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A STUDY OF THE
INCIPIENT BOILING OF SODIUM

by

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TABLE OF CONTENTS

	<u>Page</u>
NOMENCLATURE	6
ABSTRACT	7
I. INTRODUCTION	8
II. INCIPIENT NUCLEATION IN POOL BOILING	8
A. Preboiling System History	8
B. Incipient Nucleation Conditions	10
C. Experimental Apparatus and Test Procedures	12
1. Test Apparatus for Out-of-pile Tests	12
a. Mirror-surface Test Vessel and Apparatus	12
b. Sandblasted-surface Test Vessel	15
2. In-pile Test Apparatus	15
3. Out-of-pile Test Procedures	16
4. In-pile Test Procedures	17
D. Experimental Results	17
1. Preboiling System History	19
2. Heat Flux	21
3. Dissolved Impurities	23
4. Type of Inert Cover Gas	23
5. Nuclear Radiation	24
III. INCIPIENT NUCLEATION IN FORCED CONVECTION	24
IV. APPLICATIONS OF SUPERHEAT MODEL TO LMFBR's	27
V. SUMMARY AND CONCLUSIONS	28
APPENDIXES	
A. Inert-gas Bubble Dynamics	31
1. Model Development	31
2. Calculated Results	33
B. Discussion of Boiling-surface Characteristics	38
ACKNOWLEDGMENTS	41
REFERENCES	42

LIST OF FIGURES

<u>No.</u>	<u>Title</u>	<u>Page</u>
1.	Typical Surface Cavity during Liquid Penetration	9
2.	Typical Surface Cavity at Incipient Nucleation.	11
3.	Isometric Drawing of Sodium-superheat Test Apparatus	13
4.	Sodium-superheat Test Vessel.	14
5.	Photograph of Sodium-superheat Test Apparatus	14
6.	In-pile Sodium-superheat Test Vessel.	15
7.	Variation of Incipient Superheat with Time for $T_{\text{sat}} \approx 700^{\circ}\text{C}$. . .	18
8.	Variation of Incipient Superheat with Time for $T_{\text{sat}} \approx 900^{\circ}\text{C}$. . .	18
9.	Comparison of Data with Predictions for $q_w \approx 4.5 \text{ W/cm}^2$	20
10.	Comparison of Data with Predictions for $q_w \approx 5.2 \text{ W/cm}^2$	20
11.	Comparison of Data with Predictions for $q_w \approx 6.0 \text{ W/cm}^2$	21
12.	Effect of Surface Roughness upon Superheat for $q_w \approx 5.8 \text{ W/cm}^2$	22
13.	Effect of Surface Roughness upon Superheat for $q_w \approx 4.6 \text{ W/cm}^2$	22
14.	Effect of Heat Flux upon Superheat	23
15.	Effect of Dissolved Impurities upon Superheat.	23
16.	Effect of Type of Cover Gas upon Superheat	24
17.	Typical LMFBR Temperature Profiles at Incipient Boiling . . .	28
18.	Superheat Requirements for Various LMFBR Operating Conditions	28
19.	Effect of Liquid Velocity upon Bubble Dynamics	33
20.	Effect of Initial Bubble Size upon Bubble Dynamics	34
21.	Regions of Bubble Collapse and Growth in the Heat-flux- Velocity Plane.	35
22.	Sodium Temperature Profiles at Incipient Boiling from Entrained Bubbles	36
23.	Calculated Forced-convection Incipient Superheats with Entrained Bubbles	36
24.	Photomicrographs of Polished Heater Surface.	39
25.	Photomicrographs of Sandblasted Heater Surface.	40

NOMENCLATURE

English Letters

a	Channel diameter
A	Area
A,B	Constants in Henry's law
C	Molar concentration
c_p	Specific heat of liquid at constant pressure
h	Heat-transfer coefficient
k	Liquid thermal conductivity
K(T)	Constant in Henry's law
L	Channel length
L_0	Length of heated channel
M	Molecular weight of argon
Nu	Nusselt number
P	Pressure
Pr	Prandtl number
P_A^*	Partial pressure of inert gas at incipient nucleation
P_L^i	Pressure of liquid at maximum cavity-penetration conditions
Pe	Peclet number
q_w	Heat flux
R	Bubble radius
Re	Reynolds number
R_1	Coefficient depending upon Pe, Pr, and geometry
r_a	Radius of advancing surface cavity
r	Radius of curvature of vapor-liquid interface in cavity at maximum liquid penetration
T	Temperature
T_L^i	Temperature of liquid at maximum cavity-penetration conditions
t	Time
u	Velocity of liquid
x	Channel position

Greek Letters

α	Thermal diffusivity of liquid
ξ	Dimensionless axial position, defined in Eq. 11

Greek Letters

θ	Contact angle of liquid
$\kappa_1, \kappa_2, \kappa_3$	Empirical constants in heat-transfer correlation
λ	Friction factor
ν	Kinematic viscosity of liquid
ρ	Density of liquid
σ	Surface tension
τ	Dimensionless time, defined in Eq. 12

Subscripts

A	Inert gas
a	Advancing conditions
ap	Gas in plenum
e	Exit
eq	Equivalent
i	Initial conditions
L, ℓ	Liquid
mm	Mixed-mean value
m.p.	Melting point
nuc	Nucleation conditions
0	Initial conditions at start of heating or flow transient
p	Plenum
r	Receding conditions
sat	Saturation
ss	Steady-state conditions
T	Length of heated section and upper blanket
V	Vapor
w	Wall
1	Conditions at maximum cavity penetration (temperature decrease)
2	Conditions at maximum cavity penetration (pressure increase)

Superscripts

*	Conditions after step-change transient
'	Conditions due to temperature decrease

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ABSTRACT

In experimental studies that measure the amount of liquid superheat required to nucleate the liquid alkali metals, conflicting and generally insufficiently explained data have been presented. The incipient-boiling superheat has been shown or postulated to be affected by the pressure-temperature history, liquid pressure, heat flux, liquid purity, dissolved-gas content, liquid velocity, surface conditions, nuclear radiation, heating method, length of time of operation, and perhaps others. This report shows experimentally and theoretically that several of these effects are interrelated and discusses the relative importance of each.

A theoretical analysis has revealed that the heat-flux and heating-method effects upon the wall superheat are explainable by inert-gas transport between the liquid metal and the surface cavities. It has also been demonstrated that the "bulk" liquid superheat is affected by the heat flux and liquid velocity as well as by the method used to approach incipient boiling. Experiments have shown that the preboiling history of an experimental apparatus determines the size of the available nucleation sites and "overrides" initial surface-roughness variations (for surfaces with "mirror" to "sandblasted" finishes) and surface corrosion. This history effect (as well as inert-gas transport) also explains the measured variation of wall superheat with increasing time of operation.

This report also lists the essential parameters that are needed to characterize liquid-metal, incipient-boiling experiments.

I. INTRODUCTION

Experimental and theoretical studies concerned with measuring and predicting the superheat required for boiling inception have been of interest for a number of years. It was demonstrated¹ as early as 1922 that ordinary fluids could be superheated substantially above their normal boiling temperatures before nucleation. Superheating has recently received considerable attention for liquid-metal applications,²⁻⁵ particularly for fast-breeder nuclear-reactor systems. In these systems, the normally highly subcooled liquid metal could reach boiling temperatures during postulated accidents of loss of coolant flow or reactor power excursions. Since the rate of vaporization is related directly to the incipient superheat, this superheat must be predicted. This prediction can then be used in conjunction with theoretical models to determine the extent of damage resulting from various postulated reactor accidents and/or to aid in the design of instrumentation to initiate reactor shutdown to avoid damage.

Several experiments have been reported in which the incipient-boiling superheats of potassium and sodium were measured; however, the data show an extreme scatter ($\Delta T = 5^\circ\text{C}$ to 820°C) for presumably equivalent experimental conditions, and conflict in the effects of several of the parameters on the superheat. The parameters that have been postulated or determined to affect the incipient superheat are: pressure-temperature history, boiling pressure, heat flux, liquid purity, dissolved-gas content, heating-surface conditions, liquid velocity, nuclear radiation, heating method, length of time of operation, and perhaps others. This report demonstrates that several of these parameters are interrelated and that only a few of these are of major importance.

II. INCIPIENT NUCLEATION IN POOL BOILING

A. Preboiling System History

The importance of the system history of an experimental apparatus upon the incipient boiling superheat was recognized initially in Ref. 6; however, the analysis assumed that no inert gas was present in surface cavities. Inclusion of a constant inert-gas partial pressure was attempted in Ref. 5, and the agreement between the analysis and the experimental data was somewhat improved. However, it has been demonstrated⁷ that diffusion of inert gas out of surface cavities into the liquid solution is significant and can affect the incipient-boiling superheat. Therefore, the equivalent cavity model of Ref. 6 will be modified and its present shortcomings identified.

The operating history of an experimental apparatus before an incipient-boiling test and the physical condition of the liquid and heating surface are postulated to determine the size range of surface cavities

that are not completely flooded with liquid and hence are available as possible nucleation sites. The minimum wetted radius of a particular nonflooded cavity (corresponding to the maximum penetration of liquid into the cavity) is then used for calculating the critical radius for the subsequent nucleations. The conditions of maximum liquid penetration can be determined either by the specific conditions established by the experimenter, as in Refs. 3-5, or by generally uncontrolled variations in liquid pressure and temperature, as in Refs. 2 and 8. We will now study the former case in some detail and derive the equation determining the minimum wetted radius.

Figure 1 indicates that the depth of liquid penetration into a surface cavity increases as the liquid pressure is increased, and the vapor pressure and surface tension decrease as the temperature decreases. This may result in either an increase or a decrease in the penetration depth. If the initial steady-state conditions before maximum penetration are T_{Li} , P_{Li} , and P_{Ai} and the inert gas can either leave or enter the cavity, the final penetration depth will be determined by the precise sequence of changes made in T_{Li} , P_{Li} , and P_{Ai} . This is illustrated by the bubble-equilibrium equation at the initial conditions,

$$P_{Li} - P_L(T_{Li}) - P_{Ai} = \frac{2\sigma(T_{Li}) \cos[\theta_a(T_{Li})]}{r_{ai}} \quad (1)$$

113-1707

Fig. 1. Typical Surface Cavity during Liquid Penetration

If the liquid temperature is reduced from T_{Li} to T'_L , the following phenomena will occur, all of which can affect the liquid penetration depth and determine a new equilibrium condition: The vapor pressure in the cavity, P_V , will decrease, while the surface tension, σ , the inert-gas partial pressure, P_A , and the contact angle, θ_a , will increase. The contact angle increase with decreasing temperature has been experimentally measured in Refs. 9-11, and since the inert-gas solubility in the liquid metal decreases with decreasing temperature,¹² gas will be driven out of solution into the cavity gas space. Thus, the final equilibrium will be attained when

$$P_{Li} - P_V(T'_L) - P_{Ai} = \frac{2\sigma(T'_L) \cos[\theta_a(T'_L)]}{r_{ai}}, \quad (2)$$

where

$$P_{A1} > P_{Ai} \quad (3)$$

and

$$\theta_a(T_L) \approx \theta_a(T_{Li}). \quad (4)$$

The quantity P_{Ai} is difficult to determine and most likely must be assumed equal to P_{Ai} in order to calculate r_{a1} .

The second and most effective method of causing a prescribed maximum penetration is by increasing the liquid pressure at isothermal conditions. If this accomplished by increasing the partial pressure in the inert-gas blanket, the inert gas may diffuse through the liquid and come out of solution into the gas space in surface cavities. However, this effect can be minimized by having a large head of liquid between the heated surface and the gas blanket, and by maintaining a low liquid temperature in order to reduce the mass-diffusivity coefficient. Therefore, if the gas-blanket pressure is increased and the rate wetting in surface cavities is fast, relative to the rate of diffusion of inert gas in the liquid, the minimum wetted radius can be calculated from

$$r_{a2} = \frac{2\sigma(T_{Li})|\cos[\theta_a(T_{Li})]|}{P_L - P_V(T_{Li}) - P_{Ai}}. \quad (5)$$

The preceding discussion indicates that the diffusivity of inert gases in liquid metals and the wetting rates of liquid metals on solid surfaces (primarily stainless steel) must be known. However, information is not available on the former and is extremely sparse for the latter. In addition, the variation of the advancing contact angle with temperature is not well known, and its variation with liquid impurity content has been studied even less. Also, as pointed out by Chen,⁵ the normally measured value of θ_a , using relatively large liquid drops, may be inapplicable to the microscopic surface cavities under consideration. Available information on wetting of stainless steel surfaces by sodium¹¹ indicates that the contact angle is influenced strongly by the cleaning procedure used (this may or may not be of importance in microscopic cavities), and that the rate of wetting increases markedly as the temperature is increased and decreases as the dissolved oxide content increases. However, the precise variation of θ_a with temperature, impurity content, and cleaning procedure is not well known; hence, certain assumptions are necessary in order to use Eq. 5 to predict the critical-cavity size for incipient nucleation. These assumptions are discussed in Section B below.

B. Incipient Nucleation Conditions

As the temperature of the liquid is increased, the cavity pressure increases, causing the radius of curvature of the vapor-liquid interface to change sign, as shown in Fig. 2. Nucleation occurs when the conditions are reached such that the following relation is satisfied:

113-1708

Fig. 2. Typical Surface Cavity at Incipient Nucleation

$$P_V(T_w) + P_A^* - P_L = \frac{2\sigma(T_w) \left| \cos[\theta_r(T_w)] \right|}{r_{a2}}, \quad (6)$$

where r_{a2} is calculated from Eq. 5. Combining Eqs. 5 and 6 yields

$$P_V(T_w) - P_L = [P_L^i - P_V(T_{Li}) - P_{Ai}] \times \frac{\sigma(T_w) \left| \cos[\theta_r(T_w)] \right|}{\sigma(T_{Li}) \left| \cos[\theta_a(T_{Li})] \right|} - P_A^*, \quad (7)$$

where $P_V(T_w) - P_L$ is the pressure excess required for nucleation. The superheat requirement is then

$$\Delta T = T_w - T_{sat}, \quad (8)$$

with T_{sat} corresponding to the liquid pressure at boiling, P_L .

Since the inert-gas partial pressure in a cavity at nucleation is unknown, a priori, it must be assumed that no inert gas is lost from the cavity during the heating,⁵ or that the transient mass-transfer problem involving the dissolving and diffusion of inert gas must be solved.⁷ In Ref. 7, it was stated that the value of P_A^* depends upon the temperature at the start of heating, the wall heat flux, the initial inert-gas partial pressure, the liquid-metal pool depth, the nominal surface-cavity depth, and the transient temperature profile of the liquid.

As discussed in Section A above, the wetting contact angles of the advancing and receding interface in a surface cavity are not well known. Data in Ref. 11 indicate that for a chemically polished nickel plate in 225°C sodium containing ~5 ppm oxygen, the approximate values of θ_a and θ_r are 100 and 30°, respectively. Thus the ratio of the cosines in Eq. 7 is approximately 0.20 for this condition. Applicability of these results to microscopic surface cavities and the variation of this ratio with oxide content and temperature have not been determined; therefore, to use Eqs. 7 and 8 for predictive purposes, we must make some assumption concerning this variation (e.g., that the ratio is some prescribed constant).

The preceding analysis shows that quantitative prediction of the incipient-boiling superheat depends upon surface-phenomena data that are quite limited and sparse. However, several results implicit in the equations are significant and should be verified experimentally. In particular, Eq. 7 implies that the incipient-boiling superheat is independent

of surface roughness; that the superheat should increase as P_L^* is increased, or possibly as T_{Li} is decreased; and that dissolved impurities affect the superheat by changing the contact angle and the wetting rate. It is further implied that heat flux and heating techniques can affect the superheat only through variation of the inert-gas partial pressure. (This has been demonstrated theoretically in Ref. 7.) These implications will be examined in light of the new experimental data presented in Section C below.

C. Experimental Apparatus and Test Procedures

The three test vessels used to obtain the superheat data were a mirror-surface vessel and a sandblasted-surface vessel, which were run out-of-pile, and a smaller-scale mirror-surface vessel, which was run in-pile. These test vessels and the test procedures used to systematically examine the independent and combined effects of the variables affecting sodium superheat are described in the following paragraphs.

1. Test Apparatus for Out-of-pile Tests

a. Mirror-surface Test Vessel and Apparatus. Figure 3 is an isometric drawing of the sodium-superheat test apparatus, which was constructed from Type 304 stainless steel. The test vessel consisted of a $1\frac{1}{2}$ -in. nominal-diameter, Schedule 40, vertical pipe, containing sodium. The system pressure was controlled using an argon cover gas. Heating wires wrapped around the outside pipe wall supplied heat to the sodium, and cooling coils above the liquid-sodium level removed heat. Guard heaters were also employed for heating the system when test runs were not being conducted and for adjusting initial conditions.

Figure 4 is a photograph of the sodium-superheat test vessel. The inside surface of the test vessel was polished to an $8\text{-}\mu\text{in.}$ ($0.2\text{-}\mu\text{m}$) average surface roughness, and extreme care was taken to avoid weld material in the vicinity of the heated area.

Chromel-Alumel thermocouples were located in the liquid sodium, the test-vessel wall, and the gas blanket. These thermocouples were constructed from 0.012-in.-diam wires contained in a 0.062-in.-OD Inconel sheath and insulated with MgO.

The pressure in the gas blanket was measured with both a manometer and a pressure gauge, and the pressure transients caused by boiling were measured with a piezoelectric pressure transducer in contact with the liquid sodium.

The argon cover gas used to control the system pressure was purified by means of a magnesium perchlorate moisture remover, a NaK bubbler, and a NaK vapor trap, which condenses any NaK vapors that might be carried out of the NaK bubbler. This system is shown at the bottom of Fig. 5.

112-7891A

Fig. 3. Isometric Drawing of Sodium-superheat Test Apparatus

112-6765

Fig. 4. Sodium-superheat Test Vessel

112-6766

Fig. 5. Photograph of Sodium-superheat Test Apparatus

The dump tank shown in Fig. 5 was used to initially charge the test vessel with sodium and was also used for changing the sodium level in the test vessel.

The thermocouple signals were recorded on continuous-writing strip-chart recorders, and the pressure transients on an oscillograph and oscilloscope. The initiation of boiling was detected by several techniques: (1) Acoustic vibrations caused by sudden vapor growth were detected by using an accelerometer; (2) vaporization of the superheated sodium resulted in a sudden temperature drop in the liquid, which was measured by thermocouples immersed in the sodium; and (3) the vaporization caused a pressure pulse, which was recorded from signals from the piezoelectric pressure transducers. The acoustic vibrations were the most sensitive means to detect the instant of nucleation.

The first of the test vessels had a polished inside surface (average roughness of $0.2 \mu\text{m}$). Before the test apparatus was filled with sodium, the entire system was evacuated to 2×10^{-6} mm Hg at 1220°F , and liquid sodium at 600°F was forced into the test vessel from the dump tank using a slight pressure gradient. The sodium level was then adjusted to a prescribed height (measured using an X-ray fluoroscope), and the system pressure was set to a fixed low value (using purified argon).

113-147

Fig. 6

In-pile Sodium-superheat
Test Vessel

b. Sandblasted-surface Test Vessel.

The same test facility, instrumentation, and sodium were employed with a $250\text{-}\mu\text{in.}$ ($6.4\text{-}\mu\text{m}$) average-surface-roughness test vessel. This sandblasted-surface test vessel was installed into the system by (1) dumping the sodium from the mirror-surface test vessel into the dump tank, (2) cutting out the mirror-surface test vessel, (3) welding the sandblasted-surface test vessel into the system, and (4) employing the same vacuum-pumping and sodium-fill procedures as were used with the mirror-surface test vessel.

2. In-pile Test Apparatus

Figure 6 is a photograph of the in-pile sodium-superheat test vessel, which was operated in the core of the Juggernaut Reactor. Sodium was used as the heat-transfer fluid, and the entire system was constructed of Type 304 stainless steel. The sodium was contained inside of a 1-in. nominal-diameter, Schedule 40, vertical pipe. The system pressure was controlled by filling the volume above the sodium with argon gas and heating the gas to control the pressure. Resistance-type heating wires were wrapped around the heated section to supply heat to the sodium. This test vessel was placed inside a secondary containment vessel, which provided the double containment necessary to satisfy the in-pile safety requirements.

The assembled test apparatus was placed in the core of the Juggernaut Reactor. The undepressed neutron flux at the reactor centerline was approximately 2×10^{11} neutrons/ $\text{cm}^2\text{-sec}$, and the depressed flux in the sodium was estimated to be approximately 10^{11} neutrons/ $\text{cm}^2\text{-sec}$.

The inside surface of the test vessel was polished to a $10\text{-}\mu\text{in.}$ (average surface roughness) finish, and extreme care was taken to avoid

weld material in the vicinity of the heated area. Chromel-Alumel thermocouples were located in the liquid sodium and in the cover gas. These thermocouples were also constructed from 0.012-in.-diam wires contained in a 0.062-in.-OD Inconel sheath and insulated with MgO. The pressure in the cover gas was measured with a diaphragm-type pressure gauge.

The test vessel was evacuated to 10^{-6} Torr at 500°F, and a slight pressure gradient was used to force liquid sodium at approximately 500°F into the test vessel from the fill tank. The sodium level was set at a prescribed height using an X-ray fluoroscope. Argon gas was then supplied as a cover gas so that a system pressure of 5 psia was attained with the system at 500°F. This initial fill pressure and temperature provided for operating the experiment at 0.6 to 1.0 atm.

3. Out-of-pile Test Procedures

In the out-of-pile tests, the effects of surface condition, pressure-temperature history, heat flux, system pressure, dissolved-gas content, and impurity content was studied systematically. The study of these independent and combined effects required a long, tedious, experimental study, which yielded much data.

The requirement of investigating the effect of surface condition fixed the requirement of two different boiling surfaces; hence, two different test vessels. The 8- μ in. mirror-surface test vessel and the 250- μ in. sand-blasted test vessel were employed to provide two surfaces, which were significantly different.

Both test vessels were subjected to an identical sequence of pressure-temperature histories. After the establishment of each known pressure-temperature history, various tests were conducted to establish the independent and combined effects of heat flux and system pressure.

The effect of the dissolved-gas content (i.e., cover gas) was studied after the effects of surface condition, pressure-temperature history, heat flux, and system pressure were studied. The same test procedure was then used for these runs with the previously established pressure-temperature history.

The sixth variable tested in the static, out-of-pile tests was the impurity (oxide) content in the sodium. The effect of impurity content in the sodium was tested by adding impurities to the relatively clean sodium. Air was bubbled through the liquid sodium for several hours to add oxides to the liquid sodium. The same test procedure was then used during these runs.

The system was only capable of boiling sodium at pressures ranging up to slightly beyond 1 atm. Hence, after a pressure-temperature

history (for example, 60 psia and 1000°F) was established for the system, the pressure was reduced and tests were run in the 0- to 18-psia range.

During a typical experimental run, the following procedure was always followed:

a. An initial steady state was obtained at some specific temperature (always at least 500°F below the sodium boiling point) using only the guard heaters.

b. With all recorders operating, the primary heater was manually turned on, increasing the heat flux from essentially zero to a prescribed level in a step change, and maintained at this level.

c. After the sodium nucleated, no changes were made, and the steady-state boiling conditions were determined; or, if the system was at an unstable condition (i.e., repeated nucleation with significant periods of superheating), its fluctuating behavior was noted.

d. The system was then cooled, and steps a-c were repeated as the next run.

4. In-pile Test Procedures

The in-pile superheat tests were planned to obtain information concerning the effects of a reactor environment upon incipient-boiling superheats in sodium. Hence, the method of pressure control was designed to provide only a small range of pressure control. Test runs were taken out-of-pile, in-pile at zero power, and in-pile at various reactor power levels.

During all these experiment runs, the same test procedure that was employed with all the out-of-pile testing was used.

D. Experimental Results

According to the analysis in Ref. 7 and the earlier discussion, the incipient-pool-boiling superheat for a liquid metal of given purity should depend only upon the preboiling conditions at maximum liquid penetration (P_L , T_{Li} , and P_{Ai}), the heat flux (q_w), the temperature at the start of heating (T_0), the boiling pressure (P_L), or, equivalently, the saturation temperature (T_{sat}), the height of liquid above the heater, the nominal surface-cavity depth, and the inert-gas partial pressure at the start of heating (P_{A0}). To investigate this contention, we conducted a series of experimental tests in which all the above parameters were unchanged. However, the inert-gas partial pressure in the cavities could not be controlled externally, so periods of 25-150 min were allowed between test runs to permit the inert gas to reequilibrate in the apparatus. But, it is apparent from Figs. 7 and 8 that this procedure was unsuccessful. The

113-1704

Fig. 7. Variation of Incipient Superheat with Time for $T_{\text{sat}} \approx 700^\circ\text{C}$

113-1706

Fig. 8. Variation of Incipient Superheat with Time for $T_{\text{sat}} \approx 900^\circ\text{C}$

incipient superheats would increase from run to run and finally reach some maximum value. After several days at subcooled conditions, the cycle would repeat. This effect is caused by the loss of inert gas from the surface cavities; the "leveling-off" of the superheat apparently results when the amount of inert gas remaining in cavities is sufficiently small and the time between experimental runs is long enough to allow inert gas to return and the same initial conditions to be reached.

Loss of inert gas by heating and boiling and its effect upon the superheat are illustrated by the open squares in Fig. 8. The first two runs were conducted in rapid succession (~5 min apart) with all externally controllable variables fixed; the superheat increases from 44 to 115°C. The only reasonable explanation seems to be the loss of inert gas from the surface cavities.

These findings are disconcerting; if large variations in the superheat are shown to occur by cavity inert-gas losses, even though times of up to $2\frac{1}{2}$ hr were allowed between runs to permit the inert gas to return to the cavities, how can one parametrically study the effects of the other variables? One solution would be to allow 40-70 hr between tests, but this procedure would require an inordinate amount of time to obtain a statistically significant amount of data. An alternative would be to conduct a sequence of test runs in which all external variables are fixed until the measured superheat is unchanging with time. Then, one of the external variables (e.g., the heat flux) could be varied, and thus any subsequent trend in the superheat could be attributed to this variable. This approach was used to obtain the data presented in this report.

1. Preboiling System History

We have claimed that the most severe liquid penetration into surface cavities (as determined by the preboiling history) determines the critical bubble radius for nucleation and that this critical radius can be used in Eqs. 7 and 8 to calculate the incipient-boiling superheat.

Using the procedure discussed in Section C.3 above, we conducted a series of experimental runs in which only the preboiling conditions (P_L , T_{Li} , and P_{Ai}), were varied and the functional dependence of ΔT upon T_{sat} was determined. Figures 9-11 summarize the results of these tests and compares them to the predictions of Eq. 7 with $P_A^* = 0$ and

$$\{\cos [\theta_r(T_w)]\} / \{\cos [\theta_a(T_{Li})]\} = 1.$$

The qualitative and quantitative agreement between theory and experimental data is fairly good. Perhaps some of the scatter could be eliminated if the proper value of P_A^* was used with each data point and sufficient information was available on the contact angles.

113-1697

Fig. 9. Comparison of Data with Predictions for $q_w \approx 4.5 \text{ W/cm}^2$.

113-1698

Fig. 10. Comparison of Data with Predictions for $q_w \approx 5.2 \text{ W/cm}^2$.

113-1700

Fig. 11. Comparison of Data with Predictions for $q_w \approx 6.0 \text{ W/cm}^2$

An additional test of this model was to compare data obtained from systems with markedly different macroscopic surface roughness, but with equivalent preboiling histories. Two heating surfaces were used: one with a "mirror-like" finish (average surface roughness $\approx 0.2 \mu\text{m}$), and one with a "sandblasted" finish (average surface roughness $\approx 6.4 \mu\text{m}$); and tests were conducted using the same sodium. (A discussion of these surfaces appears in Appendix B.) The results of these tests (Figs. 12 and 13) show that it is not possible to distinguish any surface-roughness effect; thus we may conclude that the critical radius size for nucleation is determined by the preboiling history and not by the gross surface roughness.

2. Heat Flux

The analysis in Ref. 7 showed that the heat flux can affect the incipient superheat only through variations in P_A^* , the cavity inert-gas

113-2485

Fig. 12

Effect of Surface Roughness
upon Superheat for $q_w \approx$
5.8 W/cm²

partial pressure. Reference 7 also showed that an increase in the heat flux causes a decrease in the superheat, if only the heat flux is changed. But the present report has demonstrated that the cavity inert-gas partial pressure varies from run to run. The partial pressure should decrease with time, but this variation is generally uncontrollable. Therefore, in a series of experimental runs in which the heat flux is varied, it is possible to observe either an increase or decrease in the superheat with increasing heat flux, depending upon whether the heat flux is successively increased or decreased. This anomalous heat-flux effect is illustrated in Fig. 14, where the solid lines are from Ref. 7 for the variation of only q_w , and the dashed lines for hypothetical experiments in which q_w and P_A^* are varied. This phenomenon may be the reason for such a confused state of affairs in the reported effects of heat flux on the superheat. For example, it has been reported that with an increase in the heat flux the incipient superheat may increase,^{3,4,13,14} decrease,¹⁵ increase and decrease,² or be unaffected.^{16,17}

113-1699

Fig. 13. Effect of Surface Roughness upon Superheat for $q_w \approx 4.6$ W/cm²

113-1434

Fig. 14. Effect of Heat Flux upon Superheat

After tests were made with this sodium, a limited amount of oxygen was allowed to mix with the sodium, causing a change in the melting point from 97.8 to 101.1°C. The tests were then repeated, the resulting superheats being summarized in Fig. 15. The increase in dissolved oxide content caused a slight decrease in the incipient superheat. The decreasing incipient superheat with increasing dissolved oxide content is probably caused by the dissolved impurities increasing the wetting contact angle; hence, the incipient superheats are reduced. This effect has also been observed by Schultheiss and Smidt;¹⁸ however, more definitive experiments are required to clarify this effect.

4. Type of Inert Cover Gas

In addition to the tests previously discussed in which argon was used as a cover gas, further limited tests were conducted in which helium was used. The same sodium was used in both tests and reasonable precautions[†] taken to replace the argon with helium. The actual gas content was not measured. Figure 16 compares the results of the tests using helium to similar tests using argon; no effect can be detected. It is difficult to draw any conclusions as to this result because of the way the experiments were conducted, i.e., repeated boiling runs until the superheat would not change with time. This procedure effectively reduced the amount of gas trapped in surface cavities to a very small amount so that the role of the inert gas would be secondary to other system parameters.

[†]The sodium was dumped from the system, and repeated flushing with helium and thermal cycling was used.

The data reported in Refs. 3, 4, and 13 were obtained with the heat flux being successively increased, resulting in an increasing superheat; this is entirely consistent with the preceding discussion.

3. Dissolved Impurities

As stated in Section B above, a variation in the amount of dissolved impurities in the liquid can cause a change in the superheat by affecting the wetting contact angles. The extremely large superheats reported in Ref. 17 were apparently due to very efficient sodium purification prior to testing. In the present experiments, typical commercial (reactor)-grade sodium was used.

113-2488

Fig. 15. Effect of Dissolved Impurities upon Superheat

113-1703

Fig. 16. Effect of Type of Cover Gas upon Superheat

5. Nuclear Radiation

It is of some importance for studies of nuclear reactor safety to determine whether locally intense nuclear radiation can cause sodium to nucleate at a reduced superheat. An approximate analysis¹⁹ indicated that there would probably be no effect; however, there has been no experimental information confirming or rejecting this conclusion. Therefore, an apparatus similar to that described in Ref. 13 was placed in the Juggernaut Reactor and subjected to thermal-neutron fluxes up to approximately $2 \times 10^{11} \text{ n/cm}^2\text{-sec}$. The incipient-boiling superheat was not affected by this flux; however, since the radiation environment in a fast breeder reactor is much more severe than these conditions, further experiments in a higher-flux facility are necessary to resolve this point. Also, the incipient-boiling superheats measured in these in-pile tests are in excellent agreement with the incipient nucleation model presented in Sections A and B above.

III. INCIPIENT NUCLEATION IN FORCED CONVECTION

Some preliminary data²⁰ indicate that boiling inception is aided by the forced flow of the liquid; however, there does not now appear to be any generally accepted explanation for this effect. Whatever the cause, it is

necessary to predict the bulk-liquid superheat at nucleation as well as the wall superheat in order to determine the vaporization dynamics. The bulk-liquid temperature is defined as

$$T_{mm} = \left(\int_A u T \, dA \right) / \left(\int_A u \, dA \right),$$

and the bulk superheat is just the difference between the bulk temperature and the saturation temperature. To predict this quantity, $T_{mm} - T_{sat}$, we must know the transient liquid temperature and velocity profiles before nucleation as well as the incipient-boiling wall temperature. These profiles are influenced strongly by the method used to approach boiling, i.e., by the rate of change of either the flowrate or the wall heat flux. In liquid-metal-cooled fast breeder reactors, the rate of change of these two quantities would be relatively large for expected accident conditions (e.g., flow blockage, flow decay, or power excursion). However, several early tests in liquid-metal heat-transfer loops^{20,21} have approached boiling by slowly increasing the heat flux or decreasing the flowrate.

Since the bulk superheat is simply related to the wall superheat by

$$T_{mm} - T_{sat} = (T_w - T_{sat}) - (T_w - T_{mm}), \quad (9)$$

it is necessary to compute the difference between the wall and mixed-mean liquid temperatures in order to determine the bulk superheat for a given wall superheat. As mentioned previously, the incipient-boiling wall superheat should be unaffected by the bulk fluid conditions if no inert-gas bubbles are present; thus, the bulk superheat is seen to vary as the temperature profile of the liquid (as indicated by $T_w - T_{mm}$) changes due to flow or heat flux variations.

As demonstrated by Stein,^{22,23} the value of $T_w - T_{mm}$ depends strongly upon the rate of change of the heat flux with time and position; it also is expected that $T_w - T_{mm}$ is influenced by the rate of change of the liquid velocity.²⁴ For constant velocity and a nonuniform, transient heat flux, in first approximation,

$$T_w - T_{mm} = \frac{q_w}{h} + \frac{a}{2k} R_1 \left(\frac{\partial q_w}{\partial \xi} + \frac{\partial q_w}{\partial \tau} \right), \quad (10)$$

where

$$\xi = (4/Pe)(x/a), \quad (11)$$

$$\tau = (4/Pe)(ut/a), \quad (12)$$

R_1 is the coefficient that depends upon the Peclet and Prandtl numbers as well as the geometrical shape, and h is the steady-state heat-transfer coefficient. The steady-state heat-transfer coefficient also depends upon the Peclet number approximately as

$$Nu = \frac{ha}{k} = \kappa_1 + \kappa_2(Pe)^{\kappa_3}. \quad (13)$$

(See, for example, Ref. 25.) Therefore, combining Eqs. 10 and 13 with Eq. 9 reveals the dependence of the bulk superheat upon the wall superheat and the "bulk" fluid conditions,

$$T_{mm} - T_{sat} = (T_w - T_{sat}) - \frac{aq_w}{k} \frac{1}{Nu} - \frac{R_1}{2q_w} \left(\frac{\partial q_w}{\partial \xi} + \frac{\partial q_w}{\partial \zeta} \right). \quad (14)$$

Equation 14 shows that the nonuniformity and rate of change of the wall heat flux will affect the bulk liquid superheat at boiling inception. As shown in Ref. 23, if R_1 is taken equal to zero, an error of approximately 30°C can result in the calculation of $T_w - T_{mm}$ for a rapid heat-flux transient.

If boiling inception is approached by a "gradual" increase in the heat flux or decrease in the velocity, the derivatives of q_w in Eq. 14 can become negligibly small, so that in this so-called quasi-steady-state condition,

$$T_{mm} - T_{sat} \approx (T_w - T_{sat}) - \frac{aq_{wss}}{kNu}. \quad (15)$$

To illustrate the possible differences that can exist between the bulk and wall superheats, consider the following numerical example: tube diameter, $a = 7$ mm; liquid velocity, $u = 5$ m/sec; heat flux, $q_w = 300$ W/cm²; and $T_{sat} = 950^\circ\text{C}$; the bulk superheat is approximately 40°C less than the wall superheat. (The heat-transfer correlation was taken from Fig. 3 of Ref. 25.) This simple example shows that significant differences between the bulk and wall superheats can exist in quasi-steady-state, incipient-boiling experiments.

Furthermore, this discussion has illustrated that the bulk superheat depends strongly upon the particular transient used to approach incipient boiling. An earlier study⁷ also indicated a dependence of the wall superheat upon the type of transient. Therefore, merely stating that superheat was measured for a specified heat flux and velocity is insufficient. The superheat being measured must be clearly identified (i.e., bulk or wall superheat), and the conditions under which incipient boiling was approached must be defined.

IV. APPLICATION OF SUPERHEAT MODEL TO LMFBR's

The ultimate goal of the studies discussed in the previous sections is to gain a predictive capability of the incipient-boiling conditions in various types of postulated LMFBR accidents. Since the superheat model presented in this report has not been extensively tested in forced-convection systems, the application, at this time, will be limited to a suddenly blocked, single subassembly, where boiling occurs at zero flow.

Assuming that the reactor has operated at steady conditions for some time before the blockage of a single subassembly, and that a wide range of surface-cavity sizes remain on the fuel cladding, a distribution of equivalent-cavity sizes will be established from which boiling may occur. This distribution can be expressed as

$$R_{eq}(x) = \frac{2\sigma[T_i(x)]}{P_{\ell}(x) - \{K(T_p)/K[T_i(x)]\}P_{ap} - P_V[T_i(x)]}, \quad (16)$$

and the wall temperature at incipient boiling for a single-subassembly blockage accident can be determined by the solution of the implicit equation

$$P_V[T_w(x)] + \left\{ \frac{K(T_p)}{K[T_w(x)]} \right\} P_{ap} - P_p = \frac{2\sigma[T_w(x)]}{R_{eq}(x)}. \quad (17)$$

Assuming that the reactor power is held constant during this accident and that the flow decreases to zero instantaneously, the transient temperature can be found from a solution of

$$\frac{\partial T}{\partial t} = \alpha \frac{\partial^2 T}{\partial x^2} + \frac{4\alpha}{Ak} q_0 \sin(\pi x/L_0), \quad (18)$$

$$t = 0: T(x,0) = T_i(x); \quad x \rightarrow -\infty, \quad \frac{\partial T}{\partial x} \rightarrow 0, \quad (19)$$

and

$$x = L_T: T(L_T, t) = T_i(L_T), \quad (20)$$

and the incipient-boiling conditions are determined by the tangency of $T_w(x)$ and $T(x,t)$ at minimum time, i.e., when

$$\lim_{t \rightarrow t_{nuc}} [T(x,t)] = T_w(x). \quad (21)$$

This system of equations was solved for a variety of operating conditions; typical results are shown in Figs. 17 and 18. These results

indicate that the inception of boiling in a single-subassembly-blockage accident occurs near the center of the core and that the inception location is insensitive to the length of the unheated upper-blanket region. Figure 18 indicates the strong dependence of the incipient superheat upon the initial (preaccident) operating conditions. The incipient-boiling superheat decreases markedly as either the velocity or the heat flux is reduced.

113-2320

Fig. 17

Typical LMFBR Temperature
Profiles at Incipient Boiling

113-2319

Fig. 18

Superheat Requirements for
Various LMFBR Operating
Conditions

This analysis indicates that for typical LMFBR conditions, the incipient-boiling superheat requirement from surface cavities in a sudden, blocked-subassembly accident will be in the range of 50-100°C. As discussed in Appendix A, nucleation may occur from entrained gas bubbles at much smaller superheats, if these bubbles are present.

V. SUMMARY AND CONCLUSIONS

In this report, the incipient-boiling conditions of liquid sodium have been experimentally and theoretically studied, and a considerable insight to this problem has been obtained. On the basis of this work, it is evident that previously published data on the incipient-boiling superheat of liquid metals do not include sufficient information to characterize the experimental conditions. In pool-boiling studies, the following information must be supplied with the superheat data:

1. Preboiling history of the system (including the liquid temperature and pressure and inert-gas partial pressure at the most severe cavity deactivating conditions).
2. Dissolved impurity content of the liquid.

3. Wall heat flux (or rate of rise of liquid temperature).
4. Liquid temperature at the start of heating.
5. The liquid pressure at boiling.
6. The height of the liquid above the heater.
7. The nominal surface cavity depth.
8. The inert-gas partial pressure at the start of heating.

In addition, sufficient time must be allowed between parametric experiments so that the system can reattain the proper initial conditions. For the apparatus used to gather the data presented in this report, the time was of the order of hours.

Forced-convection, incipient-boiling experiments should specify the additional information of liquid velocity, type of approach to nucleation, and inert-gas-bubble size distribution at the heater inlet.

On the basis of the experimental results presented in this report on incipient pool boiling, the following conclusions have been reached:

1. The preboiling history of a system determines the critical cavity size for boiling inception.
2. This history overrides gross variations in surface roughness (in the range of 0.2 to 6.4 μm average surface roughness, i.e., "mirror-like" to sandblasted).
3. The heat-flux effect is caused by inert-gas diffusion from surface cavities; an increase in heat flux can result in either an increase or a decrease in the incipient superheat, depending upon the precise experimental procedure followed.
4. An increase in dissolved oxides tends to decrease the incipient superheat.
5. Use of argon or helium as a cover gas has no measurable effect on the superheat.
6. Thermal-neutron fluxes ranging up to 2×10^{11} n/cm²-sec have no measurable effect on the superheat.

If the results of such experiments are to be applied to fast-breeder nuclear-reactor studies (as most are to be), this report illustrates the extreme importance in obtaining a precise experimental simulation, especially in regard to the occurrence (or nonoccurrence) of gas bubbles at the heater inlet. This point is of major importance, since the liquid (i.e., bulk) superheat at boiling is markedly reduced (essentially to zero) if gas bubbles exist at or near the heater exit. If the bubbles do not exist there, nucleation must occur from surface cavities at relatively large superheats.

Finally, experimental data must be obtained on the solubility and diffusivity of inert gases in liquid metals, and the wetting properties of stainless steels and liquid metals. Theoretical studies of transient, single-phase, liquid-metal heat transfer are also required in order to calculate bulk superheats corresponding to a given incipient wall superheat and to determine incipient-boiling conditions when entrained gas bubbles are present. This information is needed so that incipient-boiling models can be used more fully and more confidence gained in their predictions.

APPENDIX A

Inert-gas Bubble Dynamics

In forced-convection, liquid-metal systems, such as a nuclear reactor or a heat-transfer test loop, entrained gas bubbles may enter the heated region. These bubbles could originate from either the gas cover blanket or from the heat exchanger. In the former case, the mechanism is merely entrainment by mixing; in the latter case, gas is driven out of solution as the liquid is cooled. If these bubbles enter the heated section, the incipient-boiling superheat is critically dependent upon whether the bubbles redissolve as the liquid is heated. (The solubility of inert gases in liquid metals decreases as the temperature decreases.¹²) If the bubbles redissolve before reaching the exit of the heated section (where nucleation would most likely occur), boiling must occur from surface cavities. However, if the bubbles do not redissolve in this region, boiling may occur from these bubbles acting as the nuclei, with a resultant large decrease in the incipient superheat.

Some experiments^{20,21} have demonstrated that the superheats in a forced-convection loop are less than those in a pool boiler; in fact, the superheat tends to zero as the velocity is increased. Since the presence of inert-gas bubbles could markedly reduce the incipient superheat, knowledge of the dynamics of these bubbles in a liquid-metal system is required. A simplified physical model is proposed, which, hopefully, will yield some information on the conditions required for bubble growth or collapse in the heated section of a forced-convection, liquid-metal loop.

1. Model Development

Consider a small gas bubble (containing inert gas and vapor) moving through a liquid-filled tube at the liquid velocity. As the liquid is heated, the vapor pressure in the bubble increases, but the inert-gas partial pressure decreases as the gas dissolves in the liquid. The former causes the bubble to grow; the latter causes it to collapse. Also, the liquid pressure decreases due to the pressure drop through the tube; this causes the bubble to grow. The dominant effect of these three phenomena are determined by the relative magnitudes of several parameters, such as the heat flux, liquid velocity, rate of solution and diffusivity of gas in the liquid, and physical properties of the gas and the liquid.

This model neglects the thermal effects of evaporation and condensation of the liquid as well as the liquid viscosity. Also, it is assumed that a bubble of radius R_0 enters the heated section; thus, if $R(t)$ is the transient bubble radius,* then from Rayleigh's equation,²⁶

*It is implicit in this analysis that the bubble remains small relative to the size of the tube and does not affect the liquid flow.

$$R \frac{d^2 R}{dt^2} + \frac{3}{2} \left(\frac{dR}{dt} \right)^2 = \frac{1}{\rho} \left\{ P_V[T(t)] + P_A(t) - P_L(t) - \frac{2\sigma[T(t)]}{R} \right\}, \quad (22)$$

with the initial conditions

$$R(0) = R_0, \quad \frac{dR(0)}{dt} = 0, \quad T(0) = T_0. \quad (23)$$

The functions $T(t)$ and $P_L(t)$ are easily obtained from overall heat and momentum balances for a uniform-heat-flux, plug-flow situation. This results in

$$T(t) = T_0 + \frac{4q_w t}{ac_p} \quad (24)$$

and

$$P_L(t) = P_{Le} + \frac{\rho L u^2 \lambda}{2a} \left(1 - \frac{ut}{L} \right), \quad (25)$$

where the time t is defined as the channel position x , divided by the velocity u ; i.e., $t = x/u$. $P_V[T(t)]$ is obtained using Eq. 24 with the vapor-pressure equation for sodium from Ref. 27. Evaluation of $P_A(t)$ requires the following assumptions: (1) The diffusional resistance inside the bubble is negligible; (2) equilibrium exists at the liquid-vapor interface, so that Henry's law may be used; and (3) the concentration of dissolved gas in the liquid is constant. (Assumption 3 results in a rate of loss of gas from the bubble that exceeds the "actual" case, producing a shorter bubble lifetime.) From these assumptions, $P_A(t)$ can be expressed as

$$P_A(t) = \frac{MC_A[R(t), t]}{\rho K[T(t)]}, \quad (26)$$

where Henry's-law "constant" is expressed¹² as

$$K(T) = A \exp \left(-\frac{B}{T} \right). \quad (27)$$

Furthermore, since $C_A[R(t), t]$ is assumed constant and equal to the inlet concentration C_{A0} , then $C_A[R(t), t]$ can be approximated by

$$C_A[R(t), t] \cong C_{A0} = \frac{\rho P_{A0} K(T_0)}{M}, \quad (28)$$

and Eq. 20 can be expressed as

$$P_A(t) = P_{A0} \exp \left(-\frac{B}{T_0} + \frac{B}{T} \right). \quad (29)$$

However, the initial inert-gas partial pressure in the bubble, P_{A0} , cannot be specified arbitrarily; it is determined by assuming that the bubble enters the heated section at equilibrium, so that

$$P_V(T_0) + P_{A0} - P_L(0) = \frac{2\sigma(T_0)}{R_0} \quad (30)$$

Therefore, the equations that must be solved to determine the dynamics of an inert-gas bubble are Eqs. 22, 24, 25, and 29, with the initial conditions expressed in Eqs. 23 and 30. This nonlinear system could not be solved analytically; therefore, a numerical technique (fourth-order Runge-Kutta) was used.

2. Calculated Results

Equations describing the bubble dynamics were numerically solved for the argon-sodium system, since this is of most practical interest for fast breeder reactors. Also, only the effects of the velocity, heat flux, and initial bubble size were investigated in this simplified model.

Figure 19 shows the growth and/or collapse of an argon-sodium vapor bubble in liquid sodium as it flows through a heated channel for various liquid velocities. The bubble collapses at the lower velocities, and its "life" is extended as the velocity is increased until the bubble fills the entire channel. One conflicting result caused by the many assumptions in the physical model is that it was assumed that the bubble does not affect the

113-1709

Fig. 19. Effect of Liquid Velocity upon Bubble Dynamics

liquid flow; but as the bubble fills the channel, the flow must be markedly affected. However, a bubble may travel further from the heater inlet if the liquid velocity is increased.

The behavior of various initial-sized bubbles is illustrated in Fig. 20; the smaller bubbles travel farther with the liquid than do the larger bubbles. This results because the smaller bubbles have a larger inert-gas partial pressure at the inlet, providing the other variables remain constant (see Eq. 30); thus, their initial growth rate is larger (see Eq. 22).

113-1710

Fig. 20. Effect of Initial Bubble Size upon Bubble Dynamics

The relatively strong effect of initial bubble size upon the bubble dynamics severely limits application of these results to a practical system. If bubbles exist at the heater entrance of a liquid-metal test loop or nuclear reactor, knowledge of their size (or more accurately, size distribution) is not easily obtained, either experimentally or theoretically. This analysis has indicated a definite velocity and heat-flux effect on the bubble dynamics. The results are summarized in Fig. 21, indicating the regions of bubble growth or collapse.

This modeling of bubble dynamics can be improved considerably by including the details of inert-gas diffusion from the bubble into the liquid*

*An analysis including this effect, but for an isothermal system and assuming "quasi-static" conditions, is available in Ref. 28.

and the effect of the bubble dynamics on the flow of the liquid. However, the former requires information on the solubility and diffusivity of inert gas in liquid metals, and these data are not available in the temperature range of interest. Also, the calculations in this analysis were based upon the assumption that the only inert gas present was argon. This is a good assumption for experimental sodium loops, but in a fast breeder reactor volatile fission products may also be present. These gases may enter the coolant by diffusion or leakage through the fuel cladding or directly from fuel that is vented to the coolant. Typical gases that may be encountered in addition to argon are krypton and various xenon isotopes.²⁹

113-1701

Fig. 21. Regions of Bubble Collapse and Growth in the Heat-flux-Velocity Plane

Since this analysis predicts a more rapid collapse of inert-gas bubbles than would be expected, small bubbles ($R_0 \lesssim 0.1$ mm) probably would not dissolve during the steady-state operating conditions of an LMFBFR (i.e., at $q_w \approx 300$ W/cm² and $u \approx 10$ m/sec). Thus, it is of interest to examine the incipient-boiling conditions, assuming that extremely small gas bubbles are present in the liquid stream. These bubbles would tend to remain in the high-velocity regions of a channel, i.e., near the fluid centerline. Since these bubbles, small as they may be, are much larger than typical gas- or vapor-filled surface cavities, vapor growth can occur at essentially saturation conditions with the bubbles acting as nucleation sites. Thus, when gas bubbles are present, one can calculate the wall temperature corresponding to the centerline liquid temperature at T_{sat} , if the temperature profile is known; this wall temperature would be the wall superheat at incipient boiling. The maximum wall temperature calculated in this manner would be the value for nucleation from surface cavities.

The effect of velocity and heat flux on the liquid temperature is qualitatively illustrated in Fig. 22. This figure indicates that the wall superheat at boiling inception increases with increased heat flux and decreases with increased velocity.

For a quasi-steady-state approach to incipient boiling, the wall superheat may be approximated by the use of an existing single-phase heat-transfer correlation, such as Lyon's.¹⁸

In this approximation, it is assumed that boiling starts from entrained gas bubbles when the mixed-mean liquid temperature reaches saturation, i.e., when $T_{mm} = T_{sat}$. Thus, from Eq. 13, and using the constant suggested by Lyon, we obtain

$$T_w - T_{sat} = \frac{q_w a / k}{7.0 + 0.025(RePr)^{0.8}} \quad (31)$$

113-1856 Rev. 1

Fig. 22. Sodium Temperature Profiles at Incipient Boiling from Entrained Bubbles

Superheats calculated from this relationship are shown in Fig. 23 and indicate the heat-flux and velocity effects, similar to those observed experimentally.

113-1855

Fig. 23. Calculated Forced-convection Incipient Superheats with Entrained Bubbles

A detailed analysis of the transient temperature of the liquid during flow or power transients can also be used to predict the wall superheat at incipient boiling if gas bubbles are present near the fluid centerline. However, whether the bubbles collapse before the fluid reaches saturation must be determined by a detailed analysis of the bubble dynamics, using suitable diffusivity and solubility coefficients.

APPENDIX B

Discussion of Boiling-surface Characteristics

The out-of-pile pool boiling experiments discussed in this report used two boiling surfaces with different preparatory treatments. One surface was mechanically polished to a "mirror-like" finish (average roughness of approximately $0.2\ \mu\text{m}$), and the other surface was artificially roughened by sandblasting (average roughness of approximately $6.4\ \mu\text{m}$). Figures 24 and 25 are photomicrographs of these surfaces before and after the boiling runs with sodium. The surfaces have changed during the tests, most likely by intergranular corrosion. The depth of the surface cavities is increased by this corrosion, and the surface appearance shows marked differences. The polished heating surface was immersed in sodium for approximately 3000 hr at temperatures up to 1030°C and heat fluxes up to $6.0\ \text{W}/\text{cm}^2$, with boiling occurring for about 100 hr. The sandblasted surface experienced the same temperature and heat-flux conditions, for about 1000 hr including about 40 hr of boiling.

Although the physical characteristics of the boiling surfaces continually changed during the experimental tests, there was no general change in the incipient-boiling superheat other than that expected by the prescribed changes in the preboiling history (which was continually reestablished during the tests).

Fig. 24. Photomicrographs of Polished Heater Surface

Before Testing

After Testing

(a)

0.2 mm

(c)

(b)

0.04 mm

(d)

113-1780

Heating Surface Initially Sandblasted to 6.4 μm Average Roughness

Fig. 25. Photomicrographs of Sandblasted Heater Surface

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