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ANNUAL TECHNICAL PROGRESS REPORT
LMFBR SAFETY PROGRAMS
GFY 1970

AEC Research and Development Report

Atomics International
North American Rockwell

P.O. Box 309
Canoga Park, California 91304

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Printed in the United States of America
Available from

Clearinghouse for Federal Scientific and Technical Information
National Bureau of Standards, U.S. Department of Commerce
Springfield, Virginia 22151

Price: Printed Copy \$3.00; Microfiche \$0.65

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The preceding Quarterly Progress Report was
AI-AEC-12947

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CONTRACT: AT(04-3)-701
ISSUED: AUGUST 15, 1970

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Program: Reactor Development

AEC Task: 6-D and -E, Nuclear Safety, Gaseous Effluents, Sodium Fires

Project Manager: H. A. Morewitz

Reporting Period: Government Fiscal Year 1970

General Order: 7702

Subaccount: 31111

AEC Category: 04-60-40-02.1

Principal Contributors: R. L. Koontz, C. T. Nelson, L. Baurmash,
J. Granger, J. Hopenfeld, R. P. Johnson,
M. Silberberg, S. R. Price

I. PROJECT OBJECTIVES

The general objective of this project is to develop experimental information and analytical methods which characterize the release and transport of effluents and energy generated during postulated major Liquid Metal Fast Breeder Reactor (LMFBR) sodium accidents. The source of energy generation may be from the escaping sodium coolant, by rapid thermal-energy transfer, and/or by combustion of the sodium coolant. The effluents may be sodium (as ^{24}Na) or its oxide, fuel materials, and fission products available as potentially serious dispersions of radioactivity. The information to be developed is required for the design and safeguards analysis of economical sodium-cooled fast reactors. The objectives of this project correspond to those of Task 10-2.6 and 10-2.7 of the LMFBR Program Plan, Section 10 Safety (WASH 1110). The results and technology obtained from this project are being applied for the safety-analysis and engineering-safeguards design for the Fast Flux Test Facility (FFTF).

II. TECHNICAL PROGRESS DURING FISCAL YEAR 1970

A. PUBLICATIONS

Several reports have been written and issued:

- 1) "Behavior of Molecular Iodine in the Presence of a Sodium Oxide Aerosol," L. Baurmash, C. Nelson, J. Granger, and R. Koontz, AI-AEC-MEMO-12938.
- 2) "Characteristics of Aerosols Produced by Sodium Fires, Vol. II, Comparison of Experiment with a General Equation for the Coagulation

of Heterogeneous Aerosols," M. A. Greenfield, R. L. Koontz, and D. F. Hausknecht, AI-AEC-12878.

- 3) "The Effect of Aerosol Agglomeration on the Reduction of the Radiological Source Term for the LMFBR Design Basis Accident," R. L. Koontz, R. S. Hubner, and M. A. Greenfield, AI-AEC-12837.
- 4) "An Approximate Solution to the Equation for Aerosol Behavior," R. S. Hubner, AI-AEC-MEMO-12880.
- 5) "Description of Sodium Fires Test Installation," R. P. Johnson, R. N. Cordy, and S. R. Price, AI-AEC-12759.

Four abstracts were written on proposed papers to be presented at the Eleventh AEC Air Cleaning Conference on August 31 to September 3, 1970 in Richland, Washington:

- 1) "Comparisons of Aerosol Experiments to Models Used in Reactor-Siting Evaluations," M. Greenfield, R. Koontz, R. Johnson, and H. Morewitz.
- 2) "Behavior of Iodine in the Presence of Sodium Oxide Aerosols," L. Baurmash, C. Nelson, J. Granger, and R. Koontz.
- 3) "Measurements of Particulate Filtration Efficiencies of Building Materials," C. Nelson, J. Granger, R. Koontz, and L. Baurmash.
- 4) "Characterization of Energy Release and Aerosol Behavior from Sodium Spray Fires," J. Granger, C. Nelson, and M. Silberberg.

Two abstracts were written for proposed papers at the Health Physics Society 5th Annual Symposium, to be held November 3-6, 1970, in Idaho Falls, Idaho.

"Comparison of Experiment and Theory for the Coagulation of Aerosols," by M. A. Greenfield, R. L. Koontz, and D. F. Hausknecht has been accepted for publication in the Journal of Colloid and Interface Science.

B. LABORATORY TEST CHAMBER

1. Iodine/Sodium-Oxide Experiments

Agglomeration and distribution characteristics of molecular iodine and sodium-oxide aerosols were studied in the Laboratory Test Chamber (LTC). Sodium oxide at an initial concentration of $1.0 \mu\text{gm/cc}$ was released into a molecular iodine vapor atmosphere which contained tagged iodine at a concentration of 1340 disintegrations/min/cc or $0.13 \mu\text{gm/cc}$. A large fraction of the iodine was scavenged by the sodium oxide reducing the free molecular iodine concentration by a factor of 10^3 in a few minutes. The molecular iodine activity was found to be preferentially associated with small oxide particles. By measuring sedimentation, filtration, and plating rates it was found that the oxide and iodine behaved as one material. The small fraction of the iodine activity which remained unattached could not be identified as molecular iodine.

2. UO₂ Vaporization Experiments

Producing U₃O₈ aerosol by vaporizing UO₂ in air with a carbon electrode system shows data fairly consistent with the previous uranium-oxide tests when tungsten metal electrodes were used. The concentration of the $2 \mu\text{gm/cm}^3$ aerosol (initial concentration) approached the same rate of decrease as shown for the very low concentration aerosols (10^{-4} to $10^{-3} \mu\text{gm/cm}^3$), after 10^3 minutes.

3. Particulate Filtration by Building Materials

Particulate filtration of sodium-oxide particles was investigated using concrete and porous ceramic crucibles as a filter medium (during Spray Test Vessel tests). The efficiency was evaluated by placing the material in a line backed by a membrane filter. The data are shown in Table 1.

C. SPRAY TEST VESSEL (STV)

The results of three sodium-spray fire tests made during the first quarter of this fiscal year showed that a relatively large parametric study would have to be performed to characterize spray fire energy release. The spray system as originally designed was not optimized for rapid data acquisition and analysis, therefore extensive design modifications to the injection system and instrumentation was required.

TABLE 1
PARTICULATE FILTRATION

Parameters	Concrete 1	Specimen 2	Alundum Crucible 1	Alundum Crucible 2
Diameter, in.	2	2	1.5	1.5
Thickness, in.	1.5	1.5	1/16	1/16
Aerosol	Sodium oxide	Sodium oxide	Sodium oxide	Sodium oxide + iodine
Flow at 5 psig (initial), liters/min	1.2	11.2	25	25
Flow at 5 psig (after test), liters/min	1.2	10		
Type of leak	Porous	Crack orifice	Porous	Porous
ΔP during test (across specimen), psig	5	0.5	<0.5	<0.5
Filtration efficiency factor*	$>10^{5\dagger}$	5.5×10^3	$<4 \times 10^{4\dagger}$	$>10^{3**}$
Crack width, mil [§]	None	3.5	None	None
Effective capillary opening	14 mil		13.6 mil	13.6 mil
Test vessel	Spray Test Vessel	Spray Test Vessel	Spray Test Vessel	Large Test Vessel

*Ratio of sodium collected in concrete, surface of concrete pipe (housing concrete), to sodium collected on membrane filter

[†]Limit of detection

[§]Effective crack width as calculated from flow curves

**The nonagglomerated I_2 vapor was not retained.

The sodium-spray fire equipment now used makes possible daily tests, with a minimal downtime for cleanup after a series of four fires. Fourteen channels of data are taken in an active analog form which can be plotted directly within an hour of test termination. The following is a list of the test variables which are recorded for analysis:

- 1) Drive pressure vs time
- 2) Vessel pressure vs time
- 3) Spray-zone air temperature at 3 and 8 ft vs time
- 4) Vessel internal skin temperature vs time at 2, 4, 7, and 9 ft
- 5) Vessel external skin temperature vs time at 4 and 7 ft
- 6) O₂ concentration vs time
- 7) Fallout vs time

The following additional data are obtained from each test:

- 1) Aerosol concentration vs time
- 2) Spray rate (average)
- 3) Chemical analysis of average aerosol
- 4) Filtration efficiency of porous construction materials, e. g. concrete
- 5) Aerosol size vs time.

The oxygen-analysis accuracy has been improved by throttling the sample gas to a constant pressure and passing the sample through a heat exchanger which produces a constant temperature before a measurement is made. The resolution of wall-temperature measurements has been increased to $\sim 0.1^\circ\text{F}$ by using shielded thermocouples and low noise amplifiers.

Sodium-oxide fallout as a function of time is measured by a free-floating strain gauge which is calibrated and temperature-compensated for fire conditions. Two balances using this transducer have been designed, one using a pneumatic-force chain and the other a jeweled lever arm. It was discovered that rapid pressure changes affect the pneumatic system unless precise engineering of flow passages is done.

Six sodium-spray fires were carried out during fiscal year 1970, four of them with the original instrumentation using a full-cone spray in 21 and 2% oxygen environments. The remaining two were straight jet sprays in 21% oxygen atmospheres and controlled humidities. Table 2 shows a parameter intercomparison of the spray fires done this year. One preliminary observation is that humidity appears to have a pronounced effect (perhaps catalytic) on the burning rate of sodium. Two-percent oxygen fires appear most affected (compare Tests 10 and 11) by humidity. Also note the obvious nonadiabatic nature of the test vessel.

The magnitude of the free-energy change for sodium combustion indicates that the burning rate for spray fires should be limited by the transfer of oxygen to the liquid surface. However, the surface of a spray is not easily defined. It is obvious that a sparsely populated spray has a burning surface area equivalent to the sum of the droplet surface areas and the burning-rate equation would most likely be a forced-convection model to the sphere surface. As the droplet population becomes more dense the rate at which oxygen can be brought to the burning stream becomes the limiting mechanism. Forced convection is nearly an order to magnitude faster per unit surface of single spheres than to surface burning of a cylindrical volume which bounds a dense stream of sodium.

A spray-droplet size distribution was determined from Test 11 and is shown in Figure 1, the mean size being 340μ in diameter. A calculation using this distribution for Tests 9 and 10 provides a burning rate similar to that which forced-convection of oxygen to a sphere would predict. The last two tests also demonstrate the droplet characteristics of these spray fires, thus making possible the study of spray fire kinetics using this apparatus. If the sodium spray were dense enough to be burning only at the surface bounded by the spray cone, turbulent convection to the surface would limit the rate, making kinetic studies impossible.

For the given spray fire test conditions, sodium peroxide is produced as the major reaction product and that the proportion of peroxide increases with decreasing oxygen concentrations.

Data taken during the first half of GFY 1970 suffered from the same lack of precision which instigated the vessel and instrumentation modifications described previously. Before modeling could begin, a thermal and chemical balance had to be available. Figure 2 shows the pressure-history curve for the Series-1

TABLE 2
SPRAY TEST VESSEL SPRAY-FIRE SUMMARY

Spray Test No.*	Oxygen in Vessel (%)	Sodium Sprayed During Fire (gm)	Sodium Consumed During Fire (gm)	Sodium Consumed/ Sprayed	Moles of Sodium Ejected/ Moles O ₂	Sodium Reacted as Monoxide (%)	Observed $\Delta P/\%$ O ₂ (psig)	Oxygen Used During Spray (%)	Spray Temperature in Flame Zone (°F)	Spray Period (sec)	Initial Dew Point (°F)	Nozzle Type (mils)	Grams of Spray/ sec	Grams Consumed/ sec	Initial Vessel Pressure, P _i (psia)	Measured Maximum Pressure ΔP (psig)	Computed Adiabatic Pressure, P _{ad} (psig)
7	21	78.0	31.3	0.407	0.438	30	13.6	1.17	1300	7.0	~23	65 Swirl Full Cone	11	4.5	17.7	15.9	25.7
9	2	100.3	36.3	0.360	5.95	0	11.3	2.03	952	4.6	~45	65 Swirl	21.6	7.9	16.2	22.9	34.0
10	2	68	22	0.350	9.95	-	10.9	1.18	827	4.5	High	Swirl 65	15.0	4.9	17.7	17.0	27.5
11	2	166	2	0.012	6.31	-	68.5	0.090	454	10.2	8	65 Swirl Full Cone	13.3	0.2	17.6	6.17	~8
Series 1																	
A (H)	21	130	55	0.421	0.542	25	6.45	~2.2	950	16.5	43	28 mil	7.9	3.35	19.7	14.2	34.4
A (L)	21	181	52	0.290	0.756	0	4.30	2.7	760	27.5	~28	28 mil	6.9	1.91	19.7	11.6	39.5

*In all tests the initial temperature of the sodium was 1000°F and the spray pressure was ~50 psig.

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Figure 1. Typical Particle Size of Injected Spray
Microscope - Observed Diameter vs
Cumulative Percentage Greater
Than the Given Size

Figure 2. Vessel Pressure vs Time, Computed and Measured

Test 1-AL fire with a thermal model superimposed. It must be stressed that this is a fitted model using measured oxygen depletion, chemical analysis of the aerosol, and the observed pressure-decay time constant. A burning model to predict these parameters will be defined as data are collected for equivalent droplet distributions.

Figure 3 shows both the aerosol concentration and the aerodynamic diameter of the sodium oxide as a function of time for the last of the series of tests. Because of a blocked nozzle Test B is unique in that only 2.4 gm of sodium were released and settled in a somewhat tranquil environment. The small initial particle size which is not characteristic of previous spray fires is immediately obvious. Turbulent agglomeration is the probable mechanism creating the characteristically large particles observed in Test 1-AL and as such must be modeled by the agglomeration studies.

The initial fallout half time ($T_{1/2}$) of 1-AL is less than 1-1/2 min and is typical for STV fires. As mentioned previously the aerosol composition is primarily sodium peroxide but is far from having an ideal density.

It is assumed that once the turbulent agglomeration problem is bounded it will be possible to develop a code able to adequately predict aerosol behavior of the spray.

D. PHASE II LARGE TEST VESSEL STUDIES

Additional sodium pool fire experiments were conducted in the Large Test Vessel (LTV) which involved introducing sodium into 21, 10, and 2% oxygen environments. 100, 60, and 4% combustion of the sodium occurred in the 21%, 10%, and 2% environments respectively. Table 3 gives a summary of the initial test conditions and measured data for the recent single-cell test series.

1. Aerosol Studies

The maximum aerosol concentration (average from mass balance) was approximately 4.5 $\mu\text{gm/cc}$ for Test 4 as compared to 27 for 5 and 0.6 for Test 6. The decrease in aerosol concentrations as a function of time for Tests 4, 5, 6 is shown in Figure 4. The aerosol concentrations are nearly identical after 200 min, indicating that the large-size particles are formed in the presence of high particulate concentrations and are thus removed earlier than the smaller-size particles.

Figure 3. Spray Test Vessel Aerosol Concentration and Median Size by Mass vs Time After Spray Ceases

TABLE 3
LARGE TEST VESSEL POOL-FIRE DATA SUMMARY

Parameters	Test 3	Test 4	Test 5	Test 6
Mass sodium introduced, lb	44	50	50	50
Mass sodium released, lb	17.7	17.7	8.5	0.0703
Mass-sodium fallout, lb	16.0	~16.15	8.13	0.0655
Mass-sodium wall, lb	1.7	~1.54	0.37	0.005
Concentration, aerosol maximum (mass balance), $\mu\text{gm/cc}$	42	45	27	0.6
Burning time (time to use 90% of consumed oxygen), min	63	52	70	
r_o , measured AER, μm	1.35	1.5	0.5	1.0
r_{maximum} measured AER, μm	4.2	3.5	3.25	2.0
r_v maximum, μm	5.6	4.65	4.0	1.33
Shape factor	1/4	1/4	1/4	1
Introduced sodium temperature, °F	1000	1000	1100	1100
Vessel atmosphere (initial), % oxygen	21	21	9.25	2.0
Oxygen consumed, %	10.6	10.7	5.3	0.34
Peak pressure rise (ΔP), lb/in.^2	2.14	2.77	1.59	0.41
Fraction sodium peroxide*	0.69	0.39	0.78	-

*Fraction of oxidized sodium found on floor which is peroxide

Figure 4. Large Test Vessel Pool-Fire Aerosol Concentration vs Time

An attempt was made to collect an impactor sample for examination by an optical microscope. As each stage of the impactor is calibrated in terms of an aerodynamic property $\sqrt{\rho d}$ (where ρ density is in gm/cm^3 and d = diameter in microns), the agglomerate diameter can be measured and the density computed. From these observations the effective density of sodium varied from $< 1/4$ to $> 1/2$ the ideal density, depending on the agglomerate size.

2. Energy Release

The fifth sodium-fire test in the LTV involved the introduction of 50 lb of 1100°F sodium while the system was maintained in a "closed-cell" configuration having a 10% oxygen atmosphere. Gas pressure, burning rate, and sodium and gas temperatures resulting from this test were much less than those of Tests 3 and 4. The system pressure rise due to the fire was 2.8 and 2.1 psig for Tests 3 and 4 respectively and only 1.6 psig for 5. The average burning rate was $1/2$ of that of Tests 3 and 4. Figures 5 and 6 show oxygen consumption and pressure histories for the 10%- O_2 sodium fire. A summary of the initial test conditions and observed experimental characteristics of Test 5 is given in Table 3. Figure 6 gives a comparison of the predicted test results by the SOFIRE II code. The model assumed a constant sodium-combustion ratio of 1.75 lb Na/lb O_2 during the calculation.

The resulting equilibrium sodium temperatures after the spill for Tests 5 and 6 were 640 and 900°F respectively. This increase was made by decreasing pan thickness and thus decreasing the heat capacity of the burn pan. The resulting system pressure and temperature histories for Test 6 were primarily due to heat transfer from the sodium pool by convection and radiation. The observed system pressure was best predicted without taking oxidation into account. This is also apparent on the sodium-pool temperature-history curve.

3. Chemical Analysis

Since the heat of formation of sodium oxides produces a pressure rise by nearly a factor of two difference per mole of oxygen when peroxide is formed instead of monoxide, the chemical analysis of the oxidation products of a fire is desirable.

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Figure 5. Rate of Oxygen Consumption in Test 5
(depleted O_2 , closed cell)

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Figure 6. Sodium-Fire Pressure History in Test 5
(10% O_2 , closed cell)

Primarily sodium peroxide (27%) was released to the environment, which was 29% of the total oxide formed for Test 5. The oxide which was retained in the pan was primarily sodium monoxide (93%) indicating a 71%-sodium-monoxide fire for the 10%-oxygen-environment pool fire.

The free-energy-of-formation calculations for the oxides of sodium show that in a neutral or oxidizing environment the preferred sodium-oxidation product is the peroxide up to 1650°F. The equilibrium constant for the conversion of sodium peroxide to sodium monoxide by decomposition is large enough to produce detectable amounts of the monoxide at temperatures as low as 1100°F. Binary statistics indicate that sodium peroxide is an intermediate in the formation of sodium monoxide. One would thus predict the greatest formation of monoxide during the hottest period of the test and the lowest at the earlier and later periods. This was confirmed by analyses of the aerosol of Test 5 which indicated a 61% monoxide composition over the fire during the hottest period and 18 and 40% during the earlier and later periods.

E. ANALYTICAL DEVELOPMENT

1. Heterogeneous Aerosol Agglomeration Model

The effects of particle density, shape, and gravitational collision efficiency were evaluated by comparison of Heterogeneous Aerosol Agglomeration Model (HAA-3) predictions to experimental studies in the LTV at high concentrations of sodium oxide.

The observations for which solutions were obtained were the:

- 1) Suspended mass concentration
- 2) Mass of fall-out material
- 3) Mass of wall-plated material
- 4) Median size by volume of particle distribution

Input parameters to the code were: a) the observed initial distribution of the particle population (initial) median size by volume, $r_v(o) = 0.5, 0.6$; b) the geometric standard deviation, σ , equal to 2, based on the assumption of an initial log-normal distribution; and c) the observed released mass, M_R , or its

counterpart the observed released mass concentration, $C_R = M_R/V$. (M_R is the total mass released and is distributed at any moment as suspended material, fallout, or plated material. The quantity V is the volume of the chamber into which the particles are released.)

The constants appearing in the general equation were either known physical constants or constants describing the geometry of the container. The only undefined parameter was a wall-plating constant whose value was estimated by an actual measurement. (The mass-plating on the wall was less than 5% of the total release mass in LTV.)

One of the physical constants is the material density, which is used in the expression giving the fall-out velocity in the Stokes-Cunningham expression. It has been shown that agglomerated-particles tend to form voids, even when close packing occurs, which reduces the effective density (Stokes velocity) to a fraction of the unit-particle density.^(1,2) The relative density is a function of the number of particles that have agglomerated, reaching a stable value which is constant no matter how large the agglomerated-particle may be. For spherical particles agglomerating to form nearly spherical particles, an effective density of about 63% of the unit density is reached when the agglomerated-particle is about three times as large or larger than the unit-particle size. (The new effective diameter is three times larger.) For particles that join to form agglomerates that are not closely approximated as spheres, the packing is looser with greater percentage of voids. In one such case the agglomerated-particles reached a stable percentage of approximately 25% of the unit-particle density when the effective diameter is about five times the unit-particle size of $1.17 \mu\text{m}$. Smaller unit sizes will proportionately results in smaller stable relative diameters. This smaller agglomerate diameter requires a larger particle population to produce the same size aerosol that a few larger particles would produce. For example, eight $0.2 \mu\text{m}$ -diam particles will result in an agglomerate whose density is near the unit density, while eight of $1.17 \mu\text{m}$ -diam will result in an agglomerate whose density is already 50% of the unit sphere. On the basis of this work we have assumed that if the mean size exceeds five times the original unit size, then the loosely packed effective density of 25% should be used in the Stokes expression. This simple assumption allows agreement between experiment

and calculation for the AI data obtained at high concentrations in the Large Test Vessel.

a. Comparison of HAA-3 vs LTV Experiments

(1) LTV Test 3

Figure 7 (LTV Test 3) is a graph of the observed released mass that was used as the input for the computer solution. In Figure 8, a graph showing the comparison between experimental data and the computer solution for the settled mass, the agreement is good. Agreement in Figure 9, a graph of observed mass concentration of suspended material and of the corresponding computer solution, is reasonably good. Figure 10 compares data with computer solution of the suspended mass concentration for later times. Agreement is reasonably good for several decades of values of concentration. Figure 11 is a graph comparing observation with computer solution for the median radius by volumes. It is noteworthy that both the observed data and the computer solution indicate very large radii up to 5 to 7 μm . The maximum computed size exceeds the maximum observed by some 27%. The values observed and computed for later times agree to within 10%.

It may be concluded that the four sets of data of Test 3 are well represented by the theoretical calculations.

(2) LTV Tests 4 and 5

These results show the same kind of comparison for Test 4 and Test 5.

The nature of agreement for the same four sets of parameters between theory and experiment for Tests 4 and 5 are as good as those for Test 3.

(3) LTV Test 6

In this case the released mass concentration was relatively low ($< 1 \mu\text{gm/cc}$) and the observed growth in median size by volume was modest (less than a factor of two). Nevertheless the computer solution followed the data fairly well. Figures 12 through 16 show that the agreement was good.

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Figure 7. Mass-Release Rate for Test No. 3

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Figure 8. Settled Mass for Test No. 3

Figure 9. Airborne Mass-Concentration for Test No. 3

Figure 10. Airborne Mass-Concentration at Extended Time for Test No. 3

Figure 11. Median Airborne Particle Size for Test No. 1

Figure 12. Mass-Release Rate for Test No. 6

Figure 13. Settled Mass for Test No. 6

Figure 14. Airborne Mass-Concentration for Test No. 6

Figure 15. Airborne Mass-Concentration at Extended Time for Test No. 6

Figure 16. Median Airborne Particle Size for Test No. 6

Table 4 lists the input parameters, based on observation, for Tests 3 through 6. Tests 3, 4, and 5 involved relatively large released masses of approximately 8000 and 4000 gm of sodium. The corresponding values of the released material concentration is of the order of 130 and 65 $\mu\text{gm/cc}$ (as Na). The maximum released in the LTC was 60 gm, similar to that of Test 6 in LTV. The initial oxygen concentration was 21% for Tests 3 and 4, and 9.25% for Test 5. Test 6 is quite different from the others in a number of important ways: (1) initial oxygen concentration was 2%; the value of $M_R = 36$ gm (Na), with $C_R = 0.6 \mu\text{gm/cc}$ (Na), quite low compared to the others. The shape factor for Test 6 is 1.0 since maximum particle growth did not exceed the original size by 5.0.

TABLE 4
CODE INPUT PARAMETER VALUES FOR SODIUM-OXIDE AEROSOLS
IN THE LARGE TEST VESSEL

Selected Input Parameter to Simulate	Test			
	3	4	5	6
Observed release mass, M_R , (Na): gm	8120	7800	3830	36
lb	17.7	17.5	8.5	0.08
C_R , (Na), $\mu\text{gm/cm}^3$	135	130	64	0.64
Time, (τ), sec	3800	3200	4200	1200
$Q, C_R/e_I, \mu^3/\text{cm}^3$	8×10^7	7.68×10^7	3.86×10^7	6.17×10^5
$\bar{r}_v(t=0), \mu$	0.5	0.5	0.5	0.5
Standard deviation, σ	2	2	2	2
Stokes factor	1/4	1/4	1/4	1.0
N_o , oxide particles/ cm^3	1.3×10^9	1.3×10^9	6.4×10^8	10^7
S_o , particles/ cm^3 -sec	Variable	Variable	Variable	Variable
Wall-plating parameter, (Δ), μ	1×10^{-5}	2×10^{-5}	2×10^{-5}	5×10^{-4}

Comparisons of theory and experiment have now been made for two very different geometries (volume ratio exceeds 50). Also, the maximum released sodium-oxide aerosol concentrations differ by factors of 50 for the LTC⁽³⁾ and 200 for the LTV. The ability of the theory to accommodate these large differences in mass concentration and geometry is a strong indicator of the basic validity of the model. In particular the correct prediction of the dramatic increase in particle size in the LTV experiments has a most important implication for safety, since large sizes which result from large released masses mean rapid fallout will occur in the primary containment.

The results of these comparisons indicated that the use of a gravitational-collision efficiency of 1.0 and a combined particle shape and density factor of 0.25 led to computed results with a simultaneous fit of all four measured time-dependent observations.

2. Aerosol-Model Applications

A series of parameter sensitivity calculations was made to determine the effect on the leaked mass for typical DBA conditions. Tables 5 and 6 show the effects of input size and distribution and collision efficiency, respectively for a typical containment structure whose inner volume and height are similar to that of FFTF.

3. SOFIRE II, Pool Fire Model

SOFIRE II is a computer code which solves a set of theoretical equations describing the simultaneous process of heat and mass transfer during a sodium-pool fire.

These equations are founded on the assumption that the burning rate of the liquid sodium is controlled by the rate of oxygen-arrival to the sodium surface and that the reaction is heterogeneous. These assumptions are based on experimental observations of sodium fires under various environmental conditions. The solution of the equations requires previous information about the following three physical parameters:

- 1) The nature of the chemical reaction.
- 2) The radiation interchange factor between the surface and gas (K).

TABLE 5
EFFECTS OF INPUT PARTICLE SIZE PARAMETERS

Concentration (gm/cm ³)	Minimum Particle Radius, (r_v) ₀ (μ)	Standard Deviation, (σ) ₀	Aerosol Density, ρ (gm/cc)	Initial Air Concentration Half Time (sec)	Mass Leaked From Inner Containment at 30 Days (gm)
<u>Case 1</u>					
235	0.1	1.01	1.0	62.0	73.8
235	0.5	1.01	1.0	61.5	73.4
235	1.0	1.01	1.0	56.5	67.6
235	0.1	2.00	1.0	62.5	74.7
235	0.5	2.00	1.0	40.0	48.0
235	1.0	2.00	1.0	27.1	33.5
<u>Case 2</u>					
23.5	0.1	1.01	1.0	608	84.8
23.5	0.5	1.01	1.0	603	83.7
23.5	0.1	2.00	1.0	607	86.2
23.5	0.5	2.00	1.0	400	63.3
23.5	1.0	2.00	1.0	282	51.1

TABLE 6
EFFECTS OF COLLISION EFFICIENCY OF
GRAVITATIONAL AGGLOMERATION

<u>Case 1</u>	Collision Efficiency = 1.0 (α)	Collision Efficiency = 0.5 (α)	Collision Efficiency = 0.0 (α)
Initial Concentration, μgm/cm ³	235	235	235
Initial Air Concentra- tion, Half-Time, sec	62.5	105	4.0 x 10 ³
30-Day Mass Leakage from Inner Contain- ment, gm	74.7	130	6.5 x 10 ³
<u>Case 2</u>			
Initial Concentration, μgm/cm ³	23.5	23.5	23.5
Initial Air Concentra- tion, Half-Time, sec	610	1040	1.0 x 10 ⁴
30-Day Mass Leakage from Inner Contain- ment, gm	86	156	1.6 x 10 ³

- 3) The conductivity of the oxide layer formed on the surface of the liquid.

In principle all three parameters could be theoretically determined. In practice, however, a theoretical determination of these parameters would be very difficult. The prediction of the amount of Na_2O , Na_2O_2 , and other products formed would require information about the reaction-rate constants, the local temperature, and the local concentrations of oxygen and sodium in the region where the reaction takes place. Since the reaction-rate constants are not known and the local temperatures and concentration of oxygen and sodium would depend on the properties of the oxide layer, theoretical determination of the reaction products would be impossible at present.

Theoretical methods are available for predicting the radiant heat exchange between a surface and a gas with nonuniform temperature. The calculations require, however, information about local aerosol concentrations and size. Theoretical determination of the latter in the present system would be difficult. Also, the emissivity of the sodium-pool surface is not known.

The thermal conductivity of the oxide layer can be determined if the density of the oxide and the capillary rise of the sodium in the pores of the oxide are known. The latter is not easily defined for theoretical analysis.

The critical parameter needed for the prediction of the pressure rise in a cell is the rate of chemical-energy release. This requires a description of the mechanism which controls the burning rate. SOFIRE II is based on the assumption that the slowest step in the burning process is the rate at which oxygen arrives at the surface. The purpose of comparing the experimental data with such a model is to check the validity of this assumption. Ideally it would be desirable to do the experiments in a system where the convective flow is well defined so that the mass-transfer coefficient for the oxygen flux to the sodium surface can reliably be determined. When the flow is turbulent, the mass-transfer coefficient must be obtained from empirical equations which were obtained under certain controlled conditions. Only when the experiment is done under the same

conditions can these empirical equations be applied to calculate the heat-transfer coefficient. Such equations are available to calculate the heat-transfer coefficient (and therefore by analogy the mass-transfer coefficient) from horizontal parallel plates held at different temperatures and connected by vertical walls.

The mass-transfer coefficient for the present case probably falls somewhere between the single flat-plate case and that where the air is enclosed by two parallel plates.

In assessing the results of the comparison between the theoretical burning rates and the experimental values one should keep in mind that the theoretical values are based on the experimentally determined split in the reaction products, α . An error in the split would have a marked effect on the calculated burning rate which in turn will cause a discrepancy between the latter and the measured burning rate. The differences between the calculated values of the burning rates of Tests 3 and 4 is probably due to uncertainties in the split of the reaction products.

It would be of interest to examine how the uncertainty in the calculated burning rate reflects itself in the prediction of the pressure. The agreement in the pressure for Test 4 is expected since the theoretical and experimental burning rates are almost identical. In Test 3, however, the maximum measured pressure rise is 40% higher than the calculated pressure, while the respective burning rates differ by a factor of two. The general shape of the calculated pressure curve is in agreement with the shape of the measured pressure curve.

III. EVALUATION OF EFFORT DURING FISCAL YEAR 1970

A critical reassessment of the AI aerosol modeling effort as it applies to FBR design or licensing has been made. The results of this effort have provided general substantiation of the basic validity of the HAA-3 aerosol model, and the justification for its use in siting analysis.

The HAA-3 model has been successfully compared to sodium-oxide aerosol experiments in a vessel whose height is characteristic of the inner-containment system of the LMFBR. Na_2O particulates at the Design Basis Accident aerosol concentrations were used. HAA-3 was able to provide a good fit in the LTV sodium-oxide tests to four time-dependent quantities with only one adjustable parameter. However, the HAA-3 model has not as yet been tested against LTV experiments with mixtures of UO_2 , Na_2O , and fission products.

Based on good agreement with the LTV sodium-oxide tests, HAA-3 can be applied to the site analysis of the current double-containment designs of LMFBR since all agglomeration takes place in the inner system, and only stirred settling occurs in the outer containment before leakage to the atmosphere. We believe that under these conditions HAA-3 provides a substantially conservative prediction of particulate release to the atmosphere.

Further substantiation of some of the parameters used in HAA-3 are, however, required. In particular, the terms for efficiency of gravitational agglomeration of nonspherical, open-network particles and the corrections to the Stoke's settling velocity due to the loose packing of the particles which compose the agglomerate need further experimentation and analysis. In addition, the turbulence energy available to assist agglomeration needs to be measured for a variety of experimental situations to determine if terms which account for turbulent agglomeration should be incorporated in the present model, or if the present neglect of these terms is justified.

Experiments were done which substantiated that vaporized iodine would condense on sodium-oxide aerosols thus confirming the agglomeration model which assumes that fission products and aerosols are identical. Knowledge of the behavior of aerosols includes the behavior of fission products which are scavenged by the larger particles.

The prediction of the pressure rise and aerosol release as a result of a sodium spill largely depends on the understanding of the burning mechanism. The physical model which was developed to describe the sodium-oxygen reaction is based on the assumption that the burning rate is controlled by the rate of oxygen-arrival to the surface of the liquid sodium. Experiments are done to

check the validity of this assumption. Two areas of uncertainty in comparing the experimental data with the theoretical predictions are (1) the determination of the composition of the reaction products; and (2) the fact that the test geometry is more complex than that for which equations for calculating the mass-transfer coefficient are available. In the light of the above discussion the agreement between theory and experiments is considered satisfactory for the closed-cell model. (The two-cell version of SOFIRE II has not as yet been tested.)

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Program:	Fast-Reactor Development		
AEC Task:	6-G, Nuclear Safety (Kinetics), Boiling Studies for Sodium Reactor Safety		
Project Manager:	H. A. Morewitz		
Reporting Period:	Government Fiscal Year 1970		
General Order:	7702	Subaccount:	13410
		AEC Category:	04-60-01-09.1

Principal Investigators: D. Logan, J. Landoni, and C. Baroczy

I. PROJECT OBJECTIVES

The general objective of this project is to develop basic information on two-phase flow and boiling required in the safety evaluation of LMFBR designs. This information is important because of the key role that sodium boiling plays in reactor dynamics, fuel-meltdown accidents, and the ultimate shutdown mechanism of the reactor. Specific objectives of the experimental program include the development of reliable high-flux heaters and the measurements of boiling heat-transfer characteristics, two-phase pressure drop, void fraction, liquid superheating, transient voiding rates and pressures in single channels, and hydrodynamic instabilities. Specific objectives of the theoretical study, in addition to any analyses required to support the experimental work, are: the development of digital computer codes which will predict transient void fraction, flowrate, and heat transfer for single and multichannel sodium flow; and the incorporation of these codes into a general reactor kinetics code.

II. TECHNICAL PROGRESS DURING FISCAL YEAR 1970

During FY 1970, studies on boiling initiation in a flowing annular sodium channel were generally completed. In addition boiling initiation and hydraulic investigations were started in a three-pin three-channel test section, and the forced-convection loop was modified. Modeling efforts were continued on sodium boiling during forced-convection flow.

A total of 374 single-channel sodium-liquid superheater test runs were completed this year. Test conditions were system pressure of 5 psia and fluid velocities of 3 to 9 ft/sec.

Three topical reports on boiling initiation in a single sodium channel were written, two of which were published in GFY 1970, as follows:

- 1) D. Logan, C. J. Baroczy, J. A. Landoni and H. A. Morewitz,
"Studies of Boiling Initiation for Sodium Flowing in a Heated Channel," AI-AEC-12767 (September 1969)
- 2) J. A. Landoni, "Analysis of the Statistical Distribution of Incipient Boiling," AI-AEC-12906 (December 1969)
- 3) D. Logan, C. J. Baroczy, J. A. Landoni and H. A. Morewitz,
"Effects of Velocity, Oxide Level, and Flow Transients on Boiling Initiation in Sodium," AI-AEC-12939 (to be published in first quarter GFY 1971)

Two-hundred and ninety 3-pin 3-channel sodium-liquid superheat tests at 5-psia pressure were run by increasing heater power until boiling was started. Test conditions were: (1) inlet subcooling of 100, 200, 300, and 400°F at a sodium velocity of 3 ft/sec; and (2) inlet subcooling of 100°F at sodium velocities of 6 and 9 ft/sec.

One-hundred and twenty-five 3-pin flow-coastdown tests were run at 5-psia system pressure by holding the subcooling and heat-flux constant and reducing the velocity. Test conditions were: (1) inlet subcooling of 300°F at heat fluxes of 200,000 and 270,000 Btu/hr-ft²; and (2) inlet subcooling of 400°F at a heat flux of 175,000 Btu/hr-ft².

A topical report on the multichannel sodium-boiling code, SODIFAZE, was published:

C. J. Baroczy, "SODIFAZE, a Steady-State Multichannel Boiling Sodium Code," AI-AEC-12804 (February 1970)

A. SODIUM-LIQUID SUPERHEATING

A total of 415 3-pin 3-channel sodium-liquid superheat runs (including steady flow and flow coastdown) were completed this fiscal year. In the previous report period quarterly (AI-AEC-12947) 203 runs with data groups 3A (A), 3A (B), 3A (C), and 3A (D) were tabulated and plotted, and some interpretations of the

data were made. The remaining 212 experimental runs with data groups 3A (E), 3A (F), 3B (A), 3B (B), and 3B (C) are tabulated in Tables 7 and 8. Four of these boiling-inception data groups [3A (E), 3A (F), 3B (A), and 3B (B)] consisting of 87 steady-flow and 100 flow-coastdown runs were reduced and analyzed. These experiments covered the following ranges: velocity, 2.4 to 8.8 ft/sec; inlet subcooling, 118 to 395°F; and heat flux, 0.13×10^6 to 0.32×10^6 Btu/hr-ft². Figure 17 shows the locations and identification of the thermocouples which are located at or near the end of the heated portion of the test section. The pins are centered by locator pins projecting from the test section wall at 5-in. intervals.

Figure 18 shows bulk superheat as a function of heat flux for Group 3A (E). Although the overall trend of the data (and of the all-liquid heat balance*) shows bulk superheat to increase with heat flux there are noticeable differences between the individual channels. For example, superheat in Channel 1 has the greatest vertical distribution and the least variation in heat flux, while the superheats in the other channels tend to cover a fairly broad range of heat flux. The superheat in Channels 2, 4, and 5 tends to be the highest at a given heat flux while the superheat for Channel 3 tends to be the lowest.

The general trend of the data (and of the all-liquid heat balance) shows bulk superheat to increase with heat flux for the Group 3A (F) data in Figure 19. However, the increase in sodium velocity from 3 ft/sec (Figure 18) to 8.8 ft/sec apparently results in the elimination of the tendency for the superheat in Channel 1 to be confined to a narrow range of superheat; in this way the behavior of all the channels becomes more uniform. Again Channel 3 tends to have the lowest superheat at a given heat flux whereas Channels 1, 4, and 5 have the highest superheats. For a similar range of heat flux but different inlet subcoolings the superheats for 8.8 ft/sec are less than those for 3 ft/sec.

Figure 20 shows bulk superheat results [Group 3B (A)] for essentially constant heat flux and inlet subcooling but with substantial variation in sodium velocity under flow-coastdown conditions, i. e. flow decreasing from about 3 to 4 ft/sec to 1.5. The variation in sodium velocity at boiling initiation (2 to 3 ft/sec) is believed to be the major contributor to the large vertical spread of the data. Note that the maximum superheats (Channels 2 and 5) cover the

*The heat balance is based on total heat input and total flow.

TABLE 7
SODIUM-BOILING TEST DATA
(Sheet 1 of 3)

Run No.	Test Section Pressure (psia)	Test Section Inlet Temperature (°F)	Test Section Inlet Subcooling (°F)	Heat Flux (Btu/hr-ft ² x 10 ⁶)			Bulk Superheat (°F)					Sodium Velocity (ft/sec)
				Heater I	Heater II	Heater III	TC-1	TC-2	TC-3	TC-4	TC-5	
3AE-1	4.87	1030	393	0.205	0.236	0.263	-111	-4	-17	-8	1	3.08
-2	4.91	1030	394	0.206	0.255	0.284	-60	13	-9	10	12	3.06
-3	4.95	1031	395	0.202	0.243	0.271	-93	27	-24	2	24	3.08
-4	4.95	1033	393	0.195	0.228	0.254	-124	4	-20	-12	-37	3.01
-5	4.98	1033	394	0.211	0.253	0.282	-82	50	1	25	58	3.01
-6	4.98	1036	391	0.202	0.230	0.256	-124	17	-17	1	44	3.01
-7	5.04	1038	391	0.217	0.273	0.304	-72	109	54	63	89	3.03
-8	5.06	1032	398	0.211	0.250	0.279	-97	50	3	12	19	2.99
-9	5.01	1029	399	0.208	0.249	0.278	-99	49	0	14	42	3.03
-10	4.97	1026	401	0.201	0.232	0.259	-113	36	-20	-11	-1	3.03
-11	4.96	1023	403	0.203	0.246	0.275	-95	41	-1	-14	-17	3.01
-12	4.97	1023	404	0.206	0.241	0.268	-89	47	1	-2	18	3.03
-13	4.98	1029	398	0.215	0.281	0.313	-64	144	74	68	33	3.03
-14	4.95	1028	398	0.204	0.241	0.269	-113	45	-4	-1	-2	3.06
-15	4.91	1022	402	0.206	0.243	0.271	-112	53	-18	13	1	3.08
-16	4.89	1022	402	0.215	0.243	0.243	-110	56	-15	0	31	3.01
-17	4.93	1024	401	0.196	0.233	0.259	-121	38	-30	-14	27	2.99
-18	4.92	1026	399	0.203	0.234	0.261	-111	52	-16	-3	30	2.99
-19	5.01	1027	401	0.212	0.288	0.321	-69	92	6	73	79	2.99
-20	4.93	1030	395	0.193	0.231	0.258	-121	43	-21	-3	25	2.99
-21	4.95	1031	395	0.206	0.238	0.266	-87	59	-6	15	36	2.94
-22	5.04	1029	400	0.212	0.292	0.326	-2	121	54	63	63	2.92
-23	4.94	1028	398	0.200	0.228	0.255	-127	45	-17	-1	6	2.99
-24	4.97	1028	399	0.203	0.252	0.281	-88	94	10	33	93	2.87
-25	4.94	1028	398	0.202	0.232	0.258	-67	62	-1	11	30	2.87
-26	4.74	1032	387	0.213	0.266	0.293	-23	96	58	38	60	2.96
-27	4.57	1030	383	0.199	0.244	0.269	-42	58	-7	-16	-20	2.99

TABLE 7
SODIUM-BOILING TEST DATA
(Sheet 2 of 3)

Run No.	Test Section Pressure (psia)	Test Section Inlet Temperature (°F)	Test Section Inlet Subcooling (°F)	Heat Flux (Btu/hr-ft ² x 10 ⁶)			Bulk Superheat (°F)					Sodium Velocity (ft/sec)
				Heater I	Heater II	Heater III	TC-1	TC-2	TC-3	TC-4	TC-5	
3AE-28	4.71	1028	390	0.205	0.269	0.296	-24	107	42	20	33	2.90
-29	4.69	1030	388	0.210	0.266	0.293	-16	132	54	31	140	2.90
-30	4.51	1034	377	0.182	0.213	0.234	-76	30	-28	-15	9	2.94
-31	4.63	1038	387	0.199	0.246	0.271	-19	50	-9	1	19	2.96
-32	4.74	1028	391	0.205	0.248	0.273	-44	53	-3	12	24	2.96
-33	4.73	1026	393	0.198	0.231	0.253	-60	4	-11	1	8	2.90
-34	4.82	1018	404	0.193	0.241	0.258	-61	2	-16	11	-22	2.85
-35	4.89	1022	402	0.211	0.273	0.301	1	52	28	62	65	2.90
-36	4.92	1028	397	0.202	0.246	0.271	-5	18	8	35	33	2.80
-37	5.00	1036	392	0.211	0.271	0.299	24	55	49	82	80	2.80
-38	4.97	1040	387	0.203	0.261	0.287	8	18	17	56	41	2.85
-39	5.04	1044	385	0.209	0.267	0.294	38	30	21	67	53	2.85
-40	5.15	1044	389	0.205	0.284	0.313	57	60	41	77	116	2.90
-41	4.89	1036	388	0.205	0.256	0.282	27	23	5	38	49	2.92
-42	4.90	1024	400	0.208	0.275	0.303	50	44	19	68	99	2.90
-43	4.74	1015	404	0.203	0.245	0.251	-18	-19	-39	22	36	2.90
-44	4.77	1025	395	0.206	0.260	0.286	26	26	-3	80	80	2.87
-45	4.82	1031	391	0.209	0.267	0.293	38	24	8	76	85	2.90
-46	4.88	1032	391	0.196	0.265	0.291	25	20	9	70	54	2.90
-47	4.87	1033	390	0.208	0.267	0.292	35	18	10	66	82	2.90
-48	4.97	1037	390	0.208	0.268	0.295	46	41	13	62	78	2.85
-49	4.88	1044	379	0.197	0.236	0.260	6	1	-30	35	32	2.85
-50	4.93	1049	376	0.206	0.254	0.280	25	47	0	65	49	2.83
3AF-1	5.35	1329	110	0.200	0.181	0.220	-14	-14	7	20	33	8.91
-2	5.80	1325	127	0.230	0.225	0.270	7	5	3	16	15	8.82
-3	4.91	1324	100	0.145	0.133	0.161	-22	-23	-21	-8	7	8.82
-4	6.16	1326	137	0.235	0.267	0.318	-2	11	3	28	32	8.66

TABLE 7
SODIUM-BOILING TEST DATA
(Sheet 3 of 3)

Run No.	Test Section Pressure (psia)	Test Section Inlet Temperature (°F)	Test Section Inlet Subcooling (°F)	Heat Flux (Btu/hr-ft ² x 10 ⁶)			Bulk Superheat (°F)					Sodium Velocity (ft/sec)
				Heater I	Heater II	Heater III	TC-1	TC-2	TC-3	TC-4	TC-5	
3AF-5	4.73	1321	98	0.175	0.170	0.203	-9	-5	-3	3	36	8.78
-6	5.69	1323	126	0.197	0.236	0.279	10	15	17	16	20	8.73
-7	5.34	1325	113	0.211	0.217	0.257	9	9	10	14	20	8.73
-8	5.78	1327	125	0.232	0.248	0.294	39	26	22	28	35	8.71
-9	5.63	1326	121	0.221	0.225	0.266	19	6	8	10	28	8.73
-10	6.15	1327	136	0.229	0.273	0.321	36	23	24	16	27	8.71
-11	5.20	1328	106	0.194	0.191	0.224	22	12	6	12	7	8.82
-12	5.61	1328	118	0.228	0.268	0.314	48	35	30	26	25	8.78
-13	5.75	1329	121	0.221	0.243	0.286	34	20	12	18	8	8.71
-14	5.80	1330	122	0.226	0.247	0.290	29	17	14	19	24	8.62
-15	6.32	1330	138	0.227	0.278	0.324	28	22	12	20	18	8.62
-16	5.84	1329	124	0.223	0.281	0.324	29	30	21	35	66	8.78
-17	5.24	1323	112	0.221	0.244	0.282	15	28	15	35	74	8.87
-18	5.35	1320	119	0.218	0.250	0.289	5	20	16	30	36	8.82
-19	4.78	1322	98	0.187	0.192	0.221	2	12	14	32	56	8.85
-20	5.50	1329	114	0.222	0.264	0.304	15	34	33	64	72	8.94
-21	5.97	1330	127	0.226	0.275	0.316	11	33	24	43	52	8.82
-22	5.64	1330	117	0.227	0.259	0.298	14	33	33	64	57	8.85
-23	5.73	1331	119	0.223	0.252	0.290	13	23	23	44	32	8.82
-24	5.68	1334	114	0.202	0.246	0.283	12	30	29	38	39	8.85
-25	5.63	1337	110	0.213	0.243	0.281	3	6	24	34	67	8.85
-26	5.72	1337	113	0.213	0.228	0.263	2	24	15	37	42	8.82
-27	5.60	1330	116	0.227	0.276	0.318	22	37	34	69	73	8.82
-28	5.78	1331	120	0.223	0.282	0.324	22	49	46	79	73	8.82
-29	5.70	1334	115	0.218	0.243	0.280	0	22	19	33	58	8.73
-30	5.91	1336	119	0.219	0.259	0.299	4	8	7	39	62	8.78
-31	5.72	1332	118	0.224	0.253	0.292	6	33	11	42	52	8.73
-32	5.52	1335	109	0.227	0.273	0.314	18	45	42	71	82	8.78
-33	5.65	1326	122	0.223	0.259	0.298	4	27	25	40	47	8.76
-34	5.60	1324	122	0.222	0.245	0.285	-2	16	19	34	52	8.82
-35	5.79	1324	128	0.222	0.282	0.324	14	41	40	69	69	8.85
-36	5.15	1325	108	0.206	0.243	0.268	-23	12	15	35	73	8.85
-37	6.08	1322	138	0.210	0.293	0.322	-34	52	5	34	56	8.78

TABLE 8
SODIUM-BOILING TEST DATA FOR FLOW-COASTDOWN RUNS
(Sheet 1 of 4)

Run No.	Test Section Pressure (psia)	Test Section Inlet Temperature (°F)	Test Section Inlet Subcooling (°F)	Heat Flux (Btu/hr-ft ² x 10 ⁶)			Bulk Superheat (°F)					Sodium Velocity (ft/sec)
				Heater I	Heater II	Heater III	TC-1	TC-2	TC-3	TC-4	TC-5	
3BA-1	5.25	1132	304	0.200	0.192	0.191	-36	49	-38	29	59	2.85
-2	5.22	1131	304	0.202	0.193	0.191	-37	52	-68	13	44	2.62
-3	5.25	1133	303	0.202	0.193	0.192	-6	56	-45	14	74	2.51
-4	5.29	1133	304	0.202	0.194	0.193	-19	25	-57	-6	37	2.80
-5	5.24	1133	302	0.202	0.195	0.195	-20	15	-37	1	37	2.85
-6	5.29	1135	302	0.201	0.194	0.195	3	66	-20	11	74	2.74
-7	5.28	1132	304	0.201	0.195	0.195	11	72	2	17	75	2.58
-8	5.15	1131	302	0.201	0.196	0.196	-44	36	-62	-1	27	2.96
-9	5.12	1130	302	0.200	0.196	0.195	-46	26	-39	-1	38	2.85
-10	5.12	1132	300	0.201	0.196	0.196	-5	47	-71	0	64	2.76
-11	5.16	1130	303	0.199	0.194	0.194	-16	18	-83	-11	25	2.99
-12	5.07	1131	299	0.200	0.197	0.196	-34	20	-89	10	28	2.85
-13	5.33	1131	307	0.197	0.193	0.193	22	69	15	15	80	2.05
-14	5.09	1132	299	0.200	0.196	0.195	-41	13	-87	-12	29	2.99
-15	5.06	1131	299	0.200	0.197	0.196	-13	35	-80	-20	42	2.92
-16	5.06	1131	299	0.199	0.195	0.193	-16	26	-79	-20	40	2.80
-17	5.06	1130	300	0.200	0.197	0.196	-23	39	-59	8	44	2.85
-18	5.06	1128	302	0.200	0.196	0.195	6	67	-68	1	67	2.76
-19	5.06	1129	301	0.196	0.191	0.190	-8	48	-52	-6	63	2.71
-20	5.04	1129	300	0.200	0.196	0.194	2	59	-6	9	82	2.58
-21	5.05	1128	302	0.202	0.197	0.195	-4	27	-71	-13	30	3.01
-22	5.04	1128	301	0.201	0.196	0.195	-19	39	-77	-13	38	2.90
-23	5.06	1128	302	0.201	0.196	0.194	-6	42	-82	-13	51	2.71
-24	5.17	1128	305	0.198	0.193	0.192	20	86	-5	26	110	2.39
-25	5.13	1128	304	0.200	0.196	0.194	-27	30	-75	-17	28	3.08
-26	4.99	1132	296	0.195	0.201	0.195	-6	53		-8	65	2.80
-27	5.03	1135	294	0.193	0.199	0.193	-44	44		-19	44	2.96
-28	5.09	1139	293	0.194	0.201	0.194	-33	71		2	64	2.92
-29	5.05	1139	291	0.195	0.201	0.195	-46	43		-9	48	3.08
-30	5.04	1136	293	0.198	0.204	0.197	-38	33		-27	35	3.42
-31	5.12	1135	297	0.194	0.199	0.192	-44	48		-21	49	3.12

TABLE 8
SODIUM-BOILING TEST DATA FOR FLOW-COASTDOWN RUNS
(Sheet 2 of 4)

Run No.	Test Section Pressure (psia)	Test Section Inlet Temperature (°F)	Test Section Inlet Subcooling (°F)	Heat Flux (Btu/hr-ft ² x 10 ⁶)			Bulk Superheat (°F)					Sodium Velocity (ft/sec)
				Heater I	Heater II	Heater III	TC-1	TC-2	TC-3	TC-4	TC-5	
3BA-32	5.08	1133	297	0.193	0.199	0.192	-38	58		3	46	3.12
-33	5.07	1135	295	0.197	0.202	0.195	-67	23		-33	28	3.49
-34	5.08	1132	298	0.194	0.199	0.192	-54	34		-43	30	3.26
-35	5.10	1132	299	0.194	0.200	0.193	-64	39		-24	33	3.26
-36	5.07	1131	299	0.194	0.199	0.192	-50	16		-48	18	3.44
-37	5.12	1128	304	0.196	0.201	0.193	-64	41		-36	44	3.10
-38	5.09	1129	302	0.195	0.200	0.192	-15	49		-29	58	3.06
-39	5.16	1131	302	0.195	0.200	0.192	-10	58		-30	56	3.08
-40	5.11	1128	303	0.196	0.203	0.195	-76	22		-14	22	3.44
-41	5.12	1128	304	0.193	0.198	0.191	-45	67		-24	57	2.94
-42	5.13	1129	303	0.194	0.198	0.191	-36	67		-1	62	2.80
-43	5.08	1129	301	0.195	0.201	0.193	-40	27		-51	30	3.37
-44	5.16	1129	304	0.193	0.198	0.191	-62	65		-32	44	2.85
-45	5.12	1128	304	0.196	0.201	0.193	-31	53		-34	60	2.80
-46	5.15	1128	305	0.195	0.199	0.191	-30	58		-32	61	2.94
-47	5.16	1129	304	0.195	0.200	0.192	6	80		5	94	2.35
-48	5.19	1131	303	0.195	0.198	0.191	12	94		10	98	2.49
-49	5.21	1131	303	0.195	0.200	0.192	-16	75		-18	82	2.76
-50	5.19	1131	303	0.193	0.198	0.190	-28	42		-49	38	3.17
3BB-1	4.36	1127	279	0.242	0.294	0.281	-73	5		12	17	5.81
-2	4.40	1127	281	0.238	0.292	0.278	-58	-8		-17	-40	5.81
-3	4.40	1138	270	0.237	0.293	0.281	-12	6		-11	-22	5.52
-4	4.44	1142	267	0.238	0.293	0.281	-12	7		-14	-7	5.43
-5	4.51	1138	273	0.239	0.290	0.278	21	29		14	17	3.78
-6	4.37	1137	270	0.239	0.290	0.279	-1	-18		-38	-39	6.45
-7	4.28	1140	263	0.239	0.294	0.280	-16	-25		-33	-31	6.45
-8	4.26	1142	260	0.238	0.293	0.279	-6	-9		-27	-24	6.38
-9	4.20	1130	270	0.238	0.296	0.279	1	-24		-26	-26	5.91
-10	4.31	1130	274	0.238	0.296	0.281	2	-20		-28	-27	5.95
-11	4.55	1136	277	0.238	0.300	0.284	-21	-33		-35	-23	5.91
-12	4.67	1133	284	0.237	0.296	0.281	-13	-38		-40	-37	6.04

TABLE 8
SODIUM-BOILING TEST DATA FOR FLOW-COASTDOWN RUNS
(Sheet 3 of 4)

Run No.	Test Section Pressure (psia)	Test Section Inlet Temperature (°F)	Test Section Inlet Subcooling (°F)	Heat Flux (Btu/hr-ft ² x 10 ⁶)			Bulk Superheat (°F)					Sodium Velocity (ft/sec)
				Heater I	Heater II	Heater III	TC-1	TC-2	TC-3	TC-4	TC-5	
3BB-13	4.76	1133	287	0.239	0.295	0.279	11	11		0	7	5.24
-14	4.86	1131	292	0.239	0.295	0.279	4	-6		-14	-10	5.11
-15	4.83	1129	293	0.237	0.294	0.279	2	-13		-30	-16	5.43
-16	4.87	1127	296	0.238	0.295	0.280	-18	-27		-32	-30	5.52
-17	4.82	1132	290	0.238	0.294	0.278	-6	-23		-26	-25	5.72
-18	4.82	1133	289	0.237	0.293	0.276	0	-10		-29	-18	5.59
-19	4.87	1127	296	0.238	0.299	0.282	9	-7		0	4	5.18
-20	4.89	1131	293	0.237	0.296	0.279	6	1		-10	-9	5.11
-21	4.91	1133	291	0.238	0.293	0.278	0	-1		-3	3	5.24
-22	4.91	1133	291	0.238	0.296	0.280	-1	0		7	-2	5.11
-23	4.94	1127	299	0.237	0.295	0.278	8	-7		-13	-1	4.90
-24	4.96	1130	296	0.237	0.294	0.277	15	17		24	33	4.35
-25	4.95	1129	297	0.238	0.294	0.278	8	-10		-8	1	5.08
-26	4.57	1127	286	0.240	0.296	0.277	-43	-36		10	1	4.90
-27	4.63	1133	282	0.240	0.293	0.275	11	1		-3	-6	5.13
-28	4.65	1137	279	0.240	0.295	0.277	6	-16		-6	-13	5.49
-29	4.74	1138	281	0.239	0.296	0.277	34	19		30	40	4.81
-30	4.71	1136	282	0.240	0.295	0.277	-2	-12		-8	-3	5.52
-31	4.74	1137	282	0.240	0.295	0.276	3	-15		-3	0	5.11
-32	4.71	1134	284	0.241	0.296	0.277	6	-15		-20	-3	5.63
-33	4.68	1132	285	0.242	0.296	0.278	12	-11		-10	-7	5.27
-34	4.68	1131	286	0.239	0.296	0.277	-2	-23		-22	-12	5.47
-35	4.74	1132	287	0.241	0.298	0.278	7	-17		-5	0	5.29
-36	4.80	1133	288	0.240	0.297	0.278	10	-17		-12	0	5.18
-37	4.79	1135	286	0.241	0.296	0.277	15	7		9	13	4.83
-38	4.78	1132	288	0.239	0.295	0.276	10	-14		-5	4	5.06
-39	4.80	1131	290	0.239	0.295	0.276	-1	-11		-23	-6	5.52
-40	4.80	1131	290	0.241	0.296	0.277	7	-13		0	1	5.22
-41	4.77	1133	287	0.239	0.294	0.275	3	-5		7	23	5.08
-42	4.77	1132	289	0.239	0.298	0.278	-16	-29		-23	-11	5.70
-43	4.79	1131	290	0.239	0.294	0.275	7	-14		-13	4	5.08

TABLE 8
SODIUM-BOILING TEST DATA FOR FLOW-COASTDOWN RUNS
(Sheet 4 of 4)

Run No.	Test Section Pressure (psia)	Test Section Inlet Temperature (°F)	Test Section Inlet Subcooling (°F)	Heat Flux (Btu/hr-ft ² x 10 ⁶)			Bulk Superheat (°F)					Sodium Velocity (ft/sec)
				Heater I	Heater II	Heater III	TC-1	TC-2	TC-3	TC-4	TC-5	
3BB-44	4.80	1130	291	0.237	0.289	0.271	1	2		17	18	4.72
-45	4.77	1129	291	0.239	0.297	0.277	4	-18		-12	6	5.04
-46	4.80	1131	290	0.239	0.298	0.277	6	-10		-17	-6	5.22
-47	4.79	1131	290	0.239	0.294	0.274	9	-6		-17	-7	5.06
-48	4.81	1131	290	0.239	0.296	0.277	-6	-9		-2	1	5.06
-49	4.80	1132	289	0.239	0.296	0.277	-19	-29		-32	-25	5.81
-50	4.83	1133	289	0.238	0.296	0.277	-3	-2		1	13	5.02
3BC-1	3.96	1028	364	0.174	0.181	0.173	11	11	8	10	18	1.14
-2	4.05	1030	365	0.174	0.181	0.171	33	34	-17	50	191	0.80
-3	4.05	1037	358	0.174	0.180	0.169	30	53	6	88	236	0.46
-4	4.13	1044	354	0.173	0.176	0.167	16	40	6	27	153	0.91
-5	4.17	1040	359	0.176	0.181	0.171	43	66	8	60	212	0.80
-6	4.22	1040	361	0.174	0.181	0.171	44	70	14	88	210	0.87
-7	4.28	1040	363	0.174	0.182	0.172	64	78	-3	117	257	0.41
-8	4.31	1042	362	0.178	0.183	0.173	49	57	2	78	190	0.87
-9	4.33	1044	361	0.173	0.177	0.168	55	82	8	80	262	0.43
-10	4.38	1040	367	0.175	0.181	0.171	5	26	0	49	180	0.75
-11	4.37	1034	373	0.175	0.178	0.169	16	13	9	31	176	0.46
-12	4.43	1032	377	0.174	0.179	0.169	26	54	-12	54	200	0.62
-13	4.46	1030	380	0.177	0.178	0.171	37	60	5	124	250	0.39
-14	4.50	1032	379	0.173	0.177	0.167	66	87	11	161	300	0.32
-15	4.52	1036	376	0.177	0.180	0.170	29	60	10	105	266	0.71
-16	4.57	1037	376	0.177	0.180	0.170	24	57	7	99	233	0.73
-17	4.59	1037	377	0.176	0.178	0.169	67	86	12	170	312	0.36
-18	4.60	1036	378	0.174	0.177	0.167	37	60	3	132	283	0.34
-19	4.64	1034	382	0.175	0.181	0.171	51	92	13	153	315	0.39
-20	4.68	1033	384	0.172	0.178	0.168	44	42	21	112	269	0.39
-21	4.70	1034	384	0.171	0.178	0.168	32	42	13	113	266	0.32
-22	4.71	1034	384	0.173	0.179	0.168	59	82	16	176	333	0.34
-23	4.75	1034	386	0.174	0.177	0.167	41	66	4	152	311	0.32
-24	4.77	1034	386	0.172	0.177	0.167	48	79	5	129	293	0.48
-25	4.78	1036	384	0.175	0.179	0.170	16	17	12	18	151	0.27

HEATER OD = 0.250 in.
 TEST SECTION ID AROUND HEATER = 0.357 in.
 THERMOCOUPLES ARE GROUNDED-JUNCTION,
 304-SHEATHED, 40-mil OD

Figure 17. Thermocouple Locations

Figure 18. Bulk Superheat vs Heat Flux, 3-Pin Test Section (Group 3A E1 through E50)

Figure 19. Bulk Superheat vs Heat Flux, 3-Pin Test Section
(Group 3A F1 through F37)

Figure 20. Bulk Superheat vs Heat Flux, 3-Pin
Section (Group 3B A1 through A50)
Flow Coastdown

relatively narrow bulk-superheat range of 18 to 110°F. The superheats for Channels 1 and 3 tend to be lowest, while Channel 4 is intermediate. For the average conditions of the experiment, the all-liquid heat balance provides a reasonable mean curve for bulk superheat.

The results of a second series of flow-coastdown runs [(Group 3B (B))] made at velocities of 4.4 to 6.5 ft/sec, instead of 2 to 3.4 ft/sec as shown in Figure 20, are shown in Figure 21. These results are unusual in that the heat flux for each channel was essentially fixed (but different) resulting in four distinct superheat distributions. Generally the points lie above the all-liquid heat balance. However, this is due in part to the Channel-3 thermocouple not functioning; previous data show this thermocouple to register the lowest temperature at a given heat flux. For approximately equal inlet subcoolings the maximum superheats at 4.4 to 6.5 ft/sec are lower than those obtained at 2 to 3.4 ft/sec (Figure 20). This effect is shown more clearly in Figure 22.

Figure 22 is a histogram of the maximum bulk superheats obtained for Groups 3A (E), 3A (F), 3B (A), 3B (B). The upper two histograms, for steady-flow conditions show lower superheat at 8.8 ft/sec than at 3. Similarly for the two lower histograms, for flow-coastdown conditions the superheat at 4.4 to 6.5 ft/sec is less than that at 2 to 3.4. The observed trend of decreasing superheat with increasing velocity is thus the same as that previously observed for a single channel.

The maximum bulk-superheat data already described were converted to maximum wall superheat by the use of Dwyer's correlation for heat transfer between rod bundles and in an annulus;⁽⁴⁾ Figures 23, 24, 25, and 26 show the results for Groups 3A (E), 3A (F), 3B (A), and 3B (B). For the steady-flow runs (Figures 23 and 24) wall superheat is seen to increase with heat flux.

For the flow-coastdown runs (Figures 25 and 26) the data form vertical distributions at a given heat flux, in which the highest superheats generally correspond to the lowest flowrates and the lowest superheats correspond to the highest flowrates. This trend is identical to that shown for a single channel and is expected from an all-liquid heat balance. The few negative wall superheats in Figure 26 are in error and have been omitted from the histogram in Figure 27.

Figure 21. Bulk Superheat vs Heat Flux, 3-Pin Section
(Group 3B B1 through B50) Flow Coastdown

Figure 22. Maximum Bulk Superheat Histograms for Groups 3A(E), 3A (F), 3B (A), 3B (B)

Figure 23. Maximum Wall Superheat vs Heat Flux, 3-Pin Test Section (Group 3A E through E50)

Figure 24. Maximum Wall Superheat vs Heat Flux, 3-Pin Section
(Group 3A F1 through F37)

Figure 25. Maximum Wall Superheat vs Heat Flux, 3-Pin Section, Flow Coastdown (Group 3B A1 through A50)

Figure 26. Maximum Wall Superheat vs Heat Flux, 3-Pin Section, Flow Coastdown, (Group 3B B1 through B50)

Figure 27. Maximum Wall Superheat
Histograms for Groups 3A (E),
3A (F), 3B (A), 3B (B)

Histograms of the maximum wall superheats for the data shown in Figures 23, 24, 25, and 26, are shown in Figure 27. As in the case of the bulk-superheat histograms (Figure 22) the wall-superheat histograms show that initiation superheat decreases as velocity increases both for steady-flow and flow coastdown conditions.

A comparison between the maximum wall superheats observed for the single-pin and 3-pin test sections is shown, in histogram form, in Figure 28. The comparison is based on similar test-section inlet conditions (pressure and inlet temperature) and similar average sodium velocity which translates into approximately equal total flowrate. However, it must be remembered that in the case of the 3-pin test section the local channel velocities differ somewhat from that of the average channel. For example, a 5 ft/sec average channel velocity corresponds to 6.3 ft/sec in the two interior channels and 4.6 ft/sec in the peripheral channels. Thus, even if the enthalpy increase of the sodium is the same in each channel the channel velocities are different.

At 3 ft/sec Figure 28 shows the 3-pin wall-superheat distribution to peak at 40 to 60°F and be very narrow. In contrast the single-pin distribution peaks at 60 to 80°F has a broader superheat range and greater superheat. This behavior was expected on the basis that for the regime of positive bulk superheat (i. e. less than 4 ft/sec) the distribution for the 3-pin case can be described as a superposition of single-pin superheat probability distributions. At 6 ft/sec, where subcooled nucleate boiling is occurring, the 3-pin and single-pin distributions are more alike with peaks at about 20 to 40°F and a similar range of superheat. Additional 3-pin tests are required to compare results to single-pin experiments over a wider range of subcoolings and velocities.

B. TWO-PHASE HYDRAULIC TESTS

A tantalum-sheathed 3-pin heater has been fabricated for use in making the two-phase hydraulic tests to test SODIFAZE predictions. The need for a sheath with a high melting point (tantalum melts at ~5400°F) was demonstrated during the last set of 3-pin flow-coastdown runs. During these runs the stainless-steel sheath of a 3-pin heater was subjected to vapor-blanketing, and completely melted over a section several inches long. The molten sheath material adhered to the

Figure 28. Comparison of Histograms for Maximum Wall Superheat for 3-Pin and Single-Pin Test Sections

~~test-section walls, clogged up the flow passages, and thus precluded~~ any further use of the test section. The high melting point of the tantalum sheath should limit the worst-accident condition to internal heater failure without test-section damage since the tantalum heater sheath, unlike the stainless steel, should be able to withstand a momentary vapor-blanketing.

Exploratory two-phase hydraulic runs have been made using the tantalum-sheathed heater. The expected temperature and pressure oscillations occurred during "steady-state" boiling; however, it doesn't appear at this stage that this condition will significantly affect the intended test program.

C. ELECTRICAL UPGRADING

To increase the capability of the forced-convection loop for testing multipin heater assemblies, three new single-phase power supplies and a new preheater power supply and control have been installed on the forced-convection loop. Each single-phase power supply has a capacity of 150 kw and consists of a solid-state ON-OFF switch, isolation transformer, variac, and an electronic crowbar.

Figure 29 shows the cabinet containing the three electronic crowbars and two of the three cabinets which contain an isolation transformer and a variac. Figure 30 shows: the three cabinets which contain an isolation transformer and a variac; the cabinet which contains the three solid-state ON-OFF switches; and the cabinet which contains the preheater power supply and controls. Figure 31 shows an overall view of the experimental enclosure and the control consoles. The console shown to the left of the loop operator contains the controls for the electrical upgrading package. The new electrical equipment is approximately 10 ft behind the control console on the right of the picture. Each of the 150-kw single-phase power supplies will be used to control (1) a single pin in a 3-pin heater or (2) two pins in a 7-pin heater array. In a 7-pin array the center one will be controlled by the previously existing power supply.

D. THEORY

1. Analysis of the Statistical Distribution of Incipient Boiling

The distribution of incipient-boiling cases, when the distribution of future cases depends only on the present case and not on previous history (i. e. aging

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7702-40228

Figure 29. Single-Phase Power-Supply Cabinets

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7702-40225

Figure 30. Single-Phase Power-Supply Cabinets, Including Cabinet with Controls

AI-AEC-12970
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6-11-70

7702-40226

Figure 31. Overall View of Enclosure

effect on heat-transfer surface is over), can be considered as a particular case of jump Markovian processes.

To analyze a typical distribution of incipient boiling cases, the Kohnogorov equation has to be supplemented with the equations for the all-liquid heat balance and the expression for the decay constant for the superheat range under consideration.

The study (as described in AI-AEC-12906) is based upon a single set of incipient-boiling data as shown in the histogram at the bottom of Figure 32 for flowing sodium. Nevertheless, this histogram is considered to be typical of the incipient-boiling cases vs wall superheat.

Other histograms like the one at the top of Figure 32 are considered to be spurious because of the nonvanishing constant value of superheat cases at zero wall superheat. This fact is probably due to the reduced superheat at higher velocities and to the relatively large intervals in the wall superheat (20°F).

In Reference 5 are several histograms for the frequency of incipient-boiling cases, but most of them are not suitable because of variable conditions such as modification of the heat-transfer surface (surface attack by flowing sodium) which yields $\partial P / \partial t \neq 0$, or for high-velocity runs because of the above-mentioned explanation for the histogram at the top of Figure 32, etc.

The distribution for the frequency of incipient-boiling cases vs wall superheat, i.e. N vs $T_w - T_s$, is a non-Poissonian distribution because the decay constant is not in fact constant as the wall superheat varies.

It is shown that for the situation prevailing in the LMFBR the likelihood of incipient boiling at temperatures above the observed maximum wall superheat as predicted by the "dynamic model"⁽⁵⁾ is remarkably low.

2. Thermal and Surface Effects on Liquid Superheating

This study of the thermal and surface effects on superheating is the natural continuation of the mathematical treatment reported in the topical report "Analysis of the Statistical Distribution of Incipient Boiling," AI-AEC-12906. This study is still in progress, will be reported in the next Quarterly Technical Progress Report, and finally documented as a topical report. The velocity

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Figure 32. Histograms for Various Liquid Velocities
 (KD, MA Groups)

fluctuations which affect the initiation of boiling are not presently taken into account, but the study will be extended later on by including the fluid-dynamics part.

Vapor can be formed (nucleation) in a metastable liquid (superheated liquid). This nucleation process occurs at constant volume and if the dimensions of the vapor bubble are large enough the bubble would tend to grow into a stable configuration; otherwise it would collapse. This implies that in order to survive, the nucleated vapor bubble must have, in general, minimum dimensions.

Because of symmetry considerations, the vapor bubble is considered to be of spherical shape when nucleation occurs.*

The metastable isotherms denote points of thermal equilibrium but limited stability, i. e. the stability is limited to displacements below certain values. By exceeding these values the stability is lost and the system never returns to the metastable state. This corresponds to the minimum dimensions to produce a growing bubble of vapor in the system.

Figure 33 indicates schematically the shape of an isothermal line in the pressure-volume representation for a temperature T below the critical temperature T_c .

If there is equilibrium between phases the chemical potentials are the same. This occurs at constant volume, as already mentioned, and constant temperature but different pressures prevail in the different phases. Under these circumstances the suitable function to express the minimum work to produce the missing phase is the thermodynamic potential Ω , specifically by its change $\Delta\Omega$.

The probability distribution P for an energy fluctuation to occur and produce the nucleation of the missing phase (vapor) is, then, on a thermal basis

$$P \propto e^{-\Delta\Omega/kT},$$

where k is the Boltzmann constant and T the absolute temperature of the medium where the bubble is formed.

*This description is the precursor process to the dynamic model shown in p. 87 of Reference 6. That dimensional analysis, based on the drag and surface-tension forces, considered the vapor contained in the incipient bubble to be in thermal equilibrium, i. e. after the expansion following the nucleation at the vertex of the cavity sites.

METASTABLE REGIONS

AB: SUPERHEATED LIQUID
CD: SUBCOOLED VAPOR

LIMITS OF METASTABLE REGIONS

A,B: ORDINARY PHASE TRANSITION POINTS

{ A: 100% SATURATED LIQUID
B: 100% SATURATED VAPOR

C,D: $\left(\frac{\partial P}{\partial V}\right)_T = 0$.

Figure 33. Metastable Regions in the Pressure-Volume Representation

Inserting into the thermodynamic potential the Laplace formula for spherical bubbles,

$$\Delta p = \frac{2\sigma}{a},$$

where Δp is the differential pressure between phases (nucleated vapor minus metastable liquid), " σ " is the surface tension, and " a " the radius of the bubble, the expression for the probability distribution in an unbounded (infinite) medium is obtained:

$$P \propto e^{-\frac{16}{3} \pi \frac{\sigma^3}{kT(\Delta p)^2}}$$

When additional constraints are imposed on the superheated liquid by the surface containing it, the form of the previous expression for P can be preserved in shape but the exponent should be redefined to account for the contact angle of the bubble formed at the surface and some geometrical characteristics of the confining surface (only when comparable to the size of the bubble). It is easier to generalize the results by replacing only the surface tension σ by an effective surface tension σ_e , as reported in the literature;⁽⁷⁾ so

$$\sigma_e = f\sigma$$

where f stands for a relative surface tension. For an unbounded medium,

$$f = f_\infty = 1,$$

while for a perfectly flat surface

$$f = f_0 = \left(\frac{1 + \cos \beta}{2} \right)^{2/3} (2 - \cos \beta)^{1/3},$$

where β is the contact angle. f_0 is shown in Figure 34 where some particular cases are denoted by arrows ($\beta = 0^\circ$, 90° , and 180°).

Figure 34. Bubble on Perfectly Flat Surface

For a cavity site on the surface possessing regular conical shape,

$$f = f_c = \frac{1}{2^{2/3}} \left[2 - 3 \cos \theta_o + \cos^3 \theta_o + \frac{\sin^3 \theta_o}{\tan \theta_o} \right]^{1/3}$$

where the angle θ_o is related to the contact angle β , and the half-aperture of the cone θ_c by

$$\theta_o = \frac{\pi}{2} + \theta_c - \beta.$$

For the typical cavity sites of regular conical shape found on the stainless-steel heat-transfer surface after undergoing the corrosive action of the flowing sodium,⁽⁶⁾ the half-aperture is of about

$$\theta_c = \tan^{-1} \left(\frac{1}{2} \right) \cong 26^\circ 30',$$

Figure 35. Bubble Inside Conical Cavities

It is tacitly implied in the case of conical cavity sites that the spherical bubble formed at the vertex of the cavity site, and whose center lies on the axis of symmetry of the cone, is always contained within the cavity site. In other words the solid-vapor interface does not extend beyond the surface of the conical cavity site. Figure 35 shows the behavior of the relative surface tension $f_c = f_c(\theta_c, \beta)$ where each curve for given half-aperture angle θ_c terminates at the contact angle,

$$\beta = \pi + \tan^{-1} \theta_c,$$

because at this contact angle the bubble surface is flat, indicating that the vapor and liquid are in thermal equilibrium at the same pressure and temperature.

When $\theta_c \rightarrow 0$ (cylindrical cavity site) the function $f_c(0, \beta)$ blows up. This is shown in Figure 35 by the rapidly increasing values of f_c when θ_c approaches zero.

Work is in progress to study the behavior of the metastable region for superheated liquid on the assumption of the law of corresponding states by the use of the reduced Van der Waals equation.

3. TRANSFUGUE II

The successful development of TRANSFUGUE-IIa was reported in the GFY 1968 annual report, AI-AEC-12721, and documented in NAA-SR-12503. The significant features of TRANSFUGUE are shown in Table 9.

TABLE 9
CHARACTERISTICS OF TRANSFUGUE

1. Accounts for heat diffusion in the fuel element.
2. Analyzes a single-channel situation.
3. Accounts for variable incoming flow.
4. Assumes homogeneous slipless two-phase flow.
5. Assumes thermal equilibrium (neither superheating nor supersaturation).
6. Uses the Clapeyron formula for the boiling temperature.
7. Can handle humid-vapor and dry-vapor conditions.
8. Can handle flow-reversal situations.

Further investigation showed that TRANSFUGUE required excessive computing time for typical problems, and exhibited troublesome mathematical instabilities in the two-phase flow region. In an attempt to rectify these difficulties and expand the scope of the code a vigorous program has been instituted to revise and reprogram the code.

The steady-state and all-liquid portions of the code have already been rewritten. The early phase of this effort has already greatly improved the code capabilities.

The heat-conduction equation, whose solutions were computed by a difference formula scheme, has been modified to incorporate a completely variable radial-composition mesh in which the spatial heat-source distribution may be specified. Previously the heat-generation element was represented by three cylindrical coaxial shells, with a radially constant source in the inner two.

The nonlinear implicit equation arising from the numerical integration of the momentum equation has been reprogrammed with a predictor-corrector scheme. This effort has led to substantial reduction in computer running time for this part of the calculation (factors of up to 10), because of the larger time steps afforded by the new integration scheme.

The two-phase region is now under development. The calculational procedure used at the onset of boiling is being carefully reviewed to obtain a more numerically tractable model. The homogeneous model (slipless flow) used in TRANSFUGUE, besides its simplicity yields a higher void fraction, and consequently permits the transient state to be analyzed on a conservative and stable basis.

We believe that the dry-vapor phase of the calculation should be a fairly straightforward extension to the calculational strategy.

Further developments and extensions of the basic model are currently being considered in parallel with the main effort. This is necessary in anticipation of the tasks that the code will eventually be required to solve.

The following list represents items that are in a formative evaluation phase:

- 1) Inclusion of multiple-channel calculation capabilities.

- 2) Modification to interpret the more realistic link between heat-generation and void-formation during sudden transients.
- 3) Superheat and/or supersaturation effects.
- 4) Incorporation of post-transient analysis of liquid flow entering a heated channel following its former ejection.
- 5) Full integration of the effects of a reactor primary-cooling system into the transient.

4. SODIFAZE

The SODIFAZE code provides a method for determining the steady-state pressure-drop, sodium flowrate, quality, and void fraction as a function of heated length for up to 20 interconnected channels. In any channel, boiling begins when the saturation temperature is attained. As long as boiling does not take place in some channels, only saturated liquid is transferred from the boiling channels to the nonboiling channels. Once boiling is attained in all channels, two-phase flow is transferred among the various channels. The code can operate over the following range of sodium conditions: boiling temperature, 1600 to 2000°F; quality, 0 to 0.10; mass velocity, 0.25 to 3×10^6 lb/hr-ft². Both void fraction and two-phase friction multipliers for sodium are from the correlations of Baroczy.^(8,9)

Previously⁽¹⁰⁾ it was shown that the check curve for three geometrically identical channels with different heat inputs could be substantially unlike that normally observed for a single channel. Rather than the sharp increase in pressure-drop observed when boiling starts in a single channel, the three-channel check curve can exhibit a broad essentially constant pressure-drop flow characteristic during the period when each channel attains boiling. Only when boiling has started in all three channels does the pressure-drop rise rapidly in the expected manner.

A more detailed examination of the three-channel check curve behavior (see Figure 36 for geometry) under a variety of heat-input conditions revealed a consistent trend of curve shape with changes in the heat-input ratios among the

Figure 36. Three-Channel Flow Geometry

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three channels. Figure 37 shows the variation of pressure-drop with flowrate for heat-input ratios (Channels 1 to 3) of 1, 1.1, 1.2, 1.35, 1.6, and 2.0. In all cases the heat input to Channel 1 is constant (16,000 Btu/hr-ft) and that to 2 is the average of Channels 1 and 3. Evidently each heat-input ratio (Channels 1/3) results in a distinctive check curve which is both displaced and different from the check curve for a heat-input ratio of 1 (equivalent to a single channel). As the heat-input ratio increases, the flowrate span over which the pressure-drop remains approximately constant increases, and the increase in pressure-drop (over that existing when Channel 1 starts to boil) becomes less. Thus a consistent pattern is established.

The lower curve shown to the left of the point where Channel 1 starts to boil is the locus of all points at which Channel 2 starts to boil. The curve pattern is one of slight increase then decrease in pressure-drop, with the pattern becoming broader as the heat-input ratio increases. Except for some upward and leftward displacement, the locus of all points at which Channel 3 starts to boil is a curve identical in shape to that described for the boiling initiation of Channel 2. Both of these boiling-initiation curves are in effect reduced versions of a typical single-channel check curve; presumably, the number of such curves will be equal to the number of channels less 1.

The most significant feature of the pattern shown in Figure 37 is that pressure-drop does not start to increase significantly or rapidly until all channels have attained boiling conditions. This characteristic becomes more pronounced as the disparity between channel heat inputs becomes greater. As long as one channel is not boiling, pressure-drop is restricted to that of all-liquid conditions

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Figure 37. Pressure-Drop vs Flowrate for Three Channels with Various Heat Inputs

with a maximum possible value corresponding to that of all of the flow, as liquid, being in the nonboiling channel. Generally this all-liquid pressure-drop would be much less than that possible for two-phase flow conditions in all channels. Projection of the trends observed here leads to the speculation that even for a large number of geometrically identical interconnected channels a single non-boiling channel will act as an effective rein on pressure-drop, as flow is reduced at constant power input.

To place the observed favorable pressure-drop/flow characteristic of three channels into better perspective the prevailing conditions along the curve on which Channel 3 starts to boil (see Figure 37) were examined.* Figure 38 shows flowrate, quality, void fraction, and relative pumping power for each of the channels as a function of heat-input ratio (Channels 1/3). A heat-input ratio of 1 corresponds to the all-liquid condition for each channel at the time when Channel 1 has reached saturation temperature and is about to boil. This point corresponds to the minimum pressure-drop on the all-liquid curve of Figure 37.

Figure 38 shows that as heat-input ratio increases, the flowrate in Channel 3 has a slight peaking effect but remains essentially constant. At the same time the flowrates of Channels 1 and 2 decrease continuously, as the heat-input ratio increases, until at a ratio of 2 the flowrate in Channel 1 is about one-tenth of that of Channel 3. The mixture quality in Channels 1 and 2 (the quality in 3 is zero) increases with increasing heat-input ratio until at a ratio of 2 the quality in Channel 1 is about ten times that of 2. Since critical heat flux varies directly (approximately) with mass velocity and inversely with quality the combination of low flowrate and high quality in Channel 1 poses a potential limitation that requires evaluation in reactor design. Void fractions in Channels 1 and 2 increase rapidly up to a heat-input ratio of about 1.4, and thereafter increase slowly. Despite the large difference in quality between channels at a heat-input ratio of 2, the void fractions are similar, being about 0.99 and 0.86 for Channels 1 and 2 respectively.

*The values shown for a heat-input ratio of 2 are extrapolated, as the mixture quality in Channel 1 exceeded the capability of the code.

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Figure 38. Prevailing Conditions When Channel 3 Starts to Boil

Normally the pumping power (as measured by the product of flowrate and pressure-drop) associated with a single-channel check curve increases rapidly as soon as boiling is attained, and remains high for a considerable flow range as flow is decreased at fixed power. For the three-channel case considered here, an entirely different and more desirable pumping power trend results as shown in Figure 38. If the pumping power at a heat-input ratio of 1 (all-liquid in all channels) is used as a reference, the relative pumping power increases very slightly and then decreases to about 0.6 as the heat-input ratio goes from 1 to 2. Since the total heat input to the system decreases as the ratio increases, this result is not so surprising. Of more significance is the unexpected result that a similar trend exists for the case of fixed-heat input. For example, Figure 37 shows that the relative pumping power required for a heat-input ratio of 1.6 increases very slightly over that of all-liquid conditions and then decreases to 0.76 when boiling starts in Channel 3. This demonstrates that for the entire period before Channel 3 (or, by inference, the last channel in a large group) starts to boil, the required pumping power will be only slightly greater or considerably less than that needed for all-liquid conditions.

III. EVALUATION OF EFFORT DURING FISCAL YEAR 1970

A. SINGLE-PIN TESTS

The single-pin sodium-boiling-initiation tests showed that:

- 1) At 5 psia the maximum observed bulk superheat was 161°F at a sodium velocity of 3.2 ft/sec, heat flux of $0.76 \times 10^6 \text{ Btu/hr-ft}^2$, and 200°F inlet subcooling. For velocities greater than about 4 ft/sec, negative bulk superheats (subcooled nucleate boiling) were observed. The superheat-data trends for thermal and flowrate effects were those of an all-liquid heat balance. For example for otherwise fixed conditions, increasing flowrate (velocity) resulted in lower superheat.
- 2) At 5 psia and heat fluxes typical of an LMFBR the wall superheat did not exceed 50°F at sodium velocities greater than 5 ft/sec. The measured wall superheats were a fraction of those predicted from the Laplace formula using the surface-cavity radius. Both predicted and observed wall superheats should decrease with increasing pressure level.

- 3) Projection of the 5-psia findings discussed in 1 and 2 above to LMFBR conditions (pressure level of at least 15 psia, subcooling of 800°F, sodium velocity 5 to 25 ft/sec) indicates that the initiation wall superheat should be less than 50°F (corresponding to ~4-psi pressure pulse) and that a smooth transition from nonboiling to boiling, with the vapor well distributed in the bulk liquid, should occur. Since boiling most likely starts at the wall, and the bulk fluid is below saturation temperature, the sudden flashing of the bulk liquid to vapor is improbable.
- 4) A Poisson-like statistical distribution exists for the superheat data taken at fixed conditions and it can be interpreted as a particular case of a jump Markovian process.

B. THREE-PIN TESTS

Preliminary evaluation of the three-pin (non-wire-wrapped) boiling-initiation tests shows many similarities to those of the single-channel results. For example an all-liquid heat balance describes the major trends provided by the three-pin data; a Poisson-like superheat-data distribution also exists for the three-pin data. At low velocity (~3 ft/sec) and for given conditions the superheat for one of the channels may cover a broad range at practically constant heat flux, whereas the other channels exhibit a much broader superheat heat-flux characteristic. When velocity is increased to ~6 ft/sec all channels display a fairly wide superheat heat-flux characteristic. Apparently the increased inter-channel mixing produced by the higher velocity tends to make all channels more nearly alike in boiling behavior. A comparison, at an average velocity of 3 ft/sec, of the single-channel and three-channel superheat distributions shows, as expected for three channels, a narrowing of the distribution and a reduction in the maximum wall superheat.

C. SODIFAZE RESULTS

Results from the SODIFAZE multichannel boiling-sodium code showed that the "check curve" (pressure-drop flow) characteristic for three geometrically identical channels with different heat inputs could be substantially different from that normally observed for a single channel. The relatively flat pressure-drop flow feature of the three-channel geometry during the period when each of the

channels attains boiling can be favorable for LMFBR system response and time available for corrective action. Experimental verification of the predicted multi-channel check curve is required and is in progress.

The experimental and analytical results developed so far in the program seem to substantiate the premise that the effects due to superheating under LMFBR conditions are not expected to be severe. However, the experiments are at an early stage of development and further tests are required to ensure that artifacts do not obscure the real significance of the data. In this regard a method for measuring the gas content of the flowing sodium is still required, as well as tests which do not show the effects of test-section wall thermal capacity on flow-coastdown results. The latter type of test probably requires at least seven pins in the test section.

Further investigation of the thermal-hydraulic characteristics of the multi-pin arrays, with and without wire wrap, is needed to determine the effect of cross-flow mixing on check curves and for subsequent comparison with SODIFAZE predictions.

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