089060-F COO-1723-F

THE UNIVERSITY OF MICHIGAN

COLLEGE OF ENGINEERING
Department of Chemical Engineering

Final Report

CRITICAL HEAT FLUX STUDIES IN POOL BOILING AND FORCED CONVECTION

Richard E. Balzhiser
Robert E. Barry

Project Directors

Bruce F. Caswell
Robert L. Gahman
Kwan Y. Kim
William B. Lampert

ORA Project 089060

under contract with:

U. S. ATOMIC ENERGY COMMISSION
CHICAGO OPERATIONS OFFICE
CONTRACT NO. AT(11-1)-1723
ARGONNE, ILLINOIS

administered through:

OFFICE OF RESEARCH ADMINISTRATION ANN ARBOR

October 1971

Abstract

Heat transfer to sodium in forced convection flow was studied in an annular test section at heat fluxes up to 1,659,000 Btu/hr.ft.^{2} Experiment failures prevented completion of the experimental program. Three runs were completed before burnout was experienced at the above flux. Stable boiling was observed from the bayonet surface at a heat flux of 1,120,000 Btu/hr.ft.^{2}

The temperature profile in boiling potassium near the surface of a bayonet heater was measured. The results were in agreement with the theoretical model developed which included a thermal transport component in addition to the latent heat contribution which the hydrodynamic theory predicts.

TABLE OF CONTENTS

LIST OF FIGURES

NOMENCLATURE

a	gravitational acceleration
A	heat transfer area
g	gravitational acceleration at sea level
g_c	32.2 $(ft/sec^2)\cdot(lb_f/lb_m)$
h	heat transfer coefficient
i	$\sqrt{-1}$
k	thermal conductivity
K	0.13 = constant in Kutataladze's eqn.
Pr	Prandtl number
q	heat flux
q_c	critical heat flux
q_H	hydrodynamic contribution to q_c
q_T	thermal contribution to q_c
T	temperature
T_s	saturation temperature
T_w	wall temperature
u	space coordinate
v	space coordinate
w	$u + iv$
x	space coordinate
y	space coordinate
z	$x + iy$

α	thermal diffusivity
β	three phase contact angle
λ	wave length
λ_v	latent heat of vaporization
π	3.14159
σ	surface tension
ρ_v	vapor density
ρ_l	liquid density

Introduction

Liquid metal heat transfer research was initiated in the Liquid Metals Laboratory of The University of Michigan in June of 1961 under contract with the Aeronautical Systems Division of the U.S. Air Force. These studies continued to April 30, 1966 with the final report [1] issued in January 1967. The program provided heat transfer and two phase flow information in support of the Air Force's space energy conversion program using a high temperature Rankine cycle.

The Michigan study included critical heat flux determinations in saturated pool boiling, film boiling, boiling in agravic fields, condensing, two-phase heat transfer and fluid flow behavior in metallic systems. Critical heat flux* studies in pool boiling geometries were conducted with sodium, potassium, cesium and rubidium, and the results were among the first available in the literature. Film boiling was performed with potassium and this study represents the only experimental film boiling study conducted to date with alkalai metals. Agravic studies were conducted in a centrifuge at accelerations up to 15 gees with mercury as the metallic fluid. Condensing studies were conducted with sodium and rubidium and have contributed to an improved understanding of the condensing process. The two-phase flow studies consisted of pressure drop, void

*The terms burnout and critical heat flux are used in this report to signify the heat flux which, if exceeded for a given flow rate, subcooling and pressure, results in a surface temperature excursion, frequently in excess of the melting point of most metals. This same condition is also referred to in the literature as departure from nucleate boiling (DNB) or the peak heat flux.

fraction and two-phase heat transfer measurements. The latter studies were conducted with potassium as the test fluid.

The critical heat flux studies from a bayonet surface in a pool boiling configuration and the two-phase forced circulation heat transfer study served as precursors to the program initiated with the Atomic Energy Commission in June of 1967.

The results obtained in the critical heat flux studies from a bayonet immersed in a liquid metal pool demonstrated a departure from the hydrodynamic theory for burnout, which had proved reasonably reliable in predicting burnout for non-metallic systems. These results were confirmed by a limited number of investigations carried out in other laboratories. Burnout fluxes in metallic systems were substantially above those predicted by the hydrodynamic correlations and showed a much lower pressure dependency than predicted.

Conclusions at that time suggested that for metallic fluids the latent heat contribution to energy removal from the surface was accompanied by a significant conductive-convective contribution as a result of the much higher thermal conductivity of metallic fluids. Temperature measurements in the vicinity of the boiling surface in our studies had suggested the existence of a temperature gradient in the two-phase fluid at reasonable distances away from the surface. However, the precision with which these measurements could be made in the earlier apparatus prevented a decisive assessment of the existence of such a contribution in other than qualitative terms.

Shortly after the completion of this study, Noyes[2] reported his experimental studies of sodium burnout in forced convection at the 3rd International Heat Transfer Conference in Chicago. These studies were conducted by passing sodium through an annular region with heat generation occurring from a bayonet surface at the core of the test section. His critical heat flux values were considerably above those observed in pool boiling geometries at comparable pressure levels. In addition to pressure, he varied subcooling and flow rate and was able to relate the effect of these parameters on forced convection liquid metal burnout to the hydrodynamic theory offered earlier by Zuber[3] and Kutateladze[4].

As a result of these studies and others conducted during the early 1960's which demonstrated appreciable superheating in liquid metal fluids before stable boiling commenced, increased interest in the incipience phenomenon as it related to possible boiling in the core of the LMFBR was generated. Consequently, much of the theoretical and experimental effort in liquid metal heat transfer has been devoted to better defining the parameters affecting incipience and its significance from a reactor safety point of view.

At the same time it became apparent that existing knowledge of the critical heat flux behavior in pool boiling and forced circulation modes was inadequate to predict critical heat flux behavior at the surface of fuel elements in a reactor core. Though the work of Noyes provided some suggestion as to the flux levels that could be achieved before burnout occurred, his studies were conducted from a single bayonet surface in the absence of other high flux elements in the immediate vicinity. In the core of a reactor where there are a large number of high flux surfaces operating in close proximity to one another, the question arises as to what effect boiling from a nearby surface would have on the critical flux of an immediately adjacent element.

With these considerations in mind, this study was proposed to accomplish two objectives; increase the understanding of the mechanisms by which energy is transported from a surface under conditions up to and including burnout fluxes and to assess the effect of other high heat flux surfaces on the burnout level of a nearby surface. Most of the equipment necessary for these studies was to be made available by modifying the pool boiling equipment and the forced circulation facilities already available in the Liquid Metals Laboratory at The University. Though the proposed modifications were eventually accomplished, it was not until very late in the program that data was actually obtained. These results are discussed in this report, but are too few to permit any meaningful conclusions with regard to either of the initial objectives in the study.

Forced Circulation Studies

Objectives

The principle objective of the forced circulation portion of this study was to obtain experimental results showing what effect a heat flux of up to 600,000 Btu/hr-sq.ft. on the outside of an annular passage would have on the burnout flux at the inner surface. The geometry was intended to simulate the condition that would exist at a nuclear fuel element surface which was surrounded by other high heat flux elements. The annular test section in this study was 2 inches in length by 0.073 inches as contrasted to the much longer test section used in Noyes' studies. It was felt that burnout conditions could be related more precisely to fluid enthalpy and flow conditions over this limited heated length. The enthalpy of the fluid entering the test section was controlled by adjusting power and flow rate to the preheaters located just upstream of the test section.

In forced circulation burnout it is recognized that thermal transport at the surface depends on the flow regime of the fluid. Under conditions of subcooling, boiling in non-metallic systems has been correlated reasonably well with the superposition theory which combines the latent heat and convective contributions producing reasonably good predictions over most of the boiling range. At higher qualities, when the annular regime is developed, the burnout phenomenon relates more to the break-up of the liquid film adjacent to the surface. Limiting fluxes under this condition are much below those experienced in the sub-cooled regimes with a maximum in the critical flux occurring in the low quality region where the accelerative effect of vapor generation enhances the transfer process.

Presumably limiting conditions in a reactor core could involve either of these limiting cases depending on the nature of the flow regimes established once incipience is experienced at some point in the core. Vapor expulsion studies have been conducted to learn more about the hydrodynamics associated with two-phase flow from core configurations under such conditions. Though in theory it is possible to study both high quality and sub-cooled flows, it was the latter that was of principle interest in this investigation.

A limited amount of information is available in the literature on burnout for water flowing through an annular region, heated from both sides[5,6]. These studies indicated a definite effect on burnout behavior as a result of a high flux surface in close proximity to the test surface. The intent in this study was to vary flow rate, pressure, sub-cooling and the heat flux on the outer test section wall while measuring the burnout heat flux for the bayonet surface at the interior of the annular region. The heat flux on the outer surface was to be provided by condensing sodium on the outside of the 12 mil, two-inch long test section wall in the liquid metal loop. The condensing process had been well studied in earlier investigations at heat flux levels up to 650,000 Btu/hr-sq.ft. The principle change in the apparatus required to perform this study was the insertion of a bayonet heater into this two-inch test section. Its design was to permit operation at fluxes up to 2,000,000 Btu/hr-sq.ft. without failure of the bayonet when the limiting flux condition was realized. Considerations relating to the design of a bayonet to meet these objectives are discussed in the following section of the report.

Forced Circulation Apparatus

A schematic diagram of the forced circulation apparatus used in the study is shown in Figure 1. The apparatus had been used in the studies described earlier with potassium as the fluid in the forced circulation side of the system and sodium as the fluid in the natural circulation condensing loop. On the primary side flow is produced by an electromagnetic pump and flow rate measured with an electromagnetic flow meter. The enthalpy of the fluid entering the test section is adjusted for a given flow rate by varying the power to three parallel clam shell preheater units located just upstream of the heat transfer test section. Flows from the preheaters are recombined at a point just below the test section and channeled into a one-half inch diameter flow channel 21 inches in length before the fluid reaches the test section (see Figure 2). The test section region is two-inches long and has an annular spacing of 0.073 inches. The

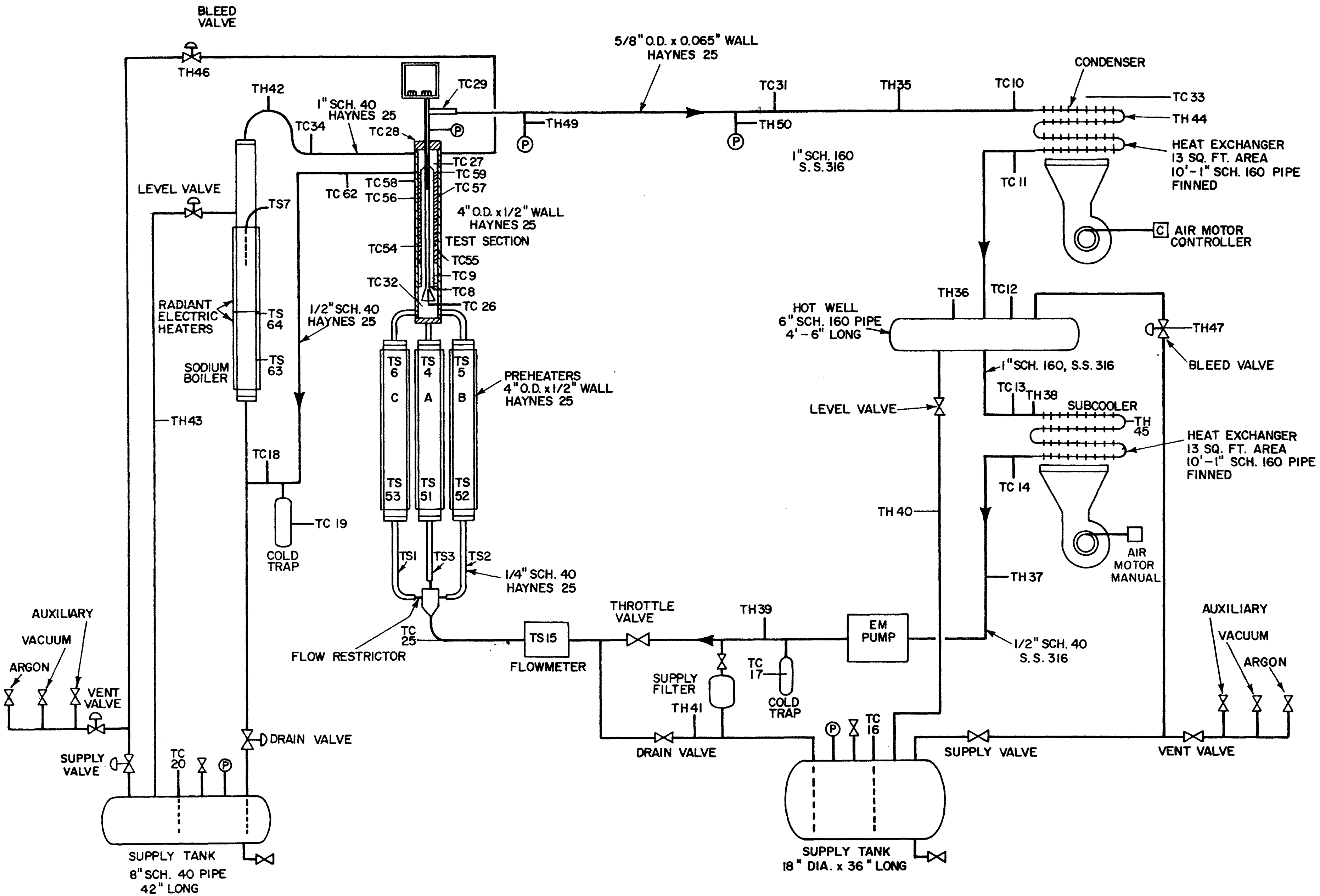


Figure 1. Flow Schematic-Boiling Liquid Metal System

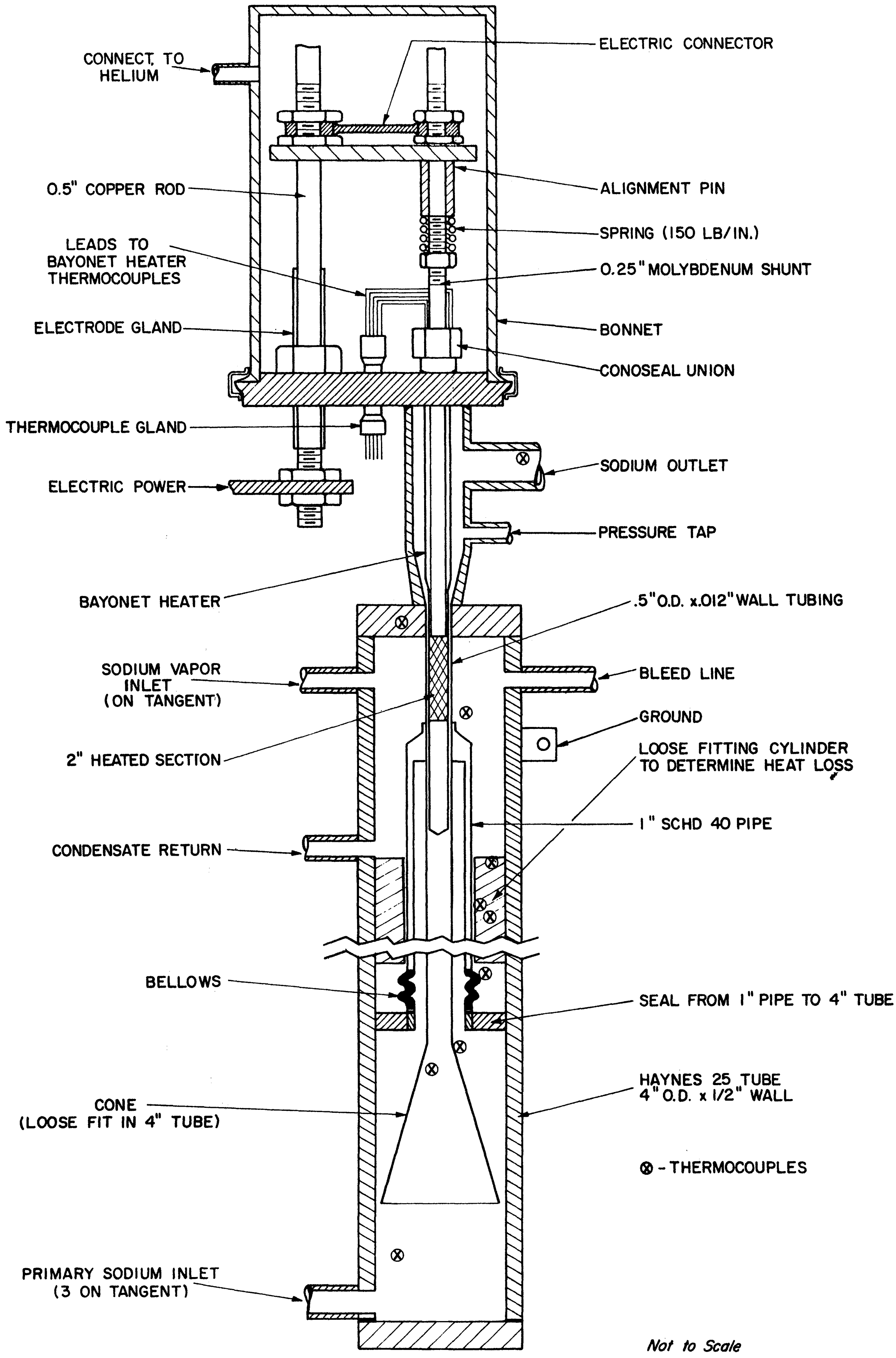


Figure 2. Test Section

bayonet extends upstream 3 inches from the point at which heating in the test section commences. Flows exiting the test section enter the tee which was added to the loop to permit insertion of the bayonet and exits through the side branch of the tee along the top of the loop. Fluid then passes on to a condenser and sub-cooler where energy is removed before returning to the pump.

The natural circulation loop consists of a boiler which is heated with clam shell heaters. The vapor, generated in the boiler, is passed overhead to the test section region where condensation occurs on the outside of the 12 mil test section wall. Condensate is returned directly to the boiler by gravity flow. Vapor traps are provided in the boiler to stabilize the boiling process and avoid perturbations in pressure and flow in that system. Stable boiling was experienced in this system at heat inputs sufficient to generate the 650,000 Btu/hr-sq.ft. flux mentioned earlier.

Temperature measurements are made in the vapor region at the point where condensation is occurring as well as several other locations around the loop. Calibration of heat losses from the natural convection loop for various temperature levels of operation permit the determination of the heat flux at the condensing surface from the power input to the boiler.

On the primary side of the system, absolute pressure measurements were made just downstream of the test section and temperature measurements were made at the inlet and the outlet of the preheaters and at a point just downstream from the test section. The bayonet was designed to incorporate five thermocouples, one of which would be located near the nose of the bayonet to measure the fluid inlet temperature to the test section. The other four thermocouples were intended to serve as burnout detectors and to indicate the point at which a rapid excursion in the temperature of the bayonet began. These thermocouples were coupled with a relay in the bayonet electrical circuit in an attempt to avoid a destructive failure of the bayonet itself once the critical flux was achieved. Sub-cooling was to be determined from vapor pressure information for potassium or sodium, the output from the absolute pressure gauge and the inlet temperature of the fluid at the test section.

The loop as originally designed consisted of an elbow just above the heat transfer test section and just ahead of the two-phase horizontal pressure drop section shown in Figure 1. That elbow was replaced with a tee fitting such that the flow entered one of the straight through sides of the tee and exited through the side of the tee into the two-phase pressure drop section. The bayonet was inserted through the other straight through branch in the manner shown in Figure 2. The replacement of the elbow with the tee and the insertion of the bayonet was accomplished as planned.

In the original design of the loop the heat transfer test section consisting of the two-inch, 12 mil wall section had been designed to achieve as high an operating heat flux as possible. Thus a 12 mil Haynes 25 section of tubing was used to minimize the thermal resistance. This 12 mil section was susceptible to corrosion as a result of a higher than desired oxygen concentration in the potassium during portions of the earlier study and the fact that the high temperature portion of the loop was fabricated from Haynes 25 alloy while the cooler portion of the loop was fabricated from 316 stainless steel. Thus the possibility for temperature gradient mass transfer within the system existed, but the extent to which this might have occurred could not be ascertained prior to commencement of this study without completely disassembling the loop. Ultimately it was a rupture, presumed to be in this 12 mil wall, which caused the study to be terminated.

The design of a bayonet to achieve 2,000,000 Btu/hr-sq. ft. at the surface was one of the major challenges in the program. Experience with bayonet heaters had been obtained in our earlier pool boiling studies, but none of these efforts had related to a bayonet of the type needed in this study. Because of the difficulty of replacing these bayonets and the associated contamination of the system that occurs each time it is opened, it was hoped that a bayonet could be designed which would withstand any temperature excursions resulting from having achieved the limiting flux condition. Because of its superior thermal conductivity and its desirable high temperature properties, molybdenum was selected as the material from which the bayonet would be fabricated. Considerable difficulty was encountered in the fabrication of a bayonet from molybdenum, and the attempt was eventually abandoned in favor of stainless steel. This change required a much thinner metal wall in the bayonet and increased the likelihood that a temperature excursion would produce failure of the bayonet surface.

Difficulties were also encountered with the small diameter thermocouples used for burnout detection purposes in the bayonet; this was also the case in the pool boiling studies to be described later. Both studies had problems with electrical continuity in the bayonets, particularly with the smaller diameter graphite resistors which were initially used. The latter problem was eventually overcome and the bayonet used in the loop operated satisfactorily at heat fluxes up to 1,650,000 Btu/hr-sq.ft. Details of the bayonet as finally designed are shown in Figure 3. A picture of the bayonet assembly before insertion is shown in Figure 4.

Operation of the Facility

Operational difficulties encountered during the early phases of the contract were discussed in the September 29, 1970 progress report. These are summarized in Appendix A of this report. From June 1967 to February 1968, the design for the modified test section was completed, the design for

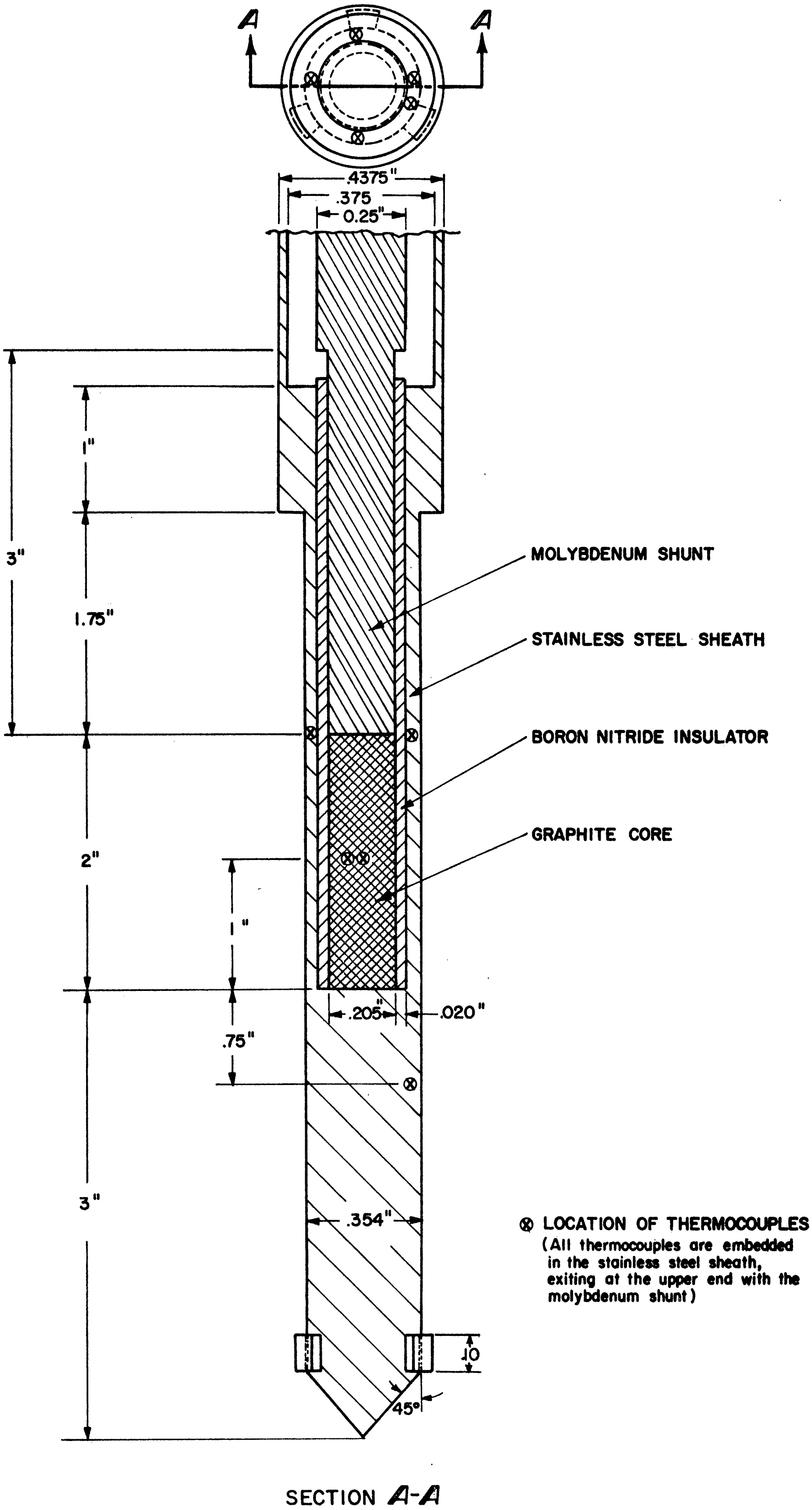


Figure 3. Bayonet Heater Detail

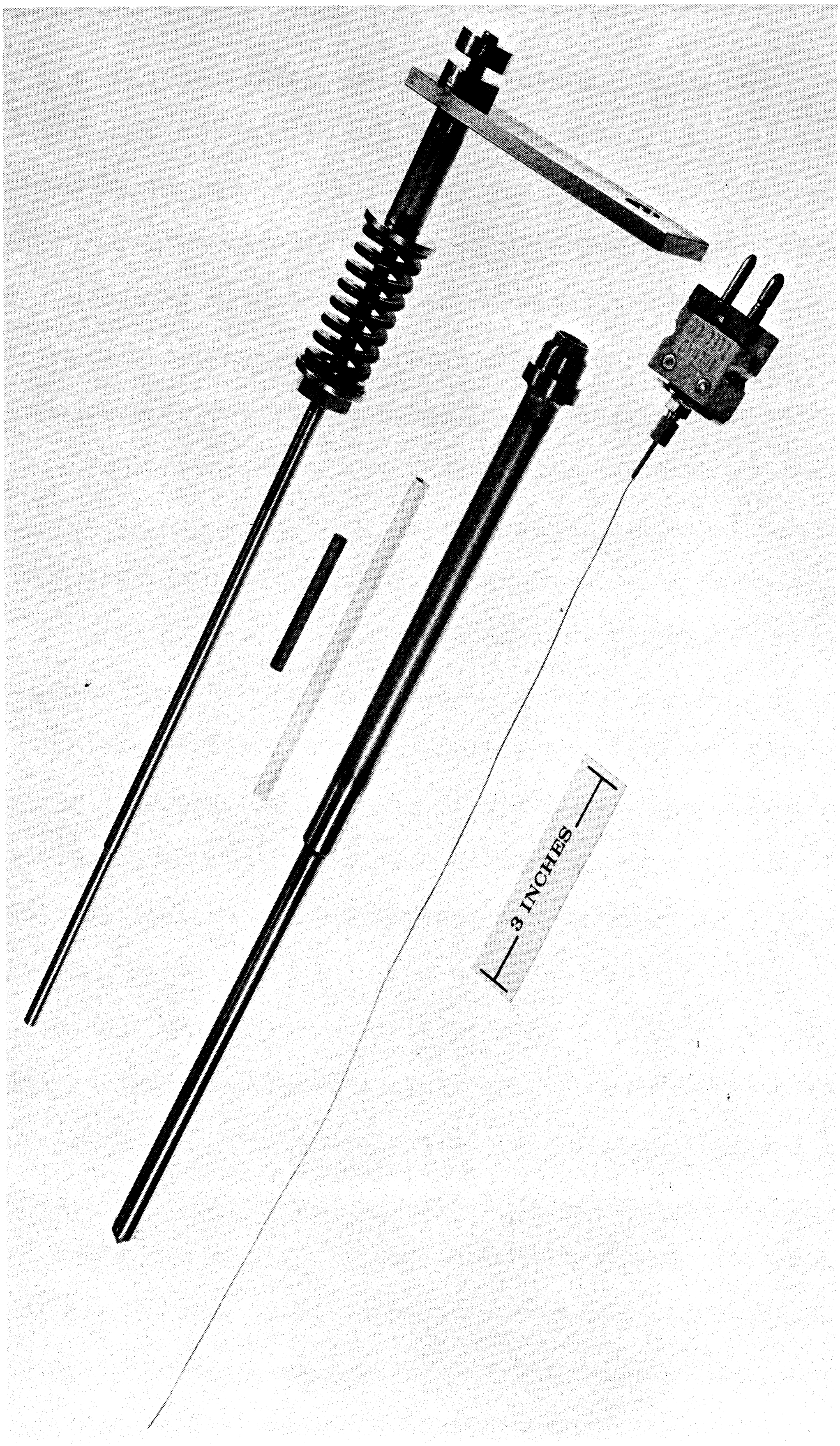


Figure 4. Bayonet Heater Before Insertion

the high flux bayonet utilizing molybdenum was completed, the preheater clam shell units were replaced and insulated, the DC power supply was installed and instruments were calibrated and repaired. During the balance of 1968 modifications were completed, but the efforts to fabricate an operating bayonet met with repeated failure.

By mid 1969 a molybdenum bayonet had been fabricated and was installed in the test section. Start-up was planned about the same time as the first contract period expired. Subsequent operation with this bayonet indicated that a crack had developed in the brazed joint as a result of embrittlement associated with recrystallization which had occurred during the brazing operation. Discussions with Climax Molybdenum Company followed and it was concluded that these problems were unavoidable unless it was possible to design a bayonet from a single piece of molybdenum. Due to the limitations imposed by the electrical discharge machining process in fabricating the thermowells, it was not possible to fabricate the bayonet from a single piece of molybdenum. Thus the decision was made to abandon molybdenum and redesign the bayonet element using stainless steel. Because of the lower thermal conductivity of stainless steel and its lower melting point, a complete redesign of the element was required and this was initiated in mid 1969.

While resolution of the bayonet heater problem was in progress, an instrumented non-heated stainless steel test section was designed, fabricated and installed in the circulation loop. In the latter part of 1969 the loop was readied for test with heat addition to the annular region from the outside surface only. Potassium was charged from the supply tank using the same procedure that had been used many times in the past. Before the charging was completed the loop became plugged and circulation could not be achieved nor could all of the partial charge be returned to the supply tank.

Attempts were made to clear the system by heating the lines to 1200°F with trace heaters from an extended period, but no flow could be achieved. The loop was cut open at several easily accessible locations in an attempt to remove the potassium trapped in the system. These attempts were only partially successful and indications were that several plugs existed, something that had never been encountered before.

The potassium removed was dissolved in water and a considerable amount of nonsoluble magnetic metallic residue was evident. The aqueous solution was analyzed spectrographically and showed significant concentrations of chromium, iron, nickel and silicon. Inasmuch as the potassium in the loop at this time was the same charge that had been used in all earlier runs with the facility, it was concluded that corrosion during this period had led to the accumulation of excessive amounts of corrosion products which were beginning to precipitate in a manner causing the deposition of extremely adherent metallic phase.

It became apparent that the job of removing the trapped potassium and cleaning the loop required more expertise than was available within our working group if safety was to be assurred. Thus Mine Safety Appliance Company (the original fabricators of the loop) was contacted in mid 1970 concerning these difficulties. Discussions with their personnel indicated that our problems would likely require a fairly major disassembly and cleaning program to alleviate the plugging. They also indicated that sodium had been much less prone to circulation difficulties than potassium and recommended we recharge with sodium. Inasmuch as sodium was of principal interest, the decision was made to bypass the potassium studies and move directly to sodium.

Several alternatives were considered with MSA but because of time and funding limitations the decision was made to cut into the loop on-sight, removing those components in which plugs occurred, and to flush only that portion of the loop below the preheaters and the sub-coolers with a water-alcohol mixture. Once these sections were cleaned, components were welded back into the loop and sodium charged and circulated for a period sufficient to dissolve potassium in the upper parts of the loop. The sodium was then dumped and removed from the supply tank. A fresh charge of sodium was inserted and no circulation difficulties were experienced from this point forward.

While the Mine Safety Appliance personnel were on sight, it was determined that the pressure gauge would not operate with the sensitivity required to make the sub-cooling measurements necessary for the study. Thus a new gauge was ordered from MSA at that time. At the same time it became apparent that a slight leak existed in the drain valve which permitted the supply tank to communicate with the loop itself during operation. Inasmuch as the loop would be operating at sub-atmospheric pressures, it was necessary to hold the supply tank at pressures below atmospheric during operation. The existing liquid level probe was not completely vacuum tight and thus it was necessary to go to a closed induction liquid level probe. This was inserted immediately upon receiving authorization from the contracting officer.

In early 1971 insertion of the stainless steel bayonet was completed along with the new pressure gauge. Some difficulties were encountered in obtaining a vacuum tight seal on the bonnet which provided a controlled atmosphere around the upper part of the bayonnet assembly. It was finally decided to use a flowing helium system rather than vacuum as had originally been planned and these difficulties were corrected by April of 1971. Thermocouple failures caused further delays through May and June with operation finally commencing in July 1971.

During preliminary circulation and calibration runs it was observed that a leak had developed in the system which permitted the forced circulation loop to communicate directly with the natural circulation loop. Such a failure could have occurred either in the 12 mil test section wall or in a bellows located lower in the test section package. This failure made it impossible to control the pressures independently on the two sides of the system and hence the condensing temperature on the secondary side. Furthermore, leakage of sodium from the primary or forced circulation side to the natural circulation sodium system was evident. The leak did not appear to be so large that operation was impossible, but it did force abandonment of our original plans to heat the annular test section region from the outside surface.

Operation was commenced in July with heating on the bayonet surface only. The bayonet heating circuit operated perfectly, though failure of three of the five thermocouples was experienced immediately. Two of the burnout detectors remained operative and boiling studies were commenced at this point. Three runs were made with stable nucleate boiling obtained in the second run. This run was terminated without pushing the system to the burnout point because of the relatively high temperature level at which the system was then operating.

It was felt that the chances of avoiding a catastrophic failure in the bayonet would be enhanced by operating at lower pressures and temperatures and thus the system was cooled before the next run was initiated. Flow conditions at the entrance to the test section were stabilized and the heat flux to the bayonet gradually increased. No stable boiling regime was observed at the bayonet surface in this run. At a heat flux of approximately 1,649,000 Btu/hr-sq.ft. the first signs of boiling from the bayonet surface were observed with rather large temperature fluctuations and almost immediately an excursion was experienced which resulted in burnout. The two remaining thermocouples ceased to perform and upon removal of the bayonet it was discovered that melting had actually occurred and operation was terminated at this point (see Figures 5 and 6). Results of these three runs are discussed in the following section.

Results

Three runs were made before a destructive burnout of the bayonet occurred. Each run consisted of a series of step increases in the power to the bayonet for a fixed flow rate and preheater power setting. Test section inlet temperature remained essentially constant during each run under these conditions. Pressure was permitted to vary during the run as increasing heat input through the bayonet increased the amount of vapor in the system.

After each increase in bayonet power, the bayonet heat flux was determined by voltage and current measurements across the bayonet, temperatures at the inlet and outlet of the test section were recorded, and pressure and flow rate readings were made. The output of the bayonet thermocouples

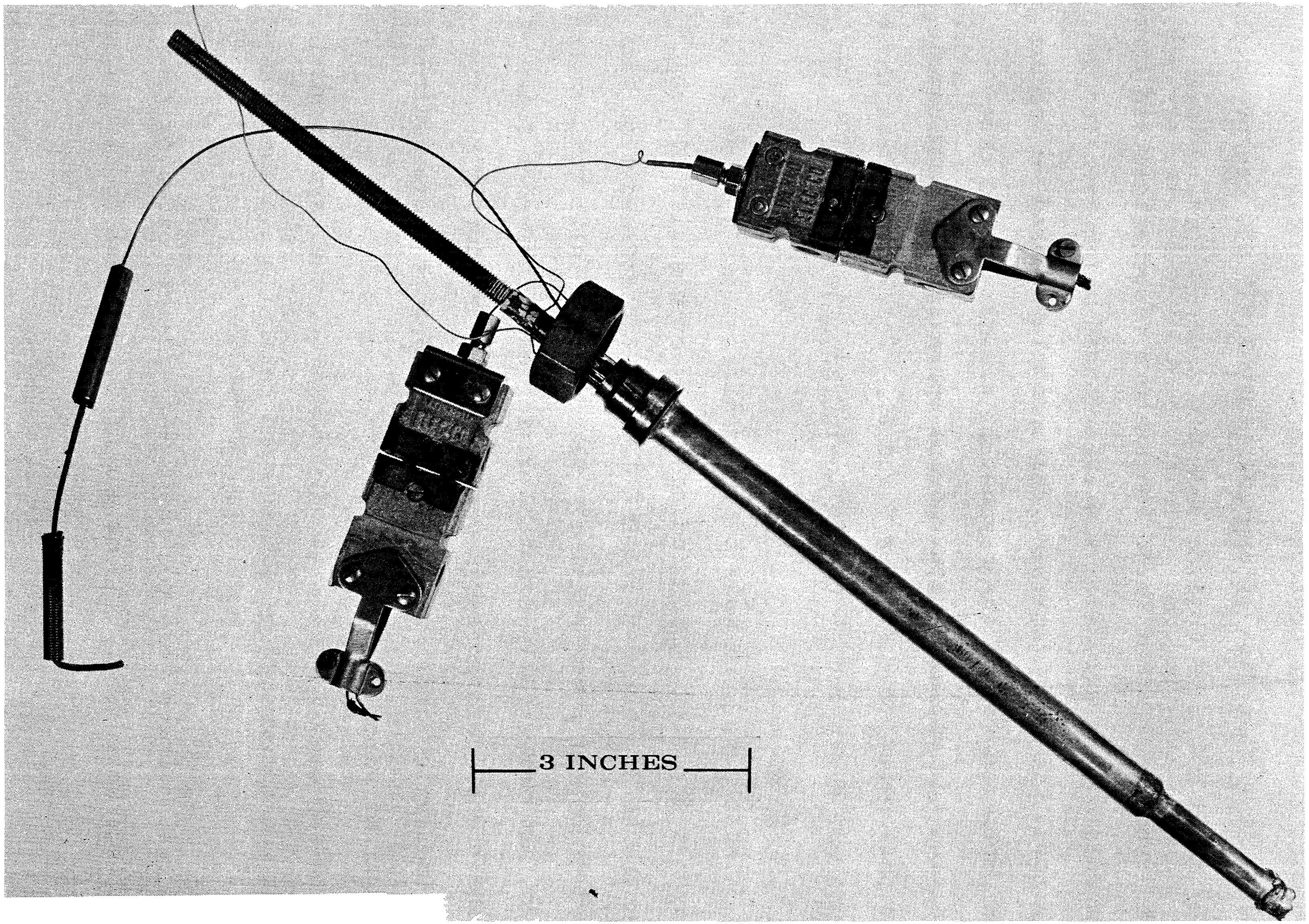


Figure 5. Bayonet Heater After Burnout

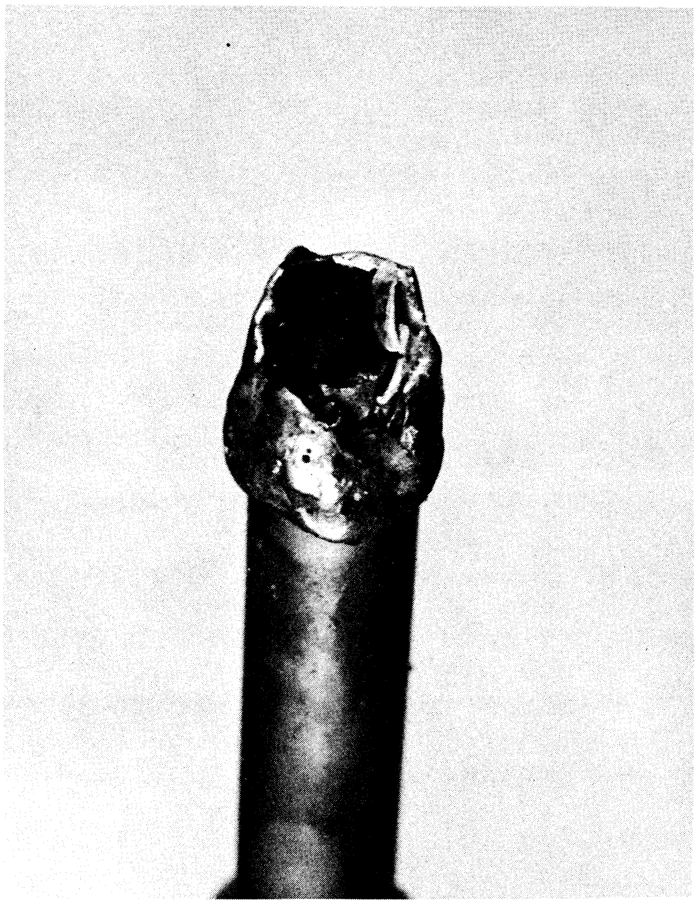


Figure 6. Melted Tip of the Bayonet Heater After Burnout

was recorded continuously and was used to indicate the onset of boiling or the occurrence of the critical heat flux condition. In the former case fluctuations as shown in Figure 7 occurred; in the latter the amplitude of the fluctuation was great enough to activate a relay interrupting power flow to the bayonet. Due to the erratic operation of the pressure gauge it was impossible to determine the pressure level and hence subcooling precisely. However qualitative observations based on test section inlet temperatures and downstream temperature measurements could be made.

Runs 1 and 2 were conducted without changing the amount of potassium or inert gas in the loop; Run 2 was made after lowering the flow rate from 2.2×10^5 lb/hr.ft^2 to 1.5×10^5 lb/hr.ft^2 and increasing the preheat power by 50 percent. These changes resulted in a test section inlet temperature increase from 1100°F in Run 1 to 1320°F in Run 2. Downstream temperature readings indicated that the pressure level in Run 1 did not exceed 4.0 psia (this is also supported by pressure readings) and that the average fluid temperature in the test section remained below saturation, even at the peak heat flux of 1,224,000 Btu/hr.ft^2.

At this flux level there was no evidence of boiling from the bayonet surface, though some vaporization may have occurred from the liquid-vapor interface in the tee just above the test section. The temperature just downstream of

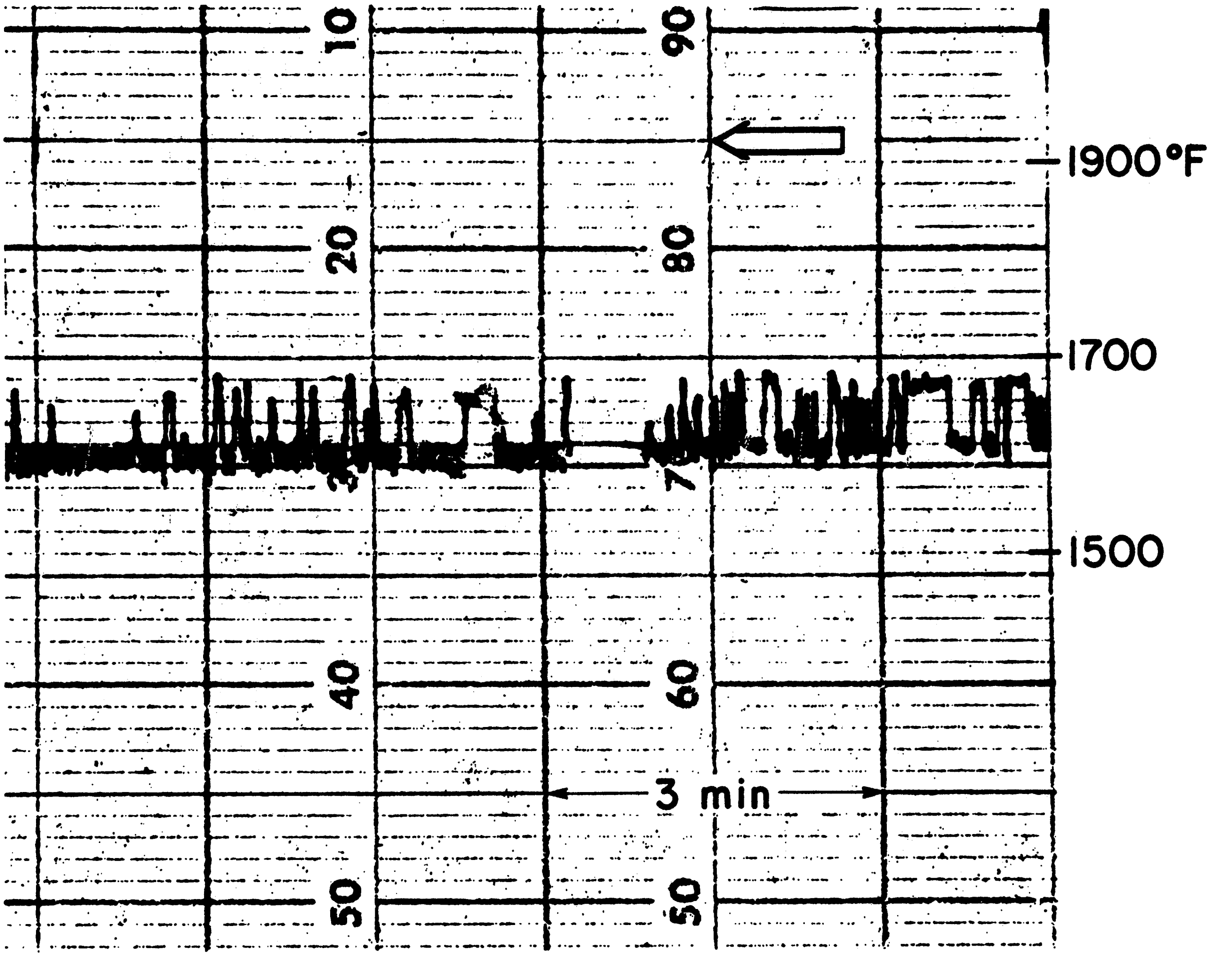


Figure 7. Bayonet Thermocouple Temperature Trace - Stable Boiling

the tee corresponded to 1340°F when the maximum flux referred to earlier was applied. This heating rate would have been sufficient to raise the fluid temperature to 1500°F in the absence of losses or evaporation above the test section outlet. Inasmuch as both likely occurred, the 1340°F reading seems reasonable. This corresponds to a saturation pressure of 2.5 psia and thus sets a lower limit on the pressure that could have existed at the time of maximum heat input. Thus it appears that the fluid entered between 250°F and 300°F subcooled in Run 1 at a pressure level of about 4 psia and for which conditions a heat flux of 1,224,000 Btu/hr.ft^2 produced neither stable boiling nor burnout at the bayonet surface.

Run 2 was commenced after the decrease in flow and increase in preheater power referred to earlier had increased the fluid inlet temperature to about 1320°F. Exit temperatures downstream of the tee increased from 1305°F with a heat flux of 45,300 Btu/hr.ft.^{2} to 1655°F with a heat flux of 1,120,000 Btu/hr.ft.^{2} At the latter condition stable nucleate boiling occurred at the bayonet surface as indicated by the temperature fluctuations experienced by the bayonet thermocouple located at the midpoint of the 2-inch test section (see Figures 3 and 7). During this period the pressure gauge just downstream from the test section fluctuated suggesting that a vapor-liquid mixture existed at the thermocouple just downstream from the tee connection. Assuming that this temperature approximates saturation, the corresponding pressure would be 16 psia.

The major difference between Run 2 and Run 1 was the decreased subcooling present at the test section inlet. In Run 2 fluid entered about 220°F above the inlet fluid temperature in Run 1 and was increased to a sufficiently high temperature within the test section so that boiling occurred at the bayonet surface at a heat flux of 1,120,000 Btu/hr.ft.^{2} This flux is below that applied in Run 1 in which no boiling occurred. In Run 2, if the energy input through the bayonet all went into sensible heat (assuming losses were negligible), the fluid temperature would have increased to 1900°F. This would result in an average test section temperature of slightly over 1600°F in Run 2 as compared to 1300°F in Run 1, (both values corresponding to the peak fluxes achieved in each run).

The bayonet thermocouple experienced fluctuations of approximately 100°F during boiling. Since the thermocouple was some unknown distance beneath the surface, actual surface temperature fluctuations likely exceeded the 100°F observed. The bayonet temperatures exceeded those of Run 1 by as much as 380°F. (The bayonet thermocouple was not calibrated and read consistently below the outlet sodium temperature. Its absolute value was thus ignored in interpreting the results.)

The conditions of Run 2 appear to lead to a lower net subcooling in the test section region in spite of the apparent pressure increase in the system. The higher bayonet surface temperature is sufficient to produce the superheat needed to nucleate at the surface.

Run 3 was separated from Runs 1 and 2 by a period of 24 hours during which time the sodium was dumped hot to the supply tank and the system cooled to about 800°F, before commencing operations again. After recharging the sodium, the loop was evacuated before heating was commenced. Thus interpretation of the results must recognize the differing pretreatment to which the bayonet surface was subjected and the possible change in oxygen level, both of which have been shown to affect incipience.

In this run the heat flux was increased in seven steps to a level of 1,659,000 Btu/hr.ft.^{2} where burnout occurred. Figure 8 shows the behavior of one of the bayonet thermocouples just prior to and after the final flux increase. Prior to achieving this level a flux of 1,473,000 Btu/hr.ft.^{2} had been sustained for 10 minutes with no sign of boiling at the surface. The flow rate was 1.63×10^5 lb/hr.ft.^{2}, but preheater power input was reduced to approximately the level of Run 1. These changes produced a test section inlet temperature of from 1036°F to 1066°F throughout the run.

Conditions prior to burnout approximated closely those achieved in Run 1 at a comparable flux level. The pressure level should have been closely related to the sodium vapor pressure as a result of the initial vacuum applied to the system. If the exit temperature is assumed to correspond to the saturation temperature this would place a lower limit on the test section pressure. This assumption leads to a

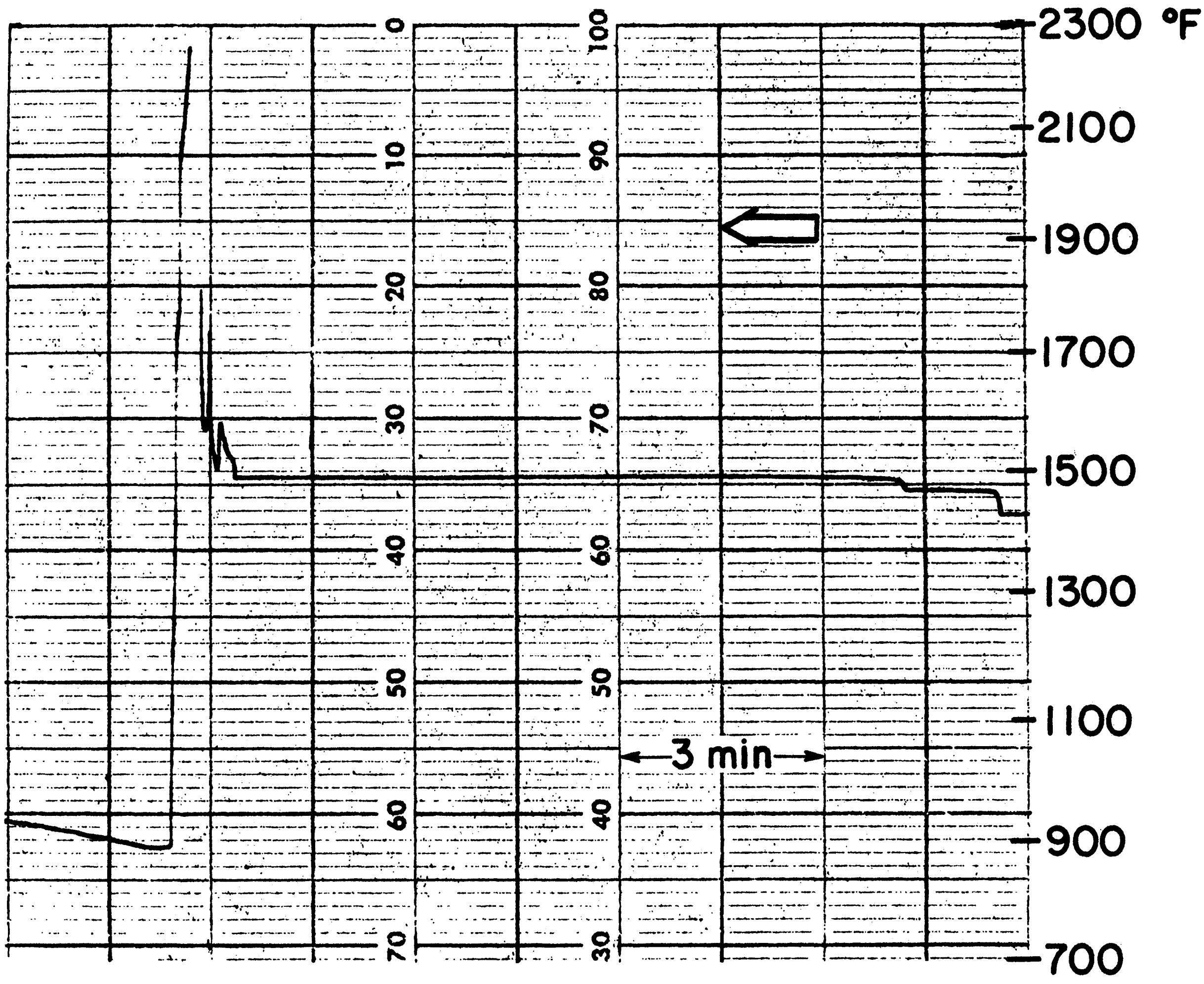


Figure 8. Bayonet Thermocouple Temperature Trace - Burnout

lower limit on the pressure at burnout of 7.5 psia (corresponding to an exit temperature of 1500°F). At the burnout flux the average fluid temperature in the test section would have been approximately 1450°F neglecting any losses. At the flux level preceding burnout an average temperature of 1320°F resulted. It thus appears that when the fluid subcooling was reduced to a point where boiling commenced the flux level was too high to sustain stable nucleate boiling and burnout occurred immediately. Such occurrences were often observed by Noyes with forced convection, subcooled flows.

Pool Boiling Studies

Objectives

The principle objective of the pool boiling portion of this study was to develop a method for predicting the critical heat flux for liquid metals and non-metallic fluids that considered both hydrodynamic and thermal transport contributions and that was fundamental in nature.

Previous work at The University of Michigan in this area was performed under contract with the U.S. Air Force and consisted of critical heat flux measurements in the saturated pool boiling of water, potassium, sodium, cesium, and rubidium.[1] These data were then correlated empirically using dimensionless groups of the properties of the liquid and vapor for the fluid being boiled[20].

Since fundamental correlations already exist for predicting the critical flux when thermal transport to the pool is neglected, namely those of Adams[7] and of Zuber[72], the approach used in this study was to attempt to develop an additional term or group of terms that would express only the thermal transport effect fundamentally.

In order to give fundamental insight into the thermal effects, pool temperature profiles in the vicinity of the heating surface were measured using a moveable thermocouple probe. These profiles are directly related to the amount of heat transferred to the pool by conduction and convection. The analytical expression obtained can be used to estimate the size of the thermal transport term.

Theoretical Model

As pointed out by Zuber[3], the energy transfer associated with the burnout point in pool boiling consists of additive contributions due to hydrodynamic instability and due to thermal transport. Therefore,

$$q_c = q_H + q_T \tag{1}$$

From Taylor and Helmholtz instability analysis it can be shown that the alternate columns of vapor and liquid flowing countercurrently to the heating surface, at the burnout point, will be spaced at a distance $\lambda/2$ apart, where λ is given by

$$\lambda = 2 \left[\frac{g_c\ \sigma}{g\ (\rho_L - \rho_V)}\right]^{1/2} \tag{2}$$

The energy contribution due to hydrodynamic instability is then

$$q_H = \frac{\pi}{24}\ \lambda_V\ \rho_V \left[\frac{\sigma g(\rho_L - \rho_V)}{g_c\ \rho_V^{\,2}}\right]^{1/4} \left[\frac{\rho_L}{\rho_L + \rho_V}\right]^{1/2} \tag{3}$$

If q_T is neglected, it has been shown that q_c is under-predicted and that the effect of pressure on q_c is over-predicted[20]. For liquid metals this discrepancy is much larger than is found for non-metallic fluids.

Various empirical correlations have represented attempts to correct equation (2) to allow for thermal transport contributions. No one as yet has reported any fundamental analysis of q_T.

Let us start with the following model in Z space:

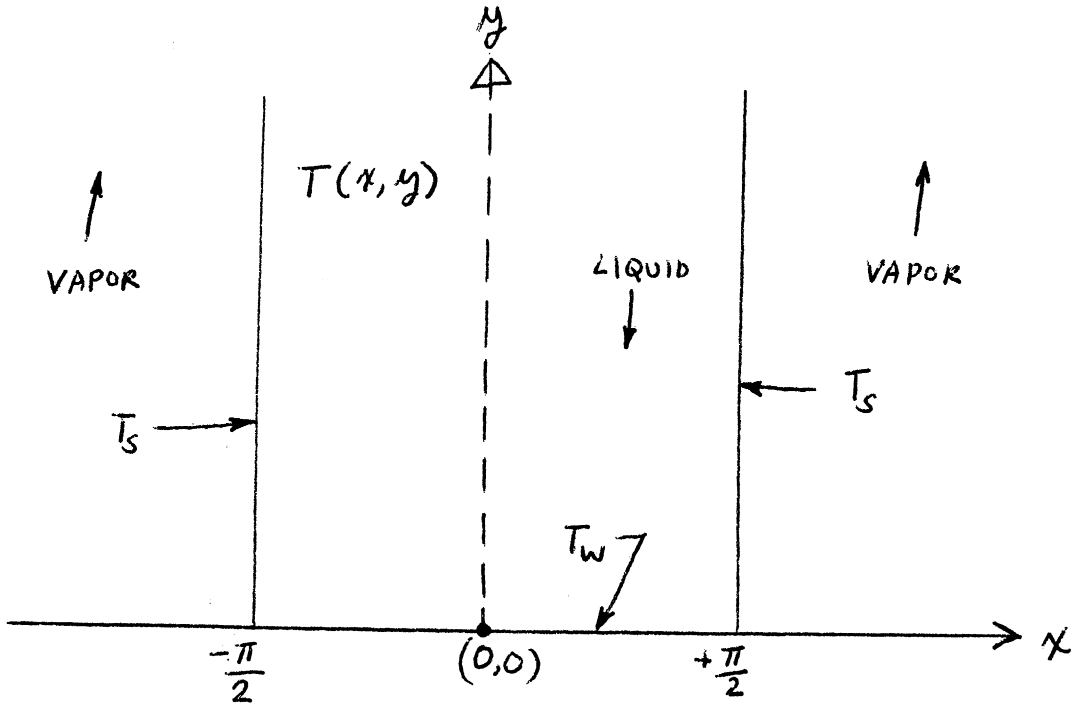


Figure 9

where Z = x + iy and where x and y are real.

This can be simplified by applying successive conformal transformations in order to obtain a geometry where the temperature field T (x,y) can be computed analytically. The application of successive inversions in reverse order will then complete the solution of the problem at hand.

It can readily be shown that the transformation in equation (4) transforms the region in Figure 9 onto the x axis, as shown in Figure 10.

$$Z' = x' + iy' = \sin Z = \text{six } x \cosh y + i \cos x \sin y \tag{4}$$

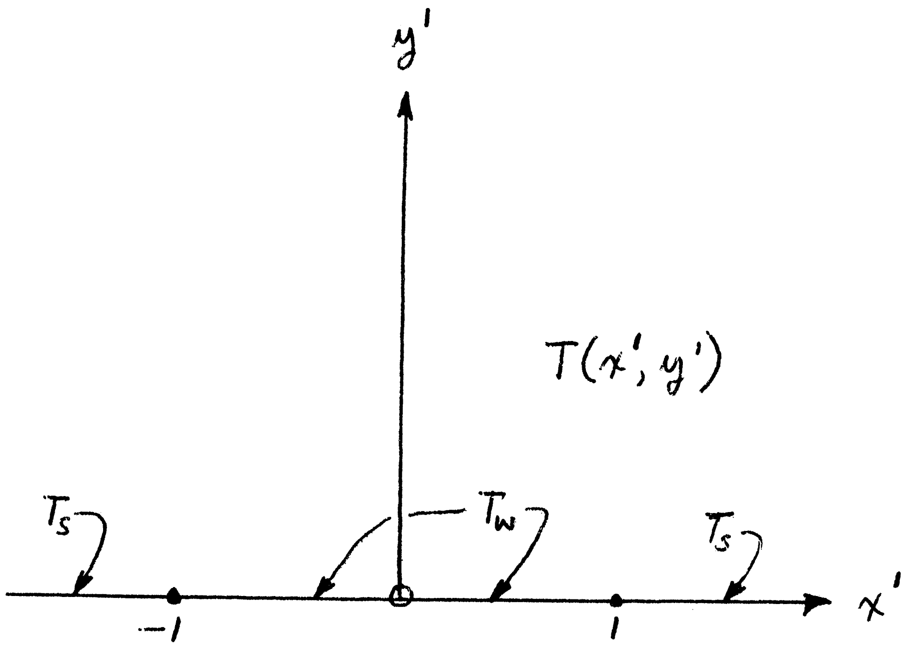


Figure 10

This region is then transformed into the infinite strip between the lines v = 0 and v = π by the transformation in equation (5).

$$w = u + iv = \ln \frac{Z' - 1}{Z' + 1} \tag{5}$$

where the imaginary part, v, can be represented by

$$v = \text{arc tan } \left(\frac{2\,y'}{x'^2 + y'^2 - 1}\right) \tag{6}$$

In w- space we now have the geometry shown in Figure 11.

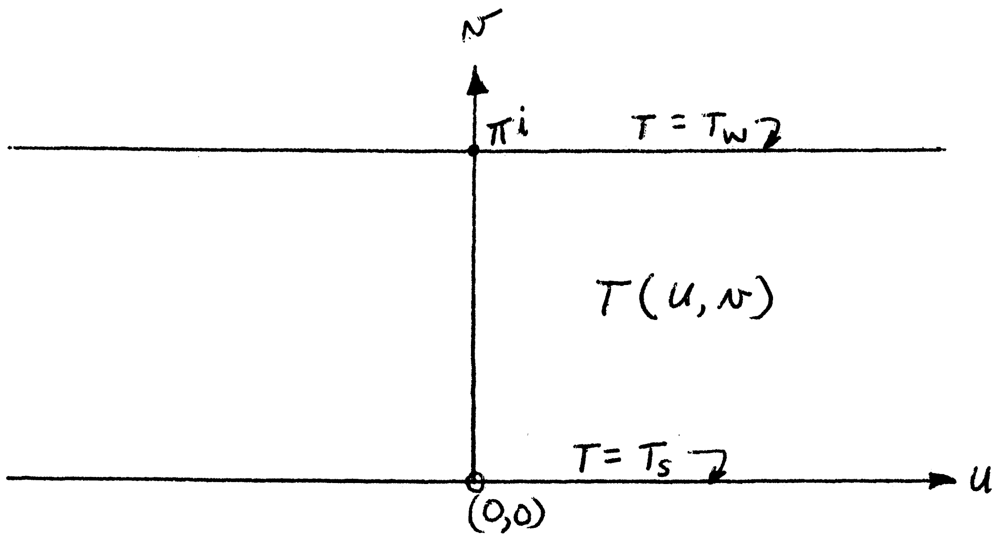


Figure 11

We have now reduced the problem to one dimensional heat transfer between two infinite planes separated by a distance π.

By inspection we have

$$T\ (u,v) = \frac{T_w - T_s}{\pi} v + T_s \tag{7}$$

The inversion procedure must be performed in reverse order. Therefore, in z'-space

$$T\ (x',y') = T_s + \frac{T_w - T_s}{\pi} \arctan\left(\frac{2\,y'}{x'^2 + y'^2 - 1}\right) \tag{8}$$

In order to transform back to Z-space, we have

$$Z' = \sin Z = \sin x \cosh y + i \cos x \sinh y \qquad (4)$$

Therefore

$$T(x,y) = T_s + \frac{T_w - T_s}{\pi} \arc \tan \left(\frac{2 \cos x \sinh y}{\sin^2 x \cosh^2 y + \cos^2 x \sinh^2 y - 1}\right) \qquad (9)$$

The denominator reduces to $\sinh^2 y - \cos^2 x$.

Therefore

$$T(x,y) = T_s + \frac{T_w - T_s}{\pi} \arc \tan \left(\frac{2 \cos x \sinh y}{\sinh^2 y - \cos^2 x}\right) \qquad (10)$$

$$= T_s + \frac{T_w - T_s}{\pi} \arc \tan \left(\frac{2 \cos x}{\sinh y}\right) \qquad (11)$$

Therefore

$$T(x,y) = T_s + \frac{2(T_w - T_s)}{\pi} \arc \tan \left(\frac{\cos x}{\sinh y}\right) \qquad (12)$$

where $\quad -\frac{\pi}{2} < x < \frac{\pi}{2}$.

We require one more transformation in order to convert the liquid stream of width π to the actual case where the width is $\lambda/2$. If we keep the same nomenclature but change the x-variable to the new variable where $-\lambda/4 < x < \lambda/4$, then we have

$$T(x,y) = T_s + \frac{2(T_w - T_s)}{\pi} \arc \tan \left[\frac{\cos \frac{2\pi x}{\lambda}}{\sinh y}\right] \qquad (13)$$

which expresses the temperature at any point, and where

$$-\lambda/4 < x < \lambda/4 \quad .$$

The flux at the wall, q_T, is given by

$$q_T = - k \left(\frac{\partial T}{\partial y}\right)_{y=0} \qquad (14)$$

Therefore

$$q_T = - \frac{2(T_w - T_s)\ k}{\pi \cos\ (\frac{2\pi x}{\lambda})} \tag{15}$$

The average flux at the wall $\bar{q}_T$, is given by

$$\bar{q}_T = \frac{4k}{\lambda\pi} \int_{-\lambda/4}^{+\lambda/4} \frac{(T_w - T_s)}{\cos(\frac{2\pi x}{\lambda})}\, dx \tag{16}$$

Equation (1) now becomes

$$q_c = \frac{\pi}{24} \lambda \rho_v \left[\frac{\sigma g(\rho_L - \rho_v)}{g_c\ \rho_v^2}\right]^{1/4} \left[\frac{\rho_L}{\rho_L + \rho_v}\right]^{1/2} +$$

$$\frac{4k}{\lambda\pi} \int_{-\lambda/4}^{\lambda/4} \frac{(T_w - T_s)}{\cos\ (\frac{2\pi x}{\lambda})}\, dx, \tag{17}$$

where $\lambda = 2\pi \left[\frac{g_c\ \sigma}{g(\rho_L - \rho_v)}\right]^{1/2}$.

Equation (17) now allows one to account for each of the energy contributions to the burnout heat flux.

The temperature profile in the liquid can be obtained from equation (13) at any y-distance from the heating surface. If we average in the x-direction, we have the following temperature profile in the pool:

$$T(y) = T_s + \frac{2(T_w - T_s)}{\pi} \text{ arc tan } \left[\frac{\frac{2}{\lambda} \int_{-\lambda/4}^{\lambda/4} \cos(\frac{2\pi x}{\lambda})\ dx}{\sinh\ y}\right]$$

$$T(y) = T_s + \frac{2(T_w - T_s)}{\pi} \text{ arc tan } \left(\frac{4/\lambda}{\sinh y}\right) \qquad (18)$$

Combining (2) and (18), we have

$$T(y) = T_s + \frac{2(T_w - T_s)}{\pi} \text{ arc tan } \frac{\frac{2}{\pi}\left[\frac{g_c\,\sigma}{g(\rho_L - \rho_V)}\right]^{-1/2}}{\sinh y} , \qquad (19)$$

which is the temperature profile in the pool.

Equation (19) is plotted in Figures 12 and 13, using dimensionless variables and a range of values for the parameters.

Previous Experimental Work

Critical heat flux data at normal gravity for saturated pool boiling of non-metallic fluids such as water and organic liquids have been obtained by several investigators.[10, 11, 14, 15,17-20,22,30,36-38,41,43,44,49,50,52,56,57,62,68,69].

Noyes[58-60], who studied sodium, was the first to report liquid metal burnout results. Balzhiser[11], Carbon[16], and Subbotin[66] have also reported sodium burnout results. Burnout data on potassium were reported by Colver[25,26], Kutataladze[4] reported results on magnesium amalgam. Caswell and Balzhiser[20] obtained data on rubidium. Adams[8] presented subcooled burnout data for magnesium. Sawle[65] has published burnout data for sulfur.

Pool boiling burnout data on liquid nitrogen and other liquified gases have been reported by Clark and Merte[23,53,54], Lyon[51], Park[61] and Robeau[64].

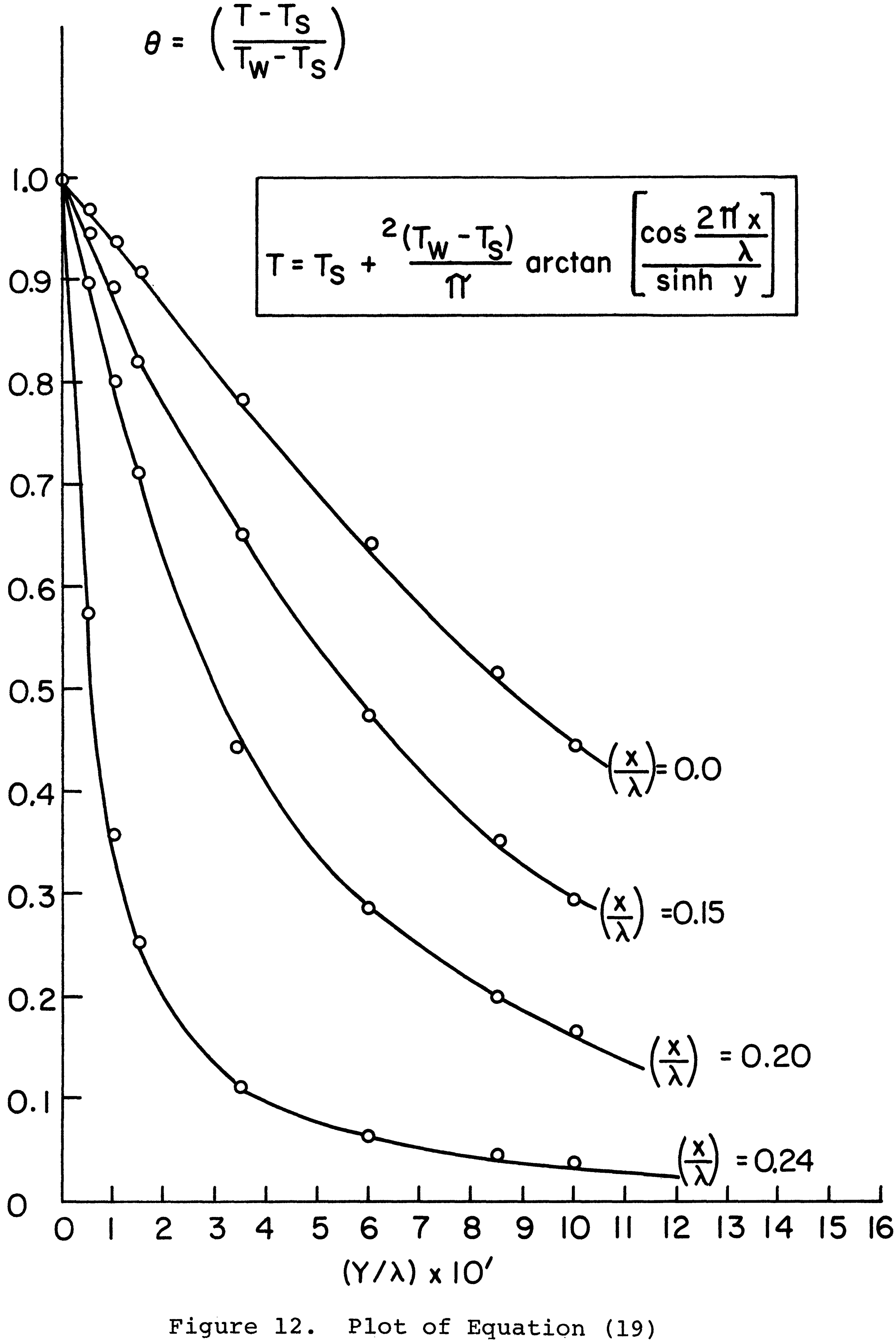


Figure 12. Plot of Equation (19)

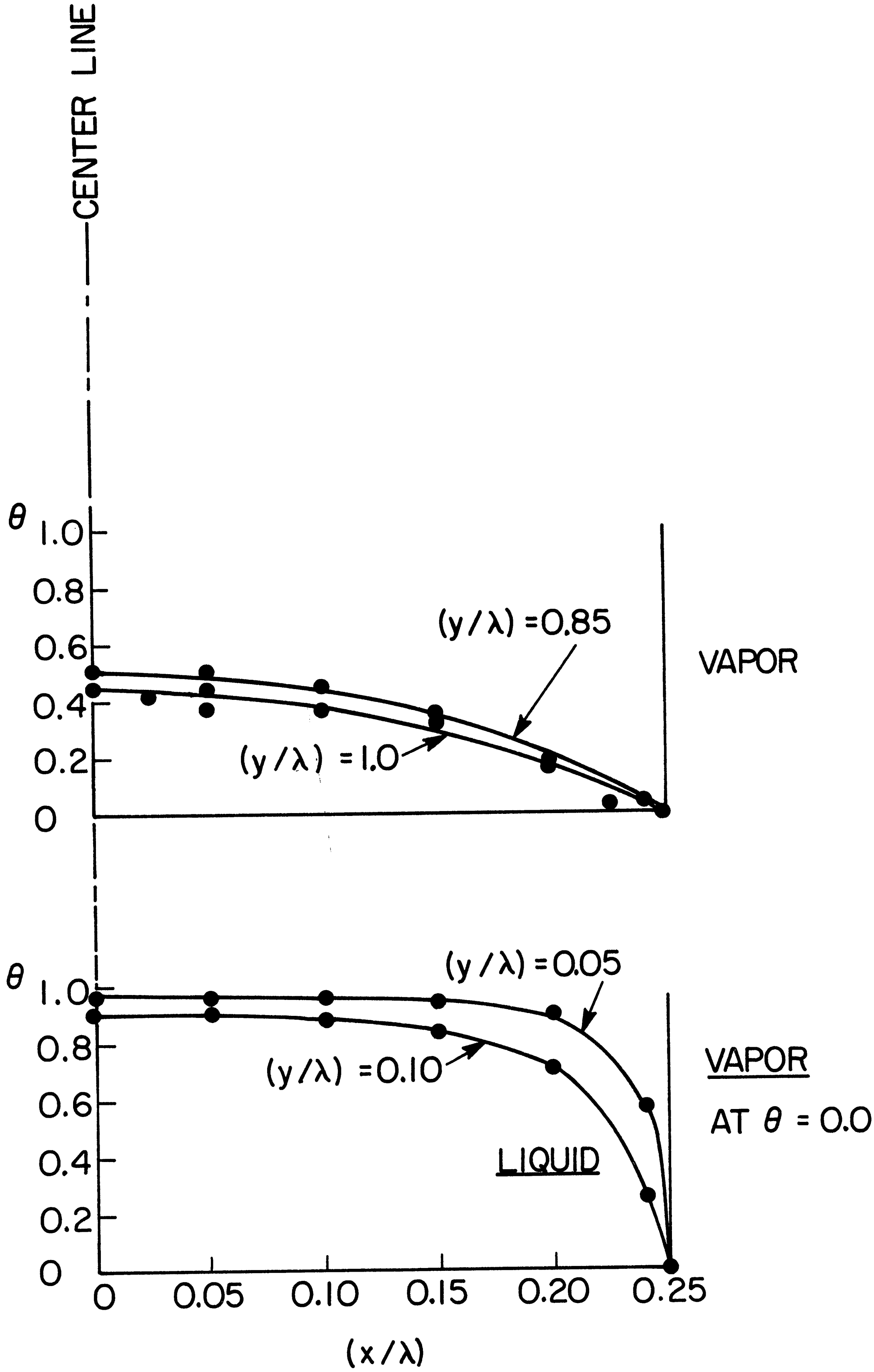


Figure 13. Cross Plot of Equation (19)

The effect on the burnout heat flux of heater conductance, thickness, and orientation has been studied in part by Averin[10], Bernath[12], Carne[17], Carne and Charlesworth[18], Ishigai[38], Ivey and Morris[41], and Lienhard and Watanabe[50]. Frea and co-workers[31] reported results obtained with a porous graphite heater having an air flow through its wall. They also determined the critical flux for water containing varying amounts of calcium sulfate. Costello and Heath[29] studied the interrelation of heater surface condition and gravitational acceleration on the critical heat flux for water and ethanol. Costello and Frea[28] measured the critical flux for water using heaters wrapped in capillary wicking.

Ivey[39,40] and Costello and Adams[27] have reported data on the effect of gravitational acceleration on the critical heat flux for water. Merte and Clark[23,53,54] have reported similar results for liquid nitrogen.

Critical Heat Flux Predictions

As noted by Gambill[33], at least fifty equations have been proposed for predicting critical heat fluxes in various types of boiling regions. Several apply to boiling with forced convection. Many of the correlations are based on data obtained on specific reactor flow channels and cannot be considered as generalized correlations. Only those generalized correlations that apply to saturated pool boiling burnout will be discussed here.

For boiling taking place on heaters of sufficient conductance and size so as not to burn out prematurely, the primary factors influencing burnout seem to be local acceleration, fluid properties, and pressure.

The correlation of Rohsenow and Griffith[63] gives a good fit of water and organic fluid pool boiling burnout data and, at the same time, contains fewer factors than most of the other commonly used equations. Their correlation is:

$$(q/A)_c = 143\ \lambda\ \rho_v \left(\frac{a}{g}\right)^{1/4} \left(\frac{\rho_l - \rho_v}{\rho_v}\right)^{0.6}$$

which is dimensional and requires the units of BTU/(hr.)(sq.ft), BTU/lb., and lb/ft.^{3}. This correlation is typical of those containing strictly hydrodynamic properties.

Addoms[9] proposed the dimensionless correlation:

$$(q/A)_c = 2.5\ \lambda\ \rho_v\ (g\ \alpha)^{1/3} \left(\frac{\rho_l - \rho_v}{\rho_v}\right)^{0.5}$$

which also agrees well with data on water and organic fluids. Sawle[65] reported that his sulfur data at atmospheric pressure were also in good agreement. Addoms' correlation differs from the purely hydrodynamic ones in that it contains the liquid thermal diffusivity α. However, it seems to overpredict slightly the effect of g which has been predicted theoretically[21,4,] and now confirmed experimentally[27,39] to be a 1/4 rather than 1/3 order effect.

Kutataladze[4,45,47] from dimensional analysis, developed the following dimensionless equation for predicting pool boiling burnout:

$$(q/A)_c = K \lambda \rho_v^{1/2} [g \sigma(\rho_l - \rho_v)]^{1/4}$$

which agreed well with a large amount of water and organic fluid data. The value of K which he now recommends is 0.13. This correlation was modified by Borishanskii[13] who showed that K is weakly dependent on a dimensionless group which includes μ. Borishanskii also showed that K decreases with pressure.

Zuber[70,71,72] developed a burnout prediction by analyzing the hydrodynamic instability of a two dimensional model having a vapor liquid interface. The equation he obtained is as follows:

$$(q/A)_c = 0.137 \lambda \rho_v \left[\frac{\sigma g (\rho_l - \rho_v)}{\rho_v^2}\right]^{1/4} \left[\frac{\rho_l}{\rho_l + \rho_v}\right]^{1/2}$$

This is essentially the same as Kutataladze's result, however it was derived by theoretical considerations and did not require the correlation of experimental data. It is in good agreement with data on water and organic fluids.

Adams[7] developed a burnout prediction from a three dimensional stability analysis of a cylindrical vortex sheet. He obtained the following equation:

$$(q/A)_c = \frac{6570\ \rho_v\ \lambda}{\left[1 + \left(\frac{\rho_v}{\rho_l}\right)^{1/2}\right]^2} \left(1 + 0.318\ \frac{\rho_l}{\rho_v}\right)^{1/2} \left[\frac{g\ g_c\ \sigma(\rho_l - \rho_v)}{\beta^2\ \rho_l^{\ 2}}\right]^{1/4}$$

The constants were obtained from $(q/A)_c$, bubble diameter, and bubble contact angle data on water. This prediction is, unfortunately, somewhat limited in usefulness at this time because of the limited availability of data on the three phase contact angle β for various combinations of fluids and solids.

Chang and Snyder[21] developed the following burnout prediction by assuming a certain arrangement of bubble sites and analyzing the buoyant and inertial forces on a bubble:

$$(q/A)_c = 1/2\ \left(\frac{\pi}{6}\right)^{5/6} (0.0119\ \beta)^{1/2}\ \lambda\ \rho_v^{\ 1/2}\ [g\ \sigma(\rho_l - \rho_v)]^{1/4} ,$$

where β is the three phase contact angle which, according to the authors, varies only within a narrow range for water and organic fluids. They also developed a similar equation by two-dimensional interfacial stability analysis. It is as follows:

$$(q/A)_c = 0.145\ \lambda\rho_v^{\ 1/2} \left[\frac{\rho_l + \rho_v}{\rho_l}\right]^{1/2} \left[g\ \sigma(\rho_l - \rho_v)\right]^{1/4} ,$$

which does not differ greatly from the equations of Zuber and of Kutataladze.

Except for Addoms, each of the forementioned investigators developed their burnout correlation by assuming that the critical heat flux in pool boiling is determined by purely hydrodynamic limitations on the two phase countercurrent flow of vapor and liquid.

It has been pointed out by several investigators[20,25,59] that, for liquid metals at moderate pressures, nearly all of the hydrodynamic-based correlations predict values of the critical heat flux that are much lower than the measured results. These correlations also overpredict the effect of pressure on the burnout flux for liquid metals. Addoms' correlation predicts values that are higher than the measured results for liquid metals and also overpredicts the effect of pressure.

Because of these discrepancies between the available correlations and his measured results for sodium, Noyes[59] developed a correlation by inserting the factor (Prandtl number)^n into Zuber's correlation. He obtained the following result:

$$(q/A)_c = 0.144 \ \lambda \ \rho_v \left[\frac{\rho_l - \rho_v}{\rho_v}\right] \left[\frac{g \ g_c \ \sigma}{\rho_l}\right]^{1/4} P_r^{-0.245} ,$$

which agreed well with his first sodium results and with water and organic fluid burnout data. The range of Prandtl numbers used in obtaining the correlation was from 3×10^{-3} for sodium to 11 for ethanol. Later Noyes[58] made a similar modification to Addoms' correlation which introduced a dependence on liquid viscosity and corrected Addoms' exponent on local acceleration. It also reduced the exponent on thermal condictivity. He obtained the following:

$$(q/A)_c = 1.19\ \lambda\ \rho_v\ (g\ \alpha)^{1/3} \left[\frac{\rho_1 - \rho_v}{\rho_v}\right]^{0.56} \left[\frac{P_r\ g_c}{g}\right]^{1/12}$$

which agrees with $\pm 30\%$ with 90% of the data used, including results on sodium and sulfur.

Caswell and Balzhiser[20], using dimensional analysis, developed a liquid metal burnout correlation that agreed well with most of the alkali metal data presently available including results on sodium, potassium, and rubidium. The correlation is:

$$(q/A)_c = 1.02 \times 10^{-6}\ \frac{\lambda^2\ \rho_v\ k}{C_p\ \sigma} \left[\frac{\rho_1 - \rho_v}{\rho_v}\right]^{0.71}$$

In order to develop a correlation that would predict both non-metallic and metallic burnout fluxes these investigators introduced the factor Pr^n and obtained the following:

$$(q/A)_c = 1.02 \times 10^{6}\ \frac{\lambda^2\ \rho_v\ k}{C_p\ \sigma} \left[\frac{\rho_1 - \rho_v}{\rho_v}\right]^{0.65} Pr^{0.71},$$

which agreed with liquid metal, water, and organic fluid results.

In view of the foregoing, it appears that both hydrodynamic and liquid thermal transport properties must be considered in predicting the critical heat flux for all types of fluids. However, thus far, all attempts at incorporating liquid thermal transport properties into this prediction have consisted of strictly empirical modifications of existing correlations. For liquid metals, these modifications raised the predicted value of the critical flux and corrected the pressure dependency to give somewhat better agreement with the experimental results. No experimental or theoretical work has yet been carried out, to our knowledge, which could enable one to gain a more fundamental understanding of the extent to which thermal transport in the liquid influences burnout. This effect should be much more significant for liquid metals than for other types of fluids and, as described previously, it is with liquid metals that the greatest discrepancy exists between the experimental results and the hydrodynamic predictions.

Burnout predictions based only on hydrodynamic considerations all contain the tacit assumption that energy from the heater is transformed directly into latent heat, causing all evaporization to occur at the heater surface. This argument is apparently valid for non-metallic fluids because of their low thermal conductivity. However, for liquid metals, it is reasonable to suspect that, in addition to the latent heat contribution, there is a substantial amount of heat being transferred to the pool by conduction (and convection) which would not cause vaporization right at the surface, but at some distance away from it. If this is true, then one would expect to observe "superheating", that is, temperature gradients existing out into the pool, even at fluxes near the burnout point. The existence of superheating in liquid metal systems has been described qualitatively by some investigators[11,25], although no precise measurements have been reported. Measurements of pool temperature profiles for water and organic compounds at low heat fluxes have been reported[24,35,42,67].

Therefore, a reasonable explanation for the measured burnout values for liquid metals being considerably higher than the values predicted by hydrodynamic-type correlations and theories, is that there is a substantial amount of heat being conducted and convected away from the heater by the pool. The purpose of this investigation is to test this hypothesis by conducting experimental work, as described in the following sections and then to develop a theoretical or semi-theoretical correlation which will include both hydrodynamic and thermal transport properties.

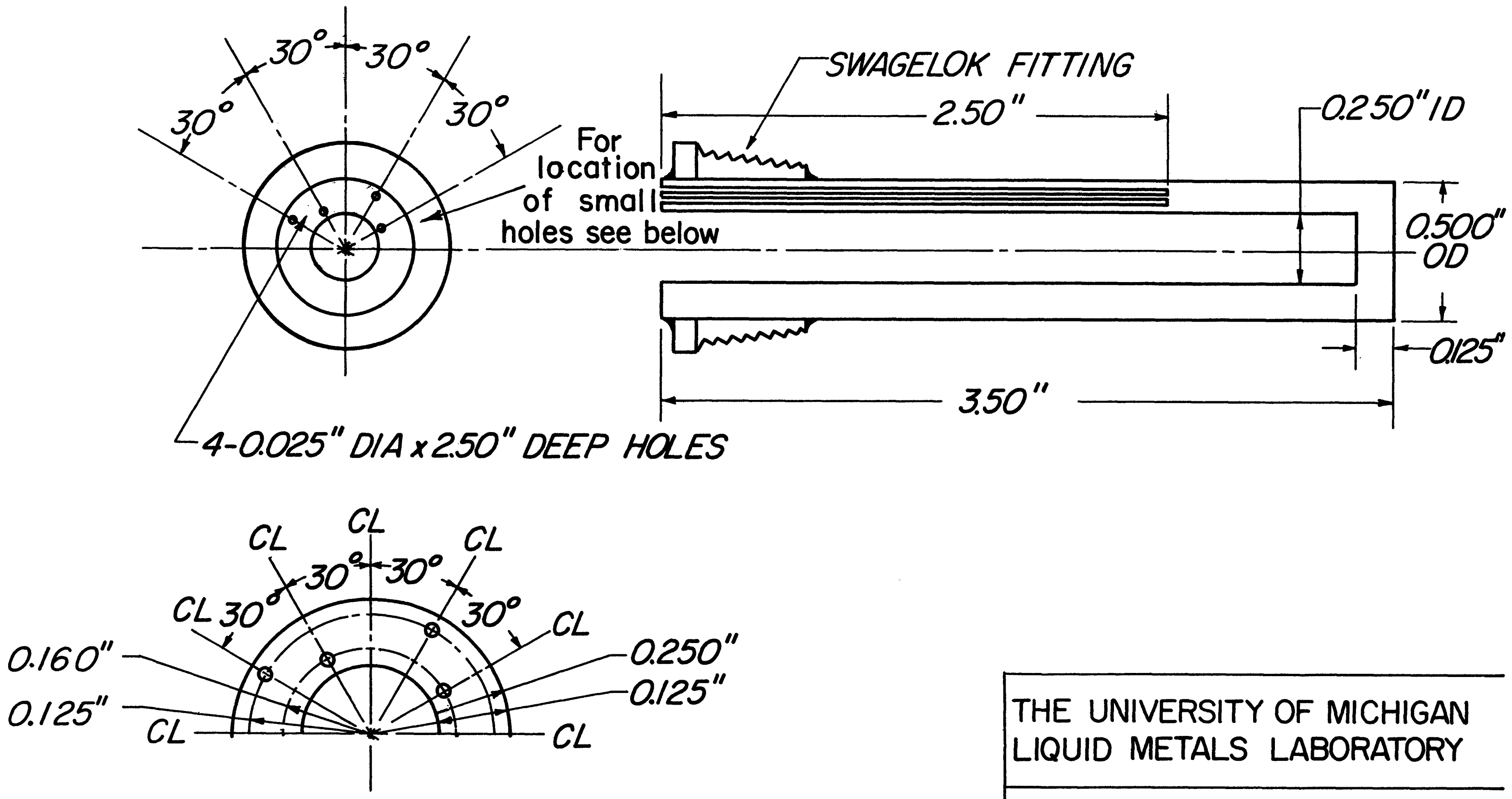
Experimental Apparatus

The experimental apparatus is described in part in Figures 14, 15, 16 and 17. The boiling vessel consists of a 16 inch by 6 inch diameter schedule 80 type 316 stainless steel pipe. The ends are 1/2 inch thick 316 stainless steel circular plate.

The electrically-heated test section is mounted horizontally through the 3/8 NPT connection in the boiling vessel. The test section is machined from 1/2 inch diameter 316 stainless steel solid rod. Four 0.025 inch diameter by 2.50 inch deep holes were drilled lengthwise in the test section wall by electrical discharge machining and these serve as thermowells. A photograph of the test section is in Figure 17 .

Four 0.025 inch diameter sheathed thermocouples were installed in the test section to measure the wall temperature at various locations as shown in Figure 15.

Nine thermocouples are located at various points in the pool as shown in Figure 15.



THE UNIVERSITY OF MICHIGAN
LIQUID METALS LABORATORY

TEST SECTION

B. CASWELL - 2/6/70

DETAIL NO. 02670

Figure 14

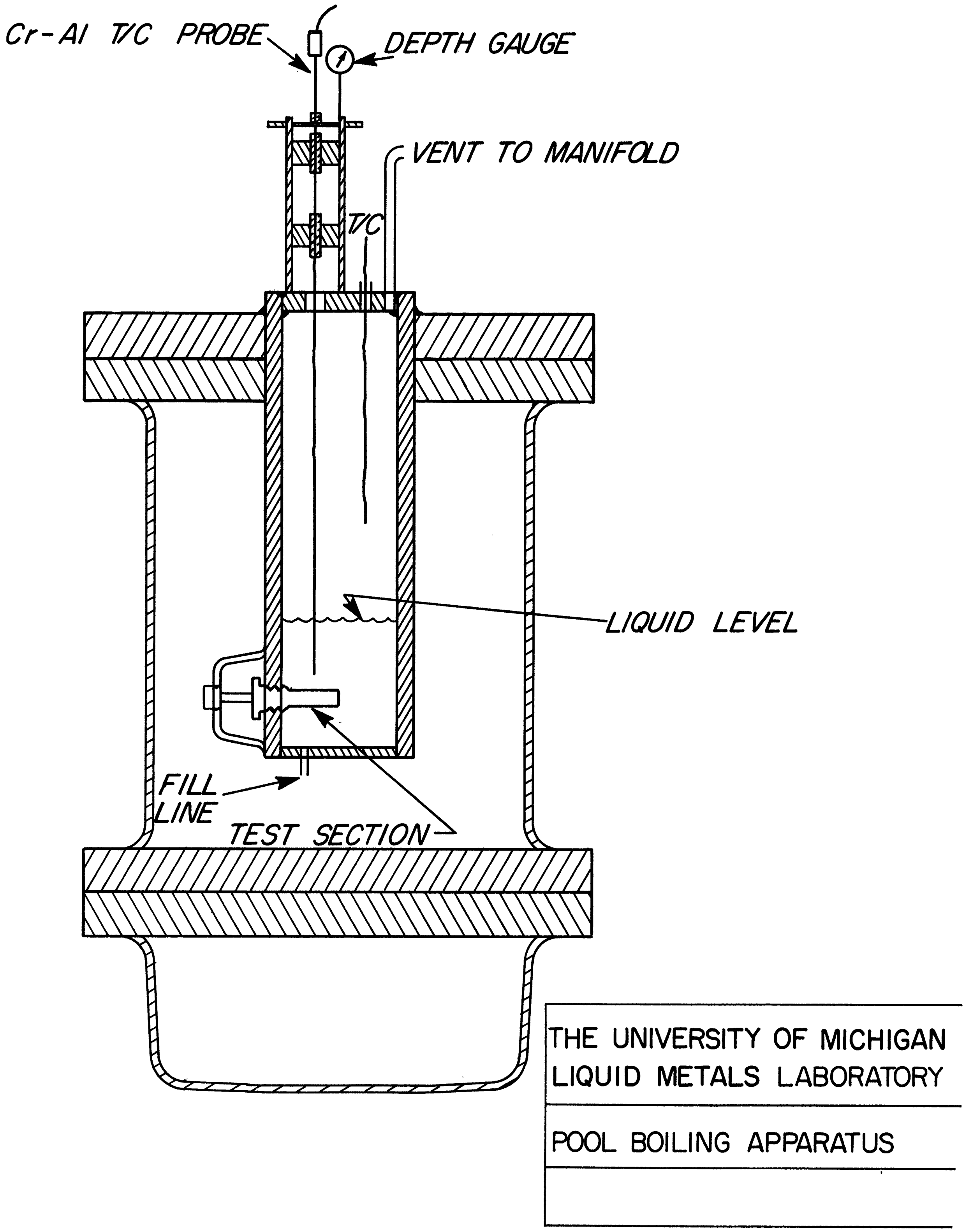

Cr-Al T/C PROBE
DEPTH GAUGE
VENT TO MANIFOLD
T/C
LIQUID LEVEL
FILL LINE
TEST SECTION
THE UNIVERSITY OF MICHIGAN
LIQUID METALS LABORATORY
POOL BOILING APPARATUS

Figure 15

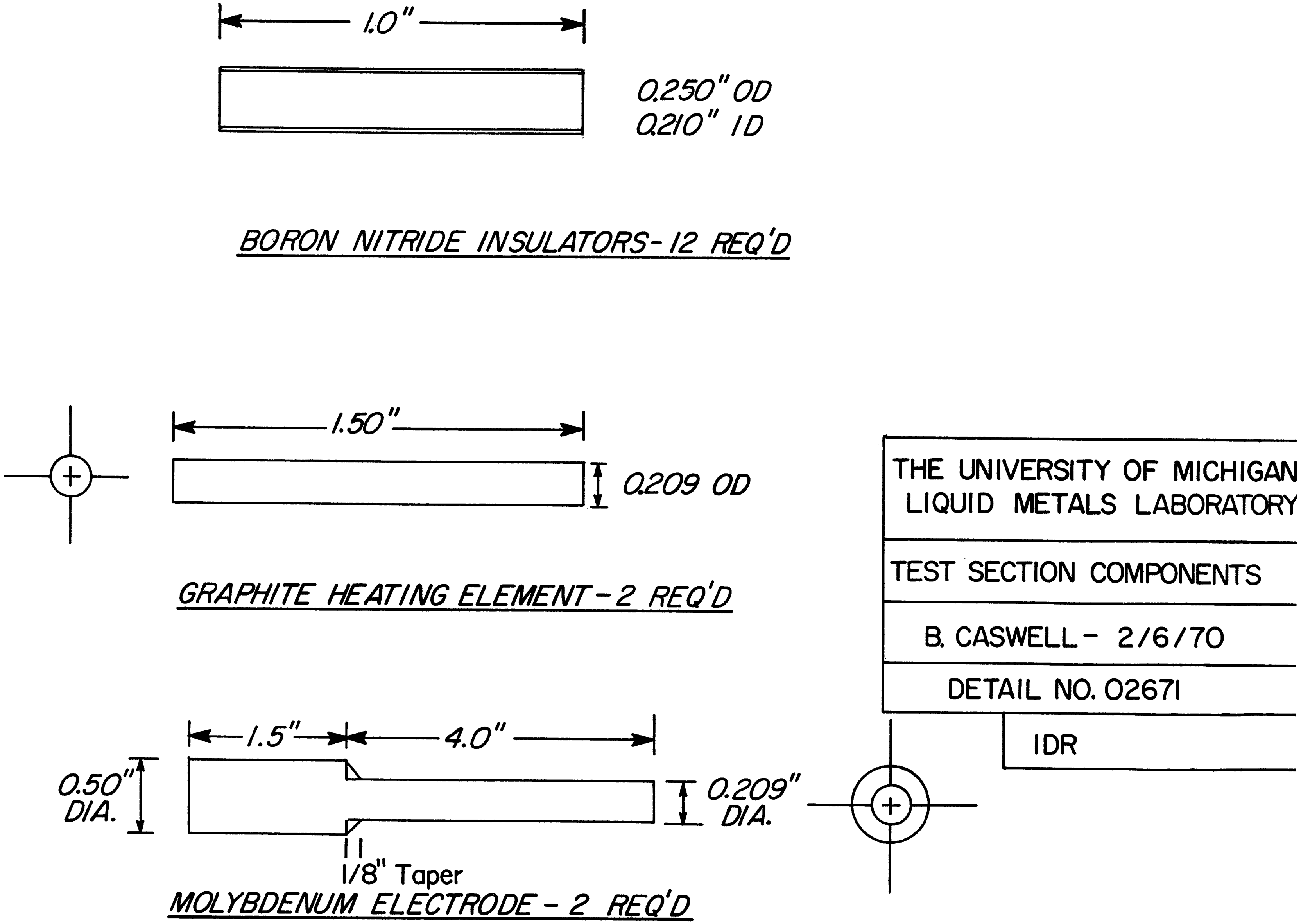

1.0"
0.250" OD
0.210" ID
BORON NITRIDE INSULATORS- 12 REQ'D
1.50"
0.209 OD
GRAPHITE HEATING ELEMENT - 2 REQ'D
1.5"
4.0"
0.50" DIA.
0.209" DIA.
1/8" Taper
MOLYBDENUM ELECTRODE - 2 REQ'D
THE UNIVERSITY OF MICHIGAN
LIQUID METALS LABORATORY
TEST SECTION COMPONENTS
B. CASWELL - 2/6/70
DETAIL NO. 02671
IDR

Figure 16

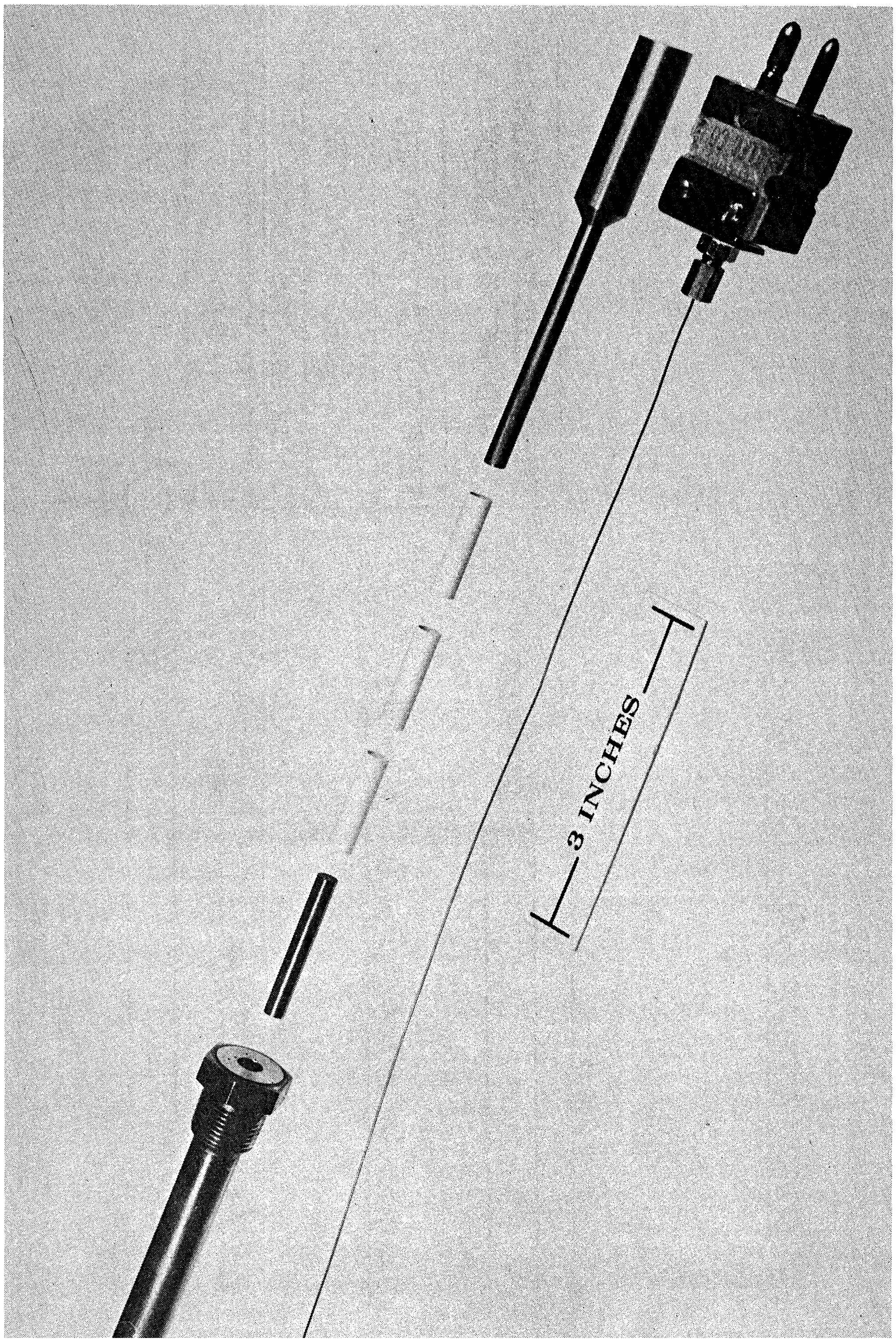


Figure 17. Test Section

A 30 inch long by 0.093 inch diameter thermocouple is located directly above the mid point of the test section heating element and is mounted in a double seal gland assembly shown in Figure 15. The thermocouple probe is connected to a fine adjustment ring and a depth gauge which allows it to be positioned vertically at measured vertical distances away from the heating surface in the pool. The depth gauge can be read in increments of 0.0005 inch up to a total distance of one inch.

Seven thermocouples are located at various points on the outer wall of the boiling vessel in order to provide control points from the guard heaters.

The guard heaters consist of two ceramic "clam shell" heater assemblies that are mounted completely around the boiling vessel.

All of the thermocouples are connected to a panel board and terminate at standard phono jacks.

Also terminating at the panel board are the various recording and measuring instruments, consisting of a 12-point Honeywell Multipoint Temperature Controller, a 12 channel Honeywell "Visicorder" oscillograph, 3 Leeds and Northrup adjustable zero-adjustable range temperature recorders, and a Brown potentiometer. Using standard patch cords, any of the thermocouples can be connected to any input on the various recording and measuring instruments.

The boiling vessel is surrounded by an outer chamber consisting of a pool piece made of 14 inch schedule 40 pipe, 4 ASA 400 lb. flanges and a 14 inch pipe cap.

The containment vessel and the boiling chamber are piped into a manifold in such a manner that either argon or vacuum can be applied.

The apparatus is filled with potassium through a stainless steel bellows-seal valve at the bottom of the vessel. An auxiliary storage vessel, or dump tank, is provided in order that the boiler can be drained for charging test sections.

Experimental Runs

The pool boiling apparatus was operated for a large number of runs. However, for the reasons described in Appendix B only one of these runs was successful to the extent that a pool temperature profile near the heating surface was obtained at a flux close to the critical heat flux. All of the runs are summarized in Appendix B.

Discussion of Results

Figure 18 shows a plot of Colver's burnout data on potassium[26] compared with Zuber's[3] prediction for the burnout flux at various pressures. Also plotted is the difference, q_{C_T}, between the two curves. This quantity, q_{C_T}, is an estimate of the amount of heat contributed to the critical flux by heat transfer directly to the pool and is relatively independent of pressure at a value of about 375,000 Btu/hr.ft.^{2} The theoretical development discussed e a r lier showed the relationship between this quantity and the pool temperature profile near the burnout point.

Figure 19 shows a comparison between the pool temperature profile for water at $(q/q_c) = 0.91$ measured by Bobst[73] in comparison with the profile predicted by the theoretical model. Reasonable agreement is observed between the two

Similarly Figure 20 shows a comparison between the temperature profile measured with potassium in run no. 14 with the calculated profile in which $\lambda = 0.400$ inches. Unfortunately this profile was measured at only $q/q_c = 0.85$ instead of a point closer to the critical flux. It was hoped that temperature profiles could be obtained at a range of value of (q/q_c) from 0.1 to 1.0. However, only the data shown were obtained.

The calculated profiles are in qualitative agreement with the measured data and a larger amount of data should be obtained in order to determine whether the model can be considered valid. After this is accomplished, Equation (17) represents the resulting prediction for the critical heat flux.

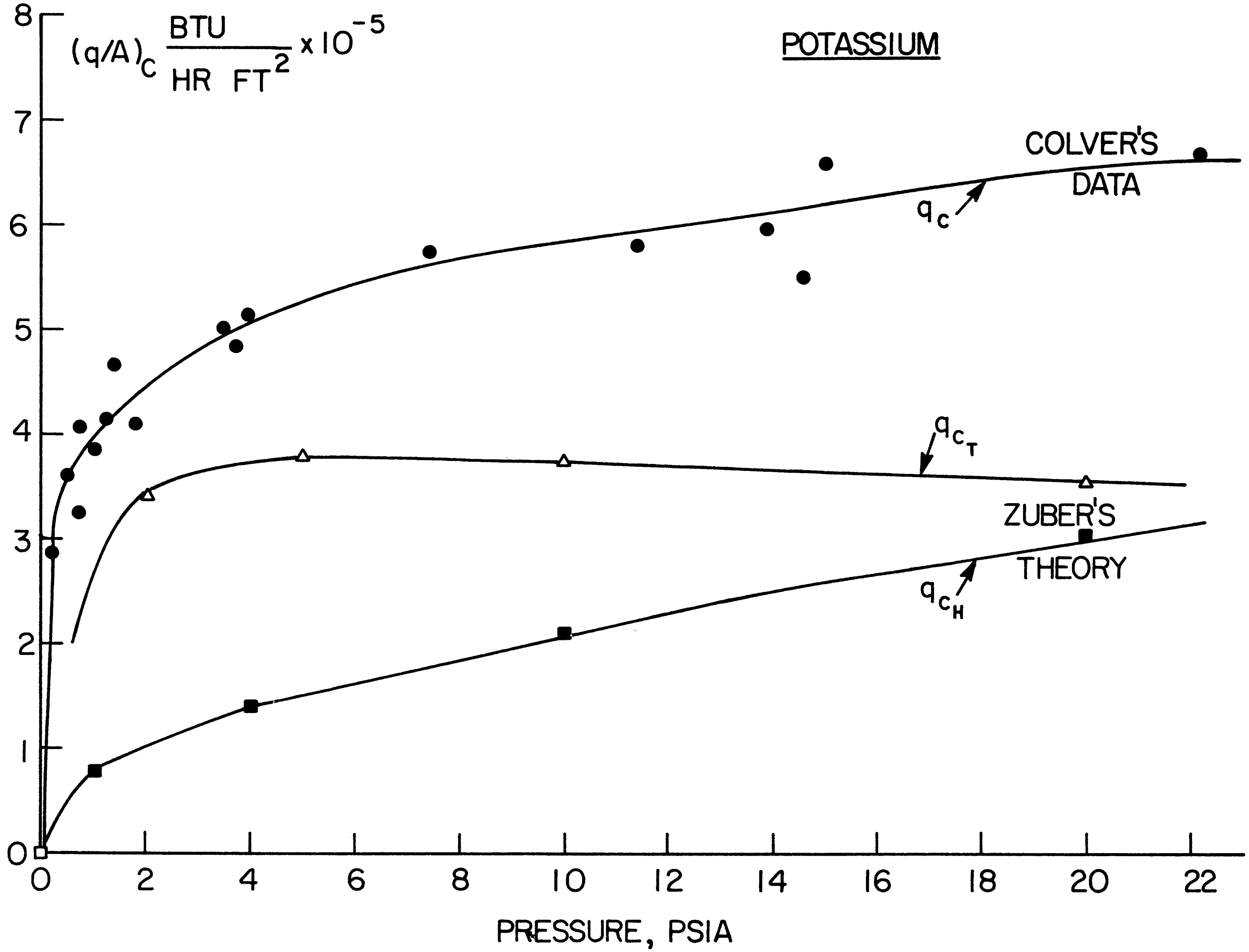
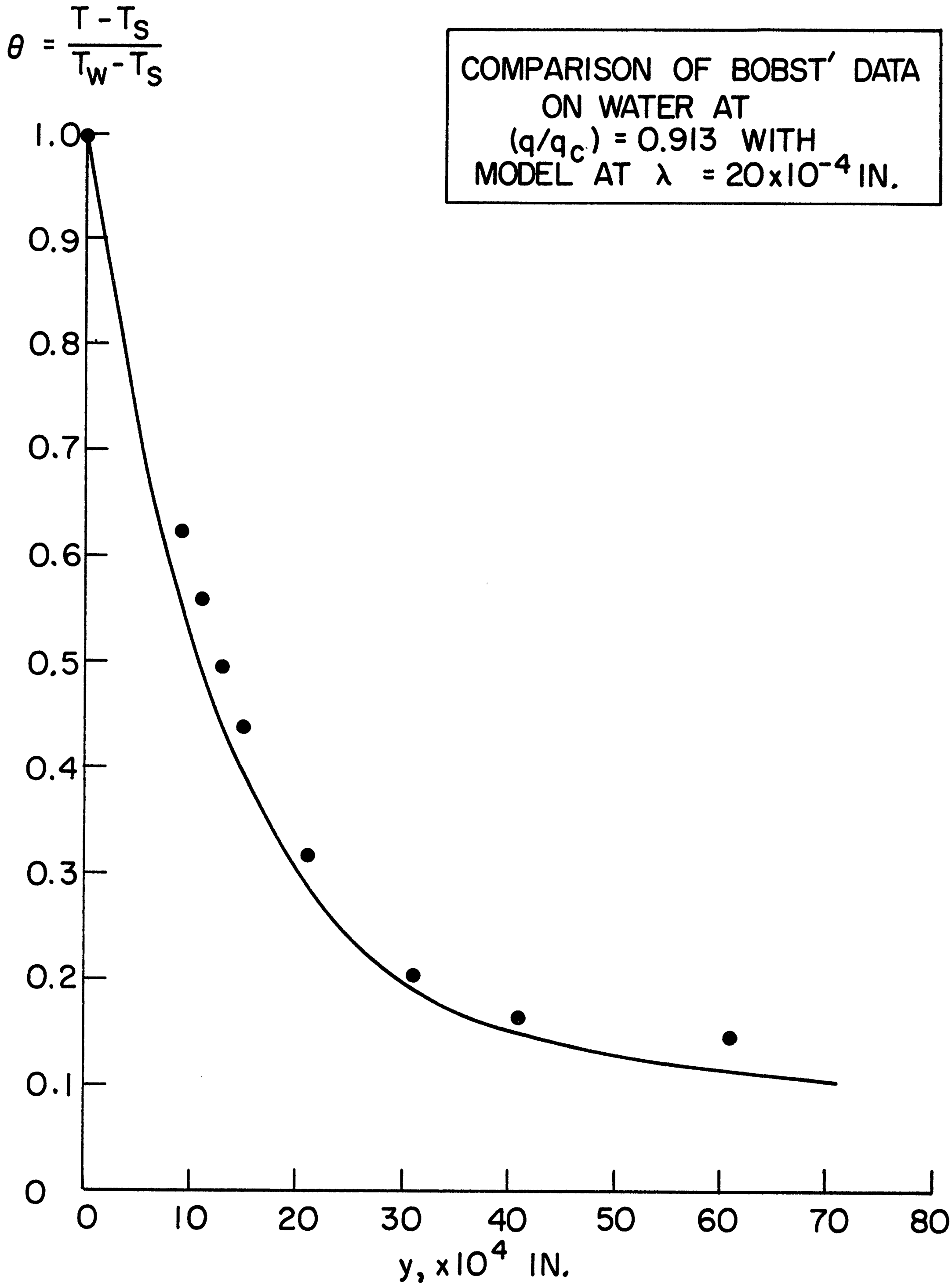
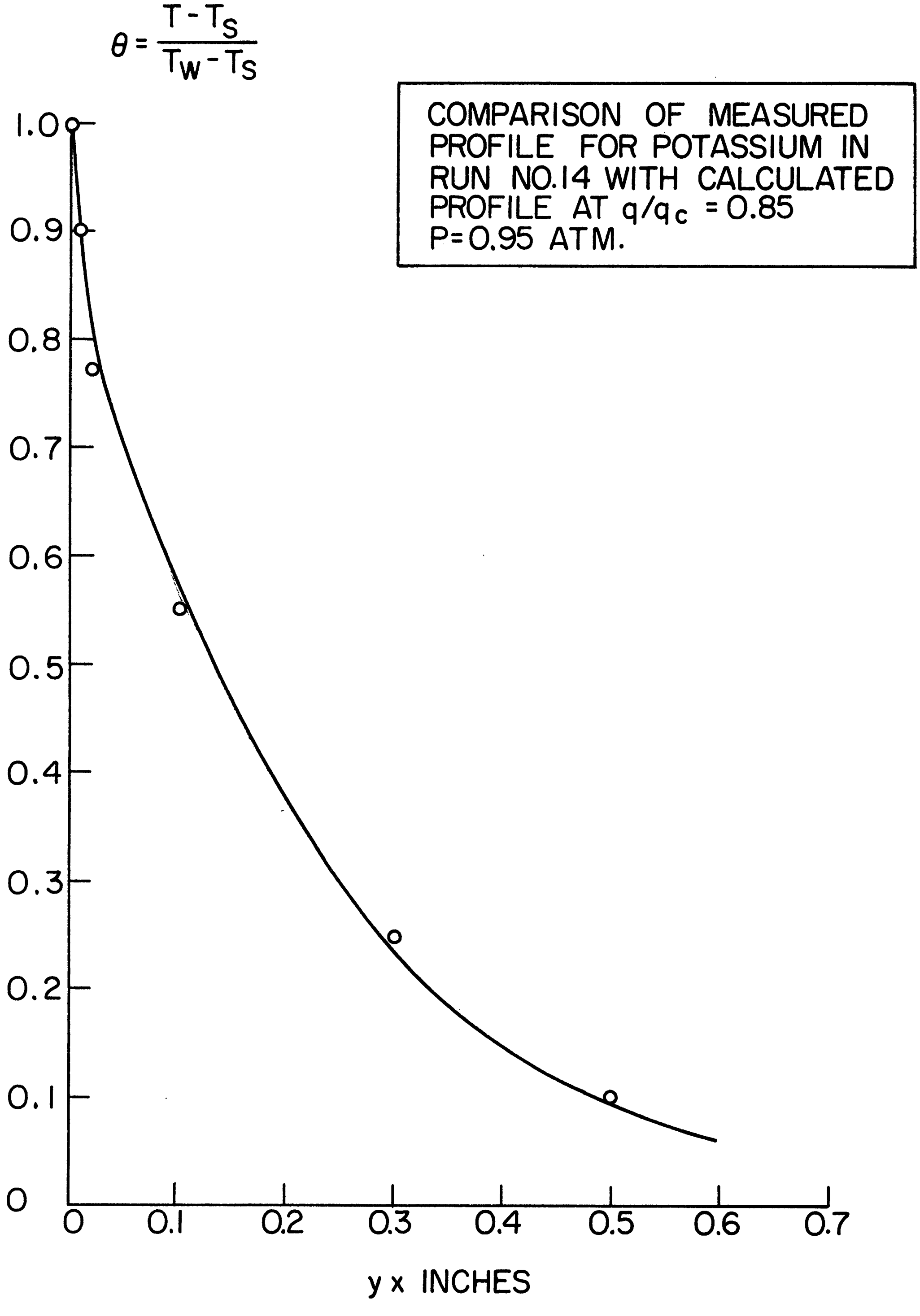


Figure 18. Comparison of Potassium Burnout Data with Zuber's Theory



$\theta = \frac{T - T_S}{T_W - T_S}$
COMPARISON OF BOBST' DATA ON WATER AT $(q/q_c) = 0.913$ WITH MODEL AT $\lambda = 20 \times 10^{-4}$ IN.
1.0
0.9
0.8
0.7
0.6
0.5
0.4
0.3
0.2
0.1
0
0
10
20
30
40
50
60
70
80
$y, \times 10^4$ IN.

Figure 19



θ = (T - TS) / (TW - TS)
COMPARISON OF MEASURED PROFILE FOR POTASSIUM IN RUN NO.14 WITH CALCULATED PROFILE AT q/qc = 0.85
P=0.95 ATM.
1.0
0.9
0.8
0.7
0.6
0.5
0.4
0.3
0.2
0.1
0
0
0.1
0.2
0.3
0.4
0.5
0.6
0.7
y x INCHES

Figure 20

Appendix A - Log of Forced Circulation Activity

February 6, 1968

1. Loop modification design completed.
2. Bayonet heater designed for 1.8×10^6 Btu/hr.ft.^{2} (previous design abandoned because of fabrication difficulties) (equivalent diameter for a pitch to diameter ratio of 1.16 - a candidate for the FFTF).
3. Preheater heating elements replaced and insulated.
4. D.C. power supply hooked up.
5. Instruments calibrated and repaired.

March 11, 1968

1. Bayonet partially fabricated.

April 12, 1968

1. Material ordered for loop modification.

May 3, 1968

1. Fabrication of bonnet and flange have begun.

June 5, 1968

1. Machining of bayonet pieces in progress.
2. Loop modification partially complete.

July 11, 1968

1. Dummy bayonet fabricated to permit alignment of pieces of modified loop assembly.

August 6, 1968

1. Machining of bayonet complete.

September 16, 1968

1. Loop modification being completed.

October 14, 1968

1. New shunt designed based on problems experienced in pool boiling experiments (Mo shunt bent, breaking insulation and allowing short against pool wall).

November 7, 1968

1. Loop cut open for insertion of modified test section assembly - internal alignment problem caused delay.

December 9, 1968

1. Loop modification complete.
2. Bayonet heater welding completed.

January 14, 1969

1. Bayonet welding problem - cracks at welds.

February 10, 1969

1. Bayonet rewelded.

March 11, 1969

1. Problems with sleeve to Mo joining so sleeve machined from Mo.

April 11, 1969

1. Mo sleeve being brazed to heater.

May 15, 1969

1. Mo sleeve brazing completed.

July 10, 1969

1. During attempted start-up of loop with bayonet in place it was discovered that the bayonet had cracked along a weld and was leaking.

NOTE: All of the above items are summarized in the September 28, 1970 report along with more which follows.

The sequence of events for the next year is as follows:

1. Instrumented dummy heater fabricated and installed in loop.
2. Attempted to circulate potassium and could not do so, indicating a plugged line.
3. Attempts were made to heat to 1200°F using trace heaters and to remove the plug. This failed.
4. The loop was cut open in several places and an attempt made to clean out points where plugs were likely to be found. This was only partially successful indicating more than one plug remaining.
5. MSA was contacted to clean out loop and recharge with Na.
6. Clean out, recharging and reinsulation completed by Fall 1970.

October 1970 through December 1970

1. Bayonet fabrication completed.
2. Check out of important loop instrumentation completed.

January - April 1971

1. Pressure transmitter received and installed.
2. Bayonet installed.
3. NaK bubbler and helium purge system set up for bonnet enclosing exposed portion of bayonet heater.
4. Vacuum system set up for bonnet evacuation and evacuation of back of pressure transmitter diaphragm.

May 1971

1. Bayonet heater circuit connected.
2. Thermocouples in bayonet connected after several were broken.
3. Attempted to evacuate bonnet, failed and finally settled on a longer helium purge.

June 1971

1. Attempted to circulate. Initially experienced problems due to poor reinsulation at some points. Finally corrected problems and established circulation.

July 1971

1. Circulated several times and dumped hot to remove as much oxide from circulating system as possible.
2. Discovered hole in annular wall between primary and secondary sides.
3. Hole had allowed Na to accumulate in secondary side. This accumulation was pushed back over to the primary side by reversing the pressure difference.
4. Decided to attempt to run with hole by maintaining low pressure differential between sides.

Last of July 1971 - Runs

1. Initially destroyed several thermocouples until only two remained operative.
2. First run - no boiling.
3. Second run - stable boiling.
4. Third run - burnout, bayonet destroyed.

Appendix B - Summary of Pool Boiling Runs

Run #1. During start-up, all four of the test section thermocouples failed. These were 0.012" diameter platinum rhodium thermocouples made by High Temperature Instruments Company.

Run #2. As before, the test section thermocouples failed during start-up. This run was made with no change in design or supplier.

Run #3. Test section thermocouples were purchased from Omega Engineering Company which were essentially the same design as those used previously. These also failed during start-up.

In each of the first three runs the test section thermocouple failures occurred at temperatures of 800-1200°F, substantially below normal operating temperatures.

Throughout these studies, thermocouple failures occurred by two basic mechanisms. The most common type of failure was the development of a junction between the lead wires inside the sheath. The other type of failure was the creation of an open circuit due to wire breakage in the sheath.

It was decided that larger diameter thermocouples would be more durable, since in previous work we were able to obtain reasonable results with 0.020" diameter thermocouples.

In order to accommodate the larger thermocouples, the test section wall thickness was increased by increasing the outside diameter from 7/16" to 1/2". Because of the increased wall thickness it was decided to substitute molybdenum for stainless steel for the heater material in order to minimize the internal temperatures in the heater. No satisfactory joining method involving brazing or welding could be readily found for joining the heater body to the 316 stainless steel threaded fitting, so a Swagelok fitting was ueed with Nicrobraze as back-up. However, the braze material cracked during cooling.

Run #4. Shortly after start-up, before the flux reached 50,000 Btu/hr.ft.^{2}, a leak developed through the Swagelok fitting and cracked brazed material. Some potassium leaked out and shorted the system.

The heater was then redesigned with a welded joint between the heater body and threaded fitting using 316 stainless steel for both parts. Because of the loss of potassium in Run #4, it was decided to drain and clean the boiler and recharge fresh potassium. At this time, clam shell guard heaters were installed in place of the asbestos wrapped coils.

Run #5. Shortly after start-up, as the pool was being heated to 1000°F, electrical continuity was lost in the heater. The resistance had increased from 0.02 to 2000 ohms. It was found that the resistance could be lowered to 2 ohms by tightening the heater electrode extremely tight. However, this was not low enough to operate and upon further tightening the molybdenum shunt was bent and shorted to the heater after cracking the boron nitride insulation.

Run #6. This run was a repeat of Run #5 except that when the heater was assembled, some graphite powder was placed at the bottom of the center hole and a small groove was placed lengthwise in the side of the graphite element in order to allow gases to escape.

This heater also lost continuity when the pool temperature had reached 1000°F during heat-up.

Run #7. This was a repeat of Run #6 except that the graphite powder was omitted and, instead, the bottom of the center hole was remachined in order to achieve better contact with the resistance element.

During heat-up the pool temperature probe developed an open circuit. It was discovered that the thermocouple had cracked at the point where it enters the top packing gland. Both sheath and insulation were cracked and the sheath was corroded at the Teflon gland. The probe assembly had been purchased from the Mo-Re Corporation. A replacement was obtained from Continental Sensing Company.

Run #8. The equipment was restarted, but heater continuity could not be maintained above 1000°F.

The heater was redesigned with rhenium discs (0.015" thick) on each end of the graphite heating element in order to prevent the formation of carbides at the inferfaces. A small quantity of powdered tungsten was placed at the bottom of the center hole in order to aid electrical contact between the heater body and the rhenium disc.

Run #9. As before, the heater lost continuity at essentially the same temperature as before, in spite of the design changes.

At this point it was theorized that the relatively small diameter (0.088 inch) of the graphite resistance element could be responsible for the continuity difficulties. Previous work with 0.230 inch diameter elements was nearly free of this type of problem.

Therefore, the heater was redesigned so that the resistance element was increased to 0.209 inch diameter. Drawings of this most recent design are attached (Figures 15 and 16).This larger diameter allowed the center hole of the heater to be drilled, reamed and end-milled at the bottom instead of EDM'd as before. This gave a much flatter contact surface than before.

Run #10. This run was made using the new heater design described above.

During the start-up procedure 24 hours of preheating at 1400°F using the guard heaters only was performed before the test section heater was turned on. The purpose of this preheating step was to allow sufficient escape of gases from the contact surfaces and to allow contact resistances to diminish before passing current through these surfaces. It was theorized that without this step, arcing might occur across these areas of contact resistance, causing very high temperatures and resultant development of increased contact resistance.

It was found that this heater, after the above pretreatment, behaved perfectly. Stable nucleate boiling was obtained at pool temperatures of 1400°F. However, during the first measurement of the pool temperature profile, the probe failed, developing a junction approximately half way up the sheath.

It was then necessary to disassemble the heater in order to remove it so that the replacement probe could be realligned by sighting through the test section opening in the boiler.

Run #11. The equipment was then restarted as in the previous run. However, this time the heater lost continuity at approximately 800°F. In addition, three of the four test section thermocouples failed.

Run #12. The test section internals were drilled out and a new set were installed. Once again, test section thermocouples were purchased from a new source, Industrial Temperature Control Company. The system was heated up to 1400°F and was starting to enter a region of stable boiling when the heater circuit shorted. Subsequent disassembly showed that the test section wall developed a hole such that potassium leaked into the center of the unit.

Run #13. This run was made with no design changes from Run #12. During the early stages of start-up before a temperature of 400°F was attained, the pool temperature probe failed by developing a junction about 14" from the tip. It was not possible to dump the potassium at this point, apparently because of a plugged drain line or drain valve. Therefore, the drain line and valve were cut off, the potassium drained into an open container, and the system was completely cleaned with acetic acid, water, dilute HCl, and more water. A new drain line and valve were welded onto the boiler and the system was recharged. A new pool temperature probe was installed.

Run #14. This run was successful in yielding one pool temperature profile and is discussed in the next section of this report. During the run, one guard heater failed and so the run was stopped before more data could be obtained.

List of References

1. Balzhiser, R.E., et al., "Investigation of Liquid Metal Heat Transfer", Final Report. Air Force Systems Command, AFAPL-TR-66-85, January 1967.

2. Noyes, R.C., "Boiling Sodium Heat Transfer", Proceedings of the Third International Heat Transfer Conference, Vol. 5, pp. 92-100, 1966.

3. Zuber, N., "Hydrodynamic Aspects of Boiling Heat Transfer", Ph.D. Thesis, University of California, Los Angeles, June 1959. (Also AECU-4439).

4. Kutateladze, S.S., "A Hydrodynamic Theory of Changes in the Boiling Process Under Free Convection Conditions", AEC-TR-1441, 1951.

5. Alekseev, G.V., Zenkevich, B.A. and Subbotin, V.I., "Critical Heat Fluxes in Annular Channels Heated from Both Sides", International Chemical Engineering, Vol. 5,No. 2, pp. 302-304 1965.

6. Becker, K.M. and Hernborg, G.,"Measurements of Burnout Conditions for Flow of Boiling Water in a Vertical Annulus", ASME Preprint 63-HT-25, 1963.

7. Adams, J.M., "An Analysis of the Critical Heat Flux in Nucleate Boiling." Paper No. 131, Presented at the 50th National AIChE Meeting, Buffalo, New York, May 5-8, 1963.

8. Adams, J.M., "An Investigation of Heat Transfer to Pool Boiling Magnesium." ASME Paper 65-HT-49, Presented at 8th National Heat Transfer Conference, Los Angeles, California, August 10, 1965.

9. Addoms, J.N., Sc.D. Thesis in Chemical Engineering, Massachusetts Institute of Technology, 1948. Also McAdams, W.H., Heat Transmission, Third Edition, p. 382, 385, McGraw-Hill, New York, 1954.

10. Averin, E.K., Izvest. Akad. Nauk, SSSR, No. 3, pp. 116-122, 1954. AERE Lib. Transl., p. 562, 1955.

11. Balzhiser, R.E., et al. "Investigation of Liquid Metal Boiling Heat Transfer." Report 05750-19-P, Liquid Metals Laboratory, University of Michigan, February, 1965.

12. Bernath, L.A., Chemical Engineering Progress Symposium Series No. 30, 56, p. 95, 1960.

13. Borishanskii, V.M., "An Equation Generalizing Experimental Data on the Cessation of Bubble Boiling in a Large Volume of Liquid." Zhurn. Tekh. Fiz., 26, p. 452, 1956.

14. Braunlich, R.H., "Heat Transfer to Boiling Liquids Under Vacuum." Sc.D. Thesis, Massachusetts Institute of Technology, 1941.

15. Breen, B.P. and Burnet, G., "Peak Heat Flux in Nucleate Boiling Heat Transfer." Iowa State University, Research and Development Report IS-810, January,1964.

16. Carbon, M.W., AEC and University of Wisconsin Report, May 30, 1964.

17. Carne, M., "Some Effects of Test Section Geometry, in Saturated Pool Boiling, on the Critical Heat Flux for Some Organic Liquids and Liquid Mixtures." AIChE Paper No. 6, Presented at the 7th National Heat Transfer Conference, Cleveland, Ohio, August 9-12, 1964.

18. Carne, M. and Charlesworth, D.H., "Thermal Conduction Effects on the Critical Heat Flux in Pool Boiling." AIChE Paper No. 11, Presented at the 8th National Heat Transfer Conference, Los Angeles, California, August 9, 1965.

19. Castles, J.T., S.M. Thesis in Chemical Engineering, Massachusetts Institute of Technology, 1947. Also McAdams, W.H., Heat Transmission, Third Edition, p. 387.

20. Caswell, B.F. and Balzhiser, R.E., "The Critical Heat Flux for Boiling Liquid Metal Systems." Presented at 8th National Heat Transfer Conference, Los Angeles, California, August 10, 1965.

21. Chang, Y. and Snyder, N.W., "Heat Transfer in Saturated Boiling." Chemical Engineering Progress Symposium Series No. 30, 56, 1960.

22. Cichelli, M.T. and Bonilla, C.F., Transactions AIChE, 41, pp. 755-787, 1945.

23. Clark, J.A. and Merte, H., "Nucleate, Transition, and Film Boiling Heat Transfer at Zero Gravity." 14, Advances in the Astronautical Sciences, Western Periodicals Co., N. Hollywood, California, 1963.

24. Colver, C.P., "Measurements of the Temperature Profiles Adjacent to the Surface During the Nucleate Boiling of Water and Methanol." M.S. Thesis in Chemical Engineering, University of Kansas, July, 1960.

25. Colver, C.P., "A Study of Saturated Pool Boiling Potassium up to Burnout Heat Fluxes." Ph.D. Thesis in Chemical Engineering, University of Michigan, September, 1963.

26. Colver, C.P. and Balzhiser, R.E., "A Study of Saturated Pool Boiling Potassium up to Burnout Heat Fluxes." Presented at 7th National Heat Transfer Conference, Cleveland, Ohio, August 9-12, 1964.

27. Costello, C.P. and Adams, J.M., "Burnout Heat Fluxes in Pool Boiling at High Accelerations." International Developments in Heat Transfer II, pp. 255-261, August, 1961.

28. Costello, C.P. and Frea, W.J., "The Role of Surface Deposits in the Attainment of High Pool Boiling Burnout Heat Fluxes." AIChE Journal, 10, No. 3, pp. 393-398, May 1964.

29. Costello, C.P. and Heath, C.A., "The Interrelation of Surface Effects and Acceleration in the Burnout Heat Flux Problem." AIChE Journal, 10, No. 2, p. 278, March, 1964.

30. Farber, E.A. and Scorah, R.L., "Heat Transfer to Water Boiling Under Pressure." Transactions ASME, 70, p. 369, 1948.

31. Frea, W.J., et al., "Some Experimental and Analytical Investigations on the Critical Heat Flux in Pool Boiling." AIChE Paper No. 10, Presented at the 8th National Heat Transfer Conference, Los Angeles, California, August 9, 1965.

32. Gambill, W.R. and Bundy, R.D., "High Flux Heat Transfer Characteristics of Pure Ethylene Glycol." AIChE Journal, 9, No. 1, pp. 55-59, January, 1963.

33. Gambill, W.R., "A Survey of Boiling Burnout." Brit. Chem. Eng., 8, No. 2, pp. 93-98, February, 1963.

34. Gambill, W.R., "An Experimental Investigation of the Inherent Uncertainty in Pool Boiling Critical Heat Fluxes to Saturated Water." AIChE Journal, 10, No. 4, pp. 502-508, July, 1964.

35. Gunther, F.C. and Kreith, F., "Photographic Study of Bubble Formation in Heat Transfer to Subcooled Water." Progress Report No. 4-120, JPL, 1950.

36. Howell, J.R. and Bell, K.J., Chemical Engineering Progress Symposium Series No. 41, 59, pp. 88-95, 1963.

37. Huber, D.A. and Hoehne, J.C. , "Pool Boiling Investigation of Benzene, Diphenyl, and Benzene-Diphenyl Mixtures Under Pressure." NAA-SR-6681, December, 1961.

38. Ishigai, S., et al., "Boiling Heat Transfer from a Flat Surface Facing Downward." International Developments in Heat Transfer II, pp. 224-229, August 1961.

39. Ivey, H.J., "Preliminary Results on the Effect of Acceleration on Critical Heat Flux in Pool Boiling." AEEW-R99, September, 1961.

40. Ivey, H.J., "Acceleration and the Critical Heat Flux in Pool Boiling Heat Transfer." Institute of Mechanical Engineering, London, England, November,1962.

41. Ivey, H.J. and Morris, D.J., "The Effect of Test Section Parameters on Saturation Pool Boiling Burnout at Atmospheric Pressure." AEEW-R-176, 1962.

42. Jakob, M., Heat Transfer, Vol. I, pp. 620-624, Wiley, New York, 1949.

43. Jordon, D.P. and Leppert, G., "Nucleate Boiling Characteristics of Organic Reactor Coolants." Nucl. Sci. and Eng., 5, pp. 349-359.

44. Kazakova, E.A., "The Influence of Pressure on the First Crisis in Boiling Water from a Flat Surface." C.E.I., Moscow, 1953.

45. Kutateladze, S.S. and Schneiderman, L.L., "An Experimental Study of the Influence of the Temperature of a Liquid on a Change in the Rate of Boiling." AEC TRANSL. 3405, 1953.

46. Kutateladze, S.S., et al., "Heat Transfer in Liquid Metals." J. Nucl. Energy II, 9, pp. 214-219, 1959.

47. Kutateladze, S.S., "Boiling Heat Transfer." Minsk, USSR Heat Transfer Conference, AD 270076, June,1961.

48. Levy, S. and Mausteller, J.W., "Heat Transfer-Natural Circulation." Liquid Metals Handbook, Sodium-NaK Supplement, p. 54, 1955.

49. Lienhard, J.H. and Schrock, V.E., ASME Paper No. 62-HT-3, Presented at the 5th National Heat Transfer Conference, Houston, Texas, August, 1962.

50. Lienhard, J.H. and Watanabe, K., "On Correlating the Peak and Minimum Boiling Heat Fluxes with Pressure and Heater Configuration." ASME Paper No. 65-HT-5, Presented at the 8th National Heat Transfer Conference, Los Angeles, California, August 9, 1965.

51. Lyon, R.E., et al., Advances in Cryogenic Engr., 9, 1964.

52. McAdams, W.H., et al., Chem. Eng. Prog., 44, p. 639, 1948.

53. Merte, H. and Clark, J.A., "Boiling Heat Transfer Data for Liquid Nitrogen at Standard and Near-Zero Gravity." Advances in Cryogenic Engr., K.D. Timmerhaus, Editor, 7, 1962.

54. Merte, H. and Clark, J.A., "Boiling Heat Transfer with Cryogenic Fluids at Standard, Fractional, and Near-Zero Gravity." Trans. ASME, 86, pp. 351-359, 1964.

55. Moore, F.D. and Mesler, R.B., "The Measurement of Rapid Surface Temperature Fluctuations During Nucleate Boiling of Water." AIChE Journal, pp. 620-624, December 1961.

56. Morozov, V.G., International J. Heat and Mass Transfer, 5, pp. 661-666, July 1962.

57. Nishikawa, K., "Photographic Study of Stable Film Saturated Boiling." Trans. ASME, 26, p. 726, 1960.

58. Noyes, R.C., "Summary of Recent Results of Sodium Boiling Studies." High Temp. Liq. Met. Ht. Trans. Tech. Conf., Oak Ridge Nat. Lab., September 4-6, 1963.

59. Noyes, R.C., Trans. ASME, No. 2, p. 125, 1963.

60. Noyes, R.C., "Boiling Studies for Sodium Reactor Safety, Part I." August 30, 1963, NAA-SR-7909.

61. Park, E.L., et al., Cryogenic Eng. Conf., 1965.

62. Rallis, C.J. and Jawurek, H.H., "Latent Heat Transport in Saturated Nucleate Boiling." Intern'l. J. Heat and Mass Transfer, 7, pp. 1051-1068, March,1964.

63. Rohsenow, W.M. and Griffith, P., "Correlation of Maximum Heat Flux Data for Boiling of Saturated Liquids." Heat, Mass, and Momentum Transfer, Chapter 9, Prentice-Hall, New York, 1961.

64. Roubeau, R., Progress in Refrig. Science and Tech., 1, p. 49, Pergamon Press, New York, 1960.

65. Sawle, D.R., "Sulfur-Cooled Power Reactor Study." Final Report, AGN-8015, December, 1960.

66. Subbotin, V.I., et al., "Heat Removal from the Reactor Fuel Elements Cooled by Liquid Metals." Third United Nations Intern'l. Conf. on the Peaceful Uses of Atomic Energy, Paper No. A/CONF. 28/P/328/USSR, May,1964.

67. Treshchov, G.G., Teploenergetika 3, 4, No. 5, pp. 44-48, 1957.

68. Van Wijk, W.R., et al., Chem. Eng. Sci., 5, pp. 68-80, 1956.

69. Westwater, J.W. and Santangelo, J.G., "Photographic Study of Boiling." Ind. Eng. Chem., p. 1605, August,1955.

70. Zuber, N. and Tribus, M. "Further Remarks on the Stability of Boiling Heat Transfer." AECU-3631, January,1958.

71. Zuber, N., "On the Stability of Boiling Heat Transfer." Trans. ASME, 80, pp. 711-720, April,1958.

72. Zuber, N., "The Hydrodynamic Crisis in Pool Boiling of Saturated and Subcooled Liquids." Intern'l. Dev. in Heat Trans., Part II, pp. 230-236, August, 1961.

73. Bobst, Robert William, "Temperature Profiles in the Superheated Boundary Layer Next to a Horizontal Heating Surface in Nucleate Pool Boiling Water", Masters Thesis, University of Oklahoma, 1967.