34/6-13-77
WAPD-TM-1296

ERDA Research and Development Report

**FLASH-6 Simulation of Top Injection
Emergency Core Cooling Heat
Transfer Tests
(LWBR Development Program)**

F. W. Lincoln

**Bettis Atomic Power Laboratory
West Mifflin, Pennsylvania 15122**

May 1977

**Prepared for the
U.S. Energy Research
and Development Administration
By Westinghouse Electric Corporation
Under Contract No. EY-76-C-11-0014**

MASTER

DISTRIBUTION OF THIS DOCUMENT IS UNLIMITED

DISCLAIMER

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

DISCLAIMER

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

FLASH-6 SIMULATION OF TOP INJECTION EMERGENCY
CORE COOLING HEAT TRANSFER TESTS
(LWBR Development Program)

F. W. Lincoln

Contract No.: EY-76-C-11-0014

MAY 1977

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

Printed in the United States of America
Available from the
National Technical Information Service
U. S. Department of Commerce
5285 Port Royal Road
Springfield, Virginia 22151

NOTE

This document is an interim memorandum prepared primarily for internal reference and does not represent a final expression of the opinion of Westinghouse. When this memorandum is distributed externally, it is with the express understanding that Westinghouse makes no representation as to completeness, accuracy, or usability of information contained therein.

MASTER

BETTIS ATOMIC POWER LABORATORY

WEST MIFFLIN, PENNSYLVANIA

Operated for the U. S. Energy Research & Development Administration by
WESTINGHOUSE ELECTRIC CORPORATION

57
DISTRIBUTION OF THIS DOCUMENT IS UNLIMITED

NOTICE

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

TABLE OF CONTENTS

<u>SECTION</u>	<u>PAGE</u>
ABSTRACT	1
I. INTRODUCTION	1
II. TEST DESCRIPTION	2
A. Test Bundle	2
B. Test Facility	3
C. Test Procedure	3
D. Test Results	4
III. FLASH-6 MODELING OF TEST NO. 1011	5
A. Model Description	5
B. Modeling Sensitivity Studies	6
1. Time Step Size	6
2. Core Flow Path Nodalization	7
3. Flow Path Hydraulic Resistance	7
IV. RESULTS OF THE FLASH-6 SIMULATIONS	8
A. Introduction	8
B. Comparison of FLASH-6 Beyond CHF Heat Transfer Options	9
1. Modified Dittus-Boelter Correlation	9
2. Dougall-Rohsenow Correlation	10
C. Discussion of Results	12
V. CONCLUSIONS AND RECOMMENDATIONS	14
References	15
Acknowledgements	15
TABLE 1	16
Figures 1 - 12	

Data from top injection ECCS tests conducted at Columbia University have been analyzed as part of an effort to qualify the FLASH-6 computer program for performing post-blowdown heat transfer calculations for the LWBR Safety Analysis. These experiments, which employed a full-scale fuel assembly with electrical heater rods to simulate an inlet rupture for a pressurized water reactor, provided test conditions and rod cooling mechanisms quite similar to those encountered in the postulated LWBR cold leg break loss-of-coolant accident. Clad temperature predictions were obtained using both the modified Dittus-Boelter and the Dougall-Rohsenow correlations to evaluate beyond CHF heat transfer coefficients.

Overall comparisons using the FLASH calculated flow rates indicated that the rod temperature calculations were conservative using either of the heat transfer correlations because virtually none of the coolant was calculated to penetrate the heated test assembly. Heat transfer model comparisons were also performed by adjusting the calculation so that coolant was injected directly into the top of the rod bundle to simulate the experimentally observed flow conditions. Once this downflow was forced, conservative temperature predictions were obtained using the Dougall-Rohsenow correlation, whereas the modified Dittus-Boelter beyond CHF option yielded non-conservative results.

FLASH-6 SIMULATION OF TOP INJECTION EMERGENCY
CORE COOLING HEAT TRANSFER TESTS
(LWBR Development Program)

I. INTRODUCTION

A series of top injection emergency core cooling tests have been conducted at Columbia University (Reference 1). Heat transfer data were obtained using a full-scale fuel assembly with electrical heater rods to simulate a cold leg break for a pressurized water reactor with low inlet nozzles and an internal flow geometry closely resembling the LWBR design.

Test conditions and core cooling mechanisms were also quite similar to those encountered in the postulated LWBR inlet break.

This report describes the FLASH-6 modeling of one of the Columbia University experiments and presents comparisons of several FLASH-6 predictions with the test data. Results were obtained using two different beyond CHF heat transfer correlations in an effort to qualify the FLASH-6 reflood heat transfer calculations used for the LWBR safety analysis. A description of the FLASH-6 computer program is given in Reference 2.

II. TEST DESCRIPTION

A. Test Bundle

The Columbia University top injection tests were conducted using a fuel assembly canister containing 173 electrically heated rods (14 x 14 array with 23 rods removed to allow passage of control rods). Design parameters and principal dimensions for the test bundle areas are as follows:

Heated Length - 100 inches

Heater Rod Diameter - 0.315 inches

Heater Rod Pitch (square) - 0.45 inches

Cladding (Sheath) Thickness - 0.026 inches

Bundle Power - 403.9 KW

Average Heat Flux - $10,600 \frac{\text{Btu}}{\text{hr-ft}^2}$

Axial Power Peaking Factor - $P_{\text{max}}/P_{\text{avg}} = 1.65$

Radial Power Peaking Factor - $P_{\text{max}}/P_{\text{avg}} = 1.23$

The axial power profile for the heater rods is bottom-skewed with the peak heat flux occurring at about 30 percent up the heated length. Five rod groups are utilized to obtain the desired radial power distribution. Rods having the highest peaking factors are located near the canister walls.

In the test bundle internal flow geometry there are ten "egg-crate" support grids equally spaced along the height of

the heater rod assembly. A top nozzle assembly (see Figure 1) and a perforated coolant distribution plate located at the base of the nozzle are attached to the top of the fuel assembly canister. The bottom nozzle assembly and grid adapter plate **are simulated by a perforated plate.**

B. Test Facility

A schematic of the test loop employed in the cold leg break experiments is shown in Figure 1. The major components of the loop are the test section assembly, the hot and cold leg piping and core bypass piping, and upper volume tank, a steam system to simulate containment conditions, and the core cooling system.

The test section assembly consists of a 9.5 foot high upper housing with an interior geometry simulating a quarter section of the upper control rod assembly, and a lower housing containing the fuel assembly canister and heater rods. Strip heaters are attached outside the housing to simulate adjacent fuel assemblies. A bottom extension assembly simulating the cold leg piping is attached to the bottom of the lower housing.

The upper volume tank is connected to the top of the upper housing and contains the emergency coolant spray nozzles. A plate at the bottom of the tank contains two 7/8 inch diameter holes which control the flow of coolant into the upper housing above the test section. The holes are positioned so that a portion of the coolant falls directly into the rod bundle through the top nozzle, while the remainder collects on the plate separating the upper and lower housings and then flows weir-fashion into the top nozzle and the bundle. A perforated plate is located below the top nozzle just above the heated rods to promote uniform flow distribution within the test bundle.

C. Test Procedure

The tests were run by first preheating and purging the system with steam. After the vents were closed and the required pressure established, power was supplied to the rods. When the desired initial

rod temperatures were reached, the core coolant spray and rod power decay were initiated simultaneously. The power decay curve used in the tests was based on the standard ANS + 20 percent decay heat curve prescribed by Appendix K of 10-CFR-50. System pressure was maintained during the experiments by an automatic regulating valve and an auxiliary steam supply. The tests were terminated when either all rods quenched, or a temperature of 1850°F was reached.

Data recorded during the tests included spray water flow rate, pressures at various points in the system, and heater rod temperatures. The rod temperature measurements were obtained at four axial levels and several radial locations. A fully instrumented octant (28 rods) was employed at the peak heat flux position (level 2).

D. Test Results

The original Columbia University test program objectives included runs for both the hot and cold leg break configurations with systematic variations in bundle power, initial rod temperature, inlet subcooling, and system pressure. Only a few cold leg break experiments have been reported to date. Some preliminary results for these tests are given in Reference 1.

The test runs described in Reference 1 include a low power test, one low and one high spray temperature test, and a high initial rod temperature test. Only the high spray temperature test results are reported in sufficient detail for comparison with FLASH predictions. Figure 2 shows the measured clad temperature history at the peak heat flux position for this test (Test No. 1011). Inferred clad temperature behavior for the other runs is also shown. The curves for runs 1003, 1010 and 1012 are based on initial and peak rod temperature data only (clad temperature histories were not provided). Straight lines are drawn between the initial and peak temperatures, and for runs 1003 and 1010, the slopes of the temperature curves prior to quenching are assumed to be similar to the results for run 1011. Approximate quench times were arrived at by examining plots of heat transfer coefficient versus time. All of the tests were conducted with a coolant injection rate of 15 gpm and a containment pressure of 29.7 psia. The high initial temperature run (No. 1012) was aborted when the clad temperature reached 1850°F. This was attributed to negligible coolant flow into the bundle due to water holdup in the top volume tank during the initial part of the transient.

III. FLASH-6 MODELING OF TEST NO. 1011

A. Model Description

The FLASH-6 model employed in simulating the Columbia University tests is shown in Figure 3. Five nodes and seven flow paths were used to represent the test loop. The rod bundle was simulated using a single heated path divided into five axial segments. Use of single core flow path model rather than a multiple channel representation was based on the following considerations:

- (1) Clad temperature data were provided for only one heater rod which had a radial peaking factor approximately equal to the bundle average radial peaking factor.
- (2) Experimentally determined heat transfer coefficients were essentially independent of radial position.
- (3) Previous FLASH analyses have shown that a one-heated-path model minimizes rod temperature predictions and thus results in a more severe test of FLASH-6 conservatism.

Table I gives the significant FLASH-6 input parameters employed in the analysis. Two different beyond CHF heat transfer correlations (available as options in FLASH-6) were used to evaluate convection heat transfer coefficients in the film boiling regime:

- (1) Modified Dittus-Boelter
- (2) Dougall-Rohsenow

The conservative form of the McDonough, Millich and King partial film boiling correlation was used in conjunction with both these methods. A minimum Nusselt number of 4.0 was specified for use with the Dougall-Rohsenow option and an equivalent minimum film boiling heat transfer coefficient of 1.87 was used in the Dittus-Boelter calculations. These minimum values were based on laminar flow data for flow in tubular channels (Reference 3). Core and

non-core hydraulic resistances were evaluated using the detailed FLASH-6 flow path model which incorporates a Reynolds number and surface roughness dependent friction factor calculation with a correction for two-phase flow. The flow path resistance input parameters listed in Table I represent best estimate values. An overall loss coefficient for the rodged test section (grid loss plus rod friction) was estimated using a correlation developed from pressure drop data for a similar geometry (Reference 4). Flow resistance factors for the two 7/8 inch diameter holes in the base of the upper volume tank and the two perforated coolant distribution plates were based on the results of full scale mockup tests sponsored by Consolidated Edison (Reference 5). Pressure losses for the hot and cold leg piping and the core bypass piping were determined using the empirical form loss coefficients and the L/D values shown in Table I.

The electrical heater rods used in the tests were simulated using two radial fuel element nodes: an inner node representing the heating element and insulation, and an outer node representing the cladding. Volumetric heat capacity and thermal conductivity for the two regions were specified as functions of temperature using the fuel element variable thermal properties model.

Selection of a final FLASH-6 model to analyze the Columbia University data was based on the results of parameter sensitivity studies described in the next section.

B. Modeling Sensitivity Studies

1. Time Step Size

A user specified time step size ranging from 100 to 500 microseconds was employed in all FLASH-6 simulations reported herein. Time step convergence was determined by rerunning selected intervals of the FLASH-6 calculations with one-half the original time step size and comparing the results. This procedure was repeated until sufficiently small variations in flow rate and clad temperature were obtained (e.g.,

less than 1 percent difference in temperatures between successive runs).

Figure 4 shows the predicted clad surface temperatures, heat transfer coefficients, qualities and flow rates obtained using time step sizes of 250 and 500 microseconds. The results demonstrate that a converged solution has been obtained, and also indicate that flow rate is the most sensitive of the four parameters shown.

2. Core Flow Path Nodalization

FLASH-6 allows the user to represent a core channel as a chain of nodes interconnected by flow paths. Separate fuel element heat transfer calculations and mass and energy balances are done for each axial segment. The degree of detail to which the heated path is described (i.e., the number of core nodes) is usually a balance between accuracy and computer run time.

A core flow path with five axial segments was selected to represent the heated test section in the Columbia University experiments. To test the accuracy of this model, a more detailed FLASH model was constructed and the results were compared. Figure 5 shows clad temperature and heat transfer coefficient predictions obtained with the five node model and also with a 15 node model. The calculations show very good agreement, with temperatures differing by less than two percent; hence it was concluded that five axial levels provided sufficient detail to model the experiment. It is worth noting that the more detailed model required approximately five times as much run time as the simpler model because of the increased nodalization and a smaller time step size necessary for convergence.

3. Flow Path Hydraulic Resistance

The sensitivity of the FLASH-6 model to hydraulic resistance was determined by varying the flow path loss coefficients independently and comparing the resulting clad temperature predictions. Figure 6 shows calculations based on the best estimate resistance model described in Section III.A, a model with the core orifice loss eliminated, and a model with the bypass resistance coefficient (path 5) reduced by

90 percent. With the orifice losses at both ends of the test section removed, there was essentially no resistance to the flow of coolant water into the core region (rod friction and grid losses were found to be negligible). Since the results are almost identical to the best estimate calculations, it appears that elevation head and pressure differences between nodes controlled the liquid flow rate into the rod bundle. The pressure differential between the top core node and the upper housing was strongly influenced by depressurization of the top volume tank caused by the subcooled injection water condensing the steam in the tank. Results obtained with only ten percent of the bypass resistance indicate water penetration into the core, and hence increased rod cooling during the initial portion of the transient. The lower pressure drop allowed steam to flow from the containment node into the upper volumes and reduce the amount of depressurization by replacing some of the steam condensed by the coolant. Predicted temperatures for the three models are in good agreement beyond about 50 seconds; thus it was concluded that the final result is not sensitive to flow path hydraulic resistance.

IV. RESULTS OF THE FLASH-6 SIMULATIONS

A. Introduction

The FLASH-6 predictions presented in this section were obtained using the input parameters given in Table I. Simulated test conditions were as follows:

Peak Initial Clad Temperature - 790°F
Rod Power, % of Nominal - 100
Coolant Injection Rate - 15 gpm
Spray Temperature - 220°F
Containment Pressure - 29.7 psia

Experimental data (rod temperature measurements and computed heat transfer coefficients) were provided for only one position in the bundle: pin no. 15 at level 2, where the peak axial power occurred. Consequently, all comparisons between FLASH calculations and the test results pertain to this location. The radial peaking factor at this position was approximately equal to one. Thus the data for this rod should be

representative of average bundle conditions as should the FLASH-6 predictions obtained with the single heated path model.

B. Comparison of FLASH-6 Beyond CHF Heat Transfer Options

1. Modified Dittus-Boelter Correlation

The modified Dittus-Boelter correlation is simply the Dittus-Boelter **correlation** evaluated using a reference heat transfer coefficient based on saturated **vapor properties** at a reference pressure and a reference mass flow rate with a correction for variations in total mass flow rate. Since the Reynolds number is based on total mass flow rate rather than vapor mass flow rate, this correlation is independent of quality and thus can be considered an upper limit for heat transfer coefficient in the **dispersed flow film boiling regime**.

The simulations were performed in two different ways to test the accuracy and conservatism of the post-blowdown heat transfer calculations. First, an overall comparison with the data was obtained by simulating the actual test conditions and coolant injection location using the FLASH-6 model shown in Figure 3. Secondly, forced flow comparisons were made by injecting coolant directly into the top of the rod bundle to provide a more accurate representation of the observed test section flow conditions and thus a more stringent test of heat transfer models. Clad surface temperatures and heat transfer coefficients predicted using the modified Dittus-Boelter correlation (for stable film boiling) are shown compared with the test data in Figures 7 and 8. FLASH-6 calculated mass flow rates within the rod bundle and also the forced flow injection rate employed are given in Figure 9.

When the calculations were made using the actual test conditions, FLASH predictions of clad temperatures and heat transfer coefficients were very conservative because virtually none of the coolant was calculated to penetrate the simulated core region. This calculated behavior conflicted with observations made during the test^x which indicated very little buildup of water above the core. As explained previously in Section III.B.3, FLASH predicts that the top volume tank and upper housing are depressurized as steam is condensed by the subcooled fill water. Consequently, the elevation head associated with the mixture

* Information obtained from Mr. D. M. Speyer, Consolidated Edison Co., New York, N.Y., December 1975.

level above the heated test section is insufficient to overcome the pressure differential and liquid holdup is predicted. This is reflected in the very conservative clad temperatures and heat transfer coefficients shown in Figures 7 and 8 for the overall model predictions. The predominant heat transfer mechanism in these calculations is convection to saturated or superheated steam at very low mass flow rates (see Figure 9) resulting in an excessive heatup rate.

The forced flow predictions were generated by artificially injecting coolant directly into the top of the rod bundle at a prescribed rate. In this case, the control volumes simulating the top volume tank, upper housing, and core bypass piping were eliminated from the FLASH model shown in Figure 3. Since the actual liquid mass flow rate into the top of the bundle was not measured in the experiments, a simple linear variation (shown in Figure 9) from zero flow at time zero to the actual spray injection rate at 100 seconds with constant flow thereafter was assumed for the analysis. The slope selected for the flow ramp was estimated from calculations using the complete FLASH model with saturated spray water injected into the top volume tank, and also by examining the experimental heat transfer coefficient behavior shown in Figure 8.

The clad surface temperature and heat transfer coefficient predictions obtained from the forced flow simulation were very non-conservative as shown in Figures 7 and 8. Computed mass flow rate at the peak heat flux position (Figure 9) is less than the forced injection rate until about 50 seconds when the predicted rod temperature begins to drop rapidly. Rod quenching is calculated to occur much earlier than in the test (95 seconds versus 250 seconds) due to the high film boiling heat transfer coefficients predicted using the modified Dittus-Boelter correlation for low quality two-phase flow conditions.

2. Dougall-Rohsenow Correlation

The Dougall-Rohsenow beyond CHF heat transfer correlation is applicable to the low quality dispersed flow film boiling regime, and is essentially the Dittus-Boelter correlation with the Reynolds number based on the saturated vapor density and the homogeneous two-phase

mixture velocity. At qualities close to 1.0, the two correlations yield approximately the same value of heat transfer coefficient.

Figures 10 and 11 show the comparisons between the FLASH-6 predictions made using the Dougall-Rohsenow correlation and the test data. The calculations were performed using both the overall model with coolant injected into the top volume tank as in the experiment, and the forced flow model with coolant artificially injected into the top of the rod bundle. Results obtained from the overall model calculations were very conservative (similar to the modified Dittus-Boelter predictions) as evidenced by the clad temperature response shown in Figure 10 (heat transfer coefficient and mass flow rate predictions are not shown for this case).

The forced flow clad temperature predictions presented in Figure 10 were obtained using the different coolant injection rate schedules. First, the simulation was performed using the flow rate ramp employed in the modified Dittus-Boelter calculations (schedule A in Figure 12). Secondly, the slope of the flow ramp was increased by a factor of two (schedule B in Figure 12) in order to assess the sensitivity of the heat transfer predictions to the artificially forced injection rate. As shown in Figure 10, the injection rate schedule had only a minor **effect** on the predicted rod temperatures throughout most of the transient. In fact, the maximum temperatures predicted using the two forced flow models were within 25°F of the measured peak clad surface temperature. Furthermore, both simulations were conservative in that turnaround time (time at which the maximum temperature occurred) and quench time were overpredicted. As shown in Figure 10, flow schedule A gives the best overall agreement with the rod temperature data.

Forced flow heat transfer coefficient and mass flow rate predictions obtained using coolant injection rate schedule A are given in Figures 11 and 12. The calculated heat transfer coefficient show reasonable agreement with the data during the initial portion of the transient except for the large discrepancy of about 50 seconds. As shown in Figure 11, the large calculated heat transfer coefficients subsequent to 50 seconds correspond to the rapid increase in mass flow

rate at the peak heat flux position during this time period. This behavior was found to be the result of the predicted quenching of a cooler region in the rod bundle above the peak heat flux position. When quenching occurred in the calculations, the high heat flux associated with the return to nucleate boiling caused increased steam generation in the simulated core which in turn produced high steam velocities below the peak heat flux position and the predicted increase in heat transfer coefficient.

Later in the calculations when the full spray injection rate for the test (2 lbm/sec) was established at the peak heat flux position, the heat transfer coefficient predictions shown in Figure 11 were conservative with respect to the data. The FLASH calculations were continued beyond the experimental quench time for level 2, but were terminated at 400 seconds since there was still no indication of impending rod quenching.

3. Discussion of Results

Comparison of the FLASH-6 calculations with the test results has shown that the conservatism of the rod temperature predictions is dependent upon how accurately the bundle flow rate is predicted. In the overall FLASH model comparisons where coolant was injected above the simulated core to model the actual test conditions, very conservative predictions were obtained using either the modified Dittus-Boelter or the Dougall Rohsenow beyond CHF correlation. This was because virtually none of the coolant penetrated the rod bundle in the calculations whereas very little liquid holdup was observed in the test.

The conservative nature of FLASH-6 in not allowing downflow is primarily a consequence of limitations in the calculational model; in particular, the assumptions that the flow is homogeneous (no slip between the liquid and vapor phases) and that the liquid and vapor are in thermal equilibrium. The most probable flow regime during the test would correspond to countercurrent flow with liquid droplets flowing downward through the rod bundle and steam flowing upward. This would allow steam from the test section to enter the upper volumes and mix with the subcooled injection

water to relieve the depressurization effect. Because of the homogeneous flow assumption in FLASH-6 the only possible flow regime which could provide cooling in the simulation is cocurrent downflow. It is also unlikely that the subcooled water and steam contained in the top volume tank and upper housing attained thermal equilibrium, since the test results indicate that these regions were not sufficiently depressurized to cause liquid holdup. FLASH-6 assumes instantaneous equilibrium whereas the actual equilibration of the liquid and vapor phase temperatures occurs in a finite time interval.

The FLASH-6 predictions made with forced coolant injection into the top of the rod bundle provided an effective means of evaluating the relative performance of the two FLASH-6 beyond CHF heat transfer options. Predicted mass flow rates at the peak heat flux location were approximately equal to the coolant spray injection rate used in the experiment. Based on the limited information available (i.e., visual observations only), the simulated bundle flow was judged to be representative of the test conditions.

The principal flow regime existing in the bundle prior to rewetting can be described as a dispersed flow with steam adjacent to the rods. Because of the high clad temperatures (above the Leidenfrost or minimum heat flux point), liquid droplets are prevented from contacting the rod surfaces. The associated heat transfer mechanism is primarily convection to steam with droplet evaporation helping to maintain the bulk fluid near saturation temperature. This beyond CHF heat transfer regime was modeled reasonably well using the Dougall-Rohsenow correlation which assumes homogeneous flow and includes a correction to the calculated mixture mass flow rate to account for the presence of liquid droplets. The modified Dittus-Boelter correlation does not include the two-phase flow correction, and thus predicts non-conservative clad temperatures and heat transfer coefficients in the low quality (less than 50% dispersed flow regime).

The fact that quenching was not predicted in the Dougall-Rohsenow forced flow simulation can be ascribed to excess conservatism in both the dispersed flow film boiling calculations and the modeling of transition from film boiling to nucleate boiling. It can be inferred

from the experimental clad temperature and heat transfer coefficient curves (e.g., Figures 10 and 11), that a transition to partial film boiling occurs at about 250 seconds in the transient. Once droplet contact heat transfer becomes significant, the surface is quickly cooled to near saturation temperature and the rewetting process is essentially complete. The conservative form of the McDonough, Millich and King partial film boiling correlation used in the analyses does not allow transition to this regime until the wall superheat becomes quite low (on the order of 300°F). Consequently, the FLASH-6 simulations would not calculate quenching to occur until the clad temperature reached about 550°F.

V. CONCLUSIONS AND RECOMMENDATIONS

1. FLASH-6 calculations based on the actual test conditions (overall model predictions) produced very conservative results, regardless of the method used to evaluate beyond CHF heat transfer coefficients. This was attributed to depressurization of the upper volumes which tended to prevent coolant in the calculations from penetrating the simulated core region.
2. Comparison of FLASH-6 beyond CHF options using artificially forced, more realistic bundle flow rates showed that (a) the modified Dittus-Boelter correlation was non-conservative at qualities less than one, and (b) the Dougall-Rohsenow correlation conservatively predicted the data.
3. A more accurate and less conservative prediction of post-blowdown thermal-hydraulic behavior could be achieved by further FLASH development in the areas of beyond CHF heat transfer, counter-current flow*, and non-equilibrium modeling.

* A drift flux model simulating gravity-induced cocurrent and countercurrent slip flow between control volumes has been incorporated in the FLASH-6 computer program and is currently undergoing qualification testing.

REFERENCES

1. "Indian Point Unit No. 1 ECCS Evaluation Model," Section IV, Consolidated Edison Co., August 1975
2. J. J. Beyer, W. D. Peterson, D. A. Prelewicz and G. W. Swartele, "FLASH-6: A FORTRAN-IV Program for Reactor Plant Loss-of-Coolant Accident Analysis," WAPD-TM-1249, February 16, 1976
3. J. G. Knudsen and D. L. Katz, Fluid Dynamics and Heat Transfer, McGraw-Hill, 1958, p. 385
4. B. W. LeTourneau and S. J. Green, "Critical Heat Flux and Pressure Drop Tests with Parallel Upflow of High Pressure Water in Bundles of Twenty 1/2-inch Rods," Nuclear Science and Engineering 43, pp. 90-104 (1971)
5. USAEC Docket-50003-103, "Loss-of-Coolant Analysis for Indian Point Unit No. 1 Using the USAEC 1971 Interim Criteria," March 1972

ACKNOWLEDGEMENTS

The author would like to thank J. J. Beyer, D. A. Prelewicz and G. W. Swartele for their advice and technical assistance. Special thanks are due to J. F. Maxin for his assistance in running the FLASH problems. Also, the cooperation of Mr. D. M. Speyer of Consolidated Edison Company is gratefully acknowledged.

TABLE I

FLASH-6 Input Parameters for Test No. 1011

<u>PARAMETER</u>	<u>VALUE</u>
Test Section Hydraulic Diameter	0.0342 ft
Core Flow Area	0.1444 ft ²
Core Heat Transfer Area	128.9 ft ²
Core Heated Length	8.33 ft
Reference Volumetric Heat Generation Rate	$2.0845 \times 10^6 \frac{\text{BTU}}{\text{hr-ft}^3}$
Reference Forced Convection Heat Transfer Coefficient (Dittus-Boelter equation evaluated at 29.7 psia)	$0.0664 \frac{\text{BTU}}{\text{hr-ft}^2-\text{°F}}$
Minimum Nusselt Number for Film Boiling Heat Transfer Coefficient Calculations	4.0
Fill System Flow Rate	15 gpm
<u>Flow Path Hydraulic Resistances:</u> *	
Path 1 - Test Section	
(a) Grid Loss + Rod Friction	$K = 37.8 \text{ Re}^{-0.11}$
(b) Inlet and Outlet Orifice Loss	$K = 19.16$
Path 2 - Two 7/8-Inch Diameter Holes	$L/D = 1.0$ $K = 1.98$
Path 3 - Four-Inch Schedule 40 Pipe with 90° Elbow	$L/D = 10.0$ $K = 0.4$
Path 4 - Four-Inch Schedule 40 Pipe with Tee	$L/D = 10.0$ $K = 1.0$
Path 5 - Four-Inch Schedule 40 Pipe with 1/2-Inch Diameter Orifice	$L/D = 60$ $K = 5860^{**}$
Path 6 - Four-Inch Schedule 40 Pipe	$L/D = 15.0$

* A Reynolds number dependent friction factor is used for all flow paths.

** Based on pipe flow area.

FIGURE I. TEST LOOP SCHEMATIC - COLD LEG BREAK CONFIGURATION

TEST SUMMARY

<u>SYMBOL</u>	<u>TEST NO.</u>	<u>% POWER</u>	<u>SUBCOOLING</u>
□	1003	65	95°F
△	1010	100	165°F
○	1011	100	30°F
▽	1012	100	90°F

FIGURE 2. MEASURED AND INFERRED CLAD TEMPERATURE BEHAVIOR FOR VARIOUS TEST CONDITIONS

FIGURE 3. FLASH-6 MODEL FOR COLUMBIA UNIVERSITY TOP INJECTION TESTS

TEST NO. 1011-LEVEL 2
DOUGALL-ROHSENOW OPTION
(SATURATED SPRAY WATER)

□ TIME STEP = 0.0005 SEC
△ TIME STEP = 0.00025 SEC

FIGURE 4. TIME STEP CONVERGENCE STUDY

TEST NO. 1011-LEVEL 2, PIN NO.15
DITTUS-BOELTER OPTION
(SATURATED SPRAY WATER)

FIGURE 5. SENSITIVITY OF THE MODEL TO
CORE FLOW PATH NODALIZATION

TEST NO. 1011 - LEVEL 2, PIN NO.15
DOUGALL-ROHSENOW OPTION

FIGURE 6. SENSITIVITY OF THE MODEL TO
FLOW PATH RESISTANCE

COLUMBIA UNIVERSITY TOP INJECTION TESTS
(MODIFIED DITTUS - BOELTER CORRELATION)

FIGURE 7. MEASURED AND PREDICTED CLAD SURFACE
TEMPERATURES FOR TEST NO. 1011

COLUMBIA UNIVERSITY TOP INJECTION TESTS
(MODIFIED DITTUS - BOELTER CORRELATION)

FIGURE 8. MEASURED AND PREDICTED HEAT TRANSFER COEFFICIENTS AT PEAK HEAT FLUX POSITION FOR TEST NO. 1011

COLUMBIA UNIVERSITY TOP INJECTION TESTS
(MODIFIED DITTUS-BOELTER CORRELATION)

FIGURE 9. PREDICTED MASS FLOW RATES AND
FORCED INJECTION RATE FOR TEST NO. 1011

**COLUMBIA UNIVERSITY TOP INJECTION TESTS
(DOUGALL-ROHSENOW CORRELATION)**

**FIGURE 10. MEASURED AND PREDICTED CLAD SURFACE
TEMPERATURES FOR TEST NO. 1011**

COLUMBIA UNIVERSITY TOP INJECTION TESTS
(DOUGALL-ROHSENOW CORRELATION)

FIGURE 11. MEASURED AND PREDICTED HEAT TRANSFER COEFFICIENTS AT PEAK HEAT FLUX POSITION FOR TEST NO. 1011

COLUMBIA UNIVERSITY TOP INJECTION TESTS
(DOUGALL-ROHSENOW CORRELATION)

CALCULATED MASS FLOW RATE
AT PEAK HEAT FLUX POSITION

MASS FLOW RATE INTO TOP OF BUNDLE
FOR FORCED FLOW SIMULATIONS

U.S. GOVERNMENT PRINTING OFFICE: 1977-703-011/480

FIGURE 12. PREDICTED MASS FLOW RATE AND
FORCED INJECTION RATES FOR TEST NO. 1011