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A HEAT TRANSFER ANALYSIS FOR NORMAL AND EMERGENCY OPERATION OF THE ORNL PRESSURIZED-WATER LOOP

C. A. Brandon

ABSTRACT

The operational integrity of an N.S. SAVANNAH reactor type of fuel element proposed for irradiation in the ORR pressurized-water loop was investigated for a design peak heat flux of 400,000 Btu/hr·ft. The maximum cladding temperature was predicted for steady-state behavior at normal and set-point operating conditions and for the transient following a loss of power to the coolant pumps. The coolant flow coast-down relationship used in the analysis was measured.

The analysis indicated that the experimental fuel assembly would retain its operational integrity throughout the emergency conditions postulated, even under the assumption that the entire loop would behave as an adiabatic system. The cladding temperature was predicted to be 600°F ±20°F for at least 3 hr following loss of coolant flow from normal operation at approximately 500°F and 1750 psia.

CONTENTS

INTRODUCTION

The purpose of this analysis was to investigate the effect of heat transfer characteristics on the operational integrity of proposed experimental fuel assembly No. 6 during testing in the ORR pressurized-water loop. The purpose of the experiment was to extend irradiation experience with the N.S. SAVANNAH reactor type of fuel element to a heat flux greater than the design peak heat flux of 275,000 Btu/hr·ft^2. The fueled assembly was to consist of three 0.5-in.-OD rods on a 0.612-in., 60° triangular pitch. Each rod was to be filled with Spencer UO_2 by vibratory compaction and was to be cold swaged. The assembly was designed to generate a peak heat flux of 400,000 Btu/hr·ft^2.

Since the maximum cladding temperature is the primary criterion for assessing operational integrity, this temperature was calculated for steady-state operation at nominal and limiting values of coolant flow rate, temperature, and pressure. The transient behavior of the cladding temperature was also investigated for a postulated scram initiated by loss of power to the coolant pumps when all operation parameters were at or near their set points.

ANALYSIS

The pressurized-water loop[1] in the Oak Ridge Research Reactor is depicted in Fig. 1, and features of the loop essential to the analysis are shown schematically in a simplified flow diagram in Fig. 2. The fuel rods are normally cooled by turbulent convection; however, following the postulated loss of coolant flow, natural-convection boiling would begin, first in subcooled and then in saturated water.

A hydraulic equivalent diameter was used to describe the actual three-rod fuel assembly, as in the idealized system shown in Fig. 3. The resulting equivalent rod idealization, which essentially assumes good mixing at any cross section, was used because information is not available to permit a quantitative analysis of the actual geometry. The approximation should be valid while the coolant is highly turbulent, and it is probably good during subcooled boiling because the diameter of the individual fuel rods and the minimum spacing between rods is large compared

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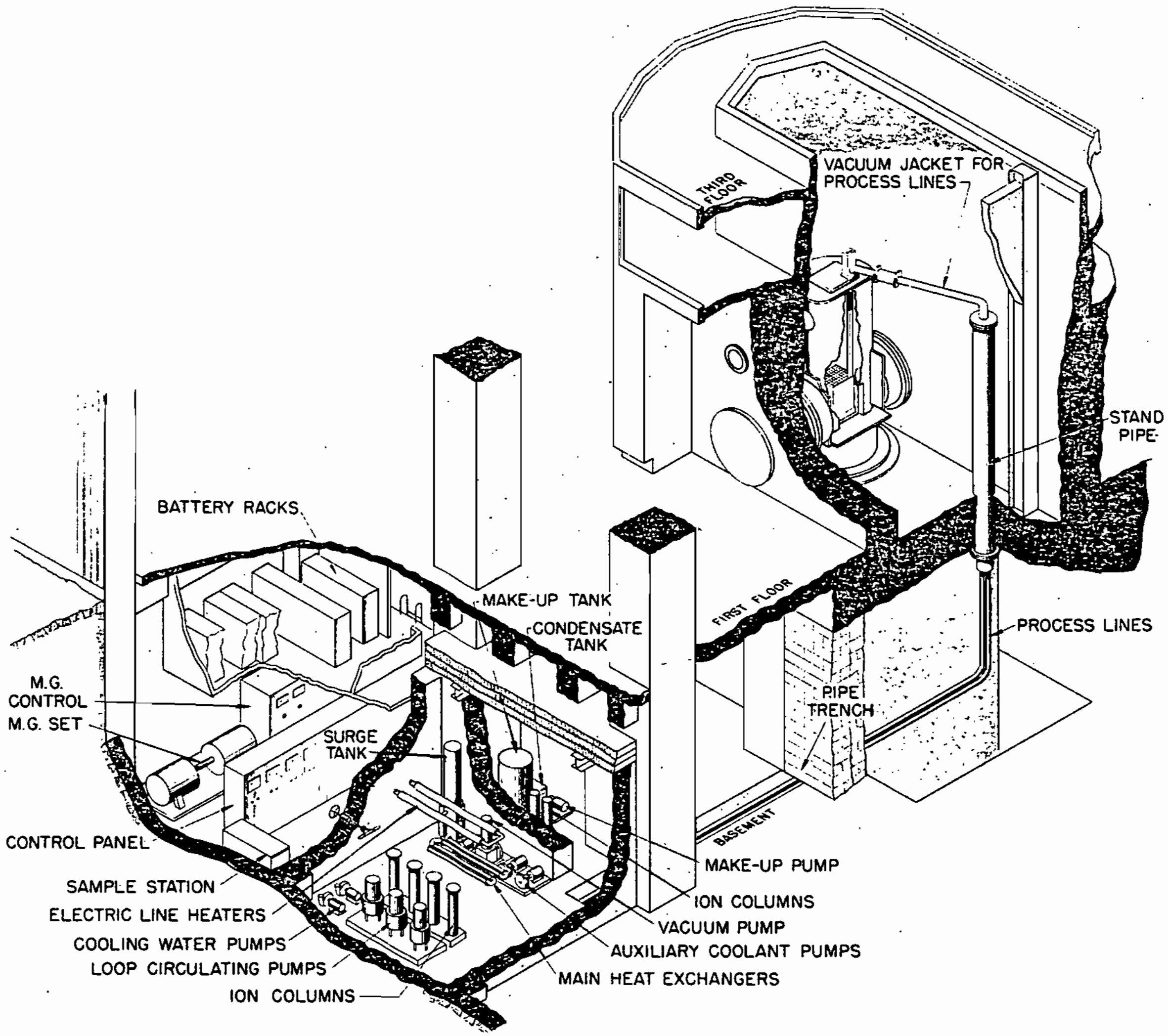

MSR Pressurized Water Loop in the ORR.

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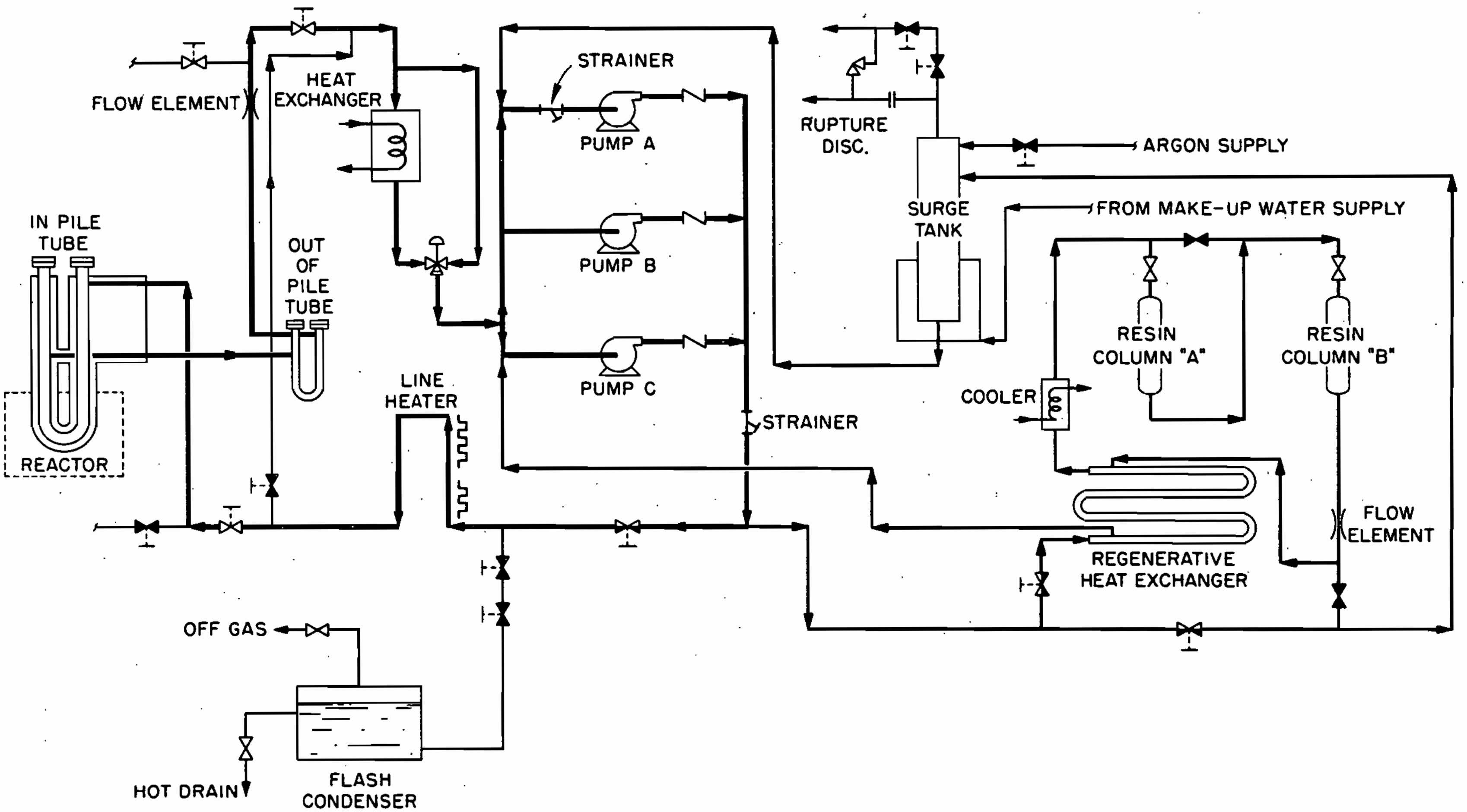

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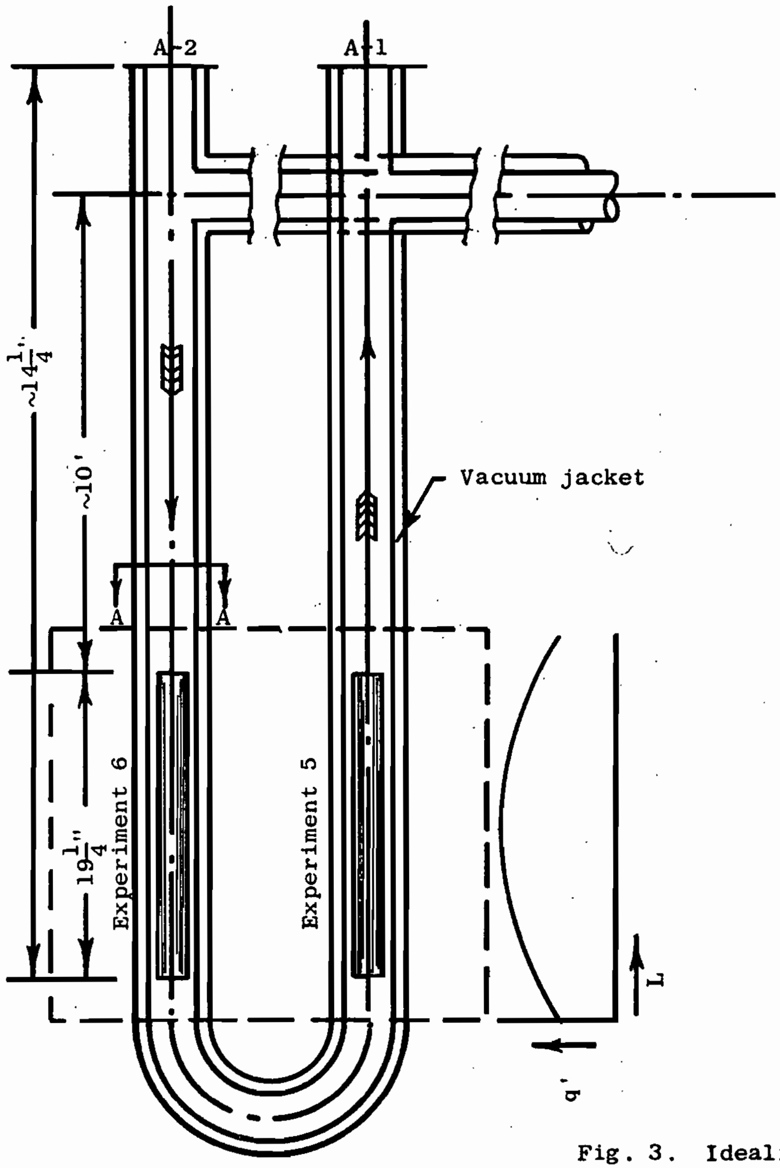

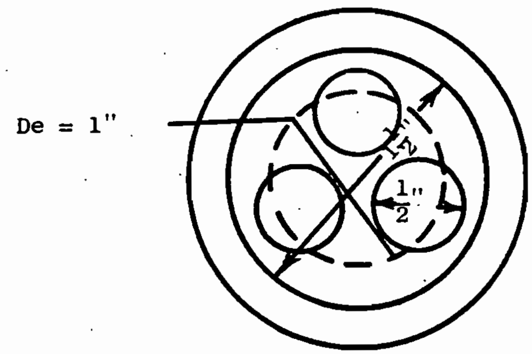

Section A-A

Assumptions:

(1) Fully developed flow

(2) Radial heat transfer only

(3) Equivalent diameter describes the geometry

(4) Geometric parameters are constants

(5) Vacuum jacket provides perfect thermal insulation

Fig. 3. Idealized System Schematic

with the size of the vapor bubbles that form and collapse at the heated surface. The actual geometry is probably an important factor, however, when natural-convection saturated boiling becomes the controlling mode of heat transfer, since the vapor bubbles formed leave the surface and grow as they rise in the bulk liquid.

Normal Loop Operation

The nominal conditions for steady-state operation (Table 1) were chosen to permit selection of reasonable ranges for each parameter while minimizing the possibility of boiling in the loop test section. The set-point operating limits on the coolant conditions are also included in the table.

Table 1. Steady-State Loop Operating Conditions

	Nominal	Set Points
Coolant		
Flow		
lb/hr	20,050	15,726
gpm	50	40
Pressure, psia	1750	1650
Inlet temperature, °F	480	500
Peak heat flux, Btu/hr·ft^2	400,000	400,000
Energy generation rate, Btu/sec	69.5	69.5

The cladding surface temperature, t_{clad}, was determined from the coolant inlet temperature, t_{inlet}, by the addition of the rise in the coolant bulk temperature, Δt_{rise}, and the local temperature potential required for heat transfer, Δt_{HT}:

$$(t_{clad}) = t_{inlet} + \Delta t_{rise} + \Delta t_{HT} \quad . \tag{1}$$

The maximum cladding surface temperature must occur near the center-plane of the fuel specimen, slightly below the position of maximum thermal-neutron flux, because of the shape of the reactor neutron flux profile.

The rise in the coolant temperature was obtained from an energy balance on the system, as sketched in Fig. 3:*

$$\int_{Li}^{L} q' \, dL = W \int_{ti}^{t} C_w \, dt_{rise} \quad . \tag{2}$$

The temperature potential required for heat transfer was found from the defining rate equation

$$(q/A) = (h \, \Delta t_{HT}) \quad . \tag{3}$$

The unit surface conductance, h, was predicted to within approximately 20% by the semiempirical relationship,[2]

$$h \simeq \frac{k}{D_e} \left[0.023 \, (Re)^{0.8} \, (Pr)^{0.33} \right] \quad . \tag{4}$$

It was also of interest to determine the cladding surface temperature at which boiling would be initiated for the operating conditions. Before boiling can commence, the surface temperature must exceed the saturation temperature of the coolant. The amount of superheat required at the surface was determined as a function of the system pressure and unit heat flux by the relationship[2]

$$\Delta t_{sat} \cong 1.9 \, (\tfrac{q}{A})^{0.25} \exp(-P/900) \quad . \tag{5}$$

The maximum ("burnout") heat flux that can be accommodated by the system with nucleate boiling was predicted as a function of the subcooling, velocity, and pressure of the coolant and the geometry of the system by the Bernath correlation[3] to within approximately 30%:**

$$(q/A)_{B_o} = \left[10{,}890 \left(\frac{D_e}{D_e + D_h} \right) + \frac{48 \, u_f}{D_e^{0.6}} \right] \left[102.6 \, \ln P - 97.1 \left(\frac{P}{P + 15} \right) - \frac{u_f}{2.22} \left(\frac{D_h}{D_e} \right)^{0.6} + 32 - t_b \right] \quad .$$

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*See Appendix A for definition of terms.

**During natural-convection pool boiling (u_f < 1 ft/sec), the vertical orientation of the fuel rods decreases the prediction of burnout heat flux to 75% of the values obtained from Eq. (6).[3]

The estimates of the heat transfer parameters obtained for steady-state operation of the loop at both the nominal conditions and the critical limits on the conditions are listed in Table 2. The physical constants used with the above relationships to obtain these results are listed in Appendix B.

Table 2. Estimated Steady-State Heat Transfer Parameters

	Nominal	Set Points
Unit surface conductance, Btu/hr·ft^2	5770	4740
Reynolds number	411,000	322,000
Peak local temperature potential for heat transfer,[a] Δt_{HT}, °F	69	85
Centerline temperature rise, Δt_{rise}, °F	5.5	6.6
Maximum cladding temperature, t_{clad}, °F	555	592
Saturation temperature, t_{sat}, °F	617	609
Degree of surface superheat, $\Delta t_{sat} = (t_{clad} - t_{sat})$, °F	Negative	Negative
Degree of subcooling, $\Delta t_{sub} = (t_{clad} - t_{inlet} - \Delta t_{rise})$, °F	131.5	102.4
Burnout heat flux,[b] $(q/A)_{B_o}$ Btu/hr·ft^2	1.6×10^6	1.14×10^6

[a]Accurate within ±20%.

[b]Accurate within ±30%.

There will be no boiling during steady-state operation. Also, in all cases the actual heat flux is less than one third the predicted "burnout flux".

Emergency Loop Operation

The transient behavior of the maximum cladding temperature was investigated for a postulated reactor scram initiated by loss of power to the coolant pumps at a time when all operation parameters were at or near their set points.

The coolant velocity and power generation would be functions of time following the postulated loss of pump power. The coast-down curve for the coolant flow was measured (Appendix C). The power generation rate was determined from consideration of fission heat, beta-particles deceleration, and gamma-ray attenuation (Appendix D). The transient cladding temperature was estimated from either the mean bulk or the saturation temperature of the water depending upon whether the controlling mechanism of heat transfer was forced convection or boiling.

The bulk water temperature at the center-plane of the fuel specimen during the coolant velocity coast-down was given by the energy balance:

$$\int_{0}^{L_{\pounds}} q'\ (L,\theta)\ dL = W(\theta) \int_{t_i}^{t_{\pounds}} C_w\ dt \quad . \tag{7}$$

The inlet temperature, t_i, was assumed to be constant, since the coolant velocity would coast down to zero before half the loop volume (approximate volume between the cooler and the test section) circulated through the test section.

After the coolant velocity reached zero in 7.5 sec, the bulk temperature would be related to the decay heat rate by,

$$\int_{7.5}^{\theta_2} q(\theta)\ d\theta = m \int_{t_i}^{t_{sat}} C_w\ dt \quad , \tag{8}$$

where θ_2 is the time at which the mass of water, m, reaches the saturation temperature and thereafter remains constant.

The unit surface conductance needed to obtain cladding surface temperature from the heat flux and bulk water temperature was estimated by Eq. (4) until the coolant velocity decreased below 1/3 ft/sec. If the cladding temperature calculated indicated surface superheat equal to or greater than that given by Eq. (5), boiling would be the controlling mode of heat transfer. The cladding temperature would then be determined from the saturation temperature of the water by Eq. (5) unless the burnout heat flux, Eq. (6), was exceeded.

Equation (8) was used to determine the water temperature as a function of time under the assumption that the water above the fuel specimen would be mixed efficiently by natural convection. If, however, the water adjacent to the fuel specimen became saturated because of negligible mixing, the vapor formed in the test section would "chug" into the subcooled water and cause quenching of the specimen. The frequency of the chugging should be low because the subcooled water would have to become saturated before additional vapor would be formed. The maximum possible amplitude of the surface temperature fluctuations would be proportional to the rate of rise in the fuel rod temperature if none of the energy generated were transferred to the water ($\sim$30°F/sec following a reactor scram).

Under natural-convection saturated boiling conditions, because of the restricted geometry of the loop, the vapor bubbles formed along the lower portion of the rod might coalesce as they rose along the elements to form "clouds" of vapor. This phenomenon, if it occurred, would result in the same sort of chugging as previously described for the subcooled case, except with a higher frequency and a correspondingly smaller amplitude, since the quenching water would already be saturated. Also, by the time saturated boiling began, the energy generation would have decreased so that the maximum rate of temperature rise would be $\sim$10°F/sec.

Once saturated boiling began, the vapor formed would displace the water into the pressurizer surge volume. As the liquid was displaced, the vapor would contact metal pipe surfaces at $\sim$110°F and be condensed. Because of the high rate of heat transfer from condensing vapors, the exposed metal would be rapidly heated to approximately the saturation temperature. If it is assumed that the exposed metal is heated, Δt_m, and that the vacuum jacket provides perfect insulation, an energy balance on the system yields the net vaporization:

$$\int_0^m dS = \left(h_{fg} + \frac{\nu_v \, m' \, C_m \, \Delta t_m}{A_f} \right)^{-1} \int_{\theta_2}^{\theta_3} q \, d\theta \quad . \tag{9}$$

Equation (9) would be applicable until either (1) all the water around the elements was vaporized or (2) the surge volume was filled. The water level above the test section at time θ would be obtained from Eq. (9) as,

$$-\int_{L_i}^{L} dL = \frac{\nu_f}{A_f} \int_0^m dS \quad , \tag{10}$$

and the surge volume remaining at time θ would be calculated from Eq. (9) as,

$$-\int_{V_i}^{V} dV = 1.75 \, \nu_v \int_0^m dS \quad . \tag{11}$$

The vapor formed in both in-pile legs of the loop displaces liquid into the surge volume, and therefore Eqs. (9) and (11) would have to be modified to account for the presence of experimental assembly No. 5. A conservative estimate of the time required to fill the surge volume was obtained by multiplying the right-hand side of Eq. (11) by 1.75, since the energy generation rate of experimental assembly No. 5 was to be $<75\%$ of that of experimental assembly No. 6.

The estimates of the heat transfer parameters obtained for transient operation of the loop following the postulated loss of coolant pump power are shown in Figs. 4 and 5. The physical constants used with the above relationships to obtain these results are listed in Appendix B.

CONCLUSIONS AND RECOMMENDATIONS

The heat transfer characteristics of experimental assembly No. 6 operating in the ORR pressurized water loop, as estimated by this analysis, indicate that the integrity of the assembly would be assured even under the conservative assumption of an adiabatic system. The predicted maximum cladding temperature is 600°F $\pm$20°F under emergency operating conditions.

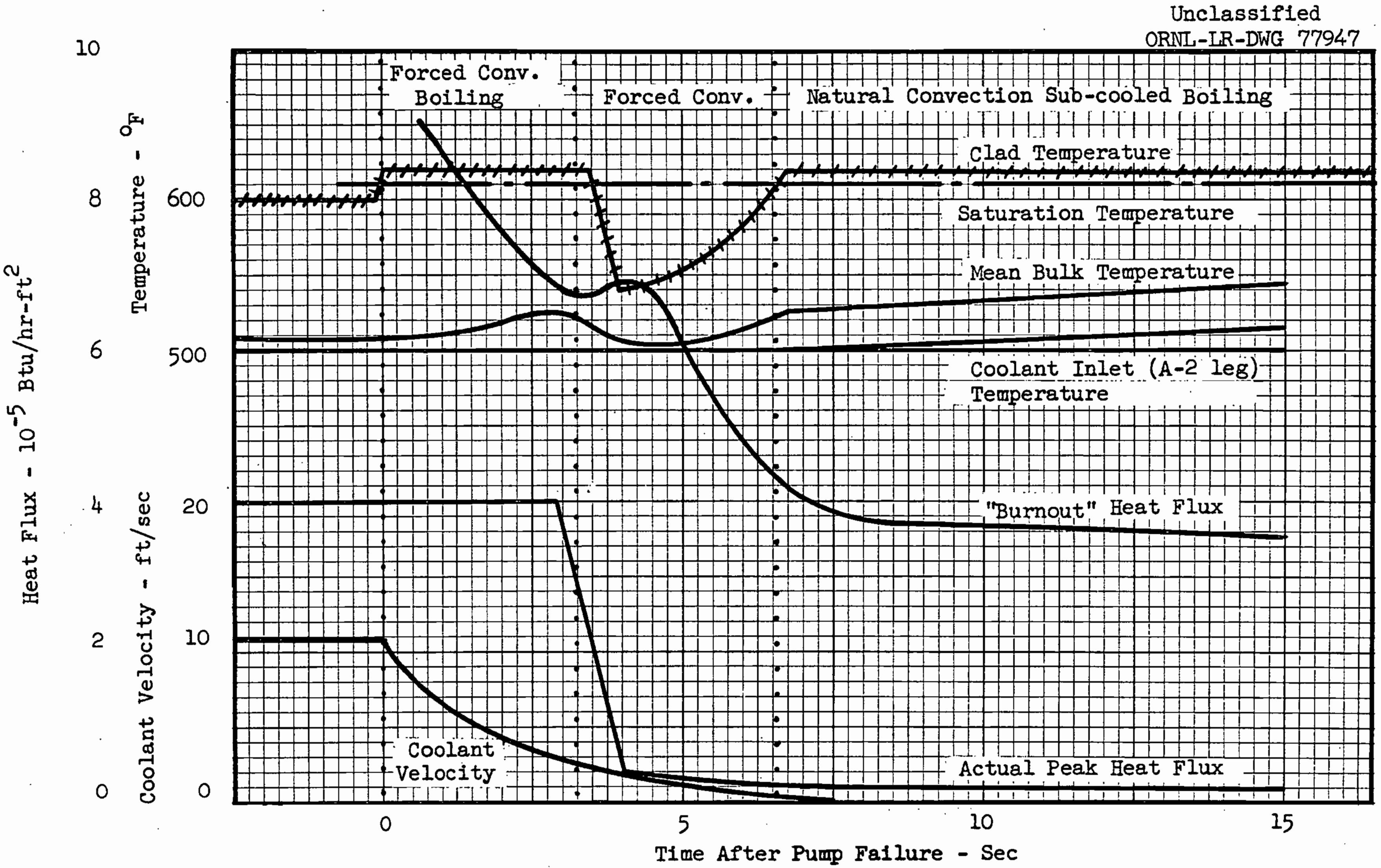

Fig. 4. Transient Behavior of Assembly No. 6
Following Loss of Power to Coolant Pumps

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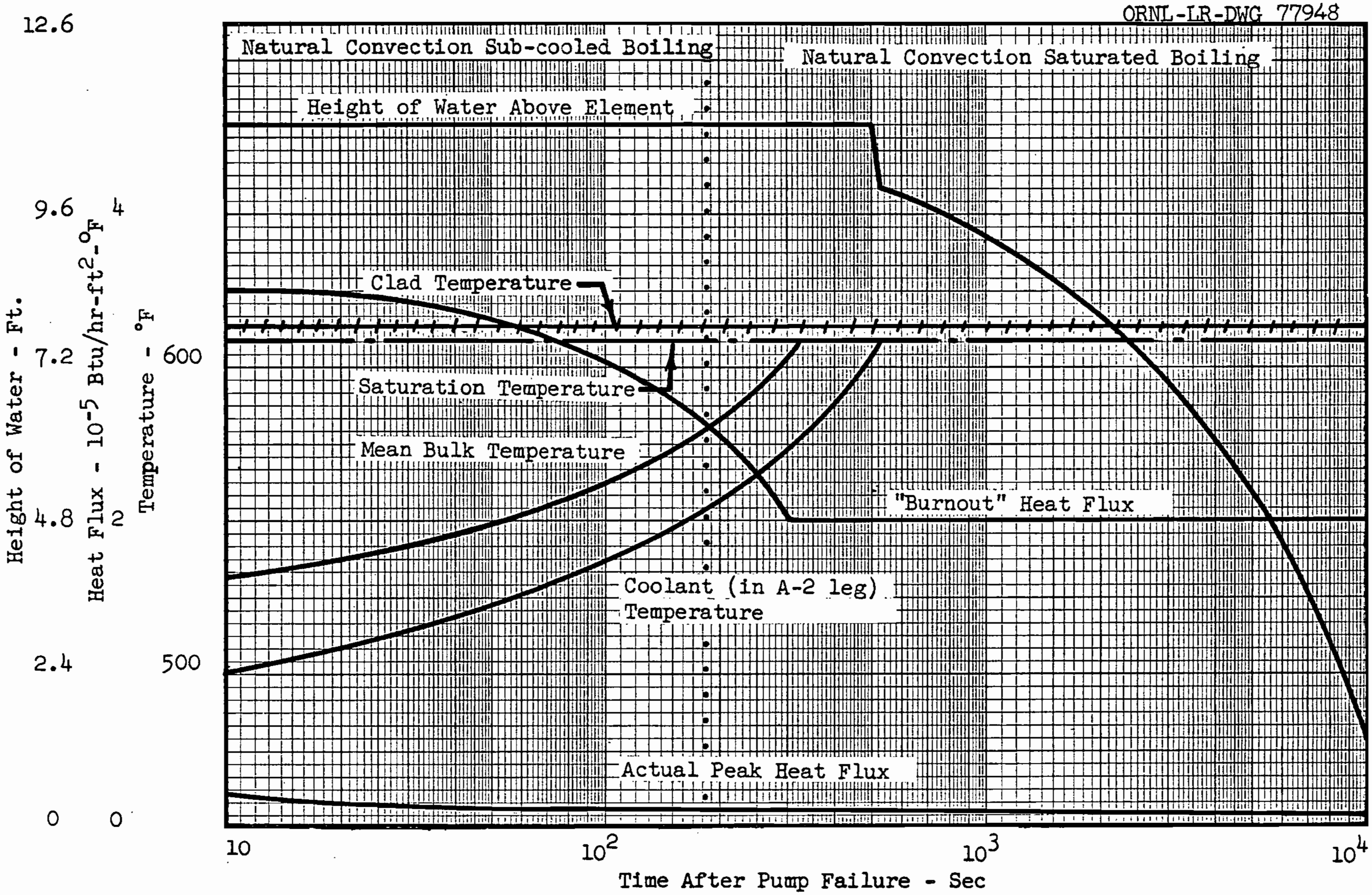

Fig. 5. Transient Behavior of Assembly No. 6
Following Loss of Power to Coolant Pumps

Only nucleate boiling should occur during the postulated emergency, since the actual heat flux will always be less than the estimated "burnout" heat flux. The heat losses through the vacuum jacket and an emergency procedure for filling it with helium would prevent the system from attaining the extremes determined by this analysis.

While the integrity of the assembly is assured, the effect of possible unstable boiling (chugging) should be determined. It is recommended that out-of-pile heat transfer tests be used to evaluate the damage that might be done by chugging.

APPENDIX A

Nomenclature

A' = surface area, ft^2/ft; A_f = flow area, ft^2

A = surface area, ft^2

C = specific heat, Btu/lb·°F

D_e = equivalent diameter $\equiv 4A_f/P'_w$, ft

D_h = equivalent heater diameter $\equiv P'_h/\pi$, ft

G = specific mass flow rate, lb/hr·ft^2

h = unit surface conductance, Btu/hr·ft^2·°F

h_{fg} = heat of vaporization, Btu/lb

k = thermal conductivity, Btu/hr·ft^2(°F/ft)

L = length along fuel elements or height of water above elements, ft

m = mass of coolant in A-2 leg, lb

m' = mass of metal per ft of pipe

P = system absolute pressure, psia

P'_w = wetted perimeter, ft

P'_h = heated perimeter, ft

Pr = Prandtl number $\equiv C\mu/k$

q = thermal energy generation rate, Btu/hr

q' = Btu/hr·ft

Re = Reynolds number $\equiv GD_e/\mu$

S = water vapor, lb

t = temperature, °F

u = water velocity, ft/sec

V = volume, ft^3

W = water flow rate, lb/hr

θ = time, sec

μ = viscosity, lb/hr·ft

ν = specific volume, ft^3/lb

Subscripts

b	=	bulk, mixed mean
B_o	=	burnout
f	=	fluid
i	=	denotes initial or inlet
℄	=	centerline or center-plane
m	=	metal
sat	=	indicates saturate conditions
sub	=	indicates subcooling
v	=	vapor
w	=	water

APPENDIX B

Physical and Dimensional Constants

A'	$= 0.393\ ft^2/ft$ of total heated area
A_f	$= 0.00817\ ft^2$
C_w	$= 1.17$ Btu/lb·°F for normal operation
	$= 1.20$ Btu/lb·°F at set points
	$\cong (0.003t_b - 0.3)$ Btu/lb·°F
C_m	$\cong 0.10$ Btu/lb·°F
D_e	$= 0.041$ ft
D_h	$= 0.125$ ft
h_{fg}	$= 530$ Btu/lb
k	$= 0.360\ Btu/hr \cdot ft^2(°F/ft)$ for normal operation
	$= 0.354\ Btu/hr \cdot ft^2(°F/ft)$ at set points
L	$= 10$ ft of water above element
	= 16-1/4-in. fueled length
m	$= 7.14$ lb total water in A-2 leg
	$= 5$ lb water above element in A-2 leg
m'	$= 3.6$ lb/ft
Pr	$= 0.796$ for normal operation
	$= 0.83$ at set points
Δt_m	$= 80°F$ (assumed)
V	$= 2\ ft^3$ of surge volume
μ	$= 0.245$ lb/hr·ft
ν_v	$= 0.245\ ft^3/lb$ at 1650 psia
	$= 0.227\ ft^3/lb$ at 1750 psia
ν_f	$= 0.0200\ ft^3/lb$ at 480°F
	$= 0.0204\ ft^3/lb$ at 500°F

APPENDIX C

Flow Coast-Down in Loop

The flow coast-down, following loss of power to the coolant pumps, in the ORR pressurized-water loop, was measured by D. E. Tidwell and G. W. Greene. The measurements were taken on August 31, 1961, with the experimental assemblies Nos. 4 and 5 in place. A high-speed Swartwout differential pressure transmitter and a Sanborn recorder were used to obtain the pressure drop across the flowmeter orifice used in the system. Fig. C-1 is a plot of a typical result of a data run. The measured differential pressure was converted to flow (in gpm) by J. A. Conlin on September 1, 1961. The resulting flow coast-down curve is given in Fig. C-2. The coast-down data were used to obtain the coolant velocity curve shown on Fig. 4.

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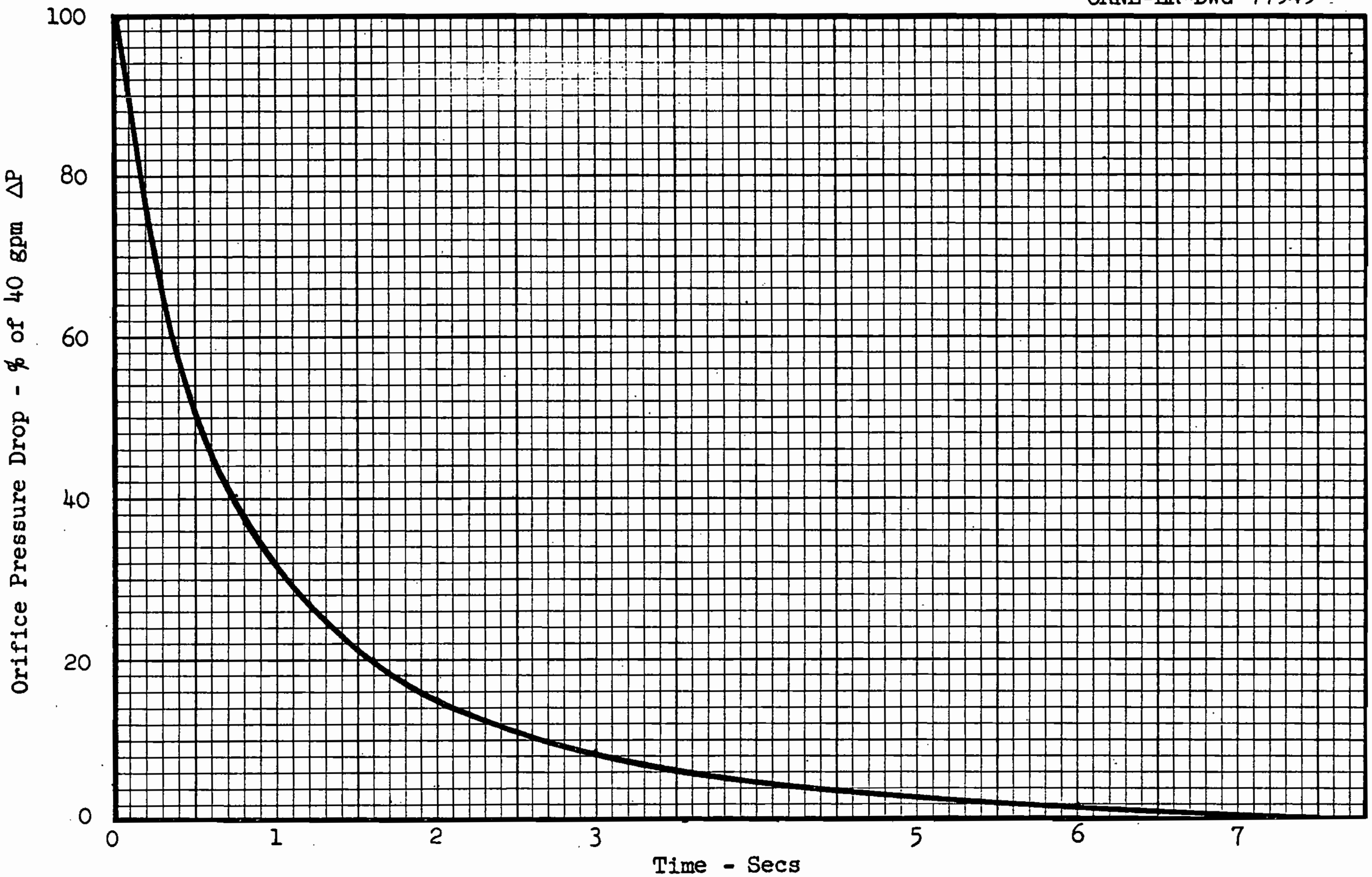

Fig. C-1. Typical Data for Orifice Pressure Differential vs Time Following Loss of Power to ORR Pressurized-Water Loop Coolant Pumps

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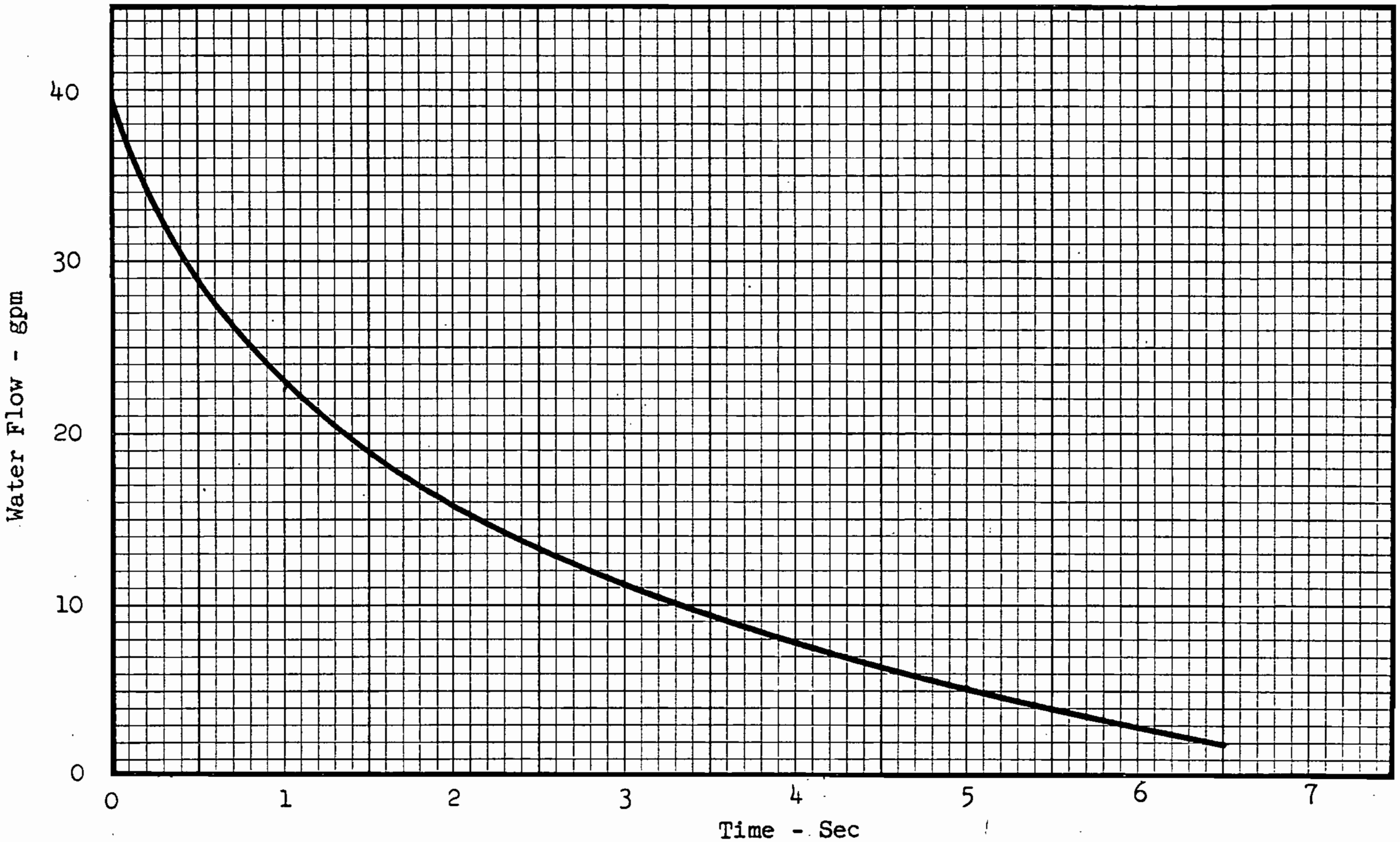

Fig. C-2. Flow Coast-down Following Loss of Power to ORR Pressurized-Water Loop Coolant Pumps

APPENDIX D

Thermal Energy Generation Rate

The total thermal energy generated in an inpile fueled experiment is considered to be generated either by fissioning of the fuel or by absorption of gamma rays. The thermal energy generated by gamma-ray absorption is assumed to be proportional to the mass, regardless of the material. The mass of material in assembly No. 6 and the "gamma heat" or power (assuming 3.75 w/g)[4] will be:

Material	Mass (g)	Power (kw)
UO_2 (fuel)	1130.0	4.24
Element structure and cladding	377.5	1.42
Loop pressure piping	2480.0	9.3
Water (coolant)	378.0	1.42
Total	4365.5	16.38

The total power of the fuel element was designed to be 62.50 kw. The fission heat was therefore

$$\text{fission heat} = 62.50 - 5.66 = 56.84 \text{ kw} \quad .$$

The total thermal energy generated in assembly No. 6 would be the sum of the fission and gamma heat, or

$$q = 16.38 + 56.84 = 73.22 \text{ kw or } 69.5 \text{ Btu/sec} \quad .$$

Following a scram, the greatly reduced heat generation rate was considered to include the energy of the beta particles from the fission-product decay in the fuel, as well as fission heat and gamma-ray absorption. The contribution of each source after 1 sec following a scram was estimated to be:

Fission heat	4% of full power fission heat
Gamma heat	33% of full power value
Beta heat	4% of full power fission heat

The thermal energy generation rate in assembly No. 6, 1 sec after a scram, was estimated to be

$$q = (56.84)(0.08) + (16.38)(0.33) = 10 \text{ kw or } 9.4 \text{ Btu/sec} \quad .$$

The heat generation rate was assumed to vary linearly during the first second following a scram. The decay heat is a function of time. From ref. 6, the time variation 1 sec after a scram is

$$q(\theta) = 9.4\ (\theta^* - 1)^{-0.2} \quad .$$

The peak heat flux for assembly No. 6 was designed to be 400,000 Btu/hr·ft^2. Following a scram, the heat flux would drop by the same ratio as the energy generation in the fuel element:

$$(q/A) = \frac{(56.84 \times 0.08 + 5.66 \times 0.33)}{62.50} \times 400{,}000 \text{ Btu/hr·ft}^2$$

$$= 42{,}800 \text{ Btu/hr·ft}^2 \quad .$$

The peak heat flux has the time variation of the generation rate:

$$(q/A) = 42{,}800\ (\theta^* - 1)^{-0.2} \quad .$$

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