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MEMORANDUM TO FILE

FROM: L. BERNATH *L.B.*

SUMMARY REPORT ON
COLUMBIA UNIVERSITY HEAT TRANSFER STUDIES

INTRODUCTION

Experimental investigations were undertaken at Columbia University to determine the heat transfer and fluid flow characteristics of fuel elements for the Savannah River reactors. Primarily the tests simulated quadrefoil operation, but a number of tests directly applicable to the operation of extended surface fuel elements were performed. Data books (44-50) were published in which the experimental data are tabulated.

SUMMARY

The purpose of this summary is to make available to the engineer all the data and results from the Columbia work in a concise and useful form. The results of the Columbia studies are reviewed and are applied to problems associated with the operation of the Savannah River reactors. In this discussion there has been no attempt to reproduce all of the Columbia material; where phases of the study have been adequately reviewed in earlier communications, the results are mentioned and references to detailed discussions are cited.

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The phases of the work at Columbia University included:

- 1) an experimental study of heat transfer by forced convection of water in concentric annuli,
- 2) experimental and analytical studies of the heat transfer and pressure drop through the coolant annulus with boiling, for both obstructed and unobstructed annuli,
- 3) an experimental study of the maximum safe power for a slug column, including the effects of high rod power on
 - a) sheath temperatures and the initiation of local boiling,
 - b) the mechanism of thermal failure, i.e., flow instability and local boiling burnout, and
 - c) the rate of corrosion of 2S aluminum in flowing, boiling water of reactor purity,
- 4) an experimental study of the power dissipation characteristics of wide (1/4 and 3/8 inch) annuli, simulating tubular fuel elements fed by increased coolant flows from the new Bingham pumps,
- 5) a photographic study of boiling in annuli, and
- 6) an experimental study of the mechanism of natural convection cooling with boiling.

I. Description of Columbia Apparatus

The apparatus assembled by Columbia consisted of a hydraulic loop, an electrical loop, instrumentation, and a main test section of annular geometry. The hydraulic loop was composed of storage tanks, pumps, dearator vessel, deionizer, heat exchangers, and the test section with by-pass paths. The electrical loop consisted of a synchronous motor-driven, D.C. generator and bus bars to connect to the heater tube in the test section. With aluminum as the heater element, a maximum current of 22,000 amperes permitted a power generation of 1300 KW in the test section. The instrumentation was an assemblage of pressure gages, recorders and controllers, orifice meters and differential pressure recorders and controllers, resistance thermometers and thermocouples with associated rapid-response recorders, and conductivity cells to measure the conductivity and gas content of the water. Additional control instrumentation was required for the D.C. generator.

The test section consisted of a seamless aluminum heater tube, 1.08 inches o.d. to simulate a column of slugs,

inserted into a quarter-section of a quatrefoil, coated internally with plastic for electrical insulation. The plastic insulation proved incapable of preventing arcing from the heater to the housing because of blistering at the high temperature. A thick-walled aluminum tube, broached longitudinally to accept three fiberglass, silicone-plastic ribs, was found to perform satisfactorily. Three such housing tubes were prepared to provide annuli of $1/8$, $1/4$, and $3/8$ inch after the heater tube was inserted. To permit measurement of wall temperatures of the heater tube (slug sheath temperatures), a thermocouple-bearing rod was inserted into the heater tube and the thermocouple beads were spring-loaded to bear firmly on the inner surface of the tube. The thermocouples were spaced at 14-inch intervals along the rod and by rotating the rod, circumferential temperature measurements were made.

Heater tubes consisted of uniform and varying wall thicknesses to provide uniform and cosine distributions of heat flux. A roof-top distribution was obtained by welding together selected sections of cosine tubes.

Flow to the test section and the by-passes came from an inlet header, through a metering orifice and flow restrictor (simulating the piston orifices). The effluent coolant passed through a simulated end-fitting containing five holes (one-fourth of the twenty holes in a quatrefoil end-fitting), into a quench tank, through which flowed the coolant from the by-passes. The quench tank was connected to a standpipe, whose fixed length established the pressure in the quench tank, simulating conditions in the reactor in the region of the quatrefoil end-fittings.

The temperature of the coolant entering the annulus was regulated by controlling the cooling water flow to the heat exchangers in the flow loop. Resistance thermometers measured the temperature of the coolant in the various regions of the flow loop.

II. Review of Columbia Test Results

A. Forced Convection Liquid Heat Transfer in Concentric Annuli

Local heat transfer coefficients were calculated from the data obtained during experiments simulating the operating behavior of reactor fuel channels. These data were analyzed and about 900 values of heat transfer coefficient were correlated by Columbia University personnel.⁽¹⁾

The ranges of the conditions represented by these data are:

Annulus widths, inch	1/8, 1/4, and 3/8
Heat flux distribution	Cosine and Uniform
Heat flux, Btu/hr-ft ²	22,000 to 625,000
Coolant velocity, ft/sec.	4 to 45
Film temperature, °C	28 to 133
Heat transfer coefficient, Btu/hr-ft ² -°C	1,200 to 12,700

By using the Columbia correlation and adapting it to account for the physical properties of D₂O, we obtain the following expression -

$$\frac{h D_e^{0.2}}{v^{0.8}} = 177 (1 + 0.0106 t_f) \left(\frac{D_2}{D_1}\right)^{0.6} \quad (1),$$

which is the recommended equation for D₂O based on the Columbia data for concentric annuli. When using this equation, the velocity should be based on a D₂O density of 69 lbs./ft³.

Although Columbia personnel presented their correlation in the form -

$$\left(\frac{h}{C_p G}\right) \left(\frac{C_p \mu}{k}\right)_f^{2/3} \left(\frac{D_e G}{\mu}\right)_f^{0.2} \left(\frac{D_1}{D_2}\right)^{1/2} = 0.0200 \pm 10\% \quad (2),$$

a critical review of the results reveals that the incorporation of the diameter ratio to the one-half power does not adequately describe the relationship between the heat transfer coefficient and the annulus width. The data presented (2) conform more precisely to the following expression:

$$\left(\frac{h}{C_p G}\right) \left(\frac{C_p \mu}{k}\right)_f^{2/3} \left(\frac{D_e G}{\mu}\right)_f^{0.2} \left(\frac{D_1}{D_2}\right)^{0.6} = 0.0190 \pm 10\% \quad (3)$$

Equation (3) may be simplified by considering the combined physical properties of water to be a linear function of the film temperature. The revised correlation in this form reduces to -

$$\frac{h D_e^{0.2}}{v^{0.8}} = 185 (1 + 0.0098 t_f) \left(\frac{D_2}{D_1}\right)^{0.6} \quad (4)$$

For the temperature range 32° to 132°C., this equation differs from Equation (3) by only 3%, when the velocity is based on a density of 62.3 lbs./ft³.

The equation that was used in the design of the Savannah River Plant reactors⁽³⁾ is

$$\frac{h D_e^{0.2}}{V^{0.8}} = 147 (1 + 0.016 t_f) \quad (5),$$

in which the effect of film temperature on the heat transfer coefficient is 63% greater than in Equation (4).

To apply Equation (4) to reactor problems, a correction must be made for the different physical properties of D₂O and water. The magnitude of this correction varies with film temperature:

Film Temp. - °C	$\frac{[h D_e^{0.2} V^{-0.8}]_{D_2O}}{[h D_e^{0.2} V^{-0.8}]_{H_2O}}$
15	0.967
35	0.983
80	0.997
100	1.005

If this correction for the properties of D₂O is combined with Equation (4), we obtain Equation (1) above. Equations (1) and (5) are compared graphically in Figure 1; the previously recommended Equation⁽⁴⁾ for D₂O, based on preliminary data, is also presented.

B. Photographic Study of Boiling in Annuli

Three full scale experiments were made at Columbia to study boiling in the annulus using a composite housing tube, the lower section of which was glass. Two of these were successful, one each in a concentric⁽³⁵⁾ and an eccentric⁽³⁶⁾ annular geometry. Figures 8 and 9 present graphically the variation in the distribution of bubbles (and, therefore, saturated coolant) with length along the channels, and in tabular form corresponding values of heat flux, coolant saturation temperature, and calculated bulk temperature of the coolant.

In the concentric annulus, boiling is initiated at the ribs and occupies more of the channel cross-section, in a symmetrical pattern, as the bulk temperature approaches the boiling point of the coolant. Actually, as shown in Figure 8, the channel becomes filled with bubbles when the bulk temperature reaches the saturation temperature of the coolant. During this series of runs, the gas content of the water was varied from 0.03 to 2 ppm of dissolved air; it was concluded that the amount of dissolved gas in the water had no significant effect on the thermal characteristics of the channel. From the photographs the formation and growth of bubbles were observed and

very large bubbles were seen to travel along the tube, sometimes merging so that water and vapor appeared as alternate slugs. The approach to instability was seen as bulk boiling began in the lower section of the annulus and proceeded upstream as the flow decreased. As instability began, a milky film formed in the annulus and the heater tube melted with a flash of white light.

In the eccentric annulus, boiling began along the small rib, i.e., in the narrowest part of the annulus. (Note that the ribs in the Columbia apparatus were displaced through an arc of about 30° from the quatrefoil configuration; this was done to obtain reproducibly positive placement of the heater tube in the annulus.) The operating condition represented in Figure 9 is particularly noteworthy in that the two small channels were full of bubbles (saturated coolant) at the point where boiling began in the large channel and under this condition, the coolant leaving the annulus was below its boiling point. High-speed motion pictures of the boiling in a small channel showed that very little cross-flow occurs between channels through the vents in the ribs and that very little mixing occurs within each channel. This statement must apply only to the region of local boiling since numerous experiments have shown that the rib vents adequately interconnect the channels during bulk boiling - no pressure differential between the channels.

Further operation of this setup was prevented by loosening of the ribs which had been glued in place with an epoxy resin. It is believed that the heat from the floodlights (used for photography) melted the glue.

Motion pictures were also taken of local boiling burnouts in the small scale apparatus (37). Two runs were made with 1/4-inch concentric annuli, one with plastic ribs and one without ribs. Both burnouts showed the formation of a vapor patch at the site of burnout prior to failure. An interesting feature of the burnout in the ribbed experiment was that the melting of the heater tube did not occur under the ribs.

C) Pressure Drop with Boiling in the Annulus

Experiments were undertaken to determine the pressure drop, through a simulated quatrefoil annulus, with boiling of the coolant. The annulus pressure drop with boiling was measured for a range of coolant flows and power levels in both 1/8 and 1/4 inch concentric annuli. These data are presented in Figure 2, plotted in dimensionless form to permit direct comparison.

Columbia personnel have derived empirical correlations⁽⁵⁾ that can be used to predict the inlet pressure versus flow characteristics of a slug column with boiling of the coolant.

One correlation is used to predict the pressure drop through the end fitting orifices and another then predicts the pressure drop through the annulus. This method of calculation yields a value of the required inlet pressure for each operating condition, i.e., each combination of coolant flow, rod power, and inlet temperature. Thus, a series of calculations must be performed if a complete pressure demand characteristic, inlet pressure versus flow for a range of flows, is to be obtained for each power level and inlet temperature. These correlations apply only to the 1/8-inch annulus and associated end fitting.

The data presented in Figure 2 were used to derive a generalized correlation based on the ratios of the actual to normal coolant flows (F/F_o) and pressure drops ($\Delta P_f/\Delta P_{f_o}$). The correlating parameter is the ratio of the rod power at the operating condition to that required to cause the effluent coolant to attain its saturation temperature at the ambient pressure outside the end fitting (P/P_{BT}). The data points in Figure 2 are seen to form a series of straight lines of negative slope (except in the region where boiling starts). A remarkable feature of this family of lines is that their slopes are all equal; the measured slopes lie in the range 3.27 to 3.34. The intersections of these straight lines, representing the two-phase region, with the liquid pressure drop curve are related to the parameter (P/P_{BT}) as follows: for a chopped cosine flux distribution,

$$(F/F_o)_{CI} = 0.041 + 0.90 (P/P_{BT}) - 0.156 (P/P_{BT})^2 \quad (6)$$

and for a uniform flux distribution,

$$(F/F_o)_{CI} = 0.041 + 0.90 (P/P_{BT}) - 0.20 (P/P_{BT})^2 \quad (7)$$

in which the subscript "C" refers to the coolant passage associated with one fuel rod of a quatrefoil.

The minimum point in the pressure demand characteristic does not lie at the calculated point for the start of boiling. It had been postulated that this "rounding-off" of the curve was due to the presence of dissolved gases in the coolant. At Columbia, several runs were made under identical operating conditions with aerated and deaerated coolant; no difference between them could be detected in the pressure drops either in the annulus or across the end fitting orifices.

The correlation above holds as well for the 1/4-inch as for the 1/8-inch concentric annulus, but has not been tested for validity in the case of the eccentric, 1/8-inch annulus. Therefore, for concentric annuli and low (~ 20 psig) pressure at the coolant exit, it may be assumed that the pressure demand characteristic at any given rod power for all concentric annuli (specifically in the range of 1/8 to 1/4 inch) can be constructed by calculating the intersection from Equation (6) or

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1. Simulated Quatrefoil - 1/8-Inch Annulus

The Columbia experiments have demonstrated that the maximum power generation in the quatrefoil is limited to that value at which the flow becomes unstable. Therefore, any rod power less than that required to raise the effluent coolant to its saturation temperature is a safe operating level.

The onset of flow instability can be estimated from a comparison of the pressure demand characteristic of the annulus (with boiling) with the pressure supply curve based on the available plenum pressure (see Figure 2). Methods of computing the demand characteristic were presented in Section I, C. Complete discussions of the plenum pressure required to avoid flow instability and the bases for inlet orificing have been presented.^(6,7)

The results of the Columbia tests simulating the quatrefoil are summarized in four graphs. Figure 3 presents the effect of increasing rod power on the flow of coolant. With concentric placement of the fuel rod in the annulus, flow increases with rod power initially (coolant viscosity decreases as the bulk temperature increases) and then decreases as boiling begins near the ribs, returning very nearly to its initial value at the power required to cause bulk boiling in the end fitting (P_{BB}). At rod powers above P_{BB} , flow decreases rapidly as the unstable flow condition is approached. Comparison of Curves E, F, and G of Figure 3 reveals the influence of plenum pressure on the decrease in flow. With increasing pressure available to be dissipated across the orifices, higher rod powers are attained before the flow becomes unstable. Curves B, C, and D for eccentric fuel rod placement show the same influence of plenum pressure. These curves also show the effect of boiling in the narrow channels on the coolant flow. The eccentricity of the heater tube used in these tests was selected⁽³⁾ to represent the maximum dislocation of the slug column likely to occur in the quatrefoil. Therefore, the flow behavior of a fuel column in the SRP reactors most probably lies between the two groups of curves in Figure 3. Curve A in this figure shows that an excess (14 ppm.) of air dissolved in the coolant does not significantly change the flow (and, hence, pressure drop) characteristics of the fuel annulus.⁽⁹⁾

The effect of plenum pressure on the rod power at instability is summarized in Figure 4. The ratio of the rod power at instability to that required to cause bulk boiling in the end fitting (P_{BO}/P_{BB}) is plotted against the orifice ratio, defined as the ratio of the pressure drop across the top and bottom orifices to that through the annulus, evaluated at the normal rate of coolant flow. Curve A, for the concentric fuel column, shows that increasing the orifice ratio causes a significant increase in the rod power at burnout. With eccentric annuli, Curve B, this gain is much less pronounced. Curve C, for the unvented, eccentric annulus, shows a more rapid approach to

instability than the other two configurations at low values of the orifice ratio, due primarily to the boiling of coolant in the narrow subchannels of the annulus. At higher values of the orifice ratio, Curve C approaches Curve B due to the deformation of the plastic ribs at the high surface temperatures and under the influence of the pressure difference (about 50 psi) between the subchannels.

With no orifice pressure drop, flow instability should occur as soon as bulk boiling begins. With vented ribs (notched to simulate the rounded edge on the bottom of the fuel slugs), no pressure difference can exist between subchannels. The quenching of vapor formed in the small subchannels in the large one permits operation up to the rod power that causes bulk boiling at the outlet of the large subchannel. For this reason Curves A and B converge at low values of orifice ratio.

Eight of the experiments, whose results are presented in Figure 4, were made with some bottom orificing, i.e., the pressure drop available for dissipation through orifices was redistributed to provide greater than normal resistance at the annulus outlet. These tests have shown⁽¹⁰⁾⁽¹¹⁾ that operation with bottom orificing does not reduce the maximum rod power attainable in the quatrefoil channel. For eccentric placement of the fuel column, it has been shown that bottom orificing permits operation at higher rod powers than for top orificing and that the rod power at burnout is essentially that for the concentric geometry.⁽¹⁹⁾ It is reasoned that this increase in maximum rod power is a direct result of the suppression of boiling in the narrow subchannels of the annulus through the increase in ambient pressure and hence, saturation temperature.

The effect of heat flux distribution upon the rod power to cause instability has been studied.⁽¹²⁾⁽¹³⁾ Both rooftop and uniform heat fluxes have been compared with the cosine distribution. In neither case did the heat flux distribution significantly alter the power at which flow instability occurred. At lower rod powers, differences in the operating characteristics of the fuel column were observed; however, each of these, e.g., sheath temperature, occurred in the manner predicted based on the distribution of heat flux.

The effect of rod power on the end fitting pressure drop is presented in Figure 5. The curves all show a large increase in the end fitting pressure drop at rod powers above that required to cause boiling of the effluent coolant. There is no apparent relation between the orifice ratio and the rate of increase of the end fitting pressure drop for the concentric annulus but a strong effect is apparent for the eccentric fuel column. These results have been applied to SRP flow monitoring problems and are discussed more fully in the following.

The effect of rod power on the maximum sheath temperature is presented in Figure 6. These data are for the fuel column cooled by H_2O and heated with a cosine distribution of

flux. The data show that the concentric geometry (curve C) has the lowest maximum sheath temperature and that rib venting in this case does not further reduce this temperature. At rod powers above 600 KW, rib venting materially lowers the maximum sheath temperature with the eccentric fuel column (compare Curves A and B). From these data it appears that flow instability occurs after the maximum sheath temperature exceeds 200°C. With D₂O as the coolant, a given sheath temperature should occur at a rod power between 3 and 10% greater than the corresponding power with H₂O. Thus, values for D₂O read from Figure 6 are conservative.

2. Simulation of Fuel Elements with Wide Annuli

All of the experiments conducted with 1/4 and 3/8 inch annuli were terminated by local boiling burnout. The data obtained from these tests have been summarized by Columbia(9,11,13,14,28) and have been analyzed by the writer. A theory of the mechanism of burnout with local boiling has been proposed and an analytical method of prediction of the onset of this mode of heat transfer failure has been developed.(15) Comparison with available data has shown the method to describe the burnout phenomenon over a wide range of experimental variables. Thus, the effect of heat flux distribution, velocity, temperature, and pressure of the coolant and of the geometry of the fuel element on the maximum safe operating power can all be predicted.

These experiments at Columbia have demonstrated clearly that fuel elements composed of coolant annuli 1/4 inch or greater in width do not fail through flow instability even at very low values of plenum pressure. It has been estimated that elements with annulus widths greater than 0.17 to 0.18 inch will always fail through local boiling burnout. Therefore, for proposed fuel elements with annuli between 1/8 and 1/4 inch, the design must be tested by both the instability method described in Section I, C and the local boiling prediction to determine the controlling mechanism of failure.

E. Small Scale Heat Transfer Studies

A small scale, heat transfer apparatus,(14)(16) adapted from equipment developed for the study of the corrosion of aluminum, was used to supplement the data obtained from full scale experiments. This apparatus was used to study the effects of the variables, e.g., coolant velocity, temperature, and pressure, annulus width, etc. on the heat flux at burnout. The results(9,11,13,14) generally confirmed the local boiling conditions necessary for burnout that had been observed in the full scale equipment. However, because of the short heated length (in some cases only 2 inches long), and the thin wall thickness (0.005 to 0.011 inch) of the heated specimens used,

these data were less reproducible than those from the full scale tests. However, enough confirmed data were obtained to validate the method of burnout prediction.⁽¹⁵⁾

Comparison of the predicted burnout heat flux with the results of these experiments revealed a measure of "conservatism" in the prediction method; i.e., the predicted heat flux at burnout frequently was less by 10 to 20% than the experimental observation. This apparent conservatism is to be expected and can be explained as follows: as the heat flux is increased very nearly to burnout (or coolant flow reduced - or, coolant temperature raised), patches of vapor formed by the agglomeration of bubbles wander over the heated surface. Any of these patches could cause melting of the surface, were one to reside at any given location for a short period of time (~ 1 second). With a short heated length, these vapor patches are swept rapidly out of the active zone. Therefore, an increase in the heat flux is required to cause a stable vapor blanket to form. Furthermore, end effects - conduction from the heated surface to the electrical bus bars - are greater with the short than with longer heated lengths. In addition, a thin walled tube requires for burnout a less stable vapor blanket than does the thick. These phenomena combine to cause the burnout heat fluxes as measured in the small scale test to be greater than those obtained in the full scale equipment.

The difference in heat transfer performance between the spiral and straight ribbed annulus geometries was investigated in the small scale equipment.⁽¹⁷⁾ No significant effect on the heat flux at burnout was observed in this comparison.

A study was made of the effect on burnout of eccentricity of the heater tube in the annulus and of defects in the quatrefoil, e.g., a dent in the housing tube, partial and full blockage of a subchannel, and a missing rib (simulating a slug in contact with the housing tube along its full length). The results of the eccentricity tests are presented graphically in Figure 7 and are tabulated below together with those from the tests of the defects.

BURNOUT HEAT FLUX IN MODIFIED ANNULI

Bases: Coolant Temperature 95°C*
 Coolant Pressure 30-40 psig
 Coolant Velocity 24 ft/sec
 Annulus Width - nominal 1/8 inch

Run Conditions	Burnout Heat Flux Pcu/hr-ft ² x 10 ⁻³	% of Burnout Heat Flux for Perfect Annulus
Perfect Annulus-concentric	795	100
Unobstructed Annulus-50 mils eccentricity	704	88.5
" " -125 " "	508	63.9
Dented Housing in one Subchannel	745	93.7
Partial Baffle " " "	587	73.9
Total " " "	568	71.4

* The data were taken at varying temperatures in the neighborhood of 95°C; the results tabulated have been normalized to make them comparable.

F. Corrosion of Aluminum in High Purity Water

Details of the Columbia program to determine the rate of corrosion of aluminum in boiling, deionized water are presented in References 11, 14, and 16. The results of this program have been summarized (20) and show that as long as the purity of the coolant is high, the corrosion rate of aluminum is negligible even with extensive boiling.

G. Natural Convection Heat Transfer

Although the cooling of fuel elements through natural convection (with boiling) holds little promise of application in the SRP reactors, it may be advantageous to cool the control rods by this heat transfer mechanism at high power levels. Accordingly, a modest experimental program was authorized at Columbia to study the variables and to divine the laws, if possible, that define cooling by natural convection.

The results of this investigation are presented in Reference 21. Earlier tests (22,23) showed that surprisingly high heat fluxes could be dissipated with natural convection boiling at atmospheric pressure; e.g., with a 1 inch aluminum tube, vertical in a pool of 50°C water, burnout occurred at a heat flux of 560,000 Pcu/hr-ft² and with a 2 inch aluminum tube, vertical in 90°C water, 246,000 Pcu/hr-ft² were dissipated at burnout. Bubble diameters of 2 to 3 inches were observed in these tests.

III. Application of Columbia Data

Preliminary discussions of the application of these data to Savannah River Plant reactor problems were reported (3,15,19) during the period of experimentation. At the conclusion of the tests, summary reports (1,5,20,21,25,26,27,28) were written by Columbia personnel. Each report describes the results and conclusions obtained from one phase of the Columbia study.

A) Heat Removal from the Slug Column

1. Sheath Temperature and Local Boiling

With forced convection cooling of the slug column, the maximum sheath temperature increases linearly with rod power (see Figure 6). The sheath temperature can exceed the boiling point of bulk coolant before local boiling begins. The higher the coolant velocity and the higher the ambient pressure (up to about 500 psia) the greater will be the "wall superheat" (the difference between the temperature of the heat transmitting surface, T_w , and the boiling point of the coolant, T_{sat}) required to cause boiling. Two methods are available for predicting the magnitude of the wall superheat (15,29). Once boiling has become fully established on a heat transfer surface, an increase in the rod power will not cause an increase in sheath temperature at constant coolant velocity and pressure even though the temperature of the coolant does increase.

In the fully-loaded quatrefoil (Mark I and VII elements), local boiling in the annulus is attended by bulk boiling in the bottom end fitting. Bulk boiling and net steam generation in the end fitting cause the ambient pressure in the annulus to rise which tends to quench the local boiling. However, this increase in pressure is accompanied by a decrease in coolant flow and velocity causing an increase in the intensity of the boiling. In the quatrefoil, the rise in ambient pressure is much more rapid than the decrease in coolant velocity (see Figure 2) and so the sheath temperature increases with increasing rod power as shown in Figure 6. Furthermore, it is clear that this rapid rise in pressure drop leads to failure of the slug column through flow instability rather than local boiling.

In the "partially loaded" quatrefoil, e.g. Mark VIII elements, neglecting the heat generated in the target slugs, very high heat fluxes can be obtained at rod powers lower than is required to cause bulk boiling in the end fitting. Under these conditions, local boiling burnout can occur in the quatrefoil. (30)

Defects in the geometry of the quatrefoil (as shown in Section I,E) can reduce the safe operating power of a fuel

column. The worst case, that of line contact of a slug with the housing due to a missing rib, could cause burnout at rod powers above 790 KW, for a chopped cosine distribution of heat flux. With 1700 effective fuel rods, the corresponding reactor power is 1340 MW, which indicates the inherent safety of the quatrefoil design.

2. Boiling Pressure Drop and Flow Instability

The methods of calculating the pressure drop with boiling in the quatrefoil were discussed in Section I, C and are compared in the Appendix.

A discussion of reactor safety considerations has been presented,⁽³⁾ including the value of inlet orificing and excess plenum pressure during transient conditions.

In Reference 5, the empirical correlations derived by Columbia personnel are used to calculate the maximum power capabilities of a slug column. In this report⁽⁵⁾, the data from the experiments are compared with the results of these calculations as applied to the following problems:

- a) prediction of flow instability,
- b) accidental inlet obstruction,
- c) accidental outlet obstruction,
- d) accidental changes in plenum pressure, and
- e) redistribution of excess pressure drop (across the "boiling disease" orifices).

The conclusions reached from this comparison of data and calculation, summarized in a series of graphs, are in accord with our analyses of these problems, except for the last one listed. In the case of the redistribution of excess pressure drop between inlet and exit orifices, the calculations predict an optimum arrangement but the experimental data do not show this, nor does our analysis yield an optimum ratio of inlet to exit orificing.

It is anticipated that new data, to be obtained in the Savannah River Laboratory facility currently under construction, will permit a critical review of the Columbia conclusions.

3. Monitoring

A rather thorough discussion of pile operation and safety considerations has been presented^(31,38). The problems considered in this discussion include inlet versus exit orificing, low and high (steaming) flow instrumentation, transient and instability conditions, and the philosophy of monitoring as it has evolved at Hanford. Earlier, the limit on safe reactor power imposed by monitoring was discussed⁽³⁾. More recently a development was presented⁽³²⁾ in which the end-fitting

pressure drop was correlated with rod power and coolant flow for the case of net steam generation in the annulus; this correlation was based on CMX and Columbia measurements. The analysis permits delineation of the regions of safe operating power as a function of plenum pressure and "window width" of monitor setting (between the Very Low Flow and High ΔP switches).

An annoying characteristic of the SRP flow monitor has been the observed "power coefficient of flow". Although a major fraction of the fuel assemblies are observed to change flow with increasing power in a predictable manner^(33,34), many others show greater changes, while some show negative changes or none at all. Figure 3 shows that coolant flow is a function of rod power, plenum pressure and eccentricity of the slug column. Figure 5 shows the overall effect of power of the end-fitting pressure drop and here the influence of slug column eccentricity is pronounced. Even the region of operation at rod powers less than that required to cause bulk boiling shows a mixed band of behavior.

The variability in eccentricity of the slug column has been discussed at length⁽³⁾. Since eccentricity also affects the initiation of local boiling in the narrower channels (and boiling can begin at rod powers around 500 KW), it is entirely possible that variations in the power coefficient of flow are due primarily to the variable eccentricities permitted by manufacturing tolerances in the quarefoil components. It is also clear, from Figure 5, that wider deviations from normal behavior are to be expected at the higher power levels; this phenomenon has been observed in the reactors.

4. Accident Simulation Tests

In October, 1953, Columbia ran a series of accident simulation tests to determine the behavior of the reactor complex during both loss-of-coolant-flow and nuclear runaway accidents. The data⁽¹⁶⁾ were reported in detail and were analyzed in SRP safeguards reports^(39,40). More recently, Columbia has issued a final report⁽²⁶⁾ summarizing their analysis of the data including a calculated comparison of the transient behavior of stainless steel (the heater rod used in the experiments), uranium, and uranium-aluminum alloy.

The experiments were performed at a relatively low rod power (370 KW) by current reactor standards. The accidents that were simulated are listed below:

- a) simultaneous loss of A.C. pump power and failure of the safety rod mechanism at constant power,
- b) simultaneous loss of A.C. and D.C. pump power and failure of the safety rod mechanism at constant power, and
- c) nuclear runaway during startup due to a failure of both the control and safety rod mechanisms.

The results of these tests are tabulated below:

<u>Type of Test</u>	<u>a)</u>	<u>b)</u>	<u>c)</u>
Max. Reactor Tank Pressure, psig.	16	16	100
Time* to Max. Tank Pressure, sec.	20-30	10-15	60-70
Time to start melting sheaths, sec.	30-40	20-30	50-60
Time to start melting fuel, sec.	50-60	40-50	60-70

* Zero time is taken as of the moment of failure of pump power (or the start of the runaway).

At current power levels, accidents of the types simulated in these tests would be catastrophic. Since there is an extremely small probability that all the safety (nuclear shutdown) mechanisms would fail, a more plausible accident has been postulated as the basis for reactor safety; it is the pump-shaft-break incident. This accident (partial loss of coolant flow followed by nuclear shutdown) has been analyzed⁽⁴¹⁾ and the power limit imposed by it is not exceeded in SRP reactor operation.

B) Heat Removal from Tubular Elements

1. Forced Convection Heat Transfer

In the quatrefoil fuel elements and the tubular element designs using one fuel tube, each heat transmitting surface is accompanied by its own coolant channel. For these designs, non-boiling heat transfer coefficients can be obtained from Equation 1 (Section I-A). In the plate and tubular (two fuel tubes) designs, there are coolant channels that receive heat from both boundary surfaces. For this configuration, Columbia personnel have shown⁽²⁷⁾ that the conventional method of calculating the heat transfer coefficient yields low values even when the fluxes from the two surfaces are equal.

The Columbia calculation shows that when heat is transmitted by two surfaces facing each other, the heat transfer coefficient based on the coolant velocity profile as properly defined is 10 to 20% higher than the usual values. For example, if the Reynolds Number is about 200,000 the Prandtl Number about 2, the corrected heat transfer coefficient is 10% greater than calculated conventionally, if both surface heat fluxes are the same. When the outer surface heat flux is 20% greater than the inner and the Reynolds and Prandtl Numbers are as above, the corrected heat transfer coefficient at the inner surface is 12% greater than calculated conventionally.

Thus, this development clearly finds application in the design of the Mark V and VI-A fuel elements, and it predicts lower surface temperatures than are currently assigned to the surfaces in the Technical Data Summaries.^(42,43)

2. Local Boiling and Burnout

The data obtained at Columbia from the experiments with 1/4 inch and 3/8 inch annulus widths are summarized(28) in a report being prepared. These data, as analyzed by Columbia personnel, permit the following conclusions:

a) The McAdams equation, for estimating the heated surface temperature during fully-developed local boiling, may be written $(T_w - T_{sat}) = (q/A)^{0.259}$. The "constant", c , normally included in this equation and considered to be a function of the amount of dissolved gas in the coolant, is shown to be unity and no justification is found for varying its magnitude with gas content. The accuracy of the prediction of wall temperature is found to be no better than $\pm 15^\circ\text{C}$ by this equation.

b) The burnout correlation, presented in DP-121, predicts the results obtained from the Columbia full-scale tests with an accuracy of $\pm 20\%$. It predicts burnout heat fluxes about 19% below the experimental values for the small-scale tests (see discussion in Section I,D,2). Attempts by the Columbia people to develop a more precise correlation have not been fruitful.

The limits imposed by local boiling and burnout are carefully considered in the design of extended surface fuel elements.(42,43) An added complication in the thermal analysis of tubular elements, although a helpful one from the standpoint of reactor safety, arises from the fact that heat is passed from both sides of the tubes into separate coolant streams. Thus, the accurate description of the behavior of the tubes during a period of transient operation, e.g., a flow reduction, must account for the split in heat flow to the two coolant streams. In the current designs of new fuel elements, machine computations have permitted accurate analysis of these conditions.

This type of analysis has demonstrated the value of bottom orificing in fuel elements having 1/4-inch or greater width of annulus. The advantages of the bottom orifice have been incorporated in the Mark V design(42); the increase in margin of safety from burnout obtained through bottom orificing has been reviewed(11,19) and is depicted graphically in Figure 10. This graph shows that about 50% more power can be generated safely in a fuel rod when the excess pressure drop is taken across a coolant outlet, rather than an inlet, orifice.

L. Bernath

LB/s

1. FORCED CONVECTION HEAT TRANSFER COEFFICIENT FOR CONCENTRIC ANNULI.
2. BOILING PRESSURE DROP CHARACTERISTICS OF ONE SLUG COLUMN - CHOPPED COSINE FLUX.
3. EFFECT OF ROD POWER ON COOLANT FLOW THROUGH Q-FOIL CHANNEL.
4. EFFECT OF ORIFICING ON ROD POWER AT INSTABILITY BURNOUT FOR SEVERAL QUATREFOIL ANNULUS CONFIGURATIONS.
5. EFFECT OF ROD POWER ON END FITTING ΔP .
6. MAXIMUM SHEATH TEMPERATURE VS. ROD POWER IN QUATREFOILS COOLED BY LIGHT WATER: COSINE HEAT FLUX DISTRIBUTION.
7. EFFECT OF ECCENTRICITY ON BURNOUT HEAT FLUX.
8. INITIATION OF BOILING IN THE CONCENTRIC ANNULUS.
9. INITIATION OF BOILING IN THE ECCENTRIC ANNULUS.
10. EFFECT OF BOTTOM ORIFICING ON ROD POWER AT BURNOUT.

FIGURE 1

DPW-56-408

FORCED CONVECTION HEAT TRANSFER COEFFICIENT
FOR CONCENTRIC ANNULI

EUGENE DIEZGEN CO.
MADE IN U.S.A.

NO. 341 20 DIEZGEN GRAPH PAPER
20 X 20 PER INCH

EFFECT OF ROD POWER ON
COOLANT FLOW THROUGH Q-FOIL CHANNEL

$\frac{P}{P_{BB}}$ = Coolant Flow at Operating Condition
Normal Coolant Flow

1.0
0.8
0.6
0.4
0.2
0

Curve	Description	Normal Flow Orifice Ratio
A	Aerated Coolant (14 ppm Air)	1.52
B	Normal Ecc. Slug Column	1.54
C	" " " "	1.93
D	" " " " - Zone II	2.78
E	Concentric Slug Column - Uniform Flux	1.28
F	" " " " - Cosine	1.42
G	" " " " - " "	1.63

Orifice Ratio = $\frac{\Delta P \text{ Across Inlet and Outlet Orifices}}{\Delta P \text{ Across Annulus}}$

$\frac{P}{P_{BB}}$ = Actual Rod Power
Rod Power to Cause Boiling in End Fitting with Normal Flow

0 0.2 0.4 0.6 0.8 1.0 1.2

FIGURE 3

DPW-56-408

**EFFECT OF ORIFICING ON ROD POWER AT
INSTABILITY BURNOUT FOR SEVERAL
QUATREPOIL ANNULUS CONFIGURATIONS**

RATIO OF ROD POWER AT INSTABILITY TO
ROD POWER TO CAUSE BOILING IN END FITTING - P_{BO}/P_{BB}

1.3
1.2
1.1
1.0
0.9
0.8
0.7
0.6
0.5
0.4
0.3
0.2
0.1
0

LEGEND

SYMBOL	CURVE	Annulus Geometry
◆	A	Concentric - Vented Ribs
■	A	Concentric - Unvented Ribs
+	B	Eccentric - Vented Ribs
○	C	Eccentric - Unvented Ribs

ORIFICE RATIO WITH NORMAL FLOW = $\frac{\text{Pressure Drop Across Orifices (Top and Bottom)}}{\text{Pressure Drop Across Annulus}}$

EFFECT OF ROD POWER ON END FITTING ΔP

LEGEND

Curve	Description	Normal Flow Orifice Ratio
A	Aerated Coolant (14 ppm Air)	1.52
B	Normal Ecc. Slug Column	1.54
C	" " " "	1.93
D	" " " " - Zone II	2.78
E	Concentric Slug Column - Uniform Flux	1.28
F	" " " " - Cosine Flux	1.42
G	" " " " - " "	1.63

$$\text{Orifice Ratio} = \frac{\Delta P \text{ Across Inlet and Outlet Orifices}}{\Delta P \text{ Across Annulus}}$$

MAXIMUM SHEATH TEMPERATURE VS. ROD POWER IN QUATREFOILS
COOLED BY LIGHT WATER: COSINE HEAT FLUX DISTRIBUTION

FIGURE 7

DPW-56-408

FIGURE 8

DPW-56-408

SHADED
AREA SHOWS
APPROXIMATE SHAPE OF
BOILING AREA IN SUBCHANNEL
AS OBSERVED

INITIATION OF BOILING
IN THE CONCENTRIC ANNULUS

Run 36C5
Rod Power = 418 KW
Flow = 16 GPM
Concentric 1/8" Annulus

INITIATION OF BOILING
IN THE ECCENTRIC ANNULUS

Run 38C2
Rod Power = 404 KW
Flow = 21 GPM
0.050" Eccentric 1/8" Annulus

EFFECT OF BOTTOM ORIFICING ON ROD POWER AT BURNOUT

GRAPHIC DISTRIBUTION UNIT

ALL 341 311 DISTRIBUTION GRAPHIC PAPER
20 X 20 PER INCH

TABLE OF NOMENCLATURE

<u>Symbol</u>	<u>Meaning</u>
C_p	Coolant heat capacity, $\text{Pcu/lb-}^\circ\text{C}$.
D_e	Hydraulic diameter of channel, ft (or inch).
D_2, D_1	Outer and inner diameters of channel, ft.
F	Coolant flow rate, gpm.
G	Coolant mass velocity, lb/hr-ft^2
h	Heat transfer coefficient, $\text{Pcu/hr-ft}^2\text{-}^\circ\text{C}$.
k	Coolant thermal conductivity, $\text{Pcu/hr-ft}^2\text{-}^\circ\text{C/ft}$.
P	Rod power, KW.
ΔP_s	Pressure drop through annulus and end-fitting, psi.
T_{in}	Coolant inlet temperature, $^\circ\text{C}$.
T_{sat}	Boiling point of coolant, $^\circ\text{C}$.
T_w	Wall (sheath) temperature, $^\circ\text{C}$.
t_f	Film temperature of coolant, $^\circ\text{C}$.
V	Coolant velocity, ft/sec.
ρ	Coolant density, evaluated at the average bulk temperature, lb/ft^3
μ	Coolant viscosity, lb/hr-ft .

Subscripts

BB	Boiling at normal end fitting pressure.
BC	Burnout condition.
BT	Boiling at tank pressure.
C	Coolant channel associated with one fuel rod.
f	Properties of coolant evaluated at film temperature.
i	Intersection between curves.
Min.	Minimum (lowest part of curve).
O	Initial or normal condition.

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APPENDIX

COMPARISON OF COLUMBIA AND DU PONT METHODS
OF PREDICTING FLOW INSTABILITY

The Columbia run series 29C culminated in a flow instability during Run No. 16. The conditions of this typical failure were measured very accurately over a period 20 minutes. During this time the inlet temperature of the coolant increased 1.5°C. With this slight increase, the flow became unstable and the heater tube burned out in about 6 seconds.

The table below summarizes the experimental and calculated results:

Run 29 C16

Plenum Pressure	183 psig.
T _{in}	43.9°C.
	<u>Exp't'l</u> <u>Columbia</u> <u>du Pont (Fig. 2)</u>
Rod Power at Burnout-KW	900 830 865
Flow at Burnout gpm	29.2 27 29

A further check of the validity of these experimental values shows that this burnout point lies very slightly above Curve A in Figure 4 of this memorandum. The calculated results show that the du Pont method agrees with experiment a little better than does the Columbia method.

Since most of the Columbia data were taken with plenum pressures considerably above those currently in practice, a problem has been formulated to compare the two calculational methods in the region of low plenum pressures. Curve X in Figure 2 represents the inlet pressure curve for this case.

Example

Plenum Pressure	135 psig.
T _{in}	35°C
	<u>Columbia</u> <u>du Pont (Fig. 2)</u>
Rod Power at Burnout-KW	825 840
Blow at Burnout, gpm	29.5 31.3

From Figure 4, we read a burnout power of 826 KW at an orifice ratio of 0.785. This example shows excellent agreement between the two methods of calculation and between the calculated values and those obtained from experiment.

It is, therefore, clear that either method of calculation may be used.

BIBLIOGRAPHY

1. CU-10-56-At-187-Ch.E., Heat Transfer in Turbulent Flow in Internally Heated Annuli, December 1955.
2. Ref. 1, Table III.
3. DPW-54-333, Columbia Subcontract-Status Report, April 15, 1954.
4. Ref. 3, Equation 3, p.4.
5. CU-14-56-At-187-Ch.E., Maximum Power Dissipation Capabilities of a Slug Column, July 1956.
6. Ref. 3, p. 37 ff.
7. Ref. 5, p. 12 ff.
8. Ref. 3, p. 12 ff.
9. CU-27-55-At-dP-Ch.E., Quarterly Progress Report (QPR-3), March 1955. See Run Series 37C.
10. Ref. 9, Runs 42C and 44C.
11. CU-26-55-At-dP-Ch.E., Semiannual Progress Report (SAPR-4), January 1955.
12. Ref. 9, Run 40CR, p. 20 ff.
13. CU-30-55-At-dP-Ch.E., Quarterly Progress Report (QPR-4), June 1955. See Run Series 7U, p. 13 ff.
14. CU-23-54-At-dP-Ch.E., Semiannual Progress Report (SAPR-3), July 1954.
15. DP-121, Heat Transfer as Limited by Fuel Element Burnout, June 1955.
16. CU-19-54-At-dP-Ch.E., Semiannual Progress Report (SAPR-2), January 1954.
17. CU-25-54-At-dP-Ch.E., Quarterly Progress Report (QPR-2), December 1954.
18. Ref. 14, p. 157 ff.
19. DPW-55-192, Fuel Element Design Considerations, March 23, 1955.
20. Summary Report on Corrosion (in preparation).
21. Summary Report on Natural Convection (in preparation).
22. Ref. 14, p. 155 ff.

BIBLIOGRAPHY (cont'd)

23. Ref. 17, p. 34 and 35.
24. CU-31-55-At-dP-Ch.E., Summary of Large Scale Tests, July 1955.
25. CU-9-56-At-187-Ch.E., Pressure Drop for Flow of Steam-Water Mixtures in a Heated Annulus and Through Orifices, December 1955.
26. CU-13-56-At-187-Ch. E., Accident Simulation Tests, May 1956.
27. CU-20-56-At-187-Ch. E., The Dependence of the Heat Transfer Coefficient on the Ratio of the Heat Fluxes from the Walls of Parallel Plane Flow Channels, August 1956.
28. Summary Report on Boiling Heat Transfer (in preparation).
29. McAdams, W. H., et al., Ind. Eng. Chem. 41, (1949) 1945-1953, Heat Transfer at High Rates to Water with Surface Boiling.
30. DPW-56-312, Heat Transfer Burnout of Mark VIII Fuel, August 1, 1956.
31. Ref. 14, p. 207 ff.
32. DPW-56-294, Quatrefoil Flow Monitoring, July 6, 1956.
33. DPW-54-327, Process Flow Changes with Temperature, April 13, 1954.
34. DPW-54-232, Effect of Temperature Upon Quatrefoil Flow, March 4, 1954.
35. Ref. 17, p. 29.
36. Ref. 9, p. 39.
37. Ref. 14, p. 155 and Figures 87 and 88.
38. CU-24-54-At-dP-Ch.E., Quarterly Progress Report No. 1 (QPR-1), September 1954, p. 71 ff.
39. DPW-54-125, Reactor Safety Determination, Savannah River Plant - Part II, January 1954.
40. DPW-56-106, Reactor Safety Determination, Savannah River Plant, February 1956.
41. DPW-56-234, Safe Reactor Power, May 1956.
42. DPST-56-374, Hydraulics and Heat Transfer in Mark V, August 20, 1956.
43. DPST-56-147, Design Data Summary - Mark II-A, February 9, 1956.

BIBLIOGRAPHY (cont'd)

44. CU-2-55-At-187-Ch.E., Du Pont Subcontract Data Book, Volume I, October 1955.
45. CU-3-55-At-187-Ch.E., Du Pont Subcontract Data Book, Volume II, November 1955.
46. CU-4-55-At-187-Ch.E., Du Pont Subcontract Data Book, Volume III, December 1955.
47. CU-5-55-At-187-Ch.E., Du Pont Subcontract Data Book, Volume IV, December 1955.
48. CU-7-56-At-187-Ch.E., Du Pont Subcontract Data Book, Volume V, January 1956.
49. CU-8-56-At-187-Ch.E., Du Pont Subcontract Data Book, Volume VI, January 1956.
50. CU-11-56-At-187-Ch.E., Calculation Methods and Results of du Pont Subcontract Data, March 1956.

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