Int. J. Multiphase Flow Vol. 6, pp. 69–95
Pergamon/Elsevier, 1980. Printed in Great Britain

THE BOILING CRISIS IN NUCLEAR REACTOR SAFETY AND PERFORMANCE

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Abstract—A state-of-the art review of Boiling Crisis is presented with particular reference to applications in Nuclear Reactor Safety and Performance. Fundamental understanding and associated needs are emphasized rather than parametric effects and quantitative aspects of empirical methods. Directions for future progress are suggested.

1. INTRODUCTION

The purpose of this paper is to examine the state-of-the art in boiling heat transfer in the pre-critical heat flux region in relation to present and future needs of the Light Water Reactor (LWR) technology. From the point of view of performance as well as safety this region is important primarily with reference to its upper limit. Various descriptive names have been utilized for this limit (or condition) over the years to denote a principle mechanism (i.e. Departure from Nucleate Boiling—DNB, (Film) Dryout—FDO), or with various degrees of "optimism" the drop-off in heat transfer effectiveness found beyond (Burnout—BO, Boiling Crisis—BC, Boiling Transition—BT). The term Boiling Crisis provides adequate reflection of the phenomena in a symptomatic sense, while maintaining sufficient generality as for the mechanisms and will be adopted here. The corresponding flux will therefore be referred to as Critical Heat Flux (CHF).

The conditions of interest here are primarily for Flow Boiling Crisis (FBC) although they may extend all the way to sufficiently low velocities, or even complete stagnation, for short periods particularly relevant to accidents, such that Pool Boiling Crisis (PBC) is important at least as a limiting condition. There are additional reasons for including Pool Boiling Crisis here: (a) its mechanisms and hence our predictive capability are in far better state of development than those of FBC, (b) some mechanistic aspects may be quite relevant to FBC, (c) consideration of "induced" (free convection) liquid and two-phase currents in PB configurations (i.e. Quasi-Flow Boiling Crisis—Q-FBC) as found in many recent studies may help elucidate, at least in part, the role of convection in FBC and (d) certain controversial features of PBC still being the subject of debate, may be pertinent also to FBC.

If the number of publications that continue to appear is an indication, the subject, after more than 200 yr since its definition (Leidenfrost 1756), is still very much alive. More than 30 papers appeared in the last International Heat Transfer Conference (1978). Frequent literature reviews are also being published (Gambill 1968a, 1968b, Hewitt & Hall-Taylor 1970, Tong & Weisman 1970, Collier 1972, Tong 1972, Lawther & Miles 1973, Hewitt 1974, Murinelli 1977, Hsu & Graham 1976, Lahey & Moody 1977). I will take this opportunity to update the most recent surveys presented by Bergles (1977) (subcooled and low quality conditions) and Hewitt (1978) (primarily high quality conditions). My primary objective, however, will be to focus on the applications in LWR technology and in this context to discuss research needs and to speculate on, or suggest, future progress.

The most significant aspect of this application-oriented view is recognizing the importance of the high pressure range of interest. For Pressurized Water Reactor (PWR) operations and transients the pressure is in the neighborhood of 150 bar, while for Boiling Water Reactor (BWR) operation and transients, as well as PWR steam generator operations and loss of coolant accidents for both PWR and BWR the range of interest is 50–70 bar. The liquid-to-vapor density ratios at these two conditions are 6 and 18–27 respectively as compared to a value of 1600 at

1 bar. The latent heat of vaporization increases only by factors of $\times 1.4$ and $\times 2$ as pressure is decreased from 150 to 60 and 1 bar respectively. These order of magnitude changes have important consequences not only on the dynamics of vapor bubble growth (Patel & Theofanous 1976, Theofanous *et al.* 1968a) but also and perhaps more importantly, on the dynamics of the two-phase flow patterns and associated detailed microprocesses. Other important variations on physical and transport properties (i.e. surface tension, viscosity, conductivity, etc.) are also present. Since most detailed information comes from low pressure systems, all the above variations must be kept clearly in mind while examining the applicability of thus inspired mechanistic models (controlling physical processes). On the other hand the disparity of research results observed through variations in geometry, pressure and pressure drop (flow) (Bergles 1975, 1977, Hewitt 1978) must be considered in the perspective of the relatively narrow range of interest here: i.e. pressure (as above), fluid (water) and geometry (vertical rod bundles). Clearly, fundamental understanding and generalization of results over the widest range of conditions is important not only as aiming at ultimate economy of efforts among various fields of applications, but also as means to attaining greater confidence on our prediction methods. Design innovations for extending the CHF limits may thus be possible and economies in design evaluation and data base accumulation may result as successive designs evolve with time (i.e. bundle size, supports, mixing vanes and grids, etc.).

The empirical approach suggested by the narrow ranges of interest is largely followed to date while a fundamental understanding is intensively sought. If not for the reasons mentioned in the above paragraph this is due to a special task taken on by nuclear industry to *demonstrate* safety even under the circumstances of certain postulated (not expected in the lifetime of the plant) severe faults. The double ended break of one of the main reactor recirculation lines (LOCA) represents such a hypothetical fault. Partly due to the vast resources needed for its evaluation and partly due to its potential to evolve into a core melt accident the LOCA has attained international notoriety over the past several years. The Boiling Crisis would occur in a LOCA under rapidly varying flow conditions. Other transients involve sharp (but of short duration) power pulses and still others may involve combinations of the two together with pressure transients. Later on we will see that the currently available tools are crude but more than sufficient for *licensing*. On the other hand, the *desire* to analyze such complex situations *realistically* gives rise to the need for a fundamental description of the Boiling Crisis. The degrees of freedom are essentially infinite and the empirical approaches at the least questionable. Much of the current work on CHF is thus motivated, with reference made to Transient-CHF (T-CHF). Even the first compilation of the literature on T-CHF has already appeared (Leoung 1978).

I have attempted to clarify the use of empirical methods against the needs for fundamentals by organizing the main portion of this paper in terms of Application Aspects (section 3) and Mechanisms (section 4). A technical introduction to these two sections providing some useful classifications and the background for motivating the suggested approach is given in the next section.

2. TRENDS AND CLASSIFICATIONS

Pool boiling crisis background

After nearly three decades of extensive investigations the overwhelming evidence is in support of Kutateladze's view (1951) that the boiling crisis, in saturated pool boiling, is of hydrodynamic origin. Kutateladze's original formulation is based on similarity arguments, concerning the hydrodynamic stability of the counter-current vapor–liquid flow and takes the form:

$$K = \frac{u_{\mathrm{vcr}}\sqrt{\rho_v}}{\{g\sigma(\rho_l - \rho_v)\}^{1/4}}; \quad u_{\mathrm{vcr}} = \frac{q_{\mathrm{cr}}}{h_{l}\rho_v}. \qquad [1]$$

In this equation q_{cr} is the critical heat flux, g is the acceleration of gravity, σ is the surface tension, h_{lv} is the latent heat of vaporization and ρ_l and ρ_v are the liquid and vapor densities respectively. The stability criterion K was determined from data to be a constant value of 0.168. Zuber (1958) developed a theoretical framework for this result and arrived at an absolute prediction of the constant in the range 0.12–0.15. Sun & Lienhard (1970) suggested that for large flat plates the vapor jet diameter would be too large for the Helmholtz instability and that the critical wavelength for Taylor instability should be taken instead. The Zuber constants thus become 0.13–0.17 in good agreement with the original value of K. Further, as was shown by Zuber *et al.* (1962) the details of the instability regimes do not matter; bubble coalescence and interaction of bubble columns, instability and disintegration of liquid streams by upward moving vapor phase and suspension of liquid droplets by upward moving vapor phase, giving essentially the same results. Additional support of the hydrodynamic hypothesis was provided by many studies and notably by those of Lienhard *et al.* (Sun 1970, Ded 1972, Dhir 1973) who considered the effects of geometry (cylinders, spheres) upon the development and configuration of the Taylor jets. In particular with small heaters or reduced body force fields (increased Taylor wavelengths) the discrete number of jets over the heater surface must be taken into account and a larger value of the critical heat flux is predicted and observed. Similar results were obtained by Borishanskii & Fokin (1975) who derived expressions for the bubble departure diameters from cylinders and spheres and related CHF fluxes to these diameters by:

$$\frac{q_{cr}}{q_{cr\infty}} = \left(\frac{d_\infty}{d}\right)^2. \qquad [2]$$

Most recently Griffith *et al.* (Bjornard 1977, Aredisian & Walkush 1975) found that the Boiling Crisis in rod bundles with "slow" two-phase flow (approaching pool boiling) could be simply correlated in terms of the Kutateladze–Zuber formulation in terms of the void fraction, α:

$$\frac{q_{cr2\phi}}{q_{cr}} = 1 - \alpha. \qquad [3]$$

Heater and surface properties are considered unimportant to the Boiling Crisis according to the above. This matter, considered controversial (Hsu & Graham 1976) for a considerable length of time, received considerable clarification just recently. A number of experimental studies, for water and cryogenics, have confirmed (i.e. Guglielmini & Nannei 1976, Grigoriev *et al.* 1978) the importance of the "thermal conductance" $\sqrt{(k_s\rho_s c_s)}$, of the solid for extremely thin heaters. The effect vanishes for heaters thicker than a few tenths of a millimeter (see figure 1). The effect is very important since it suggests the development of sporadic dry-out zones well before the onset of complete breakdown of the counter-current flow. Such sporadic dry-out zones in fact would be expected considering the nature of the hydrodynamics as the crisis is approached and have been "observed" experimentally by Yu & Mesler (1977). It may be concluded therefore that heater thermal capacity, $\delta\rho c$, is important in moderating the temperature rise at the dryout spots, while lateral thermal conduction in the solid helps dissipate such localized "crises" prior to their development into hot spots unless the heater provides an inadequate conduction path (i.e. thin in relation to the size of the dry spots). The latter process is facilitated with increasing $\sqrt{(k\rho c)}$ as indicated by the trends of figure 1. More detailed analysis must be made, however, considering the two-dimensional transient heater response as well as possibly the properties of the coolant, i.e. much stronger reductions in q_{cr}, were noted for helium (only a few data points reported) (Grigoriev *et al.* 1978) compared to those shown in figure 1. Clearly microlayer response (Theofanous *et al.* 1978, Von Stralen & Zijl 1978) including capillary effects (Wayner 1976) may have to be included in such evaluations. Further, the mechanism of local quenchings (by incoming liquid), which is also affected by the thermal

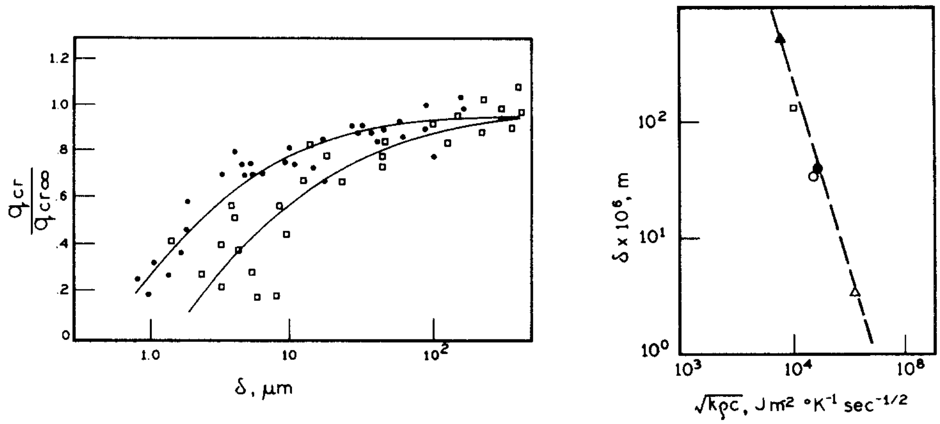

Figure 1(a). Normalized burnout heat flux for different thicknesses of zinc and tin heaters in pool boiling of water. ● Zinc, $q_{cr\infty} = 150 \times 10^4$ Wm^{-2}, □ tin, $q_{cr\infty} = 150 \times 10^4$ Wm^{-2} (Guglielmini & Nannei 1976).

Figure 1(b). Variation of heater thickness, below which significant heater-thickness effect on q_{cr} is observed, with "thermal conductance." □ Tin, ● zinc, O nickel, △ copper, ▲ stainless steel (Guglielmini & Nannei 1976).

conductance of both the liquid and the solid, but in direction opposite from that indicated in figure 1, must be examined as potentially participating in the above described mechanism of local dry-out development into a boiling crisis. Along the same lines could also be viewed sporadically published information on the effects of solid–liquid wettability (i.e. Hahne & Dissenlhorst 1978). Engineering surfaces are however usually covered with oxide layers that promote wetting and the question is of interest primarily in establishing completeness in Boiling Crisis regime possibilities. However, due to the small thickness of the fuel rod cladding, additional investigations into the hot spot development mechanism discussed above would seem warranted, although I would expect its significance to diminish for the pressure range of interest.

Significant new developments of the purely hydrodynamic approach were just announced by Kutateladze & Malenkov (1978). Based on dimensional considerations the compressibility of the gaseous phase was introduced to scale the velocity of propagation of capillary waves, in the form of a "Mach" number:

$$M_*^2 = \frac{\rho_v}{P}\left\{\frac{g\sigma}{\rho_l - \rho_v}\right\}^{1/2} \qquad [4]$$

where P is the system pressure. The stability criterion K (sometimes referred to as the Kutateladze "constant") was shown to strongly depend on M_* both for bubbling of various

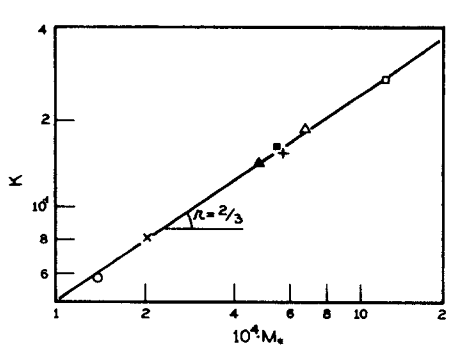

Figure 2. Dependence of K on M_* at bubbling of water by: O air, × helium, + nitrogen, △ argon, □ xenon; and of ethanol by: ▲ nitrogen, ■ argon (Kutateladze & Malenkov 1978).

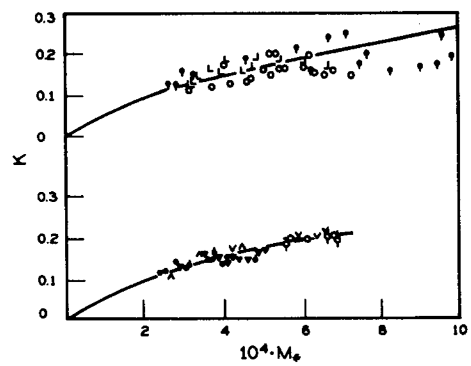

Figure 3. Dependence of K on M_* at boiling (P, bar): helium (0.06 ÷ 2.52), ∠ hydrogen (0.96 ÷ 12), argon (0.07 ÷ 45.7), ○ nitrogen (1.09 ÷ 32.4), ● water (0.2 ÷ 190), ▲ ethanol (1.0 ÷ 60.0), ϕ benzene (1.0 ÷ 47), -○- heptane (1.0 ÷ 40.5), v pentane (1.0 ÷ 31), ∧ propane (21 ÷ 34), methanol (1.0 ÷ 63) (Kutateladze & Malenkov 1978).

gases through water (figure 2), as well as for boiling of various organic and cryogenic liquids as well as water (figure 3). Thus for nonviscous liquids we have:

$$K = 3M_*^{2/3}. \qquad [5]$$

Nearly 30% reduction from the classical value of K (at 1 bar) is thus predicted for water at the high pressure conditions of interest to us. Viscosity effects were also generalized by the ratio of buoyancy to viscous forces (Archimede's number), as shown in figure 4. Finally important generalization of nucleate boiling data was achieved in terms of the "Mach" and Peclet numbers:

$$\mathrm{Nu}_* \sim \left(\frac{M_*^2}{\mathrm{Pe}_*}\right)^n \quad 0.6 < n < 0.8. \qquad [6]$$

The remarkably simple results of Tolubinskiy (1974, 1976) for boiling and critical heat flux should also be mentioned here. He defines a Nusselt number based on the capillary length also, but he introduces a characteristic vapor velocity defined in terms of bubble departure diameter and frequency, $u^* = Df$. The thermal and hydraulic properties of the liquid are now explicitly taken into account by introducing the Prandtl (Pr) and Fourier (Fo_*) numbers.

$$\text{For boiling: } \mathrm{Nu}_* = 75\, \mathrm{R}_*^{0.7}\, \mathrm{Pr}^{-0.2};\ \mathrm{R}_* = \frac{q}{h_{lv}\rho_v u^*}\ , \qquad [7]$$

$$\text{For crisis: } \mathrm{R}_* = 7\left\{\mathrm{Fo}_* \frac{\rho_l}{\rho_v}\right\}^{1/2} \quad \mathrm{Fo}_* = \frac{\alpha_l}{D^2 f}\ , \qquad [8]$$

$$\text{which yields: } q_{cr} = 7\, h_{lv}\, \{\alpha_l f \rho_l \rho_v\}^{1/2}\ . \qquad [9]$$

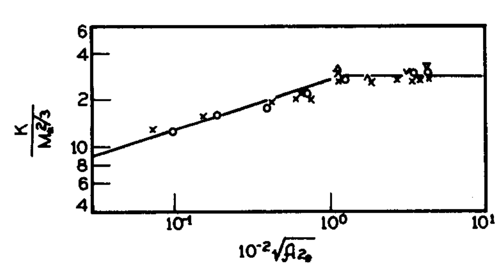

Figure 4. Dependence of K on bubbling viscosity. Water–glycerin mixtures: × nitrogen, ○ helium; water: v xenon, ∇ argon, □ nitrogen; ethanol: △ argon, ∧ nitrogen (Kutateladze & Malenkov 1978).

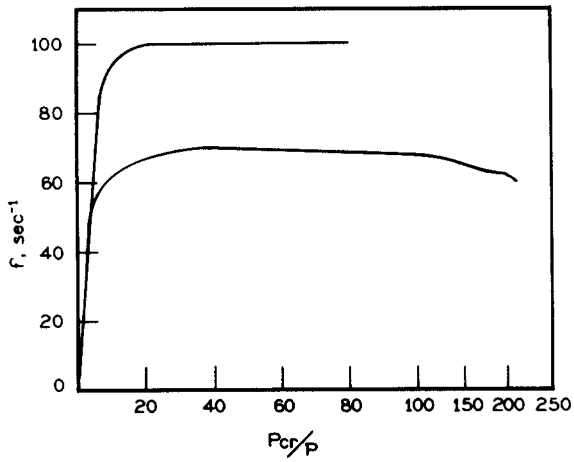

Figure 5. Mean bubbling frequency as a function of reduced pressure. (1) Water, (2) other liquids except liquid metals (Tolubinskiy *et al.* 1976).

Empirical, but "universal" relations were given for u^* and f in terms of the reduced pressure (figure 5) and excellent agreement was reported for wide range of conditions and variety of liquids (water, hydrocarbons, cryogenics). For liquid metals actual values of f and/or surface temperature fluctuation frequencies gave good agreement with data.

Quasi-flow boiling crisis background

Even weak convection currents may considerably affect the Boiling Crisis by altering the characteristics of the two-phase environment in the neighborhood of the heating surface. Many examples of this effect may be found in the literature. I choose to discuss two recent and particularly interesting ones.

Tribault & Hoffman (1978) utilized a newly developed heat flux meter to directly measure the complete boiling curve, during quenching of large copper cylinders (13 cm in diameter) in water, for various positions around the circumference. Although the Kutateladze–Zuber pre-

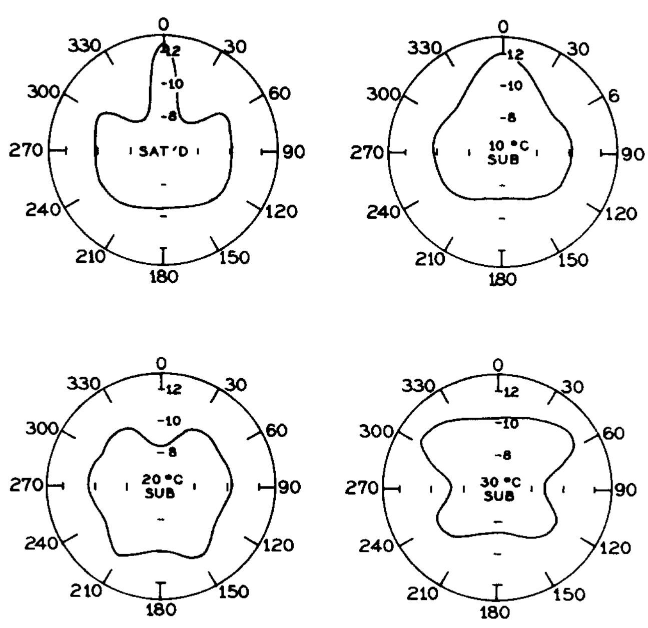

Figure 6. Variation of local CHF around a cylinder for different subcoolings. (Tribault & Hoffman 1978).

dictions were found in excellent agreement with CHF values averaged over the whole cylinder surface, local variations of more than 50% were consistently measured (figure 6). The indicated change in local variation pattern with subcooling strongly suggests the influence of rising voids in promoting CHF at higher locations.

Hahne & Disselhorst (1978) measured significant increases of CHF (1–7 mm wires, water) with void-induced liquid convective velocity. Even more interestingly, they found that the frequency distribution of random velocity fluctuations in free convection flow indicated a similar boiling crisis frequency as a consequence.

The two-phase structure may be even more drastically affected in the presence of forced convection. The new degrees of freedom thus created are discussed next.

Flow boiling crisis trends

For flow boiling the forces that control the liquid approach to the vapor emitting channel wall are not clearly understood and for power transients additional complexities are added. As a consequence a comprehensive representation of the huge amount of experimental data is still lacking and the empirical approach is largely followed. This subsection, therefore, has primarily an introductory purpose. Two regimes corresponding to the two extreme quality ranges have been identified. The negative (subcooled) and low quality crisis represents a breakdown of nucleate boiling, somewhat akin to that considered above and will be referred to as DNB. The high quality crisis represents the breakdown (or depletion) of the liquid film (annular flow pattern) wetting the heater and will be referred to as FDO. For highly subcooled conditions DNB depends only on local conditions. With increasing quality void convection (downstream along the heater surface) increases and so does the influence of upstream flux distribution (history effects). For large qualities FDO depends on droplet entrainment and deposition and since both of these processes have long relaxation lengths (times) strong history effects would be expected. Important differences exist in the consequences of DNB and FDO as may be explained with the help of figures 7–9 (Bjornard & Griffith 1977). The pre-CHF boiling heat transfer in these figures is based on Chen's correlation (1963), and for CHF the Biasis' correlation was used (Biasi *et al.* 1967). As seen in figure 7 occurrence of DNB at full flow conditions entails rapid overheating, but undiminished cooling is maintained beyond FDO. This is not the case, however, for reduced flow (figure 8). The effect of pressure is rather insignificant in the occurrence and consequences of DNB at low flows as seen in figure 9. For a detailed description of the important experimental trends reference is made to Collier (1972) and Hewitt (1978).

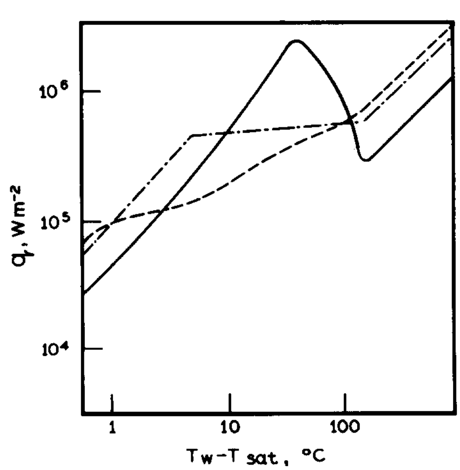

Figure 7. Effect of quality on heat transfer in the high flow region. $D_e = 0.0127$ m, $P = 69$ bar, $G = 2.7 \times 10^3$ kg m^{-2} s^{-1}, *ss* oxide surface. —— $x = 0.1$, –.–.–. $x = 0.5$, ---- $x = 0.9$ (Bjornard & Griffith 1977).

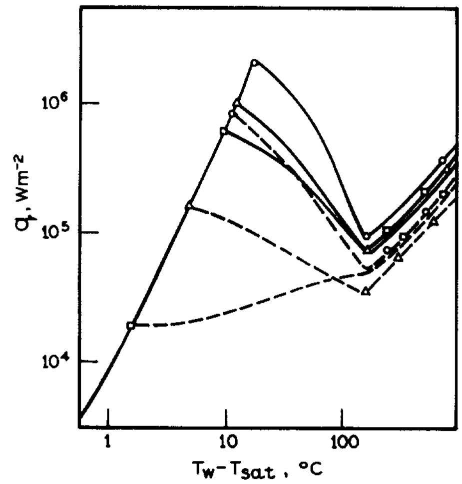

Figure 8. Effect of flow direction on heat transfer in low flow region. $D_e = 0.0127$ m, $P = 69$ bar, $G = 27$ kg m^{-2} sec^{-1}, *ss* oxide surface. ——— upflow, ---- downflow, ○ $x = 0.1$, △ $x = 0.5$, □ $x = 0.9$ (Bjornard & Griffith 1977).

The task of elucidating the detailed FBC regimes is made doubly difficult by the lack of detailed flow regime data at the immediate vicinity of the crisis. Thus, it is difficult to correctly isolate the origin of deficiencies in particular mechanistic models and resorting to empiricism produces a variety of different models with appropriately adjusted parameters. Before any more detailed considerations on this subject, however, let us attempt an ordering of the problem areas.

Classification

A classification in terms of successive "spheres" of influence would appear worthwhile. With reference to a reactor core or stream generator bundle and in view of the significance of flow regimes as discussed above we should identify Global-, Local- and Micro-Hydrodynamics. Global refers to core (bundle)-wide flow and phase distribution. Local refers to a small axial segment of a subchannel and Micro refers to the immediate vicinity of the heater surface with absolute scale characteristic of the two-phase structure (i.e. microlayers, drops, bubbles) ((see figure 10). Clearly, the three are intimately coupled, however, the separation is instructive as a development tool.

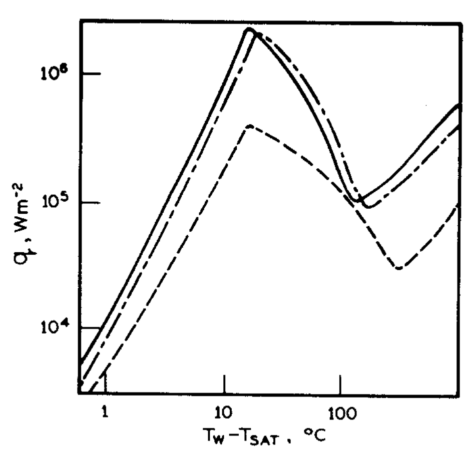

Figure 9. Effect of pressure on heat transfer in low flow region. $D_e = 0.0127$ m, $G = 27$ kg m^{-1} s^{-1}, $x = 0.1$ *ss* oxide surface. ---- 6.9 bar, -.-.-. 69 bar, —— 110 bar (Bjornard & Griffith 1977).

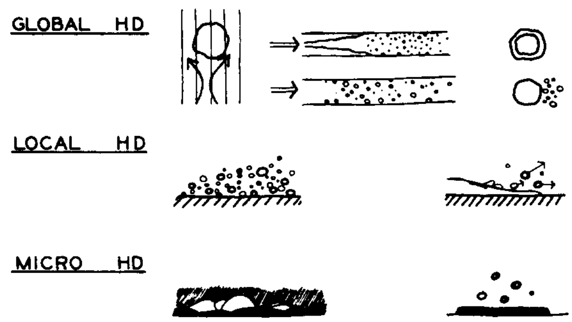

Figure 10. A schematic representation of the Micro-, Local- and Global-Hydrodynamic regions.

The global approach can help evaluate such effects as local hydraulic diameter changes (i.e. rod bow), power distribution effects and hydrodynamically unstable situations. One such example (Theofanous 1978b) points to the significance of interconnected parallel channel effects in providing temporary stability during the two-dimensional boiling of sodium in a rod bundle (figure 11). Although such global calculations are presently utilized in the industry, significant uncertainties remain in correctly prescribing the lateral void distribution behavior. The local hydrodynamics on the other hand in concert with local phase changes "*drive*" the micro-hydrodynamics which are ultimately responsible for the onset of crisis and even further removed from direct experimental observation. It would appear fruitful therefore, to carefully establish the complete range of causual relationships between local and micro-hydrodynamics and thus the mechanism(s) of crisis. Having established with confidence the crisis mechanism the general problem of prediction (including arbitrary transients) reduces to one of prediction of local fluid conditions and local flow regime development from imposed boundary conditions and global hydrodynamics.

This separation might appear somewhat contrived especially in view of what has already been said for the two extremes of the quality range, i.e. FDO being exclusively a history effect and DNB involving only micro-hydrodynamics. As we will see shortly these views are still intensely debated even for steady-state conditions. For transients and a significant portion at the (intermediate) quality range, relative significance of the two effects is even less substantiated.

A comparatively elementary example of this step-wise approach may be found in the field of heat and mass transfer (gas absorption, vapor condensation, etc.) at free turbulent gas (vapor)/liquid interfaces. Such interfaces are found in a wide variety of flow situations including open channel flows, bubbly and stratified channel flows, liquid jet injection in gas (vapor) spaces, falling film flows, etc. A wide variety of correlations have been developed for each case. Based on the initial ideas of Fortesque & Pearson (1967) and Scott *et al.* (Lamont 1966,

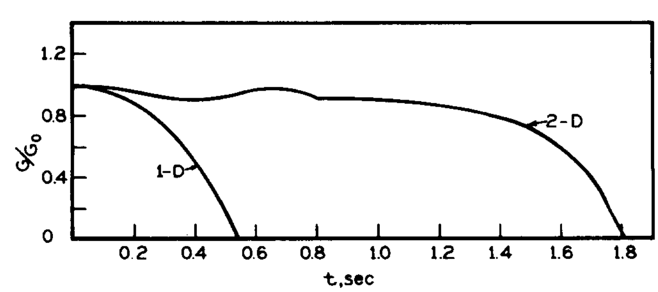

Figure 11. Inlet flow transients for one-dimensional (1-D) and two-dimensional (2-D) boiling in rod bundles (Theofanous 1978b).

Banerjee & Rhodes 1968), bypassing the micro-hydrodynamics (see figure 12), we were able to demonstrate the usefulness of the two-step approach in unifying all the available mass transfer data (Theofanous *et al.* 1976b, Brumfield *et al.* 1975). Since in all but one case the *local* turbulence characteristics (intensity and macroscale) were not measured standard calculational techniques were utilized for prediction (for falling films the actual dual wave/substrate structure determined experimentally by Dukler *et al.* (Telles 1970, Chu 1974) was taken into account). These in-turn were related to the local transfer rates (mass transfer coefficient k_L) by:

$$k_l = 0.7F(\tau_e)\left\{\frac{\Delta u'}{L_u}\right\}^{1/2} \text{ for } \mathrm{Re}_t < 500 \quad , \qquad [10]$$

$$k_L = 0.25\sqrt{\Delta}\left\{\frac{u'^3}{\nu L_u}\right\}^{1/4} \text{ for } \mathrm{Re}_t > 500 \quad , \qquad [11]$$

where

$$\tau_e = t_{\exp}\frac{u'}{L_u} \text{ and } \mathrm{Re}_t = \frac{u' L_u}{\nu} \quad , \qquad [12]$$

and u' and L_u are the turbulence intensity and integral length scale respectively. Δ is the molecular diffusivity and $t_{\exp}$ is the interface exposure time to mass transfer. $F(\tau_e)$ is a factor accounting for the transient boundary layer development. Just recently Bankoff (1979) showed the applicability of the heat transfer analogues of these equations above

$$\mathrm{Nu}_t = 0.25\,\mathrm{Re}_t^{3/4}\,\mathrm{Pr}^{1/2} \qquad \text{for } \mathrm{Re}_t > 500 \quad , \qquad [13]$$

$$\mathrm{Nu}_t = 0.7F(\tau_e)\,\mathrm{Re}_t^{1/2}\mathrm{Pr}^{1/2} \quad \text{for } \mathrm{Re}_t < 500 \quad , \qquad [14]$$

where

$$\mathrm{Nu}_t = \frac{hL_u}{k} \qquad [15]$$

to a number of different condensing flows (stratified channel flow and bubble collapse). Furthermore, Thomas (1979) extended these results and showed applicability for condensation in stratified channel and jet flows. The conclusion is that in this manner one can build a further reaching understanding of the transport mechanisms, while although the prediction of local turbulence from global considerations is admittedly crude, it has been shown adequate for prediction purposes.

Extending these ideas to the case at hand we can see that in general an adequate description of the local hydrodynamics, must involve in addition to time average velocity and phase distributions, the characterization of their fluctuating components (i.e. root mean square

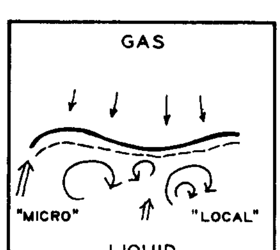

Figure 12. Illustration of Micro- and Local-Hydrodynamic regions for interface mass transfer.

values—R.M.S., and correlation lengths—macroscales) as well. Clearly this local information is also needed to establish the dynamics of flow regime development. This may appear as a formidable task, especially for boiling systems, but any amount of additional understanding may be particularly rewarding (Lahey & Dreu 1978, Sullivan *et al.* 1978). For the time being crude regime classification can be made in terms of the low and high quality ranges. Further elaboration of the above ideas is made specific to each regime in section 4. Finally, individual consideration is given to mechanisms specifically related to fast transients. Before getting to the mechanisms certain more immediately practical aspects should be discussed.

3. APPLICATION ASPECTS

Normal operation and "slow" transients

Since boiling crisis *may* lead to clad overheating and failure it is to be avoided during normal reactor maneuvers as well as all anticipated (within the plant lifetime) flow and/or power transients. The concern is of economic importance rather than public safety at large, since such failures even if they occurred they would be localized, but the resulting contamination (release of fission products) could significantly affect operations. Excellent discussions of design procedures and tradeoffs have been presented by Tong & Weisman (1970) and Lahey & Moody (1977) for PWRs and BWRs respectively.

The reactor vendors have taken advantage of modern instrumentation and data acquisition and analysis techniques to build an impressive (but unfortunately proprietary) data base for full scale rod bundles and prototypic thermal-hydraulic and thermodynamic conditions. The bundle simulates all external geometric effects including rod supports and flow mixing grids and vanes and imposed perturbations (i.e. rod touching, unheated rods, etc.) as well as axial and radial power distributions. For internally heated rods the power is shaped by heater wire coiling, while for directly heated claddling the wall thickness variation provides this control. Stainless steel is commonly utilized as the cladding material in these experiments. Notice that the "thermal conductance" of the Zr–Al cladding is nearly by 70% lower than that of stainless steel and with reference to figure 1 we should expect thin wall effects for cladding thicknesses well over its normally used values. It would seem important therefore to determine the applicability of this mechanism as well as its quantitative aspects (i.e. extent of thin wall effect for steel cladding utilized in the simulations) for the pressure range and flow conditions of interest.

For PWRs the low quality range ($<5\%$) is relevant and DNB has been described by empirical equations based on the "local" nature of the phenomenon:

$$q_{cr} = f(P, x, G, d) \quad . \qquad [16]$$

Together with additional corrections for inlet subcooling, history effects (Tong's F factor (1966)), good representation of the experimental data has been achieved.

For BWRs the higher quality range is of interest and FDO has been described by empirical equations that account for flow history effects, i.e. critical quality, x_{cr}, related to flow boiling length Z_B:

$$x_{cr} = f(P, G, d_t, Z_B) \quad . \qquad [17]$$

Together with additional empirical correlations for actual heated length and local power distribution good representation of experimental data has been achieved.

These correlations when applied to the data bases directly relevant to the intended application including subchannel analyses typically prove good to within a few per cent (standard deviation). The economic investments in achieving such extreme accuracy are now paying dividends as more sophisticated design approaches are being implemented. On the other hand some of the ramifications of coupling various empirical correlations (each originating on a different data base) with subchannel analysis for the prediction of DNB of still another data set may be deduced from a report by Cuta & Wheeler (1975). Finally a good feel for the state-of-the art in this field and the stringency of licensing requirements, may be summarized from a number of available regulatory review reports (e.g. Stello, 1974).

Application of these design equations to flow and power transients (pump seizure, reactivity insertions, loss of load or turbine trip, etc.) is done in conjunction with a calculation (e.g. Tong & Weisman 1970, Lahey & Moody 1977) of local fluid and flow conditions (taking into account neutronic feedback effects). This quasi-static assumption is producing adequately conservative results. However, the data base is not extensive and lacking data on these local conditions it is hard to gain good insight from such comparisons.

The study of Boiling Crisis and post CHF fuel rod behavior under well instrumented in-pile (prototypic in many essential aspects) experiments is presently carried out at INELs Power Burst Facility (PBF). As an example let us consider briefly the results from a recent test program with isolated (single rod shrouds provided) 1 m long Zr–Al cladded fuel rods. Nominal PWR conditions were used and DNB was achieved by flow reductions at 0.5–3% per sec. The upper bound represents typical flow reduction rates if all electric power to the primary coolant pumps of a PWR is lost. The results indicate that for such moderately fast flow reductions DNB is not affected by imposed rate. The Westinghouse W-3 correlation (Martinson & McCardell 1977) predicted adequately the conditions for the onset of DNB, for three of the rods tested. For the fourth one the calculated flow rate was higher than the measured one by nearly 30% (i.e. DNB was calculated around 30 sec earlier). It was also noted that this resistance to DNB was also observed in previous flow reduction cycles during which the fourth rod did not enter DNB although its coolant mass flux was lower compared to the other three rods undergoing DNB. Such sudden (but beneficial) changes in behavior with aging (more than a factor of two in flow at DNB) were also found in earlier PBF tests (Larson *et al.* 1976, Martinson *et al.* 1976) and are not well understood to date. A rather complete data base including short period power burst is expected over the next few years.

Even for well defined steady-state conditions, however, there is room for fundamental insight. A currently important example is found in connection with unfavorable experience with PWR steam generator tube failures. Growing evidence that thermal-cycling-induced fatigue may play a role prompted interest in elucidating local wet–dry transitions on steam generator tubes and tube-support clearances. Vorob'yev *et al.* (1973) studied temperature fluctuations in the 2.5–7 cm FDO zone of a steam generator tube at 140 bar. The r.m.s. values increased with heat flux in the 7–24°C range and main frequency content was found at 0.3 Hz. Similar results were found for liquid sodium heated tubes at ANL (France *et al.* 1976). Chu *et al.* (1978) utilized these results and assuming an oscillatory rivulet pattern they calculated the resulting thermo-elastic behavior suggesting design (ASME code) procedures. Maskalenko & Kharionorskii (1977) assumed special fatigue models together with a stochastic treatment of the thermo-elastic field (driven by the randomly imposed temperature oscillations) to obtain failure rates of the steam generator tubes. A detailed evaluation of the rivulet model was given by Gardner & Kubie (1976) who carried out some interesting simulation tests utilizing two slightly miscible liquids to approximate the liquid–vapor density ratio at the elevated steam generator pressures. Hinoki *et al.* (1977) examined global, as well as local conditions within steam-generator bundles and concluded that dry–wet phenomena occur only in the narrow region just under the antivibration bars. They concluded that in case of phosphate treatment this wet–dry condition causes local deposits and steam generator surface attack (thinning). Their results appear to correctly correlate with actual plant experience and they recommended a tube support structure less susceptible to this problem.

Accidents and "fast" transients

The large break LOCA adequately epitomizes the Boiling Crisis considerations under conditions considerably more variable than those considered just above. Such a severe accident is postulated in order to demonstrate efficacy of the Emergency Core Cooling System (ECCS) in preventing overheating of the cladding beyond a safety limit set at 1200°C. One of the early descriptions of the accident and analytical tools used in its evaluation was presented by Ybarronto *et al.* (1972). A most recent overview was presented by Tong (1978). The current state of the act has been presented in considerable detail in a collection of articles (Jones & Bankoff 1977).

Typical pressure and flow transients for a BWR (given a LOCA) are shown in figures 13 and 14. The Blowdown Heat Transfer Program (BDHT) at General Electric (General Electric, 1976) simulating the blowdown characteristics (figures 13 and 14) in full scale electrically heated bundles, demonstrated the applicability of GEs steady-state, $x_{cr} - L_B$ type correlation (on the basis of calculated local conditions). The average power bundles are expected to remain well cooled for much of the blowdown period and until depletion of their water inventory. The peak power bundles suffer Boiling Crisis within the first 1–2 sec in the transient. Redistribution of stored energy following the breakdown in cooling effectiveness produces rapid cladding temperature rise as illustrated in figure 15. This early crisis, however, is believed to be due to special facility characteristics and atypical of BWR behavior, i.e. good blowdown heat transfer for the whole BWR core until core flow stagnation or bundle water inventory depletion is predicted.

Due to initially highly subcooled state and loop-type configuration, LOCA-induced transients are considerably more severe in the PWR case. Typical core pressure and inlet flow transients, for a vessel inlet break, are shown in figure 16 and 17. The essentially instantaneous flow reversal was for licensing "conservatively" interpreted as signaling the onset of DNB. The rapid cladding heating expected from stored heat redistribution is shown in figure 18. Many speculated, however, that in reality significant delays and thus considerable additional cooling (~30°C reduction in peak clad temperature for each second of cooling) would be expected. This did not materialize for the high power locations, however. A simple, but powerful, argument was put forth by Griffith *et al.* (Smith & Price 1976, Pearson & Lepkowski 1977) to demonstrate that clad heatup would ensue within 1–2 sec not due to flow reversal but due to local core voiding (development of a flow stagnation zone within the core). Similar dryout times were also observed experimentally in the Semiscale core. A similar interpretation for these data was also provided by Snider (1977) with the help of COBRA calculations utilizing the measured fluid conditions at the core boundaries. Finally similar behavior is indicated in the results just obtained from the first nuclear (low power) *L*2-2 LOFT test (Batt 1979). Among the seven well

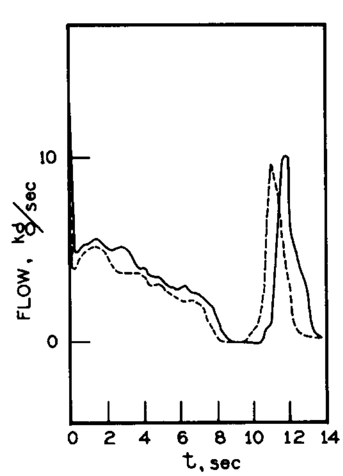

Figure 13. Illustration of BWR inlet core flow transient following LOCA. —— 4.5 MW, – – – – 6.1 MW bundle power (Jones & Bankoff 1977).

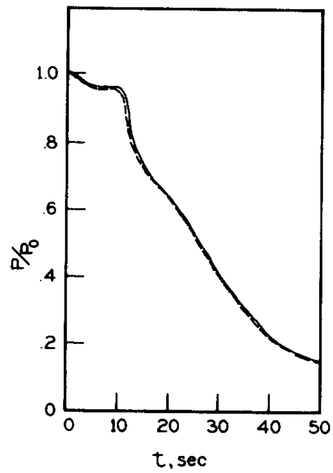

Figure 14. Illustration of BWR depressurization transient following a LOCA. — 4.5 MW, ——— 6.1 MW bundle power (Jones & Bankoff 1977).

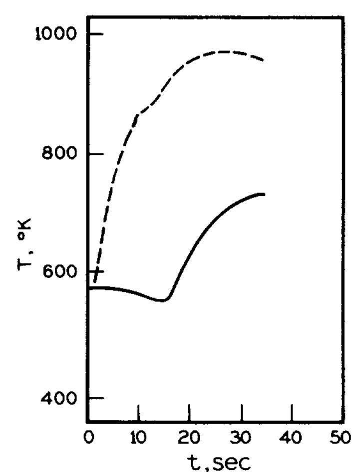

Figure 15. Illustration of BWR clad temperature excursions due to Boiling Crisis during LOCA. — 4.5 MW, ——— 6.1 MW bundle power (Jones & Bankoff 1977).

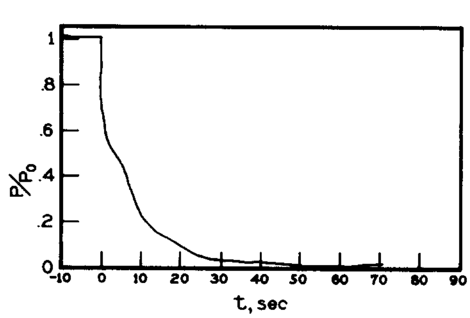

Figure 16. Illustration of PWR depressurization following a large LOCA (Jones & Bankoff 1977).

known correlations tried by Snider only GEs correlation, with a strong quality dependence

$$q_{cr} = 3.15 \times 10^6(0.84 - x)\ \mathrm{w/m^2}\ \text{for}\ G < 0.5 \times 10^6 1\ \mathrm{bm/hr\text{-}ft^2}\quad , \qquad [18]$$

$$q_{cr} = 3.15 \times 10^6(0.8 - x)\ \mathrm{w/m^2}\ \text{for}\ G > 0.5 \times 10^5\ \mathrm{lbm/hr\text{-}ft^2}\quad , \qquad [19]$$

was found to reflect the observations. Also the widely utilized Biasi, MacBeth and B&W-2 correlations performed acceptably well according to Snider. It should be noted, however, that all these correlations predict dryout in the top third of the core while none is observed experimentally. In contrast to these interpretations Henry & Leung (1977) proposed a mechanism based on the unavailability of nucleation sites for boiling and sudden attainment of spontaneous nucleation conditions at the clad coolant interface (which becomes thus blanketed). This mechanism predicts blanketing, correctly, only for the lower two thirds of the core. Furthermore a special test initiated from 121 bar instead of the usual 156 bar was correctly predicted to escape such early vapor blanketing. Further discussion of this mechanism will continue in the next section.

Little doubt remains that the high power regions of PWRs will suffer a Boiling Crisis within the first 1–2 sec of a large LOCA. The realistic prediction of the onset of such events for the remaining portions of the core remains very much in doubt. This inadequacy is unimportant for conservative licensing calculations but it may prove crucial in determining the success of currently underway large-scale assessment efforts for the next generation codes. Accurate prediction of core-wide temperature distributions and thus Dryout and Rewet sequence, is an essential prerequisite for *realistic* description of the reflood process. This already difficult task is further aggravated by including factors that complicate the overall hydraulics, such as phase separation and nonequilibrium effects found in intermediate size breaks and/or ECCS interactions (i.e. Upper Head Injection, Core Spray Systems, etc.). Also, as additional portions of the core enter CHF at later times, as the pressure drops further and the void fraction of the system changes, new degrees of freedom are potentially added (i.e. change in mechanisms controlling CHF) as may be deduced from the next section. Finally it should be emphasized that the calculation of PWR core flows (and qualities) during the first 15–20 sec (see figure 17) following a large LOCA is inherently one of the most uncertain aspects of the overall calculation of the system hydraulics. This is not only due to uncertainties in modeling the two-phase fluid dynamics of the system in this highly transient (oscillatory) mode in a strongly two-dimensional (thermally and hydraulically) region, although the presently under development "advanced codes" have already demonstrated significant improvements in this area. Perhaps more limiting is the sensitivity of this aspect of the calculation to the flow details ("boundary conditions") at

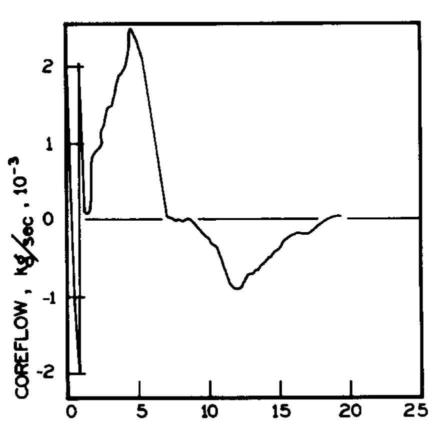

Figure 17. Illustration of PWR core flow transient following a LOCA (Jones & Bankoff 1977).

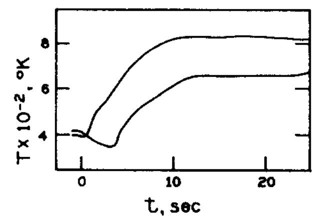

Figure 18. Illustration of PWR clad temperature excursion due to Boiling Crisis during a LOCA (Jones & Bankoff 1977).

the *two ends of the break* that "drive" the whole calculation. This sensitivity may easily be seen by the fact that an "appropriate" break may *always* be selected such that flow stagnation, somewhere in the core, results early in the transient. For the remainder of the calculation the core flows depend on the time wise variations of *small differences* in *large pressures* at the two ends of the core. Considering in addition our complete ignorance of *how* a large, roughly one meter in diameter, recirculation line *might fail in actuality* and remaining uncertainties in critical flow calculations (originating both in modeling break behavior as well as the feedback from the whole calculation, i.e. break flow strongly depends on fluid properties *calculated to reach the break*) we can conclude that a *realistic* treatment of a whole-core temperature transient for a *worst-type but physically realizable break* is incompletely defined. It is these considerations that should provide an appropriate perspective against the above mentioned task and the detailed mechanistic aspects to be discussed next.

4. MECHANISMS

Direct transitions

The occurrence of direct transition from single phase free convection to film boiling has been known for some time. Skripov *et al.* (1965a, 1965b, 1967) studied this phenomenon by pulse-heated samples and demonstrated its relation to achieving the spontaneous nucleation limit at the coolant-heater interface. Heating times were just too short in these experiments to allow bubble nucleation and growth from existing sites. Such behavior is possible in the absence (deactivation) of nucleation sites as discussed recently by Henry & Leung (1977), in connection with the semi-scale core. It was suggested that cladding surface would become deactivated (down to 40–60Å) in the lower part of the core during steady-state operation (the upper portion is in subcooled nucleate boiling). During depressurization and flow reversal additional coolant-cladding heating by a few tens of degrees is sufficient to produce spontaneous nucleation and vapor blanketing (film boiling) in this region. They provided additional verification of this mechanism by inducing flow reversal transients in a heated tube of Freon-11.

Somewhat related "direct" transitions have been proposed by Koralev & Rbychinskaja (1978) and Arksentyuk & Kutateladze (1977) to explain critical heat flux data obtained at conditions of scarcity of nucleation centers (i.e. low pressure alcohols, liquid metals). These models, however, are based on sporadic nucleation and growth of vapor bubbles within the highly superheated boundary layer and are thus on the opposite extreme of the true direct transition mechanism discussed above. Instead development of hot spots under such bubbles and considerations similar to those presented in connection with the surface "thermal conductance" effects (section 2) would appear more appropriate here.

"Direct" transitions were also mentioned by Tolubinskiy *et al.* (1976) who pulse-heated metal wires immersed in various liquids. The measured unsteady critical heat fluxes occurred usually within 200–400 μsec and were substantially lower than steady-state ones for ethyl alcohol and acetone, with the difference diminishing with pressure rise. No such reductions were noted for water. Results were explained in terms of free convection currents which are supposed to be more intense in the lower viscosity water. Thus the lower thermal conductivity organics should heat up more rapidly at the interface yielding an effective vapor blanket upon bubble nucleation. These results are interesting in that they represent a rather unique case where a transient is more limiting than a steady-state and a good illustration of the importance of phase distribution dynamics in determining Boiling Crisis. However, the suggested explanation is rather simplistic. Instead, mechanisms like those described in the previous paragraph would appear more likely and certainly consistent with the good wetting properties of organics at low pressures, the water being notoriously "bad" in that respect.

High quality region

With reference to figure 19 (Subbotin *et al.* 1978) the high quality region, defined as that corresponding to the annular-disperse flow regime, is seen to extend beyond $x = 0.15$ for the pressure range of interest. This boundary would be slightly higher for an order of magnitude reduction in mass flow rate (Hosler 1967). It is widely accepted that FDO is the governing mechanism for Crisis in this region. A major portion of the fundamental work was performed at Harwell, U.K., by Hewitt *et al.* over the past few years. The work culminated with a relatively well defined fundamentally based calculational procedure which already has received quite diverse testing with accurate results (Whalley *et al.* 1978). The model (Whalley *et al.* 1974) is based totally on hydrodynamic history effects with droplet entrainment and deposition supplied through empirical correlations on the basis of local flow conditions throughout the boiling zone. The effects of local boiling, beyond being an additional source for liquid film depletion by vaporization, are ignored. Although rather broad scatter in the entrainment correlation may be noted and interesting objections as to the completeness of such an approach have been reported, particularly by Russian authors, the model appears to provide a good example of the unifying powers of fundamentally-based approaches even when all details are not completely known.

The starting point of the Harwell model is a mass balance on the liquid film taking into account depletion by droplet entrainment (E) and vaporization, and replenishment by droplet deposition (D) and has been utilized many times before (e.g. Biasi *et al.* 1969, Isbin *et al.* 1961):

$$\frac{dG_{lf}}{dz} = \frac{4}{d}\left(D - E - \frac{q}{h_{ev}}\right). \qquad [20]$$

The success this time is due to the fundamentally based description of the entrainment and deposition processes. Entrainment (equilibrium drop concentration) is assumed directly dependent upon the interfacial shear stress, τ_i (i.e. local pressure gradient) and it is scaled by the

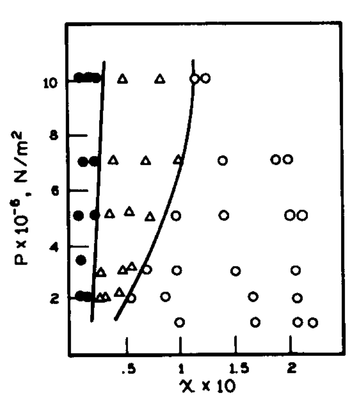

Figure 19. Flow pattern diagram for 1000 kg/m^2 s pipe flow: ● bubble, △ slug, ○ annular-dispersed (Subbotin *et al.* 1978).

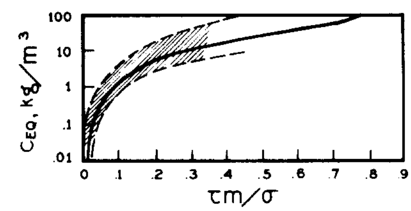

Figure 20. Variation of equilibrium concentration of entrained droplets with $\tau m/\sigma$. Data range is also indicated (Whalley *et al.* 1978).

surface tension force, σ/m, which provides a force opposing this process. The resulting correlation was originally based on entrainment data at low pressure, two-component (adiabatic), air–water and air–alcohol systems. More recently agreement with steam–water data at 70 bar was reported (Whalley *et al.* 1978) as shown in figure 20. Thus entrainment rate is taken proportional to the equilibrium droplet concentration and the deposition rate proportional to the actual concentration. The constant of proportionality (mass transfer coefficient), assumed the same, was correlated with surface tension.

A detailed experimental study of the core region in diabatic upward flow of Freon-11 at 3 bar (corresponding to liquid-to-vapor density ratio of water at the pressure range of interest) was recently reported by Ueda *et al.* 1978). They found that the mass transfer coefficient is significantly affected by droplet concentration and correlated this variation from the theoretically predicted (Namie & Ueda 1972, 1973) value of dilute dispersions in terms of the droplet mass flux. Based on these results excellent predictions of dryout location for a wide range of inlet flow conditions were reported. Although some turbulence work in such dispersed flows is presently under investigation in a number of laboratories for low pressure two-component dilute systems additional experiments at the range of conditions here would seem worthwhile. Significant analytical steps would be required to reduce this experimental work to the understanding necessary to express generically intensities and turbulence scales and hence deposition mass fluxes.

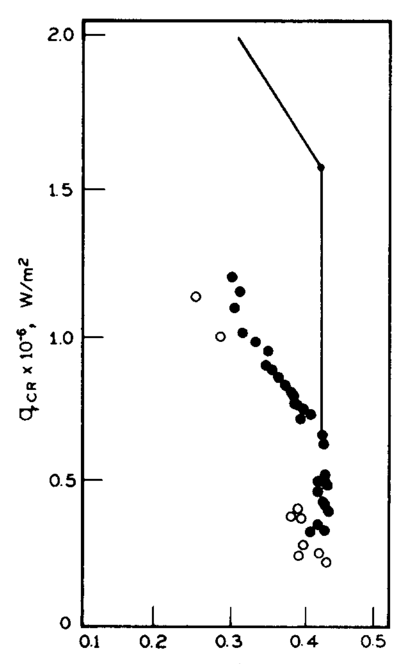

Figure 21. The effect of heat flux nonuniformity on magnitude of critical heat flux. —— uniform, ● $\epsilon = 3$, ○ $\epsilon = 11$. $P = 9.8$ MN/m^2, $G = 1500$ kg/m^2 s.

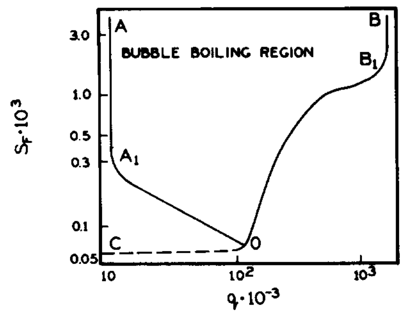

Figure 22. Boundaries of nucleate boiling region. Water at 0.4 MPa. S_f is film thickness in *m* and *q* is heat flux in W/M². Line BB_1 corresponds to nucleate boiling crisis and line B_1O to boiling film destruction (Tolubinskiy *et al.* 1978).

The question of "direct" boiling effects (local hydrodynamics) was brought up by a number of Russian investigators. Doroschuk *et al.* (1978) measured FDO fluxes with water at high pressures with cosine power distributions with peak-to-minimum ratios (ϵ) up to 11. As illustrated in figure 21 the regime of limiting critical quality gradually disappears as this ratio increases. They speculated that this trend is indicative of augmented entrainment due to the boiling process itself. By means of holographic visualization they observed the onset of annular-dispersed flow regime, with a quality of ~ 10% of boiling water at 20–40 bar. The bubble–annular region ("slug") was determined at $0.02 < x < 0.10$. However no direct support of the above mechanism was reported. The "direct" effect mechanism is also *indirectly* supported by the interesting experimental results of Tolubinskiy *et al.* (1978) on the behavior of stagnant boiling liquid films (at 1–10 bar). An example of boiling suppression and film destruction boundaries found is given by figure 22. It should be noted, however, that turbulent film motion would drastically alter these results.

A mechanistic analysis of heat transfer in turbulent falling films based on the statistical behavior of the waves (Telles & Duckler 1970, Chu & Duckler 1974) was recently presented by Brumfield & Theofanous (1976) and was discussed by Seban (1978). Several determinations on the stochastic characteristics of liquid films in the ranges of particular interest here (high pressure, heat addition) have since appeared (Kirillav *et al.* 1978, Subottin *et al.* 1978). More realistic determination of heat transfer and most importantly nucleation characteristics should therefore be possible and worthwhile pursuing. Based on the above experimental results we learn, for example, that the maximum-to-minimum film thickness ratio decreases with quality increase, from a value of 20 at the inception of annular flow, to nearly unity at high qualities.

The study of Styrikovich *et al.* (1978) is an excellent example of investigating the "local hydrodynamics" that I have mentioned earlier. This study conducted at low pressures, high heat fluxes, in addition to reporting data on stochastic film characteristics provides some new insight into the role of boiling on entrainment. Direct visual observation revealed that in addition to the usually accepted modes of entrainment (from wave crests) entrainment may originate to an even greater extent from vapor bubble growth within the film. In fact it was suggested that at high heat loads this becomes the "dominant mode of entrainment." However, the extent of applicability of such conclusions to high pressure boiling water would appear at least questionable in view of the increased shifting from inertial to heat transfer controlled bubble growth (Patel & Theofanous 1976, Theofanous *et al.* 1978a) and hence diminishing magnitude of the perturbation.

The effect of obstacles in disturbing the film flow has also been recognized. Shiralkor & Lahey (1975) reported data from two component low pressure, air–water systems. Again the effects of boiling and high pressure-temperature condition need additional study.

I will conclude my appeal for establishing the mechanisms through relevant but fundamentally oriented "simulations" by citing a recent study by Gardner & Kubie (1976). They found that contrary to previously deduced behavior from air–water systems, an isoamyl alcohol–water system, much better simulated density ratio in steam generator tubes, yielding a stratified ribbon of alcohol while flowing in inclined tubes. This observation accounts for some corrosion failures in boilers.

Low quality region

The upper boundary of the low quality region is defined on the basis of figure 19, at $x \sim 0.15$. In fact for flows and pressure ranges of major interest (including accidents) a boundary range may be taken at $x \sim 0.10 \div 0.15$ (*Whalley et al.* 1978). However, since development of annular flow would diminish as mass velocity decreases (no data exist at very low flows), perhaps even higher qualities would be required for true annular flow. Since even a 10% quality represents a substantial void fraction we can see that this "poorly defined" bubbly-slug flow regime region would be relevant in significant portions of the core for significant periods of time following a LOCA in a PWR. In comparisons to the studies made in the high quality range (annular flow) this region has attracted little attention in recent years. This could be attributed to the relevance of the high quality regime to the most limiting early CHF at the high power stagnated flow region (licensing calculations). Another part of the reason could be due to differences in difficulty and amenability for fundamental understanding between these two regimes.

The available work deals almost exclusively with the subcooled high flow conditions relevant fo PWR normal operation and mild transients (non-LOCA). Bergles (1975) gave an adequate account of the modeling studies in this area. I will highlight certain aspects of this work as I bring in a few new elements.

One of the attempted lines of thought is that the critical heat flux in forced convection is made out of two components: the pool boiling critical heat flux plus the single phase forced convection flux q_c, calculated for the flow conditions and the heater wall temperature (Gambill 1963, Levy 1962). This would appear reasonable for the extreme of high convection velocities since as shown by Gunther (1951) for the subcooled region

$$\frac{q_{cr}}{q_c} = 3 \quad \text{at} \quad 13\ \text{m/s} \qquad [20]$$

$$\frac{q_{cr}}{q_c} = 6 \quad \text{at} \quad 1.7\ \text{m/s} \ . \qquad [21]$$

It would also appear reasonable for the very low convection velocities as suggested by [3]. For the intermediate cases, however, this approach would appear rather simplistic. Although, however, the linear superposition may not be valid, it would appear that the *superposition of mechanisms may be appropriate.*

On a somewhat related idea is based Kutateladze & Leont'ev's (1966) proposal that

$$q_{cr} = q_{cr\,pool} + q_{cr\,blow} \qquad [22]$$

where $q_{cr\,blow}$ is the equivalent heat flux required for the produced vapor to cause boundary layer separation (and thus vapor blanketing). Tong attempted the same idea by neglecting the pool component and introducing an empirical expression which is a function of properties and flow conditions (Tong 1968). In a more phenomenological but still semi-empirical approach Tong (1975) extended this work to include subcooling and turbulent convection heat transfer, bubble layer shielding, spacer grid and two-phase friction, with considerable correlating success (high flow and pressure range, $\alpha < 0.35$).

An interesting dimensional analysis approach is suggested by Kutateladze & Malenkov (1978). For low viscosities:

$$K = f\{M_*,\ \alpha,\ \rho_l/\rho_v,\ Fr_*\} \ . \qquad [23]$$

The stability criterion utilized for pool boiling (see [1]) may be seen to be nothing else but a (liquid drop) Froude number, based on vapor flow and the capillary length, l, characteristic of two-phase systems:

$$l = \left\{\frac{\sigma}{g(\rho_l - \rho_v)}\right\}^{1/2}. \quad [24]$$

Thus [23] is seen to relate the liquid drop Froude number in addition to M_* (as in pool boiling) to void fraction, density ratio and a vapor bubble Froude number. This would appear reasonable for horizontal heaters, although the functional relationship should depend on the heater geometry (plate, pipe, etc.). Its application to vertical flows, however, remains problematical even in the form:

$$K = f\{M_*, \alpha, \rho_l/\rho_v\} \quad [25]$$

since now gravity is *not* the force reflecting restoration of the liquid layer under the vapor blow-off forces. As a minimum a change of the critical value of K would be expected from that for horizontal systems. In this connection the success of Griffith's scheme (see [3]) is also surprising and it would be worthwhile to test it under a wider variety of conditions. The approach suggested by the above equations would require a clear definition of the "appropriate" void fraction in a flow system (averaged over some distance *away* from the wall layer). Kutateladze and Malenkov described a system and experimental technique to test their similarity approach but no data were presented.

For highly subcooled flow conditions a sharply defined two-phase wall layer exists. In addition to the early work of Gunther (1951), Jiji & Clark (1964) attempted to probe the behavior of this layer experimentally, while Bankoff (1961) based on Gunther's data, attempted to elucidate the mechanisms analytically. Bankoff felt that "burnout is preceded or accompanied by a relative inability of the turbulent core liquid to remove heat from the edge of the two-phase wall layer as fast as it is transmitted through the layer" thus attributing a major significance to the condensation process. Hence he proceeded with some condensation experiments (Bankoff 1962) of a minute steam jet in the stagnation flow region of turbulent subcooled water flow. The extremely high heat transfer coefficients measured cannot be attributed to turbulent eddy transfer as may be deduced by applying such models (i.e. [13–15]) with the calculated eddy sizes and frequencies of turbulent flow (Theofanous 1976). Bankoff (1979), however interpreted these results by an appropriate choice of eddy size and frequency based on the observed bubble characteristics. Clearly in this case the condensation process "drives" the local turbulence. This however, may not be the case at high pressures! It would be interesting, therefore, to repeat Bankoff's experiments at high pressures.

I conclude this section by referring to my discussion under pool boiling and especially that concerning hot spot development mechanisms and possible high pressure effects on it.

5. CONCLUSIONS

Current practice in predicting occurrence of boiling crisis is based on highly accurate (for the particular application) empirical correlations for design applications and for predicting transient behavior, and on conservative approaches for predicting the performance of Emergency Core Cooling System under postulated LOCA conditions. Realistic (accurate) prediction of core heatup, however, in the latter case would require significant advances in fundamental understanding of the mechanisms. Furthermore this prediction is inherently limited by uncertainties in certain other parameters.

For both low and high quality flows the better understanding of local hydrodynamics, i.e. drop or bubble fluxes and distributions, would be essential for further progress at the fundamental level. Better description of turbulence properties (length scales, intensities) and effects (bubble and drop motions, breakup and coalescence) would in turn be required for this purpose. Finally, establishing a thorough mechanistic relationship of the Boiling Crisis to the micro-hydrodynamics under all relevant regimes of interest would be necessary to make the results from the local hydrodynamics studies useful.

NOMENCLATURE

Ar_* Archemedes number $= \dfrac{gl^3}{v^2}$
c heat capacity
d hydraulic diameter
D bubble departure diameter
f bubble frequency
Fr_* Froude number $= u_l\left\{\dfrac{\rho_l - \rho_v}{g\sigma}\right\}^{1/2}$
g acceleration of gravity
G mass velocity
h heat transfer coefficient
h_i enthalpy of i
k thermal conductivity
k_L mass transfer coefficient
K Kutateladze's stability constant [1] and [5]
l capillary length $= \left\{\dfrac{\sigma}{g(\rho_l - \rho_v)}\right\}^{1/2}$
L_u integral length scale of liquid velocity fluctuations
m liquid film thickness
M_* Mach number per [4]
Nu_* capillary Nusselt number $= \dfrac{h}{k}\left\{\dfrac{\sigma}{g(\rho_l - \rho_v)}\right\}^{1/2}$
Pe_* capillary Peclet number $= \dfrac{\rho_v u_v c}{k}\left\{\dfrac{\sigma}{g(\rho_l - \rho_v)}\right\}^{1/2}$
Pr_i Prandtl number $= \dfrac{\nu_i}{\alpha_i}$
P pressure
q heat flux
u velocity
x quality (steam)
Z_B boiling length $(Z_{cr} - Z_{sat})$

Greek symbols
α void fraction
α_i thermal diffusivity of i
δ heater thickness
Δ molecular (mass) diffusivity
ν_i kinematic viscosity of i
ρ density
σ surface tension

Subscripts
$'$ fluctuating component r.m.s. value
2ϕ two-phase medium
B boiling
c convective
cr critical

l liquid
lf liquid film
lv liquid-to-vapor change
o inital ($t = 0$)
s solid
sat saturation
t thermal hydraulic or turbulence
v vapor
ω wall

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