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MASTER

COMMENTS ON THE APPLICATION OF PWR-FLECHT
EXPERIMENTAL RESULTS TO A LOCA

W. R. Gambill
J. J. Keyes
T. S. Kress
C. G. Lawson

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SUMMARY

This report contains the results of four studies that are concerned with the Pressurized Water Reactor Full Length Emergency Cooling Heat Transfer (PWR-FLECHT) tests and application of the results of these tests to calculation of the temperature of fuel pin cladding under conditions that might prevail in a PWR during the reflood portion of a loss of coolant accident. The report is divided into four parts.

- (1) Description of the PWR-FLECHT Tests
- (2) Effect of Uniform Coplanar Blockage on Reflood Rate
- (3) Effect of Droplet Entrainment on Heat Transfer During Reflood
- (4) Estimate of the Effect of Localized Flow Blockages on PWR Clad Temperature During Reflood

The objective of the FLECHT test program as stated by Westinghouse was

"to investigate the behavior of a simulated pressurized water reactor core during the core recovery period that follows a loss-of-coolant accident.

The following specific information will be obtained:

1. Time-temperature behavior of the cladding
2. Conditions that result in adequate core cooling
3. Minimum flooding heights at various power densities
4. Effectiveness of core coolant system to prevent core damage
5. Effect of pressure on emergency core cooling (capability)
6. Effect of soluble poison on emergency core cooling
7. The effect of cladding damage (on emergency core cooling)
8. Local pressure drop."

All the results are for tests with forced flow of water to the test bundle. The tests with constant flooding rate show that rates above 2 in./sec are likely to keep the peak cladding temperature of present PWR designs below 2300°F if the temperature at the beginning of reflood does not exceed 2200°F.

If the flooding begins at a high rate of 5 to 10 in./sec for several seconds, then a flooding rate as low as 1 in./sec for the remainder of the transient may be sufficient to limit the peak cladding temperature.

Calculations were made to investigate the effect of core blockage in reducing the reflood rate in typical four-loop and two-loop, four-pump PWR plants. The calculations indicated that exit flooding rates of 1.0 and

1.3 in./sec could be expected with an unblocked core in a four-loop and a two-loop, four-pump plant, respectively, with the containment pressure at 50 psia. Reducing the containment pressure to 25 psia would reduce the exit flooding rate to 0.75 to 0.9 in./sec with an unblocked core. Corewide midplane blockage greater than 90% would be required to reduce the exit flooding rate by 25%, and blockage of 70% would have a negligible effect with a containment pressure of 50 psia. The pressure drop and reflood rates that were calculated agree acceptably with the more elaborate calculations of Combustion Engineering and Westinghouse.

The effect of droplet entrainment on heat transfer during reflood was estimated. These calculations show that when the flooding rate is 1 in./sec the presence of droplets could increase the steam convection heat transfer coefficient from values less than 1 to values of 10 and 20 Btu/hr ft² °F (based on the coolant temperature at saturation) depending upon the amount of entrained liquid carried in the vapor. The calculated heat transfer coefficients have the same trend and magnitude as the reported data. In addition more attention should be given to the magnitude of the heat removal coefficients in the upper half of the core where the steam quality is relatively high.

Calculations were made of cladding temperature changes associated with various localized partial flow blockages (subchannel area reductions) during reflood following a FWR LOCA. A physically realistic model which allows for radial flow-diversion effects was adopted by equating pressure drops for regions of high blockage with pressure drops for adjacent regions of lesser or zero blockage over various flow-disturbance heights. The choice of the finite heights with which the blocked-region flow reductions were calculated was guided by experimental and calculated (COBRA-III) values for the upstream-diversion and downstream-redistribution lengths (5 to 8 d_e and 20 to 60 d_e , respectively) below and above the blockage corresponding to radial flow/pressure equilibration. The maximum value of total flow-disturbance length selected corresponds to 30% of the height of a 12-ft assembly.

The analysis yields hot-pin cladding temperatures for nine of the ten blockage combinations examined that exceed the temperature for the

unblocked case by 400°F or more. By introducing a nonarbitrary, but uncertain, blocked-region heat-transfer coefficient multiplier of ~1.5 in an attempt to account for reduced-equivalent-diameter and entrance-region effects, the peak cladding temperatures were reduced significantly. Negative values of Δt_{clad} were predicted for some blockage combinations; but the results are strongly dependent on the radial blockage distribution, and the increases in cladding temperature for a 90% blockage embedded in a region of 0% blockage, for 90% in 50%, for 90% in 70%, and for 80% in 0% were $\geq 400^\circ\text{F}$. When the heat-transfer coefficient multiplier was increased to 2.0 (an increase which would correspond to a steam desuperheating of $\sim 420^\circ\text{F}$ caused by droplet evaporation in the blockage region), the cladding temperature increase for only the 90% in 0% and 90% in 50% cases remained above 400°F. The subject estimates of cladding temperature rise for high blockage significantly exceed estimates based on overall hot-assembly flow reduction in the absence of lateral flow diversion.

Recommendations for further study are made in each of the subsections where the author considered them appropriate.

INTRODUCTION

The emergency core cooling system of a PWR is designed to flood the reactor vessel of a plant that has undergone a hypothetical loss-of-coolant accident (including any pipe size up to and including a double-ended guillotine break of the main circulating pipe) without violating the physical limits established by the Interim Acceptance Criteria.

Data for use in assessing the capability of these emergency core cooling systems to accomplish this reflood were provided by the PWR-FLECHT Program that was performed by Westinghouse Electric Corporation under sub-contract C-625 with the Phillips Petroleum Company. The interpretation of the data from this program is in contention because the test conditions may be mostly nontypical. Consequently a review of these experiments and the applicability of the results was undertaken in support of the AEC Regulatory staff review of the testimony from rulemaking hearings on the Interim Acceptance Criteria for Emergency Core Cooling System of Light Water Reactors.

This review consists of four separate parts:

Part I. PWR FLECHT - Description of Tests by C. G. Lawson

The tests and the test variables are described and the range of the test variables are compared with the reactor conditions.

Part II. Effect of Uniform Coplanar Blockage on Reflood Rate by J. J. Keyes

The constant flooding rate and the effect of core blockage upon the flooding rate are calculated for typical PWR configurations.

Part III. Effect of Droplet Entrainment on Heat Transfer During Reflood by T. S. Kress

The phenomena that cause entrained droplets to effect the FLECHT heat transfer coefficients are examined and the magnitudes of the effect are estimated.

Part IV. Estimate of the Effect of Localized Flow Blockages on PWR Clad Temperature During Reflood by W. R. Gambill

The effect of realistically shaped blockages upon the cladding temperature rise downstream (above) of the immediate swelling location is calculated. The effects of flow diversion, entrance lengths, blocked region length and radial blockage are included in the calculations.

The four papers are included in this one report in order to make the information accessible in one place.

I. DESCRIPTION OF TESTS WITH REACTOR COMPARISON

C. G. Lawson

SUMMARY

The Pressurized Water Reactor Full Length Emergency Cooling Heat Transfer Program (PWR-FLECHT) was conducted by Westinghouse Electric Corporation under Subcontract C-625 with the Phillips Petroleum Company.

The objective of the test program as stated by Westinghouse was "to investigate the behavior of a simulated pressurized water reactor core during the core recovery period that follows a loss-of-coolant accident.

The following specific information will be obtained:

1. Time-temperature behavior of the cladding.
2. Conditions that result in adequate core cooling.
3. Minimum flooding height at various power densities.
4. Effectiveness of core coolant system to prevent core damage.
5. Effect of pressure on emergency core cooling capability.
6. Effect of soluble poison on emergency core cooling.
7. The effect of cladding damage.
8. Local pressure drop."

All the results are for tests with forced flow of water to the test bundle. The tests with constant flooding rate show that rates above 2 in./sec are likely to keep the peak cladding temperature of present PWR designs below 2300°F if the temperature at the beginning of the reflood does not exceed 2200°F. If the flooding begins at a high rate of 5 to 10 in./sec for several seconds, then a flooding rate as low as 1 in./sec for the remainder of the transient may be sufficient to limit the peak cladding temperature to 2300°F.

The following recommendations are made:

1. Tests and analyses should be performed that model the downcomer as a gravity feed device. These will define more clearly the relation between flooding rate, steam back pressure and gravity leg. Reliable values should be established for the components of back pressure resistance, the pumps for example.
2. These tests should be performed at the flooding rates and system pressure appropriate to a reactor.

3. The effects of surface temperature should be determined with Zircaloy cladding at initial temperatures from 1600 to 2200°F and at flooding rates between 0.5 and 2 in./sec.

4. The capacity of the accumulators to supply coolant during the early stages of the reflood should be clearly established because of the strong reliance currently being placed on the cooling from the high injection rate during the first few seconds of reflood.

5. In consideration of the error analysis made by the FLECHT experimenters, it is recommended that the FLECHT heat transfer correlation be reduced by an appropriate amount. This reduction should allow for the effects of measurement errors on the experimental heat transfer coefficients and of the limitation of the correlation to describe the experimental results.

1. BUNDLE DESIGN

The test bundle was designed to model a section of a pressurized water reactor (PWR) core including the fuel rods, grid spacers, and control and instrument thimbles. Heater pins were used in place of fuel rods. The heater pins had the following dimensions:

1. Heater length - 12 ft
2. Heater diameter - 0.422 in.
3. Heater pitch - 0.563-in.
4. Control rod diameter - 0.545 in.
5. Instrument thimble diameter - 0.463 in.

The heater elements were Kanthal or Nichrome V resistance wire with a boron nitride dielectric. The heater pins were stainless steel clad in all but four of the tests in which they were Zircaloy clad. Stainless steel clad heaters have the advantage of being re-useable. Zircaloy clad heaters generally were useable only once if subjected to temperatures in excess of 1800°F.

The assembly arrays utilized in the tests were 7 x 7 and 10 x 10 elements. A PWR assembly is a 15 x 15 array. Plan views of the two heater bundles are shown in Figs. 1 and 2, including the location of

Fig. 1. 7×7 Test Section Simulating a PWR Assembly and Power Distribution (from Ref. 2).

Fig. 2. 10 × 10 Test Section Simulating a PWR Assembly and Power Distribution (from Ref. 2).

simulated control rods and instrument thimbles which were not heated. The number in each of the rods represents the ratio of the rod average power to the bundle average power for the test section. In addition to the radial power distribution so represented, the indicated heater pins had a stepped axial profile as shown in Fig. 3. The axial power profile was obtained by varying the resistance wire coil density within the heater. Thermocouples (chromel-alumel in a 0.040-in.-diam 304 stainless sheath) were built into the heaters for measuring wall temperatures during the transient. These thermocouples were located in the boron nitride dielectric with the thermocouple sheaths touching the tube wall.

The bundle assemblies of Figs. 1 and 2 were installed in a housing with a clearance of 0.2 in. between the outer edge of the heater pins and the wall. The housings that enclosed the assemblies were 4.200 ± 0.02 in. and 5.889 ± 0.020 in. square with an internal surface finish of 125 microinches.¹ The length was sufficient to supply a pressure containment for the entrance regions, the heated length and the exit regions.

The axial expansion of the heater pins was through ethylene-propylene rubber O-ring penetration seals. The heater pins were powered through three solid state control rectifiers. The power was controlled during the tests to simulate decay heating.

A more detailed description of the test section and instrumentation system is presented in Ref. 1.

2. BUNDLE OPERATING VARIABLES IN TEST

A flow sheet of the PWR-FLECHT facility is shown in Fig. 4. The facility contained the test section housing, a steam generator, and a gas pressurized water accumulator.

The following description of the test procedure used in the FLECHT experiments is from Ref. 1:

"The experimental facility is to be operated as a once-through system. The accumulator is filled with demineralized water and pressurized with gas to a pressure of about 500 psi above the pressure in the test section housing. The accumulator is maintained at a constant pressure during the experimental run. The required coolant temperature is established by means of the externally clamped-on heaters.

Fig. 3. Heat Rod Axial Power Profile

LEGEND:

TE - TEMP ELEMENT
 TIC - TEMP INDICATOR CONTROLLER
 DT - PRESS TRANSDUCER
 PI - PRESS INDICATOR
 LT - LEVEL TRANSMITTER
 LDR - LEVEL PRESS RELIEF
 DA - PRESS ALARM
 RTD - RESISTANCE TEMP ELEMENT
 FRC - FLOW RELAY OR CONTROLLER
 PRC - PRESS RELAY OR CONTROLLER
 VLDAS - VALVE DRIVEN - DUAL
 ACCUMULATORS
 FM - FLOW METER

Fig. 4. FLECHT Test Facility Flow Sheet (EDSK 341638D) from Ref. 2.

"A small amount of water is allowed to enter the test section housing to a level just below the heated length of the test rods.

"The walls of the test section are heated to a temperature in the range 600°F to 800°F and then pressurized with steam from the boiler to the required pressure.*

"Power is then applied to the test rods and the rods are allowed to heat up. It is anticipated that the rods with a nichrome heater element will be heated at constant power while the rods with the [Kanthal] heater elements will be heated at constant amperage. However, the rate of heating may be varied as desirable. When the temperature in a designated rod reaches a pre-set value, the solenoid valve V-2, is opened by means of a manually operated switch and the coolant is injected into the test section housing at the desired rate.

"Coolant drainage or trace heating or a combination of both will be utilized to assure that the coolant water in the test section housing and the pipe line between the test section housing and the accumulator is at the correct temperature at the time water injection is initiated.

"On initiation of coolant injection, decay of test rod power at a programmed rate corresponding to the reactor decay heat is automatically started. The programmed rate of decay is shown in Figure 12,** Reactor Power Decay Curve. The control valve V-5 regulates the test housing pressure by releasing steam to the atmosphere. The 'burped out' water, after partial separation, is collected in the catch vessel and measured.

"The injection of water can be terminated automatically or manually."

The original FLECHT program¹ was divided into three groups of tests according to the materials and the limiting operating temperature of the heaters as follows:

- Group I - Low peak heater rod temperatures using stainless steel clad heaters and Nichrome heating elements.
- Group II - High peak heater rod temperatures using stainless steel clad heaters and Kanthal or molybdenum[†] elements.

* Sometimes not done in actual experiments.

** Figure 7 in this report.

[†] The molybdenum heaters were not used.

Group III - High peak heater rod temperatures using Zircaloy clad
Kanthal heater elements.

The operating conditions of the test were selected to vary about a set of reference conditions that were based on conditions "predicted to occur prior to core recovery following a double-ended break on current generation four-loop pressurized water reactors."¹

The range of operating variables originally selected, the reference conditions, and the range actually run are listed in Tables 1 and 2.

In addition to the variables listed in Tables 1 and 2, a number of tests were specific to the effect of (1) dissolved boron salts upon heat removal rate, (2) coplanar blockages upon the heat removal rate during reflood, and (3) the housing temperature upon heat removal rate.

Most of the blockage tests were performed on the 10 x 10 array. Four blockage configurations were tested and the results of ten tests were included in the final FLECHT Report.² A few blockage tests were performed on the 7 x 7 array (Group I tests).

The test on the 7 x 7 array included blocking off uniformly the area of the 16 flow channels surrounding each and all of the central nine pins. In two tests the flow channel area was blocked off 50% and in four tests the area was blocked off 75%. The flow channels exterior to the central 16 were not blocked. The blockage was in the form of a flat plate, 3/8 in. thick with holes in it for the heaters and flow channels. It was located about 1 in. below the bundle midplane (see Fig. 4). Flooding rates of 6, 4, and 2 in./sec and initial temperatures of 1600 and 1800°F were tested.

Nine blockage tests utilizing four blockage configurations were performed in the 10 x 10 array of Group II. The Group II blockage plate initially blocked 75% of the total flow area in the 10 x 10 bundle with no bypass. The blockage of 16 central flow channel areas was later increased up to 100% with the rest of the bundle blocked 75 to 90%. This was the most severe blockage configuration investigated. The Group II blockage plate is shown in Fig. 5. Figure 6 is a schematic of the various blockages used in the Group II series. The severity of blockage was increased in going from blockage geometry 1 to blockage geometry 4.

Table 1. Range of Operating Variables for Stainless Steel FLECHT Tests

Variable	Reference Conditions	Original Selected Range ¹	Final As-Run Range of Variables ²
Initial clad temperature, °F	1600	800 to 2400	800 to 2200
Flooding rate, in./sec	6	1 to 8	0.6 to 19
Peak power density, kW/ft	1.24	0.69 to 1.4	0.69 to 1.4
Coolant pressure, psia	60	15 to 90	15 to 90
Inlet subcooling, °F	143	23 to 223	17 to 189
Bundle size		7 x 7 and 10 x 10	7 x 7 and 10 x 10
Number of tests run with stainless steel		53	83

Table 2. Tests with Zircaloy Cladding

Variable	Original Selected Range	Final As-Run Range of Variables
Initial temperature, °F	1600 to 2400	1970 to 2297
Flooding rate, in./sec	2 to 10	1.1 to 10
Peak power density, kW/ft	1.24	1.24
Coolant pressure, psia	60	58 to 64
Inlet subcooling, °F	143	138 to 141
Bundle size	7 x 7	7 x 7
Number of runs with Zircaloy	9	4

For this series, flooding rates were 4, 1, and 0.6 in./sec and initial temperature was held constant at 1600°F. Also, one test with blockage was performed using a variable flooding rate. Special thermocouples were installed at 6 ft 4 in. and 6 ft 8 in. for the Group II flow blockage tests. In both plate designs, blockage was increased by inserting special pins into the plate.

Additional scouting tests were performed to determine the effect of dissolved boron salts on heat transfer during reflood, the effect of the test bundle housing temperature on heat transfer, and the effect of "fallback" on heat transfer rates and quench front velocity.

Several tests were performed in which the flooding rate was varied to approximate the calculated reactor behavior more closely.

A complete listing of the tests, the independent variables, and the major results is presented in Tables 3, 4, and 5.

3. COMPARISON OF FLECHT EXPERIMENTAL VARIABLES WITH REACTOR OPERATING CONDITIONS

The FLECHT experiments measured the thermal response of an assembly of heater pins over a range of variables beyond the values anticipated to occur during the design basis LOCA. Accordingly, these variables are discussed now in the context of pressurized water reactors. The order in which these variables are discussed is not necessarily related to the relative importance.

ACTUAL SIZE

Fig. 5. Group II Flow Blockage Plate (Ref. 2).

BLOCKAGE GEOMETRY	BLOCKAGE OF FLOW AREA IN CHANNELS DESIGNATED:			
	a	b	c	ALL OTHERS
1	75%	75%	75%	75%
2	90%	90%	90%	75%
3	100%	90%	90%	75%
4	100%	100%	90%	75%

Fig. 6. Schematic of Group II Flow Blockage Geometries.
(Ref. 2).

Table 3. FLECHT Data Summary, Stainless Steel Clad Constant Flow Tests,
Hot Rod Midplane Elevation (6 ft), Ref. 2

Run No.	Pressure (psia)	Peak Power (kw/ft)	Decay Power	Flooding Rate (in./sec)	Inlet Subcooling (°F)	Initial Temperature (°F)	Temperature Rise (°F)	Turnaround Time (sec)	Quench Temperature (°F)	Quench Time (sec)	Bundle Size	Remarks
1	60	1.24	A	5.9	137	1326	137	25	730	124	7 x 7	High Housing Temp
2	59	1.24	A	5.8	136	1646	46	10	735	130	7 x 7	High Housing Temp
3A	56	1.24	A	5.8	141	1812	74	7	784	116	7 x 7	High Housing Temp
3B	56	1.24	A	6.0	142	1761	76	7	671	126	7 x 7	High Housing Temp
3C	57	1.24	A	5.9	132	1764	63	6	822	124	7 x 7	High Housing Temp
4	57	1.24	A	17.6	134	1589	9	2	907	44	7 x 7	High Housing Temp
0105	56	1.24	A	6.0	137	801	226	36	728	75	7 x 7	
0307	58	1.24	A	9.8	139	1210	59	5	827	50	7 x 7	
0408	59	1.24	A	10.0	140	1625	30	3	797	51	7 x 7	
0509	58	1.24	A	9.9	137	2017	20	3	797	64	7 x 7	
0610	15	1.24	A	5.8	138	1403	134	20	908	104	7 x 7	
0711	15	1.24	A	5.9	137	1600	82	15	932	104	7 x 7	
0812	15	1.24	A	5.9	136	1807	55	7	841	105	7 x 7	
0913	15	1.24	A	9.8	136	1601	42	6	1006	61	7 x 7	
1002	56	1.24	A	6.0	137	1605	70	6	808	76	7 x 7	
1116	73	1.24	A	6.0	142	1611	63	7	864	75	7 x 7	
1314	57	1.24	A	5.8	189	1604	81	8	991	65	7 x 7	
1417	90	1.24	A	5.8	150	1611	70	7	822	73	7 x 7	
1615	30	1.24	A	5.8	130	1594	105	9	834	105	7 x 7	
1720	61	1.24	A	5.9	25	1617	53	7	794	165	7 x 7	
1806	56	1.24	A	5.9	135	1606	94	9	919	52	7 x 7	Unheated Housing
1902	55	1.24	A	5.9	133	1612	53	6	765	132	7 x 7	Hot Housing
2322	15	1.24	A	4.0	148	1612	168	43	899	185	7 x 7	
2636	57	1.24	A	5.9	131	1600	60	5	1015	54	7 x 7	50% Flow Blockage
2737	58	1.24	A	5.9	137	1799	42	4	1015	56	7 x 7	50% Flow Blockage
2838	57	1.24	A	5.9	144	1600	79	7	950	47	7 x 7	75% Flow Blockage
2939	56	1.24	A	5.9	140	1800	61	5	1057	49	7 x 7	75% Flow Blockage
3100	57	1.24	A	2.1	144	1609	131	14	1048	89	7 x 7	75% Flow Blockage
3300	58	1.24	A	4.0	137	1632	81	7	1164	62	7 x 7	75% Flow Blockage

Table 3. (continued)

Run No.	Pressure (psia)	Peak Power (kw/ft)	Decay Power	Flooding Rate (in./sec)	Inlet Subcooling (°F)	Initial Temperature (°F)	Temperature Rise (°F)	Turnaround Time (sec)	Quench Temperature (°F)	Quench Time (sec)	Bundle Size	Remarks
3440	55	1.24	B	5.9	132	1204	138	13	780	65	10 x 10	
3541	57	1.24	B	5.9	141	1598	90	8	752	71	10 x 10	
3642	57	1.24	B	5.9	141	1805	62	5	804	87	10 x 10	
3724	57	1.24	B	1.9	141	1187	427	60	790	175	10 x 10	
3823	57	1.24	B	1.9	22	1200	394	57	779	233	10 x 10	
3920	55	1.24	B	5.8	16	1608	54	5	745	162	10 x 10	
4027	57	0.69	B	1.9	141	1592	103	28	712	130	10 x 10	
4129	60	1.40	B	1.9	134	1596	372	55	850	224	10 x 10	
4225	59	1.24	B	1.9	139	1596	247	35	799	192	10 x 10	
4321	58	1.24	B	3.9	140	1604	107	14	813	102	10 x 10	
4442	59	1.24	B	5.8	138	1813	69	5	805	74	10 x 10	
4526	57	0.69	B	5.9	139	1608	26	5	849	54	10 x 10	
4628	59	1.40	B	5.9	150	1606	85	6	856	71	10 x 10	
4718	55	1.24	B	5.9	86	1610	49	6	753	103	10 x 10	
5019	55	1.24	B	9.8	17	1605	39	4	779	118	10 x 10	
5123	55	1.24	B	1.9	24	1607	241	45	810	246	10 x 10	
5231	18	1.24	B	5.9	53	1603	96	11	543	170	10 x 10	
5332	28	1.24	B	5.9	87	1604	61	7	738	95	10 x 10	
5433	71	1.24	B	5.9	156	1614	72	7	807	61	10 x 10	
5534	84	1.24	B	5.9	162	1614	58	7	874	54	10 x 10	
5642	60	1.24	B	5.8	141	1811	67	6	767	75	10 x 10	
5730	60	1.24	B	6.0	145	1602	59	7	758	66	10 x 10	Borated Coolant

Table 3. (continued)

Run No.	Pressure (psia)	Peak Power (kw/ft)	Decay Power	Flooding Rate (in./sec)	Inlet Subcooling (°F)	Initial Temperature (°F)	Temperature Rise (°F)	Turnaround Time (sec)	Quench Temperature (°F)	Quench Time (sec)	Bundle Size	Remarks
5948	58	1.24	A	1.0	142	1512	170	66	928	254	7 x 7	Power Scram at 107 Sec.
6047	57	1.40	A	6.2	140	2006	51	4	853	76	7 x 7	
6155	57	1.24	A	5.9	143	2212	31	3	902	78	7 x 7	
6256	60	1.24	A	3.9	134	2199	39	4	838	102	7 x 7	
6351	59	1.24	A	1.0	136	1795	321	57	914	256	7 x 7	
6553	59	1.24	A	1.0	148	2012	278	62	926	257	7 x 7	
6658	60	1.24	A	2.0	140	2195	85	7	900	163	7 x 7	
6749	58	1.24	A	0.6	153	1505	—	—	—	—	7 x 7	
6848	59	1.24	C	1.0	144	1503	532	94	952	265	7 x 7	
6948	58	1.24	B	1.0	147	1502	465	95	944	266	7 x 7	
7057	58	1.24	A	0.8	145	1501	506	111	960	285	7 x 7	
7158	54	1.24	A	2.0	149	2156	88	9	805	156	7 x 7	
7845	55	1.24	B	4.3	142	1500	80	5	715	119	10 x 10	75% Flow Blockage
7946	58	1.24	B	1.0	143	1595	193	15	729	239	10 x 10	75% Flow Blockage
8054	56	1.24	B	3.9	135	1570	80	5	685	103	10 x 10	75-90% Flow Blockage
8162	57	1.24	B	1.1	142	1595	195	15	690	242	10 x 10	75-90% Flow Blockage
8265	57	1.24	B	3.9	137	1500	90	6	710	107	10 x 10	75-90-100(4)% Flow Blockage
8366	58	1.24	B	1.0	143	1580	245	17	695	246	10 x 10	75-90-100(4)% Flow Blockage
8567	60	1.24	B	3.9	133	1558	84	6	740	113	10 x 10	75-90-100(16)% Flow Blockage
8668	57	1.24	B	1.0	138	1500	220	17	700	253	10 x 10	75-90-100(16)% Flow Blockage
8764	57	1.24	B	0.6	146	1505	—	—	—	—	10 x 10	75-90-100(16)% Flow Blockage Power Scram at 97 Sec
9278	58	1.24	B	1.0	142	2028	258	57	908	260	7 x 7	Fallback

Table 4. FLECHT Data Summary Stainless Steel Clad Variable Flow Tests,
Hot Rod Midplane Elevation (6 ft), Ref. 2

Run No.	Pressure (psia)	Peak Power (kw/ft)	Decay Power	Flooding Rate (in./sec)	Inlet Subcooling (°F)	Initial Clad Temperature (°F)	$\Delta T_{rise 1}$ (°F)	$t_{turn 1}$ (sec)	T_{min} (°F)	t_{min} (sec)	$\Delta T_{rise 2}$ (°F)	$t_{turn 2}$ (sec)	T_{quench} (°F)	t_{quench} (Sec)	Bundle Size	Remarks
7310	57	1.24	B	8.7/6.3-1.0	1-5	1805	35	3	1687	23	29	39	1063	144	7 x 7	
7452	56	1.24	B	9.0/6.7-1.0	1-5	2200	25	3	----	---	---	---	----	---	7 x 7	Power Off At 121 sec
75-9	57	1.24	B	8.5/6.9-1.0	1-5	1805	35	3	1623	25	7	36	1050	127	7 x 7	Fallback
7610	57	1.24	B	8.8/5.7-1.0	1-0	2195	25	3	----	---	---	---	1025	148	7 x 7	Fallback
7711	56	1.40	B	8.6/6.4-1.0	1-2	2195	40	3	----	---	---	---	1025	167	7 x 7	Fallback
84-3	60	1.24	B	3.9-1.0	1-0	1600	88	6	1580	14	35	26	670	221	10 x 10	75-90-100 (4)X Flow Blockage
8915	60	1.24	B	5.8-1.0	1-8	2301	45	4	2181	13	69	36	930	249	7 x 7	Fallback
9017	55	1.24	B	6.2-1.0	1-2	2138	42	4	2151	10	100	34	815	276	7 x 7	Fallback
9116	58	1.24	B	5.9-1.0	1-0	2197	34	4	2202	9	79	41	827	239	7 x 7	Fallback Low Housing Temp
9319	61	1.24	B	6.0-1.0	1-8	2146	45	4	2145	8	135	47	964	252	7 x 7	

Table 5. FLECHT Data Summary, Zircaloy Clad Tests,
Hot Rod Midplane Elevation (6 ft), Ref. 2

Run No.	Pressure (psia)	Peak Power (kw/ft)	Decay Power	Flooding Rate (in./sec)	Inlet Subcooling (°F)	Initial Clad Temperature (°F)	$\Delta T_{rise 1}$ (°F)	$t_{turn 1}$ (sec)	T_{min} (°F)	t_{min} (sec)	$\Delta T_{rise 2}$ (°F)	$t_{turn 2}$ (sec)	T_{quench} (°F)	t_{quench} (sec)	Bundle Size	Remarks
2443	56	1.24	A	10.0	138	2035	67	6	----	----	----	----	----	----	7 x 7	Degraded Quench of Hot T/C
2544	58	1.24	A	4.0	140	2015	127	12	----	----	----	----	----	----	7 x 7	Degraded Quench of Hot T/C
8874	64	1.24	B	6.0-1.0	141	2297	63	8	2060	12	87	40	971	204	7 x 7	Fallback
9573	61	1.24	B	1.1	140	1970	350 at 18 seconds				----	----	----	----	7 x 7	

3.1 Power and Power Distribution

The power level to the PWR-FLECHT assembly after flooding was initiated is shown in Fig. 7. Five curves are plotted as percent of operating power density of 19 kW/ft vs time after beginning of reflood. The five curves represent the following:

1. Fission product decay heat according to ANS +20%.
2. Curve B of the Westinghouse FLECHT tests procedure displaced 20 sec to account for the time between reactor power scram and start of reflood.
3. Curve B with no time offset, initial power 1.24 kW/ft.
4. Curve A - A different test power-time curve used in PWR-FLECHT.
5. ANS recommended curve for fission product decay.

Curve B, the power trace used in most of the tests, is substantially below the ANS +20% curve for fission product decay heat. However, the ANS Standard curves are plotted for time after shutdown. To be on a comparable basis, curves A and B should be displaced by about 20 sec to account for the time between shutdown and the beginning of reflood. As shown on Fig. 7, this shift makes the curve B heat rates about the same as those of the ANS +20% curve for the reflood period.

In the FLECHT series of tests, 43 were conducted utilizing curve B as a heat source and 35 used curve A.

None of these power curves attempted to reproduce Zircaloy-water reaction heat when stainless steel heaters were utilized.

The power density distribution of the heaters used in FLECHT determines the distance the cooling water enters the assembly before boiling and droplet entrainment occurs. Thus, the turnaround time (this is the time required to arrest the temperature rise of the cladding after reflood starts) may be a function of power distribution. The power density of the FLECHT heaters (see Fig. 3) had a peak to average ratio of 1.6 over the center 15 in. and a power density ratio of 0.45 for the 21 in. at each end. Current reactors³ have flatter profiles with peak to average axial power densities between 1.4 and 1.5. In addition, a comparison of FLECHT results from PWR and BWR flooding tests shows that the steeper profile used in the PWR tests resulted in a higher heat transfer rate and a higher

Fig. 7. Comparison of Decay Heat Curves - ANS Standard With PWR FLECHT

water accumulation rate.⁴ These factors tend to make the PWR-FLECHT heat transfer results unconservative. However, this unconservatism is likely to be significant only if the flooding rates are marginal.

The housing of the FLECHT assembly may absorb or release energy to the fluid and heaters. Tests performed when the housing was heated showed lower peak temperatures and longer quench times than tests when the housing was unheated. Heat transfer results obtained on the central rods were largely unaffected until quenching began when compared on a run-to-run basis. Although the housing temperature effect certainly is present, it is likely not significant except at marginal flooding rates and may be included in an overall allowance for experimental error.

3.2 Initial Peak Temperature and Temperature Distribution

The temperature distribution of the cladding surface is closely related to the power distribution in the heaters and it has an important effect on the heat transfer, on the amount of blockage that might occur, and on the distribution of this blockage through the assembly. The comments relating to power distribution are made in the preceding section. The comments relating to blockage are made in documents prepared by R. L. Rittenhouse. However, certain comments on the relationship between the surface temperature and blockage distribution are in order. The temperature data near the midplane of the peak temperature rods are reported at 2-ft intervals for certain tests. These measurements imply certain gradients of temperature that may be compared with the test condition observed in the ORNL multirod swelling tests. These tests were performed with temperature gradients of 80°F/ft or lower. In run 7670, (see Table 3, FLECHT Data), the peak initial temperature is 2195°F with an average gradient of 175°F/ft over the 2 ft below the centerplane and 275°F/ft above the centerplane. The peak temperature in run 7569 is 1800°F. The average gradient over 2 ft on either side of the centerplane maximum is 150°F/ft below the centerplane and 170°F/ft above the centerplane for this run. For run No. 8567, the peak initial temperature is 1554°F and the temperature gradient in the 2 ft above the centerplane is about 80°F/ft. These gradients are at least as high as in the ORNL tests.

When the power distribution of reactor fuel pins is like that in the FLECHT rods, it appears that temperature gradients of $100^{\circ}\text{F}/\text{ft}$ or higher might exist near the centerplane at temperatures above 1600°F and that the swelling and burst locations in rods that rupture at higher temperatures will tend to be mostly coplanar. This observation might be extended to conclude that the temperature gradients would be less at lower temperatures and with less peaked power distributions and that swelling and rupture locations would tend to be more distributed over a greater length in groups of rods that rupture below 1600°F .

FLECHT experiments were performed with peak initial cladding temperature varied independently from 800 to 2200°F . The temperature of the cladding at the end of blowdown and beginning of reflood in a LOCA are not known with certainty. The peak cladding temperature may be from ~ 1200 to $\sim 2000^{\circ}\text{F}$ depending upon the assumptions made concerning blowdown heat removal heatup during refill and the time required for refill of the bottom plenum. This uncertainty is a major item in the evaluation of the LOCA since the peak temperatures are closely related to flooding rate and initial temperature.

The effect of initial clad temperature with stainless steel heaters is shown parametrically on Fig. 8 where peak temperature is plotted vs flooding rate. Also shown are three sets of data obtained with Zircaloy clad assemblies.

For the stainless steel bundles where the flooding rate was above 2 in./sec, the cladding temperature increased only slightly after coolant injection was initiated. Accordingly there was only a slight increase in the cladding temperature regardless of the initial temperature from values of 1600°F up to 2200°F . This was a result of the heat transfer coefficient being sufficiently high and the steam-liquid flow above the quench front being sufficiently large to maintain a reasonable temperature difference between clad and coolant at the prevailing values of surface heat flux. As the coolant injection rate was lowered below 2 in./sec, the flow of coolant and the heat transfer coefficient decreased sufficiently to require an increase in surface temperature well above 1600°F to remove the decay heat flux of about 25,000 Btu/hr ft². Thus,

Fig. 8. Effect of Flooding Rate and Initial Cladding Temperature on Peak Cladding Temperature (Ref. 2)

as the constant flooding rate decreased below 2 in./sec the effect of a lower initial temperature on the peak cladding temperature became less important. Finally, when the flooding rate was about 0.6 in./sec, the test with stainless steel cladding was terminated before turnaround by scrambling the power. The peak cladding temperature went well above 2300°F even though the initial cladding temperature was only 1600°F.

Three constant flooding rate tests were performed with Zircaloy clad heaters. These tests were performed with an initial peak temperature of 2000°F. The results of the tests are also plotted on Fig. 8. The test results indicated greater temperature increases than were observed in the tests with stainless steel clad heaters under similar conditions, probably because of the additional heat associated with the Zircaloy metal-water reaction (see Section 3.5). At a flooding rate of 1.1 in./sec and an initial temperature of 2000°F, the power was turned off when the temperature was approaching 2400°F and still increasing. Electric arcing occurred in this test. The arcing is a sign of faulty heater pin construction. Therefore, the results of the tests with Zircaloy cladding are sufficiently obscured by the arcing that it appears mandatory for additional tests to be run.

The initial cladding and fuel temperature at the start of reflood does have some effect on the peak temperature achieved when the flood rate is below 1 in./sec. The heat transfer coefficient, h , at these low flood rates are about 12 Btu/hr ft² °F or less before quenching is achieved because of the high steam quality above the liquid-vapor interface. At these low values of h the temperature rise to remove the decay heat decreases from about 3000°F to about 1000°F as the fission product decay heat flux decreased from about 36,000 to about 12,000 Btu/hr ft². Clearly the lower the initial fuel temperature the longer the time required to achieve some given temperature. There is a large uncertainty associated with calculating the fuel pin temperature at the start of reflood because of the question associated with the extent of flow maldistribution during blowdown, fuel clad swelling during blowdown and heat transfer coefficients during blowdown. Accordingly, until more certain methods are achieved for calculating the cladding temperature at the beginning of reflood, limited power density and high reflood

rates should be relied upon to limit the peak cladding temperature during the LOCA transient.

3.3 Flooding Rate

The flooding rate in the FLECHT tests was varied from 0.6 to 19 in. of coolant injected per sec. The effect of constant flooding rate on the peak clad temperature with initial peak cladding temperature as a parameter is shown in Fig. 8. It is evident that at flooding rates above 2 in./sec there is only a slight increase in the peak cladding temperature as the flooding rate decreases. For flooding rates below 2 in./sec the increment above the initial temperature increases rapidly with diminishing flooding rate. At 1 in./sec stainless steel clad heaters with an initial surface temperature of 2000°F reached a value near 2300°F. At a flooding rate of 0.6 in./sec the power to a stainless steel bundle had to be turned off to prevent a temperature runaway.

In the current generation reactors, the flooding rate is calculated to be between 5 and 10 in./sec for a period of 5 to 10 sec and then to diminish to values near 1 in./sec.^{5,6} Therefore, tests were performed with a variable flooding rate that attempted to reproduce the calculated values and measure the cladding temperatures that resulted. It is clear that the high initial flooding rate was beneficial. In the tests of a flooding rate sequence of 6 in./sec for six sec followed by 1 in./sec there was not thermal runaway with either Zircaloy or stainless steel clad heaters when the initial cladding temperature was 2200°F. The validity of the 6 in./sec initial flooding rate for a reactor is uncertain since the amount of water required to refill the pressure vessel bottom may take the total accumulator inventory and the pumps cannot supply water at this high rate.

The flooding rate in the FLECHT tests was achieved by forcing water into the test section with a pump. The test section was pressurized and vented. The FLECHT test assembly did not model in any way the gravity feed leg represented by the downcomer or core barrel annulus in a PWR pressure vessel. The potential for gross flow oscillations and the effect of these oscillations upon the cooling process was not determined or estimated. Accordingly, the high initial flooding rates need

considerable experimental and analytical confirmation. The achievable flooding rate in a reactor on a steady basis is limited by the driving head available in the downcomer and by the resistance to steam flow in the exit path. Any excess of water may spill out or be carried out as entrainment through the break on the core inlet pipe.

3.4 Effect of System Pressure

The pressure in the test section was held constant during each run and the run-to-run pressure was varied from 15 to 90 psia. The reference pressure was 60 psia. The pressure in the pressurized water reactor containment may be in the range of 40 to 55 psia.

The pressure variation data obtained at a 6 in./sec flooding rate indicated that the heat transfer coefficient increased as the pressure increased. This result is a little odd since the amount of liquid entrained in gas (at constant mass velocity) tends to increase with a decrease in vapor density. An alternate explanation is that the higher pressure led to more rapid filling of the lower region of the heater bundle and a more rapid quench. The effect of pressure was determined only for a flooding rate of 6 in./sec and should be repeated at lower flooding rates to resolve this question of mechanism. This is important because the anticipated reactor flooding rate and containment pressure are lower than were used in the FLECHT tests.

3.5 Effect of Zircaloy vs Stainless Steel Cladding

The primary difference between operating with Zircaloy clad and stainless steel clad heaters is associated with the chemical reaction between Zircaloy and water. The heat of formation of ZrO_2 from water vapor and zirconium is -144 Kcal/g-mole. The reaction rate and equivalent heat flux may be calculated from the Baker-Just relation,⁸

$$\frac{w^2}{t} = 33.3 \times 10^6 \exp (-45,500/RT)$$

when

$w = \text{mg Zr reacted/cm}^2$

$T = ^\circ\text{K}$

$t = \text{seconds}$

$R = \text{gas constant } \frac{\text{cal}}{\text{g-mole-}^\circ\text{K}}$

The heat flux may be related to the amount of reaction for an isothermal reactor through the relation,

$$\frac{Q}{A} = \frac{3.49 \times 10^{11} \exp(-22,897/T)}{w},$$

where Q/A is the heat flux in Btu/hr ft² and the other symbols are those noted above.

The above equation assumes that all the oxygen sorbed by the metal reacts to form ZrO₂ and that the reaction is not steam limited. Figure 9 shows a plot of heat flux vs temperature with the oxide thickness as a parameter. The range of heat flux due to decay heat is shown for the hot spot and for the hot rod average and the heat flux values are also related to the percent of the peak operating heat flux. At 2300°F the heat flux for a 1 mil oxide thickness (~3 1/2% of a PWR fuel cladding wall) on Zircaloy is about 11,000 Btu/hr ft². This would increase the decay heat flux by a factor of 1.25 to 2 during the reflood. This effect may be observed by comparing the cladding surface temperature increases with the Zircaloy rods to those with stainless clad rods.

The reciprocal of the temperature increase from the peak temperature at the beginning of reflood to the maximum temperature during reflood is plotted vs the flooding rate in Fig. 10. The initial temperature is plotted as a parameter. The stainless steel data selected for this plot were from runs that had the same initial power, subcooling, and pressure, had no blockage, and had a constant flooding rate. The three constant flooding rate tests with Zircaloy are also plotted. The flooding rate of 0.6 in./sec for stainless steel and 1.1 in./sec for Zircaloy were selected as intercepts on the constant flooding rate axis. Comparing the results at 2000°F initial temperature for Zircaloy and stainless steel shows that at a flooding rate of 4 in./sec the Zircaloy surface increased about 135°F compared to about 50°F for stainless steel. At 1 in./sec the stainless steel cladding reached 2300°F, the interim criteria limit, and the Zircaloy test had to be terminated with the temperature still increasing (above 2300°F) when electric arcing was observed.

These tests suggest that with the existing uncertainties in the initial cladding temperature and in the initial flooding rates, it is

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Fig. 9. Heat Flux from Metal-Water Reaction Based on Baker-Just Reaction Rate with Equivalent ZrO₂ Thickness as Parameter

Fig. 10. Effect of Flooding Rate and Initial Peak Cladding Temperature on Temperature Rise of Cladding. (Ref. 2)

questionable whether the peak cladding temperature can be limited to 2300°F at constant flooding rates of 1 in./sec and currently contemplated power densities.

Tests which model the vessel downcomer and the reactor plant back-pressure will likely display flooding conditions that are highly oscillatory and that will have different cooling characteristics than were observed in the constant flooding rate test.

I believe considerable testing and analysis should be done to assure the understanding of cooling by core flooding through other than empirical methods. This has been attempted with only moderate success by T. S. Kress at ORNL and by Rowe and Sutey of BNWL.¹⁰

3.6 Error Analysis

An error analysis is reported in the FWR-FLECHT test plan¹ and the estimate is that a 12.4% uncertainty exists in the experimental results due to distributed random errors and undistributed errors that might be a bias. No allowance has been made in the data for this uncertainty. The marginality of the behavior of the core and injection system should the ECC system be required, suggest that consideration should be given to reducing the heat transfer coefficients calculated by use of the FLECHT correlations. A reduction would have a significant effect at a low flooding rate and a negligible effect at a high flooding rate.

Allowance should also be made for any bias between the calculated and experimental results that exists at the flooding rates up to 1.5 in./sec. The emphasis should be placed at the lower flood rates because (1) these are the flooding rates anticipated in a double-ended clad by break and (2) the heat transfer coefficients measured and calculated indicate that the margin for error is small if the clad temperatures are to remain below 2300°F and if clad melting is to be precluded.

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II. EFFECT OF UNIFORM COPLANAR BLOCKAGE ON REFLOOD RATE

J. J. Keyes, Jr.

SUMMARY

Calculations were made to investigate the effect of core blockage in reducing the reflood rate in typical four-loop and two-loop, four-pump PWR plants. The calculations indicated that exit flooding rates of 1.0 and 1.3 in./sec could be expected with an unblocked core in a four-loop and a two-loop, four-pump plant, respectively, with the containment pressure at 50 psia. Reducing the containment pressure to 25 psia would reduce the exit flooding rate to 0.75 to 0.9 in./sec with an unblocked core. Core-wide midplane blockage greater than 90% would be required to reduce the exit flooding rate by 25%, and blockage of 70% would have a negligible effect with a containment pressure of 50 psia. The pressure drop and reflood rates that were calculated agree acceptably with the more elaborate calculations of Combustion Engineering and Westinghouse.

1. PURPOSE

This analysis was performed to provide an estimate of the magnitude of uniform (core-wide) coplanar blockage that would be required in order to have a substantial effect on the reflood rate during a design basis LOCA in a typical PWR. The LOCA is assumed to result from a double-ended cold-leg break in a four-loop PWR and in a two-loop, four-pump PWR. The calculations were made for the quasi-steady-state periods during the reflood when the pressure drop in the external loops due to the large steam flow limits the reflood rate. While the analysis is indeed pedestrian in comparison with the computer codes that have been developed for reflood calculations, it was expected to and did give results in reasonable agreement with the more elaborate calculations and provides insight into the factors that have an important bearing on the pressure drop and the reflood rate.

2. ASSUMPTIONS AND BASES FOR THE CALCULATIONS

The major assumption, steady-state flow and heat transfer, was made to approximate a period of reflood during which relatively slow changes in flow rates were occurring. This period corresponds to a situation well into the reflood phase when a steam-water mixture is flowing from the top of the core at a quasi-steady rate determined by decay heat evolution, all of which is assumed to be absorbed by the coolant as sensible plus latent heat. No superheating is considered in the core. One-dimensional energy and momentum equations were used in the pressure drop calculations.

Significant assumptions of a more specific nature include:

1. Total heat source is constant at 5% of the thermal power. This corresponds to the total residual decay heat at about 60 sec after shutdown as it was estimated for the Indian Point Station, Unit No. 2.
2. The loss coefficient is 10 (based on outlet pipe velocity) for all pumps. From Ref. 1 this corresponds to a pump speed of 40 to 50% of full speed.
3. Two axial nodes are considered in both the core and steam generator.
4. The equivalent static head of saturated water in the core is assumed to be 2 ft, and in the downcomer 17 ft (full to break), giving a constant pressure difference of 6.2 psi to drive reflood water into the core.
5. Core bypass flow is neglected.
6. Blockage is of the plate (orifice) type.
7. Compressibility effects are neglected. (Maximum Mach number at 50 psia containment pressure and inlet flooding rate of 2.5 in./sec is 0.45 and occurs between core and break.)
8. The safety injection water does not interact with the steam in the loop piping or downcomer.
9. Water enters the bottom of the core at 200°F.
10. Containment pressure is constant at 50 psia or 25 psia.
11. Steam density is based on containment pressure.
12. Mass flow rate of water (liquid plus vapor) is invariant through the core; that is, all water leaving the effective liquid level is assumed to exit through the core at the top.

3. DESCRIPTION OF THE CALCULATIONAL PROCEDURES

Table 1 summarizes the plant characteristics important in this analysis.

Table 1. Plant Characteristics^a

	Two Loop	Four Loop
Thermal output, MW(t)	3390	3411
Core		
Rod diameter, in.	0.44	0.42
Rod pitch, in.	0.58	0.56
Rod length, ft	12	12
Flow area, ft ²	53.2	51.4
Number of grids	9	7
Heat-transfer area, ft ²	55,000	52,200
Steam generator		
Tube diameter, in.	0.75	0.875
Tube wall, in.	0.048	0.050
Tube length, ft	55	75
Number of tubes per generator	10,370	3260
Steam temperature, °F	530	525
Piping		
Hot leg, ID, in.	42	29
Cold leg, ID, in.	30	27.5
Steam generator to pump, ID, in.	30	31

^aBased on Combustion Engineering two-loop, four-pump plant and Westinghouse Electric four-loop, four-pump plant.

3.1 Core (four- and two-loop plants)

The core pressure drop was estimated by assuming that the mixture of steam and water, the quality of which was determined by a heat balance, flows through each of the two 6-ft-long axial nodes at the constant quality representative of the node exit. The Lockhart-Martinelli method was used to estimate frictional pressure loss, and acceleration and grid spacer losses were added to the frictional loss. A spacer loss coefficient (K factor) of 1.0 was used based on the velocity of a homogeneous mixture

through an area that was 75% of the full channel area. An additional multiplier of 1.3 accounted for deviations of experimentally observed pressure drops in rod bundles from values calculated using the equivalent diameter concept.² The heat balance over the lower node included the heat to raise the subcooled inlet water (200°F) to saturation at the assumed containment pressure (282°F at 50 psia and 240°F at 25 psia). All additional decay heat was absorbed by vaporization at the saturation temperature. No superheating of steam in the core was considered.

It should be emphasized that assumption 12 (see Sect. 2) together with the saturation assumption implies that part of the liquid water entrained by steam is vaporized above the entrainment level to satisfy the heat balance. The difference between the total mass flow rate, W_E , corresponding to the chosen value of "exit flooding rate"* and the mass flow rate of steam, W_S , is the mass flow rate of the liquid, W_L . The quality is

$$x = \frac{W_S}{W_S + W_L} = \frac{W_S}{W_E}$$

3.2 External Loop

The total pressure drop, core inlet to break, is the sum of the pressure drops in the reactor core, plenums, piping, steam generator, and pumps. Because of possibilities for different flow distributions between the intact loops for the four- and two-loop plants, the calculational procedures are distinct in some respects as indicated below.

3.2.1 Four-Loop Plant

Figure 1 depicts the steam flow paths assumed in calculating pressure drops.³ Note that the three intact loops, assumed to have identical flows and Δp 's, are represented as a single loop at the left. These three loops discharge separately into the annular region at the top of the downcomer formed by the reactor vessel wall, the core barrel, the downcomer top cover plate, and the free surface of water in the downcomer. However,

* See discussion on p. 40 with regard to "exit flooding rate."

the discharges from the three loops combine in the annulus and are exhausted through a single cold leg to the break. The effect of the combined flow shows up in the pressure loss into and through the cold leg to the break.

The procedure followed was to estimate overall pressure drop between the effective water level in the core and the containment sink as a function of total flow of water (liquid plus vapor) leaving the top of the core via the hot leg. The parameter, n^* , is referred to here as the "exit flooding rate" with units of in./sec of liquid water at core inlet conditions, and is related to the customary flooding rate entering the core, n , by the carryover rate fraction, F (see assumption 12),

$$n = \frac{n^*}{F}$$

F is available as a function of flooding rate and time from PWR-FLECHT data. Values of F in the range from 0.75 to 0.85 for times >100 sec after flooding and for values of n from 1 to 2 in./sec appear to be appropriate.⁴ Thus the exit flooding rates estimated can be converted approximately to the bottom flooding rate by multiplication by 1.25. The total exit mass flow rate is

$$W_E = \frac{n^* A_c \rho_i}{12}, \quad \text{lb}_m/\text{sec},$$

where A_c is the core area and ρ_i the inlet liquid density.

With n^* as parameter, the distribution of flow between the intact and broken loops was determined by assuming equal flows in the three intact loops and equating pressure drops for the intact and broken loops between the core and break. A trial-and-error solution for the fraction of the total flow in each loop led to the result that 27% of the total core exit mass flow passes through the broken loop, leaving $(73/3)\% = 24.3\%$ through each intact loop.

Pressure drops corresponding to each significant loop component (as numbered in Fig. 1) were determined as follows:

A. Intact Loops

1. Entrance to hot leg

Contraction loss for two-phase mixture of quality x was calculated using the homogeneous flow model of Geiger described by Tong.⁵

2. Hot leg

The Lockhart-Martinelli two-phase frictional loss model was employed. Heat input from the pipe walls was neglected.

3. Steam generator - flow inside tubes

Because of the large reservoir of high-temperature (530°F) steam on the shell side of the steam generator, all liquid water entering was vaporized and the steam was superheated. An order-of-magnitude calculation indicated that an overall heat-transfer coefficient of 10 Btu/hr·ft²·°F or more would be sufficient to produce saturated steam leaving the first half-length of the generator tubes for the largest value, $n^* = 2$, considered. A coefficient of this magnitude would be expected. Thus, the analysis was based on two equal length axial nodes. In the first node the steam was taken as being saturated by the nodal exit, and in the second it was superheated to 500°F at the exit. The pressure drop in the first node was based on Lockhart-Martinelli-Nelson analysis assuming a linear variation of quality with length; and in the second node it was based on single-phase steam superheated to 500°F at containment pressure. This exit steam condition was assumed to persist to the break.

4. Contraction into cold leg pipe and pipe friction

The pressure drop was calculated for superheated steam at containment pressure and 500°F.

5. Pump

Pressure drop was based on a loss coefficient, $K = 10$ velocity heads, using the cold leg steam velocity as the basis.

6. Pipe friction drop

The same as No. 4.

7. Flow division into downcomer annulus

A loss coefficient $K = 1.0$ was assumed based on the inlet velocity.

8. Downcomer annulus

Considered as a length of straight pipe (~ 25 ft/180 deg turn).

9. Contraction into cold leg leading to break

$K = 0.25$ was assumed based on the annulus velocity for discharge of the three parallel intact loops.

10. Pipe loss and infinite expansion to containment

Five feet of pipe, core to break, was assumed. No pressure difference occurs across an infinite expansion for the flat velocity profile assumed.

B. Broken Loop

The same calculational procedure was employed as described for the intact loops except that pressure drops corresponding to 7, 8, and 9 do not exist in the broken loop. Thus slightly more steam flow vents through the broken than through the intact loops - 27 and 24.3%, respectively.

The calculated distribution of pressure drop around the loops is shown in Table 2, where the entries are percentages of overall pressure drop.

Table 2. Distribution of Pressure Drop in a
Four-Loop Plant

Percent of total pressure drop
(Ref. Nos. shown in Fig. 1)

Loop	Component								Total
	1	2 + 4 + 6	3	5	7	8	9	10	
Intact	6	5	17	49	4	3	12	4	100
Broken	8	9	21	61	0	1	0	0	100

The relatively small difference between the fraction of flow venting through the intact and broken loops is attributed to the large fraction of the total drop represented by the steam generator (3) and pumps (5). Thus, the contributions of (7), (8), (9), and (10) affect the distribution only slightly.

C. Two-Loop, Four-Pump Plant^{6,7}

The principal difference in the calculational procedure for the two-loop plant as compared with the four-loop plant is the need for consideration of unequal division of flow among the three intact pump cold legs.

Referring to Fig. 2, it was assumed that the flows were equal in the two pump legs on the left, since no unbalancing mechanism exists. However, the vent flow distributes unevenly between the broken and intact pump legs on the right, as well as between the two hot legs leaving the reactor vessel. The two unknown flow distribution fractions, (left hot leg/right hot leg) and (broken pump leg/intact pump leg), were determined by trial and error from pressure drop balances around the competing circuits. The assumptions, models, and methods of calculation of pressure drops for each significant component were the same as those described for the four-loop plant. It was found that the flow split into the hot legs is such that about 52% of the total exit mass flow vents through the loop with the break. Of this, about 54% vents through the broken pump leg. As was the case for the four-loop plant, the large pressure drops represented by the pumps and steam generator relative to the total pressure drop minimize the effect of the break in unbalancing the flows.

3.3 Uniform Blockage

To estimate the uniform, coplanar blockage fraction, f , required to decrease reflood rate substantially, a plate-type (orifice) blockage was assumed to be uniformly distributed across the core at either the midplane or the core-outlet elevation. The pressure drop associated with the assumed blockage was calculated by the correlation of Murdock as presented by Tong.⁸

$$\sqrt{\Delta p_{\text{TPF}}/\Delta p_{\text{GPF}}} = 1.26 \sqrt{\Delta p_{\text{LPE}}/\Delta p_{\text{GPF}}} + 1 \quad ,$$

which correlates the pressure drop, Δp_{TPF} , for two-phase flow through orifices over a wide range of pressure, temperature, quality, and Reynolds number.

Fig. 2. Cold Leg Break Steam Flow Path
Two-Loop, Four-Pump Plant.

Δp_{GPF} and Δp_{LPF} , the pressure drops for single-phase flow of gas and liquid, respectively, were obtained from the incompressible orifice relation

$$\Delta p = W^2 / 2C_D^2 A_O^2 \rho g_c ,$$

where the orifice area, A_O , is related to the core free-flow area, A_c , by

$$A_O = (1 - f) A_c .$$

The procedure employed in determining the minimum blockage fraction, f , was to choose a value of n^* considered to be marginal and from the estimated pressure drop vs n^* presented in the next section, determine the additional pressure drop permitted under assumption 4 so that a total of 6.2 psi was available to drive reflood water through the core. This additional pressure drop was absorbed by the blockage as Δp_{TPF} . The corresponding f was then calculated.

4. RESULTS

Pressure drops for the unblocked core are summarized in Table 3.

Table 3. Unblocked Core Pressure Drop

Containment Pressure (psia)	Exit Flooding Rate, n^* (in./sec)	Core Pressure Drop (psi)	
		Four-Loop Plant	Two-Loop Plant
50	0.65	0.33	—
	0.75	0.37	0.35
	1.0	0.47	0.45
	1.5	0.68	0.65
	2.0	0.89	—
25	0.65	0.61	—
	0.75	0.69	0.65
	1.0	0.90	0.84
	1.5	1.29	1.21
	2.0	2.65	—

Table 4 summarizes the pressure drop around the external loops between the core outlet and the containment.

Table 4. Loop Pressure Drop

Containment Pressure (psia)	Exit Flooding Rate, n^* (in./sec)	Loop Pressure Drop (psi)	
		Four-Loop Plant	Two-Loop Plant
50	0.65	2.1	1.6
	0.75	2.8	—
	1.0	5.1	3.5
	1.5	10.5	7.4
	2.0	18.2	13.1
25	0.65	3.9	3.0
	1.0	9.5	6.5
	1.5	17.8	13.8
	2.0	34.0	24.5

The results in Tables 3 and 4 are combined and plotted in Figs. 3 and 4 as total pressure drop. Note, that, whereas the core pressure drop for the four-loop plant is only about 5% greater than for the two-loop plant, the external loop pressure drop is about 40% greater for the four-loop plant. This difference is attributed primarily to the smaller area of the cold legs (pump discharge) and the smaller flow area and longer tubes in the steam generator for the four-loop plant.

Table 5 summarizes the estimates of the uniform coplanar blockage for two values of exit flooding rate and two blockage elevations.

Table 5. Uniform Coplanar Blockage Fractions Corresponding to Two Exit Flooding Rates

Containment Pressure (psia)	Exit Flooding Rate, n^* (in./sec)	Midplane Blockage fraction, f		Core Exit Blockage Fraction, f	
		Four Loop	Two Loop	Four Loop	Two Loop
50	0.75	0.95	0.96	0.89	0.93
	1.0	0.90	0.95	0.82	0.89
25	0.75	0	0.92	0	0.85
	1.0 ^a				

^a1.0 in./sec is above the unblocked flooding rate.

Fig. 4. Total Pressure Drop - Unblocked Core Plus External Loop, Double-Ended Cold Leg Break and 25 psia Containment Pressure.

Note that, at 25 psia, the exit flooding rate is down to 0.75 without blockage for the four-loop plant, and neither unblocked plant can be expected to reflood at an exit rate of 1.0 in./sec. The exit flooding rate for the unblocked four-loop plant is estimated to be 0.9 in./sec at 25 psia.

Figures 5 and 6 illustrate graphically the influence of uniform core-wide blockage on flooding rate, as discussed in Section 5.

5. DISCUSSION AND CONCLUSIONS

From the results presented in Tables 3 and 4, it is clear that the dominant pressure drop occurs between the core outlet and the break, the unblocked core drop being in the range from 6 to 11% of the total pressure drop. Thus, relatively large uniform blockage fractions are required to significantly degrade the flooding rate. For example, from Table 5 at 50 psia, 95 and 96% midplane blockage for four- and two-loop plants, respectively, is necessary to decrease the exit flooding rate to 0.75 in./sec (from 1.05 in./sec and 1.30 in./sec for the unblocked cores as read from Figs. 3 and 4). Lower blockages are required at the core exit and for the higher flooding rate, 1.0 in./sec. However, uniform core-exit blockage is considered improbable. At 25 psia the exit flooding rate is 0.75 in./sec for the unblocked core in the case of the four-loop plant.

The insensitivity of the exit flooding rate to uniform core-wide blockage fractions less than ~0.80 is illustrated in Fig. 6 derived from Fig. 5 for two assumed overall driving pressures, 6.2 psi and 3.1 psi, for a four-loop plant and 50 psia containment pressure. At the higher driving pressure (corresponding to a full downcomer and an equivalent static head of 2 ft in the core), a blockage fraction of 0.95 is required under the assumed conditions to decrease the flooding rate by 25%. At the lower driving pressure, the blockage fraction required for 25% decrease in flooding rate is 0.93.

It needs to be pointed out that, if superheated steam is generated below the blockage plane as observed in some PWR-FLECHT experiments (contradicting the assumption made here of saturation), the calculation lead to lower values of allowable blockage. For example, with steam superheated

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Fig. 5. Total Pressure (Core plus Loop) vs Exit Flooding Rate with Fractional Blockage as Parameter, Double-Ended Cold Leg Break and 50 psia Containment Pressure. Four-Loop Plant.

Fig. 6. Effect of Uniform Core-Wide Midplane Blockage on Flooding Rate, Double-Ended Cold Leg Break and 50 psia Containment Pressure. Four-Loop Plant.

to 1000°F at 50 psia, allowable blockage may be reduced from 95 to 92%, or from 82 to 73% depending on the plant type and blockage elevation.. Thus, the question of superheating in the core become important when blockage is considered, and the assumption of saturation is nonconservative in this respect. Westinghouse Electric chose a worst-case design-basis value of 65% for the average core-wide blockage.⁹ Combustion Engineering estimated an average core blockage of 35%.¹⁰ From this analysis it is concluded that uniform blockages of 70% or less would not have significant effect on reflood rates at 50 psia or greater containment pressure.

For the three-loop Turkey Point Plant, Westinghouse calculated a gross flooding rate of from 0.8 in./sec to 1.1 in./sec for 40 sec to 180 sec after the break.¹¹ The corresponding driving pressure varied from 1.6 to 3.2 psi. From Fig. 3 this pressure range corresponds to exit flooding rates of 0.5 in./sec to 0.7 in./sec for a four-loop plant and from 0.6 in./sec to 0.9 in./sec for a two-loop, four-pump plant. The corresponding inlet flooding rates (obtained from the relation $n = n^*/0.8$ discussed in Sect. 3.2) are 0.7 to 0.9 in./sec and 0.8 to 1.1 in./sec for the four- and two-loop plants, respectively, in good agreement with the Westinghouse calculations. Combustion Engineering estimated a maximum steam-flow rate leaving the upper plenum for a two-loop, four-pump plant of 275 lb/sec (100% quality) which corresponds to an inlet flooding rate of 1.3 in./sec, compared with 1.6 in./sec estimated by this analysis. The fact that Combustion Engineering used a pump K-factor of 12.5 compared with 10 used here accounts for much of the difference.

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III. EFFECT OF DROPLET ENTRAINMENT ON HEAT TRANSFER DURING REFLOOD

SUMMARY AND CONCLUSIONS

Boiling at the quench front during the reflood stage of LOCA causes droplets to be entrained and carried upward with the steam. The presence of these droplets could enhance the heat transfer in three ways: (1) by droplets impinging on the walls creating local small area "quenches" at the points of impact, (2) by radiant heat exchange between the droplets and the clad surface, and (3) by convective heat exchange from the steam to the droplets.

An estimate was made of the possible magnitude of these effects and their significance at FLECHT conditions. An idealized steady-state case was examined in which the clad surface temperature was assumed constant. For ease of computation, the droplets were treated as having constant size and assumed to be at saturation conditions. The steam physical properties were also assumed constant at saturation conditions although considerable steam superheat was indicated by the calculations.

The steam temperatures and FLECHT-type heat transfer coefficient were determined through a differential heat balance and shown to depend on the steam quality (droplet volume fraction), droplet diameter, elevation (above the quench front), clad surface temperature, and reflood rate. A limited parametric variation of these indicated that entrained droplets can significantly increase the heat transfer film coefficients as they are determined in the FLECHT program. Most (typically 80%) of this improvement resulted from direct contact heat exchange between steam and droplets which reduced the steam superheat. The rest was due to direct radiation to the droplets. The effect of droplet impingement was considered to be negligible because the clad surface temperature was so high above the "Leidenfrost" value.

Treating elevation in the steady-state calculations as distance above quench front and assuming reasonable values for quality and droplet size resulted in calculated midplane heat transfer coefficients that were consistent with the measured FLECHT transient values.

The effect of blockage was estimated by assuming a generated droplet size and using a criterion of Weber number = 6 to establish the droplet breakup due to increased shear in the blockage region. A 78% area blockage without flow reduction resulted in halving the droplet size from the assumed generated value of 0.015 in. to 0.0075 in. Beyond the blockage at the 10-ft elevation (near the exit), the steam temperature was reduced by $\sim 112^{\circ}\text{F}$ and the heat transfer film coefficient was increased from ~ 2.7 to ~ 4.4 . This apparent improvement in heat transfer above a blockage area probably would not be realized under circumstances where redistribution of coolant to regions of lesser blockage causes local reductions in flow through the blocked channels.

1. DROPLET IMPINGEMENT

Droplets carried along by the flowing steam can be transferred to surfaces by turbulent diffusion and inertial impaction. If impinging droplets create a local "quench," there conceivably could be a significant increase in the cooling rate over that of steam alone. However, for droplets to provide an effective quench, the hot-surface temperature must be below the "Leidenfrost" point, which has been estimated in pool boiling to be at wall superheats as low as 50°F . For flowing systems, Tong⁵ reports Leidenfrost superheats as high as 350°F . Consequently, at the assumed saturation temperature of 287°F , the clad temperature can be no greater than $\sim 637^{\circ}\text{F}$ for droplet quenching to occur. Since the clad surface does not get below this temperature before arrival of the quench front, the effect of droplet impingement is considered to be negligible.

2. DIRECT RADIATION

The heat transferred from the wall of the droplets by radiation can be estimated using the radiant exchange equation for small convex surfaces contained in a large nearly isothermal enclosure,¹

$$Q_{1 \rightarrow 2} \cong \sigma A_1 [e_1 T_1^4 - a_1 T_2^4] ,$$

where e_1 is the droplet emissivity at T_1 and a_1 is estimated as the value

of e_1 at T_2 . In this idealized case it is assumed that $e_1 = a_1$. The radiant heat exchange per droplet is therefore

$$Q \cong (0.17) \pi (d^2) \left[\left(\frac{T_{\text{clad}}}{100} \right)^4 - \left(\frac{T_{\text{sat}}}{100} \right)^4 \right]$$

The total number of droplets in the subchannel can be calculated from

$$N = \frac{6 \Phi L A_{\text{cs}}}{\pi d^3} = \frac{6 \Phi A D_e}{4 \pi d^3},$$

where

Φ = volume fraction occupied by droplets

L = channel length

A_{cs} = subchannel cross-sectional area

D_e = subchannel equivalent diameter

d = droplet diameter

A = subchannel surface area.

The local radiant heat flux, then, is given by

$$Q_r/A = NQ/A = \frac{(6)(0.17)\Phi D_e}{4d} \left[\left(\frac{T_{\text{clad}}}{100} \right)^4 - \left(\frac{T_{\text{sat}}}{100} \right)^4 \right] \quad (1)$$

The radiant heat flux is inversely proportional to the droplet diameter. Therefore a reduction in droplet diameter by a factor of 10 (as might be encountered in a severe blockage) increases the local radiant heat flux by a factor of 10.

The coefficient, $6\Phi D_e/4d$, is seen to be the ratio of droplet surface area per channel unit length to surface area of the channel per unit length. Consequently, Eq. (1) would not be expected to be valid if this coefficient exceeds a value of about 2.

3. DIRECT HEAT EXCHANGE BETWEEN STEAM AND DROPLETS

The analysis for this part is simplified as follows:

1. The steam and droplets are assumed to be at saturation conditions initially. The steam superheats as it passes up the subchannel but the droplets remain at saturation temperature, and are treated as if their size and number are constant and not affected by vaporization.

2. The convective heat transfer coefficient from the wall to the mixture is calculated as if the steam were flowing alone.

A heat balance in a differential element of length of a subchannel (shown below)

results in the simple ordinary differential equation

$$\frac{dT}{dZ} + AT = B, \quad (2)$$

where

$$A \equiv \left(\frac{4h}{D_e} + h_d a \right) / \rho_g V C_p,$$

$$B \equiv \left(\frac{4hT_{clad}}{D_e} + h_d a T_{sat} \right) / \rho_g V C_p,$$

a = droplet surface area per unit volume

h = heat transfer film coefficient: clad-to-steam

h_d = heat transfer film coefficient: steam-to-droplet

P = perimeter of a subchannel

T = steam temperature

T_{clad} = clad surface temperature

T_{sat} = droplet temperature

V = steam velocity

ρ_g = steam density

C_p = steam specific heat

The solution of Eq. (2) with the boundary condition $T = T_{sat}$ at $Z = 0$, is

$$T = \frac{B}{A} (1 - e^{-AZ}) + T_{\text{sat}} e^{-AZ} \quad (3)$$

Equation (3) can be used to calculate the bulk steam temperature at any elevation above the quench front as a function of the parameters comprising the constants, A and B. The product of the wall-to-steam film coefficient and the temperature difference between the clad surface and the steam, then, will give the local convective heat flux.

Equations for estimating the parameters in A and B are discussed below.

4. HEAT TRANSFER COEFFICIENT - WALL TO STEAM

A reasonable estimate for the wall-to-steam convective heat transfer film coefficient, which includes radiation to the steam at temperatures of interest during reflood, can be obtained simply from the Dittus-Boelter correlation without making property variation corrections.² Therefore, h is estimated from

$$h \cong (0.023) \frac{k}{D_e} (Re)^{0.8} (Pr)^{0.4} \quad (4)$$

5. HEAT TRANSFER COEFFICIENT - STEAM TO DROPLET

Very little data exist for direct contact heat-exchange between entrained liquid droplets and high-temperature steam. Therefore, Eq. (5),

$$Nu = 0.98 Re_d^{0.417} Pr^{0.333} \quad (5)$$

is taken from a correlation for mass transfer to fluidized droplets through the heat-mass transfer analogy.³ A thorough search of the literature should reveal more appropriate correlations (obtained at high steam temperatures with actual heat transfer to droplets). However, their forms should be similar to Eq. (5) which was used in the present illustrative calculations. To use Eq. (5), a relative velocity between droplet and steam is needed to determine the droplet Reynolds number,

Re_d . An estimate of the ratio of turbulent inertial forces to gravitational forces [a "turbulence" Froude number] indicates that gravitational forces dominate at a reflood rate of 1 in./sec. Therefore, a likely estimate for the relative velocity should be the terminal velocity determined by balancing gravity and drag forces. Assuming the droplet Reynolds number is in the range $2 < Re_d \leq 200$, then

$$F_d \text{ (drag forces)} = 18.5 \mu^2 Re_d^{1.4} / 8g_c \rho_g$$

and

$$F_d \text{ (gravity forces)} = \rho_f \pi d^3 / 6g_c$$

Letting $F_d = F_g$ gives

$$Re_d = \left[\frac{8 d^3 \rho_g \rho_f g}{(6)(18.5)\mu^2} \right]^{1/1.4} \quad (6)$$

For a given droplet size, Re_d should be checked to confirm that it is in this range. If not, a different drag correlation should be used.

The use of Eq. (6) to establish the droplet Reynolds number assumes the maximum amount of slip between droplet and steam. The heat transfer calculations are therefore optimistic in that maximum heat transfer between steam-and-droplet results.

6. CALCULATING DROPLET SURFACE AREA PER UNIT VOLUME

The volume fraction of liquid is approximately related to the quality by

$$\Phi \cong \frac{1 - x}{(\rho_f / \rho_g) x S} \quad (7)$$

and the surface area per unit volume is

$$a \equiv \frac{6\Phi}{d} \quad (8)$$

In the present calculations, S will be assumed to be approximately 1 for determining volume fractions.

7. CALCULATION OF HEAT TRANSFER COEFFICIENTS

In the FLECHT experiments the local heat transfer film coefficients were determined by dividing the "measured" instantaneous heat flux, Q/A , by the instantaneous temperature difference, $T_{\text{clad}} - T_{\text{sat}}$. For the idealized steady-state case considered here, $T_{\text{clad}} - T_{\text{sat}}$ is constant and coefficients determined in the above manner will depend on the local heat flux only which will be given by the product $h(T_{\text{clad}} - T)$ plus the amount due to direct radiation, Q_{rad}/A .

Therefore, the heat transfer coefficients, h_f , determined as in the FLECHT experiments are calculated by

$$h_f = \frac{h(T_{\text{clad}} - T)}{(T_{\text{clad}} - T_{\text{sat}})} + \frac{Q_{\text{rad}}/A}{(T_{\text{clad}} - T_{\text{sat}})} \equiv h_s + h_r \quad (9)$$

Therefore, the steam temperatures and the apparent FLECHT-type heat transfer coefficients as determined in this manner depend on the reflood rate (steam Reynolds number), clad surface temperature, steam quality (droplet volume fraction), droplet diameter, and elevation (above the quench front).

8. STEAM FLOW

At a reflood rate of 1 in./sec, the inlet liquid flow rate will be

$$W_l = \rho_f VA_{cs} = \frac{(57)(1)(0.177)(3600)}{(1728)} = 21 \text{ lb}_m/\text{hr} ,$$

and at 6 in./sec it will be $(21)(6/1) = 126 \text{ lb}_m/\text{hr}$. In the FLECHT experiments, the carryover rate fraction, R , (fraction of core inlet flow which exits as steam and liquid) was observed to be relatively constant during the course of a run. At reflood rates of 1, 2, and 6 in./sec, the steady values measured were, respectively, 0.75, 0.85, and 0.92. Consequently, the flow rates leaving the channel were, respectively, 15.7, 35.7, and 116 lb_m/hr . If these were all steam (no entrained droplets) the respective velocities using saturation properties would be 28, 63.6, and 207 ft/sec.

9. DROPLET SIZE AND EFFECT OF BLOCKAGE

In general, droplets will be injected into the vapor by bubbles bursting at interfaces near the quench front. Mitsuishi⁴ found that liquid injected in this manner comes from bubbles with diameters of 2 mm or less, and that a bubble diameter of 2 mm corresponds to a droplet size of about 300 microns (0.015 in.). Droplets of this size are easily carried upward through the core by the estimated steam velocities. The above droplet size is already smaller than the equilibrium size that would be estimated for a 1 in./sec reflood rate using a criterion of Weber number equal to 6:

$$d_e = \frac{12 \sigma g_c}{\rho_g S^2 v^2} \quad (10)$$

The slip, S , is difficult to determine. A value could perhaps be obtained through a relation like Eq. (6). However, for the present purposes, the greatest relative velocity between steam and droplet is obtained if $S = 1$ meaning the droplet is just suspended. The resulting droplet size due to atomization using this maximum relative velocity will be conservatively small.

Using physical properties listed in the next section, Eq. (10) then gives

$$d_e = \frac{(12)(0.0035)(32.2)(7.787)(12)}{(28)^2} \approx 0.16 \text{ in.}$$

The effect of blockage on the droplet size, however, may be significant. From Eq. (10), if we can once again use the ratio of steam velocities as a measure of the ratio of relative velocities,

$$d_{e2} = d_{e1} \left(\frac{A_{cs2}}{A_{cs1}} \right)^2$$

The subscripts 1 and 2 refer to upstream of the blockage and within the blockage, respectively. The equilibrium droplet size for ~78% area blockage (assuming no reduction in flow) is ~0.0075 in., which is half of the assumed generated size of 0.015 in. Thus, uniform blockage with

no significant reduction in flow would be expected to break up the droplets to smaller size as was observed in the FLECHT experiments with blockage plates. A flow area reduction of only 70% gives

$$d_{e2} \approx 0.014 \text{ in.}$$

Therefore, based on this criterion, uniform blockage would have to exceed 70% for atomization at 1 in./sec reflood rates to occur from an initial droplet size of 0.015 in.

At a reflood rate of 6 in./sec, the equilibrium droplet size based on the above type calculation is

$$d_e = (0.16) \frac{28}{207}^2 = 0.0029 \text{ in.} ,$$

which is smaller than the generated size. Consequently, at this high reflood rate, some droplet atomization can be expected for all levels of blockage if the blockage does not result in lower shear rates due to redistribution and reduction of flow.

The effect of blockage on steam temperature and heat transfer coefficient was estimated for a 1 in./sec reflood rate using Eqs. (1) through (9) with assumed arbitrary quality of 0.8 and clad surface temperature of 1800°F. No blockage with droplet diameter of 0.015 in. throughout the length resulted in a 10 ft elevation steam temperature and heat transfer film coefficient of 1601°F and ~2.7 Btu/hr ft² °F, respectively. With blockage located at midplane to reduce the droplet diameter to 0.0075 in. (~78% area blockage), the 10-ft elevation steam temperature decreased to 1479°F (with no reduction in flow) and the apparent FLECHT-type heat transfer film coefficient increased to ~4.4 Btu/hr ft² °F.

10. PARAMETRIC VARIATION

Using the relations developed previously, a limited parametric variation was made to determine the sensitivity of the steam temperature and the FLECHT-type heat transfer coefficients to changes in the variables. The steam temperatures were calculated using Eq. (3), radiant heat flux using Eq. (1) and the FLECHT-type heat transfer coefficients using Eq. (9).

The steam generated near the quench front was assumed to be saturated at 55 psia and 287°F. Fluid properties were assumed constant at the following values:

$$\begin{aligned}\sigma &= 51 \text{ dynes/cm} = 0.0035 \text{ lb}_f/\text{ft} \\ \rho_g &= 1/7.787 \text{ lb}_m/\text{ft}^3 \\ \mu &= 0.035 \text{ lb}_m/\text{ft hr} \\ k &= 0.015 \text{ Btu/hr ft } ^\circ\text{F} \\ C_p &= 0.46 \text{ Btu/lb}_m ^\circ\text{F} \\ \rho_f &= 57 \text{ lb}_m/\text{ft}^3 \\ Pr &= 0.96.\end{aligned}$$

When a given parameter was varied, representative arbitrary constant values were chosen for the others. The results are given in Tables 1 through 5.

Table 1. Variation of Quality, X

Reflood rate = 1 in./sec
Droplet diameter = 0.015 in.
Wall temperature = 1800°F
Elevation = 10 ft

X	T (°F)	Btu/hr ft ² °F		
		h_s	h_r	h_f
1.0	1750	0.66	0	0.66
0.9	1649	1.53	0.21	1.74
0.8	1543	2.19	0.48	2.67
0.7	1390	3.03	0.83	3.86
0.6	1197	3.89	1.29	5.18
0.5	945	4.74	1.93	6.71

Table 2. Variation of Elevation, Z

Reflood rate = 1 in./sec
 Droplet size = 0.015 in.
 Wall temperature = 1800°F
 Quality = 0.8

Z (ft)	T (°F)	h_s	h_r	h_f
0	287	16.7	0.5	17.2
2	975	9.1	0.5	9.6
4	1313	5.4	0.5	5.9
6	1479	3.5	0.5	4.0
8	1561	2.6	0.5	3.1
10	1601	2.2	0.5	2.7

Table 3. Variation of Droplet Diameter, d

Reflood rate = 1 in./sec
 Wall Temperature = 1800°F
 Elevation = 10 ft
 Quality = 0.8

d (in.)	T (°F)	h_s	h_r	h_f
0.015	1601	2.19	0.48	2.7
0.0075	1469	3.64	0.96	4.6
0.0015	887	10.03	4.80	14.8

Table 4. Variation in Wall Temperature, T_w

Reflood rate = 1 in./sec
 Elevation = 10 ft
 Quality = 0.8
 Droplet diameter = 0.015

T_w (°F)	T (°F)	h_s	h_r	h_f
1800	1601	2.19	0.48	2.67
1900	1688	2.33	0.57	2.90
2000	1775	2.48	0.68	3.16
2100	1861	2.62	0.79	3.41
2200	1948	2.77	0.93	3.70

Table 5. Variation in Reflood Rate

Elevation = 10 ft
 Quality = 0.8
 Droplet diameter = 0.015 in.
 Wall temperature = 1800°F

Reflood Rate (in./sec)	T (°F)	h_s	h_r	h_f
1.0	1601	2.19	0.48	2.67
2.0	1635	3.56	0.48	4.04
6.0	1597	11.06	0.48	11.54

Inspection of Tables 1 through 5 reveals that the presence of droplets can significantly increase the heat transfer film coefficients as they are determined in the FLECHT program. Most of the improvement results from direct contact heat exchange between the steam and the droplets, which reduces the steam superheat. The amount of augmentation strongly depends on the quantity of entrained liquid and the size of the droplets.

The magnitudes of the coefficients estimated here are consistent with FLECHT results typically illustrated in Fig. 1 in which "Period-I" is the applicable region for cooling by steam and entrained droplets. The steady increase in the measured coefficients with time during "Period-I" corresponds to a decrease in elevation in the steady-state calculations (see Table 2) as the quench front advances. "Period-I" typically ends at a dimensionless time, t/t_{q6} , of ~ 0.2 (the ratio of elapsed time to the time required for quench at the 6-ft elevation). If the quench front is moving at a steady pace, at this time it should have advanced up into the core a distance of $\sim (0.2)(6) = 1.2$ ft or a distance of ~ 4.8 ft away from the midplane. To obtain the measured typical midplane value at the end of "Period-I" of 8 Btu/hr ft² °F (reflood rate of 1 in./sec) from the steady-state calculations at an elevation of 4.8 ft; a droplet diameter of 0.015 in., and a wall temperature of 1800°F, a quality of ~ 0.42 is required. However, if the droplet diameter is reduced to 0.0075 in., then the required quality is only 0.63, which appears to be a reasonable value.

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Fig. 1. Schematic Diagram of FLECHT Heat Transfer Coefficient versus Dimensionless Time.

The temperature maximum as reflood rate decreases (Table 5) results from competing heat transfer processes. The heat coming from the clad surface tends to increase the steam temperature whereas that going to the droplets from the steam tends to decrease the steam temperature. Decreasing the reflood rate from 2 to 1 in./sec would normally increase the steam temperature at a given elevation in the absence of droplets. However, at a given droplet volume fraction, the amount of heat transferred from steam to droplets in the calculations is independent of reflood rate and its relative influence on the steam temperature increases as flow is reduced.

The relative insensitivity of the calculated coefficients to the assumed value for wall temperature (Table 4) justifies the use of steady-state calculations to some extent. However, the calculations also assumed a constant droplet diameter. For the first two cases in Table 3 ($d = 0.015$ and 0.0075 in.), the amount of vaporization of liquid from the droplets was calculated to be 4.4 and 7.6 lb_m/hr, respectively, over the 10 ft length. At a quality of 0.8, the amount of liquid available in the droplets is only $(0.2)(15.7) \sim 3.1$ lb_m/hr. Clearly the assumption of constant droplet size is not justified. Consequently, the heat transfer augmentation indicated in these calculations is optimistic. A more sophisticated analysis should allow changes in droplet size and transient wall temperatures.

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IV. ESTIMATE OF EFFECT OF LOCALIZED FLOW BLOCKAGES ON PWR CLAD TEMPERATURES DURING REFLOOD

W. R. Gambill

SUMMARY

Calculations were made of cladding temperature changes associated with various localized blockages during reflood following a PWR LOCA. A physically realistic model which allows for radial flow-diversion effects was adopted by equating pressure drops for regions of high blockage with pressure drops for parallel regions of lesser or zero blockage over various flow-disturbance heights. The choice of the heights with which the blocked-region flow reductions were calculated was guided by experimental and calculated values for the upstream-diversion and downstream-redistribution lengths below and above the blockage corresponding to pressure/flow radial equilibration. The maximum value of total flow-disturbance length selected corresponds to 30% of the height of a 12-ft core.

The analysis yields hot-pin cladding temperatures for nine of the ten blockage combinations examined that exceed the temperature for the unblocked case by 400°F or more. By introducing a nonarbitrary, but uncertain, blocked-region heat-transfer coefficient multiplier of ~1.5 in an attempt to account for reduced-equivalent-diameter and entrance-region effects, the peak cladding temperatures were reduced significantly. Negative values of Δt_{clad} were predicted for some blockage combinations; but the results are strongly dependent on the radial blockage distribution, and the increases in cladding temperature for a 90% blockage embedded in a region of 0% blockage, for 90% in 50%, for 90% in 70%, and for 80% in 0% were $\geq 400^\circ\text{F}$. When the heat-transfer coefficient multiplier was increased to 2.0, the cladding temperature increase for only the 90% in 0% and 90% in 50% cases remained above 400°F. The estimates of clad temperature rise reported here for high blockage generally significantly exceed the vendors' published estimates, which apparently correspond to overall hot-assembly flow reductions in the absence of lateral flow diversion.

1. INTRODUCTION

Independent estimates^{1,2} of the effect of uniform core blockage on coolant flow rates in PWR's during reflood have yielded substantially the same result: that significant core flow reductions will not occur, at the higher containment pressures, for core midplane blockages less than ~70%.

In this section, I have estimated the effect of relatively large flow area reductions over a small cross-sectional area of the core on the reduction of flow and the corresponding change of clad temperature in the region of high blockage.

2. BASE CONDITIONS ASSUMED

Unblocked clad temperature [*] :	$(t_c)_o = 2000^\circ\text{F}$
Average core pressure:	$P = 55 \text{ psia}$
Coolant temperature:	$t_b = 287^\circ\text{F} = t_{\text{sat}}$
Coolant film temp:	$\Delta t_f = 1713^\circ\text{F}$
Maximum hot-pin heat flux:	$\phi = 30,000 \text{ Btu/hr}\cdot\text{ft}^2$
Reference heat-transfer coefficient ($\phi/\Delta t_f$):	$h_{\text{ref}} = 17.51 \text{ Btu/hr}\cdot\text{ft}^2\cdot^\circ\text{F}$

The heat flux chosen corresponds to a time ~60 sec after the LOCA start and to ~40 sec into reflood, as taken from the afterheat curve representing (ANS + 20%). Babcock and Wilcox (B&W) values of nominal rod diameter d (0.430 in.), pitch p (0.570 in.), and pitch-to-diameter ratio p/d (1.326), were used since they are quite close to the average values for the arrays used by the three PWR vendors. The corresponding unit-cell equivalent diameter (d_e) is 0.532 in. (0.0443 ft).

A single reflood rate of 1 ips was assumed; using a ratio of outlet steam weight flow rate to inlet water weight flow rate of 0.75, the corresponding saturated-steam velocity is 28 fps and the nominal-bundle:

$$N_{\text{Re}} = 17,400.$$

^{*}It is noted that if severe blockage occurs early in the blowdown phase, increased clad temperatures would be anticipated at the beginning of reflood because of the reduced heat extraction during blowdown and the retarded initiation of reflood.

3. CALCULATION SEQUENCE

For various combinations of high blockage embedded in surrounding regions of zero or of finite (lower) blockage, the following sequence was followed:

a. Calculate the ratio of the blocked-region velocity to the surrounding-region velocity (V_B/V_{surr}). If the coolant density is constant radially, $V_B/V_{surr} = G_b/G_{surr}$, where G is mass velocity. Further comments will be made regarding this critical step of the calculation in the following section.

b. Calculate the blocked-region h from:

$$h_B = h_{ref} (V_B/V_{surr})^m \quad (1)$$

Values of $m = 0.6$ and 0.8 were used, but only the results for $m = 0.6$ (see Ref. 3) are reported here. It is noted that use of $m = 0.6$ might be nonconservative since Heineman⁴ reported $m = 0.84$ for turbulent superheated steam flow in tubes; and Schack, in his evaluation⁵ of the velocity effect for turbulent gas flow, found (from 12 studies, 10 with steam) that m ranged from 0.685 (average minimum) to 0.84 (average maximum), and he recommended an overall average of $m = 0.752$. In addition, Lord⁶ found that for forced-convection surface-to-steam heat transfer, including radiation to the steam at temperatures of interest during reflood, h can be obtained from the Dittus-Boelter correlation - i.e., with $m = 0.8$. In their response of August 15, 1972, to AEC Regulatory questions, Westinghouse showed (on p. III. 48-3) that in the relation $h \propto V_{in}^m$, $m \approx 0.6$ for V_{in} down to values of 1.0 to 1.2 ips, below which velocity range the slopes change sharply. Only one set of PWR-FLECHT data was used in the plot (Fig. III. 48-1), and no points are shown.

c. Calculate the blocked-region film temperature drop from:

$$(\Delta t_f)_B = \phi_B / h_B \quad (2)$$

The value of ϕ_B was taken equal to the unblocked-case heat flux (ϕ_{UB}). This assumption will be discussed in more detail in the following section.

d. Calculate the blocked-region clad temperature from:

$$(t_c)_B = (\Delta t_f)_B + t_{sat} \quad (3)$$

e. Compute the clad temperature change associated with blockage from:

$$\Delta t_c = [(t_c)_B - (t_c)_o] , \quad (4)$$

in which $(t_c)_o = 2000^\circ\text{F}$.

4. REGARDING THE CALCULATION OF VELOCITY REDUCTION AND THE VALUE OF BLOCKED-CASE HEAT FLUX

4.1 Velocity Reduction

The writer is in accord with the equations used by B&W in their response to AEC Regulatory questions (July 7, 1972, pp. II. 4-1 to II. 4-2), but differs in the application of the equations. Neglecting differences in elevation pressure drops between adjacent blocked and unblocked sub-channels - i.e., assuming that $\bar{\rho}_B = \bar{\rho}_{UB}$, the B&W relationship is:

$$\frac{G_B}{G_{UB}} = \sqrt{\frac{K_f + K_g + K_a}{K_f + K_g + K_a + K_B}} , \quad (5)$$

in which K , the loss coefficient, is defined by:

$$K \equiv \frac{2g_c \rho \Delta P}{G^2} = \frac{\Delta h}{V^2/2g_c} , \quad (6)$$

and in which subscripts f , g , a , and B denote friction, grids, acceleration, and blockage, respectively.

Modifying Eq. (5) to account for the possibility that the region surrounding the zone of high blockage is also blocked (though to a lesser degree) yields the equation I have used:

$$\frac{V_B}{V_{surr}} = \sqrt{\frac{K_f + K_g + K_a + K_{surr}}{K_f + K_g + K_a + K_B}} , \quad (7)$$

in which subscripts B and $surr$ denote "high-blockage region" and "surrounding region," respectively. $K_{surr} = 0$ for an adjacent blockage of zero and $K_{surr} > 0$ for finite adjacent blockages.

It is reasonably clear from examining the B&W document (p. II. 4-2) that Eq. (5) was applied to a full-length (12-ft high) hot assembly,

assuming zero lateral flow - i.e., that the lateral flow-diversion loss-coefficient $K_L = \infty$. Carbiener⁷ used the same approach in the computations he performed with the MUCHA-1 computer code, explicitly noting that the "code does not consider crossflow and would predict only an overall channel flow reduction" (emphasis added).

B&W (ibid) calculated $G_B/G_{UB} = 0.86$ for an 80% blockage and inlet reflood rate of 2.9 ips. Using their K factors:

$$K_f = 90, K_g = 25, K_a = 8.5, K_B = 39,$$

and Eq. (5), I calculate $G_B/G_{UB} = 0.872$. Assuming a blockage length of 6 in., using the K factors given in Appendices I and II:

$$(K_f)_B = (10.0 \text{ ft})(0.764/\text{ft}) = 7.64; (K_f)_{\text{surr}} = (10.5 \text{ ft})(0.764/\text{ft}) = 8.02,$$

$$K_g = (9 \text{ grids})(1.40/\text{grid}) = 12.6 \text{ } (L_g = 1.5 \text{ ft}),$$

$$K_a = 8.5 \text{ (B\&W value retained),}$$

$$K_{\text{surr}} = 0 \text{ (zero adjacent blockage),}$$

$$K_B = 31 \text{ (for 80\% blockage),}$$

and applying Eq. (7) to the full height of 12 ft, I calculate $V_B/V_{UB} = 0.698$. The maximum difference between these velocity ratios is 25%; the difference is primarily attributable to the unusually large, and questionable, value of K_f used by B&W, which corresponds to a frictional loss of one velocity head over a length of $\sim 2.6 d_e$ (vs one velocity head loss over a length of $\sim 30 d_e$ for $K_f = 8.02$).

[Note that in Ref. 3 (pp. D-50 and D-51), Westinghouse reported a blocked hot-assembly reflood mass velocity at the blockage elevation, as computed with the CHARM code, of 0.7 times the core-average mass velocity. The design blockage value used was 65%.]

Following the previously described calculation sequence, the corresponding clad temperature increments are as follows:

V_B/V_{UB}	$\Delta t_{\text{clad}} (^{\circ}\text{F})$
0.698	413
0.860	162
0.872	147

The last two values for Δt_{clad} are comparable to B&W's estimate of 120°F for the blowdown phase (p. 6-16 of the B&W Direct Testimony); the effect of blockage during reflood was not quantitatively estimated. My estimate, largely because of the difference in K_F values, is ~260°F higher even for this calculation allowing no lateral flow diversion.

In a phenomenological approach, I have applied Eq. (7), which is based on equating blocked and unblocked (or lesser blocked) channel pressure drops, over distances upstream and downstream of assumed blockages which correspond to experimentally determined lengths required for pressure/flow radial redistribution. The concept is illustrated in the figure below:

in which L_{UD} , L_B , and L_{DR} represent the upstream diversion length, the length of significant blockage, and the downstream redistribution length, respectively. Since $(P_1 - P_6)$, $(P_1 - P_2)$, and $(P_5 - P_6)$ are each equal for all subchannels in parallel, the object is to choose a physically realistic value for $L = (L_{UD} + L_B + L_{DR})$ so that $(P_2 - P_5)$ is also equal for parallel subchannels. By varying L , K_L is implicitly varied (increasing with L).

4.2 Blocked-Case Heat Flux

The assumption was made that $\phi_B = \phi_{UB} = 30,000 \text{ Btu/hr}\cdot\text{ft}^2$. Factors tending to cause ϕ_B to exceed ϕ_{UB} are the metal-water reaction (MWR) incremental ϕ (~11,000 Btu/hr·ft² at 2300°F with a ZrO₂ thickness of 1 mil)⁸

and pressed-together clad surface inaccessible to coolant for large deformations. Factors potentially tending to cause ϕ_{UB} to exceed ϕ_B are the additional clad surface area accessible to coolant (not touching other clad) and possible lack of thermal equilibrium between the pellet and expanded clad.

Using Lorenz's "equal circumferential strain" fuel-rod expansion model,⁹ which results in highly expanded cladding having a rounded-corner square shape with interior quadri-cusp flow channels, the ratio of coolable blocked-case surface area to unblocked surface area, $(A_s)_R/(A_s)_{UB}$, was calculated; its variation with percent blockage is shown in Fig. 1. If symmetrical swelling occurs, the rods just touch one another when the blockage reaches 61.2%, and the surface area is ~33% larger than for an undeformed array. The surface-area ratio reaches unity, however, at a blockage of 78%, and at higher blockages, there is less coolable surface area (67% of nominal at 90% blockage). The variation of flow-channel equivalent diameter with blockage is also shown in Fig. 1 for this idealized model.

The pellet-cladding thermal interaction is assumed not to alter the unblocked ϕ at $\theta \approx 1$ min based on recent Westinghouse information,¹⁰ which shows that the clad temperature with blockage does not fall below the temperature without blockage before the pellet reaches a temperature level sufficient to transfer adequate heat to the cladding. Using an $h_{gap} \approx 0.04 \text{ w/cm}^2 \text{ } ^\circ\text{C} = 70.5 \text{ Btu/hr}\cdot\text{ft}^2\cdot^\circ\text{F}$, a Δt (pellet $\rightarrow$ cladding) of 426°F gives $\phi = 30,000 \text{ Btu/hr}\cdot\text{ft}^2$, and from Ref. 10, this temperature rise occurs in roughly 10 to 15 sec.

The combined effect of these multiple factors seems impossible to quantify at this time. In effect, no credit was taken for increased surface area (possibly conservative) and the ϕ contribution from any MWR was taken as zero (nonconservative for most cases).

Fig. 1. Variation of Coolable Surface Area and of Equivalent Diameter with Blockage.

Based on PWR elements, symmetrical swelling, and equal circumferential strain model.

5. CASES CONSIDERED

Four cases were considered, as outlined below:

- Case I. $L = L_B = 5$ in. This corresponds to $K_L = 0$. $K_B = 0$.
This is a limit case at the opposite extreme of the one discussed previously ($K_L = \infty$).
- Case II. $L = 18.3$ in., composed of $L_B = 5$ in., $L_{UD} = 5 d_e = 2.66$ in., and $L_{DR} = 20 d_e = 10.64$ in. $0 < K_L < \infty$. One grid spacer.
- Case III. $L = 31.5$ in., composed of $L_B = 7$ in., $L_{UD} = 6 d_e = 3.20$ in., and $L_{DR} = 40 d_e = 21.3$ in. $0 < K_L < \infty$. One grid spacer.
- Case IV. $L = 43.2$ in., composed of $L_B = 7$ in., $L_{UD} = 8 d_e = 4.26$ in., and $L_{DR} = 60 d_e = 31.9$ in. $0 < K_L < \infty$. Two grid spacers.

Case I is unrealistic in the sense that a portion of the flow would diverge instantly and without lateral resistance at the blockage base and converge in the same manner at the end of the blockage region. Case II reflects data¹¹ for turbulent entry-region heat transfer in tubes with inlet configurations producing extremely skewed initial velocity profiles which show an essentially complete approach to asymptotic heat-transfer rates within 20 diameters. This case also approximates the results reported by Rowe¹² for the distance over which the blockage influenced the flow in his COBRA-III calculation of a partially blocked PWR-FLECHT 7 x 7 bundle* with a 1 ips flooding rate ($\sim 4 d_e$ upstream and $\sim 23 d_e$ downstream). Case III approximates the blockage disturbance lengths found experimentally by Rowe and his co-workers¹³ at BNWL for a 90% planar blockage in a bundle of 1-in.-OD rods on a square pitch ($\sim 6 d_e$ upstream and 40-50 d_e downstream) as well as the downstream flow redistribution distance he calculated¹⁴ for the Zion plant† ($\sim 47 d_e$). Case IV is based on Swiss data¹⁵ for air flow through two parallel channels divided by a laterally adjustable gap and with an axially movable blockage in one channel. Reynolds moduli were $\geq 37 \times 10^3$ and the blockage was varied from 47 to 88%. In all cases, the ΔP between subchannels and the difference in mass flow rates between the

* With a unit-cell $d_e = 0.534$ in.

† For a 6-in.-long 50% blockage of one assembly; $d_e = 0.512$ in.

blocked and free subchannels was essentially zero at distances upstream of the blockage of $8 d_e$ or greater. Downstream of the blockage, pressure equilibration was also attained within $\sim 8 d_e$, but flow redistribution occurred more slowly. The following values were obtained from a plot of the data:

Blockage (%)	(x/d_e) downstream ^a
70	~ 12
80	33
85	50
87	60

^aOne anomalous point corresponding to $x/d_e = 58$ for a 47% blockage was omitted.

Case IV represents the maximum value of L which can be based on experimental data known to the writer. For each of the four cases - i.e., for each of the blockage-disturbance heights - Eq. (7) was used to calculate the velocity ratio for ten combinations of blockage: 20% surrounded by 0% (designated as 20/0), 70/0, 70/50, 80/0, 80/50, 80/60, 90/0, 90/50, 90/70, and 90/80.

6. RESULTS

Using the K factors and calculation procedure described, the results are depicted in Fig. 2,* in which t_{MP} is the melting point of Zircaloy-4 and t_{IAC} is the Interim Acceptance Criteria temperature limit. Even for Cases III and IV ($L = 31.5$ in. and 43.2 in.), all values of Δt_{clad} exceed 300°F except for the 20/0 combination.

7. ESTIMATION OF h MULTIPLIER

In an effort to account realistically for a possibly enhanced heat-transfer coefficient in the blockage region caused by reduced- d_e and entrance-region effects, an h multiplier = 1.51 was formulated as follows:

*See Table 1 for explanatory notes for Figs. 2, 3, and 4.

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Fig. 2. Effect of Localized Flow Blockage on Cladding Temperature During Reflood in PWR.

- With crossflow and h multiplier = 1.0.

Table 1. Explanatory Notes for Figs. 2, 3, and 4

Assumed cladding temperature without blockage = 2000°F.

Assumed heat flux through cladding = 30,000 Btu/hr.ft².

Heat-transfer coefficient, without blockage, $h_{ref} = 17.51 \text{ Btu/hr.ft}^2\text{.}^\circ\text{F}$.

Heat-transfer coefficient with blockage,

$$h_B = h_{ref} \left(\frac{\text{velocity with high blockage}}{\text{velocity in adjacent region}} \right)^{0.6}$$

$$A/B = \frac{\% \text{ blockage in region of high blockage}}{\% \text{ blockage in surrounding region}}$$

Flow lengths over which pressure drops were assumed to be equal in parallel regions of high and low blockage for calculating the effect of blockage are tabulated below. Increasing flow redistribution length has the effect of increasing K_L for crossflow. $K_L = 0$ for the 5-in. flow length.

Blockage Length, in.	Flow Redistribution Lengths, Equivalent Diameters*		Flow Length, in.
	Upstream	Downstream	
5	0	0	5.0
5	5	20	18.3
7	6	40	31.5
7	8	60	43.2

* $d_e = 0.532 \text{ in.}$

$t_{MP} = \Delta t$ above 2000°F to reach 3360°F, the melting temperature of Zircaloy-4.

$t_{IAC} = \Delta t$ above 2000°F to reach 2300°F, the limit set by the Interim Acceptance Criteria.

In Fig. 3 an h multiplier = 1.0 is applied to the case 20/0 and a multiplier = 1.51 is applied to all other cases.

In Fig. 4 an h multiplier = 2.0 is applied to all cases.

(a) Reduced equivalent diameter:

For large clad deformations to rupture, individual flow-channel shapes and corresponding equivalent diameters seem to vary considerably, as seen in the cross-sectional views of deformed 32-rod bundles.¹⁶ The reference geometry was taken to be that for a 61% blockage caused by symmetrical swelling (rods just touching). For this case, $(d_e)_B = 0.1557$ in., and the h increase factor $= (0.532/0.1557)^{0.2} = 1.28$. The exponent of -0.2 is a standard value; for short lengths, the value could be smaller. Heineman,⁴ e.g., gives $h \propto d_e^{-0.13}$ for $6 < L/d_e < 60$.

(b) Entrance effect:

ORNL axial distribution curves for blockage¹⁸ show a mean entry length (to the plane of maximum area reduction) of ~ 2.0 in. Defining L/d_e as:

$$L/d_e = \frac{\text{Mean entry length}}{\text{Reduced } d_e \text{ for rods touching}}, \quad (8)$$

$L/d_e = 2.0/0.1557 = 12.8$. For this dimensionless distance, the following values of $(h_{entr,mn}/h_\infty)$ - i.e., the ratio of the average h in the entrance region to the asymptotic (fully developed) h - have been calculated:

Source	$h_{entr,mn}/h_\infty$
Ref. 17	1.18
Ref. 18 (Hausen correlation)	1.18
Ref. 18 (Iatzko correlation)	1.11
Ref. 11 (p. 190)	1.16

An h increase factor of 1.18 was selected.*

Combined factor $= (1.28)(1.18) = 1.51$.

8. MODIFIED RESULTS

In the calculation sequence, the value of h_B used previously was multiplied by 1.51 and new values of Δt_{clad} caused by blockage were computed. The multiplier was not used for the 20%/0% blockage combination because of the small area reduction. The results are given in Fig. 3.

* A discussion of Westinghouse's use of a factor of 2.0 during blowdown³ is presented in Appendix III.

Fig. 3. Effect of Localized Flow Blockage on Cladding Temperature During Reflood in PWR.

- With crossflow and h multiplier = 1.51.

Though all values of Δt_{clad} are smaller than in Fig. 2 (except for 20/0), Case III values for blockage combinations of 90/0, 90/50, 90/70, and 80/0 still represent clad temperatures exceeding the IAC limit.

It is worth noting that negative values of Δt_{clad} are predictable with this model including the h multiplier for some blockage combinations; e.g.,

Case	Blockage Combination	Δt_{clad} (°F)
I	70/50	-105
II	80/60	-15
III	80/60	-44
IV	70/0	-100

9. ESTIMATION OF INCREASED h MULTIPLIER AND RESULTS

To determine the effect of increasing the reflood h multiplier to 2.0, an additional factor of 1.325 was introduced. Physically, this incremental factor might be justified to account for a 420°F decrease of steam superheat in the blockage region caused by droplet evaporation. This increase of the h multiplier is not clearly realistic, since the unblocked h_{ref} is 17.5 Btu/hr·ft²·°F, while the h calculated for convective steam-cooling alone is 9.3 Btu/hr·ft²·°F (53% of the total). The remaining 47% of this FLECHT-type heat-transfer coefficient may be attributed to clad → droplet radiation, clad → steam radiation, and steam → droplet convection, and droplet effects might be overestimated by including the additional factor of 1.325.

Of the ten blockage combinations examined, the cladding temperature rise caused by blockage became negligible or negative for seven cases. For a flow redistribution length of 25 in. (between Cases II and III), the results were as follows for the remaining three cases:

Blockage:	80/0	90/50	90/0
Δt_{clad} (°F):	45	500	1080

The variation of Δt_{clad} with flow length is depicted in Fig. 4.

The ratio of $[2(h_B)_{\text{no multiplier}}/h_{\text{UB}}]$ for the 90/50 blockage combination, the case closest to AEC Regulatory's current best estimate of maximum/average blockage, is ~0.75 for realistic flow redistribution

Fig. 4. Effect of Localized Flow Blockage on Cladding Temperature During Reflood in FWR.

- With crossflow and h multiplier = 2.0.

lengths. Inclusion of estimated PWR-FLECHT experimental and correlation uncertainties and deviations could yield a ratio of h/h_{FLECHT} of ~ 0.6 as an evaluation value for a 90/50 blockage combination.

10. DISCUSSION

Because of the uncertainties associated with several of the factors entering any reflood thermal-hydraulic analysis, no exactness is claimed for the results shown in Figs. 2 through 4. The qualitative model is physically realistic, however, in that allowance is made for steam cross-flow over a flow length smaller than the core height. The result that the magnitude of Δt_{clad} is strongly dependent on the radial blockage distribution is not surprising. The FLECHT final report²⁶ included the following important qualifier: "Therefore, it is important to recognize the need to take the radial flow distribution into account in using FLECHT data for reactor loss-of-coolant accident analyses."

The existence of the blockage-distribution factor has been qualitatively recognized by the vendors. In Ref. 19, e.g., B&W states "... a 50% blockage of the hot bundle at some location with an 80% blockage of adjacent bundles on the same plane may not be as severe as, say, a 40% blockage of the hot bundle with no blockage in adjacent bundles" In the same reference, mention is made of "the low subchannel-to-subchannel cross flow resistance," an important factor which apparently is not included in their reflood analysis. In Ref. 20, CE states that "It is apparent that localized blockage in the hot bundle has a greater effect on peak clad temperature than uniform blockage across the entire core" and shows in a plot (Fig. 1, p. III-32-16 of Ref. 20) that the peak clad temperature with blockage is higher from their hot-bundle calculation than for their average-core calculation when the blockage exceeds 70%.*

The often-cited thermosiphon, "chimney," or "draft" effect caused by a radial contrast of coolant density should act to reduce hot-bundle (lower density) clad temperatures by increasing the flow rate in the hot bundle,

*CE proceeds²⁰ to estimate, for a 65% bundle blockage, $V_B/V_{UB} \approx 0.90$ and $\Delta t_{\text{clad}} \approx 26^\circ\text{F}$. Using the same procedure I applied to a B&W case on p. 72, I calculate $V_B/V_{UB} = 0.88$ and $\Delta t_{\text{clad}} = 135^\circ\text{F}$ (for zero crossflow).

but not in the region above the quench front where the steam volume fraction approaches unity (~ 0.99 for the conditions of this study), and especially not over the relatively small flow-disturbance heights used in this analysis. In an initial approximate analysis, I assumed an unrealistically large lateral $\Delta\rho_{\max} = (\rho_{\text{sat}} - 0)$ to calculate a corresponding driving head and found that it was too small by a factor of ~ 9 to equal frictional resistance alone for Case III. It seems that while this effect would be helpful at lower core elevations below the quench front, it would be insignificant at higher elevations during blocked reflood.

It is important to note that since the reference value for $\underline{h}$ includes all operative heat-transfer mechanisms, and since the analysis is a relative one by which cladding-temperature changes are calculated from velocity changes, the basic assumption is that the relative importance of the contributing mechanisms doesn't change significantly (during the period of early reflood) when blockage changes. For this reason, small changes of unblocked clad temperature, $(t_c)_0$, about the assumed value of 2000°F and of reflood velocity about the assumed rate of 1 ips should not change significantly the values of Δt_{clad} plotted in Figs 2 through 4. A comprehensive analysis would include blockage effects during the complete transient beginning at the time of break.

If the PWR vendors' estimates of maximum blockage (65 to 70% on a bundle basis) are correct, this analysis indicates (when an $\underline{h}$ multiplier is included) that the increase in clad temperature will not exceed $\sim 100^\circ\text{F}$ because of blockage-caused flow reductions. If the higher values of probable maximum blockage estimated by others (CNI and ORNL, e.g.) are correct, or if the $\underline{h}$ multiplier < 1.5 , the analysis indicates that many combinations of blockage could lead to clad temperature increases $> 300^\circ\text{F}$.

To refine blocked-reflood Δt_{clad} calculations, it is clear that cross-flow effects should be incorporated in reflood analyses in an appropriate manner, whether by use of this model or by including in computer calculations lateral flows based on experimental values of K_L (lateral-flow loss coefficients). Determination of the most probable values for the magnitude and radial distribution of LOCA-induced blockages is obviously a pressing need if realistic estimates of Δt_{clad} are to be made. Heat-transfer

coefficients applicable to high void fraction steam-droplet flow through bundles with axially distributed obstructions simulating cladding bulges existing over a limited cross-sectional area should be measured and correlated. In addition, more axial loss-coefficient data for blockages than those shown in Fig. I-1 are needed to lend additional confidence to the basic calculations of relative axial flow resistances. It also would seem appropriate to determine statistically the best-fit exponent in the relation $h \propto V_{in}^m$ using all relevant PWR-FLECHT data.

APPENDIX I

FLOW LOSS COEFFICIENTS FOR DEFORMED PWR ROD
BUNDLES TESTED AT ORNL

The following summarizes the subject flow tests.

The axial pressure-drop data we obtained for an undeformed reference bundle of PWR configuration and for two deformed bundles containing radially swollen and perforated tubes were reported by me to P. L. Rittenhouse, who in turn partially reported the results in two Oak Ridge National Laboratory Nuclear Safety Research and Development Program Bimonthly Reports: that for January-February 1971 (pp. 5-7), ORNL-TM-3342, and that for May-June 1971 (pp. 5-6), ORNL-TM-3483. The results I reported were expressed in terms of experimental conditions and equations for the dependence of component and overall pressure drops on flow rate. The bundles contained 32 rods of 0.420-in. initial outside diameter on a 0.566-in.-square pitch and were contained in a stainless steel shroud of irregular octagonal internal cross section. The equivalent diameter based on the full flow cross section was 0.499 in.; that based on a unit cell of the square lattice was 0.551 in.

I have since calculated a number of dimensionless flow head-loss factors, defined by

$$K \equiv \Delta h / (V^2 / 2g_c) \quad , \quad (1)$$

in which Δh is the total head loss across a particular region, V is the water velocity, and g_c is the gravitational conversion constant. The regions selected for K-factor computation were the upstream spacer grid of the reference bundle and the two 5-in.-long zones of significant deformation immediately downstream of the inlet spacer grids of Bundles W-1 and W-2 (the bundles used in FRFP Tests No. 9 and No. 10, respectively). The percent channel blockages at the ends of the 5-in.-long deformed zones were 5% (upstream) and ~6% (downstream) for Bundle W-1 and 5% (upstream) and 1.1% (downstream) for Bundle W-2. The percent channel blockage of a spacer grid - i.e., the percent reduction of net flow area - was calculated from the dimensions of a spacer (removed from an undeformed bundle) to be 16.5%. The axial variation of local channel blockage for the

5-in.-long regions of the two deformed bundles corresponds to the following average and maximum blockage values:

<u>Bundle No.</u>	<u>Percent Channel Blockage</u>	
	<u>Average^a</u>	<u>Maximum</u>
W-1	23.2	52.7
W-2	41.4	68.2

^aObtained by graphical integration of a large-scale version of a curve showing the axial distribution of local blockage. (see Fig. 25 on p. 31 of ORNL-4752, Jan. 1972).

In my report of the results, the component and overall experimental pressure drops for the reference and deformed bundles were represented by equations of the form:

$$\Delta P = a Q^n, \quad (2)$$

in which ΔP is axial static-pressure drop, Q is volumetric flow rate, and a and n are numerical constants. By combining these equations, the reference bundle grid-spacer ΔP and the ΔP across the 5-in.-long deformed regions were calculated to be:

<u>Q (gpm)</u>	<u>ΔP (psi)</u>		
	<u>Reference Bundle Spacer</u>	<u>Deformed Region of Bundle W-1</u>	<u>Bundle W-2</u>
100	0.20	0.67	1.71
200	0.76	2.59	6.36

The meaning of the loss-coefficient K [Eq. (1)] is ambiguous unless the velocity with which it is calculated is explicitly defined. Basing K on nominal-bundle velocities - i.e., on velocities calculated for zero rod deformation - the following results were obtained for $Q = 100$ - 200 gpm ($V = 56.6$ to 113.3 in./sec):

<u>K:</u>	<u>Reference Bundle Spacer</u>	<u>Deformed Region of Bundle W-1</u>	<u>Bundle W-2</u>
	1.26-1.33	4.32-4.48	10.59-11.40

Basing K on mean velocities in the restricted regions - i.e., on velocities calculated from the average channel blockages - the following results were obtained for the same flow-rate range:

	<u>Reference Bundle Spacer</u>	<u>Deformed Region of Bundle W-1</u>	<u>Deformed Region of Bundle W-2</u>
K:	0.88-0.93	2.55-2.64	3.64-3.92

In calculating losses across sudden changes of flow area, the conventional reference velocity is the maximum in the region considered. Adopting this convention and basing K on maximum velocities in the restricted regions - i.e., on velocities calculated from the maximum channel blockages - the following K factors were obtained for the same range of flow rates:

	<u>Reference Bundle Spacer</u>	<u>Deformed Region of Bundle W-1</u>	<u>Deformed Region of Bundle W-2</u>
K:	0.88-0.93	0.97-1.00	1.07-1.15

The arithmetic average of the six K values based on the maximum velocities is 1.00, meaning that the average total pressure loss across each of the three partially obstructed regions was simply one velocity head (within $\sim \pm 10\%$). This result, though based on data for only one grid spacer and two deformed zones, is considered reasonable since the losses involved are caused by flow-area restrictions and the lengths of the regions are only ~ 5 tube diameters (4 equivalent diameters) for the spacer and ~ 12 tube diameters (10 equivalent diameters) for the deformed tubes.

The averages of the K-factor pairs tabulated in the three preceding tables are plotted as curves 1, 2, and 3, respectively, in Fig. I-1. Also shown in Fig. I-1 as line 4 is a portion of a curve through four points calculated by W. A. Carbiener of BMI ["The Effects of Clad Swelling on Emergency Core Cooling Performance," Nuclear Technology, 11: 526-531 (August 1971); see p. 529] as input to the MUCHA-1 (Multiple Channel Analysis) computer code. The K factors of curve 4 were obtained from relationships previously developed for flow across rod-bundle spacer-grid assemblies. The equation defining the K factors of curve 4:

$$K = 2 g_c \rho \Delta p / G^2, \quad (3)$$

in which ρ is fluid density, Δp is pressure drop, and G is mass velocity, is identical to Eq. (1), since $\Delta p = \rho \Delta h$ and $G = \rho V$. The other relation depicted in Fig. I-1, that of curve 5, was reported as ORNL data by

ORNL-DWG 72-7500

Fig. I-1. Flow Loss Coefficient vs Flow Blockage for Deformed PWR Rod Bundles.

P. L. Rittenhouse in Fig. B-2 of Appendix B of Exhibit No. 1020. There is good agreement for maximum blockages exceeding 50% between my curve 1 and Carbiener's curve 4.

A plot was also made of K vs average percent flow area reduction, but curves 1 through 3 were each concave downward and less comparable with Carbiener's curve 4 and Rittenhouse's curve 5 than in the depiction of Fig. I-1 in which K is plotted vs maximum percent flow area reduction.

For each K-factor pair in the tables (corresponding to curves 1 through 3), the smaller value corresponds to $Q = 200$ gpm and the slightly larger value to $Q = 100$ gpm. For the two-fold flow-rate decrease, the average increase in K is 5.6%. If this dependence of K on velocity were valid down to a PWR reflood-velocity range (1-2 in./sec, e.g.), the K factors given by curves 1 through 3 of Fig. I-1 would be $\sim 1/3$ larger. No data are available, however, to lend credence to such an extrapolation.

It is not clear to me from the material I have examined whether curves 4 and 5 are based on average or maximum velocities or whether the distinction was made. It is clear that for the reference-bundle spacer grid and the primary deformed regions of the two swollen bundles tested at ORNL, the use of maximum velocities in computing K factors yields an especially simple relation:

$$K = 1 (\pm 0.1)$$

APPENDIX II

FLOW LOSS COEFFICIENTS USED IN THE CALCULATIONS

- (a) Friction. Using Tong's suggested friction-factor multiplier²¹ of 1.30 for $p/d = 1.33$, $f = 0.06/N_{Re}^{0.2}$. For the base conditions, this corresponds to a frictional pressure gradient of 0.0083 psi/ft and a loss coefficient $K_{l\ ft} = 0.764$ (K based on nominal bundle velocity).
- (b) Grid Spacer. In the bundles we tested (see Appendix I), the grid blockage was 16.5%. Carbiener states⁷ that "a typical spacer grid represents a flow area reduction of ~26%." A grid blockage of 20% was assumed, which corresponds to $K = 1.40$ from curve 1 of Fig. I-1 (based on nominal bundle velocity).
- (c) Blockage. K values were obtained from curve 1 of Fig. I-1. For the highest blockages considered (80% and 90%), curve 1 values (based on nominal velocity) were obtained by extrapolating curve 3 of low slope and converting the resulting $K_{max\ V}$ to $K_{nom\ V}$. This procedure gave $K_{80\%} = 31$ and $K_{90\%} = 143$ (cf $K_{90\%} = 153$ from Ref. 7).
- (d) Acceleration. $K_a = 0$ was assumed for the small heights considered in these calculations. Equating values of K_a in the high-blockage (B) and surrounding zero or low blockage (surr) regions would be slightly nonconservative since $(K_a)_B > (K_a)_{surr}$.
- (e) Tabulation. The component and total (summed) K values for each of the cases examined are tabulated in Table 2, in which HB and LB denote high blockage and low blockage, respectively.

Table 2. K Factors for Reflood Calculations

Case	% HB	K_{HB}				% LB	K_{LB}			
		K_f	K_g	K_B	K_Σ		K_f	K_g	K_B	K_Σ
I. (K_f for 0.0 in., except 5.0 in. for 0% LB)	20	0	0	1.4	1.4	0	0.32	0	0	0.32
	70	0	0	12.25	12.25	50	0.32	0	3.83	3.83
	80	0	0	31.0	31.0	60	0.32	0	6.52	6.52
	90	0	0	143.0	143.0	70	0.32	0	12.25	12.25
						80	0.32	0	31.0	31.0
II. (K_f for 11.3 in., except 16.3 in. for 0% LB)	20	0.72	1.4	1.4	3.52	0	1.04	1.4	0	2.44
	70	0.72	1.4	12.25	14.37	50	0.72	1.4	3.83	5.95
	80	0.72	1.4	31.0	33.12	60	0.72	1.4	6.52	8.64
	90	0.72	1.4	143.0	145.12	70	0.72	1.4	12.25	14.37
						80	0.72	1.4	31.0	33.12
III. (K_f for 22.5 in., except 29.5 in. for 0% LB)	20	1.44	1.4	1.4	4.24	0	1.88	1.4	0	3.28
	70	1.44	1.4	12.25	15.09	50	1.44	1.4	3.83	6.67
	80	1.44	1.4	31.0	33.84	60	1.44	1.4	6.52	9.36
	90	1.44	1.4	143.0	145.84	70	1.44	1.4	12.25	15.09
						80	1.44	1.4	31.0	33.84
IV. (K_f for 32.2 in., except 39.2 in. for 0% LB)	20	2.05	2.8	1.4	6.25	0	2.50	2.8	0	5.30
	70	2.05	2.8	12.25	17.1	50	2.05	2.8	3.83	8.68
	80	2.05	2.8	31.0	35.85	60	2.05	2.8	6.52	11.37
	90	2.05	2.8	143.0	147.85	70	2.05	2.8	12.25	17.10
						80	2.05	2.8	31.0	35.85

APPENDIX III

COMMENTS REGARDING WESTINGHOUSE'S USE OF AN h MULTIPLIER
OF 2.0 DURING BLOWDOWN

In Ref. 3, it is stated that the heat-transfer coefficient was modified by multiplying it "by a factor of two to account for entrance region heat transfer." In Ref. 10 (p. III. 48-1), the statement is made that "The factor 2.0 is used to account for flow entrance region effects and is discussed in the following references" [Refs. 22-24 of this paper].

In Ref. 22, the Sherwood modulus (N_{Sh}) was measured for $0.02 < (L/d_e) < 3.85$, and for three annulus diameter ratios and $N_{Re} = 21,000$ to $76,000$, $(N_{Sh})_x / (N_{Sh})_\infty = 2.0$ only for values of $L/d_e \leq 0.5$.

In Ref. 23, Deissler's classical analysis gives, for $N_{Pr} = 1$, $(N_{Nu})_{mn} / (N_{Nu})_\infty = 2.0$ only for $L/d \leq 0.5$.

In Ref. 24, for annulus diameter ratios of 0.19 to 0.50 and $N_{Re} \approx 40,000$, the maximum value of $(N_{Nu})_x / (N_{Nu})_\infty \approx 1.67$ at $L/d_e \approx 2$. At $L/d_e = 12.8$, the same ratio of N_{Nu} moduli = 1.06 to 1.17, depending on the annulus diameter ratio.

In addition, Fig. 4 of Sutherland's paper,²⁵ which depicts the axial variation of N_{Nu} for turbulent air flow in a 7-rod cluster, shows that $(N_{Nu})_x / (N_{Nu})_\infty = 2.0$ at $L/d_e \approx 1$.

An h multiplier of 2.0 seems excessive; only for $L/d_e = 1/2$ to 1 would such a multiplier be justified. For ratios of L/d_e which seem to be characteristic of deformed PWR rod bundles ($L/d_e \approx 13$, e.g.), a maximum h multiplier of ~ 1.2 would be more appropriate to account for entrance-region effects.

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