MASTER

230
4-4-75
UC-798
UK, Summary & Japan

OPERA SINGLE-PIN PUMP-COASTDOWN
EXPULSION AND REENTRY TEST

by

R. E. Henry, R. M. Singer, D. J. Quinn,
E. A. Spleha, E. G. Erickson, and W. C. Jeans

BASE TECHNOLOGY

U of C-AUA-USERDA

DISTRIBUTION OF THIS DOCUMENT UNLIMITED

ARGONNE NATIONAL LABORATORY, ARGONNE, ILLINOIS

Prepared for the U.S. ENERGY RESEARCH
AND DEVELOPMENT ADMINISTRATION

Division of Reactor Research and Development
under Contract W-31-109-Eng-38

Released For Announcement in Nuclear
Science Abstracts. Distribution Limited
to Participants in the LMFBR Program.
Others request from TIC.

The facilities of Argonne National Laboratory are owned by the United States Government. Under the terms of a contract (W-31-109-Eng-38) between the U. S. Energy Research and Development Administration, Argonne Universities Association and The University of Chicago, the University employs the staff and operates the Laboratory in accordance with policies and programs formulated, approved and reviewed by the Association.

MEMBERS OF ARGONNE UNIVERSITIES ASSOCIATION

The University of Arizona	Kansas State University	The Ohio State University
Carnegie-Mellon University	The University of Kansas	Ohio University
Case Western Reserve University	Loyola University	The Pennsylvania State University
The University of Chicago	Marquette University	Purdue University
University of Cincinnati	Michigan State University	Saint Louis University
Illinois Institute of Technology	The University of Michigan	Southern Illinois University
University of Illinois	University of Minnesota	The University of Texas at Austin
Indiana University	University of Missouri	Washington University
Iowa State University	Northwestern University	Wayne State University
The University of Iowa	University of Notre Dame	The University of Wisconsin

NOTICE

This report was prepared as an account of work sponsored by the United States Government. Neither the United States nor the United States Energy Research and Development Administration, nor any of their employees, nor any of their contractors, subcontractors, or their employees, makes any warranty, express or implied, or assumes any legal liability or responsibility for the accuracy, completeness or usefulness of any information, apparatus, product or process disclosed, or represents that its use would not infringe privately-owned rights. Mention of commercial products, their manufacturers, or their suppliers in this publication does not imply or connote approval or disapproval of the product by Argonne National Laboratory or the U. S. Energy Research and Development Administration.

Printed in the United States of America
Available from
National Technical Information Service
U. S. Department of Commerce
5285 Port Royal Road
Springfield, Virginia 22161
Price: Printed Copy \$4.00; Microfiche \$2.25

DISCLAIMER

This report was prepared as an account of work sponsored by an agency of the United States Government. Neither the United States Government nor any agency Thereof, nor any of their employees, makes any warranty, express or implied, or assumes any legal liability or responsibility for the accuracy, completeness, or usefulness of any information, apparatus, product, or process disclosed, or represents that its use would not infringe privately owned rights. Reference herein to any specific commercial product, process, or service by trade name, trademark, manufacturer, or otherwise does not necessarily constitute or imply its endorsement, recommendation, or favoring by the United States Government or any agency thereof. The views and opinions of authors expressed herein do not necessarily state or reflect those of the United States Government or any agency thereof.

DISCLAIMER

Portions of this document may be illegible in electronic image products. Images are produced from the best available original document.

ANL-75-17

ARGONNE NATIONAL LABORATORY
9700 South Cass Avenue
Argonne, Illinois 60439

OPERA SINGLE-PIN PUMP-COASTDOWN
EXPULSION AND REENTRY TEST

by

R. E. Henry, R. M. Singer, D. J. Quinn,
E. A. Spleha, E. G. Erickson, and W. C. Jeans

Reactor Analysis and Safety Division

NOTICE

This report was prepared as an account of work sponsored by the United States Government. Neither the United States nor the United States Energy Research and Development Administration, nor any of their employees, nor any of their contractors, subcontractors, or their employees, makes any warranty, express or implied, or assumes any legal liability or responsibility for the accuracy, completeness or usefulness of any information, apparatus, product or process disclosed, or represents that its use would not infringe privately owned rights.

February 1975

DISTRIBUTION OF THIS DOCUMENT UNLIMITED

~~Released For Announcement in Nuclear
Science Abstracts. Distribution Limited
to Participants in the LMFBR Program.
Others request from NRC.~~

.

.

TABLE OF CONTENTS

	<u>Page</u>
NOMENCLATURE.....	8
ABSTRACT.....	9
I. INTRODUCTION.....	9
II. EXPERIMENTAL APPARATUS.....	10
III. TEST CONDITIONS.....	17
IV. DISCUSSION OF RESULTS.....	19
V. CONCLUSIONS	30
APPENDICES.....	32
A. Data Tabulation.....	32
B. Calculation of Incipient Superheat.....	37
C. Comparison of the OPERA Single-pin Pump Coastdown with SAS2A Predictions.....	38
Simulation of Experimental Initial Conditions by SAS2A.....	38
Simulation of Experimental Flow Transient by SAS2A.....	39
Comparison of Predicted and Measured Transient Behavior.....	39
REFERENCES.....	44

LIST OF FIGURES

<u>No.</u>	<u>Title</u>	<u>Page</u>
1	Schematic of OPERA Loop.....	10
2	Photograph of the OPERA Facility.....	11
3	Schematic of Single-pin Test Section.....	12
4	High-flux Heater (Overall View).....	12
5	High-flux Heater (Closeup).....	13
6	Test-section Instrumentation.....	14
7	Frictional Pressure-drop Data for Test Section.....	16
8	Pressure Losses in Test Section.....	16
9	Friction Factor for the Wire-wrapped Region.....	16
10	Experimental Flow Decay.....	18
11	Inlet and Outlet Temperatures during the Coastdown.....	18
12	Inlet and Outlet Pressures and Total Pin Power during the Coastdown.....	19
13	Experimentally Measured Voiding Profile.....	21
14	Flow at the Test-section Inlet Following Boiling Inception.....	22
15	Flow from Test-section Outlet Following Boiling Inception.....	22
16	Average Void Fraction in Test Section during Voiding.....	23
17	Temperature Profiles in Test Section during Voiding.....	23
18	A Comparison between the Local and Test-section Average Void Fractions during Voiding. Voltage-tap Increment 0.5-1.0 in. Downstream.....	26
19	A Comparison between the Local and Test-section Average Void Fractions during Voiding. Voltage-tap Increment 1.5-2.0 in. Downstream.....	27
20	Local Void Fractions in Coolant during the Voiding Transient. Voltage-tap Increment 0.5-1.0 in. Downstream.....	27

LIST OF FIGURES

<u>No.</u>	<u>Title</u>	<u>Page</u>
21	Local Void Fractions in Coolant during the Voiding Transient. Voltage-tap Increment 1.5-2.0 in. Downstream.....	27
22	Local Void Fractions in Coolant during the Voiding Transient. Voltage-tap Increment 2.5-3.0 in. Downstream.....	28
23	Local Void Fractions in Coolant during the Voiding Transient. Voltage-tap Interval 4.5-5.0 in. Downstream.....	28
24	Profiles of Axial Void Fraction during the Voiding Transient. Voltage-tap Intervals Recorded on Magnetic Tape.....	28
25	Estimated Pressure within the Voided Zone.....	29
26	Radiograph of the Failed Section of the Heating Element.....	30
C.1	Comparison of Experimental and Calculated Steady-state Axial Pressure Profile.....	39
C.2	Comparison of Experimental and Calculated Flow Transient Prior to Boiling.....	39
C.3	Comparison of Experimental and Calculated Structural Temperature.....	40
C.4	Variation of Maximum Heater Temperature with Time.....	40
C.5	Axial Profile of Coolant Temperature Prior to and after Boiling.....	41
C.6	Radial Temperature Profile in Test Section during the Transient.....	41
C.7	Comparison of Measured and Calculated Mass Flow Rates at the Inlet.....	42
C.8	Comparison of Measured and Calculated Mass Flow Rates at the Outlet.....	42
C.9	Comparison of Measured and Calculated Voiding Profiles.....	43

LIST OF TABLES

<u>No.</u>	<u>Title</u>	<u>Page</u>
I	Frictional Pressure-Loss Calibration.....	15
II	Dimensions of Test Section.....	17

NOMENCLATURE

A	Cross-sectional area
D	Diameter
f	Friction factor
g	Gravity
h	Height
I	Current
L	Length
P	Pressure
q	Heat
R	Electrical resistance
V	Voltage
W	Flow rate

Greek Symbols

α	Void fraction
γ	Electrical resistivity
Δ	Difference
ρ	Density
σ	Surface tension

Subscripts

b	Bubble
E	Empty
F	Full
f	Frictional
fg	Difference between vapor and liquid
h	Hydraulic
I	Inlet
l	Liquid
Na	Sodium
o	Outlet
St	Structure
V	Vapor
2 ϕ	Two-phase

OPERA SINGLE-PIN PUMP-COASTDOWN
EXPULSION AND REENTRY TEST

by

R. E. Henry, R. M. Singer, D. J. Quinn,
E. A. Spleha, E. G. Erickson, and W. C. Jeans

ABSTRACT

Transient boiling results are reported for sodium boiling in a narrow annular channel with a high heat-flux input. Boiling is initiated by exponentially decreasing the liquid coolant flow rate in a manner similar to that expected for an LMFBR system in the event of a loss of power to the primary pumps. The test-section power (single pin) was maintained at a constant value during this flow transient. Analysis of the data shows that boiling inception occurred at the end of the heated zone at a small bulk superheat. The early voiding pattern is heavily influenced by the local condensation potential in the vicinity of boiling inception, and initial flow pattern, while reflecting some multibubble behavior, appears to be annular in nature. The vapor zone grows preferentially downstream until that region becomes saturated, at which time inlet flow reversal occurs. Following inlet flow reversal, no multibubble character is observed within the voided zone. Coolant reentry through the gas plenum region was only observed early in the voiding transient.

I. INTRODUCTION

The primary objectives of the OPERA single-pin (of 22.5-in. heated length) pump-coastdown test were to:

- (1) obtain detailed experimental results on the coolant behavior during the early stages of voiding (until heater failure occurred);
- (2) obtain definitive results for the two-phase flow patterns generated due to the voiding transient, i.e., single-bubble, multibubble, wavy annular, etc.;
- (3) demonstrate the sensitivity of the voiding transient to large bulk subcooling (this will aid in understanding some available in-pile results).

The annular geometry used in these tests, although far removed from the geometry of a reactor subassembly, does present a well-defined system

which affords a test of the physical assumptions made in the current voiding models. Although the general conditions for the test were considerably different from those of a reactor (heated length, gas-plenum region, etc.) characteristics of the FFTF were used where possible in determining the test parameters.

II. EXPERIMENTAL APPARATUS

This experiment served as a proof test for the Out-of-pile Expulsion and Reentry Apparatus (OPERA). A schematic of the facility, which is a onecethrough (blowdown) system is shown in Fig. 1. Coolant flow through the

Fig. 1. Schematic of OPERA Loop.

a cartridge-type unit, 0.230 in. in diameter, with a coiled Nichrome V heating element, 22.5 in. long. Electrical resistance, and hence the heat flux, was uniform over the axial length. The downstream end of the heater was grounded to the sodium. The outer wall of the test section was of 0.500-in.-OD, 0.375-in.-ID stainless steel tubing, providing an annular coolant gap of 0.073 in. This annular spacing was maintained by 0.070-in. wire wrapped on a 12-in. axial pitch. Since the wrap itself never provides support in opposing directions, the heater would be free to move away from the wire wrap or "snake." A reactor fuel pin within a wire-wrapped subassembly will be supported from two opposing directions at intervals of $1/6$ the axial pitch, or every 2 in. for a wrap with a 12-in. axial pitch. To minimize snaking and to support the pin in a manner characteristic of a subassembly, short wire "starts" (of 0.70-in. diameter) were spot-welded onto the heater sheath at 2-in. intervals and 180° out of phase with the wire wrap. The resulting configuration is shown in Figs. 4 and 5.

test section is generated by pressurizing the blowdown vessel with argon gas and opening the control valve at the inlet to the test section. The pump-coastdown flow transient was generated by depressurizing the blowdown vessel through a variable flow resistance which could be preset by calibration procedures to give the desired flow decay.

A photograph of the facility with the single-pin test section installed is shown in Fig. 2. As shown, the apparatus has been lifted from the sodium-service pit and the outer shroud has been removed.

Figure 3 illustrates the basic configuration of the single-pin test section, the inlet and outlet instrumentation, and several cross sections of the high-heat-flux electrical heater. The heater was

*Fig. 2. Photograph of the OPERA Facility.
ANL Neg. No. 900-2334.*

Fig. 3.
Schematic of Single-pin
Test Section.

Fig. 4. High-flux Heater (Overall View). ANL Neg. No. 900-2520.

Fig. 5. High-flux Heater (Closeup). ANL Neg. No. 900-2518.

In addition to instrumentation to measure the inlet and outlet pressures, temperature, and flow rate, shown in Fig. 3, the outer wall of the test section was outfitted with thermocouples and voltage taps. Their locations with respect to the end of the heated zone is given in Fig. 6. The pair of voltage taps that bracket the end of the heated zone are 0.2 in. apart, and the remaining taps are spaced upstream and downstream of the end of the heated zone at 0.5-in. intervals. The data from this instrumentation is discussed in Ch. IV.

In addition to establishing the flow-decay curve, the calibration procedures prior to the test required calibration of the electromagnetic flowmeters, pressure transducers, and thermocouples. These procedures are outlined below.

Flowmeters: Each vessel is equipped with internal thermocouples which are spaced at preset intervals. By using these thermocouples to determine the sodium level in a vessel, one can calibrate the differential pressure transducer for that vessel. These differential transducers can, in turn, be used to calibrate the flowmeters, since the time rate of change of the sodium head in the vessel is easily related to the mass flow rate. Many calibration runs were taken for the flowmeters, and since a very slight change with time was noticed, a calibration check was taken immediately prior to the experimental run.

Pressure Transducers: All the pressure transducers were strain-gauge units, mounted on standoffs to keep the temperature below 600°F. These units were calibrated by emptying the sodium from the test section and then calibrating

Fig. 6.
Test-section Instrumentation.

against a 0-60-psia gauge (with 0.05-psia subdivisions) attached to the receiver vessel. The standoffs for the two exit pressure transducers also contained shut-off valves to protect the transducers; thus, these measurements can be assumed to have a rather slow response time. Calibration checks were run on the pressure transducers immediately prior to the run.

Thermocouples: The inlet and outlet bulk thermal measurements were checked against the tank internal thermocouples and the standard tables, and no appreciable difference could be detected. Therefore, the standard tables were used throughout.

Several energy balances were taken for the system. These were accomplished by measuring the voltage and current applied to the heater, and comparing the product with the rise of coolant temperature at a given flow rate. At bulk temperatures of 900°F, a 97% energy balance was achieved. At higher bulk temperatures, the energy balance was somewhat worse due to increased heat losses.

Calibration runs were also taken to determine the frictional pressure-drop characteristics of the test section. The results are tabulated

in Table I. The total pressure drop (ΔP_t) experienced between the inlet

Table I. Frictional Pressure-Loss Calibration

Bulk Temp, °F	$\Delta P_t = P_i - P_o$, psi	W, lb _m /sec	ΔP_f , psi
700	21.0	0.502	19.8
700	17.0	0.452	15.8
700	13.0	0.386	11.8
700	7.8	0.295	6.6
700	5.8	0.245	4.6
700	3.8	0.170	2.6
700	2.8	0.128	1.6
500	19.9	0.502	18.6
500	15.0	0.422	13.7
500	9.0	0.315	7.7
500	4.4	0.190	3.1
500	23.7	0.570	22.4
500	8.5	0.320	7.2

and outlet (P_o) pressure transducers is a combination of losses due to acceleration, wall shear, expansion, and head drop. For simplicity all losses, except for the static head loss, have been lumped into a frictional pressure-loss term:

$$\Delta P_f = P_i - P_o - \rho h . \quad (1)$$

Figure 7 demonstrates the correlation between steady-state frictional pressure loss through the test section as a function of the flow rate. The equation

$$\Delta P_f = 68 W^{1.83} \quad (2)$$

is a representative function for the calibration data, where ΔP_f is in psi and W is in lb_m/sec. By using one-dimensional approximations and negligible pressure recovery at all area expansions, an estimate of the Moody friction factor can be obtained for the wire-wrapped region of the test section. Figure 8 illustrates the manner in which ΔP_f can be segmented. Because of the comparatively small length-to-diameter ratio between the end of region 2 and the outlet pressure tap, only the head drop is considered over that length. If the friction factors (f_1 and f_2) are assumed to be equal, the data given in Table I can be used to calculate the

Fig. 7. Frictional Pressure-drop Data for Test Section.

Fig. 8. Pressure Losses in Test Section.

dependence of the friction factor upon Reynolds number (N_{re}) for the wire-wrapped (region 1) portion of the heater. These results are compared to the curve for a smooth tube in Fig. 9. This illustration shows that the combination of wire wrap and wire starts has the same effect on the friction factor as wall roughness.

Fig. 9. Friction Factor for the Wire-wrapped Region.

The frictional pressure drop in the 1-in. pipe between the outlet and the free surface in the upper plenum of the receiver vessel can be neglected. Only the static head term ($\rho_l h$) needs to be considered; the vertical dimension characteristic of this term is 7.8 ft.

To provide assurance that the voltage taps were properly sequenced and operating as expected, a gas-injection line was installed at the test-section inlet. By injecting gas into the test section when it is filled with liquid and monitoring

the voltage-tap output, any anomalous behavior is readily apparent. Table II summarizes some of the dimensions and parameters discussed in this section.

Table II. Dimensions of Test Section

Heater OD	0.230 in.
Heated length	22.5 in.
Unheated gas-plenum region	7.5 in.
Wire-wrap diameter	0.070 in.
Wire-start diameter	0.070 in.
Outer-wall ID	0.375 in.
Outer-wall OD	0.500 in.
Flow area (wire-wrapped region)	0.065 in. ²
(neglecting wire starts)	0.000451 ft ²
Wetted perimeter (wire-wrapped region)	2.12 in.
(neglecting wire starts)	0.176 ft
Hydraulic diameter	0.123 in.
(neglecting wire starts)	0.010 ft

III. TEST CONDITIONS

This experimental investigation provided basic data about coolant expulsion and reentry behavior following a pump-coastdown transient under full-power conditions. Since these conditions are representative of a hypothetical whole-core accident, the ideal test parameters would be to coastdown from a steady-state test velocity of 25 ft/sec at a total pin power of 33 kW (11 kW/ft). It is also desirable to have essentially infinite inlet and outlet plenums such that flow reversal does not cause a significant increase in the inlet pressure. The OPERA facility provides an excellent simulation of the infinite inlet and outlet plenums. However, the single-pin test section, due to the shortened heater length, comparatively large sodium channel, and sizable thermal capacity of the outer wall is not suited for a coastdown beginning at 25 ft/sec, i.e., at 25 ft/sec and 11 kW/ft, the steady-state temperature rise of the coolant is only 105°F.

To compensate for these shortcomings, a reduced steady-state flow rate and an elevated temperature were used. Since the early single-phase domain of a whole-core pump coastdown is essentially quasi-steady state, these conditions can be viewed as picking up the FFTF flow-decay curve at several seconds into the transient. Figure 10 compares the flow transient used for the single-pin test and that calculated for the initial operation of the FFTF. The velocity-decay curves are equal at about 10 sec into the FFTF coastdown, with the single pin coasting down somewhat faster. To avoid confusion in discussing the results, the time scale will always be referred to FFTF flow coastdown as it is shown in Fig. 10.

Fig. 10. Experimental Flow Decay.

Fig. 11. Inlet and Outlet Temperatures during the Coastdown.

The inlet temperature for this test was 1060°F, and the total heater power was 20.1 kW (10.7 kW/ft) at steady state. These conditions along with the velocity in the test section shown in Fig. 10 correspond to a steady-state temperature rise of 302°F and, thus, an energy balance within 93%. When we consider the average temperature level of the test section, this energy balance is acceptable.

Figure 11 compares the inlet and outlet temperatures during the flow transient. The small temperature rise in the outlet temperature is a reflection of large thermal capacity of the outer wall. This effect is further demonstrated in Fig. 11 by comparing the measured outlet temperature with that calculated from a steady-state heat balance using the instantaneous flow rate. These aspects are discussed in the next section.

In order to estimate the incipient wall superheat at the onset of voiding, as described in Ref. 1, it is necessary to know the pressure-temperature history of cavities on the heated wall as well as the pressure-

temperature history at the sodium-argon interface.

- (a) Gas Interface - High-purity argon was the only gas used in the entire system. For all periods other than the short times (10 min) required for calibration runs, the system was maintained under an argon blanket at approximately 20 psia. Over a period of weeks, the bulk temperature was gradually increased up to 900°F, and this temperature was maintained for two weeks prior to the test. Therefore, it is reasonable to assume the sodium is saturated with argon gas at 900°F and 20 psia. One day before the test the temperature was increased to 1060°F. In this short time, it is unreasonable to assume that the argon could diffuse through and saturate the 11-ft column of sodium in the blowdown vessel. However, bounding calculations can be run with these numbers.

(b) Wall Cavities - Care was taken during the calibration procedures to avoid any deactivating condition that would be more severe than those experienced during the conduction of the test itself. The following sequence is believed to produce the most severe deactivation for this test:

1. Steady-state, isothermal (1060°F) flow was initiated with sodium that was saturated with argon gas at 900°F and 20 psia. This flow produced inlet and outlet pressures corresponding to those shown at $t = 0$ in Fig. 12.

Fig. 12. Inlet and Outlet Pressures and Total Pin Power during the Coastdown.

2. The power was slowly increased over a period of minutes until the full-power condition shown in Fig. 11 ($t = 0$) existed between the inlet and outlet plenums.

The inlet and outlet pressures, as well as the heater-pin power, are given in Fig. 12. The results of such measurements during the voiding transient are discussed in the next section.

IV. DISCUSSION OF RESULTS

The general experimental conditions, as well as the results of measurements of transient flow rate, heater power, and pressure, were presented in the preceding section. Although these results clearly define the single-phase portion of the flow transient, the most definitive instruments for detection of boiling and for mapping the voiding picture are the voltage taps. These voltage taps, which are driven by a small (10 A) DC current which is passed through the sodium coolant and the outer wall, enable one to

detect the location of liquid two-phase interfaces. It is important that the distinction be made between liquid-vapor and liquid-liquid phases because when voiding is initiated, the voltage taps primarily indicate a departure from the all-liquid condition. Therefore, temporal signals from the voltage taps can be utilized to describe axial regions of the coolant channel that are in an all-liquid condition and those which are not. Such a voiding picture for the 22.5-in.-heated-length, single-pin, pump-coastdown test is illustrated in Fig. 13. To help orient the reader, a schematic of the test section is shown on the left in the figure.

As shown in Fig. 13, inception of a two-phase region occurred at 17.40 sec at the end of the heated zone. This small void, which is barely distinguishable from the all-liquid case, then collapsed, and a second occurrence took place at the end of the heated length. The vapor produced progresses about 3 in. downstream before collapse. At 17.66 sec, boiling inception occurred approximately 1-3/4 in. upstream from the end of the heated length. For the next second, several more nucleation sites successively became active, driving liquid slugs up the channel. Meanwhile, vapor bubbles moved further and further down before condensing, thus warming up the outer wall and the unheated portion of the heater.

A comparison with the inlet-flowmeter data for this test, which is given in Fig. 14, shows that there is net positive flow until about 19.3 sec into the coastdown. The first decrease in the inlet flow is coincident with the rapid increases of inlet pressure shown in Fig. 12. In addition, the perturbations experienced by the inlet flow are of little consequence until 18.8 sec, at which time the flow decreased rapidly. From the void profile shown in Fig. 13, it is seen then that, at 18.8 sec into the coastdown, the unheated downstream section has achieved saturation, as have regions 3 in. upstream from the end of the heated length. This large voided (two-phase) region caused the frictional pressure drop to increase dramatically, resulting in a sharp decrease in the inlet flow, and, thus setting the stage for flow reversal at the inlet. This demonstrates that there is an excellent one-for-one correlation between the flowmeter and voltage-tap results.

It should be reiterated that the results shown in Fig. 13 simply describe the boundaries between what is all liquid and what is not all liquid. To illustrate this, the sketch on the right shows an instantaneous flow distribution, as indicated by the voltage taps at 18.3 sec, as it might appear in a circular channel. The most important aspects of this illustration are the multibubble character of the flow and the small local void fractions.

Figure 15 displays the outlet flowmeter results during the voiding transient. In contrast to results from the inlet flowmeter, a small but distinct reaction to inception of boiling is apparent, as is the small decrease in flow as the small void collapsed. At approximately 17.6 sec into the coastdown, another expulsion is recorded, which is in agreement with the voltage-tap results. Here again the excellent correlation between the flowmeter results and the voltage-tap data is evidenced.

By integrating the results from inlet and outlet flowmeters with respect to time and subtracting the mass entered from that which exited,

Fig. 13. Experimentally Measured Voiding Profile.

*Fig. 14.
Flow at the Test-section Inlet
Following Boiling Inception.*

*Fig. 15.
Flow from Test-section Outlet
Following Boiling Inception.*

one can calculate the voided mass and, hence, the voided volume. This was accomplished by digitizing the data in 0.0125-sec intervals and numerically integrating the results. (The tabulation of data from inlet and outlet flowmeters and from pressure transducers is given in Appendix A.) If the length of channel voided is taken from Fig. 13, an average void fraction can be calculated for the voided region. The results of such calculations are shown in Fig. 16. Such calculations are reliable only as long as the

Fig. 16. Average Void Fraction in Test Section during Voiding.

Fig. 17. Temperature Profiles in Test Section during Voiding.

demonstrates the level of confidence that may be placed in these measurements. (The rise in temperature after the end of the heater zone is due to the measurement being an outer wall type instead of a true bulk measurement.) During the flow decay, the outer-wall temperature rises rapidly, the end of the heater zone having the maximum temperature. At inception of boiling, coolant and wall subcooling increase in both directions from the end of the heater zone, the rate of increase being far greater in upstream direction. Therefore, as voiding begins, the void progresses preferentially downstream because net positive flow is maintained and because

the upper and lower limits of the vapor-phase boundary are "in view" of the voltage taps, which is until 18.55 sec into the transient. By far the most striking result is the low average void fraction during the early stages of voiding. This is specifically due to the sizable thermal capacity of the outer wall. The thermal lag of the outer wall provides a large heat sink to condense the vapor volume early in the voiding. This is substantiated by the results from the outer-wall thermocouple shown in Fig. 17. At the location and time of inception, the temperature of the outer wall is 70°F below the saturation temperature at the local pressure ($T_{\text{sat}} = 1595^{\circ}\text{F}$, $P = 13.1$ psia).

The temperatures shown in Fig. 17 do not reflect any rapid thermal transients in the coolant channel because of the sizable thermal time constant of the outer wall. However, such data are representative of the average behavior during the flow transient and present a picture of the voiding process that is consistent with the voltage-tap results given in Fig. 13. The agreement between the outer-wall temperature rise at $t = 10.0$ sec and the steady-state energy balance

the subcooling is much less in that direction. As illustrated in Fig. 17, this movement of the vapor quickly reduces the subcooling in unheated downstream portion ($t = 18.6$ sec) so that this entire region is then voided as shown in Fig. 13. A comparison with the results from the outlet thermocouple for the same times shows the extreme subcoolings downstream of the heater. However, this is of little consequence because the region with the small hydraulic diameter, namely, the downstream unheated portion of the annular coolant channel, has been voided at $t = 18.6$ sec, and the large two-phase pressure drop in this region means that it has become very difficult to transport the vapor generated in the heated region out to where the cold structure and coolant are located.

The dramatic increase in the two-phase frictional pressure drop is known as flooding, and the fact that such a phenomenon exists can be illustrated by a simple calculation. As shown in Ref. 2, the onset of flooding for a given system is determined by the liquid and vapor velocities. For this demonstrative calculation, the liquid velocity can be neglected, in which case the correlation for turbulent flow in both components is simply

$$0.6 = u_v \left[\frac{\rho_v}{g D_h (\rho_\ell - \rho_v)} \right]^{\frac{1}{2}} . \quad (3)$$

For the conditions of this experiment, flooding would begin at a vapor velocity of about 19 ft/sec. If it is assumed that all the energy generated per unit length in the heater is used to generate vapor over the length of voided heater region, the vapor flow rate is given by

$$W_v = q_\ell / h_{fg} . \quad (4)$$

If it is assumed that all the vapor flows downstream, then from continuity

$$u_v = W_v / \rho_v \alpha A . \quad (5)$$

From Fig. 13, the voided heated length at 18.6 sec is 1.3 in., and if the local void fraction is assumed to be 1.0, the vapor velocity is 100 ft/sec, which means the downstream unheated region is experiencing the flooding phenomenon if only one-fifth of the vapor generated travels downstream. Therefore, the saturation of the unheated downstream region, which has occurred at about 18.6 sec into the coastdown, is the paramount condition for establishing downward voiding and thus reversal of inlet flow. With regard to Figs. 13 and 17, after downstream saturation and reversal of inlet flow were achieved, multibubble flow patterns were no longer witnessed.

In the above discussion, it was assumed, for the sake of calculation, that the local void fraction was 1.0. Selected voltage-tap signals were recorded on magnetic tape along with the necessary calibration data. If it is assumed that none of the liquid in a given interval exists as a discrete globule or droplet, then the voltage-tap recordings for that interval can be reduced to average void fractions for that locale. The DC current through the interval was essentially constant at 10 A; hence, the change in voltage drop across an interval reflects a change in local resistivity:

$$V/I = \frac{1}{\frac{1}{R_{St}} + \frac{1}{R_{Na}}} \quad (6)$$

Effectively R_{St} is constant and equal to the value for the empty channel, and the resistance of the coolant over a given interval is

$$R_{Na} = \gamma L / A_\ell \quad (7)$$

Since the current is constant during the transient, Eq. 6 can be written for the two-phase and completely voided conditions such that the ratio of these two can be expressed as

$$\frac{\Delta V_{2\phi}}{\Delta V_E} = \frac{\frac{1}{R_{St}}}{\frac{1}{R_{St}} + \frac{A_\ell}{\gamma L}} \quad (8)$$

or

$$\frac{\Delta V_{2\phi}}{\Delta V_E} = \left[\frac{R_{NaF}}{R_{NaF} + (1 - \alpha)R_{St}} \right] = \left[1 + \frac{(1 - \alpha)R_{St}}{R_{NaF}} \right]^{-1} \quad (9)$$

The structural resistivity is calculated from the single-phase signal just prior to inception, assuming the resistivity of liquid sodium corresponds to the value at saturation.

To ascertain the level of confidence that can be placed in these measurements, the data from the increment 0.5-1.0 in. downstream is compared to the average void fractions deduced from the integrated flowmeter results in Fig. 18. Since this increment is in close proximity to the end of the heated length, one would expect the local void fraction to be generally higher than that averaged over the entire voided length, and this is generally the case. Also, since the whole downstream end is voided at

Fig. 18.
A Comparison between the Local and Test-section Average Void Fractions during Voiding. Voltage-tap Increment 0.5-1.0 in. Downstream.

18.55 sec, one would expect the two techniques to give approximately the same results for these times removed from inception. Figure 18 shows this indeed to be the case and, thus, adds a great deal of credence to the local measurements. A similar comparison is shown in Fig. 19 for a different axial segment, and similar agreement is demonstrated. Figures 20-23 exhibit local void fractions in the coolant at several axial increments as a function of time during the voiding transient. As alluded to above, the void fractions are rather small, and values of 0.70 to 0.80 are approached after considerable time. This may reflect the large heat sinks in this particular test geometry. It must be remembered that the analysis which yields the local void fractions assumes that the liquid sodium is in an axially uniform continuous film and that entrained droplets can be neglected.

If local void fractions are compiled at specific times, profiles of axial void fractions can be generated. Such profiles are presented in Fig. 24 at four different times in the voiding transient. The behavior is in good agreement with the results from the outer wall thermocouple shown in Fig. 17, i.e., the early voiding causes the downstream region to saturate quickly, as evidenced by the almost uniform void profile in that region, which results in a high frictional pressure-drop path for the vapor. This is a necessary condition for producing flow reversal at the inlet. The dip in the void profile at the end of the heated length is due to a wire start that spans this interval. This wire start essentially presents two more unheated walls to retain a sodium film and, hence, the lower void fraction. That such is the case can be verified by calculating the liquid fraction, which is unity minus the void fraction, at any time and compare the value for the end of the heated length with any other interval. For times after flooding, the liquid fraction at the end of the heated length is twice that for the other intervals.

The oscillatory behavior of the local void fraction demonstrated in Figs. 20-23 after 19.0 sec into the coastdown can be correlated with the oscillations of inlet flow.

If compressible effects are neglected, the motion of the liquid coolant in the heated region is a function of the pressure difference across

Fig. 19. A Comparison between the Local and Test-section Average Void Fractions during Voiding. Voltage-tap Increment 1.5-2.0 in. Downstream.

Fig. 20. Local Void Fractions in Coolant during the Voiding Transient. Voltage-tap Increment 0.5-1.0 in. Downstream.

Fig. 21. Local Void Fractions in Coolant during the Voiding Transient. Voltage-tap Increment 1.5-2.0 in. Downstream.

Fig. 22. Local Void Fractions in Coolant during the Voiding Transient. Voltage-tap Increment 2.5-3.0 in. Downstream.

Fig. 23. Local Void Fractions in Coolant during the Voiding Transient. Voltage-tap Interval 4.5-5.0 in. Downstream.

Fig. 24. Profiles of Axial Void Fraction during the Voiding Transient. Voltage-tap Intervals Recorded on Magnetic Tape.

the region. If, in addition, a slug-type flow pattern is assumed, the inlet-flowmeter and inlet-pressure data can be combined to estimate the behavior of the local pressure at the end of the heated zone. For times when $dV_{\ell 1}/dt = 0$, the one-dimensional momentum equation can be written as

$$P_i - P_b = \frac{W^2}{2\rho_{\ell}A_1^2} + f_1 \frac{L}{D} \frac{W^2}{2\rho_{\ell}A_1^2} + \rho_{\ell}L_{\ell} \quad (10)$$

where L is the length of the liquid slug. Since this is only an approximate calculation, f_1 can be assumed constant at 0.035 and $49 \text{ lb}_m/\text{ft}^3$ can be used as an average density. As discussed in Ch. II, the measured pressure at the inlet is only indicative of the average pressure. For these illustrative calculations, a constant value 14.7 psia can be assumed. If the length of the liquid slug is taken from Fig. 13, estimates can be obtained for the pressure within the voided heated length. The results of these calculations are given in Fig. 25, and they substantiate the existence of

Fig. 25. Estimated Pressure within the Voided Zone.

the flooding phenomenon in the unheated downstream region. Between 18.2 and 18.5 sec into the coastdown, the driving force across the downstream region ($P_b - P_o$) increases from 0.8 to 1.3 psi; this time interval, as shown by Fig. 13, corresponds to a period when the downstream region reaches saturation and is voided over almost the entire length. This increase in frictional pressure drop is even larger because, at inception, 0.16 psi of the difference $P_b - P_o$ is static head, and at 18.6 sec there is essentially no static head exerted in this region. Therefore, at this time, the frictional pressure drop in the downstream region has increased by

approximately a factor of two over its value at inception.

When the heater was extracted from the system, its appearance was exactly the same as shown in Fig. 4. No discernible damage could be detected on the outside of the heater. Radiographs of portions near the end of the heated length (see Fig. 26) show a simple melting of the nichrome V element. However, it is also apparent from the figure that considerable melting occurred at other axial locations. From the locations of the melted regions and that of the wire wrap, it is reasonable to conclude that the melting was preferentially oriented away from the wire wrap. This is a strong indication that the wire wrap retains a sodium film which provides considerable cooling for that location well into the voiding transient.

*Fig. 26. Radiograph of the Failed Section of the Heating Element.
ANL Neg. No. 900-2874.*

V. CONCLUSIONS

From the discussion in the preceding section, several significant conclusions can be enumerated.

1) Inception was a mild phenomenon barely distinguishable on the outlet flowmeter and the voltage-tap signal. Since the outer wall was highly subcooled at inception, the liquid superheat at the heater wall can only be estimated.

2) The early voiding pattern is characterized by a multibubble flow regime. This appears to result from a successive activation of nucleation sites lying further upstream. When inception occurs at these sites, liquid slugs are driven up the coolant channel. However, this may be peculiar to this particular design of test section.

3) During the early stages of the voiding transient, net positive inlet flow preferentially carried the vapor downstream, where it condensed and heated the outer wall of the test section and the unheated portion of the heater pin.

4) After the downstream region had achieved saturation, as demonstrated by essentially uniform temperature and void-fraction profiles, the flooding phenomenon caused a dramatic increase in the frictional pressure drop in this region.

5) Once flooding was established, the vapor generated in the heated zone could not be easily moved through the downstream region. This led to pressurization of the heated zone and thus to flow reversal at the inlet.

6) When downstream saturation was reached and net positive flow had been considerably reduced, the multibubble character of the flow disappeared.

7) The voltage-tap data show that for this type of flow transient (pump coastdown) reentry does not occur after the downstream region is saturated. Prior to saturation, some partial reentries were observed.

8) Radiographs of the high-flux heater showed that the element melting was preferentially oriented away from the wire wrap. This indicates the wrap retained a sodium film which provided additional cooling.

APPENDIX A

Data Tabulation

Time (sec)	Inlet Flow (lb _m /sec)	Exit Flow (lb _m /sec)	Pressure		
			Inlet P _I (psia)	Exit P ₀₁ (psia)	P ₀₂ (psia)
17.6000	0.095	0.095	14.760	12.750	12.500
17.6125	0.095	0.094	14.965	12.800	12.500
17.6205	0.095	0.096	15.170	12.800	12.500
17.6375	0.095	0.098	14.555	12.800	12.500
17.6500	0.095	0.098	14.555	12.750	12.550
17.6625	0.094	0.099	14.555	12.650	12.550
17.6750	0.094	0.100	14.555	12.600	12.600
17.6875	0.094	0.102	18.450	12.700	12.550
17.7000	0.094	0.109	14.350	12.750	12.500
17.7125	0.094	0.111	14.145	12.800	12.450
17.7250	0.094	0.109	17.015	12.800	12.400
17.7375	0.094	0.098	13.940	12.850	12.350
17.7500	0.093	0.080	15.580	12.850	12.350
17.7625	0.095	0.067	14.760	12.800	12.350
17.7750	0.096	0.077	15.170	12.750	12.400
17.7875	0.095	0.093	14.760	12.700	12.450
17.8000	0.094	0.102	14.760	12.650	12.550
17.8125	0.094	0.111	14.760	12.600	12.650
17.8250	0.094	0.121	14.965	12.500	12.650
17.8375	0.093	0.122	14.965	12.500	12.650
17.8500	0.093	0.120	15.170	12.550	12.600
17.8625	0.093	0.116	14.965	12.650	12.550
17.8750	0.092	0.100	14.965	12.800	12.450
17.9000	0.092	0.089	14.965	12.750	12.400
17.9125	0.091	0.072	15.170	12.750	12.400
17.9250	0.091	0.065	14.965	12.750	12.350
17.9375	0.090	0.067	14.965	12.800	12.350
17.9500	0.090	0.071	14.965	12.850	12.300
17.9625	0.087	0.077	14.965	12.850	12.300
17.9750	0.085	0.084	14.760	12.800	12.300
17.9875	0.081	0.085	14.760	12.750	12.350
18.0000	0.083	0.089	14.965	12.750	12.400
18.0125	0.090	0.091	14.965	12.750	12.450
18.0250	0.095	0.100	14.760	12.750	12.450
18.0375	0.100	0.109	14.760	12.700	12.400
18.0500	0.096	0.113	14.965	12.650	12.450
18.0625	0.091	0.117	15.170	12.550	12.500
18.0750	0.086	0.116	14.965	12.550	12.550
18.0875	0.081	0.114	14.965	12.550	12.600
18.1000	0.077	0.113	14.760	12.600	12.600
18.1125	0.075	0.114	14.760	12.600	12.550
18.1250	0.075	0.116	15.785	12.600	12.500
18.1375	0.075	0.114	14.350	12.650	12.450
18.1500	0.076	0.101	14.965	12.650	12.400

Time (sec)	Inlet Flow (lb _m /sec)	Exit Flow (lb _m /sec)	Pressure		
			Inlet	Exit	
			P _I (psia)	P _{O1} (psia)	P _{O2} (psia)
18.1625	0.080	0.094	14.760	12.650	12.450
18.1750	0.083	0.095	15.375	12.650	12.550
18.1875	0.087	0.093	14.965	12.650	12.600
18.2000	0.091	0.092	14.760	12.650	12.550
18.2125	0.091	0.087	14.965	12.700	12.500
18.2250	0.090	0.084	14.760	12.700	12.400
18.2375	0.089	0.083	14.760	12.750	12.400
18.2500	0.086	0.073	14.760	12.800	12.400
18.2625	0.085	0.069	14.965	12.750	12.450
18.2750	0.084	0.063	14.760	12.650	12.550
18.2875	0.084	0.063	14.965	12.600	12.700
18.3000	0.081	0.068	16.400	12.600	12.700
18.3125	0.079	0.074	15.170	12.700	12.500
18.3250	0.075	0.077	15.375	12.750	12.350
18.3375	0.073	0.076	15.170	12.850	12.300
18.3500	0.073	0.069	14.965	12.900	12.300
18.3625	0.072	0.062	14.965	12.900	12.300
18.3750	0.071	0.049	14.760	12.750	12.300
18.3875	0.070	0.037	14.760	12.700	12.300
18.4000	0.069	0.053	14.760	12.650	12.450
18.4125	0.069	0.071	14.760	12.600	12.600
18.4250	0.070	0.089	14.760	12.500	12.700
18.4375	0.070	0.105	14.760	12.450	12.650
18.4500	0.069	0.114	14.760	12.400	12.650
18.4625	0.067	0.117	15.990	12.500	12.600
18.4750	0.065	0.119	15.375	12.550	12.500
18.4875	0.066	0.113	15.990	12.700	12.450
18.5000	0.066	0.096	15.375	12.800	12.400
18.5125	0.067	0.080	13.940	12.850	12.350
18.5250	0.070	0.053	15.375	12.900	12.300
18.5375	0.074	0.027	14.760	12.850	12.250
18.5500	0.074	0.018	15.990	12.800	12.250
18.5625	0.070	0.022	15.170	12.850	12.250
18.5750	0.065	0.040	14.965	12.900	12.250
18.5875	0.065	0.067	14.965	12.950	12.250
18.6000	0.065	0.089	14.965	12.950	12.250
18.6125	0.066	0.101	15.375	12.950	12.250
18.6250	0.069	0.109	15.990	12.850	12.350
18.6375	0.072	0.111	14.555	12.750	12.400
18.6500	0.076	0.111	15.170	12.750	12.400
18.6625	0.077	0.131	14.760	12.750	12.350
18.6750	0.074	0.154	14.350	12.750	12.400
18.6875	0.069	0.167	14.965	12.700	12.450
18.7000	0.064	0.174	14.965	12.600	12.550
18.7125	0.060	0.174	14.760	12.400	12.750
18.7250	0.059	0.174	14.760	12.300	12.850
18.7375	0.059	0.165	14.965	12.150	13.000
18.7500	0.061	0.133	14.965	12.050	13.100

Time (sec)	Inlet Flow (lb _m /sec)	Exit Flow (lb _m /sec)	Pressure		
			Inlet	Exit	
			P _I (psia)	P ₀₁ (psia)	P ₀₂ (psia)
18.7625	0.065	0.107	15.990	11.950	13.100
18.7750	0.069	0.077	15.785	12.000	13.000
18.7875	0.073	0.062	15.580	12.200	12.950
18.8000	0.076	0.052	15.170	12.350	12.850
18.8125	0.079	0.045	14.965	12.450	12.750
18.8250	0.080	0.039	14.760	12.550	12.600
18.8375	0.080	0.027	14.760	12.650	12.500
18.8500	0.074	0.020	14.760	12.700	12.400
18.8625	0.067	0.071	14.965	12.850	12.300
18.8750	0.062	0.102	15.990	12.900	12.200
18.8875	0.057	0.049	15.580	12.950	12.150
18.9000	0.057	0.022	15.375	13.000	12.100
18.9125	0.056	0.012	15.375	13.050	12.050
18.9250	0.053	0.012	15.375	13.100	12.050
18.9375	0.045	0.017	15.785	13.100	12.000
18.9500	0.040	0.025	15.375	13.150	12.000
18.9625	0.039	0.036	15.375	13.200	12.000
18.9750	0.039	0.027	15.375	13.200	12.050
18.9875	0.041	0.067	15.170	13.150	12.150
19.0000	0.040	0.084	15.990	13.050	12.200
19.0125	0.034	0.102	14.965	12.900	12.400
19.0250	0.030	0.123	16.605	12.700	12.600
19.0375	0.030	0.125	15.170	12.500	12.750
19.0500	0.032	0.111	15.375	12.350	12.900
19.0625	0.037	0.089	15.375	12.250	12.900
19.0750	0.045	0.098	14.965	12.300	12.750
19.0875	0.050	0.113	15.580	12.400	12.650
19.1000	0.060	0.109	14.760	12.450	12.550
19.1125	0.067	0.109	14.965	12.600	12.500
19.1250	0.074	0.098	14.965	12.700	12.350
19.1375	0.076	0.109	14.965	12.750	12.250
19.1500	0.070	0.117	14.965	12.850	12.200
19.1625	0.061	0.107	14.965	12.900	12.150
19.1750	0.051	0.089	14.965	12.950	12.100
19.1875	0.040	0.062	15.170	12.950	12.100
19.2000	0.034	0.044	14.965	13.000	12.100
19.2125	0.031	0.027	14.965	13.000	12.100
19.2250	0.032	0.013	14.760	13.000	12.050
19.2375	0.035	0.004	14.965	13.000	12.050
19.2500	0.036	-0.004	14.965	13.000	12.100
19.2625	0.036	-0.004	14.965	13.000	12.150
19.2750	0.032	0.002	15.375	12.950	12.200
19.2875	0.027	0.007	15.170	12.850	12.300
19.3000	0.023	0.007	14.965	12.700	12.400
19.3125	0.018	0.009	15.170	12.550	12.600
19.3250	0.014	0.013	15.375	12.450	12.750
19.3375	0.010	0.018	14.965	12.350	12.800

Time (sec)	Inlet Flow (lb _m /sec)	Exit Flow (lb _m /sec)	Pressure		
			Inlet	Exit	
			P _I (psia)	P _{O1} (psia)	P _{O2} (psia)
19.3500	0.007	0.031	14.965	12.350	12.800
19.3625	0.003	0.044	15.375	12.400	12.800
19.3750	0.001	0.047	15.375	12.500	12.700
19.3875	0.0	0.044	15.375	12.650	12.550
19.4000	-0.004	0.047	15.375	12.800	12.450
19.4125	-0.010	0.049	14.965	12.900	12.350
19.4250	-0.015	0.042	15.375	13.000	12.200
19.4375	-0.021	0.051	15.170	13.050	12.150
19.4500	-0.026	0.057	15.375	13.000	12.150
19.4625	-0.032	0.042	15.170	12.900	12.250
19.4750	-0.035	0.033	15.170	12.800	12.400
19.4875	-0.035	0.022	15.170	12.700	12.500
19.5000	-0.035	0.020	14.965	12.550	12.700
19.5125	-0.033	0.025	14.965	12.450	12.800
19.5250	-0.031	0.040	14.965	12.350	12.800
19.5375	-0.026	0.042	14.965	12.300	12.800
19.5500	-0.022	0.049	14.965	12.350	12.800
19.5625	-0.016	0.061	14.965	12.450	12.750
19.5750	-0.011	0.062	14.760	12.500	12.650
19.5875	-0.003	0.052	14.760	12.600	12.550
19.6000	0.005	0.043	14.760	12.750	12.500
19.6125	0.012	0.036	14.965	12.800	12.450
19.6250	0.019	0.028	14.965	12.850	12.300
19.6375	0.024	0.020	14.965	12.900	12.200
19.6500	0.030	0.013	15.785	12.950	12.150
19.6625	0.034	0.011	15.375	13.000	12.100
19.6750	0.036	0.006	15.375	13.150	12.050
19.6875	0.036	0.005	15.785	13.250	12.000
19.7000	0.035	0.003	15.785	13.300	11.950
19.7125	0.031	0.0	15.785	13.300	11.950
19.7250	0.028	0.0	15.990	13.250	11.950
19.7375	0.025	0.0	15.785	13.150	12.050
19.7500	0.021	-0.015	15.785	13.000	12.250
19.7625	0.015	-0.019	15.785	12.800	12.400
19.7750	0.010	-0.015	15.785	12.700	12.550
19.7875	0.002	-0.005	15.785	12.550	12.650
19.8000	-0.002	-0.002	15.580	12.500	12.700
19.8125	-0.009	0.003	14.965	12.500	12.650
19.8250	-0.015	0.036	14.965	12.500	12.550
19.8375	-0.021	0.061	14.965	12.550	12.500
19.8500	-0.026	0.069	14.760	12.600	12.350
19.8625	-0.033	0.074	14.555	12.650	12.300
19.8750	-0.036	0.071	14.555	12.700	12.250
19.8875	-0.039	0.066	14.760	12.800	12.250
19.9000	-0.039	0.056	14.350	12.850	12.200
19.9125	-0.036	0.044	14.555	12.850	12.250
19.9250	-0.031	0.036	14.555	12.800	12.250
19.9375	-0.025	0.027	14.350	12.750	12.350

Time (sec)	Inlet Flow (lb _m /sec)	Exit Flow (lb _m /sec)	Pressure		
			Inlet	Exit	
			P _I (psia)	P ₀₁ (psia)	P ₀₂ (psia)
19.9500	-0.019	0.022	14.350	12.700	12.400
19.9625	-0.009	0.020	14.350	12.600	12.500
19.9750	0.001	0.018	14.760	12.550	12.500
19.9875	0.009	0.018	14.965	12.600	12.500
20.0000	0.020	0.016	14.760	12.650	12.500
20.0125	0.029	0.015	15.375	12.700	12.450
20.0250	0.039	0.013	15.170	12.700	12.400
20.0375	0.044	0.012	15.170	12.800	12.350
20.0500	0.046	0.013	15.580	12.850	12.300
20.0625	0.046	0.010	15.375	12.900	12.250

APPENDIX B

Calculation of Incipient Superheat

As explained in the main text, the experimental conditions were controlled so that the most severe deactivation conditions would be known. The one parameter that is extremely difficult to control is the amount of argon gas dissolved in the sodium. A best estimate is that the gaseous concentration corresponds to saturation (Henry's law) at 1 atm and 900°F. For this condition, the partial pressure of the gas at 1060°F is 0.232 atm (3.4 psia).³ If a reentrant cavity model is used, then the deactivation condition is

$$P_l - P_v(T) - P_g(T) = \frac{2\sigma(T)}{r} ;$$

$$P_l = 13.1 \text{ psia} ;$$

$$P_v(1060^\circ\text{F}) = 0.29 \text{ psia} ; \quad (\text{B.1})$$

$$P_g(1060^\circ\text{F}) = 3.4 \text{ psia} ;$$

$$\sigma(1060^\circ\text{F}) = 0.01025 \text{ lb}_f/\text{ft} ;$$

$$r = 1.5 \times 10^{-5} \text{ ft} = 1.8 \times 10^{-4} \text{ in.} \quad (\text{B.2})$$

The saturation pressure and surface tension for sodium were taken from the tabular values in Ref. 4. If the amount of noncondensable gas in the cavity is assumed constant and also that it heats up in a constant-volume manner as the test-section power is brought up to the test condition, then the gas partial pressure at activation increases to 4.6 psia. With the liquid pressure at 13.1 psia, a temperature can be found such that a cavity of the radius given in Eq. B.2 reaches the limit of stability. This temperature is 1636°F as compared to the local saturation value of 1595°F. Therefore, the estimated liquid superheat at the wall at inception, based on the coolant conditions before and during the test, is 41°F. Such a value is in agreement with the benign expulsion witnessed at inception.

APPENDIX C

Comparison of the OPERA Single-pin Pump
Coastdown with SAS2A Predictions

The necessary geometric input parameters for this experiment are listed below:

Heater-pin diameter	= 0.5842 cm	(0.230 in.)
Wire-wrap diameter	= 0.1778 cm	(0.070 in.)
Inner diameter of can	= 0.9525 cm	(0.375 in.)
Thickness of can wall	= 0.1588 cm	(0.063 in.)
Heated length of pin	= 57.15 cm	(22.5 in.)
Unheated pin length downstream	= 19.05 cm	(7.5 in.)
Heater power	= 10.7 kW/ft	
Initial liquid velocity	= 2.79 m/sec	(9.15 ft/sec)
Initial mass flux	= 222.4 g/sec-cm ²	(455.5 lb _m /sec-ft ²)
Inlet temperature	= 571°C	(1060°F)
Initial temperature rise	= 162°C	(292°F)
Initial pressure at end of pin	= 1.014 atm	(14.90 psia)

Simulation of Experimental Initial Conditions by SAS2A

Once the geometric parameters used in Single-pin Pump Coastdown Test were used to generate the necessary input information for SAS2A, the next step was to simulate properly the experimental thermal-hydraulic characteristics. The initial, steady-state temperature rise was measured as 162°C, and the SAS2A input variables of power per node was adjusted to also yield this same rise. Due to the experimental heat losses, a somewhat smaller heater power was used in SAS than measured electrically. Using the experimentally measured friction factor, the friction correlation in SAS was adjusted to yield the same axial pressure profile as was experimentally determined. The agreement between the SAS2A profile and the experimental profile is shown in Fig. C.1.

The initial pressure level in the experiment was determined by a pressure tap downstream from the end of the heater. The pressure at the

Fig. C.1.
Comparison of Experimental and
Calculated Steady-state Axial
Pressure Profile.

end of the heater was calculated using the experimental friction factor and the liquid head to give a value of 1.014 atm. This value was then used as the reference value to compute the necessary SAS input information.

Simulation of Experimental Flow Transient by SAS2A

Since SAS2A required a table of values of the "inlet coolant driving pressure" as a function of time in order to compute values of the transient mass flux, and the measured inlet pressures did not correspond to the same basis as is used by SAS2A, it was necessary to construct a new pressure-time table that resulted in the same flow transient that was experimentally measured. By use of the experimental pressure transient at the inlet as a guide, such a table was formed by successive approximation. The computed flow transient is compared to the measured values in Fig. C.2.

Fig. C.2.
Comparison of Experimental and
Calculated Flow Transient Prior
to Boiling.

Comparison of Predicted and Measured Transient Behavior

Since the primary experimental measurements were of the outer-can (structure) temperature, mass flow rates at inlet and outlet, and the void profile, only these will be compared to the computed values from SAS2A.

The variation of the computed structural temperature with time, both prior to and during boiling, is shown in Fig. C.3, along with the measured temperatures. The com-

Fig. C.3. Comparison of Experimental and Calculated Structural Temperature.

puted curve is for $z = 57.2$ cm from the start of the heated zone; the experimental points are from $z = 57.2$ cm and 58.4 cm. Although the predicted temperatures are consistently larger than those measured during the transient, the rate of change of temperature with time shows good agreement. Since SAS does not include heat losses from the structure, which tend to increase with increasing temperature level, and the entire coolant and structure region is represented by only two nodes, a better agreement than that indicated should not be expected.

The experimentally determined times of boiling inception and heater melting were 17.4 and

Fig. C.4. Variation of Maximum Heater Temperature with Time.

20.4 sec, respectively, whereas SAS indicated values of 18.0 and 23.8 sec. The large difference in the time of failure is primarily due to the sensitivity of the heater temperature upon the state of the coolant, as indicated in Fig. C.4. The heater coil temperature, as predicted by SAS2A, is seen to rapidly and strongly respond to successive liquid-film dryout and rewetting. It is clear that the particular models used for dryout and rewetting will have an important impact upon the internal heater temperature, and thus upon the time of heater failure. As stated in the main text, after downstream saturation was achieved, a multibubble flow pattern was no longer apparent.

Prior to discussing the voiding dynamics and related flow oscillations, it is of interest to examine the axial and radial temperature profiles existing during

the transient. In Fig. C.5, the development of the axial profile for coolant temperature with time is illustrated, indicating its linear character prior to boiling ($t < 18.0$ sec). At the heater ends, at 76.2 cm, there was an abrupt change from annular to tubular geometry; this was accounted for in SAS2A by artificially extending the upper reflector beyond this point and changing the hydraulic diameter accordingly. Thus, predicted behavior in this region is subject to considerable question. An examination of the radial temperature profile in the test section, as shown in

Fig. C.5. Axial Profile of Coolant Temperature Prior to and after Boiling.

Fig. C.6. Radial Temperature Profile in Test Section during the Transient.

Fig. C.6, perhaps illustrates the serious difficulty one would expect from SAS2A predictions of coolant and structural thermal responses in this particular test section. The coolant and structure are represented by a total of two nodes; yet, in this single-pin arrangement, the coolant and structure represent approximately 79% of the test-section volume. On this basis alone it would be anticipated that SAS2A should be used for, at best, a qualitative assessment of single-pin behavior and might, in fact, be inadequate. If we keep this severe limitation in mind, the following discrepancies between the predicted and measured voiding dynamics cannot be considered to be a fair test of the ability of the code to predict multipin geometries, where the structural volume forms a much smaller fraction of the total system volume and, thus, is of considerably reduced importance.

The predictions for mass flow rates at the inlet and outlet are compared to the results of experimental measurements in Figs. C.7 and C.8, respectively. Figure C.8 is rather inconclusive except for the large reentry flows which were not witnessed in the experiment, but Fig. C.7 offers a very interesting comparison. SAS2A predicts the inlet flow rate should decrease to essentially zero 0.5 sec after inception and, as such, differs considerably from the experimental observations. This could be a result of the large thermal capacity (condensing

potential) in this test section and the difficulty SAS has in handling such conditions. If this is the case, the measured outlet flow rate should be less than the SAS prediction because more condensing is occurring than the model predicts, and Fig. C.8 shows this to be the case until 19.0 sec into the coastdown. As discussed in the main text, it is approximately at this point in time that flooding occurs, and condensation in the downstream gas-plenum region has been greatly reduced because saturation has been achieved.

Fig. C.7.
Comparison of Measured and Calculated
Mass Flow Rates at the Inlet.

Fig. C.8.
Comparison of Measured and
Calculated Mass Flow Rates
at the Outlet.

Figure C.9 compares the voiding profiles as measured in the experiment with those predicted by SAS2A for two different condensation

Fig. C.9. Comparison of Measured and Calculated Voiding Profiles.

coefficients of 60 W and 600 W/cm²-°C. The solid and dashed lines trace out the predicted voiding profiles for these respective coefficients. As shown in the illustration, a change of this order of magnitude has little effect on the early upstream-voiding behavior which is to be expected and a more significant effect on the downstream voiding which is also expected. The upstream-voiding comparison is in agreement with the discussion of inlet flow rate in that the measured upstream-voiding rate is much less than predicted by SAS; hence, net positive flow exists at a greater rate and for a longer time than predicted. Downstream-voiding behavior is roughly bracketed by the SAS predictions; however, it must be remembered that the early voiding is characterized by small local void fractions which differ from the high void-fraction configuration utilized in the SAS model. Therefore, the voiding profiles may demonstrate similarity, but the actual liquid displaced differs considerably, as discussed above.

REFERENCES

1. R. E. Henry, *Incipient Boiling Superheat in a Convective Sodium System*, Trans. ANS, 15, 410 (June 1972).
2. G. B. Wallis, *One-dimensional Two-phase Flow*, McGraw-Hill Book Co., Inc., New York (1969).
3. E. Veleckis, R. Bloomquist, R. Yonco, and M. Perin, "Solubility of Argon in Liquid Sodium," *Chemical Engineering Division Semiannual Report, July-December, 1966*, ANL-7325, p. 128 (Apr 1967).
4. G. H. Golden and J. V. Tokar, *Thermophysical Properties of Sodium*, ANL-7323 (Aug 1967).