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Heat Transfer Correlations in Nuclear Reactor Safety Calculations

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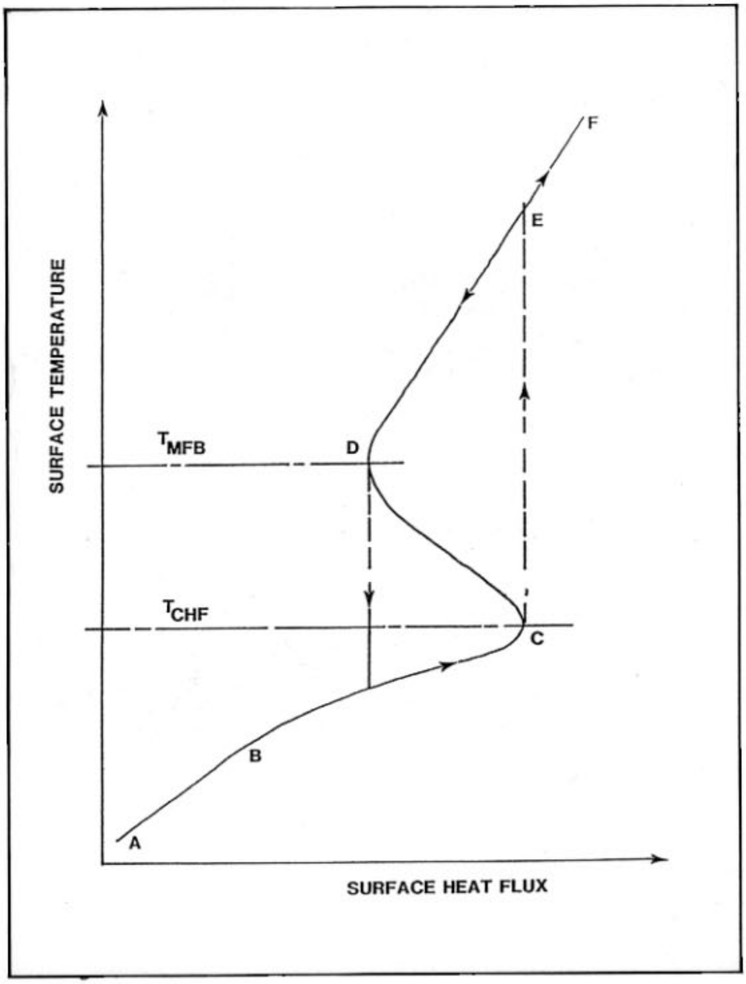

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HEAT TRANSFER CORRELATIONS IN NUCLEAR REACTOR SAFETY CALCULATIONS
SÄK-5

H. Abel-Larsen, Risø
A. Olsen, Risø
J. Miettinen, VTT
T. Siikonen, VTT
J. Rasmussen, IFE
A. Sjöberg, Studsvik
K. Becker, KTH

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Risø: Risø National Laboratory, Denmark
VTT: Technical Research Centre of Finland
IFE: Institute for Energy Technology, Norway
Studsvik: Studsvik Energiteknik AB, Sweden
KTH: Royal Institute of Technology, Stockholm, Sweden

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ABSTRACT

Heat transfer correlations, most of them incorporated in the heat transfer packages of the nuclear reactor safety computer programmes RELAP-5, TRAC (PF1) and NORA have been tested against a relevant set of transient and steady-state experiments. In addition to usually measured parameters the calculations provided information on other physical parameters. Results are presented and discussed.

The report consists of a main report (Vol.I) and appendices (Vol.II). Chapters 3,4 and 5 of the main report are primarily intended for computer programme users. Chapter 6 is recommended for those looking for main results rather than details. The appendices will be useful for computer programme developers.

INIS descriptors:

CRITICAL HEAT FLUX - COMPARATIVE EVALUATIONS - CORRELATIONS - COORDINATED RESEARCH PROGRAMS - DENMARK - DROPLETS - EVAPORATION - FINLAND - FAILURES - FUEL ELEMENTS - FILM BOILING-FORCED CONVECTION - HEAT TRANSFER - LOSS OF COOLANT - NORWAY - N CODES - NUCLEATE BOILING - NATURAL CONVECTION - R CODES - REACTOR SAFETY - SWEDEN - T CODES - TRANSIENT - TRANSITION BOILING - THERMAL RADIATION

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Steering committee for the NKA/SÄK reactor safety programme, SÄK-5 1981 - 1985:

C. Gräslund (Chairman)	Swedish Nuclear Power Inspectorate, SKI Stockholm
T. Eurola	Finish Centre for Radiation and Nuclear Safety, Helsingfors
E. Hellstrand	Studsvik Energiteknik AB, Nyköping, Sweden
D. Malnes	Institute for Energy Technology, Norway
F. Marcus	NKA, Roskilde, Denmark
B. Micheelsen	Risø National Laboratory, Roskilde, Denmark
E. Sokolowski	Nuclear Safety Board of the Swedish Utilities, Sverige

SUMMARY

Knowledge about temperatures on the surface of nuclear fuel rods plays an important role in nuclear reactor safety analysis. There is a need to calculate surface temperatures during operational transients and postulated accidents under the assumption that the engineered safety features of the reactor are activated and working. A typical sequence to be calculated is a loss-of-coolant accident (LOCA).

The purpose of the calculations is to examine whether the integrity of the fuel rod cladding, the first of three engineered barriers against radiological release, may be threatened by the rise in surface temperature that may occur.

The integrity of the cladding depends on several individual phenomena, primarily metallurgical, all very sensitive to the cladding temperature.

For this purpose thermo-hydraulic computer programmes have been developed e.g. the American programmes TRAC and RELAP-5 and the Norwegian programme NORA, all used in the present project.

In order to calculate realistic temperature differences between the cladding surface and the coolant, reliable heat transfer correlations are needed for the different heat transfer regions occuring during a transient (cf. the front page figure).

The heat transfer correlations actually used in the computer programmes are empirical to a high degree. They have been found from the results of independent steady-state experiments, using boundary conditions, that coincide with boundary conditions in safety calculations only partly.

A comprehensive test program is therefore necessary to show whether the computer programmes can provide reasonable simulations of well-defined experiments that are selected so that their conditions may be comparable with those occurring in the fuel element channels of a nuclear reactor.

During the project, 16 experiments, (3 of them transient and 13 steady state) were chosen, and their results were compared with calculations with the computer programmes TRAC (PF1), RELAP-5 and NORA.

Not all relevant parameters for the evaluation of a correlation can be measured in an experiment. Those missing will be calculated by the computer programmes according to their inherent logics and models.

Using the transient experiments it turned out that the heat transfer packages in the computer programmes did not give an adequate picture. This may be due to the influence of other transport phenomena (subcooled void, slip, thermodynamic non-equilibrium) just as different empirical correlations apart from the heat transfer correlations may dominate the calculation. More simplified experiments appeared to be needed, preferably steady state experiments with well-defined boundary conditions. These experiments would need to cover the complete spectrum of heat transfer modes in each flow region with the critical heat flux (CHF) exceeded. CHF is characterized by the loss of contact between the liquid coolant and the cladding surface, when the heat flux applied to the surface is monotonously increased to this point.

CHF is an important parameter, since, with an appropriate safety margin, it separates normal and abnormal operation of a nuclear reactor.

The heat transfer correlations used in pre-CHF flow regions were found to be adequate. The heat transfer in these regions is very efficient and will not be a limiting factor for the fuel and cladding during an operational transient or a postulated loss of coolant accident.

The CHF correlations used in the computer programmes (Biasi, CISE-4) are, however, not adequate. The correlations cannot predict the locus of CHF with sufficient accuracy.

As a consequence, it was decided to start an independent examination of four relevant CHF correlations: Barnett, Becker, Biasi and CISE-4, using results from six full-scale rod bundle experiments (table B4.1.I), and using a separately developed computer programme.

The conclusion from these calculations is that the Biasi and CISE-4 correlations cannot predict the CHF-conditions with adequate accuracy. An explanation may be that these two correlations are developed from single tube data and have no provisions to incorporate the influence of unheated surfaces and internal rod-to-rod power distribution.

The two other correlations, developed in the late 1960's for rod bundle geometries, correlate the experimental data better.

The post-CHF heat transfer conditions strongly depend on the locus of CHF. As the locus could not be calculated with sufficient accuracy it was fixed in the calculations as measured in the continuous examinations of the 13 steady state experiments.

After this the surface temperatures were recalculated. They were too low in RELAP-5 and TRAC, too high in NORA. These deviations were referred back to erroneous predictions of the thermodynamic nonequilibrium.

The thermodynamic nonequilibrium is governed primarily by mass transfer between droplets and vapour due to interfacial heat transfer. A decrease in the vapour generation rate will increase the vapour temperature (superheating) under equal conditions thus allowing the thermodynamic nonequilibrium to be more pronounced. Based on this it must be concluded that the interfacial heat transfer coefficients are too high in RELAP-5 and TRAC too low in NORA.

A recently developed semi-empirical vapour generation model was therefore implemented in the programmes. Hereafter, the results were substantially improved, and a better agreement was obtained between measured and calculated post-CHF surface temperatures.

It is obvious even from these last few calculations, together with the examinations of transition boiling heat transfer using a separately developed programme, that more realistic and phenomenological heat transfer models have to be developed for the post-CHF regions. It is recognized that the degree of thermodynamic nonequilibrium at any axial level will depend on the upstream competition between the heat transfer mechanisms wall-to-vapour by convection, wall-to-droplet by droplet impingment on the surface and vapour-to-droplet by interfacial heat transfer.

The present project has been limited to application of the heat correlations in nuclear reactors. However, the same basic technical questions concerning heat transfer in fluids with droplets or particles and the correlations used to descibe the heat transfer find wide application in a number of fields such as chemical processes, fluidized bed combustion, spray cooling, heat and mass transfer in evaporators, once-through steam generators to name a few.

The problems described in this report are an example of how advanced methods developed for the nuclear area can contribute to other technical areas of current interest.

SAMMENFATNING

Uranbrændslet i en kernekraftreaktor er stærkt radioaktivt. Det er vigtigt, at brændslet indesluttes effektivt, så ingen radioaktive stoffer kan frigøres til omgivelserne. Tre konstruerede barrierer tjener dette formål. De tre barrierer er først indkapslingen, der indeslutter det nukleare brændsel, dernæst reaktortanken, der omslutter reaktorkernen og det primære kølesystem og endelig den tryktætte reaktorbygning, der indslutter reaktoranlæggets primære systemer.

Et vigtigt led i sikkerhedsanalysen af kernekraftreaktorer er at eftervise, at temperaturerne på overfladen af indkapslingen under unormale driftsforstyrrelser og påstulerede uheld, som f.eks. tab-af-kølemiddel-uheld, ikke overstiger en fastsat værdi, når reaktoranlæggets beskyttesessystemer virker.

Overstiger indkapslingslingstemperaturen den fastsatte grænseværdi kan indkapslingen revne. Årsagen hertil er flere metallurgiske fænomener alle følsomme overfor temperaturen.

Den absolutte størrelse af temperaturen afhænger af dels, hvor godt man kan bestemme de metallurgiske fænomeners temperaturafhængighed og dels, hvor godt man kan bestemme indkapslingens overfladetemperatur.

Projektet har beskæftiget sig med een side af sidstnævnte punkt.

Beregning af realistiske temperaturforskelle mellem indkapslingens overflade og kølemidlet forudsætter pålidelige varmeovergangskorrelationer. Ved en korrelation skal her forstås et matematisk udtryk, der nok støtter sig til fysiske principper, men primært er baseret på statistisk behandling af forsøgsdata.

Det primære formål med nærværende projekt var at undersøge pålideligheden af nogle udvalgte korrelationer for varmetransporten med henblik på deres anvendelse i datamaskine-programmer, der modellerer forholdene i reaktorkernen under et uheldsforløb.

Et eksempel på en brændelsstavs overfladetemperatur undet såvel normal drift som under et uheldsforløb er givet på forsiden af denne rapport. Kølemidlet er kogende vand, d.v.s. der er både damp og vand tilstede (to-fase køling).

Ved normal drift af en kernekraftreaktor skal overfladetemperaturen af indkapslingen holdes under den temperatur, der svarer til den kritiske varmestrøm (punkt C på forsidefiguren).

Undersøgelsen af de korrelationer, som anvendes ved bestemmelsen af varmetransporten <u>før</u> kritisk varmestrøm, bekræftede, at de er tilstrækkelig pålidelige. Varmetransporten her er meget effektiv og vil ikke være en begrænsende faktor for indkapslingen.

Kritisk varmestrøm er karakteriseret ved tabet af kontakt mellem indkapslingens overflade og kølevand på grund af dannelsen af et overhedet lag af damp.

De korrelationer, som blev benyttet til bestemmelse af værdien af og stedet for kritisk varmestrøm i de anvendte datamaskine-programmer var ikke tilstrækkelig pålidelige. En uafhængig undersøgelse med et separat udviklet program blev derfor udført på dels de to anvendte korrelationer og dels to andre, ældre korrelationer. Resultatet var, at de to ældre korrelationer bedre kunne forudsige forsøgsdata fra 6 forsøg med elektrisk opvarmede brændselselementer i fuld-skala.

Undersøgelsen af korrelationer for varmetransporten <u>efter</u> kritisk varmestrøm viser, at de tilstrækkelig pålideligt kan eftervise målte overfladetemperaturer i de forsøg, som er benyttet ved sammenligningen, når der tages nødvendigt hensyn til to fysiske fænomener:

1. termodynamisk uligevægt
2. dråbekøling.

Ved den termodynamiske uligevægt skal forstås, at damptemperaturen, efter at den kritiske varmestrøm er passeret, kan blive højere end vandtemperaturen, idet varmetransporten primært vil ske til dampen og herfra videre til vanddråber i dampen. Dampen overhedes d.v.s. varmetransporten til dråber er ikke i ligevægt med varmetransporten til dampen. Ved at tage hensyn til denne overhedning kunne en væsentlig bedre overensstemmelse mellem forsøgsdata og beregnede overfladetemperaturer opnås.

Resultaterne fra undersøgelsen af termodynamisk uligevægt og resultater fra undersøgelsen af den såkaldte transition-kogning (området mellem punkterne C og D på forsidefiguren) viser, at dråbekølingen er et særdeles vigtigt led i varmetransporten.

Ved transitionkogning er varmetransporten bedre end i området for filmkogning (området mellem D og E på forsidefiguren), fordi dråber, som rammer indkapslingens overflade, kan væde overfladen. Kølingen forstærkes ved direkte fordampning af dråberne.

De to nævnte fænomener afhænger stærkt af hinanden, idet graden af den termodynamiske uligevægt vil afhænge af konkurrencen mellem varmetransporten: indkapslingsoverflade til damp ved konvektion, indkapslingsoverflade til dråber ved fordampning og endelig damp til dråber ved såkaldt interfase varmeovergang.

Undersøgelserne i SAK-5 projektet har klarlagt dråbernes betydning for varmetransporten i områderne efter kritisk varmestrøm. Man har vist, at om man vil forbedre pålideligheden er det nødvendigt at inddrage de fysiske fænomener mere d.v.s., der bør udvikles mere realistiske modeller, som i højere grad baseres på de fysiske fænomener end korrelationerne er. Det skal dog bemærkes, at fænomenologiske modeller, der kan bestå af flere matematiske udtryk, kan øge kompleksiteten af beregningsprogrammet og beregningstiden. Et valg mellem den detaljerede fænomenologiske model og den statistiske korrelation kan derfor blive *nødvendig.*

Undersøgelserne har været begrænset til varmetransport i kernekraftreaktorer, men de grundlæggende fysiske og tekniske spørgsmål er de samme for varmetransport indenfor store områder af den moderne teknik, der således også kan drage nytte af undersøgelserne.

1. INTRODUCTION

1.1. The aim of the project

The aim is to establish a set of reliable heat transfer correlations primarily for application in best-estimate computer programmes for nuclear reactor safety calculations.

Correlations in this sense are sets of mathematical expressions, based on physical principles and experimental data, but resting primarily on experimental data.

A best estimate is the most favourable with respect to reality. Favourable is in this context according to use and best knowledge.

Reality refers to those transients that have to be evaluated to assure the authorities that the safety of the system is acceptable. The transients are all expected operational transients and postulated accidents such as a break in the pressure boundary integrity resulting in a loss of core cooling water (Loss-of-Coolant-Accident, LOCA).

1.2. Organization of the report

This report contains a main report (VOL.I) and appendices (VOL.II) The main report covers the examined area from a more general point of view. It provides a natural introduction to the appendices, which describe the work done in detail.

The participating organizations and the actual distribution of the work are dealt with in Chapter 2.

The project was initiated with a literature search. The general considerations and results are discussed in Chapter 3 and Appendix A.

The comparison of selected heat transfer correlations with experimental data using the American computer programmes RELAP-5, TRAC(PF1), the Norwegian computer programme NORA, and separate programmes developed especially for this project is described in Chapter 4 and Appendix B.

Discussions and recommendations based on the results attained are given in Chapter 5.

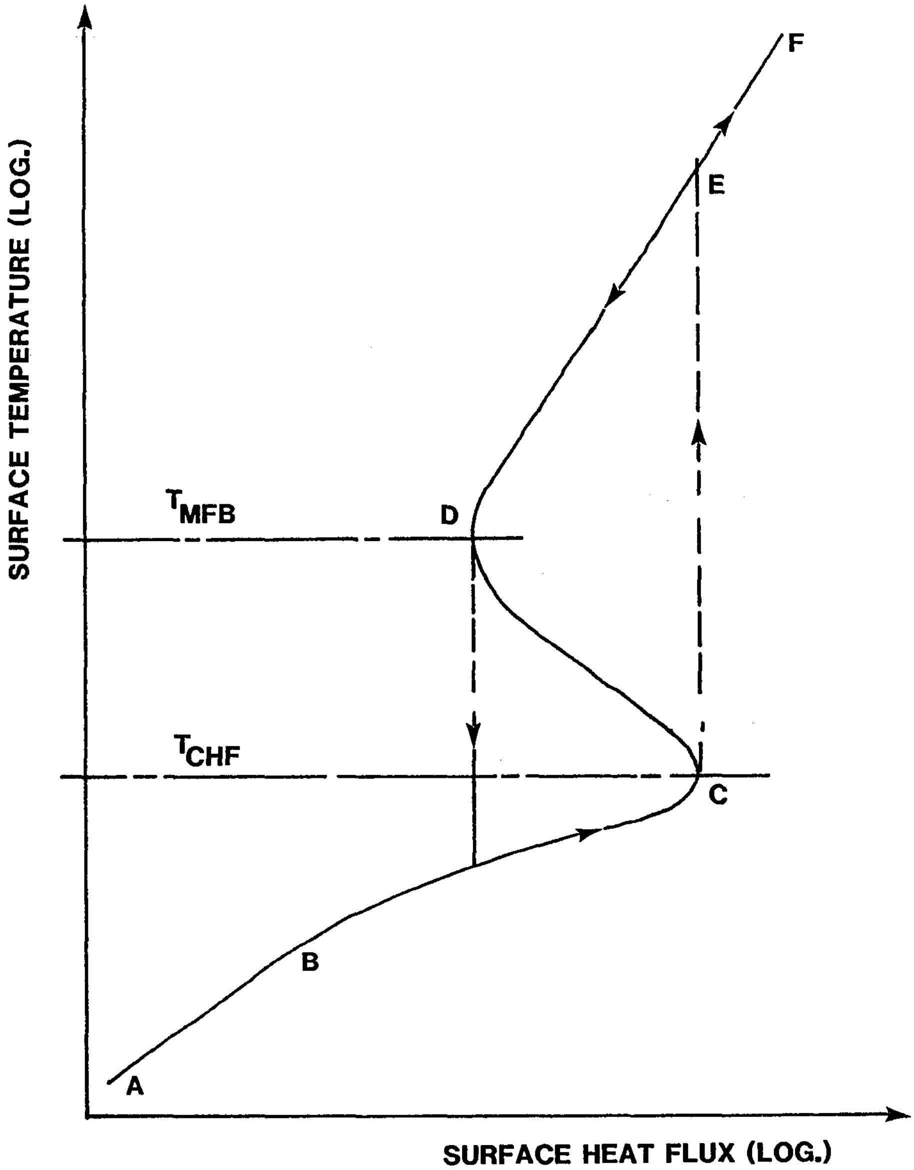
Chapter 6 contains the conclusion of the report.

1.3. Heat transfer regions

The heat transfer correlations are closely connected with the actual flow region. The expressions flow region and heat transfer region may therefore be interchangeable.

The heat transfer regions and their occurrence in light water reactors may be demonstrated by considering the temperature course that a local spot on a heat transfer surface may experience during a LOCA.

An example of such a temperature course as function of surface heat flux is shown in Fig. 1, the so-called boiling curve. The course from A to B represents the single-phase liquid flow region. In the succeeding boiling region two different modes of flow can occur. In one mode, the nucleate boiling mode, the liquid is the continuous fluid; vapour is generated at specific nucleation sites on the heating surface and vapour bubbles are discretely distributed through the continuous, saturated liquid phase (liquid continuous). The other mode, the forced convective boiling mode, is characterized by the vapour as being the continuous fluid with the liquid distributed partly as a film on the heating surface and partly as droplets in the vapour (vapour continuous).



BOILING CURVE (Heat Flux Controlled)

FIGURE 1

When the heat flux applied to the surface in contact with the liquid is progressively increased, a point is reached at which the continuous contact between the surface and the liquid is lost and the critical heat flux (CHF) is attained.

The temperature course in Fig. 1 is shown for a heat flux-controlled surface, i.e. the heat flux is the independent variable as in electric and nuclear-heated systems.

When the surface heat flux in a heat flux-controlled system is further increased the temperature will jump to point E. This temperature increase may cause a burnout in the real sense of the word and a certain safety margin to the CHF point has to be secured during normal operation of a nuclear reactor.

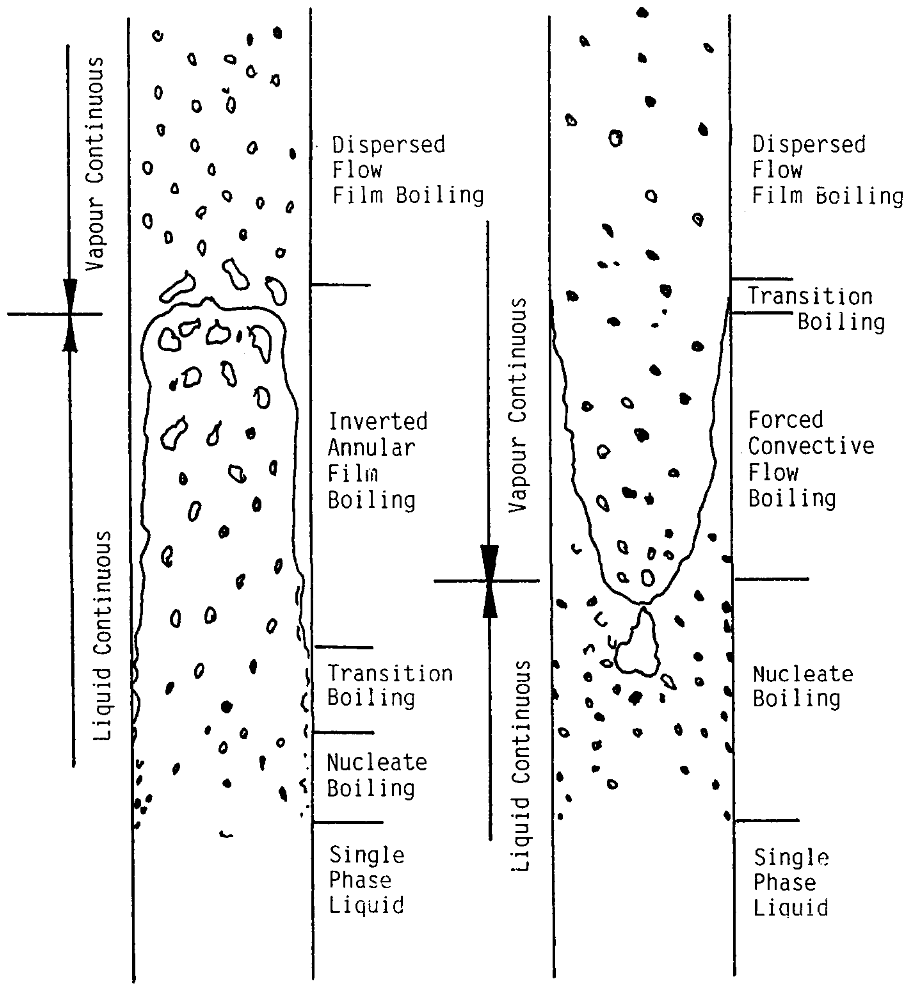
The surface temperature may go from F to E and further to D by decreasing the heat flux. The temperature at D is the so-called minimum film boiling temperature. From here the temperature may jump to the nucleate boiling, i.e. a form of hysteresis effect may exist.

During a strong transient, such as a LOCA, the fuel rods may behave as a temperature-controlled system due to their thermal capacities. The temperature course can then move into the transition boiling flow region (from C to D), where the heat transfer is much better than in the film boiling flow region (from D to E and F).

The transition boiling flow region is active between the temperature at CHF and the minimum film boiling temperature and can be realized only in temperature-controlled systems.

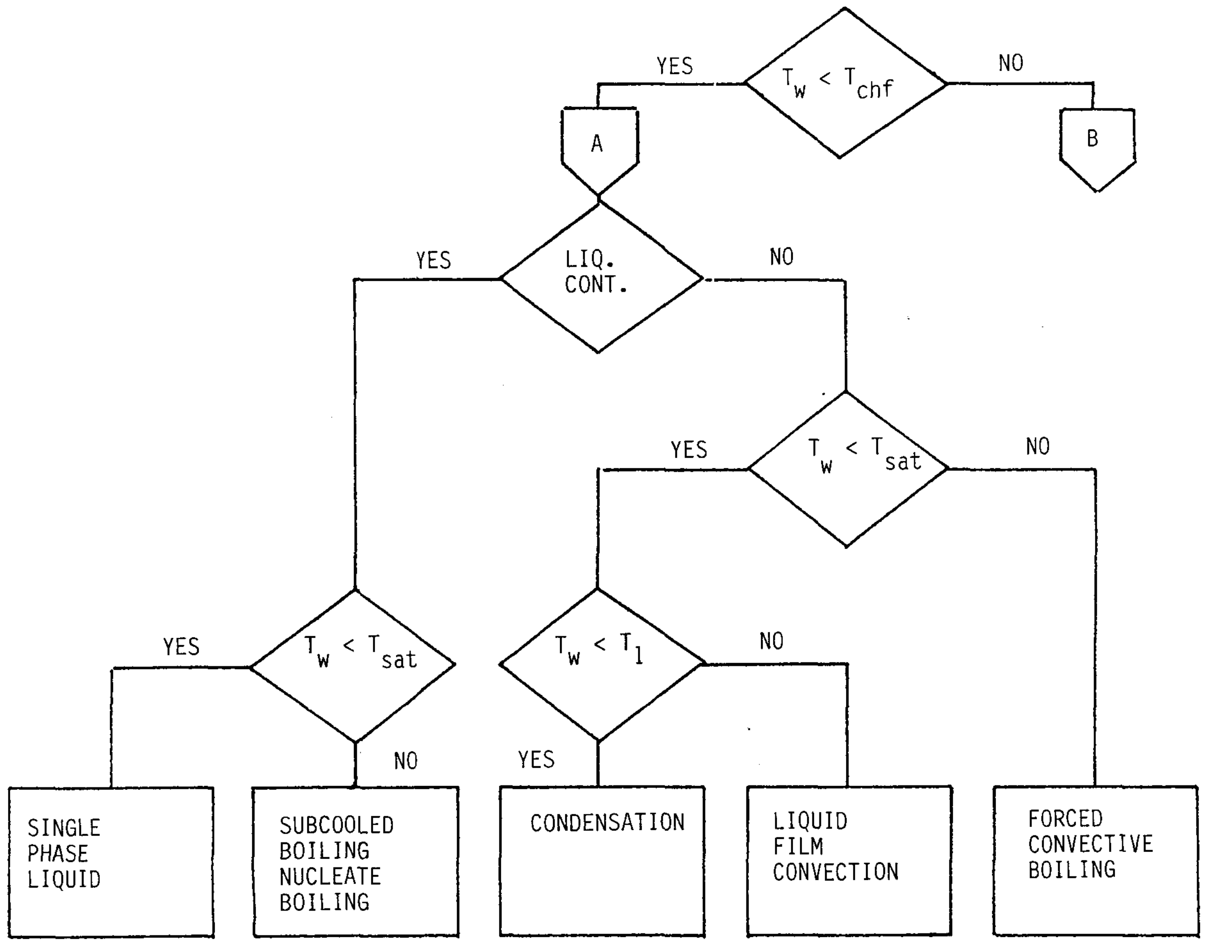
The flow regions as they may occur in emergency core cooling by reflood is shown in Fig. 2.

In general the logic in the selection of the heat transfer corlations may appear from these thoughts. Figures 3 and 4 show the main features of the selection.



HEAT TRANSFER REGIONS

FIGURE 2



HEAT TRANSFER REGIONS

FIGURE 3

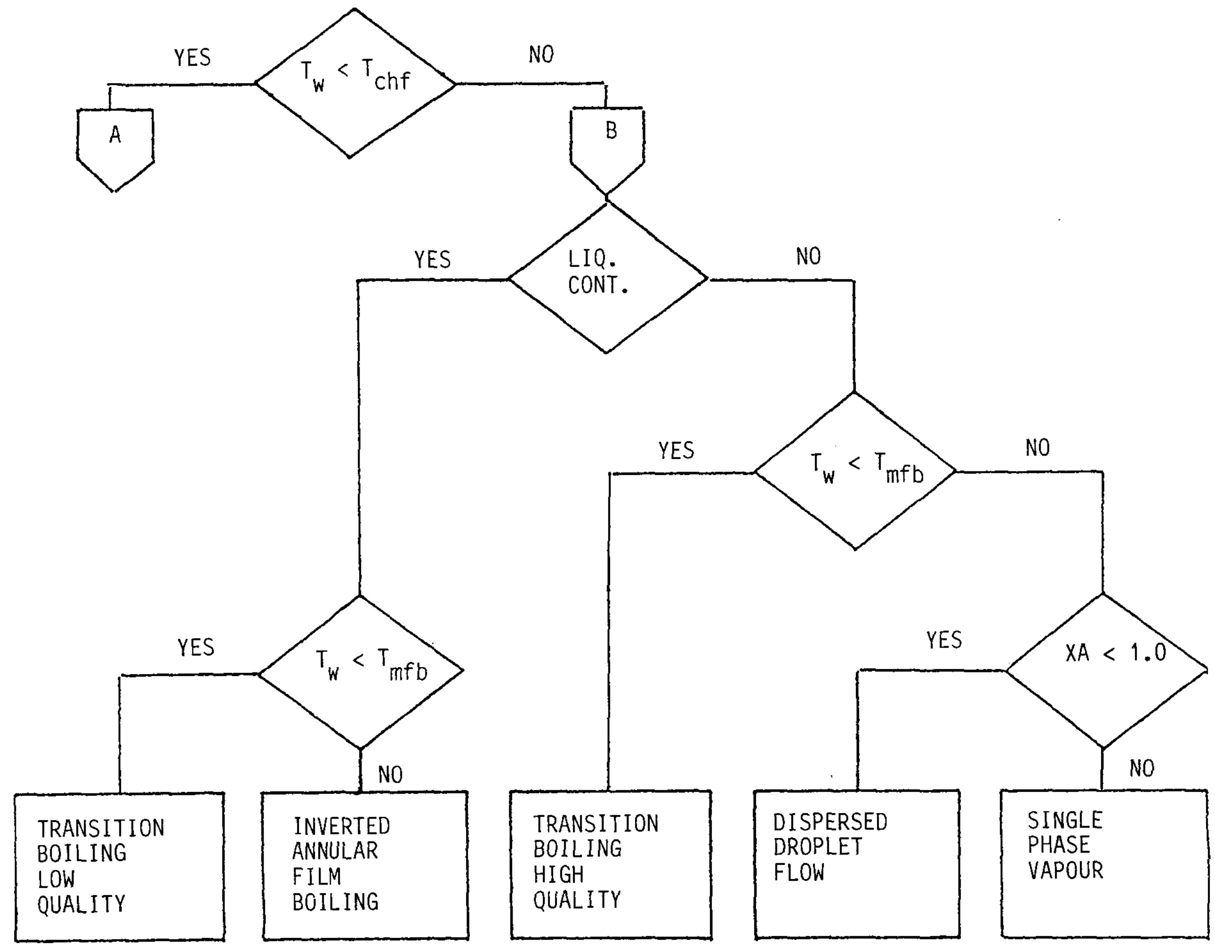


FIGURE 4.

HEAT TRANSFER REGIONS

From what is stated above, the heat transfer during the whole temperature course can be traced to be dependent primarily on the surface temperature and secondarily on whether the flow is liquid continuous or vapour continuous.

The transport equation of heat, first set up by Newton in 1701, is written:

$$Q = h \cdot A \cdot \Delta T \ ,$$

where Q is the heat flow rate, A a characteristic surface area, ΔT a characteristic temperature difference, and the proportionality factor h is defined as the heat transfer coefficient.

The heat transfer coefficient depends on the physical properties of the fluid, mass flux, pressure, absolute vapour quality and the geometry of the system.

2. ORGANIZATION OF THE PROJECT

The project is part of the safety programme of NKA, the Nordic Liaison Committee for Atomic Energy and is in part financed by funds from the Nordic Council of Ministers.

2.1. Participating organizations

The participants have been:

Risø National Laboratory, Denmark
H. Abel-Larsen
A. Olsen (project leader),

Technical Research Centre of Finland, VTT,
J. Miettinen,
T. Siikonen,

Institute for Energy Technology, IFE, Norway,
J. Rasmussen,

Studsvik Energiteknik AB, Studsvik, Sweden,
A. Sjöberg,

Royal Institute of Technology, KTH, Sweden,
Department of Nuclear Energy,
K.M. Becker.

2.2. Distribution of work

The logic classification of the boiling curve has been used as a base for the initial distribution of work in the literature search.

IFE has examined the single phase liquid and the nucleate boiling and forced convective boiling flow region including the critical heat flux.

Risø has examined the transition boiling flow region.

Studsvik examined the film boiling and single phase vapour flow region.

VTT examined the quenching phenomena during the emergency core cooling through top-spray and bottom-reflooding and the interfacial heat transfer.

Professor Becker has taken part in the project since January 1983 as a consultant.

3. PRESENT KNOWLEDGE

The result of a literature search is given in Appendix A. Below is given a general view of the transport phenomena of heat for the physical understanding of the heat transfer and its correlation.

3.1. General considerations

Some basic criteria and general requirements for the selection of heat transfer correlation may be set up as follows (1):

a. The range of the experimental data base on which the correlation is based must coincide with the range of interest.

b. The deviation of the predicted results with the correlation from the experimental data should be low, i.e. a correlation with a lower standard deviation should be selected over another correlation with higher standard deviation.

c. If a. and b. are satisfied, the correlation based on phenomenological or mechanistic considerations should be preferred to the purely statistical correlation.

The phenomenological correlation may offer the possibility of extrapolation outside the range of the data it is tested against and further it may be possible to gain a better understanding of the physical processes involved.

Phenomenological correlations often consist of various mathematical expressions based on primarily physical principles. The phenomenological model may, however, increase the complexity of the computer programme management and increase the use of computer time. A compromise between the detailed phenomenological model and the statistical correlation may therefore be necessary. This depends primarily on the sensitivity of the surface temperature to the selected correlation. If the sensitivity is low, it may be justifyable to lower the complexity.

A key feature of a well-made computer programme is a high degree of modularity so that correlations and models can be easily changed.

3.2. Heat transfer in different flow regions

Heat transfer coefficients in single-phase fluids, liquids or gases, are determined from dimensional analysis and experiments.

Heat transfer coefficients are calculated from correlations determined in this way:

$$\frac{h \cdot D}{k} = C1 \cdot \left(\frac{G \cdot D}{\mu} \right)^{C2} \cdot \left(\frac{C_p \cdot \mu}{k} \right)^{C3} \cdot F$$

where the dimensionless term on the left side is the Nusselt number and the two dimensionless numbers on the right side are the Reynold and Prandtl numbers, respectively. C1, C2 and C3 are constants that are to be determined from experiments under similarity conditions. F is a correction factor, which e.g. may take the length-to-diameter ratio into account.

The other parameters are defined in the nomenclature list.

Heat transfer in two-phase flow is much more complicated due to the interacting interfaces between the two phases, liquid and vapour. There are four types of liquid-vapour interfaces:

1. bubbles in liquid continuous flow
2. droplets in vapour continuous flow,
3. vapour film in liquid continuous flow, and
4. liquid film in vapour continuous flow.

Further, the three different types of heat transfer have to be considered:

a. convection
b. conduction, and
c. radiation.

Figure 5 gives schematically an impression of the highly complex mechanisms involved in the two-phase flow heat transfer.

It is understandable that it is not possible to obtain a single heat transfer correlation that may cover all the flow regions shown in Fig. 1 and schematically in Figs. 3 and 4.

Not all the heat transfer mechanisms occur at the same time and they differ radically in the various flow regions. In most cases the thermal radiation is negligible.

To a great extent, the physical considerations from structuring single-phase heat transfer correlations have been transferred to two-phase flow with appropriate additions even for interfacial heat transfer.

Superposition of the different heat transfer mechanisms is assumed. Nucleate boiling heat transfer, e.g., is correlated as the sum of the heat transfer by liquid-vapour exchange caused by bubble agitation in the boundary layer (microconvection) and the heat transfer by the single-phase liquid convection between patches of bubbles (macroconvection). Appropriately determined weighing factors are assigned to each term.

In the post-CHF regions with vapour continuous flow the heat transfer is very much dependent on the droplet concentration and the surface temperature. At relatively low temperatures the droplets will be able to wet the heating surface when striking it and thus can be evaporated by direct contact with the surface. The heat transfer may be assumed to be a weighted sum of

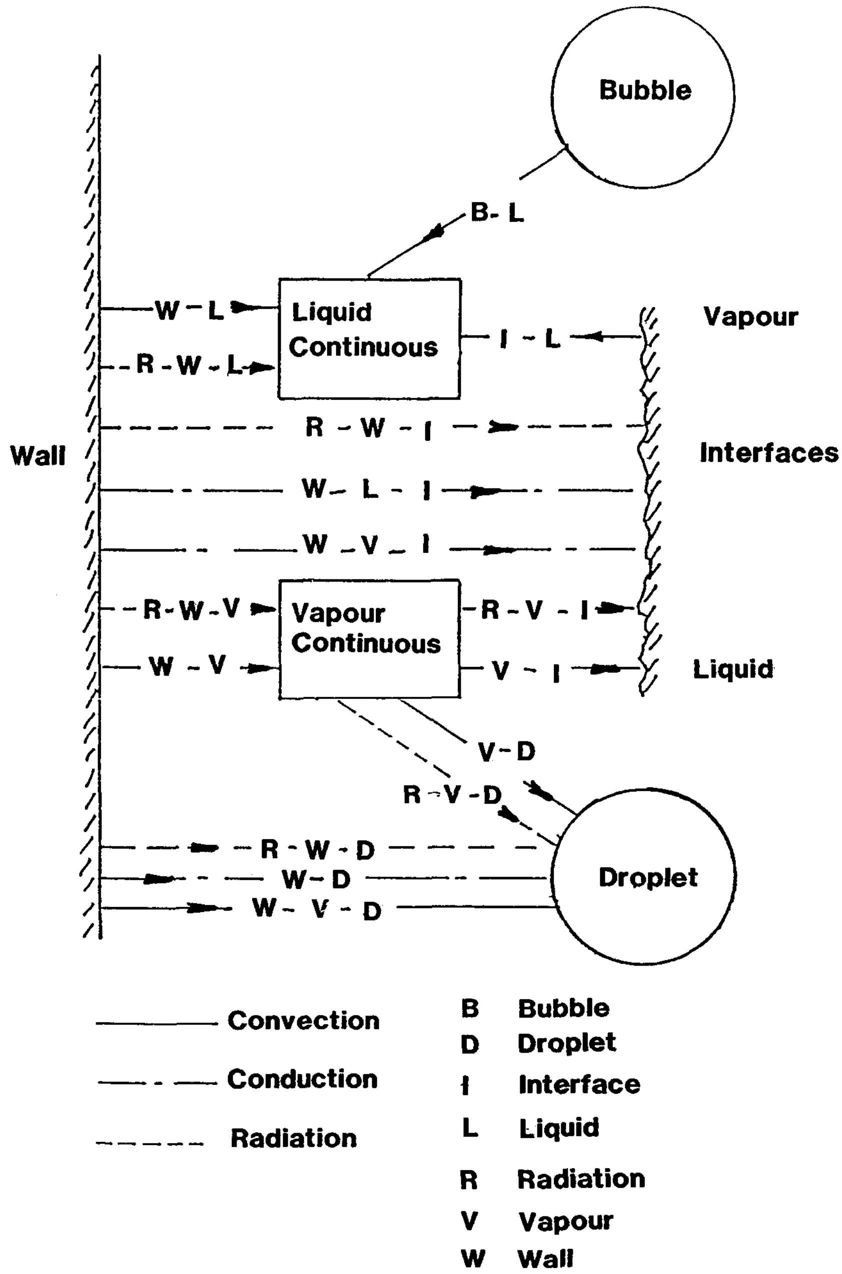

Bubble
B- L
W - L
R - W - L
Liquid Continuous
I - L
Vapour
R - W - I
Wall
Interfaces
W - L - I
W - V - I
R - W - V
Vapour Continuous
R - V - I
W - V
V - I
Liquid
V - D
R - V - D
R - W - D
W - D
Droplet
W - V - D
Convection
Conduction
Radiation
B Bubble
D Droplet
I Interface
L Liquid
R Radiation
V Vapour
W Wall

FIGURE 5.

wall-to-droplet heat transfer and vapour convection heat transfer (transition boiling heat transfer). With a relatively high surface temperature the droplets are no longer able to wet the surface. The heat transfer is decreased, but the presence of droplets in superheated vapour will lower the bulk temperature of the vapour towards saturation temperature due to interfacial heat transfer, thus increasing the heat transfer. Further the temperature profile of the vapour will be changed causing a steeper temperature gradient close to the heating surface, which will also enhance the heat transfer.

Two important points on the boiling curve with their corresponding temperatures are very decisive for the flow region and thus the heat transfer:

1. critical heat flux, and
2. minimum film boiling heat flux.

The critical heat flux (CHF) is by far the most important as it, separates normal and abnormal operation with an appropriate safety margin.

Two mechanisms of CHF are postulated, departure from nucleate boiling (DNB) in liquid continuous flow and dryout (DO) in vapour continuous flow (Fig. 6).

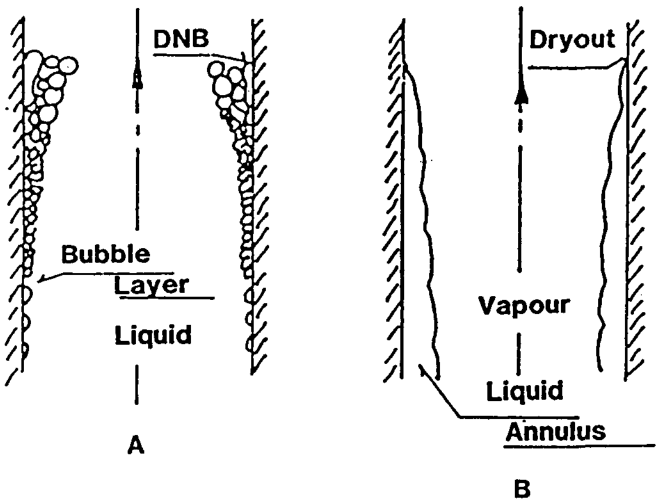
DNB occurs on a heating surface under subcooled or saturated nucleate boiling. Bubbles become crowded in the vicinity of the heating surface and form a moving bubble layer as shown in Fig. 6a. When the bubble layer becomes thick enough to impede cooling liquid contacting the hot surface the bubbles will merge into a vapour film changing the boiling in heat flux controlled systems from the efficient nucleate boiling to the highly inefficient film boiling. Surface temperature excursion is high and fast. Earlier DNB was therefore called fast burnout.

Dryout occurs on a heating surface under forced convective boiling. CHF occurs when liquid film becomes too thin and breaks down into dry patches as shown in Fig. 6b. Surface temperature excursion is low and slow. Dryout was therefore called slow burnout in the past.

It is obvious that the two CHF mechanisms cannot be expressed by the same correlation.

The minimum film boiling heat flux and the corresponding minimum film boiling temperature is an important parameter in temperature-controlled systems as it separates the high temperature region, where the inefficient film boiling takes place, from the lower temperature region, where the more efficient transition boiling occurs. It thus provides a limit to the initiating rewetting by emergency core cooling. The heat transfer coefficient on either side of the minimum film boiling temperature can differ by two orders of magnitude.

As the starting point, more recent studies do not take the minimum film boiling heat flux but rather whether the droplet can wet the surface or not. The surface can, in fact be rewetted even if the minimum film boiling temperature has already been passed, if the kinetic energy of the droplets perpendicular to the surface can overcome the repulsive forces. This is discussed in Appendix C.



Two postulated Mechanisms of CHF

A: Departure from Nucleate Boiling (DNB)

B: Departure from Forced Convective Boiling (DFCB) or Dryout.

FIGURE 6.

4. COMPARISON OF CORRELATIONS WITH DATA

4.1. Computer programmes used

Another task (SÄK-3) under the SAK-project has been to provide one or more computer programmes suitable for small break LOCA analysis.

The SÄK-3 work has mainly been concentrated on the American computer programmes RELAP-5 and TRAC(PF1). It is therefore natural to use the heat transfer correlations and the programme selection logic in these programmes as the starting point for the comparison. The Norwegian programme NORA has also been used in the work.

The programme heat transfer packages are described in Appendix B1.

It cannot be expected that all necessary input data are measured in relevant experiments. It is therefore obvious to let the programme in which the correlation is used calculate these unknown parameters. This has been done in some of the comparisons.

However, using large computer programmes like RELAP-5 and TRAC(PF1) in the comparison, especially in transient calculations, include influences from so many physical phenomena and empirical correlations that it cannot be excluded that other transport phenomena apart from the heat transport may be dominant and thus impede the actual aim of assessing heat transfer correlations.

Separate developed programmes have been used in comparisons of the transition boiling heat transfer and to a certain degree also in the comparison of CHF-correlations in rod bundles.

4.2. Comparisons with data

Several sets of experimental data have been used for the assessment of the heat transfer correlations. In all the experiments the basic measured parameter was the wall temperature of the heater surface. Very minor amounts of information were supplied about the local prevailing two-phase conditions of the system. For this reason some reliance had to be placed in the two-phase flow models of the computer programmes that were used. These two-phase flow models provided the necessary input parameters to the heat transfer correlations; thus, it was obvious that the wall temperature calculations were strongly dependent on the adequacy of the two-phase models as well as the heat transfer correlations. It was an early suggestion in the project that test sections with simple tubular geometry should preferably be used in order to have a possibility to track the two-phase flow calculations. In these experiments the boundary conditions for the test section should be well defined by adequate measurements.

The different experiments selected as test cases are summarized in Table 4.2.I. In all the cases the test section was vertically oriented and the power distributions were axially and in the rod bundle cases also radially uniform. (For details of the different experiments and of the comparisons, refer to Appendix B2.)

In what follows there will be only a summary of the results obtained from the comparison between calculated and measured parameters.

The three first test cases in Table 4.2.I were different types of transients. As pointed out in Appendix B2 it was very difficult to assess the adequacy of the heat transfer package in the computer programmes from the results of these test cases.

The basic reason for this was that in transient calculations the results are influenced by a number of models and empirical correlations describing different kinds of transport phenomena and it is a delicate task to separate the influences from the heat transfer correlations alone. Hence, in the selection of experiments it was decided to switch from transient to steady-state experiments concentrating on the Becker cases.

The Becker cases were conducted at the Royal Institute of Technology in Stockholm and reported in (2), and were kindly placed at the projects disposal by Prof. Becker. The test section was a vertical circular tube directly heated in the tube wall and insulated on the outer surface. The heated length was 7 meters with an uniform power distribution. The inner wall surface temperatures were determined from thermocouple measurements at the outer surface and the calculations with RELAP-5, TRAC and NORA were aimed at simulating the axial temperature distributions when measured inlet and outlet conditions were applied as boundary conditions.

As there were no options in RELAP-5 nor in TRAC to calculate the steady state this had to be done by calculating a transient with time invariant boundary conditions until the solution was stable. With 20 to 35 axial nodes the CPU-time on a CDC CYBER 170-835 for obtaining the steady state in these cases ended up in the range 10 to 20 minutes while the NORAS programme (steady-state version of NORA) required only a few seconds.

From the comparisons between measured and predicted wall surface temperatures, it was obvious that the pre-CHF heat transfer calculations were quite accurate. For single phase forced convection the heat transfer coefficient was calculated from an ordinary Dittus-Boelter type correlation and in saturated boiling region from Chen's correlation (3) in all three of the programmes.

The DO-predictions were not satisfactory in any of the computer programmes. In RELAP-5 the W-3 correlation (4) or the Biasi correlation (5) was used, but an upper void limit 0.96 was also used, and CHF was assumed to have occurred if this value was

TABLE 4.2. I.

Test case	Type of experiment	Geometry	Initial conditions			
			Pressure (MPa)	Mass flux ($kg/m^2/sec$)	Subcooling (^oC)	Heat rate (MW/m^2)
Roumy Case 1	Flow transient	Single tube	14.00	1532	178.1	2.267
Roumy Case 2	Flow/power transient	Single tube	14.00	1527	93.6	2.010
CE/EPRI Case 11	Blowdown	Single tube	15.86	3526	50.5	0.459
Becker Case 1	Steady-state post-DO	Single tube	5.02	1476	9.8	1.015
Becker Case 2	Steady-state post-DO	Single tube	10.01	502	12.1	0.457
Becker Case 3	Steady-state post-DO	Single tube	2.98	498	8.9	0.562
Becker Case 4	Steady-state post-DO	Single tube	7.02	1000	11.7	0.815
Becker Case 5	Steady-state post-DO	Single tube	3.00	1487	9.7	0.869
Becker Case 6	Steady-state post-DO	Single tube	3.01	1005	10.2	0.765
Becker Case 7	Steady-state post-DO	Single tube	13.99	500	9.5	0.405
ORNL Case B	Steady-state Post-DO	Rod bundle	12.76	713	19.1	0.910
ORNL Case C	Steady-state post-DO	Rod bundle	12.46	334	34.8	0.560
ORNL Case H	Steady-state post-DO	Rod bundle	8.89	256	38.0	0.417
ORNL Case K	Steady-state Post-DO	Rod bundle	4.38	226	45.8	0.440
ORNL Case N	Steady-state post-DO	Rod bundle	8.52	806	14.3	0.940
ORNL Case O	Steady-state post-DO	Rod bundle	5.98	307	22.2	0.530

exceeded. In TRAC the Biasi correlation or a void limit of 0.97 was used. In both RELAP-5 and TRAC calculations the DO was generally predicted to occur too far upstream. In the RELAP-5 case the DO predictions were also subjected to some flow oscillations during the course to steady state, which were influencing the results. For that reason a separate examination and assessment of CHF-correlations was made and the results from these efforts are summarized in Chapter 5.2 and Appendix B4.1.

In order to make it possible to examine the post-CHF heat transfer, both RELAP-5 and TRAC were modified so that the DO position could be specified through the input. In NORAS another approach was used: The CHF-value from the correlation was adjusted by a factor to obtain the measured DO position. From the cases, so recalculated it was obvious that the predicted post-DO wall temperatures were significantly lower than the measured ones and also that the calculation of the nonequilibrium effects typical for the post-DO region was inadequate.

Both NORAS and RELAP-5 were modified in order to have the post-DO calculations improved. As the nonequilibrium effects are basically governed by the mass transfer rates between the phases it is obvious that a decrease of the vapour generation rate will increase the vapour temperature thus allowing for the nonequilibrium conditions to be more pronounced. For that reason a new vapour generation model (6) was implemented for the post-DO region in these programmes. In RELAP-5 also the heat transfer models were modified so that the heat was transferred from the wall to the vapour phase and then from the vapour phase to the droplets. The calculated results after these modifications were substantially improved and a satisfactory agreement between the measured and calculated post-DO wall temperatures was obtained.

4.3. Experiences using the computer programmes

During the work a number of difficulties have been encountered with the programmes. The main ones have been concerned with the numerical methods used in RELAP-5 and TRAC. These programmes turned out to be inefficient in the calculation of steady state tests. The implicit methods used have been more effective by a factor of 10...100 in the present test cases.

Another group of difficulties has been caused by the constitutive models. Because RELAP-5 and TRAC are primarily intended for transient calculations, the constitutive models have not been suitable for steady-state situations. An example is the way the critical heat flux is calculated in RELAP-5. Furthermore, many set of correlations contain discontinuities, which may cause troubles for the numerical method. In some cases it is possible that no steady-state solution can be found by RELAP-5.

The most important experiences using the computer programmes are, however, concerned with the accuracy of the correlation packages used. The main emphasis has been paid to wall heat transfer correlations. During the SÄK-5 project it appeared that also the friction correlations and especially the interfacial heat transfer correlations used in the system programmes were poorly tested. The interfacial heat transfer correlations are closely connected with wall heat transfer correlations and have a strong effect on wall surface temperatures in the post-dryout region. The experiences using computer programmes are described in more detail in Appendix B3.

4.4. Comparisons using separate programmes

The test section in the vast majority of experiments is made as direct resistance heated tubes with no filler material in the tubes. The thermal capacity of such a test section is much smaller than in nuclear reactor fuel rods. The transient behaviour, e.g. in a simulation of a LOCA will not be correctly reproduced, the velocity of the rewetting front will be too high and the transition boiling region is hardly reproduced. It is possible to make electrical resistance heaters that can simulate the stored heat, but the costs of such heaters are high, at least 50 times higher than a simple resistance heater tube.

Further, compared to the other flow regions, the transition boiling flow region is short both in extent and time for run-through and difficult to measure in a larger integral post-CHF experiment.

Rewetting and transition boiling heat transfer are therefore examined in experiments made for this special purpose, i.e. special considerations are taken to make the experiment temperature controlled, e.g. use of thermal storage block in connection with the test section or the water is boiled by indirectly heating by another fluid, e.g. hot mercury as in the EPRI-experiment.

Recalculation of such experiments calls for separately developed smaller computer programmes.

An advantage of such programmes is that several heat transfer correlations may be compared with experimental data. It is then necessary, however, to assume that the correlations used in the prediction depend only on local parameters, i.e. rather than on the upstream history. It must be foreseen that should more realistic models be developed, the degree of nonequilibrium at any axial location must be the result of a historically developed state dependent on the upstream competition between the heat transfer mechanisms wall-to-vapour, wall-to-droplet and vapour-to-droplet.

5. DISCUSSIONS AND RECOMMENDATIONS

5.1. Nucleate and forced convective boiling

Highly accurate heat transfer correlations in the nucleate boiling and forced convective boiling flow regions are not required in order to calculate heat transfer during a LOCA (7). The reason for this is that heat transfer in these regions is very efficient and should not be a limiting factor concerning the behaviour of fuel and cladding during a LOCA. For using heat transfer correlations in steam generators in PWR systems, the situation is somewhat more restrictive.
During a LOCA the heat flow in the steam generators may change direction and cause steam generation in the recirculation loops - which strongly influences the flow of coolant through the loops.

In single-phase forced convection any well-known heat transfer correlation may be used, e.g. Dittus-Boelter.

In the nucleate boiling flow region, most heat transfer correlations can be written in the following form:

$$h_{NcB} = B1(P) \cdot (T_W - T_{SAT})^{B2}$$

$$q''_{NcB} = h_{NcB} \cdot (T_W - T_{SAT}).$$

The heat transfer correlations that seem to give the best fit to experimental data have a value close to 2.0 for B2. The correlation by Stephan and Auracher (Table A1. II) has this value- and is recommended to be used for both the reactor core and steam generators. For the reactor core, a somewhat simplified version of the Chen correlation is considered accurate enough.

5.2. Critical heat flux

The results of the literature search are given in Appendix A2 and the outcome of some recent assessments of CHF correlations in rod bundle geometries is summarized in Appendix B4.1.

Leung(22) has demonstrated that an appropriate steady state CHF correlation was adequate for the prediction of CHF onset during a wide range of transients. He also indicated that the local condition hypothesis can be used for CHF predictions. This method is most advantageous in transient analyses. It is very difficult to define an adequate boiling length at each instance during the course of a transient, which is a requirement when other methods are employed.

In Appendix A2 it is recommended to use Griffith-Zuber correlation for the low mass flux range:

$$-240 < G < 100 \text{ kg/m}^2\text{s}.$$

Outside this mass flux range the Biasi and CISE-4 correlations were found to be equally good possibilities. However, it has to be emphasized that this outcome was based on the comparison between the measured and predicted time to CHF during different kinds of transient in round-tube or scaled-rod bundle test sections. When Biasi and CISE-4 correlations were used for prediction of the CHF in full-scale rod bundle steady-state experiments they were found to be very non-conservative (cf. Appendix B4.1). This was attributed to the development of these two correlations from single-tube data. There were no provisions for incorporating the effects of unheated wall surfaces and internal rod-to-rod power distributions. As would be expected, the rod bundle CHF correlations were found to be much more accurate when predicting the CHF in this type of geometry, and it was recommended that the Becker rod bundle CHF correlation be used within the following parameter ranges:

Pressure	3.0 - 9.0 MPa
Mass flux	400 - 3000 kg/m^{2}s
Heat flux	0.5 - 3.0 MW/m^2

For mass fluxes between 100 and 400 kg/m^{2}s an interpolation of the Becker and the Griffith-Zuber correlations has to be made.

Above 9.0 MPa it is not clear which correlation to use. No testing of CHF correlations in this pressure range has been performed within the project, and no firm recommendation can be given. In the literature several possible correlations are reported, e.g. the correlation proposed by EPRI (8) and the one by Bezrukov et al. (9), but these have to be more thoroughly tested and validated before drawing any conclusions.

Also, the situation in the low-pressure range ($P < 3.0$ MPa) is not very well examined. Due to the lack of rod bundle experimental CHF data for these low pressures no assessments of CHF correlations have been made and thus no recommendations can be given. It is obvious that there is a strong need for more experimental data and analysis in this low-pressure range.

5.3. Transition boiling

The experimental data used in this comparison are primarily in the vapour continuous flow region and only very few in the transition flow between vapour and liquid continuous (churn/slug flow).

The correlations selected for comparison (except the correlation by Hsu) were developed primarily for the dispersed flow region in vapour continuous flow. It cannot be expected that the correlations can be used in the inverted annular flow region, i.e.

liquid continuous flow.

The comparison shows the necessity of taking thermodynamic non-equilibrium into account.

The comparison also shows that the direct liquid-wall contact heat transfer is important and has to be accounted for especially in regions near the CHF location. This is in contradiction to the widely used assumption that the contact heat transfer is negligible.

Much more work is needed, however, before transport phenomena in transition boiling can be described.

It is the purely empirical and simple heat transfer correlation by Bjornard and Griffith (11) that relates the test data best; therefore, it must be recommended for use until better phenomenological correlations or models are developed.

The inverted annular flow region in liquid continuous flow has not been examined.

5.4. Rewetting

The rewetting process means a reversal from film boiling conditions after DO or DNB back to nucleate boiling and the main interest concerning the phenomena is connected with large LOCA reflooding or spray cooling.

Two possible methods may be seen for the modelling of the rewetting front:

The two-dimensional heat conduction equation may be solved numerically and the moving mesh techniques are used to split the heat structure around the rewetting front into finer meshes. The numerical inaccuracy is avoided if the finest meshes in axial direction are shorter than 0.001 mm. In moving-mesh techniques the heat transfer correlations are used for the nucleate boiling, critical heat flux and transition boiling. The moving-mesh techniques should not be used if the computing time consumption is limited.

The second possibility is to apply a mathematical correlation for the rewetting velocity. Then the formula of Dua and Tien is recommended:

$$Pe = \left[\frac{Bi}{\theta_w \cdot (\theta_w - 1)} \cdot \left(1 + 0.40 \cdot \frac{Bi}{\theta_w \cdot (\theta_w - 1)}\right)\right]^{0.5}$$

$$Pe = \frac{\delta \cdot \rho \cdot c \cdot u}{k} ; Bi = \frac{h \cdot \delta}{k} ; \theta = \frac{T - T_{SAT}}{T_O - T_{SAT}} ; \theta_w = \theta(T_w).$$

The fitting parameters of the mathematical formula are the heat transfer coefficient h in the Biot, number Bi and the rewetting temperature T_O in the nondimensional temperature. The recommended heat transfer coefficient is the critical heat flux of Zuber correlation divided by the temperature difference ($\Delta T_{SAT} = T_{CHF} - T_{SAT}$); this difference is calculated from the wall temperature during nucleate boiling determined by the Chen correlation. The slowing down of the rewetting due to void fraction is taken into account by the factor $(1-\alpha)$ proposed by Griffith. Two possibilities are recommended for the rewetting temperature: the use of minimum film boiling temperature or a simpler formula like: $T_O = T_{SAT} + 160 + 6 \cdot \Delta T_{sub}$.

5.5. Film boiling

The film boiling region can be divided into basically two flow sub-regions: inverted annular flow and dispersed droplet flow. The development of these sub-regions is strongly influenced by the prevailing pre-CHF region which can be determined from a flow region map.

For the inverted annular flow the heat transfer mechanisms are as yet not very well understood. Until more research sheds light on these issues a simplified approach using a modified Bromley correlation is recommended to calculate the wall heat transfer:

$$q'' = h_{FB} \cdot (T_W - T_{SAT})$$

$$h_{FB} = 0.62 \cdot \left[\frac{k_v^3 \cdot \rho_v \cdot (\rho_\ell - \rho_v) \cdot H_{fg}' \cdot g}{\lambda_c \cdot \mu_v \cdot \Delta T_{SAT}} \right]^{1/4}$$

$$\lambda_c = 2\pi \cdot \left[\frac{\sigma}{g(\rho_\ell - \rho_v)} \right]^{1/2}$$

The parameter range for this correlation is

Pressure	0.1 - 0.7 MPa
Wall heat flux	30 - 130 kW/m^2
Subcooling	< 78 K
Velocity	< 0.3 m/s
Void	< 0.4

The transition of inverted annular flow to dispersed droplet flow may be related to a critical Weber number. When this Weber number is exceeded the liquid core in the inverted annular flow will break down into slugs and droplets. The Weber number is defined as

$$We_{tr} = \frac{\rho_v \cdot (V_g - V_\ell)^2 \cdot D}{\sigma}$$

and the critical We-number according to this definition has the range 10-20.

For the dispersed droplet flow different approaches have been adopted when calculating the wall heat transfer. The most promising seems to be the phenomenological approach in conjunction with a separate fluid model. Different heat transfer modes are then identified and described as well as the interfacial transport phenomena. This approach is also very well suited for thermohydraulic programmes like RELAP-5 and TRAC.

In the film boiling region it is usually assumed that the radiation and wall-to-liquid heat transfer terms are negligible compared with the wall-to-vapour terms. This latter heat transfer can be calculated according to the revised version of the modified CSO (Chen-Sundaram-Ozkaynak) correlation:

$$q'' = h \cdot (T_w - T_v)$$

$$h = h_{mod\ CSO}\ (1+F_s)(1+0.8/(L/D))$$

$$F_s = 250 \left[\frac{P}{P_c}\right]^{0.69} \left[\frac{1-XA}{XA}\right]^{0.49} Re_v^{-0.55}$$

$$h_{mod\ CSO} = \frac{f}{2} \cdot C_{pvf} \cdot G \cdot XA \cdot Pr_{vf}^{-2/3}$$

$$f = f_o \cdot \left(\frac{T_w}{T_v}\right)^{-0.1}$$

$$\frac{1}{\sqrt{f_o}} = 3.48 - 4 \cdot \log_{10}\left[\frac{2\varepsilon}{D} + \frac{9.35}{Re_v \sqrt{f_o}}\right]$$

The basic interfacial processes are dealt with in Chapter 5.6.

When the flow region is single-phase steam flow, where the steam may be superheated, the well-proven correlations of Dittus-Boelter's type can be used to calculate the heat transfer coefficient. The Sieder-Tate correlation is one example of this type:

$$q'' = h \cdot (T_w - T_v)$$

$$h = \frac{k_v}{D} \cdot Re_v^{0.8} \cdot Pr_v^{1/3} \cdot \left(\frac{\mu_v}{\mu_w}\right)^{0.14}$$

The revised version of the modified CSO correlation seems to have a smooth transition to this correlation when XA approaches unity.

For very low flows natural convection may become a significant contributor to the total heat transfer. For turbulent natural convection heat transfer to single-phase vapour the following correlation can be used:

$$q'' = h \cdot (T_w - T_v)$$

$$h = 0.13 \cdot k_{vf} \left(\frac{\rho_{vf}^2 \cdot g \cdot \beta_{vf} \cdot (T_w - T_v)}{\mu_{vf}^2} \right)^{1/3} \cdot Pr_{vf}^{1/3}$$

where the properties are evaluated at film temperature.

5.6. Interfacial heat transfer

When thermodynamic nonequilibrium between the phases is assumed, a constitutive model is needed for the interfacial heat transfer. The way in which the constitutive model is applied depends on the assumptions made in the hydraulic model. When a two-fluid model is applied two constitutive equations are needed, one for the heat transfer from the interface-to-vapour phase (Q_{ig}) and another from the interface-to-liquid phase (Q_{il}). These are related to the interfacial mass transfer as

$$\Gamma = - \frac{Q_{ig} + Q_{i\ell}}{H_{fg}}$$

where H_{fg} is the latent heat of evaporation.

If simplifying assumptions have been made in the hydraulics the number of interfacial constitutive equations decreases. The usual assumption is that one of the phases is saturated while the other phase is in thermodynamic nonequilibrium. In that case only one equation for the interfacial energy transfer must be specified. Usually the model is applied for Γ. Because there is a lack of experimental correlations the same approach is very often used also with the two-fluid model. The interfacial heat transfer rates are expressed as:

$$Q_{ik} = h_{ik} \cdot (T_{SAT} - T_k)$$

where T is temperature, h_{ik} is a kind of heat transfer coefficient (Joule/m^3•s•^{o}C) and the subscript k is either g or ℓ . When this equation is used the phase can be forced to be saturated by applying a large interfacial heat transfer coefficient h_{ik}. In some cases the interfacial heat transfer is closely related to the wall heat transfer. One example is subcooled boiling. In this case a separate wall boiling model must be applied, because when the average fluid is subcooled, no evaporation is predicted through the above equations. If Γ_w is the amount of wall flashing, the total flashing rate is expressed as:

$$\Gamma_{tot} = \Gamma_w + \Gamma$$

where Γ is calculated from the above mentioned equation. Γ is negative (condensation) in the case of subcooled boiling. In practice it is very difficult to model Γ_w so that the total flashing rate is correct. For example, the TRAC(PF1)-programme was unable to predict any subcooled boiling in the test cases analyzed in the SÄK-5 project.

Another case where the interfacial heat transfer rate has a strong effect on the wall heat transfer is post-dryout heat transfer. In the post-dryout flow region the interfacial area between the phases is small and the heat transfer coefficient h_{ig} is low. Consequently, the vapour temperature is increasing in that region, which results in higher wall surface temperatures if the wall heat transfer coefficient remains constant. Encouraging results have been obtained in the SÄK-5 project by applying interfacial heat transfer models in the post-dryout region. Another approach in the post-dryout region would be the modification of the wall heat transfer coefficient so that better agreement with experimental surface temperature is obtained. In fact, this is the only way if equilibrium between the phases is assumed. The use of a nonequilibrium assumption enables the more realistic prediction of the wall temperature distributions.

Although the interfacial heat transfer has an important role in two-phase flow predictions, relatively few empirical correlations exist at present. In the SÄK-5 project only two areas have been studied and only preliminary results have been obtained. These results cover the area of flashing and post-dryout flow region.

The flashing rate is important in the prediction of the pressure recovery following a rapid depressurization and in the prediction of the liquid superheat in quasi-steady flow. The latter is important in the calculation of critical flow. Several flashing correlations have been tested (14). According to the test cases performed the experimental correlation of Bauer et al. (15) gave the best agreement with experimental data. This correlation can be expressed for $h_{i\ell}$ as

$$h_{il} = \frac{(1-\alpha)\cdot\rho_\ell\cdot C_{p\ell}}{\tau}$$

where α is void fraction, C_p is specific heat and ρ_ℓ is liquid density. The time constant τ is defined as

$$\tau = \frac{660}{(\alpha+\alpha_o)(u+u_o)^2\sqrt{P}}$$

where u is liquid velocity and P is pressure in Pa. This correlation is valid in the range of $0.01 < \alpha < 0.96$ and 5 m/s $< u < 54$ m/s, when $\alpha_o = u_o = 0$. However, because there are no other suitable correlations, it is suggested that this correlation is extrapolated outside its validity limits. For this purpose the constants α_o and u_o have been determined to be 0.003 and 4 m/s. In this form the correlation predicts critical flow data with good agreement. In the prediction of the pressure recovery following a rapid depressurization, the agreement seems to depend on the flow area. Relatively good pressure history is predicted with medium-size pipes. In the case of Edward's pipe test, the predicted pressure recovery is too slow and in the case of a large vessel it is too fast. However, this kind of uncertainty has not such a vital importance as has the uncertainty in the calculated critical flow.

Another area that has been studied in the SÅK project is the post-dryout flow region. Calculations have been performed using NORAS and RELAP-5 programmes, which both gave good results. The correlation used for Γ has been developed by Webb et al. (6)

$$\Gamma = 1.32 \left[\frac{P}{P_c}\right]^{-1.1} \left[\frac{GXA}{\alpha}\right]^2 \frac{(1-\alpha)^{2/3} \cdot k_v \cdot (T_v - T_{SAT})}{\rho_v \cdot D \cdot \sigma \cdot H_{fg}}$$

where P_c is critical pressure, G is mass flux, XA is actual quality, k_v is thermal conductivity of superheated vapour, D is hydraulic diameter, and σ is surface tension. This correlation has been used only for the post-dryout region. In that region $Q_{ig} >> Q_{il}$, which makes it possible to express the correlation for h_{ig} as

$$h_{ig} = 1.32 \cdot \left[\frac{P}{P_c}\right]^{-1.1} \cdot \left[\frac{G \cdot XA}{\alpha}\right]^2 \cdot \frac{(1-\alpha)^{2/3} \cdot k_v}{\rho_v \cdot \sigma \cdot D}$$

This correlation has been extensively compared with experimental data elsewhere (16). According to the recent studies the correlation gives a mass transfer rate just after the DO position that is too low. Because of this a modified version of it has been developed (17). The increased vapour generation rate in this "near-field region" is referred to some sputtering effects when the wall liquid film is driven violently off (or reformed by droplet-wall contact) at the CHF-location, i.e. this is not really an interfacial heat transfer. At present, a final evaluation of the correlation as yet has not been made.

In calculating the interfacial heat transfer one possibility is to use the above equations for $h_{i\ell}$ and h_{ig} whenever evaporation occurs. This is justified by the simple fact that there are no better correlations. Furthermore, usually either Q_{ig} or $Q_{i\ell}$ is dominant in the calculation and the extrapolations of the other correlations outside its range are not so harmful. It is obvious that a lot of work must be performed in the future with the interfacial heat transfer mechanism. In this context only two recommendations can be made. However, the correlations recommended are not yet thoroughly tested; their use with a wall boiling model especially remains to be studied in the future.

6. CONCLUSION

It is both natural and rather obvious to test heat transfer correlations in the environment in which they are to be used, i.e. the computer programmes TRAC, RELAP-5 and NORA. The advantage is that parameters not measured in the experiment are calculated under conditions in the programme which ought to correspond to reality in the experiment.

It was, however, not possible in the transient calculations to assess the adequacy of the heat transfer package in the individual programmes. It can not be excluded that other transport phenomena (subcooled void, slip, thermodynamic nonequilibrium) and empirical correlations veil the actual aim, viz. to assess the heat transfer correlations.

The general idea, to use the computer programmes in the comparison, was retained, but it was decided to make a switch in the selection of experiments from transient to steady state, simple experiments.

The accuracy of heat transfer correlations in pre-CHF regions i.e. the nucleate boiling region and the forced convective boiling region does not have to be very high for the purpose of calculating heat transfer during a LOCA. The heat transfer in these regions is very efficient and should not be a limiting factor for the fuel and cladding during a LOCA.

The critical heat flux (CHF) calculations were carried out with the W-3 correlation (4) (primarily a DNB correlation) in RELAP-5 as well as the Biasi correlation (5). The predicted dryout locations were too far upstream (up to ~ 3 m at low pressure). The CHF were in some cases determined by a cutoff void limit of 0.96 when the Biasi correlation was used.

The results from TRAC were fairly accurate except at low pressure. TRAC makes use of the Biasi correlation, but in none of the cases did the correlation determine the locus of dryout. An arbitrarily chosen cutoff void limit of 0.97 caused dryout.

The NORAS calculations used three different CHF-correlations, Biasi, CISE-4 (24) and Becker (64). Biasi and Becker correlations predicted dryout too far downstream while CISE-4 correlation predicted dryout too far upstream at high quality and too far downstream at low quality.

It was therefore necessary to freeze the locus of CHF at the measured location in order to compare the surface temperatures. This comparison was not good in any of the programmes used. The calculated surface temperatures were too low in RELAP-5 and TRAC indicated an interfacial heat transfer that was too high. NORAS gave too high surface temperatures, i.e. too low interfacial heat transfer.

This can be seen from a single calculation using NORAS and 5 calculations using RELAP-5 in Figs. B2.21-B2.27. In two of the RELAP-5 cases the interfacial heat transfer was so high that no vapour superheat was calculated (the liquid and vapour post-dryout temperatures were about the same).

The test section was an electrically heated tube with a very low thermal capacity i.e. a typically heat flux controlled experiment. The thermodynamic nonequilibrium effects are therefore primarily governed by mass transfer between droplets and vapour due to interfacial heat transfer. A decrease in the vapour generation rate will increase the vapour temperature under equal conditions, thus allowing the thermodynamic nonequilibrium to be more pronounced.

The programmes were modified by implementing a recently developed vapour-generation model by Chen (6). Recalculated results were substantially improved and a satisfactory agreement between measured and calculated post-dryout surface temperatures was obtained.

From these examinations, together with the examinations of the transition boiling heat transfer with a separate computer programme, it is obvious that more realistic and phenomenological heat transfer models have to be developed. It must be recognized that the degree of thermodymamic nonequilibrium at any axial location is dependent not only on local conditions but especially on the upstream competition between the heat transfer mechanisms wall-to-vapour, wall-to-droplet and vapour-to-droplet.

Up to now this report treated the post-CHF region in more independent chapters, transition boiling heat transfer, rewetting, interfacial heat transfer and film boiling with and without droplets. To get realistic models the transport phenomena have to be considered in more detail and the post-dryout region may be treated as one region where the dividing point between transition boiling and film boiling is not the minimum film boiling temperature, but rather the point where the droplet can wet the surface or not. The surface can, in fact, be rewetted even if the minimum film boiling temperature has been passed, if the momentum of the droplets perpendicular to the surface can prevail over the repulsive forces due to evaporation at the wall. The project decided to touch upon these phenomena in a somewhat more detail and the results are discussed in Appendix C.

With respect to the discouraging results of critical heat flux calculations, the project decided to make a closer elaboration of 4 relevant CHF-correlations: Barnett, Becker, CISE-4 and Biasi using results from full-scale rod bundle experiments. The results indicate that Biasi and CISE-4 correlations, which are used in computer programmes like RELAP-5 and TRAC, cannot predict the CHF-conditions with adequate accuracy. An explanation is believed to be that these two correlations are developed from single tube data and have no provisions to incorporate the influence of unheated surfaces and internal rod-to-rod power distribution. The two other CHF-correlations are developed for rod bundle geometries and correlate the experimental data more accurately. It is therefore recommended that these correlations instead of Biasi and CISE be used.

NOMENCLATURE LIST

A	Area	m^2
C_p	Specific heat capacity	joule/kg^{o}C
C_o	Distribution parameter	
d	Diameter droplet	m
D	Diameter	m
E	Entrainment	
e	Internal energy	joule/kg
f	Friction coefficient	
G	Mass flux	kg/m^2·s
g	Gravitational constant	m/s^2
h	Heat transfer coefficient	W/m^{2o}C
H	Enthalpy	joule/kg
H_{fg}	Latent heat of evaporation	joule/kg
j	Volumetric flux (superficial velocity)	m^3/m^2s
k	Thermal conductivity	W/m^{o}C
L	Length	m
L_C	Characteristic length	m
M_{ik}	Generalized interfacial drag force	N/m^3
n	Normal vector	
P	Pressure	Pa
P_c	Critical pressure	Pa
q"	Heat flux	W/m^2
Q	Heat flow rate	Watt
r	Radius droplet	m
R	Gas constant	joule/kg^{o}C
T	Temperature	^{o}C
t	Time	s
u	Velocity	m/s
v	Velocity	m/s
V	Volume	m^3
XA	Quality (nonequilibrium)	
XE	Quality (equilibrium)	
y	Distance normal to wall and positive towards centre of channel	m

Greeks

α	Void fraction	
β	Volumetric expansion coefficient $(-\ 1/\rho\ (\partial\rho/\partial T)_p)$	$1/^{o}C$
δ	Wall thickness or boundary layer thickness	m
Γ	Vapour generation rate	kg/m^3s
ε	Surface roughness	m
κ	Thermal diffusivity	m^2/s
μ	Dynamic viscosity	$kg/m \cdot s$
ξ	Integral variable	s
ρ	Density	kg/m^3
σ	Surface tension	N/m
τ_z	Shear stress	N/m^3
τ_{zz}	Viscous or turbulent stresses	N/m^3

Subscripts

b	Boiling
CHF	At critical heat flux
d	Droplet
e	Equivalent
f	Fluid (liquid)
FB	Film boiling
FF	Far field region
g	Vapour (saturated)
i	Interface
k	Generalized phase index
ℓ	Liquid
LC	Liquid contact
MFB	At minimum film boiling
NF	Near field region
NcB	Nucleate boiling
NU	Nonuniform
SAT	At saturation
TB	Transition boiling
T	Total
V	Vapour (superheated)

Dimensional groups

Bi	Biot number	$\frac{h \cdot \delta}{k_w}$
Fr	Froude number	$\frac{v^2}{g \cdot D}$
Gr	Grashof number	$\frac{\rho^2 \cdot \beta \cdot g (T_w - T) D^3}{\mu^2}$
Nu	Nusselt number	$\frac{h \cdot D}{k}$
Pe	Peclet number	$Re \cdot Pr$
Pr	Prandtl number	$\frac{C_p \cdot \mu}{k}$
Re	Reynold number	$\frac{G \cdot D}{\mu}$

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