500 watts show the reproducibility of the measurements.

Figure 48 shows a comparison of the longitudinal void fraction distributions for the loop before and after being modified. The position at which boiling begins is about the same in each case but at the point of

Figure 47. Measurements Of The Longitudinal Distribution Of Steam Void Fraction

Figure 48, Comparison Of The Longitudinal Void Fraction Distribution Between Modified And Unmodified System

bulk saturation the curves are much different. The difference in void fraction distributions is typical of the channel-to-channel variations which we have experienced in the past.

4.3.3.2 An Annular Flow Model

A flow model has been formulated in order to evaluate the distributional effects in the light of our momentum flow measurements. Time histories and transverse scans of the exit void fraction indicate that the flow regime at the channel exit is annular. The following assumptions were made in the formulation of the model:

- a) The annular liquid film is of uniform thickness
- b) The liquid velocity profile in the film is linear, starting from zero at the wall.
- c) The vapor velocity is uniform, but not necessarily equal to the interfacial liquid velocity.
- d) Some fraction of the liquid is entrained in the vapor and travels at the vapor velocity.

The assumptions relative to the velocity distribution appear to be reasonable for annular flow. The rough liquid-vapor interface generates turbulence in the vapor stream, so the vapor velocity profile is probably very flat. The liquid velocity profile should be very nearly linear because of the small film thickness and because the liquid is driven by the vapor stream.

The assumption of a uniform liquid film thickness is adequate. This film thickness was measured by traversing a 5 mil wide x-ray beam across the wide dimension of the channel. The amount of liquid detected at each position was assumed to be equally distributed on the two walls, and any entrained liquid was negligible. Figure 49 shows an example of film thickness. While the film thickness is not uniform, the agreement of the model with the experimental data suggests that the non-uniformity is not of major importance.

Figure 49. Film Thickness Distribution. Power = 500 Watts; Subcooling = 3.3°C ;
Inlet Flow Rate = 29.3 cc/sec

The equations to be used in formulating a flow model for the system are as follows:

Definition of Quality

$$X \equiv \frac{W_s}{W_t} = \frac{\alpha \rho_s \bar{V}_s}{\alpha \rho_s \bar{V}_s + (1-\alpha) \gamma \rho_w \bar{V}_s + (1-\alpha) (1-\gamma) \rho_w \bar{V}_{wo}} \quad (18)$$

Mass Flux

$$\frac{W}{A} = \bar{V}_{wo} \left[(1-\alpha) (1-\gamma) \rho_w + S_o \alpha \rho_s + S_o (1-\alpha) \gamma \rho_w \right] \quad (19)$$

Momentum Flux

$$\frac{M}{A} = \left[(1-\gamma) (1-\alpha) \rho_w + \gamma (1-\alpha) \rho_w S_o^2 + \alpha \rho S_o^2 \right] \bar{V}_{wo}^2 \quad (20)$$

Conservation of Energy

$$X = \frac{P}{\rho_w \dot{W}_{sw}} - \frac{c \Delta t}{h_{sw}} \quad (21)$$

The average liquid velocity in the film, $\bar{V}_{wo}$, may be expressed as :

$$\bar{V}_{wo} = \frac{1}{A_f} \int_{A_f} V_w dA_f = \frac{a \delta}{2} \quad (22)$$

and similarly, the average of the liquid velocity squared, $\overline{v_{wo}^2}$, is expressed as:

$$\overline{v_{wo}^2} = \frac{1}{A_f} \int_{A_f} v_w^2 dA_f = \frac{a^2 \delta^2}{3} \quad (23)$$

where the small corner effects have been neglected. Substituting these values into Equations (18), (19), and (20) yields the following:

Definition of Quality

$$X = \frac{\alpha \rho_s \overline{v_s}}{\alpha \rho_s \overline{v_s} + (1-\alpha) \rho_w \left[\gamma \overline{v_s} + (1-\gamma) \frac{\delta a}{2} \right]} \quad (24)$$

Mass Flux

$$\frac{W}{A} = \alpha \rho_s \overline{v_s} + (1-\alpha) \rho_w \left[\gamma \overline{v_s} + (1-\gamma) \frac{\delta a}{2} \right] \quad (25)$$

Momentum Flux

$$\frac{M}{A} = \alpha \rho_s \overline{v_s^2} + (1-\alpha) \rho_w \left[\gamma \overline{v_s^2} + (1-\gamma) \frac{(\delta a)^2}{3} \right] \quad (26)$$

The film thickness δ is related to the void fraction and entrainment fraction by:

$$\delta(P_o - 4\delta) = (1-\gamma)(1-\alpha)A \quad (27)$$

Using experimental measurements of mass and momentum flow rates, void fraction and mass quality, Equations (24), (25), (26), and (27) may be solved simultaneously for the unknowns, $\bar{V}_s$, γ , δ , and 'a'.

The results of the above calculations show that the entrainment is zero within the experimental accuracy of our measurements. Therefore, an explicit expression for the slope of the velocity profile may be derived from continuity. It yields the slope 'a' as a function of inlet flow rate, void fraction, quality, channel dimensions, and physical quantities. The relationship is:

$$a = \frac{2W(1-x)}{A\rho_\ell(1-\alpha) \left[\frac{P_o - \sqrt{P_o^2 - 16(1-\alpha)A}}{8} \right]} \quad (28)$$

Utilizing values of 'a' calculated from the above equation, the momentum flow was calculated. In Figure 50 a comparison is made between the theoretical momentum flow and the experimentally measured momentum flow as a function of power. The dashed line represents the calculated momentum flow based on the usual assumption of a uniform velocity for each phase. The theoretical results of the annular flow model are given by the solid line. Similar results are presented in Figure 51 as a function of inlet flow rate.

The measurements have shown that, when the usual analytical approach of assigning a single average velocity to each phase is used, the calculated momentum flow is considerably smaller than that measured. Neglecting the distributional character of the two-phase flow can mean an error in the calculated momentum flow as large as 60%. This conclusion is based upon

Figure 50 Momentum Flow Versus Channel Power for Inlet Flow Rate = 29.3 cc/sec;
Subcooling = 3.3°C

Figure 51. Momentum Flow Versus Inlet Flow Rate for a Channel Power = 500 Watts;
Subcooling = 3.3°C

the experimental measurements and is independent of the assumed flow model. However, the model presented predicts the momentum flow within experimental errors ($\pm 15\%$) for all cases. This implies that the neglected phenomena, such as a non-uniform film thickness, are not of major importance.

4.3.3.3 Slip Ratio

One of the first analyses of distributional effects in two-phase slip flow was that of Bankoff.²⁹ He proposed a model which assumed that the local phase velocities are equal and that slip is due to the vapor being concentrated at the center of the flow channel where the stream velocity is highest. Bankoff's slip equation is:

$$S \equiv \frac{\bar{V}_s}{\bar{V}} = \frac{1 - \alpha}{K - \alpha} \quad (29)$$

where K is a flow parameter dependent upon the shape of the velocity and local void fraction profiles. It was later shown that, in a general sense, this flow parameter is a spacial correlation factor between the probability that vapor exists at a point in the flow, p , and the expectation stream velocity at the same point.³⁰ It may be evaluated for any profile form from the following equation:

$$\frac{1}{K} = \frac{\frac{1}{A} \int p V dA}{\left[\frac{1}{A} \int p dA \right] \left[\frac{1}{A} \int V dA \right]} \quad (30)$$

where V is the local expectation velocity, $(1 - p) V_w + p V_s$.

The flow regime structure we have assumed in this report implies that a slip relation exists of the form of Equation (29). With the information available, we can derive an equation for K in terms of the average void fraction, the film thickness, the mass flow rate, and the channel geometric

parameters. Actually, K may be evaluated directly from Equation (29) without the assumed profiles since both α and S are known. The purpose of this analysis is to show the relation between K and the assumed distribution parameters. Equation (30) may be written as:

$$\frac{1}{K} = \frac{\int_A \left[P (1 - p) V_w + p V_s \right] dA}{\alpha \int_A \left[(1 - p) V_w + p V_s \right] dA} \quad (31)$$

Equation (31) is then integrated to give an equation for K,

$$K = \frac{a\alpha\rho_s}{XWA} \left[P_o \frac{\delta_o^2}{2} - 8 \frac{\delta^3}{3} \right] + \alpha \quad (32)$$

where P_o is the channel perimeter and A is the channel cross-sectional area. K is determined from the values of a, δ , and W previously calculated. These values are given in the following table.

Table 3. DISTRIBUTION PARAMETERS FOR TRW SYSTEMS DATA

δ cm	a, sec^{-1}	S	K	α
.088	1735	4.17	.520	.37
.063	3272	4.23	.645	.54
.053	4564	4.80	.689	.61
.046	6124	5.01	.726	.66
.033	9338	3.40	.822	.75
.076	2794	6.45	.552	.45
.081	2690	6.35	.513	.41

The variable density model was proposed for bubble flow by Bankoff, and the velocity and vapor distribution profiles were assumed to be power law functions of the radial position in the tube.²⁹ When the flow parameter K was correlated by Bankoff, a simple relationship was chosen which made K a function of pressure only. This choice was based upon the fact that the slip ratio must be unity at the critical pressure, and therefore so must K. The generalization of the flow parameter presented here and in Reference 30 shows that the same kind of analysis is applicable to other flow regimes, and is independent of the form of the profiles. To be more generally applicable, Equation (29) requires that K have two limiting values. As previously stated, K must be unity at the critical pressure of the fluid where the hydrodynamic properties of the vapor and liquid are equivalent and the slip ratio is one. A second limiting value of K is that it approach unity as α does. This is necessary in Equation (29) if S is to remain finite.

The experimental values of the distribution parameter K were plotted versus the void fraction. The resulting curve, shown in Figure 52, is linear and the best fit of the points, bearing in mind the requirement that the curve must pass through $K = 1$ when $\alpha = 1$, This line has the equation:

$$K = 1 - 0.79 (1 - \alpha) \quad (33)$$

This equation is to be compared with a constant value of $K = 0.71$ predicted by Bankoff's empirical correlation.

Equation (33) has several interesting features. If it is substituted into Equation (29) in place of K, we obtain

$$S = 4.76 \quad \left(\begin{smallmatrix} 34 \\ 34 \end{smallmatrix} \right)$$

That is, the slip ratio is constant. This is surprising, and at first disturbing, because the data of Table 3, from which this result was obtained, show that the slip ratio differs by $\pm 35\%$ from the constant value of 4.76

Figure 52. The Functional Relationship Between Bankoff's Flow Parameter and Void Fraction for the Data of Firstenberg and Neal

predicted by the model. However, the variation is random as a function of void fraction and shows the experimental error in determining S. The reasons that the data of Figure 52 exhibit comparatively little deviation from the linear relationship is that K is quite insensitive to errors in S at large void fractions. The void fraction is also relatively insensitive to variations in S, but is very sensitive to K. This suggests that K is a more meaningful correlation parameter for determining α than is S. We can investigate this by deriving equations which relate the errors in these quantities. Equation (15) can be written as,

$$S \left(\frac{1}{X} - 1 \right) = \frac{\rho_w}{\rho_s} \left(\frac{1}{\alpha} - 1 \right) \quad (35)$$

Substituting Equation (29) into Equation (35) for S gives,

$$\left(\frac{1}{X} - 1 \right) = \frac{\rho_w}{\rho_s} \left(\frac{K}{\alpha} - 1 \right) \quad (36)$$

An investigator of two-phase flow makes measurements of α and X, and calculates values of S from Equation (35) or K from Equation (36). These calculated values are then correlated by an equation or graph and stored away for the future when he is required to predict the void fraction in a new system. He will then know X, so using the correlation of S or K he can return to Equation (35) or (36) and calculate α . However, his original measurements contained errors which were passed on to the correlating parameter. The best correlating parameter is the one which amplifies these errors the least. From Equation (35) the relation between errors in α and those in X and S is:

$$d \ln S = \frac{d \ln X}{1-X} - \alpha \frac{d \ln \alpha}{1-\alpha} \quad (37)$$

From Equation (36) the relation between errors in α and those in X and K is:

$$d \ln K = d \ln \alpha - \frac{\alpha \rho_s}{X \rho_w K} d \ln X \quad (38)$$

These equations show that when $\alpha = X = 0$, the percent errors in S and K are equal. However, as α and X increase to unity the error in S is amplified by a monotonically increasing factor to infinity, but the error in K decreases by a monotonically decreasing factor whose magnitude depends upon K .

A second interesting feature of Equation (33) is that it introduces the slope of the K versus α line as a parameter which should approach zero at the critical pressure due to the limiting conditions imposed upon K . Equation (33) may be written generally to include the new flow parameter k , as:

$$K = 1 - k(1 - \alpha) \quad (39)$$

Values of k were calculated for pressures from atmospheric to 2000 psi using data from ten different laboratories.^{33,34,35,36,37,38,39,40,41} Figure 53 is a plot of k versus pressure. It indicates that k takes values from 0.79 at atmospheric pressure to zero at the critical pressure as postulated.

Experimental results of Rouhani and Becker are shown in Figure 54.³³ The data approach the point ($K = 1$, $\alpha = 1$) linearly. At a void fraction of approximately 0.8, however, the data break away from this straight line that corresponds to constant S and approach a line of constant K . The data from all of the experiments exhibited the same general behavior, but the value of the void fraction at the transition from a constant slip to a constant K varied from case to case.

Figure 53. k as a Function of Pressure

Figure 54. The Functional Relationship Between Bankoff's Flow Parameter and Void Fraction for the Data of Rouhani and Becker

It appears to us that the transition in the data from a constant K relationship to a constant slip relationship represents a change in flow regime from the bubble and/or slug flow regimes to an annular flow regime. The channel exit measurements taken in our laboratory were made in a flow regime that was always annular. These data showed a linear relation between K and α as shown previously by Figure 52. However, lower in the boiling region of the channel, the flow contained slugs and small bubbles. Data from this region of the channel broke away from the linear relationship in a way similar to the data in Figure 54. Figure 55 is a plot of K as a function of α for both annular and developing flow. It indicates that the constant slip model may be applied during annular flow only, while a constant K model may be utilized for developing flow.

The results of our analysis and experimental observations are in agreement with other published correlations, and two of these will be discussed. Armand presented his experimental data on a plot of void fraction versus volumetric quality.⁴² He observed that the ratio α/β was constant from $\alpha = 0$ to approximately $\alpha = 0.6$. At higher values of α , the ratio increased, approaching unity at $\alpha = 1$. The ratio α/β is equivalent to Bankoff's flow parameter K. Figure 56 is a plot of void fraction versus volumetric quality for the data of Rouhani and Becker.³³ The solid curves shown represent the conditions of both constant K and constant slip.

An empirical modification of the variable density model was made by A. B. Jones by defining the flow parameter as a function of void fraction such that $K \rightarrow 1$ as $\alpha \rightarrow 1$.⁴³ His relationship is:

$$K = K' + (1 - K') \alpha^r \quad (40)$$

where K' is the flow parameter given by Bankoff's correlation and r is a second correlating parameter. These correlations are:

Figure 54. The Functional Relationship Between Bankoff's Flow Parameter and Void Fraction for the Data of Rouhani and Becker

Relationship of K to α in Annular Flow and Developing Flow for the Data of Firstenberg and Neal

It appears to us that the transition in the data from a constant K relationship to a constant slip relationship represents a change in flow regime from the bubble and/or slug flow regimes to an annular flow regime.

Figure 56. The Data of Rouhani and Becker Presented on a Void Fraction Versus Volumetric Quality Basis

$$K' = 0.71 + 0.29 \left(\frac{P}{P_c} \right) \quad (41)$$

and

$$r = 3.33 + 0.577 \left(\frac{P}{P_c} \right) + 4.73 \left(\frac{P}{P_c} \right)^2 \quad (42)$$

In Equation (40), the void dependence of K is very weak at low void fractions, but it dominates for α greater than K' . Equation (40) simulates the shape of the complete K versus α curve, and it does so quite well. Figure 57 shows the predicted curve for very low pressures where $P/P_c \approx 0$. In the limit as $\alpha \rightarrow 1$, the predicted curve has a greater slope than our data indicate, 0.965 versus 0.79, and the intercept at $\alpha = 0$ is too large. This may be due to the following: Bankoff's correlating equation for K' was derived so that the best fit of data was obtained for void fractions between 0 and K . This was done by finding a K at each pressure which gave a correct relation between void fraction and mass quality at an intermediate α (as an example at $\alpha = 0.6$ for 1 atm pressure). Consequently, Bankoff's correlation predicts slip ratios which are small for void fractions smaller than the intermediate point and too large for void fractions which are larger. It was Jones' desire to have no effect upon Bankoff's correlation for small α but to change it for higher α 's so that the slip ratio remained finite. As a result, Jones' modified model predicts slip ratios which are small for all void fractions. Had K' and r been obtained simultaneously (rather than using Bankoff's correlation for K' and finding the necessary value of r), both would have been somewhat smaller. The values which fit our atmospheric pressure data best are $K' = 0.67$ and $r = 2.63$. These are to be compared with values of 0.71 and 3.33 predicted by Jones' equations.

Figure 57 A Comparison of Jones' Model with the Annular and Developing Flow Data of Firstenberg and Neal

4.3.3.4 Conclusions

- 1) The annular flow model proposed in this report predicts the momentum flow from the exit of the channel correctly.
- 2) When the usual analytical approach of assigning a single average velocity to each phase is used, the calculated momentum flow is considerably smaller than that measured. In our particular experiments, neglecting the distributional character of the two-phase flow can mean an error in the calculated momentum flow as large as 60%.
- 3) The annular flow model implies a variable density slip model where the flow parameter is of the generalized form.
- 4) In annular flow the flow parameter is a linear function of void fraction. The slope of the linear relationship is a function of pressure.
- 5) The flow parameter is independent of void fraction in bubble and slug flow. Its magnitude is a function of pressure.
- 6) The flow parameter is a better correlating parameter than is the slip velocity ratio because it amplifies the errors in void fraction and mass quality measurements by a much smaller factor.

Part of this material was presented in a paper at the Denver meeting of the American Nuclear Society in June, 1966. The paper, by A. F. Firstenberg and L. G. Neal was entitled "Momentum Flux Measurements and Entrainment in a Boiling Channel."⁴⁴

4.3.3.5 Nomenclature for Section 4.3

A	cross section area
a	slope of velocity gradient in liquid film
c	heat capacity
h_{sw}	latent heat of vaporization
k	slope of linear relationship between 'K' and ' α '
K	distribution parameter
M	exit momentum flow
P	channel power
P_o	channel perimeter
p	probability that vapor exists at a point
S	slip ratio ($\bar{V}_s/\bar{V}_w$)
S_o	$\bar{V}_s/\bar{V}_{wo}$
t	subcooling
V	velocity
$\bar{V}$	average velocity
W	flow rate
X	quality
α	void fraction
β	volumetric quality
δ	film thickness
γ	entrainment fraction
ρ	density

Subscripts

e	exit
s	steam
w	water
wo	water in the liquid film
f	film

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