The Boiling Crisis Phenomenon in Two-phase Flow

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An analytic model of the flow boiling crisis in annular flow (at high vapour quality) and own experimental results of Freon 21 are presented in the paper. The model is based on the analysis of the film drying process on the channel wall, thus expressing the mass balance of both the film and the core by means of differential equations. The solution of these equations contains the parameters determined experimentally since theoretical predictions are not possible at this stage.

Nomenclature

C	parameter in the mass transfer equations
c	concentration of droplets in the flow core
D	mass flux of deposition
d	non-dimensional mass flux of deposition d = D/G, channel diameter
E	mass flow of entrainment
e	non-dimensional mass flux of entrainment e = E/G
G	mass flux of the main flow
g	non-dimensional mass flux of the main flow
K	parameter in the mass transfer equations
k	mass transfer coefficient
p	pressure
r	latent heat of vaporization
S	non-dimensional constant $S = \frac{Yz_{cr}}{2x_{cr}}$
t	non-dimensional parameter $t = x_{cr}\frac{z^+}{z^+_{cr}}$
V	non-dimensional specific volume $V = \frac{v_L}{v_G}$
v	specific volume
x	steam quality
Y	parameter
z	longitudinal co-ordinate
z^+	non-dimensional longitudinal co-ordinate
q	heat flux
q^+	non-dimensional heat flux

Greek symbols

ρ	specific density
σ	surface tension
μ	dynamic viscosity
ν	kinematic viscosity

Subscripts

c	droplet in the core; core
E	entrainment; equilibrium
D	deposition
F	film
G	gas
j	flow core
cr	boiling crisis, critical state
L	liquid
o	beginning of the annular flow, channel diameter

Introduction

The boiling crisis phenomenon occurs in different technical devices, e.g. in nuclear reactors, steam generators and refrigeration evaporators. In the case of the high parameters equipment with two-phase vapour-liquid flow (boiling nuclear reactors, steam generators) the boiling crisis may result in serious damage due to the burn-out of the steam channels. In the low parameters steam generators, e.g. working on low-boiling media, the crisis diminishes the heat transfer intensity and is not desirable for technical reasons. Such generators are of increasing interest. Besides in refrigeration technology, they are used in utilizing the waste heat of chemical processes and will probably be applied in the new geothermal or solar plants with turbines working on low-boiling media.

Boiling is generally characterized by a high heat transfer coefficient, so that a large heat flux can be sustained at a fairly low temperature difference between the heat transfer surface and the boiling fluid. However, if attempts are made to increase the heat flux beyond a certain level, the nature of the boiling process changes radically and the heat transfer coefficient drops dramatically. This limiting heat flux is known as the "critical heat flux – CHF" or as "burn-out" [7, 10]. Numerous investigations performed in the last decade were concerned with the mechanism of the boiling crisis and the methods of its prediction.

Satisfactory solutions of this very complex problem have not yet been obtained. Practically no comprehensive analytic studies of the boiling crisis phenomenon exist, except for some simple models. As a substantial number of data does now exist, mostly for water, empirical or semi-emperical solutions [1, 15, 25], as well as a solution based on an annular flow model [6, 9-13, 26-29] were established, these solutions being applicable to any fluid.

Another criterion of dry-out as a limit of mist flow in upstream approach [18] can be very useful in establishing the scaling law for the modelling of the CHF in drop-annular flow.

A general review on recent work in this field is provided for example in [17] and [24]. In this paper an analytic model of the boiling crisis at high vapour quality and the author's investigations of that phenomenon are presented. The model employs the analysis of the film dry-out process on the wall which allows formulation of the differential equations of mass balance in the film and the core. The solution of these equations contains the parameters which are determined experimentally, since their theoretical prediction involves serious difficulties.

The theoretical model of the flow boiling crisis

The analytic model of flow boiling crisis in annular flow (at high vapour quality) is presented below. The model is based on the Harwell annular CHF model [26]. The Harwell model based on the film dry-out process involves a complex computer procedure, but gives fairly good results for water and other media, e.g. Freon 12, Fig. 1,2 [9]. In this paper the basis of an analytical model of the boiling crisis at high vapour quality and the author's investigations of that phenomenon are presented [20-24]. Let us consider the two-phase annular flow of mass flux G in a circular channel of internal diameter d_0. The liquid film

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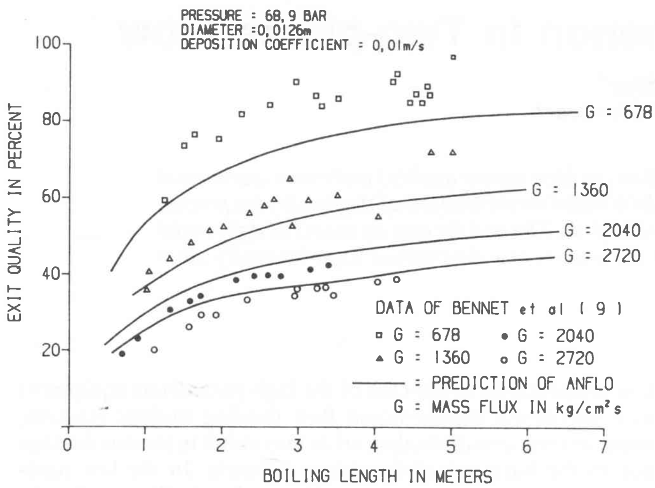

Figure 1 – Dryout in steam-water flow after [9]

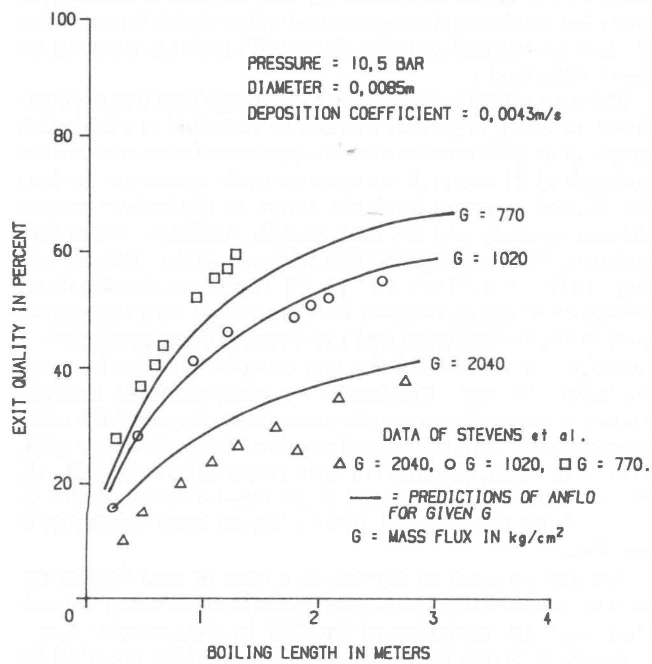

Figure 2 – Dryout in Freon-12 flow, after [9]

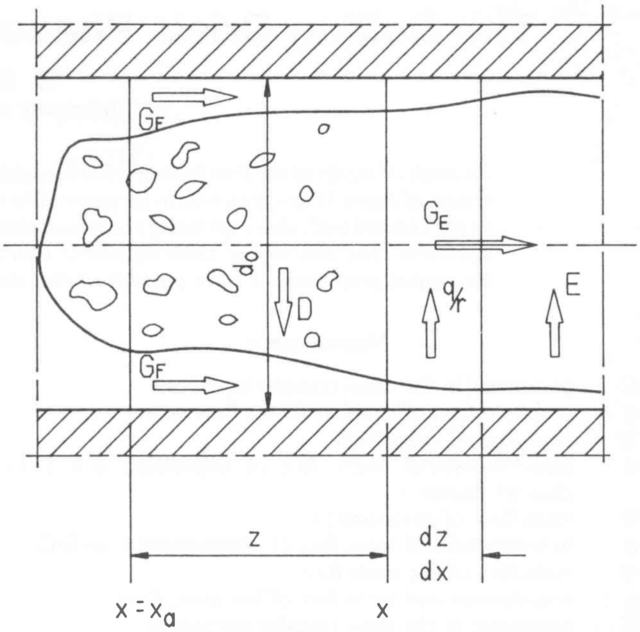

Figure 3 – A model of the annular flow with boiling

covers the channel wall, and the vapour with suspended droplets of liquid, flows in the core, Fig. 3. The beginning of the annular flow pattern is assumed to be known and from that point the mass balance starts. As a criterion of the CHF the film dry-out on the wall is assumed; however, recent investigations [19] suggest film breakdown into rivulets rather than its entire evaporation. For the sake of simplicity it is assumed that the crisis occurs at a film flow-rate equal to zero. This simplification was justified in [21, 22]. The mass flux of evaporation from the liquid film due to the wall heat flux q, is equal to q/r. This process is accompanied by the droplet entrainment from the film surface E and the droplet deposition onto the film surface D. The "flashing" terms [28] are not considered as secondary effects.

In accordance with the assumed model, the elementary mass changes of the film and the core are:

$$\frac{dg_F}{dz^+} = 4\,(d - e + q^+) \tag{1a}$$

$$\frac{dg_E}{dz^+} = 4\,(-d + e) \tag{1b}$$

where

$$d = \frac{D}{G};\; e = \frac{E}{G};\; q^+ = \frac{q}{rg};\; z^+ = \frac{z}{d_o}$$

In order to solve the set of equations (1a, b) the fluxes d and e must be known. It was assumed

$$e = K.C.g_F \tag{2}$$

$$d = C.g_E \tag{3a}$$

where K and C are the parameters not exactly defined at the moment. This assumption differs from the Harwell model, but according to [2, 3, 5, 8, 14, 29], experiments show that the dimensionless deposition coefficient is a function of the concentration of entrained droplets and the rate of entrainment is proportional to the liquid film flowrate and to the gas velocity, Fig. (4, 5). These relations are also expressed by eqs. (2, 3a).

According to [26] the deposition mass flux rate d may be expressed by

$$d = \frac{k.c_j}{G} \tag{3b}$$

where k is the mass transfer coefficient and c_j signifies the concentration of liquid within the core and may be determined after [13], as

$$c_j = \frac{1 - x_j}{(1 - x_j)\,v_L + x_j\,v_g} \tag{4}$$

Steam quality within the flow core is defined as follows:

$$x_j = \frac{G_G}{G - G_F} = \frac{1 - g_E - g_F}{1 - g_F} \tag{5}$$

The general formula describing the mass transfer coefficient k is given in [21] as

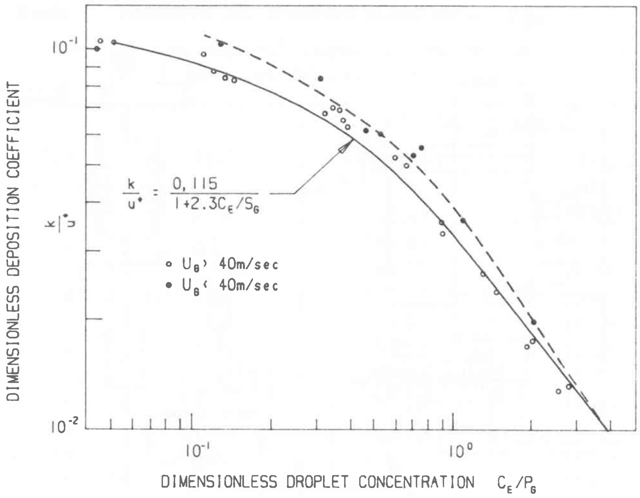

Figure 4 – Correlation of deposition coefficient by dimensionless relaxation time, after [3]

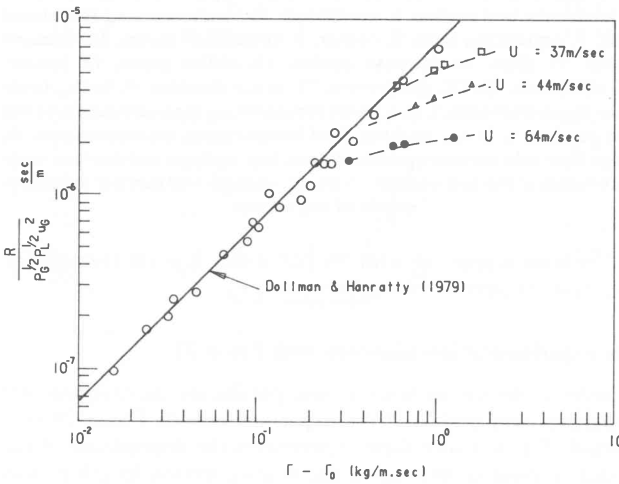

Figure 5 – Correlation of the rate of entrainment as a function of the Liquid flowrate per unit perimeter, after [3]

$$k = C_k \frac{G^n}{d_o^m} (1 - g_F - g_E)^n \qquad (6)$$

where C_k is a function of the fluid properties and the two-phase characteristics (v_G, μ_G, T, ρ_c, v_c, d_c ...) and m, n are not exactly known exponents*.

Combining equations (2), (3a, b), (4) and (6) one finds

$$d = C_k' \frac{g_E(1 - g_F - g_E)^n}{g_E\, v_L + (1 + g_F - g_E)\, v_G} \qquad (7)$$

$$e = KC'_k \frac{g_F\,(1 - g_F - g_E)^n}{g_E\, v_L + (1 + g_F - g_E)\, v_G} \qquad (8)$$

$$\text{where} \quad C_k' = C_k \frac{G^n - 1}{d_o^m} \qquad (9)$$

Then from equation (1a, b), (7) and (8) a set of nonlinear differential equations describing the dimensionless mass fluxes within the film and the core is obtained

$$\frac{dg_F}{dz^+} = 4\,(C_k' \frac{g_E\,(1 - g_F - g_E)^n}{g_E\, v_L + (1 + g_E - g_F)\, v_G} -$$

$$K\, C_k' \frac{g_F(1 - g_F - g_E)^n}{g_e\, v_L + (1 - g_E - g_F)\, v_G} - q^+) \qquad (10a)$$

$$\frac{dg_E}{dz^+} = 4\,(-C_k' \frac{g_E\,(1 - g_F - g_E)^n}{g_E\, v_L + (1 + g_E - g_F)\, v_G} +$$

$$K\, C_k' \frac{g_F(1 - g_F - g_E)^n}{g_E\, v_L + (1 - g_E - g_F)\, v_G}) \qquad (10b)$$

Rearranging equations (10a, b) one obtains the first-order nonlinear differential equation for the film mass flux

$$\frac{dg_F}{dt} = S \frac{(1 - g_F - g_E)^n (g_E - K.g_F)}{g_E\, V + (1 - g_E - g_F)} - 1 \qquad (11)$$

$$\text{where} \quad t = x_{cr} \frac{z^+}{z_{cr}^{\cdot}} \qquad (12)$$

$$S = \frac{4z_{cr}^{\cdot}\, C_k'}{x_{cr}\, v_G} = \frac{Y\, z_{cr}}{2\, x_{cr}} \qquad (13)$$

$$V = \frac{v_L}{v_G} \qquad (14)$$

$$\text{and} \quad q^+ = \frac{x_{cr} - x_o}{4z_{cr}^{\cdot}} \simeq \frac{x_{cr}}{4z_{cr}} \qquad (15)$$

According to Hewitt [13], and Whalley et al [26], it was recognised that the annular flow pattern exists at a steam quality equal to 0,01 and if the share of the film flow is about 0,01 of the total mass flow-rate of the liquid phase. However the most recent results [9], show that for high-pressure water-steam flow the annular flow conditions occur at $x_o = 0{,}05$. For the higher steam qualities at the onset of annular flow, equation (11) takes the following form:

$$\frac{dg}{dt^*} = S^* \frac{(1 - g_F - g_E)^n\, (g_E - K\, g_F)}{g_E\, V + (1 - g_E - g_F)} - 1 \qquad (11^*)$$

where

$$t^* = (x_{cr} - x_o) \frac{z^+}{z_{cr}^{\cdot}} \qquad (12^*)$$

$$S^* = \frac{4z_{cr}^{\cdot}\, C_k}{(x_{cr} - x_o)\, v_G} = \frac{Yz_{cr}}{2(x_{cr} - x_o)} \qquad (13^*)$$

$$q^+ = \frac{x_{cr} - x_o}{4z_{cr}^{\cdot}} \qquad (15^*)$$

and other quantities remain unchanged.

Equation (11) or (11*) cannot be solved analytically and, hence, a numerical method was used. As a result the function of the critical steam quality x_{cr} vs the non-dimensional co-ordinate Yz_{cr} for selected values of K, n and V was obtained. The results are shown in Fig. 6. For the sake of comparison the results of the analytic solution of the simplified form of equation (11) with the assumption of n = 1 and $V \cong 0$ are presented in Fig. 6. The above simplifications allow direct determination of the steam quality in the cross section of crisis (where the film disappears) as a function of non-dimensional co-ordinate of boiling Yz_{cr} (or $Yz_{cr}^{\cdot}$).

*For small droplets $d_o \leq 0{,}1\ \mu m$, n = 0,8, m = 0,2, [21].

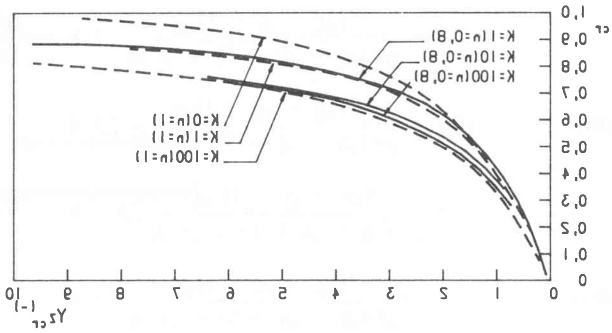

Figure 6 – The generalized function X_{cr} (Yz_{cr}) predicted numerically for n = 0,8, V = 0,5 vs the simplifled analytic solution for n = 1, V = 0 after equation (16)

From equation (11)

$$x_{cr} = \frac{0{,}99\, e^A - (0{,}98 - 0{,}01K)}{\frac{K}{A}(e^A - 1) + e^A} \cong \frac{1}{\frac{K}{A} + \frac{e^A}{e^A - 1}} \qquad (16)$$

where $A = \frac{1 + K}{2} Yz_{cr}$

For large values of K (> 20) equation (16) takes a simpler form

$$x_{cr} = \frac{1}{1 + 2/Yz_{cr}} \qquad (16a)$$

which is similar to that determined experimentally by Silvestri (after [4]). From equation (11*)

$$x_{cr} = \frac{(1 - g_{Fcr}(1+K))e^{A^+} + (x_o + g_{Fo}(1+K) - 1) + K/A^+ X_o(e^{A^+} - 1)}{K/A^+(e^{A^+} - 1) + e^{A^+}} \qquad (16^*)$$

$$A^+ = \frac{Y z^+_{cr}(1 + K)}{2}$$

The influence of the quantities g_{Fo}, x_o and g_{Fcr}, is shown in Fig. 7. It is seen from Fig. 6 that the analytic solution of the simplifled form of equation (11) given by equation (16) is similar to the numerical solution of the exact equation (11). The further analysis is continued with the simplifled model described by equation (16).

The parameter Y has a dimension [m^{-1}] and, as it follows from the analysis performed in [21], is a function of the typical parameters influencing the crisis: mass flux G, channel diameter d_o, viscosity of the gas phase μ_G, specific volume of the gas phase v_G etc.

The analytic determination of Y for given crisis conditions is rather difficult and it sems most reasonable to correlate that parameter on an experimental basis.

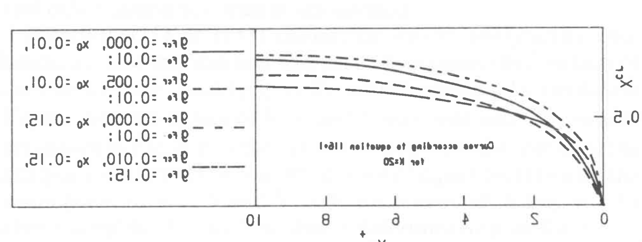

Figure 7 – Analytic solution X_{cr} vs Yz^+_{cr} after equation (16*); n = 1, V = 0

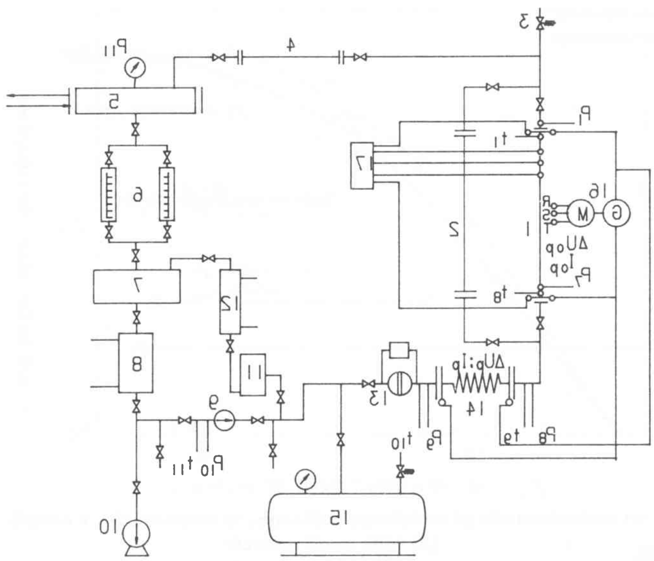

Figure 8 – Layout of the experimental apparatus for the boiling crisis investigations. 1. vertical test section, 2. by-pass, 3. safety-valve, 4. horizontal test section, 5. condenser, 6. condensate measurement tank, 7. equalizing tank, 8. cooler, 9. circulation pump, 10. vacuum pump, 11. filter, 12. by-pass cooler, 13. orifice plate, 14. heater, 15. main tank, 16. DC generators, 17. crisis detector, P_1 to P_{11} pressure measurements, t_1 to t_8, wall temperature measurements of the test section, t_9 to t_{11} working fluid temperature measurements, G, mass flow rate measurements, △Uop, Iop, voltage and current measurements of the test section, △Up, Ip, voltage and current measurements of the heater.

The same is applicable for the parameter K in the formula for the mass transfer of entrainment rate e.

The experimental investigations with Freon 21

In order to determine both of these parameters an experimental apparatus was built and the experiments with Freon 21 performed, Fig. 8. From these experiments the dependence of the critical vapour quality x_{cr} vs the boiling section length z_{cr} was established. The length of the boiling section was determined from the heat balance with an additional assumption of $x_o = 0{,}01$ at the beginning of the annular flow pattern. Subsequently, the curves predicted from the simplifled analytic model [eq. (16)] were fitted to the experimental points using the least squares technique. Therefore, as a result of optimisation computations based on equation (16) the best values of the parameters K and Y were obtained. It was established that the parameter K for Freon 21 varies between 16 and 26 and may be assumed to be a constant equal to 20. Such an assumption slightly affects the analytic curve (16) and substantially simplifies further considerations.

It was mentioned before that the parameter Y is the following function

$$Y = Y\,(G,\, p,\, d_o,\, \ldots) \qquad (17)$$

Moreover, this parameter may be affected by the other properties, not expressed in equation (17), but influencing deposition or the entrainment process, e.g. surface tension σ. Dimensional analysis of Y provides the non-dimensional number Yd_o dependant on the two-phase flow criterial numbers. The following expression for Yd_o is proposed.

$$Yd_o = A \left(\frac{G\, d_o}{\mu_G}\right)^b \left(\frac{\rho_L\, \sigma\, d_o}{\mu_G^2}\right)^{b_1} f\left(\frac{p}{p_{cr}}\right) \qquad (18)$$

The constants A, b and b_1 and the form of the function $f(p/p_{cr})$

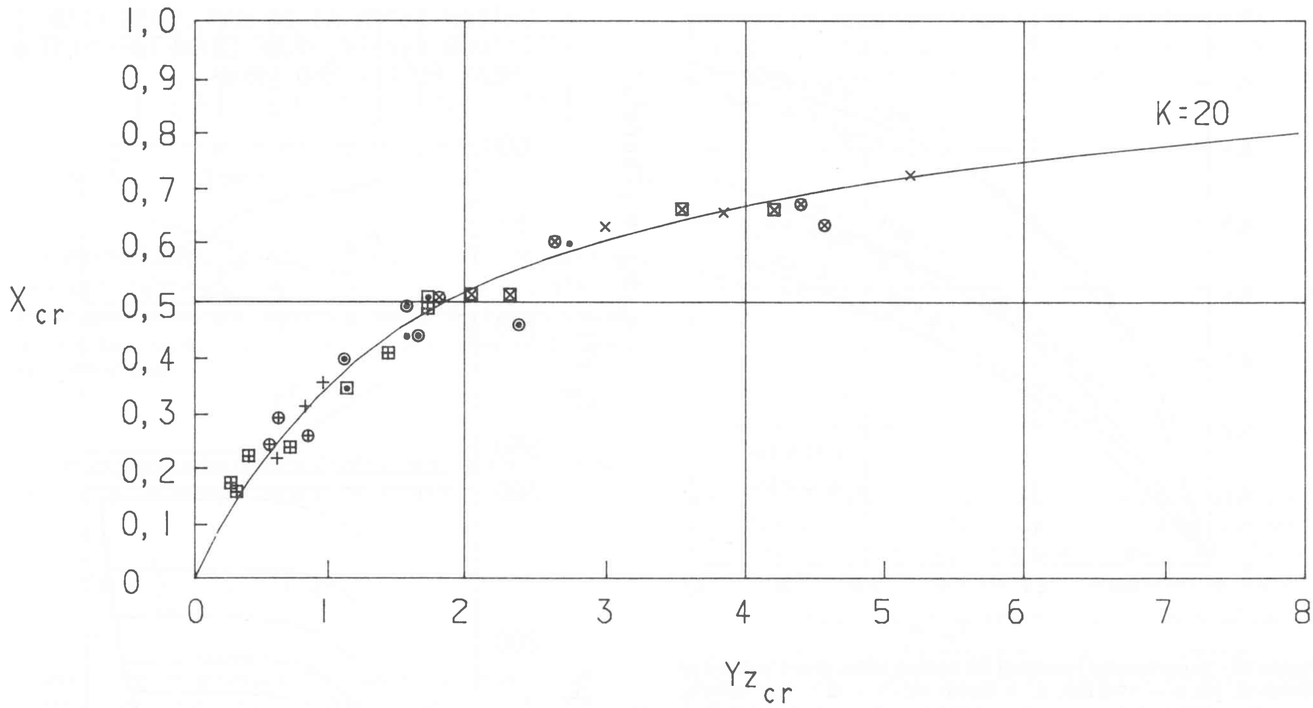

Figure 9 – Critical vapour quality X_{cr} vs non-dimensional critical boiling length YZ_{cr} after eqns (16) and (20) in comparison with the experimental data for Freon-21 (d_o = 0,008 m, p = 5,5, 10,6, 15,0 bar)

were determined from the experiments with Freon 21 so that the following formula for Y d_o was obtained.

$$(Y\,d_o)_{R-21} = 1{,}27 \times 10^6 \left(\frac{G\,d_o}{\mu_G}\right)^{-1{,}40} \left(1 - 1{,}9\,\frac{p}{p_{cr}}\right) \qquad (19)$$

In the experiments with Freon 21 the mass flux G and the pressure were variable and the tube diameter d_o was constant.

The critical vapour quality x_{cr} as predicted by equation (16) and (19) is compared to experimental results in figure 9. It follows that 74% of the experimental points lie within the error of plus minus 10% and all of them within plus minus 25% error.

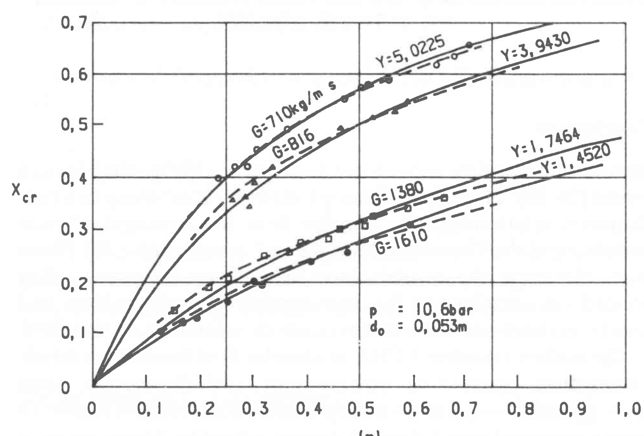

Figure 10 – Experimental results of the boiling crisis investigations of Freon-12, [4], p = 10,6 bar, d_o = 0,0053 m: ○ – G = 710 kg/m²s, △ – G = 815 kg/m²s, □ – G = 1 380 kg/m²s, ● – G = 1 610 kg/m²s, (...) after [4], (–) eqn (16)

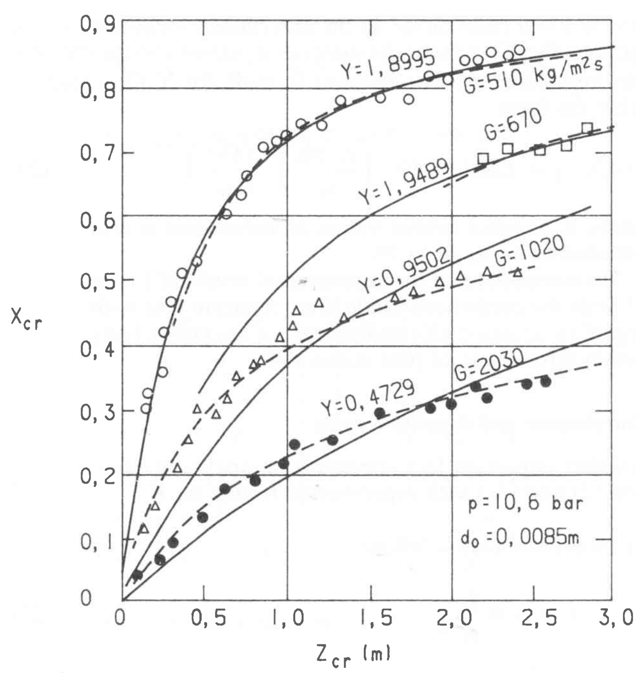

Figure 11 – Experimental results of the boiling crisis investigations of Freon-12, [4], p = 10,6 bar, d_o = 0,0085 m: ○ – G = 510 kg/m²s, □ – G = 670 kg/m²s, △ – G = 1 020 kg/m²s, ● – G = 2 030 kg/m²s; (...) after [4], (–) eqn (16)

The comparison of the experimental results for Freon 21 and Freon 12

For the sake of comparison the predictions after the model described here were carried out for the experiments with Freon 12 performed by Stevens, (after [4]). The results, shown in Figs. 10-12 are in very good agreement with the experiments, particu-

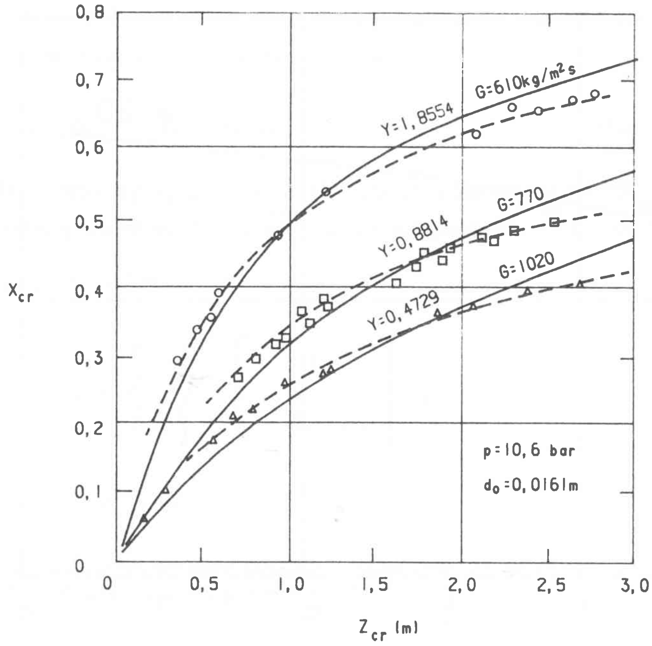

Figure 12 – Experimental results of the boiling crisis investigations of Freon-12, [4], p = 10,6 bar, d_o = 0,0161 m: ○ – G = 510 kg/m²s, □ – G = 770 kg/m²s, △ – G = 1020 kg/m²s; (...) after [4], (–) eqn (16)

larly at lower mass fluxes. In the experiments mentioned above the mass flux G and the tube diameter d_o varied and the pressure was kept constant. The relevant formula for Y for Freon 12 takes the form.

$$(Yd_o)_{R-12} = 1{,}805 \times 10^{-3}\left(\frac{\rho_L\,\sigma d_o}{\mu_G^2}\right)^{1{,}17}\left(\frac{Gd_o}{\mu_G}\right)^{-1{,}60} \tag{20}$$

where K assumes similar values as before and is also assumed to be equal to 20.

The comparison of the experimental results of Freon 12 with the predictions made after equation (16) with regard to equation (20) shows most of the points lying within error limits of plus minus 15%.

Entrainment and deposition rates

Another important fact appears from analysing equation (2) and (3a) with experimental results for K.

From (2) and (3a) it follows

$$e = k.d.\frac{g_F}{g_E} \tag{21}$$

where K can be assumed as about 20 and $g_{Fo}/g_{Eo} \simeq 0{,}01$ (according to Hewitt [13]).

then

$$e_o \cong 0{,}20\ d_o \tag{21a}$$

which is a local value at the beginning of annular flow and corresponds fairly well with the last results of the calculation of e and d rates, Fig. 13, [29]. It can also be seen that the local entrainment to the deposition rate becomes less in the axial direction toward CHF point which is in agreement with the assumed model and was previously indicated by Hewitt, [13].

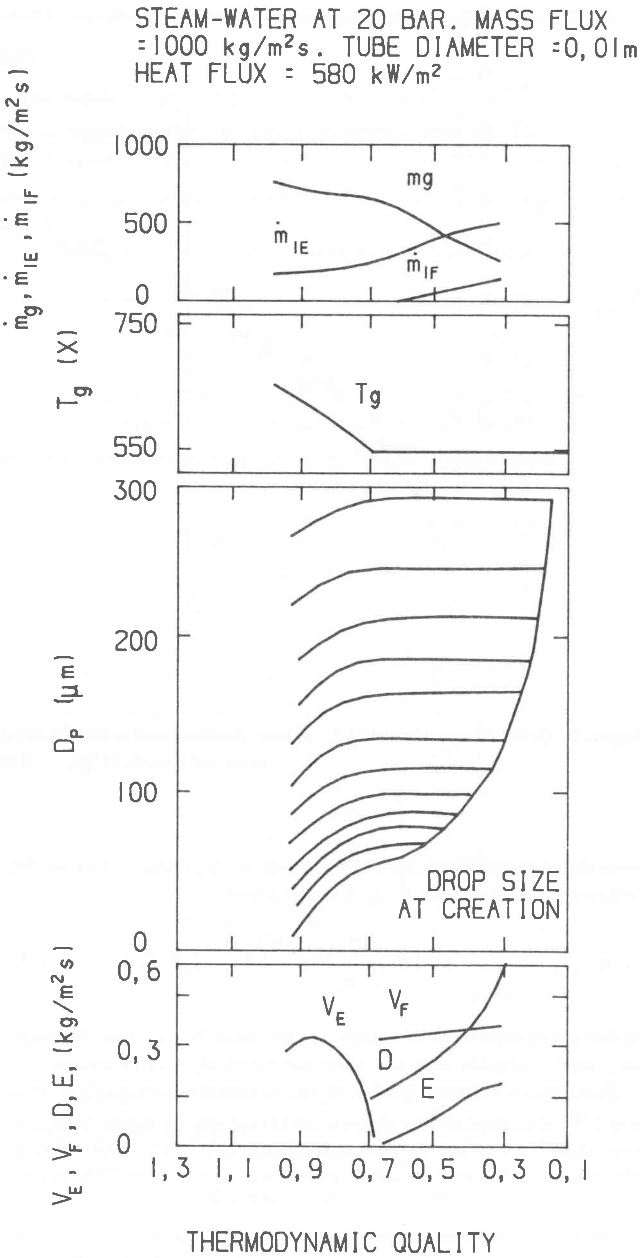

Figure 13 – Calculations of local parameters in evaporating flow with dryout and subsequent post-dryout region (UKAEA Harwell/University of Oxford), after [29]

Conclusions

Present annular flow models are based primarily on the Harwell model [26-29]. The calculations are made by combining two fundamental relationships for annular flow: the "triangular" relationship and the "interfacial roughness" correlation, [10]. However, although the models have been very successful, they depend on correlations for entrainment rate, deposition and interfacial friction. The current research is focused on this field.

The author's model of CHF in annular flow based on a simple assumption concerning entrainment and deposition rates ($e \sim g_F$ and $d \sim g_E$) gives a simple analytical solution similar to recent empirical correlations (Silvestri, after [4]). These assumptions are in agreement with most recent results [3, 5, 12, 29].

The correlations established for K and Y [equations (19), (20)] are, presently specific for each medium.

It is hoped that further theoretical and experimental research will result in a more precise identification and determination of these parameters and, possibly, a general relationship valid for different media will be produced.

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