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**A PROGRAM OF
TWO-PHASE FLOW INVESTIGATION

THIRTEENTH QUARTERLY REPORT
APRIL - JUNE 1966**

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SCHENECTADY, N.Y.

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THIRTEENTH QUARTERLY REPORT APRIL - JUNE, 1966

by

P.G. Kroeger
N. Zuber

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NOMENCLATURE

MLT θ System of Units with $H = ML^2/T^2$

A_c	Cross-sectional area $[L^2]$
C_o	Distribution Parameter
c_p	Specific heat $[H/M\theta]$
G	Mass flow $[M/L^2T]$
g	Acceleration due to gravity $[L/T^2]$
i	Enthalpy $[H/M]$
Δi_{fg}	Latent heat of vaporization $[H/M]$
j	Volumetric flux density of the mixture $[L^3/TL^2]$
j_f	Volumetric flux density of the fluid $[L^3/TL^2]$
j_g	Volumetric flux density of the gas $[L^3/TL^2]$
L	Length of Heated Section $[L]$
Δz	Axial length along heated section from z_0 to z_i $[L]$
m	Exponent for radial flux profile equation
n	Exponent for radial void profile equation
p_r	Reduced pressure
q	Heat flux $[H/TL^2]$
r	Radial distance in a tube $[L]$
R	Tube radius $[L]$
T	Temperature $[\theta]$

T_i	Liquid bulk temperature at the inlet of the heated duct $[\theta]$
T_L	Liquid bulk temperature $[\theta]$
T_o	Liquid bulk temperature at z_o $[\theta]$
T^+	Dimensionless liquid bulk temperature $\frac{T_L - T_o}{\Delta T_s}$
ΔT_s	Temperature difference ($T_{sat} - T_o$) $[\theta]$
$\Delta T_{s.in}$	Inlet subcooling ($T_{sat} - T_i$) $[\theta]$
v	Velocity $[L/T]$
v_{fi}	Liquid inlet velocity $[L/T]$
V_{gj}	Drift velocity of the vapor $[L/T]$
$\overline{V}_{gj}$	Weighted mean vapor drift velocity $[L/T]$
x	Vapor quality
x_{eq}	Vapor quality assuming thermodynamic equilibrium
z_1	Axial coordinate at point of $T_L = T_{sat}$, assuming thermodynamic equilibrium
z	Axial coordinate at point along heated section measured from the inlet $[L]$
z_o	Axial coordinate at point of incipient vapor formation $[L]$
z^+	Dimensionless axial coordinate $\frac{z - z_o}{\Delta \ell}$
z^*	Dimensionless axial coordinate z/L
α	Vapor volumetric concentration
Γ	Rate of mass formation $[M/TL^3]$
ξ_h	Heated perimeter $[L]$
ρ	Mass density $[M/L^3]$

$\Delta\rho$	Density difference ($\rho_f - \rho_g$) $[M/L^3]$
σ	Surface tension $[ML/T^2 L]$
$< >$	Value averaged over the cross-sectional area

Subscripts:

c	At the centerline
CH	Value along a chord of a tube
DIA	Value along the diameter of a tube
f	Liquid
g	Vapor
i	At the inlet of the heated section
r	Relative
sat	At saturation
w	At the wall

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INTRODUCTION

The United States and the European Atomic Energy Community (Euratom), on May 29, and June 18, 1958, signed an agreement which provides a basis for cooperation in programs for the advancement of the peaceful applications of atomic energy. This agreement, in part, provides for the establishment of a Joint U. S. - Euratom research and development program which is aimed at reactors to be constructed in Europe under the Joint Program.

The work described in this report represents the Joint U. S. -Euratom effort which is in keeping with the spirit of cooperation in contributing to the common good by the sharing of scientific and technical information and minimizing the duplication of effort by the limited pool of technical talent available in western Europe and the United States.

SUMMARY

First and Second Contract Years

During the first contract year of this effort, March 1963, to March 1964, the experimental equipment was designed, constructed and installed. This work is summarized in [14]. The analytical work accomplished during this time period is summarized in the next section, which describes the completed objectives.

During the second contract year, constant and oscillating heat input data were obtained at reduced pressures less than 0.25. In the glass test section, only constant heat input data were taken. This experimental work, as well as the analytical effort completed during this period, are summarized in the next section which describes the completed objectives. The second contract year effort is documented in [18], [19], [21] and [22].

THIRTEENTH QUARTERLY PERIODAnalysis

The dynamic behavior and the stability of a two phase mixture with heat addition are significantly affected by the void formation in the subcooled boiling regime.

In previous work [1, 2] an analysis concerned with predicting the vapor volumetric concentration in subcooled boiling was presented together with a comparison to available experimental data. It was shown that prediction of the void fraction requires knowledge of four parameters:

- the distribution parameter C_0 ,
- the vapor drift velocity V_{gj} ,
- the point of incipient vapor formation z_0 , and
- the axial temperature distribution function T^+ .

The effects of the above parameters on the void fraction in subcooled boiling have been analyzed and are presented in this report.

The distribution parameter C_0 is of major importance towards the end of the subcooled regime, where thermodynamic equilibrium is approached.

The vapor drift velocity has only a minor effect in forced convection systems with high mass flow rates.

The point of incipient vapor formation z_0 greatly affects the predicted void fraction in the subcooled regime. No reliable criterion for predicting the location of this point exists at this time. It is concluded therefore, that further work to define and predict its location is mandatory for reliable and accurate prediction of void fractions in subcooled boiling.

The axial temperature distribution has been approximated by different functions that satisfy the physical boundary conditions. It appears that the precise specification of the temperature distribution is of secondary importance when compared to the need of locating properly the point of initial vapor formation z_0 . The profiles used up to date give satisfactory results when compared to available experimental data. However, final conclusion on this point has to be reserved until a method for predicting z_0 becomes available.

The results of the analysis show that changing the conditions of the heated surface and the introduction of additives may have two effects on the void fraction in subcooled boiling. These effects are discussed and a method for taking them into account is presented.

Satisfactory agreement is shown between predicted void fractions and recent experimental data obtained in subcooled boiling of Refrigerant -22 and of water over a pressure range of $0.22 \leq P_r \leq 0.65$ and $0.005 \leq P_r \leq 0.22$ respectively.

Experiments

Several tests at a reduced pressure of 0.65 and with constant heat input have been run. It was found that at this pressure level non-symmetric void profiles prevailed up to qualities of $x = 0.2$, while at lower pressures symmetry

had been observed above $x = 0.03$, [18, 19]. The tests taken to date indicate that the average void fractions are well repeatable. The data are not yet conclusive regarding the repeatability of the local void fraction readings. Further tests investigating this point are presently being run, taking five void readings at different radius ratios for each of the six vertical stations.

Those profiles approaching the region of bulk boiling follow the general relationships developed at lower reduced pressures.

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Sixth Quarterly Report, July - September, 1964, GEAP 4733
Seventh Quarterly Report, October - December, 1964, GEAP 4778
Eighth Quarterly Report, January - March, 1965, GEAP 4844
Ninth Quarterly Report, April - June, 1965, GEAP 4910
Tenth Quarterly Report, July - September, 1965, GEAP 4959
Eleventh Quarterly Report, October - December, 1965, GEAP 5067
Twelfth Quarterly Report, January - March, 1966, GEAP 5142

Topical Report

The Effects of Non-Uniform Flow and Concentration Distributions and the
Effect of the Local Relative Velocity on the Average Volumetric Concentration
in Two-Phase Flow - 1964, GEAP 4595

PAPERS PUBLISHED UNDER AT(04-3) -189, PA35

1. N. Zuber and J. A. Findley, "Average Volumetric Concentration In Two-Phase Flow Systems", Transactions ASME Journal of Heat Transfer, Volume 87, Series C, 1965, pg. 453.
2. N. Zuber, F.W. Staub and G. Bijwaard, "Vapor Void Fraction in Subcooled Boiling and in Saturated Boiling Systems", Proceedings Third International Heat Transfer Conference, Volume V, 1966 pg. 24.
3. F.W. Staub and N. Zuber, "Void Fraction Profiles, Flow Mechanisms and Heat Transfer Coefficients for Refrigerant 22 Evaporation in a Vertical Tube", ASHRAE Transaction (in print).
4. N. Zuber and F.W. Staub, "Stability of Dry Patches Forming in Liquid Films Flowing Over Heated Surfaces", International Journal of Heat and Mass Transfer" (in print),
5. N. Zuber and F.W. Staub, "The Propagation and the Wave Form of the Vapor Volumetric Concentration on Boiling Forced Convection System Under Oscillatory Conditions", International Journal of Heat and Mass Transfer. (in print).

STATEMENT OF PROBLEM

The design, cost, efficiency and reliability of high power systems which are characterized by the presence of two phases, depend to a large extent upon the understanding of the two-phase flow and boiling processes. Experiments and practice have shown that in two-phase flow systems steady-state oscillations of flow, of vapor content, of heat transfer coefficient and of local pressure can occur even when the independent parameters are kept constant. Under some conditions, not yet understood, the amplitudes of these oscillations may increase with time and lead to unstable operation. An understanding of the conditions leading to thermal-hydraulic oscillations and instabilities and the ability to predict them are therefore essential for an optimum design and reliable operation of engineering systems characterized by high power densities and change of phase.

The overall purpose of the proposed experimental and analytical research program is two-fold. First, the purpose is to gain an understanding of the various phenomena which lead to thermal-hydraulic oscillations in forced convection two-phase flow systems with heat addition and second, the purpose is to obtain equations and criteria which can be used for predicting the inceptions of these oscillations and instabilities as function of input parameters and system characteristics.

The experimental investigation is being carried out with Refrigerant-22* as the working fluid. This fluid was selected to meet the requirement of obtaining simultaneously recorded data on the heat transfer coefficient, pressure drop, vapor content and the visual identification of the two-phase flow regimes in a vertical forced convection boiling system over a reduced pressure range from 0.1 to 0.7. The use of Refrigerant-22 thus facilitates and reduces the cost of obtaining such data over a wide pressure range. It also provides data which can be used to test the effect of thermodynamic and transport properties

*CHClF₂

of the working fluid on the inception of thermal-hydraulic oscillations and instabilities. The experimental investigation will therefore complement other two-phase flow and heat transfer studies which are being carried out with water as the working medium under the sponsorship of the USAEC Division of Reactor Development.

The purpose of the analytical investigations is to determine the mechanisms which lead to thermal-hydraulic oscillations, stable or divergent, and to derive equations and criteria which may be used to predict the inceptions of these oscillations. The derived equations are to be of general form so that they may be used for predicting and for interpreting experimental results obtained with various working fluids.

OBJECTIVES COMPLETED IN FIRST CONTRACT YEAR

Experimental

1. Completed modification of the experimental loop installed at the Advanced Technology Laboratories of the General Electric Company to permit operation over the defined ranges of pressure, flow, inlet enthalpy, and steady-state or oscillating, heat input.
2. Completed electrically heated metal test section instrumented for the measurement of local wall temperature, local static pressure, local void fraction, inlet and discharge fluid conditions. Provide and install appropriate instrumentation conversion and recording equipment.
3. Completed shakedown testing of the loop and test section(s).
4. Evaluated feasibility of employing a direct electrically heated (coated) glass test section identical to, and to be used in addition to, the metal test section. Constructed and operated this test section after approval.

Analytical

1. All available formulations and analyses concerned with the transient behavior of two-phase flow systems have been examined and found to be basically incomplete if not incorrect.

In all analyses but one [23] the set of equations which describes the system is incomplete because the void diffusion equation, or better void propagation equation, is not taken into account.

All analyses are incorrectly formulated because none of them is formulated in terms of the center of mass of the mixture. It is well known from mechanics that the dynamic behavior of a multi-particle or component system gives meaningful solution only by considering the dynamic response of the center of mass of the system. This was not done in two-phase flow system.

2. In view of the conclusions above the set of equations which describe the two-phase flow system was derived in terms of the center of mass. Furthermore, the void propagation equation was formulated in terms of kinematic waves; it gives the void response to variations of pressure, of volumetric flux density of the mixture, of power density and of flow regime.
3. Since the ability to predict the void response depends upon the ability to predict the steady-state void fraction $\langle \alpha \rangle$, a general expression for either predicting $\langle \alpha \rangle$ or for analyzing and interpreting experimental data was derived. The analysis takes into account both the effect of non-uniform flow and concentration profiles and the effect of the local relative velocity. Good agreement with experimental data for adiabatic systems was shown. [3]
4. Since it was predicted analytically (from the void propagation equation) and confirmed experimentally [24] that the transient void response depends on the flow regime an analysis of the thermal effects on the stability of a liquid film was performed.

Two problems dealing with the destabilizing effects of heat addition on the two-phase flow and flow patterns were analyzed. First, the destabilizing effect of the vapor thrust on a liquid film flowing over a heated surface was considered and relations which predict the destruction of such film under these conditions were derived. The second problem considered deals with the stability of dry patches in a liquid film flowing over a heated surface. Criteria which predict the stability of such patches were derived.

OBJECTIVES COMPLETED IN SECOND CONTRACT YEARExperimental

1. Experiments were carried out at reduced pressures less than 0.25 and at steady-state heat input in the metal and glass test sections at several mass flow rates ranging between 50 lbs/hr and 600 lbs/hr. Steady-state power input data at each mass flow rate were gathered at several heat input levels and degrees of subcooling below the instability thresholds. A total of 61 test runs in both test sections were completed at constant heat input, with the photographic and visual identification of the flow mechanism carried out with the glass test section.
2. Minimum cost methods of steady-state and oscillatory data reduction were developed including the use of digital computers to evaluate the experimental mass flow rate, heat flux, local heat transfer coefficient, local pressure drop and local void fraction.
3. A total of 45 oscillatory power input tests in the metal test section were carried out at several frequencies between .01 and 10 cps and at several different amplitudes up to $\pm 40\%$ of the average input. More than four different operating conditions, i. e., combination of flow rate, heat addition, subcooling and/or reduced pressure, were selected from the results of the steady-state experiments.
4. The facility condenser was replaced for high pressure operation by a shell-tube unit which was found to be stable during the subsequent experiments.

Analytical

The following analytical objectives have been completed during the second contract year:

1. An equation which can be used either for predicting the void fraction or for analyzing and interpreting experimental data was derived. [18]. The analysis takes into account both the effect of non-uniform flow and concentration profiles as well as the effect of the local relative velocity.
2. The void propagation equation has been generalized in [21] to predict the

void response to variations of 1) power density, 2) system pressure, 3) pressure drop, 4) energy storage in the liquid and in the vapor, 5) compressibilities of the liquid and of the vapor, 6) friction factor, 7) flow regime and 8) gravitational force field. This void propagation equation, which is formulated in terms of the kinematic waves, predicts both the rate of propagation as well as the wave form of void disturbances which are caused by the various perturbations.

3. A computer and an approximate but simple, closed form analytical solution of the void propagation equation was derived [25] for the case of oscillating power input to the liquid and low inlet subcooling.

The values predicted by the analysis have been compared to the experimental data obtained in this research program. The predicted 1) average void fraction, 2) rate of propagation and 3) the wave form of the void variations were found to be in good agreement with the experimental data.

4. An analytical solution of the void propagation equation was derived for the case of oscillating inlet flow.
5. In analyzing the dynamic characteristics of the two-phase mixture (item four in the analytical objectives for the second contract year) it was found necessary to predict the void fraction in subcooled boiling. Although an analysis of the behavior of a two-phase mixture in subcooled boiling was not one of the objectives of the present program, such an analysis had to be performed to render completeness to the investigation of the stability of the two-phase mixture. The results are reported in [2] which presents an equation for predicting the void fraction in subcooled boiling.

The analysis of the dynamic characteristics and of the stability of the two-phase mixture will continue as part of the final contract period.

FINAL CONTRACT PERIOD OBJECTIVESExperimental

1. (a) Complete any additional tests required by the results of the analysis at low reduced pressure.
(b) Drain loop, replace glass sections, check at high pressure, leak check and recharge loop for operation up to a reduced pressure of about 0.7.
2. Carry out and complete constant heat input experiments in both metal and glass test sections at reduced pressures up to about 0.7.
3. Carry out and complete oscillating heat input tests in the metal test section at reduced pressures up to about 0.7.

Analytical

1. The analysis of the dynamic behavior and of the stability of a two-phase system, which was initiated during the last quarter of the second contract year, will be completed.
2. The solution of the set of equations will be obtained. Computer solutions will be used where necessary.
3. A special effort will be placed on obtaining simple analytical solutions as well as criteria for predicting the conditions leading to thermal-hydraulic oscillations and instabilities.
4. The results predicted by the analysis will be compared to experimental data.

A. An Analysis of the Effects of Various
Parameters on the Average Void Fraction in Subcooled Boiling

I. INTRODUCTION

The ability to predict accurately the vapor void fraction in subcooled boiling is of considerable interest to nuclear reactor technology. Both the steady-state performance and the dynamic response of a reactor depend on the void fraction. In particular, several studies of the dynamic behavior of a two-phase mixture have revealed that the stability of the system depends to a great extent upon the power density and the void behavior in the subcooled boiling region.

In view of the foregoing, an analysis was conducted with the purpose of deriving an equation that can be used to predict the vapor void fraction in a two-phase mixture in thermodynamic non-equilibrium. The results of that study have been reported in [1, 2] which lists also, the more significant investigations that preceded it.

It was shown in [1, 2] that four effects must be taken into account in an analysis concerned with predicting the vapor void fraction in subcooled boiling. These are the effects: 1. the local relative velocity; 2. the concentration profile; 3. the flow profile; and 4. the temperature profile. In [1, 2] these four effects have been accounted for by means of the vapor drift velocity $\overline{V}_{gj}$, the distribution parameter C_0 and the dimensionless axial temperature distribution T^+ .

The equation derived in [1, 2] for predicting the average void fraction (averaged over the cross-sectional area of the duct) was given by

$$\langle \alpha \rangle = \frac{\frac{q}{\rho_g \Delta i_{fg}} \frac{\xi_h (z - z_0)}{A_g} - \frac{v_{fi} \rho_f c_p \Delta T_s}{\rho_g \Delta i_{fg}} T^+}{C_o \frac{\Delta p}{\rho_f} \left\{ \frac{q}{\rho_g \Delta i_{fg}} \frac{\xi_h (z - z_0)}{A_g} - \frac{v_{fi} \rho_f c_p \Delta T_s}{\rho_g \Delta i_{fg}} T^+ \right\} + \left\{ C_o v_{fi} + \bar{V}_{gj} \right\} \left\{ 1 + \frac{c_p \Delta T_s}{\Delta i_{fg}} (1 - T^+) \right\}} \quad (1)$$

where the symbols are defined in the nomenclature.

In Equation 1, the coordinate z_0 defines the location in the duct where voids are first found on the heated wall, whereas $\Delta T_s = T_{sat} - T_o$, is the bulk subcooling at that point. Equation 1 indicates therefore, that in order to predict the vapor void fraction in subcooled boiling, it is necessary to determine first, the conditions for incipient vapor generation. This, indeed is not surprising.

The results predicted by Equation 1 have been compared in [1, 2] to the experimental data available at that time. Satisfactory agreement was reported. However, the importance of the various parameters which appear in Equation 1 have not been investigated in detail. This last aspect is important for planning of future experimental and analytical investigations. Furthermore, it is important also, when comparing the predicted results with experimental data.

II. PURPOSE OF THE PAPER

This paper has two purposes. The first one is to evaluate the effects of the various parameters on the ability to predict accurately the average value of the vapor void fraction in the subcooled boiling as well as in boiling liquids at saturation temperature. The second objective is to compare the predicted results with experimental data which have appeared in the literature following the writing of [1, 2].

The parameters whose effects are evaluated in this paper are: the distribution parameter C_0 , the axial temperature distribution T^+ , and the location of the point of incipient vapor generation z_0 . As noted above, these parameters take into account the effects of the flow and concentration profiles and of the thermodynamic non-equilibrium of the mixture.

It will be seen that the results of this evaluation define the range of operating conditions in which the effect of a particular parameter is most pronounced in determining the average value of the void fraction $\langle \alpha \rangle$. Information of this kind is desirable for predicting the void fraction, for analyzing available experimental data and for planning of future experiments.

III. PHYSICAL VARIABLES AFFECTING THE VAPOR VOLUMETRIC CONCENTRATION

It was noted in [1,2] that a flowing two-phase mixture in thermodynamic non-equilibrium is a medium where the temperature, the volumetric concentration and the velocity profiles are varying. If one recalls the characteristics of the single-phase multi component flow systems with heat and mass transfer, we find that they are determined by specifying the temperature, concentration, and the velocity profiles. It should not be surprising therefore, to see that the characteristics of a two-phase flow system will also depend upon these three parameters. However, since in two-phase mixtures the velocities of the two phases are not equal, a fourth parameter, which accounts for the relative velocity, must be taken into consideration when analyzing a two-phase flow problem. In what follows, we shall first discuss the significance of these four variables and the way they have been accounted for in the analysis of [1,2]. This will then be followed by a quantitative evaluation of their effect on the void fraction.

III.1 Local Relative Velocity

The local relative velocity between the two phases depends upon the drag exerted at the vapor-liquid interface as well as upon the geometry of this interface. It can be expected, therefore, that the relative velocity will vary whenever the topology of the two-phase mixture changes. Such changes from dispersed spherical to separated cylindrical and back to dispersed spherical interface geometry occur when the flow regime of the two-phase mixture changes from bubbly to annular and from annular to mist (fog) flow regime respectively.

In [1, 2, 3, 4, 5], the effect of the relative velocity was taken into account by means of the vapor drift velocity defined as the vapor velocity relative to the velocity of the center of volume of the mixture. The expressions for the drift velocities appropriate to the various flow regimes are given in [1-5]. For the bubbly churn flow regime, of interest to this paper, the weighted mean vapor drift velocity is given by:

$$\overline{V}_{gj} = 1.41 \left[\frac{\sigma g \Delta \rho}{\rho_f^2} \right]^{1/4} \quad (2)$$

III. 2 Concentration and Flow Profiles

In order to understand the effect of radial void and flow profiles on the average void fraction $\langle \alpha \rangle$ it is instructive to examine the variation of these profiles along the axial coordinate in a uniformly heated duct. Such a variation is shown schematically in Figure 1-a for the void profile $\alpha(r)$, and in Figure 1-b for the volumetric flux density profile of the mixture $j(r)$ (defined as the sum of the volumetric flux densities of the liquid and of the vapor).

Figure 2 is an example of experimental data obtained in this investigation, which show the concentration profiles as function of quality and of subcooling. Since these experiments have been performed with an axially uniform power input, the functional dependence of $\langle \alpha \rangle$ upon quality and subcooling can be expressed also as function of distance. Similar profiles obtained with other fluids are shown in [5]. We note that simultaneously recorded data on concentration and flow profiles in subcooled boiling have not been reported in the literature. Consequently, some of the $j(r)$ profiles shown on Figure 1-b, still need experimental verification.

Consider now the profiles shown in Figure 1. For a degassed fluid with a sufficiently high inlet subcooling, no bubbles will be present at the entrance of the heated section (station 1). Consequently, at this station $\langle \alpha \rangle$ will be zero whereas the volumetric flux profile of the mixture will correspond to the velocity profile of the liquid only. For turbulent flow, the latter will have the shape shown on Figure 1-b.

The temperature of the wall T_w , will reach the value of the saturation temperature somewhere downstream from the inlet. From this point on bubbles can be generated on the heated wall. The exact location where this process starts will depend on the nucleating characteristics of the surface

Figure 1. Variation of Concentration and Flow Profiles along a Uniformly Heated Duct. (a) Volumetric concentration of the vapor, (b) Volumetric flux density of the mixture.

Figure 2. Experimental Chordal Void Fractions in Subcooled Boiling.

as well as on the surrounding flow and temperature fields.

Following the nucleation, bubbles grow on the heated surface. However, since the bulk liquid temperature is still below saturation, bubbles will either collapse on the surface or detach and collapse in the bulk liquid. Consequently, the vapor concentration profile will decrease from a given value at the wall to zero at the center line (see station 2). Whether bubbles slide along the wall or detach and move parallel to the wall as they collapse in the bulk, they will contribute to the axial volumetric flux density of the mixture. Since this contribution will be most noticeable in the vicinity of the heated wall, the $j(r)$ profile will be flatter at station 2 than at station 1.

In the bulk of the liquid, the rate of bubble collapse decreases as the temperature increases. Because of the radial temperature distribution in the liquid and because of the bubble migration toward the center, the concentration profile will exhibit two maxima and a minimum point (see station 3). At pressure levels where the density of the vapor is considerably lower than the density of the liquid, the volumetric flux density of the mixture will be greatly affected by the volumetric flux density of the vapor. Consequently, at these pressures, it can be expected that the $j(r)$ profile will be similar to that of $\alpha(r)$ as indicated at station 3 on Figure 1-b.

When the bulk liquid reaches saturation temperature, the bubble collapse will cease, whereas the migration toward the center will continue. Consequently, the concentration profile will become first flat as shown at station 4 and then more pronounced as the bubbly flow regime fully develops further downstream (see station 5).

As evaporation along the duct continues, and the vapor void fraction together with the volumetric flux increases, the flow regime will change from bubbly churn turbulent to the annular flow regime. As a result of this change of interface geometry and re-distribution of phases, the concentration as well as the flux profiles will flatten out (see station 6).

Further downstream, as droplets become entrained in the vapor core, the flow regime will change to annular-mist flow. For sufficiently long ducts, the liquid film adjacent to the heating wall may disappear as a consequence of evaporation and/or of drop entrainment. When this occurs, the flow regime changes to mist flow in which the only liquid present is in the form of droplets concentrated in the central region of the vapor core. For such a flow regime, the vapor concentration is highest at the heated wall; indeed it may have there, the value of unity for a dry wall. The lowest vapor concentration is in the central region where droplets are present. Such a vapor concentration

profile is shown at station 7 together with the corresponding flux profile of the mixture. The latter does not change the curvature because of the requirement that the vapor velocity must be zero at the wall.

Finally, for complete vaporization as encountered in once-through systems, the vapor concentration becomes unity, whereas the profile of the volumetric flux density of the mixtures becomes equal to the velocity distribution of the vapor phase only, as shown at station 8.

It can be seen from the preceeding discussion that in a two-phase system with head addition, the concentration and the flux profiles change along the duct as evaporation takes place. Since it is well known from studies of single phase flow systems with mass transfer that the shapes of the concentration and of the velocity profiles have an important effect on the value of the average concentration, it can be expected that similar results will be evident in vapor-liquid mixtures. In particular, it can be expected that when the vapor is concentrated in the low velocity region, (station 2 and 7) it will be transported at a velocity that is lower than the average velocity $\langle j \rangle$. Conversely, when voids are concentrated in the high velocity region (station 5), they will move faster than $\langle j \rangle$. This is indeed the case as shown on [3].

It was shown in [6, 1-5] that the concentration and velocity profiles affect the average void fraction through a distribution parameter C_0 , defined by

$$C_0 = \frac{\langle f_1 f_2 \rangle}{\langle f_1 \rangle \langle f_2 \rangle} \quad (3)$$

where f_2 and f_1 are the radial profiles of concentration and of flow respectively.* By examining Equation 1, it can be seen that, with all other parameters being equal, an increase of C_0 results in a decrease of the average void fraction $\langle \alpha \rangle$ and vice versa.

The concentration and flux density profiles analyzed in [1-5] were of the form

* The inverse of C_0 was first introduced in [6] where it was referred to as the "flow parameter". In that reference, the effects of neither the local relative velocity nor of the subcooling were considered.

$$\frac{\alpha_c - \alpha_w}{\alpha_c - \alpha_w} = f_2 \left(\frac{r}{R} \right) = 1 - \left(\frac{r}{R} \right)^n \quad (4)$$

$$\frac{j}{j_c} = f_1 \left(\frac{r}{R} \right) = 1 - \left(\frac{r}{R} \right)^{m+1} \quad (5)$$

where the subscripts c and w denote quantities evaluated at the center line and at the wall respectively. The reason for selecting these profiles was their simplicity as well as their accuracy in representing the experimental data [2, 5]. It can be seen that Equation 4 and Equation 5 describe the profiles shown at stations 2, 4 through 8. Clearly, a more complex expression is required to describe the profiles at station 3.

With the profiles given by Equation 4 and Equation 5, the distribution parameter C_0 becomes

$$C_0 = 1 + \frac{2}{m+n+2} \left[1 - \frac{\alpha_w}{\langle \alpha \rangle} \right] \quad (6)$$

when expressed in terms of α_w and

$$C_0 = \frac{m+2}{m+n+2} \left[1 + \frac{\alpha_c}{\langle \alpha \rangle} \frac{m}{m+2} \right] \quad (7)$$

when expressed in terms of α_c .

It can be seen from Equations 6 and 7 that for a radially uniform concentration ($\alpha_c = \alpha_w = \langle \alpha \rangle$) the distribution parameter is unity, i. e., $C_0 = 1$.

Equation 7 shows that when α_c is zero, i. e., when the voids are concentrated in the low velocity region (see station 3) then $C_0 < 1$. Assuming all other parameters being equal, Equation 1 indicates that a system with such profiles will have a higher value of $\langle \alpha \rangle$ than a system with a radially uniform concentration. Conversely, lower values of $\langle \alpha \rangle$ are to be expected in a system where voids are concentrated in the high velocity region (station 5) because for such profiles, Equation 6 shows that $C_0 > 1$. It was shown in [3, 4] that, for profiles given by Equation 4 and 5, the maximum value of C_0 is $C_0 = 1.5$, which corresponds to peaked profiles at the center. For flat profiles (see stations 4 and 6) the value of C_0 tends to unity. The changes of the concentration profiles shown in Figure 2 as well as those shown in [5] indicate that such variations of C_0 , and the attendant effects on $\langle \alpha \rangle$, can be expected as vaporization takes place and flow regimes change along the heated duct.

III.3 Thermodynamic Non-Equilibrium

In order to determine the vapor volumetric concentration, it is necessary to determine first, the volumetric flux density of the vapor. In subcooled boiling, the latter depends on the rate of bubble nucleation and on their subsequent growth and collapse. Were the equations describing these processes known and/or sufficiently reliable, they could be used to formulate the appropriate constitutive equation of evaporation which, together with the energy equation of the mixture, would specify the vapor flux [1, 2].*

However, since neither the process of nucleation nor the process of bubble growth and collapse can be predicted reliably, it is desirable and advantageous to determine the vapor volumetric flux density from the energy equation of the mixture by assuming an appropriate axial temperature distribution [1, 2]. This latter assumption is equivalent to specifying the appropriate form for the state of thermodynamic non-equilibrium of the mixture for a given operating condition. Such an assumption is required because in subcooled boiling, part of the energy which is transferred from the heated wall is used to increase the temperature of the subcooled liquid whereas the other is used for vaporization. This is illustrated in Figure 3 which shows the quality and the liquid bulk temperature as function of distance for a system with

* This approach is discussed further in Section VI of this paper.

Figure 3. Schematic Presentation of Liquid Temperature and Vapor Quality in a Uniformly Heated Duct.

an axially uniform power input.

Let us consider first, a system in thermodynamic equilibrium. By definition, in such a system neither can vapor be present in the subcooled liquid region nor can the liquid become superheated. Consequently, in the subcooled region the entire energy transferred to the system is used to increase the enthalpy of the liquid to saturation; beyond that point it is used entirely for vaporization. In such a system, therefore, the bulk liquid temperature will increase linearly with distance until it reaches saturation at a point $z = z_1$, thereafter remaining constant. Furthermore, up to point z_1 , there will be no vapor generation; thereafter the vapor flux and therefore, the quality will increase linearly with distance (see Figure 3).

A real system, however, will not be in thermodynamic equilibrium because vapor generation may start already at point z_0 , where the temperature of the wall exceeds saturation although the bulk liquid temperature T_0 is below T_{sat} , i. e., the bulk liquid is still subcooled. From this point on, the vapor flux and therefore, the quality increase with distance although, not linearly. In this region, part of the energy transferred from the heating wall is used to increase the enthalpy of the liquid, the other is used for vaporization. Consequently, past point z_0 , the increase of the bulk liquid temperature will cease to be linear with distance. Since the rate of temperature rise is decreased, the bulk will reach saturation temperature further downstream from point z_1 . Beyond that point, the vapor flux and the quality will increase again linearly with distance.

It is evident from the foregoing that the axial quality and temperature distributions are related. It is evident also, that either distribution can be used to describe the state of thermodynamic non-equilibrium. In [1,2], this state was described by assuming the temperature distribution. The boundary conditions and the form which this distribution must satisfy are given in [1,2]. Among the various functions which would satisfy these conditions two were selected and were used in Equation 1 to predict $\langle \alpha \rangle$. These functions, chosen in view of their simplicity, were:

$$T^+ = \frac{T_L - T_0}{T_{sat} - T_0} = 1 - \exp \left[-\frac{z - z_0}{\Delta l} \right] \quad (8)$$

and

$$T^+ = \frac{T_L - T_o}{T_{sat} - T_o} = \tanh \left[\frac{z - z_o}{\Delta \ell} \right] \quad (9)$$

where the characteristic length $\Delta \ell$ is given by

$$\Delta \ell = z_1 - z_o = \frac{A_c}{q \xi_h} v_{fi} \rho_f c_p [T_{sat} - T_o] \quad (10)$$

The relation derived in [1, 2] between the axial quality distribution and the temperature distribution was given by:

$$G_g = x G = \rho_g \langle j_g \rangle = \rho_g \left\{ \frac{\frac{q}{\rho_g \Delta i_{fg}} \frac{\xi_h (z - z_o)}{A_c} - \frac{v_{fi} \rho_f c_p \Delta T_s}{\rho_g \Delta i_{fg}} T^+}{1 + \frac{c_p \Delta T_s}{\Delta i_{fg}} (1 - T^+)} \right\} \quad (11)$$

It can be seen from Equations 8, 9, 10 and 11, as well as from Equation 1, that in order to compute the quality and the average void fraction, it is necessary to determine first the location of the point z_o where vapor generation starts. Before discussing this aspect of the problem, it will be instructive to examine the effects of thermodynamic non-equilibrium on the void profiles as well as on the average void fraction.

Figures 4 and 5 show measured average void fractions and those computed from Equation 1 as a function of axial distance for the test conditions of Figure 2. Above each experimental value of $\langle \alpha \rangle$ on these two figures, there is a sketch of the corresponding profile in Figure 2, together with the value of the bulk liquid temperature T^+ at that point computed from Equation 8. The location of z_o shown in these figures was estimated from the experimental data, whereas point z_1 was computed. Note that this latter point corresponds

Figure 4. Axial Void Distribution in Subcooled Boiling with Corresponding Void Profiles at Various Stations (For Details on Void Profiles and Test Conditions see Fig. 2b).

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Figure 5. Axial Void Distribution in Subcooled Boiling with Corresponding Void Profiles at Various Stations (For Details on Void Profiles and Test Conditions see Fig. 2a).

to the location where the quality based on thermodynamic equilibrium is zero.

Two observations can be made with respect to Figures 4 and 5. First, it can be seen that the concave and double profiles which are characteristic of subcooled boiling persist well beyond the point z_1 where $x_{eq} = 0$. This could be interpreted as an indication that, because of thermodynamic non-equilibrium, the bulk liquid temperature reaches saturation downstream from the point z_1 . It is noteworthy that the change to convex profiles, which are characteristic of fully-developed bubbly regime, takes place where the computed bulk liquid temperature is close to saturation, i. e., $T^+ \sim 0.96$. Second, Figure 5 shows that there is almost no change in the value of $\langle \alpha \rangle$ between the two locations, where $T^+ = 0.92$ and $T^+ = 0.96$. This result could be attributed to some experimental error were it not for the fact that similar behavior was also observed in several other experimental runs. Such a behavior of $\langle \alpha \rangle$ could be accounted also by the change of profiles from uniform at $T^+ = 0.92$ to convex at $T^+ = 0.96$. It was discussed above that, with all other parameters being equal, radially uniform profiles will give higher values of $\langle \alpha \rangle$ than convex profiles. Although this explanation is still tentative, the results shown on Figures 4 and 5, as well as those reported in [5] clearly indicate the interaction between the concentration and temperature profiles and their effect on the average void fraction.

III. 4 Point of Initial Vapor Formation

It was noted above that for degassed liquids, the vapor generation can start as soon as the temperature of the wall exceeds saturation, i. e., as soon as the liquid adjacent to the wall becomes superheated. The amount of superheat required to generate the vapor depends on the nucleating characteristics of the surface as well as on the flow and temperature field. For degassed liquids, therefore, the location of z_0 will be downstream from the point $z_{w\text{ sat}}$ where $T_w = T_{\text{sat}}$. Furthermore, for liquids that cannot maintain a large bulk liquid superheat* z_0 will not exceed z_1 . The problem is then reduced to locate accurately z_0 between these two limits, i. e., between $z_{w\text{ sat}} \leq z_0 \leq z_1$.

Several criteria for predicting vapor formation have been reported [7, 8, 9 among others] ; some of these predict the conditions required to nucleate the first bubble, others predict the conditions for bubbles to detach

* This may not be true for liquid metals.

from the wall and rise in the subcooled main stream. Whether any of these conditions can be related to z_0 , determined from void measurements remains to be proven.

In 1,2 some of the available criteria were used to compute z_0 . With water at elevated pressures flowing through rectangular channels, the values predicted by the criterion of [9] were sufficiently close to the points where the voids were first detected experimentally. However, the agreement with experimental data was not very satisfactory at low pressures and for fluids other than water. It was concluded in 1,2 that a criterion which could be used to predict z_0 for various fluids and operating conditions is not yet available.

IV. EFFECTS OF VARIOUS PARAMETERS

The influence which various physical variables have on the average void fraction were discussed in the preceeding section. In what follows we shall evaluate quantitatively their effect.

In order to analyze the various effects it is advantageous to rewrite Equation 1 in the form of

$$\langle \alpha \rangle = C_0 \frac{\Delta \rho}{\rho_f} x + \left[C_0 + \frac{\bar{V}_{gj}}{v_{fi}} \right] \frac{\rho_g}{\rho_f} \quad (12)$$

where the quality is given by Equation 11. This latter can be also expressed as

$$x = \frac{q \epsilon_h \Delta h}{GA_c \Delta i_{fg}} \left\{ \frac{z^+ - T^+}{1 + \frac{c_p \Delta T_s}{\Delta i_{fg}} (1 - T^+)} \right\} \quad (13)$$

or as

$$x = \frac{c_p \Delta T_s}{\Delta i_{fg} + c_p \Delta T_s (1 - T^+)} (z^+ - T^+) \quad (14)$$

where

$$z^+ = \frac{z - z_0}{\Delta l} \quad (15)$$

Using these expressions the effects of the distribution parameter C_0 , of the axial temperature distribution T^+ , and of the initial point z_0 , can be evaluated by varying these parameters in Equation 12. The results of such an evaluation are discussed below.

IV.1 Distribution Parameter

Figure 6 shows the computed values of $\langle \alpha \rangle$ for given z_0 and T^+ as function of the distribution parameter C_0 . The range of variation of C_0 used in preparing this figure covers the profiles discussed in Section III-2 and illustrated in Figure 1 and 2.

It can be seen that whether Equation 8 or Equation 9 is used for the temperature distribution the effect of C_0 is most pronounced in the region where $z > z_1$, i. e. in the bulk boiling region. Conversely, in the subcooled region the effect of C_0 is less pronounced.

It was discussed in Section III-2 that as the process of evaporation takes place, the concentration and flow profiles will change. Consequently, it can be expected that the value of C_0 will change starting with $C_0 = 1$ at the point where $\langle \alpha \rangle = 0$, decreasing to $C_0 < 1$ in the subcooled region and increasing to $C_0 > 1$ in the bulk boiling region. If, in absence of experimental data on flow profiles in the subcooled region, a constant value of C_0 is to be taken for the entire test section, then the results shown in Figure 6 suggest that this value should correspond to the bulk boiling region.

IV.2 The Axial Temperature Distribution

Figure 7 shows the computed values $\langle \alpha \rangle$ for given z_0 and C_0 using

Figure 6. Effect of Distribution Parameter C_0 on Void Formation in Subcooled Boiling. (a) Using Hyperbolic Tangent Function $T^+ = \tanh(z^+)$, (b) Using Exponential Function $T^+ = 1 - \exp(-z^+)$.

Figure 7. Effect of Axial Temperature Distribution on Computed Void Fractions in Subcooled Boiling.

Equation 8 and 9 for the axial temperature distribution. The void fraction computed by assuming thermodynamic equilibrium is also indicated.

It can be seen from this figure that initially the void fraction computed by using the exponential distribution exhibits a faster increase than that computed by using the hyperbolic tangent distribution. Furthermore, the difference between the two curves could be almost eliminated by an appropriate shift of the point z_0 . It is evident that in absence of a reliable method for selecting and predicting z_0 either temperature distribution could be used for predicting $\langle \alpha \rangle$.

It is important to note that in the bulk boiling region the computed $\langle \alpha \rangle$ based on either temperature distribution approaches the value of $\langle \alpha \rangle$ computed by assuming thermodynamic equilibrium. This indicates that the degree of subcooling initially present has a negligible effect on the void fraction in the bulk boiling region.

IV.3 Point of Initial Vapor Formation

Figure 8 shows the computed values of $\langle \alpha \rangle$ for a constant C_0 and T^+ as function of the initial point z_0 . The void fraction computed assuming thermodynamic equilibrium is also plotted on this figure.

It can be seen from Figure 8 that, in the bulk boiling region, the effect of accurately locating the point z_0 of initial vapor formation is small and of secondary importance in determining $\langle \alpha \rangle$. Apparently, the liquid in the bulk boiling region has a poor memory, i. e. it does not remember the initial state of thermodynamic non-equilibrium.

In the subcooled region however, the effect of z_0 is most pronounced. Indeed, of the three parameters C_0 , T^+ and z_0 the last one is definitely the most important. It is evident that the ability to accurately predict the value of $\langle \alpha \rangle$ in the subcooled region depends on the ability to predict z_0 as function of system geometry, fluid properties and operating conditions. It was noted in Section III-4 that such a criterion is not available at the present time.

It is of interest to note here that in several analyses concerned with the stability of a two phase mixture, it was assumed that the change of z_0 results in a lateral shift of the void curve along the z axis. The results shown

Figure 8. Effect of Point of Incipient Vapor Formation z_0 on Void Fractions in Subcooled Boiling. (a) Using Hyperbolic Tangent Function $T^+ = \tanh(z^+)$, (b) using Exponential Function $T^+ = 1 - \exp(-x^+)$.

in Figure 8 indicate that such a lateral shift without the attendant change of curvature (as it was assumed in these analyses) is in error.

In the section that follows we shall compare the results predicted by Equation 12 to experimental data. In view of the results obtained in this section and since no information is available on the flow profiles in subcooled boiling, the computations will be based on a constant value of C_0 evaluated in the bubbly regime. Furthermore, the values of z_0 will be determined from experimental data. The comparison will indicate therefore with what accuracy one could predict $\langle \alpha \rangle$, assuming C_0 to be constant in Equation 12, were a reliable method for predicting z_0 available.

V. COMPARISON WITH EXPERIMENT

Based on the analysis covered in the preceding sections, recent experimental data of several investigators have been evaluated and compared with analytical results. The test sections considered include rectangular, annular, and circular configurations. The test fluids cover water at reduced pressures from 0.005 to 0.22 and Refrigerant-22 at reduced pressures between 0.22 and 0.65.

It was shown in Section IV-1 that the ability to predict accurately the average void fraction $\langle \alpha \rangle$ is most sensitive to the value of the distribution parameter C_0 in the bulk boiling region where $x_{eq} > 0$. In absence of experimental data on flow profiles in subcooled boiling it is desirable therefore to select for C_0 values appropriate to the bubbling flow regime. It is shown in [5] that for this regime the value of C_0 is approximately 1.25 for circular ducts with diameters up to 2.0 inches, whereas for rectangular ducts it is equal to 1.5. Actually, somewhat lower values should be expected at moderate and high subcoolings because as profiles vary along the duct, C_0 changes from a value below unity to a value above one. It was recommended in [1, 2, 5] that, in absence of accurate experimental data, values of $C_0 = 1.13$ or 1.4 for circular or rectangular geometries respectively be used in Equation 1 for predicting $\langle \alpha \rangle$. This value will be used also in the present work in order to test the generality of the correlation.

As outlined in Section IV.1 the axial temperature distribution is not very critical in predicting void fractions. Some of the data have been correlated well with an exponential temperature function; for others, the hyperbolic tangent function appeared to yield better agreement. Unless otherwise noted, the hyperbolic tangent function has generally been used here in comparing analytical predictions with experimental data.

Figure 9 represents a comparison between analysis and experiments for Refrigerant-22 in a circular tube at several reduced pressures. Using a constant distribution parameter $C_0 = 1.13$, it was found that at low reduced pressures (Figure 9-a) the predicted void fractions are somewhat high, while at high pressures, the analysis underpredicts the void fractions slightly. This indicates that the distribution parameter C_0 changes with increasing pressure, in agreement with the results, of [2, 3, 5, 6] where it is shown that C_0 tends to unity as the pressure increases. In Figure 10, the data of Figure 9c are replotted, and compared to computed profiles using a distribution parameter of $C_0 = 1.0$. The results show that either an exponential temperature function with $z_0 = 0$ or a hyperbolic tangent function with a negative value for z_0 can be used to obtain good agreement between test and predictions. A negative value for z_0 would physically imply that voids existed at the inlet to the heated section which is improbable but not impossible with Refrigerant-22 at high pressures and low subcoolings.

Figure 11 shows test results obtained by Firstenberg and Neal [10] in a rectangular channel with water at atmospheric pressure for different power inputs. The values predicted by Equation 1 with $C_0 = 1.13$ and hyperbolic tangent temperature distribution are also shown.

As noted above, for rectangular ducts and mixtures with high exit qualities i. e., with convex concentration profiles the recommended value for C_0 was equal to $C_0 = 1.4$. However, for the tests of [10] with low exit quality it is probable that the profiles were not fully developed resulting in a lower value for C_0 . Note also that in Figure 11-a the void fractions towards the end of the test section may approach the region of fully developed profiles suggesting a value of C_0 larger than 1.13 used in computations, as borne out by experimental data.

It appears from Figure 11 that the predicted results are in satisfactory agreement with experimental data if the point z_0 of incipient vapor formation is selected correctly. As lower limit, this point has to be downstream of the point where the wall temperature reaches the saturation temperature. On the other hand, nucleation will certainly occur upstream of z_1 , where the bulk of the liquid would reach saturation temperature if thermodynamic equilibrium persisted. The exact location where the wall temperature reaches the saturation temperature will depend on the flow and temperature profiles at the inlet to the heated section. For the experiments of [10] the uncertainties in the velocity profile (following a bend and a flow meter) permit only an approximate determination of that point. Inlet effects on the heat transfer coefficient in this region were computed in accordance with recent results given by Stone [11].

REFRIGERANT 22

TEST NO. SC24 II 3

 $p = 11.3 \text{ atm}$ $q = 1.37 \text{ W/cm}^2$ $\Delta T_{s \text{ in}} = 13.1 \text{ C}$ $G = 13.1 \text{ g/sec cm}^2$

Figure 9. Comparison of Predicted with Measured Void Fractions in Subcooled Boiling. Authors' Data, Circular Tube 1.01 cm ID, Heated Section 154 cm long, Refrigerant 22.

Figure 10. Comparison of Predicted with Measured Void Fractions in Subcooled Boiling. Authors' Data, Circular Tube 1.01 cm ID, Heated Section 154 cm long, Refrigerant 22.

Figure 11. Comparison of Predicted with Measured Void Fractions in Subcooled Boiling. Data of A. Firstenberg and L. G. Neal, Channel 2.11 x 0.295 cm, Heated Section 62.5 cm long, Water, $p = 1 \text{ atm}$, $\Delta T_{\text{S in}} = 3.3^\circ\text{C}$, $G = 45.1 \text{ g/sec cm}^2$.

Two experimental criteria given by Griffith [8] and Bowring [9] to identify the point of incipient boiling have been evaluated for the prevailing test conditions, and are indicated in Figure 11; the agreement with the experimental evidence is not too good. However, both criteria had been obtained in correlating test data at significantly higher pressures, and the disagreement should therefore not be surprising.

It can be seen on Figure 11 that with the hyperbolic tangent temperature distribution good agreement between predicted and experimental results is obtained if z_0 is located upstream from the point where voids were first detected. Whether this result stems from the assumed temperature profile or whether it implies that voids too small to be detected existed upstream cannot be concluded at this time. Figure 12 shows however, that the results predicted by Equation 1 using an exponential temperature distribution (instead of hyperbolic tangent) are even in better agreement with the data of Figure 11-b if z_0 is taken in the region where voids are first detected. This result clearly indicates the importance of determining an appropriate and reliable criterion for predicting the point of incipient vapor formation.

Figure 13 shows a comparison between the void fractions predicted by Equation 1 and the experimental data of Cook [12] for water at 42 atm. flowing through a rectangular channel. For both tests, a distribution parameter of 1.4 was used since for each case the major part of the test section was in the region where $\bar{x} > 0$ indicating fully developed convex profile. In Figure 13-a with relatively low e_{eq} subcooling, the wall temperature will reach the saturation point at the inlet of the heated section. Both criteria, Bowring's, as well as Griffith's, predict incipient nucleation at the inlet to the test section. With increased subcooling, the point of incipient nucleation is moved downstream as shown for the test reproduced in Figure 13-b. Here again, both Bowring's and Griffith's criteria predict the point of beginning nucleation very well.

Figure 14 reproduces test data obtained by Rouhani [13] in an annular channel, where the inside perimeter is heated uniformly. The tests were conducted with water 49 atm. Using a distribution parameter of $C_0 = 1.13$ in Equation 1, good agreement between test and analysis is obtained. The initial point predicted by Bowring's criterion is however only in fair agreement with the data. Again, this can be expected, since an annular cross-section with only the inside perimeter being heated falls outside of the geometries covered in deriving the criterion.

It appears from the preceeding results that the analytical predictions are in satisfactory agreement with experimental data if the proper value of

Figure 12. Comparison of Predicted and Measured Void Fractions in Subcooled Boiling, Using Different Functions for T^+ . Data of A. Firstenberg and L.G. Neal, Channel $2.11 \times 0.295 \text{ cm}$, Heated Section 62.5 cm long, Water, $p = 1 \text{ atm}$, $\Delta T_{s, in} = 3.3 \text{ C}$, $G = 45.1 \text{ g/cm}^2 \text{ sec}$, $q = 1.9 \text{ W/cm}^2$.

Figure 13. Comparison of Predicted with Measured Void Fractions in Subcooled Boiling. Data of W.H. Cook, Channel 9.37 x 1.11 cm, Heated Section 122 cm long, Water, $p = 42 \text{ atm}$, $q = 14.7 \text{ W/cm}^2$.

Figure 14. Comparison of Predicted with Measured Void Fractions in Subcooled Boiling. Data of Z. Rouhani, Annulus, 1.2 cm ID, 2.5 cm OD, Water, $p = 49 \text{ atm}$.

z_0 is selected in the subcooled region and C_0 is approximated by the value appropriate to the bulk boiling region. Therefore, in order to predict accurately the void fraction in subcooled boiling it is of utmost importance to derive a reliable criterion for predicting the point z_0 of initial vapor generation. Conversely, for the bulk boiling region it is of primary importance to determine the distribution parameter from measured flow and concentration profiles.

VI. THE FORMAL SOLUTION

It was noted in Section III-3 that in order to compute the void fraction in subcooled boiling it is necessary to determine the vapor mass flow rate G_g given by Equation 11. It can be seen that in order to evaluate G_g , it is necessary to determine first the axial temperature distribution T^+_{fg} . In [1, 2] the latter was assumed to be of the form given by Equations 8 and 9; which were obtained by considering the physical boundary conditions that the temperature distribution must satisfy. The formal approach for determining this distribution was also presented in [1, 2]; in what follows we shall discuss some additional aspects and difficulties inherent to this formal method.

Consider a steady, one dimensional flow of a two phase mixture with constant properties and assume that the effects of the kinetic and potential energies can be neglected. Then, the problem of determining the temperature distribution in the liquid can be formulated [1, 2] by considering:

- 1) the conservation of mass for the mixture:

$$G = G_f + G_g$$

- 2) the continuity equation for the vapor:

$$\frac{\partial G_g}{\partial z} = \Gamma_g$$

- 3) the energy equation for the mixture:

$$G_f \frac{\partial i_f}{\partial z} + G_g \frac{\partial i_g}{\partial z} + \Gamma_g \Delta i_{fg} = \frac{q \xi_h}{A_c}$$

4) the equation of state for the vapor:

$$i_g = i_g(P, T)$$

and

5) the constitutive equation of evaporation in the bubbly regime:

$$\Gamma_g = \frac{\xi_h}{A_c} \int_{z_0}^z \frac{dm(z, z')}{dz} J(z') dz'$$

In the above set we need to discuss only the last equation because all other relations are self-explaining. The vapor source term Γ_g , represents the rate of vapor mass formation per unit volume. In the bubbly flow regime, it depends on the rate of nucleation $J(z')$ and on the bubble growth $m(z', z)$. The first term represents the rate of bubble nucleation per unit area at a location z' in the duct. The latter describes the mass at a point z in the duct of a bubble that nucleated at z' . Both, the nucleation and the growth depend on the local liquid temperature. Consequently, when both $m(z', z)$ and $J(z')$ are specified, the constitutive equation of evaporation becomes a known function of liquid temperature which, in turn, varies with distance.

It can be seen that the problem is defined by five equations in terms of five variables, i. e., G_f , G_g , Γ_g , i_f and i_g . Since all of these variables are functions of temperature, the substitution of four of these equations into the energy equation yields an integro-differential equation in terms of the liquid temperature. If we assume that the vapor in the subcooled boiling region remains at saturation temperature, then this equation takes the form of:

$$\left[G - \frac{\xi_h}{A_c} \int_{z_0}^z m(z', z) J(z') dz' \right] c_p \frac{dT_L}{dz} + \left[\frac{\xi_h}{A_c} \int_{z_0}^z \frac{dm}{dz}(z', z) J(z') dz' \right] \Delta i_{fg} = \frac{q \xi_h}{A_c}$$

Thus, when the bubble nucleation and growth are known functions of temperature, the solution of this equation will yield the axial temperature distribution in the liquid. By substituting this distribution in Equation 11 one obtains the desired vapor mass flux density appropriate to the particular constitutive equation of evaporation for the bubbly flow regime.

The difficulty in following this formal approach at this time stems from the uncertainties related to 1) the rate of heterogeneous nucleation $J(z')$, 2) the nucleating characteristics of the surface which affect $J(z')$, and 3) the

bubble growth law $m(z^+, z)$. It was noted in [1, 2] that at the present time none of these parameters is known with sufficient accuracy to warrant the use of the formal approach to obtain the temperature distribution. Although several assumptions could be made to first evaluate the constitutive equation of evaporation and then to integrate the integro-differential equation for the temperature distribution, experimental data are not available which could be used to support these assumptions. Instead of using several assumptions pertaining to $J(z^+)$ and $m(z^+, z)$, only one assumption was introduced in [1, 2] i.e., the form of the temperature function was assumed directly. In addition to simplicity, this latter approach has two advantages at present. First, the boundary conditions for the temperature distribution are known and two, it is always more desirable to reduce the number of assumptions that are required to make a problem tractable.

The results presented in this paper indicate that the accurate determination of the initial point z_0 is by far more important than the precise specification of the temperature distribution in subcooled boiling. Consequently, the exponential or the hyperbolic tangent approximation for the temperature distribution may prove to be sufficiently accurate to reduce the incentive for determining the constitutive equation of evaporation. However, if investigations in the future reveal that a change of surface conditions or the introduction of additives changes the void fraction in the subcooled region, then the determination of the appropriate constitutive equation will be required. This statement follows from the fact that the rate of heterogeneous nucleation $J(z^+)$ is affected by the surface conditions of the heated wall as well as by the additives. It can be seen from the integro-differential equation that these will influence therefore the temperature distribution in the subcooled region. Consequently, different distributions T^+ could be expected for different nucleating characteristics.

It appears therefore that variations of surface conditions and introduction of additives may have two effects on the average void fraction. One may result from the shift of the point of initial vapor formation z_0 in Equation 1; the other from a change of the temperature distribution T^+ . Whether this is indeed the case remains to be shown by future experiments.

VII. CONCLUSIONS

1) It was shown previously [1, 2] that four effects must be taken into account in an analysis concerned with predicting the vapor void fraction in subcooled boiling. These are the effects of: 1a) the local relative velocity; 2b) the concentration profiles; 3c) the flow profile and 4d) the axial temperature profile. In [1, 2] these four effects were accounted for by means of

the vapor drift velocity V_{gj} , the distribution parameter C_0 , the dimensionless axial temperature distribution T^+ and the point of initial vapor formation z_0 .

2) In this paper the effects of these four parameters on the ability to predict accurately the void fraction in subcooled boiling has been evaluated and the results have been compared to recent experimental data.

3) It is shown that the effect of the distribution parameter C_0 is most pronounced in the bulk boiling region. Consequently, if in absence of experimental data on flow profiles in the subcooled region, a constant value of C_0 is to be taken for the entire test section then results of this paper indicate that this value of C_0 should correspond to the bulk boiling region.

4) The vapor drift velocity has only a minor effect in forced convection systems with high mass flow rates.

5) The point of incipient vapor formation z_0 greatly affects the predicted void fraction in the subcooled regime. No reliable criterion for predicting the location of this point exists at this time. It is concluded therefore, that further work to define and predict its location is mandatory for reliable and accurate prediction of void fractions in subcooled boiling.

6) The axial temperature distribution has been approximated by different functions that satisfy the physical boundary conditions. It appears that the precise specification of the temperature distribution is of secondary importance when compared to the need of locating properly the point of initial vapor formation. The profiles used up to date give satisfactory results when compared to available experimental data. However, final conclusion on this point has to be reserved until a method for predicting z_0 becomes available.

7) It appears that the previous history of the liquid in the subcooled region has only a minor effect on the void fraction in the bulk region.

8) The results of the analysis show that changing the conditions of the heated surface and the introduction of additives may have two effects on the void fraction in subcooled boiling. These effects are discussed and a method for taking them into account is presented.

9) Satisfactory agreement is shown between predicted void fractions and recent experimental data obtained in subcooled boiling of Refrigerant-22 and of water over a pressure range given by $0.22 \leq p_r \leq 0.65$ and $0.005 \leq p_r \leq 0.22$ respectively.

10) The results presented in this paper indicate that for the bulk boiling region, future investigations should be directed toward determining the flow and concentration profiles which specify the distribution parameter C_0 .

In the subcooled boiling region however, the primary emphasis should be placed on determining the point of incipient vapor formation as function of fluid properties, system geometry and operating characteristics.

B. EXPERIMENTAL RESULTS

The experimental results reported here were obtained in a vertical circular stainless steel tube of 0.40" inside diameter and 60.6" long. The data were taken at a reduced pressure of 0.65. The inlet subcooling was varied between 6F and 35F, the Refrigerant-22 flow rate between 290 lbs/hr and 560 lbs/hr, the exit quality between 0.07 and 0.36, and the heat flux between 6700 BTU/hr ft² and 19400 BTU/hr ft². The static pressures wall temperatures, and local chordal void profiles were taken at six points along the metal test section. The inlet and discharge pressures and temperatures, as well as the flow rate and electrical heat input, are continuously recorded. All measurements are carried out with instrumentation systems having an unattenuated frequency response of at least 30 cps, except for the wall sensors whose response is still much faster than that of the 0.02" wall to which they are fastened. A summary of these instrumentation systems is given in [14] with further information in [15] and [17]. A description of the test loop, from which the test sections have been successfully isolated as far as hydrodynamic or thermodynamic feedback is concerned, is also given in the above references.

Nine experimental runs in the metal test section at constant head addition are tabulated here. At present, further data at this reduced pressure are being taken to obtain further insight into void profiles in subcooling and at low qualities. These will be reported in a future quarterly report. The experimental accuracy, method of data reduction, and secondary observations are summarized in Appendices I, II and III of [18].

The local chordal and average void fractions for these runs are tabulated in Table 1. While the local heat flux, the wall-to-saturation temperature differences, and the local pressures have also been determined for these runs, they are not reproduced here but will be reported subsequently.

The experimental emphasis in this report is again placed on the description of the experimental and reduced void profiles as well as the average void fraction obtained at a reduced pressure of 0.65. The corresponding data obtained at reduced pressures of 0.12, 0.22 and 0.39 are discussed in [18] [19] and [20].

TABLE I
Void Fraction Data For Metal Test Section at $p_r = 0.65$

Symbol	Run No.	Discharge Pressure (PSIA)	Flow Rate (lb/hr)	Inlet Subc. (F)	Heat Flux (BTU/hr.ft. ²)	Local Qual. x_{eq}	Avg. Void Fraction	A $r/R = -.84$	A' $-.75$	B $-.49$	C 0	D $+.49$	E $+.75$	E' $+.84$
◆	SC149VI1	469.5	292.3	11.6	13,900	4.8F*	0.09	0.28		0.12	0.05	0.06		0.05
						0.02	0.25	0.37		0.23	0.26	0.25		0.23
						0.11	0.45				0.43	0.50		0.41
						0.20	0.53				0.69	0.59		0.23
						0.30	0.61				0.77	0.67		0.11
						0.36	0.66		0.51	0.70	0.77	0.77	0.56	
◇	SC150VI2	466.8	291.4	7.0	6,700	3.7F	0.03	0.14		<0.02	0.02	0.03		0.09
						0.02	0.14	0.23		0.11	0.16	0.13		0.16
						0.04	0.26				0.25	0.22		0.42
						0.09	0.39				0.38	0.41		0.44
						0.13	0.38				0.49	0.43		0.06
						0.17	0.45		0.33	0.48	0.54	0.50	0.31	
□	SC153VI3	469.5	299.8	15.9	8,200	11.4F	0.03	0.04		<0.02	0.02	0.04		0.15
						7.0F	0.11	0.65		0.04	0.10	0.09		0.27
						0.8F	0.19				0.12	0.17		0.39
						0.05	0.33				0.28	0.35		0.43
						0.10	0.38				0.42	0.44		<0
						0.14	0.42		0.42	0.44	0.49	0.46	0.34	
●	SC157VI6	470.4	301.9	17.5	10,440	11.9F	0.02	0.37		<0.02	<0.02	0.02		>0.02
						6.3F	0.13	0.38		0.10	0.12	0.14		0.14
						<0.02	0.20				0.15	0.21		0.28
						0.08	0.43				0.35	0.45		0.59
						0.15	0.51	0.31	0.45	0.53	0.56	0.54	0.55	0.85
						0.20	0.51		0.30	0.52	0.63	0.53	0.60	

*F here refers to local sub-cooling based on thermodynamic equilibrium

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TABLE I Continued

Symbol	Run No.	Discharge Pressure (PSIA)	Flow Rate (lb/hr)	Inlet Subc. (F)	Heat Flux (BTU/hr.ft. ²)	Local Qual. x _{eq}	Avg. Void Fraction	A r/R=-.84	A' -.75	B -.49	C 0	D +.49	E +.75	E' +.84
▼	SC158VI7	469.7	537.3	6.1	16,900	1.8F 0.03 0.09 0.15 0.21 0.25	0.09 0.32 0.47 0.58 0.59 0.65				0.05 0.20 0.37 0.52 0.68 0.76	0.09 0.31 0.46 0.62 0.42 0.68		0.17 0.58 0.74 1.0 0.60 0.57
▽	SC159VI8	470.5	561.1	16.3	19,060	11.0F 5.5F 0.02 0.09 0.15 0.20	<0.02 0.14 0.28 0.47 0.64 0.63	0.30 0.52		<0.02 0.13	<0.02 0.08 0.18 0.38 0.56 0.61	<0.02 0.17 0.30 0.47 0.65 0.69		<0.02 0.21 0.42 0.66 0.83 0.80
▲	SC160VI9	469.8	553.4	8.3	8,670	6.0F 3.4F <0.02 0.03 0.06 0.09	0.02 0.07 0.17 0.32 0.33 0.38	0.30 0.30		<0.02 0.04	<0.02 0.03 0.10 0.19 0.32 0.37	<0.02 0.11 0.19 0.34 0.38 0.45		<0.02 0.14 0.23 0.54 0.11 0.28
▽	SC161VI10	470.5	560.5	35.3	19,400	27.3F 21.1F 13.9F 5.6F 0.02 0.07	<0.02 0.05 0.16 0.24 0.27 0.44	0.28 0.05		<0.02 0.02	<0.02 0.04 0.02 0.05 0.21 0.36	<0.02 0.08 0.16 0.19 0.27 0.45		<0.02 0.09 0.97 0.67 0.14 0.61
▽	SC162VI11	470.6	298.9	18.0	10,600	12.2F 6.5F 0.01 0.08 0.15 0.20	0.03 0.09 0.24 0.37 0.50 0.51	0.21 0.21 0.47 0.18 0.61 0.20		<0.02 0.08 0.22 0.36 0.55 0.49	<0.02 0.05 0.17 0.34 0.53 0.65	<0.02 0.11 0.23 0.40 0.52 0.62		0.32 0.11 0.41 0.71 0.27 0.49

I. VOID FRACTION PROFILES

At lower reduced pressures it was found [18], that the radial void profiles in the quality region could be correlated by

$$\frac{\alpha}{\alpha_c} = 1 - \left(\frac{r}{R} \right)^n \quad (16)$$

α_c and n are obtained from the measured values of the chordal void fraction along a tube diameter α_{DIA} , and the average void fraction in a cross section of the tube $\langle \alpha \rangle$ using

$$\frac{\alpha_{DIA}}{\langle \alpha \rangle} = \frac{n+2}{n+1} \quad (17)$$

and

$$\langle \alpha \rangle = \alpha_c \frac{n}{n+2} \quad (18)$$

It was further found and reported in [18] and [19], that at low reduced pressures, the void profiles were essentially symmetric at qualities above 3%. For these circumstances, it was shown there that three local chordal void measurements (at $r/R = 0, +0.49$, and $+0.83$) were sufficient to obtain reliable void profiles and average void fractions for the bulk boiling regime. Only at the first and second station along the test section and at the top (sixth) station, a complete traverse of five measurements was taken at $r/R = 0, \pm 0.49$ and ± 0.83 (or ± 0.75). At the two lower stations sub-cooling usually prevailed while at the top station secondary effects from the subsequent adiabatic section may have disturbed the symmetry of the void profiles.

The same traversing arrangement was initially used for the tests reported here. However, it soon became apparent that at reduced pressure of 0.65, the non-symmetric profiles dominate at qualities up to 0.20. A tentative explanation for this result can be advanced by noting that the propensity for bubble migration toward the center is reduced as pressure is increased. A decreasing migration propensity with increasing pressure

can be expected as the densities of the vapor and of the liquid phase approach each other. Furthermore, at higher pressures, an increase of outlet quality is limited by the formation of dry patches on the wall and the subsequent burn-out. Consequently, the region of subcooled or non-equilibrium boiling which at lower pressures affected the inlet profile only, extends over most of the test section at a reduced pressure of 0.65.

Some of the local chordal void profiles obtained from the tests are shown in Figures 15 to 18. The data for the region of subcooled boiling, extending up to a quality of 0.20, generally exhibit great irregularity and asymmetry. The corresponding curves in Figures 15 to 18 have been fitted to the test data as well as possible. They generally display the major typical profiles of Figure 1. However, it is obvious that the individual curves as shown, can by no means be taken as firm results. There is even reason to doubt whether the individual local chordal measurements are repeatable. The profiles of Figures 16 and 17 were obtained for the same test conditions. As can be seen, the local void fractions are not well repeated, except at the center where agreement is fair. The average void fractions at each station for the above two tests are compared in Figure 19 and agree well for stations one, five and six. At stations three and four, better agreement might have been obtained if test No. SC 157 VI 6 (Figure 17) had been run with 5 point traverses at these stations as they were taken for test No. SC 162 VI 11 (Figure 16). This contention is reinforced by the smooth curve which can be fitted through the data of the latter test.

The above discussion clearly indicates the need for further tests to determine the repeatability of

- (a) local chordal void fractions
- (b) average void fractions

Some of the local chordal void profiles suggest that local effects, like the surface nucleation characteristic are superimposed over the general profiles which develop as described in Figure 1. For instance, Figures 16 to 18 show extremely high void fractions for the fourth station at a radius ratio of +0.83. However, in Figure 15 the corresponding test point falls into the region of bulk boiling and the void fraction at that point is no longer excessive.

The parabolic chordal profiles of the bulk boiling region can be fitted with sufficient accuracy by Equation 16. Figure 20 shows the resulting radial

Figure 15. Experimental Chordal Void Fractions. — — — — — Curves Fitted to Data, — — — — — Curves Computed from Radial Profiles.

Figure 16. Experimental Chordal Void Fractions.

Figure 17. Experimental Chordal Void Fractions.

Figure 18. Experimental Chordal Void Fractions.

Figure 19. Repeatability of Average Void Fractions in Subcooled Boiling at High Pressures.

Figure 20. Local Void Fractions Computed from Experimental Chordal Data.

profiles, corresponding to the chordal profiles of Figure 15, which are in good agreement with the test points.

II. AVERAGE VOID FRACTION

The method of obtaining the average void fraction over the tube cross section at each of the six vertical stations is given in [18]. These average values together with the local thermodynamic quality and mass flow rate are included in Table 1.

In [18] the following relationship for void fractions with and without heat addition was derived*

$$\frac{\langle j_g \rangle}{\langle \alpha \rangle} = C_o \langle j \rangle + \bar{V}_{gj} \quad (19)$$

where the distribution parameter C_o and the weighted mean vapor drift velocity are defined as

$$C_o = \frac{\langle \alpha j \rangle}{\langle \alpha \rangle \langle j \rangle} \quad (20)$$

and

$$\bar{V}_{gj} = \frac{\langle \alpha V_{gj} \rangle}{\langle \alpha \rangle} \quad (21)$$

For the churn turbulent bubbly flow regime the weighted mean vapor drift velocity is

$$\bar{V}_{gj} = 1.41 \sqrt[4]{\frac{\sigma_g \Delta \rho}{\rho_f}} \quad (22)$$

* References to preceding work are given in [18].

The experimental data for the bulk boiling region obtained at reduced pressures of 0.12, 0.22 and 0.39 have verified that the distribution parameter remains constant within a flow regime. Plotted in a $\langle j_g \rangle / \langle \alpha \rangle - \langle j \rangle$ plane, (i. e. vapor velocity versus mixture flux plane) the test data within a flow regime can then be presented as a straight line, with the gradient equal to C_0 , and the $\langle j_g \rangle / \langle \alpha \rangle$ axis intercept equal to $\bar{V}_{gj}$.

Most of the data obtained to date at a reduced pressure of 0.65 fall into the subcooled boiling region. However, those points which fall into the region of bulk boiling at the low mass flow rate of 340,000 lb/ft²hr (46 g/sec.cm²) are presented in Figure 21 in a velocity-flux plane. With a vapor drift velocity of 7.5 cm/sec the few available data points are well presented by a distribution parameter C_0 of 1.10. This provides an initial confirmation that the method of predicting void fractions as developed and proven at lower reduced pressures can be extended to a reduced pressure of 0.65.

At lower reduced pressures the average void fractions have been presented as function of vapor quality in thermodynamic equilibrium. For the tests taken to date at a reduced pressure of 0.65 most of the data have been shown to fall into the region of subcooled boiling, where the actual steam quality is higher than the value computed for thermodynamic equilibrium, as indicated in Figure 3. For uniform power input a presentation of average void fraction $\langle \alpha \rangle$ versus equilibrium quality x_{eq} is equivalent to a plot of $\langle \alpha \rangle$ versus the axial coordinate z , with a scale factor between quality and axial coordinate which can be obtained from an energy balance, as function of heat flux and mass flow:

$$x_{eq} \equiv \frac{i - i_{f \text{ sat}}}{\Delta i_{fg}} = \frac{q \xi_h}{A_c G \Delta i_{fg}} (z - z_1) \quad (23)$$

The scale can be extended into the region below $x_{eq} = 0$ using the same relationship which then gives "negative qualities". From above considerations, it is obvious that two tests of identical mass flows and heat input should show the same void fraction versus quality relationship for the region of bulk boiling. Figure 22 shows that the experimental data confirm such behavior for the bulk boiling region which both curves diverge in the subcooled region, corresponding to different amounts of inlet subcooling. These results confirm also the conclusions made in section IV-3 of Part A that the effects of the subcooled region will not significantly affect the bulk boiling region (see Figure 8).

Figure 21. Plot of Experimental Data at a Reduced Pressure of 0.65 in the Velocity-Flux Plane.

Figure 22. Average Void Fractions for Different Inlet Subcooling at a Reduced Pressure of 0.65.

A direct comparison of the experimental data with those predicted by Equation 1 for subcooled boiling at reduced pressures of 0.65 is included in part A of this report.

In correlating the experiments with oscillating heat input, a plot of the ratio of chordal voids along the diameter to the average voids $\alpha_{DIA} / \langle \alpha \rangle$ as a function of quality had been introduced, [21]. There has been some indication that in the region of bulk boiling and at low qualities the ratio $\alpha_{DIA} / \langle \alpha \rangle$ decreases, which can also be expected from the schematic void profiles of Figure 1 (Station 4). However, the test data available to date are insufficient to prove this point now. The subject will be discussed in a future quarterly report, when more data are available.

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