A STEAM GENERATOR CODE ON THE BASIS OF THE GENERAL COOLANT CHANNEL MODULE CCM.
PART I: THEORY
PART II: CODE UTSG-3 AND ITS APPLICATION

Alois HOELD
Bernaysstr. 16A, D-80937 Munich, F.R. of Germany
Phone: ++49(89)3117589; E-mail: a.hoeld@t-online.de

ABSTRACT

A theoretical model for the simulation of the steady state and transient behaviour of a natural circulation U-tube steam generator (including its main steam and feedwater systems) will be presented. Based on it, the newest and most advanced version of the digital code UTSG could be established, the code being constructed (like its predecessors) with the aim to be applicable both as a stand-alone code or as a part of more complex transient codes (e.g., the thermal-hydraulic GRS system code ATHLET).

This latest version (UTSG-3) takes advantage of a newly developed and very generally applicable thermal-hydraulic coolant channel module (CCM) which is based on a theoretical drift-flux related three-equation mixture-fluid approach. This module calculates automatically characteristic parameters of a coolant channel and can thus be used as a general element for the simulation of coolant channels in complex systems (Combination of parallel channels in a 3D core, channels in an once-through or horizontal VVER steam generator etc.). For the code version UTSG-3 the module has been applied for the simulation of the three main channels of this steam generator type, the ‘primary and secondary heat exchange and the riser/separator region’. The procedure demonstrates the possibilities of CCM to simulate within the code UTSG-3 sections with sub-cooled, two-phase and superheated flow together with the movement of their boundaries (boiling boundary, mixture level) along their nodes. Besides them, the code UTSG-3 takes care of the pressure build-up within the top plenum and main steam line, the movement of the enthalpy front along the downcomer and its possible dry-out and the natural-circulation behaviour along the secondary SG loop. The later one has been based on the pressure drop balance along this loop consisting of the (secondary) heat exchange, riser and downcomer regions and based on the fact that the sum all these pressure drop terms must equal zero.

A special example of a transient calculation will be presented in order to demonstrate the properties and validity of the advanced code version UTSG-3. Additionally, these calculations have been used to verify the coolant channel module CCM and to demonstrate its effectiveness by comparing the results of UTSG-3 calculations with calculations of former versions.

1. INTRODUCTION

At the Gesellschaft für Anlagen- und Reaktorsicherheit (GRS) at Garching/Munich very early activities have been started to develop theoretical models and digital codes which have the potential to describe in a detailed way the overall transient and accidental behaviour of both a NPP core but also its main components. For one of these components, namely the natural circulation U-tube steam generator including its main steam system, an own theoretical model has been derived. The resulting digital code UTSG [5] could be used both in a stand-alone way but also as a part of more comprehensive transient codes, such as the thermal-hydraulic GRS system code ATHLET [2,9]. Based on the experience of many years of application both at the GRS and a number of other institutes in different countries but also due to the rising demands coming from the safety-related research studies the UTSG theory and code had to be and had been continuously extended, yielding finally a very satisfactory and mature code version UTSG-2 [8].

From the experience won during the development of these codes it arose the question how to establish an own basic element which is able to simulate the thermal-hydraulic situation in a cooled or heated coolant channel in an as general as possible way so that it can be applied for any modular construction of complex thermal-hydraulic assemblies of pipes and junctions. The resulting theoretical model has been based on a theoretical drift-flux related three-equation mixture fluid approach, its description being presented in a very detailed form in [13]. Based on it, the resulting modular code package CCM (coolant channel module) should allow to calculate automatically all the characteristic parameters of a coolant channel and thus be a valuable tool for the establishment of complex codes [11, 12]. It can, for example, be used for the construction of 3D thermal-hydraulic codes which are needed for the simulation of non-symmetric single- or two-phase flow situations within large NPP (PWR or BWR) cores or for the description of the primary or secondary side of different types of steam generators (U-tube , once-through or VVER-440 with a special attention to the sometimes very complicated mass flow situations in these types). The coolant channel module can especially be important for the main demand on such 3D codes, namely the automatic calculation of the flow distribution into different parallel coolant channels after a non-symmetric perturbation of the entire system. This problem can be solved by taking into account the fact that during a transient the pressure drops over all channels must stay equal to an average one. If this is not the case the inlet mass flow into different channels simulated by CCM must be manipulated in a recursive way until, within each intermediate time step, the upper demand is fulfilled.

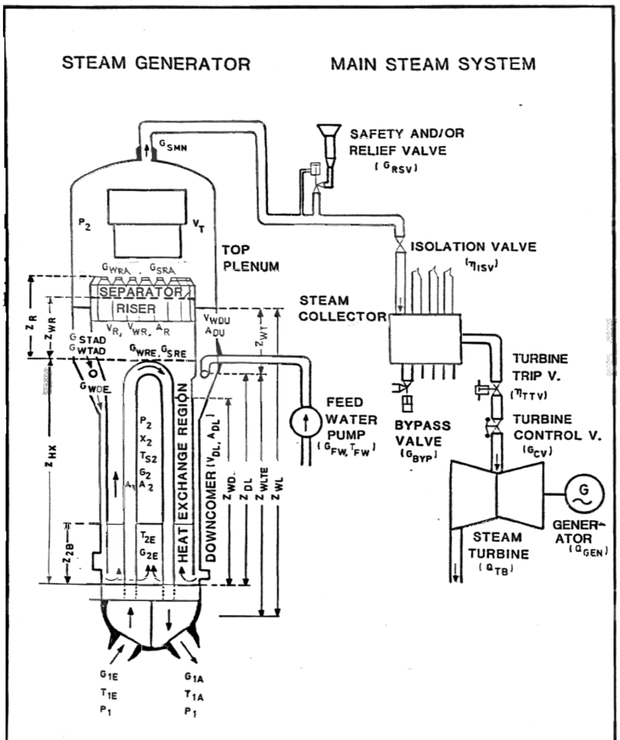


FIG. 1 : LAYOUT OF A NATURAL-CIRCULATION U-TUBE STEAM GENERATOR WITH ITS CORRESPONDING MAIN STEAM SYSTEM

To check the performance and validity of the code package CCM (and to verify it) the digital code UTSG-2 [8] has been extended by means of this package to the new version (UTSG-3). It is, similarly as the code UTSG-2, based on the same U-tube and main steam system layout as sketched in fig.1. This means, the vertical natural-circulation U-tube steam generator is considered to consists of a (primary and secondary) heat exchange (HEX) region (evaporator), a riser (index R) ending with a steam separator, a top plenum (index T) with its main steam system (index MS) and a downcomer (DCM). The HEX region is separated by a number (N_{TUBES}) of vertical U-tubes into a primary and secondary loop, with the primary coolant flowing on the inner side (index 1), the secondary coolant on the outside (index 2) of these tubes. The DCM is split into an upper and (with respect to the FW entrance) lower part (indices DU and DL). A feedwater system (index FW) transports sub-cooled water into the DCM. To each of the U-tube steam generator systems (four in the case of a PWR) belongs a main steam line (with an isolation valve and a sequence of relief- and safety valves), all the lines ending in a steam collector (SC) with steam lines to the bypass and steam turbine system (bypass, turbine-trip and turbine control valves).

Among other essential improvements (e.g., providing a more advanced simulation of the riser and downcomer regions), in the here presented version the three characteristic channel elements of the code UTSG-3, i.e. the primary and secondary side of the heat exchange region and the riser region, have now been replaced by adequate CCM modules, the modules being distinguished by different key numbers (KEYBC = 1, 2 or 3).

In this paper only a short review of the coolant channel module CCM can be given (chapter 3), for more details see [11,12]. The paper is concentrated on a comprehensive description of the newest and advanced status of the theoretical U-tube steam generator model and its code version UTSG-3 as being based on CCM. Finally, its flexibility will be demonstrated on an example by post-calculating a complicated situation after a transient ('loss of main feedwater in a PWR NPP with turbine trip and reactor scram') with the code UTSG-3. A comparison with already existing (and tested out) UTSG-2 calculations for the same case will give an insight into the validity of the new code version UTSG-3 and thus help to verify the general coolant channel module CCM too.

2. FUNDAMENTAL EQS. FOR SINGLE AND TWO-PHASE FLOW

The here presented thermal-hydraulic model is based on the Fourier heat conduction eq. for the heat transfer through the tube walls and the classical 3-equation mixture fluid theory, i.e. on conservation equations for mass, energy and momentum for single and/or two-phase water/steam flow, thereby including into the considerations also adequate constitutive eqs. such as tables for thermodynamic and transport properties of water and steam, correlation packages for heat transfer coefficients, one- and two-phase friction coefficients etc. Among these constitutive eqs. the right choice of an adequate drift-flux package plays an important role, yielding not only the necessary 4-th eq. within the overall system of differential and constitutive eqs. but being also responsible for the possibility to simulate in a very detailed way stagnant or countercurrent flow conditions or the appearance of entrainment within such a coolant channel too.

Usually the conservation eqs. are given in form of partial differential eqs. (PDE-s), In a closed loop a fourth conservation eq. is demanded, namely the 'volume balance eq.', needed for the determination of an absolute (system) pressure parameter whereas the momentum balance yields only the pressure differences over a certain channel element.

The characteristic time- and local-dependent single- and two-phase flow parameters of a coolant channel can be determined by starting from the classical set of partial differential equations (PDE-s), i.e. the conservation eqs. for mass, energy and momentum, and corresponding constitutive eqs.

2.1 CONSERVATION EQS. (Single- and two-phase flow):

Mass balance :

$$\frac{\partial}{\partial t}\{A[(1-\alpha)\rho_W+\alpha\rho_S]\}+\frac{\partial}{\partial z}G=0 \qquad (1)$$

Energy balance :

$$\frac{\partial}{\partial t}\{A[(1-\alpha)\rho_W h_W+\alpha\rho_S h_S-P]\}+\frac{\partial}{\partial z}[G_W h_W+G_S h_S]=U_{TW}q_F=A\,q \qquad (2)$$

Momentum balance:

$$\frac{\partial}{\partial t}(G/A)+\left(\frac{\partial P}{\partial z}\right)_A+\frac{\partial P}{\partial z}=\left(\frac{\partial P}{\partial z}\right)_S+\left(\frac{\partial P}{\partial z}\right)_F+\left(\frac{\partial P}{\partial z}\right)_X \qquad (3)$$

consisting besides the general local pressure gradient parameter $\frac{\partial P}{\partial z}$ and the corresponding term for the external perturbations $(\frac{\partial P}{\partial z})_X$ (as caused, for example, by an external pump or the pressure adjustment due to mass exchange between parallel channels) of additional terms for mass acceleration $(\frac{\partial P}{\partial z})_A$, static head $(\frac{\partial P}{\partial z})_S$ and single- and two-phase friction $(\frac{\partial P}{\partial z})_F$

$(\frac{\partial P}{\partial z})_A = \frac{\partial}{\partial z}[(G_W v_W + G_S v_S)/A]$ with $v_W \rightarrow G/(\rho_W A^2)$ or $v_S \rightarrow G/(\rho_S A^2)$ if $\alpha \rightarrow 0$ or 1 (4)

$(\frac{\partial P}{\partial z})_S = -\cos(\Phi_{ZG})\, g_C[\alpha\rho_S + (1-\alpha)\rho_W]$ (5)

(with Φ_{ZG} marking the angle between upwards and flow direction, i.e., $\cos(\Phi_{ZG}) = \pm |z_{EL}|/z_L$ representing the relative elevation height with a positive sign at upwards flow)

$(\frac{\partial P}{\partial z})_F = -0.5 f_R\, |G|G/(d_{HW}\rho A^2)$ with $f_R = f_{DW}$ or $f_R = f_{DW}\Phi^2_{2PF}$ at 1- or 2-phase flow (6)

The three conservation eqs. (with the terms U_{TW}, q_F and q representing the heated perimeter, local heat flux and power density and the possibility of a varying cross flow area A) describe the steady state and dynamic behaviour of three or (at two-phase flow) four characteristic fluid variables. These are the total mass flow G, the fluid temperature T (or enthalpy h) at single-phase or void fraction α at two-phase conditions and the local pressure P. At two-phase conditions for a fourth variable, i.e. the steam mass flow G_S, an own relation is asked. This can be, for example, a drift-flux correlation which yields (together with an adequate correlation for the phase distribution parameter C_0) the drift velocity v_D and thus also G_S, hence closing the set of eqs. They are interconnected by the definition eqs.

$G_S = G - G_W = XG = A\,\alpha\,\rho^{//} v_S = G - A\,(1-\alpha)\,\rho^{/} v_W = \alpha\,\rho^{//}\,(C_0 G/\rho^{/} + A\, v_D)/C_{GC}$ (7)

$v_D = (1-\alpha\, C_0)\, v_S - (1-\alpha)\, C_0 v_W$ (8)

$C_{GC} = 1-(1-\rho^{//}/\rho^{/})\,\alpha\, C_0 \rightarrow 1$ if $\alpha \rightarrow 0$ and $\rightarrow \rho^{//}/\rho^{/}$ if $\alpha \rightarrow 1$ (9)

The further treatment of the conservation eqs. can be done either in a direct way thereby yielding, due to the fast pressure wave propagation (and thus small time constants), a set of 'stiff ' eqs. whose solutions turns out to be enormously time-consuming. To avoid this costly procedure a method has been established by treating the energy and mass balance eqs. separately from momentum balance without loosing too much on exactness. Thereby the thermodynamic parameters in the mass and energy balance eqs. will be determined on the basis of a pressure profile from a recursion step before. Having then solved these two eqs. the nodal pressure gradient terms from eqs. (4), (5) and (6) and thus, from momentum balance, the actual nodal pressure difference terms can be determined.

An important chapter had to be devoted to the handling of the pressure distribution along the channel, among others by introducing a special renormalization procedure in order to compensate also pressure drop contributions from spacers, tube bends etc., terms which are analytically difficult to be represented The resulting pressure drop along the entire BC is the key for the application of the module within an assembly of channels.

2.2 CONSTITUTIVE EQS.:

For the exact description of the steady state and transient behaviour of single- or two-phase fluids there is, besides the conservation eqs., a number of mostly empirical constitutive eqs. needed. Naturally, any effective correlation package can be used for this purpose. A number of such correlations have been developed at the GRS and thoroughly tested, showing very satisfactory results.

Thermodynamic and transport properties of water and steam (State relationships):

Saturation temperature T_{SAT}, densities ($\rho^{/}$, $\rho^{//}$), enthalpies ($h^{/}$,$h^{//}$) for saturated water and steam with respect to their local pressure (P) and corresponding densities (ρ) and enthalpies (h) for sub-cooled water or superheated steam (index W and S) again with respect to their independent local parameters T and P and corresponding derivatives (T_{SATP}, $\rho^{/P}$, $\rho^{//P}$, $h^{/P}$, $h^{//P}$) and partial derivatives (ρ^T, ρ^P, h^T, h^P) with respect to their independent local temperatures T but system pressure P can be determined by using adequate water/steam packages. (See, for example, the code packages MPPWS and MPPETA [10] which have been derived on the basis of tables given by Schmidt et al. [23] and Haar et al. [3]).

Heat transfer coefficients:

The needed heat transfer coefficients along different flow regimes (into and out of a tube wall) can be calculated automatically if applying appropriate heat transfer coefficient packages such as HETRAC [6].

Single and two-phase friction factors:

In the case of single-phase flow the friction factor f_R is, as recommended by Moody [21], equal to the Darcy-Weisbach single-phase friction factor f_{DW}. This factor can be approximated by

$$f_R = f_{DW} = 1 / \xi^2 \qquad \text{(at single-phase flow)} \qquad (10)$$

with the parameter ξ depending on the Reynolds number $Re=Gd_H/(A\eta)$ and the relative roughness of the wall ε_{TW}/d_H according to the relation

$$\xi = 2 \log_{10}(d_H / \varepsilon_{TW}) + 1.14 \qquad \text{if } Re > Re_{CTB} = 441.19\ (\varepsilon_{TW}/d_H)^{-1.1772} \qquad (11)$$

$$= -2 \log_{10}[2.51\xi/Re + \varepsilon_{TW}/(3.71 d_H)] \qquad \text{if } Re < Re_{CTB} \qquad (12)$$

For two-phase flow conditions the factor f_R can be extended to

$$f_R = f_{DW}\, \Phi^2_{2PF} \qquad \text{(at two-phase flow)} \qquad (13)$$

with the single-phase part f_{DW} to be determined under the assumption that the fluid moves with the total mass flow G (= 100 % liquid flow). The only on steam quality and pressure dependent two-phase multiplier Φ^2_{2PF} is given by Martinelli-Nelson [20] as measured curves. A possible attempt to describe these curves could be given by the approximation function

$$\Phi^2_{2PF} = \exp[\, f_1 X / (1+f_2 X+f_3 X^2)^{1/2}\,] \qquad (14)$$

with the factors

$$f_1 = 44.216 + 0.7428\ 10^{-6}\ P,\quad f_2 = 12.645 + 4.9841\ 10^{-6}\ P \text{ and}$$

$$f_3 = 17.975 + 25.7440\ 10^{-6}\ P \qquad \text{(P in Pa)} \qquad (15)$$

It has to be taken into account that for the special case of the steam quality X approaching 1, the friction term is nearing the single-phase steam friction factor $(f_{DW})_S$, i.e., the two-phase multiplier has to tend (after a maximum at about 0.8) to the value

$$\Phi^2_{2PF} \rightarrow (\rho^{/}/\rho^{//})\ (f_{DW})_S / (f_{DW})_W \qquad (\text{if } X \rightarrow 1) \qquad (16)$$

Drift flux correlation:

A very effective drift-flux package (MDS) has been established by Hoeld [13,14] providing a connection between the drift velocity and the local void fraction (in dependence of the system pressure and geometry data of the channel such as cross flow area, hydraulic diameter etc. but independent from the total mass flow). The corresponding correlation is based on the drift-flux theory established by Sonnenburg [22] and Hoeld et. al. [17] and special correlations for the onset and amount of entrainment as proposed by Ishii-Grolmes [17] and Ishii-Mishima [17,18].

For the case of a vertical channel the correlation has the basic form

$$v_D = 1.5 v_{WLIM} C_0 C_{VD}\, [(1+C_{VD}^2)^{3/2} - (1.5+C_{VD}^2) C_{VD}] \qquad (17)$$

with the coefficient

$$C_{VD} = 2\,(1-C_0\,\alpha)\, v_{SLIM} / (3 C_0 \alpha v_{WLIM}) \qquad (18)$$

More details about the determination of the phase distribution parameter C_0 (as a function of the entrainment fraction E_d) and its limit velocities v_{SLIM} and v_{WLIM}, (in dependence of different geometry types) can be found in the references [11,13].

At steady state conditions or after abrupt changes in steam mass flux (i.e., changes in mass flow or cross section) the steam mass flow G_S is demanded as an independent parameter, the void fraction α has then to be determined from the inverse drift-flux correlation. Special care had to be taken to the cases where the void fraction approaches 0 or 1 and to the determination of the nodal gradients, all of them being needed by the formulation of the theoretical model. Besides the cases of vertical up- or downwards, co-, stagnant or even countercurrent two-phase flow situations (along pipes, rod bundles etc.) the drift-flux correlations must have the potential to describe also two-phase flow situations through inclined or even horizontal channels in order to make the theoretical model as generally applicable as possible.

Originally, the drift-flux theory is based on steady state considerations and steady state measurements. Within transient codes they can thus be used only in a pseudo-stationary way, i.e., a change in void fraction results in an immediate change in drift velocity and thus in all the other characteristic two-phase parameters. There exist, however, physical phenomena (melt-water interactions, condensation shocks) where the delay between relative velocity and void fraction has a special importance. In the separate-phase models this is taken care by the (time-dependent) exchange term within the mass balance eqs. of the two phases water and steam. To cover thus also such phenomena by the drift-flux theory this theory has to be extended to such transient situations by providing the drift velocity v_D with a corresponding time-delay function of 1-st order. Hence the original (pseudo-steady state) drift velocity parameter $v_D = v_{DPSE}$ has to be expanded to its transient counter-part

$$v_D = v_{DPSE} - (v_{DPSE} - v_{DB}) \exp[-(t-t_B)/\Theta_{VDT}] \qquad (19)$$

with $v_{DB} = v_D$ at the begin of a time interval $t=t_B$. All the other two-phase parameters are then calculated accordingly.

The disadvantage of not knowing the time coefficient Θ_{VDT} is outweighed by the advantage of having a direct and controlled input coefficient, avoiding thus the uncertainties of the sometimes very complex separate-phase theory. There exist different possibilities to determine this coefficient, either from similar theoretical considerations as performed to establish the exchange terms, from experience or from parameter studies.

3. COOLANT CHANNEL MODEL AND MODULE CCM

The aim of an effective and very generally applicable thermal-hydraulic <u>c</u>oolant <u>c</u>hannel <u>m</u>odule CCM is to simulate the steady state and transient behaviour of a heated or cooled fluid flowing (in upwards, stagnant or even downwards direction) along a basic coolant channel (with the possibility of varying cross sections). Such a CCM can then be seen as an important basic element for the construction of complex thermal-hydraulic codes. Distinguished by corresponding key numbers (KEYBC =1,2, etc.) it can, for example, be applied for the simulation of the steady state and transient behaviour of 3D PWR, BWR or other nuclear reactor core channels, but also for different types of steam generators with sometimes very complex primary and secondary loops (vertical U-tube, vertical once-through or horizontal VVER-440 assemblies). The assumption of varying cross sections makes it also possible to simulate two fluids at different states flowing side by side by treating them as fluids within two channels, the heat exchanged between them being known (See the case of a cold water injection into a steam dome). In this paper the validity of CCM is demonstrated on the case of an U-tube steam generator model.

A very detailed description of the theoretical background of the thermal-hydraulic coolant channel module CCM is given by Hoeld [11,12]. Here only a short review of its main characteristics will be presented.

The theoretical model and module CCM has the potential to be extended, in a second phase, to a 'porous' coolant channel model too, porous at each node boundary, i.e. to the more detailed case where coolant mass (water, steam and/or water/steam mixtures) is exchanged also at nodal boundaries of neighbouring channels (and not only at BC entrance or outlet).

3.1 BASIC CHANNEL AND SUBCHANNELS

One of the fundamental assumptions of the code package CCM is that such a 'basic channel (BC)', can, according to their flow characteristics, be subdivided into a number of 'sub-channels (SC-s)' with sub-cooled (L_{FTYPE}=1), saturated (L_{FTYPE}=0) and superheated (L_{FTYPE}=2) flow conditions. All

SC-s can, however, belong to only two types of them, a SC with an only single-phase fluid (either sub-cooled water or superheated steam) and a SC with a saturated water/steam mixture flow. Subdividing such a BC into a number (N_{BC}) of BC nodes means that each SC is subdivided into a number (N_{SC}) of SC nodes too. The corresponding conservation equations for mass, energy and momentum (given in form of PDE-s of 1-st order) can then be discretized along these SC nodes by applying a special spatial 'modified finite volume method' (see e.g., Hirsch [4]). 'Modified' means that also the possibility of time-varying SC entrance and outlet positions has to be considered. Hence, if integrating the PDE-s over the corresponding SC nodes three types of discretization elements can be expected. Integrating functions within the PDE-s yields nodal mean values, integrating over a gradient yields functions values at node boundaries and finally integrating time-derivatives over eventually time-varying nodal boundaries (e.g. SC entrance or outlet positions) yields time-derivatives of mean function values together with time-derivatives of the SC entrance or outlet positions. Hence, it is obvious that appropriate methods had to be developed that could help to establish relations between the mean nodal and the node boundary function values. There exist different possibilities and concepts to solve this problem in an adequate way. Within the scope of CCM this has been done by a specially developed quadratic polygon approximation procedure (named 'PAX'). Thereby it had been assumed that the solution function of a PDE along a SC can be approximated by a quadratic polygon function over a segment which reaches not only over its node length but also over the adjoining one. (For more details see Hoeld [12]).

Taking the constitutive equations into account yields a set of non-linear ordinary differential equations (ODE-s) of first order for the characteristic parameters of each of these SC and finally also BC nodes. These are the total mass flow (G), the local pressure (P) and, either for a SC with a single-phase flow, the fluid temperature (T) (or fluid enthalpy h) or, at two-phase conditions, the void fraction (α) and steam mass flow (G_S). Hence, to close the set of eqs. in the case of two-phase flow an own relation for a fourth variable is asked. This can be a procedure establishing, for example, a relation between steam mass flow and void fraction. For this purpose the specially developed drift-flux correlation package MDS (Hoeld [13,14]) has been chosen, a correlation which can take into account also the possibility of counter-current flow or the existence of entrainment. Such drift flux correlations play thus a central role in a 3-eq. mixture fluid approach describing in a very detailed way the movement of water and steam phases against each other.

3.2 CODE PACKAGE CCM

Based on this theory the code package CCM could then be established (see Hoeld [11,12]), containing all routines which are needed to describe in a very compact way the steady-state and transient thermal-hydraulic behaviour of the most characteristic parameters of such a SC and thus BC with single- and two-phase fluids. The resulting set of eqs. can then be combined with other sets of ODE-s and algebraic eqs. coming from additional parts of a complex theoretical model.

Needed boundary input parameters:

As boundary conditions the following BC input parameters are needed (in the code they will then automatically be translated into the corresponding SC values):

- Power profile along the entire BC channel, i.e., either the nodal heat power density terms q_{BE} and q_{Bk} (at entrance or each node k=1,N_{BT}) or q_{BE} and the nodal power terms $\Delta Q_{Bk} = V_{Bk}(q_{Bk}+q_{Bk-1})$. For normalization purposes as an additional parameter the (steady state) total nominal heat power $Q_{NOM,0}$ is asked.
- Channel entrance temperature T_{BEIN} (or enthalpy h_{BEIN})
- The total mass flow G_{BEIN} at BC entrance together with pressure terms at BC entrance P_{BEIN} and outlet P_{BAIN}. At steady state these three parameters are needed for normalization purposes. In the transient case they can be used for the determination of mass flow time-derivatives, e.g., at channel entrance (dG_{BE}/dt). However, during a transient very often the term P_{BAIN} is sometimes not directly

available (e.g., at the outlet of parallel channels or in a closed loop consisting of several BC-s). Hence, the corresponding total mass flow time-derivatives, for example at BC entrance (dG_{BE}/dt), can be determined only from the fact that at the end of an intermediate time-step when the loop (over several BC-s) is closed and the pressure difference between entrance and outlet is zero and the assumption that these derivatives will be almost equal along the entire loop. Then, knowing the time-derivatives at the begin of a new time step and the parameters G_{BE} and G_{BEB} the corresponding steam mass flow time-derivatives being needed in the set of ODE-s can be estimated before being available at the end of the time-step.

- If at two-phase flow conditions steam mass flow $G_{SBEIN} > 0$ is entering the BC entrance the corresponding entrance void fraction α_{BE} has then automatically to be determined within the code by applying the inverse drift-flux package MDS.
- The time-derivative (dP_{SYS}/dt) of the system pressure terms, e.g. at certain positions (BC entrance, outlet or outside of the channel). Due to fast pressure wave propagation it can be assumed that corresponding pressure time-derivatives along all channel positions can be taken as identical.

Steady state:

Integrating the steady state part of the basic eqs. over each node volume yields a set of non-linear algebraic eqs. for characteristic nodal BC parameters in dependence of certain inlet parameters. This can be the parameters 'inlet mass flow G_{BEIN0}', 'in- and outlet pressure P_{BEIN0} and P_{BAIN0}' and 'total steady state BC heat power Q_{BIN0}'. These are demanded to adjust the friction coefficients to the actual flow conditions and to readjust the uncertainties in the definition of the heat transfer coefficients and wetted surfaces to the real situation. The solution of this set yields (after some recursion steps) for each SC (within the BC) automatically the nodal function values T_{CNn} and α_{CNn} (at $n=1,N_{CT}$) together with the end positions z_{CA} of each SC (i.e., boiling and superheating boundaries). From all these SC parameters the corresponding BC parameters can be constructed. Together with the BC entrance functions and the boundary positions then, by applying the polygon approximation procedure PAX (see Hoeld [12]), adequate nodal mean BC function values (T_{BMn} and α_{BMn} at $n=1,N_{BT}$) can be determined which are needed as starting values (= initial conditions) for transient calculations.

Transient situation:

For the transient calculation the same parameters as above result from each integration step. They have to be transferred as input to CCM, used for the determination of the new time-derivatives

$$dz_{CA}/dt \ \ \text{(for each SC within a BC)}, \ dT_{BMn}/dt \ \text{and} \ d\alpha_{BMn}/dt \qquad (\text{at } n=1,.N_{BT}) \qquad (20)$$

These differentials are then needed, together with other characteristic channel parameters, within the overall set of differential and constitutive eqs. of a comprehensive code.

Special solution procedurefor the momentum balance eq.:

Discretizing the momentum balance eq.(3) (by integrating it in flow direction over the corresponding SC nodes) yields (for both single- or two-phase flow situations) relations for the pressure increase ΔP_{Nn} along these nodes (i.e., pressure drop if provided with a negative sign)

$$\Delta P_{Nn}=P_{Nn}-P_{Nn-1} \cong \Delta z_{Nn}\, \Delta P_{BAEI} /(z_{BA}-z_{BE})$$
$$= -\Delta P_{GNn}-\Delta P_{ANn}+\Delta P_{SNn}+\Delta P_{XNn}+[1+(f_{FMP0}-1)\varepsilon_{DP}]\Delta P_{FNn}+\Delta P_{FADD}\Delta z_{Nn}/(z_{BA}-z_{BE}) \quad (n=1, N_{CT}) \quad (21)$$

with contributions from external pressure acceleration (pump) or perturbation ($\Delta P_{XNn} >0$ or <0), static head (ΔP_{SNn}), mass acceleration (ΔP_{ANn}) and wall friction (ΔP_{FNn}). In the transient case additionally a pressure difference term (ΔP_{GNn}) had to be considered resulting from time-dependent changes in total mass flow. Since these changes can be assumed to be almost independent from its channel position, the term ΔP_{GNn} can be estimated as

$$A_{Mn}\Delta P_{GNn} \cong \int_{z_{Nn-1}(t)}^{z_{Nn}(t)} \dot{G}\,(z,t)\,dz \;\cong\; \Delta z_{Nn}\dot{G}_{BE} \qquad \text{(Note: } \Delta P_{GNn,0} = 0 \text{ at steady state)} \tag{22}$$

The total pressure difference $\Delta P_{BAE} = P_{BAI} - P_{BEI}$ between BC outlet and entrance follows then by summing up the corresponding contributions from all SC nodes and all SC-s within a BC

$$\Delta P_{BAE} = -\Delta P_{GBAE} - \Delta P_{ABAE} + \Delta P_{SBAE} + \Delta P_{XBAE} + \Delta P_{FBAE} + \Delta P_{ZBAE} \tag{23}$$

The term ΔP_{GBAE} represents the pressure difference due to the time-dependent changes in total mass flow and is given in dependence of the total mass flow time-derivative at BC entrance as

$$\dot{G}_{BE} \cong d_{GBE}\,\Delta P_{GBAE} \quad \text{(and = 0 at steady state)} \qquad \text{with } 1/d_{GBE} = \sum_{n=1}^{N_{BT}} (\Delta z_{Nn}/A_{Mn}) \tag{24}$$

Thereby the time-derivative of the mass flow term can be assumed to be equal along the entire loop.

To the overall pressure balance in the eqs.(21) and (23) an additional friction term

$$\Delta P_{ZBAE} = (f_{FMP0}-1)\varepsilon_{DPZ}\Delta P_{FBAE} + \Delta P_{FADD} \tag{25}$$

has been added by considering the fact that the actual total friction part along a BC (ΔP_{FBAE}) can not be described in a satisfactory manner by sole analytical expressions. There will always be uncertainties in the exact determination of the friction coefficients, the correct consideration of all contributions from spacers, tube bends, abrupt changes in cross section etc.

This means that either an additive friction term (index FADD) is added to the formula above or the friction part is provided with a multiplicative friction factor f_{FMP0}. Which of them should prevail can be governed from outside by an input parameter $\varepsilon_{DPZ}=\varepsilon_{DPZI}$. Thereby the additive part will be assumed to be proportional to the square of the total coolant mass flow (e.g., at BC entrance)

$$\Delta P_{FADD} = -f_{ADD0}(z_{BA}-z_{BE})G_{BE}\,|G_{BE}|\,/(2\rho_{BE}\,d_{HWBE}\,A_{BE}^2) \tag{26}$$

For steady state conditions the term $\Delta P_{ZBAE,0}$ can be determined from eq.(23) by taking into account that $\Delta P_{GBAE,0}=0$ and the steady state pressure difference term over the entire BC is known from input ($\Delta P_{BAE,0}=\Delta P_{BAEI}$).

Defining the additive steady state pressure difference term $\Delta P_{FADD,0}$ as the $(1-\varepsilon_{DPZ})$-th part of the total additional friction term

$$\Delta P_{FADD,0} = (1-\varepsilon_{DPZ})\Delta P_{ZBAE,0} \tag{27}$$

the friction factor f_{ADD0} for the additive part can be determined directly from eq.(26), the multiplicative friction factor f_{FMP0} from eq.(25)

$$f_{FMP0} = 1+\Delta P_{ZBAE,0}/\Delta P_{FBAE,0} \qquad (\text{if } \varepsilon_{DPZ} = 0 \text{ or} > 0) \tag{28}$$

In order to make sure that the total (steady state) friction part stays always negative ($\Delta P_{FBAE,0}+\Delta P_{ZBAE,0} < 0$) the input data must obey certain restrictions. This means that it must be demanded that the input term $\Delta P_{BAEIN,0} < \Delta P_{BULM} = \Delta P_{SBAE,0} - \Delta P_{ABAE,0} + \Delta P_{XBAE,0}$, otherwise it has to be chosen in an adequate way. Additionally, if only $\Delta P_{ZBAE,0} > 0$, ε_{DPZ} has to be set automatically = 1, i.e. $\Delta P_{FADD,0} = f_{ADD0}=0$, with f_{FMP0} (< 1) to be determined from eq.(28).

The validity of the multiplicative and additive coefficients f_{FMP0} and f_{ADD0} has to be expanded to transient situations too. This can be done by assuming that these coefficients should remain constant, i.e. time-independent. Thereby it can be recommended to give more preference to the additive part (i.e., $\varepsilon_{DPZ} \to 0$). If the multiplicative term prevails, changes in the two-phase situation will get (due to the two-phase multiplier) much higher influence, making thus eventually the entire system unstable.

In the transient situation only for the case of a closed loop the total pressure difference ΔP_{BAE} between loop outlet and entrance is known (namely being equal to 0). Hence, if inserting for a BC where the (time-dependent) total pressure difference ΔP_{BAE} is not known it will be proposed to insert into eq.(24) instead of the (unknown) total mass flow time-derivative at BC entrance its equivalent of a recursion step before (e.g., at the begin of the time step) the term ΔP_{GBAE} and thus, from eq.(23), also ΔP_{BAE} can be estimated. Then it follows, at the end of the recursion time step, from the fact that ΔP_{BAE} = 0 for a closed loop from the eqs. (24) and (23) the term ΔP_{GBAE} over the entire loop and thus also the exact mass flow time-derivative at BC entrance (and at any other position along the loop) replacing the previously estimated values by exact parameters.

In case of parallel channels, the total pressure differences over each (eventually unsymmetrically perturbed) channel have to be estimated in a recursive way as the corresponding mean value over all these channels, yielding then different total mass flow time-derivatives and thus the needed mass flow distribution into all these channels.

4. THEORETICAL U-TUBE STEAM GENERATOR MODEL

4.1 HEAT TRANSFER THROUGH AN U-TUBE WALL

Fundamental eqs.:

Radial heat transfer from the primary HEX coolant side through the U-tube wall into its secondary side (indices 1, TW, 2) is governed, at an axial position z, by the 'Fourier heat conduction eq.'

$$\rho_{TW}\, c_{TW}\, \frac{\partial}{\partial t} T_{TW}(z,r,t) = \frac{1}{r}\frac{\partial}{\partial r}\left[\rho\lambda_{TW}\frac{\partial}{\partial r} T_{TW}(z,r,t)\right] \qquad (29)$$

(thereby neglecting the heat transfer in axial direction) with its initial condition

$$T_{TW}(z,r,t{=}0) = T_{TW0}(z,r) \qquad \text{(Index 0: Steady state)} \qquad (30)$$

As boundary conditions the heat transfer conditions at the inner and outer surfaces of a single tube wall (index TW) have to be taken into account

$$-\lambda_{TWk}(z,r,t)\,\frac{\partial}{\partial r} T_{TW}(z,r,t)\ (\text{at } r{=}r_k) = -\,\lambda_{TWk}(z,t)\, T^{(r)}_{TWk}(z,t) = q_{TWk}(z,t)$$

$$= \pm\,\alpha_{TWk}(z,t)\,[T_k(z,t) - T_{TWk}(z,t)] \qquad (+ \text{ if } k{=}1 \text{ and } - \text{ if } k{=}2) \qquad (31)$$

with the inner and outer radii r_1 and r_2 (and $\Delta r=r_2-r_1$), the (given) local primary and secondary fluid temperatures $T_k(z,t)$, the tube wall surface temperatures $T_{TWk}(z,t)=T_{TW}(z,r_k,t)$ and the heat fluxes $q_{TWk}(z,t)$ into and out of such a (single) tube wall.

The nodal power terms ΔQ_{TW1n} from the primary node n into a (single) tube and ΔQ_{TW2n} out of it are then given as

$$\Delta Q_{TWkn} = 0.5 A_{TWkn}\,(q_{TWkn} + q_{TWkn-1}) \qquad (k=1,2) \qquad (32)$$

A_{TWkn} represents the nodal wetted surfaces of a single U-tube k (The tube bend will be taken care by setting for the last node $A_{TW2n} = A_{TW1n}\, r_1/r_2$). The corresponding total power terms Q_{TWk} over such a U-tube k are then the sum over its axial nodes.

The needed heat transfer coefficients $\alpha_{TWk}(z,t)$ (along the tube wall surfaces) have to be determined from an adequate heat transfer coefficients package, e.g. HETRAC [6]. Density (ρ_{TW}) and specific heat (c_{TW}) of a metallic tube wall can be assumed to be independent from its wall temperature. Since the heat conduction coefficients show linear behaviour their surface values can be described as

$$\lambda_{TWk}(z,t) \cong \alpha_{\lambda T} + \beta_{\lambda T} T_{TWk}(z,t) \qquad (k=1,2) \qquad (33)$$

To solve the partial differential eq. (PDE) of 2-nd order usually the tube wall has to be subdividing into a number of N_{RT} layers with the PDE to be integrated over these layers. For the special case of a steam generator U-tube it turned out that its representation by a single layer (N_{RT}=1) is sufficient. It can even be assumed that, because of the relative thin tube wall and thus very small heat capacity of the material, the left side of the Fourier heat transfer eq.(29) can be neglected without loosing on exactness. This means that in the special case of an overall heat transfer (naming it N_{RT}=0) the time-derivative of the mean tube wall temperature can be set equal to 0.

From the resulting (ordinary) differential eq. (in the case of steady state or if N_{RT}=0 from the corresponding non-linear algebraic eqs.) the mean tube wall temperature $T_{TWM}(z,t)$ defined as

$$\tfrac{1}{2}(r_2^2 - r_1^2) T_{TWM}(z,t) = \int_{r1}^{r2} r\, T_{TW}(z,r,t)\, dr \qquad (N_{RT}=1) \qquad (34)$$

can be determined.

If it should be necessary to subdivide the tube wall into more than 1 layers ($N_{RT} > 1$) an approximation of its radial shape by a quadratic function is recommended (See for example [5] for the cases N_{RT}=2 or 3).

<u>Steady state</u> (if N_{RT}= 1) or <u>overall heat transfer</u> (N_{RT}=0 or 1):

The assumption the radial shape of the tube wall temperature to be described by a straight line

$$T^{(r)}_{TW1}(z,t) = T^{(r)}_{TW2}(z,t) = [T_{TW2}(z,t) - T_{TW1}(z,t)]/\Delta r \qquad (35)$$

has the consequence that $\lambda_{SF2}(z,t) = \lambda_{SF1}(z,t)\, r_1 / r_2$ with the mean layer temperature

$$T_{TWM}(z,t) = 0.5\, [T_{TW1}(z,t) + T_{TW2}(z,t)] \qquad (36)$$

From the boundary conditions and the fact that $dT_{TWM}(z,t)/dt = 0$ it follows then that

$$q_{TW1}(z,t) = (r_2 / r_1)\, q_{TW2}(z,t) = \alpha_{OV1}(z,t)\, [T_1(z,t) - T_2(z,t)] \qquad (37)$$

with the overall heat transfer coefficient

$$1/\alpha_{OV1}(z,t) = 1/\alpha_{TW1}(z,t) + \Delta r/\lambda_{TW1}(z,t) + (r_1/r_2)/\alpha_{TW2}(z,t) \qquad (38)$$

From the relations (31) and (36) the parameters $T_{TWk}(z,t)$ and $T_{TWM}(z,t)$ can then be determined.

<u>Transient situation</u> (Single layer, N_{RT}=1):

Integrating eq.(29) over the layer length and inserting from the boundary conditions yields finally an (ordinary) differential eq. of the form

$$dT_{TWM}(z,t)/dt = [q_{TW1}(z,t) - (r_2/r_1)\, q_{TW2}(z,t)]/[(\Delta r(1+0.5\Delta r/r_1)\, \rho_{TW}\, c_{TW}] \qquad (N_{RT}=1) \qquad (39)$$

Estimating the temperature gradients at the wall surfaces as a combination of two gradients

$$T^{(r)}_{TW1}(z,t) \cong 2[T_{TWM}(z,t) - T_{TW1}(z,t)]/(0.5\Delta r) - [T_{TW2}(z,t) - T_{TW1}(z,t)]/\Delta r \qquad (N_{RT}=1) \qquad (40)$$

$$T^{(r)}_{TW2}(z,t) \cong 2[T_{TW2}(z,t) - T_{TWM}(z,t)]/(0.5\Delta r) - [T_{TW2}(z,t) - T_{TW1}(z,t)]/\Delta r \qquad (N_{RT}=1) \qquad (41)$$

the local surface temperatures can be determined from eq.(31) as

$$T_{TW1}(z,t) = [4(2+C_{TW2})\, T_{TWM}(z,t)] - C_{TW1}(3+C_{TW2})T_1(z,t) - C_{TW2}T_2(z,t)] \\ /[(3-C_{TW1})(3+C_{TW2})-1] \qquad (N_{RT}=1) \qquad (42)$$

$$T_{TW2}(z,t) = [4(2-C_{TW1})T_{TWM}(z,t) + C_{TW1}\, T_1(z,t) + (3-C_{TW1}) - C_{TW2}T_2(z,t)] \\ /[(3-C_{TW1})(3+C_{TW2})-1] \qquad (N_{RT}=1) \qquad (43)$$

with the abbreviations

$$C_{TWk} = \Delta r\, \alpha_{TWk}(z,t)/\lambda_{TWk}(z,t) \qquad (k=1,2) \qquad (44)$$

Finally, from the boundary conditions (31) and eq.(39) the nodal heat fluxes terms $q_{TWk}(z,t)$ into and out of a single tube wall and the corresponding time derivatives can be determined.

Heat transfer through the entire U-tube ensemble:

The corresponding nodal power terms (ΔQ_n) and thus also its total value (Q) over all U-tubes (with $N_{TUBES} = A_1/ (r_1^2\pi)$ are then given as

$$\Delta Q_k = N_{TUBES}\ \Delta Q_{kn} = 0.5\ \Delta V_{kn}\ (q_{kn}+q_{kn-1}) = 0.5\varepsilon_{QTW} N_{TUBES} \Delta V_{kn}(q_{TWkn}+q_{TWkn-1}) \quad (k=1,2) \qquad (45)$$

The terms q_{kn} represent the overall nodal power density, ΔV_{kn} the nodal volume of channel k.

Since the nodal power terms ΔQ_{TWkn} depend on the heat transfer and heat conduction coefficients which in turn are functions of the tube wall and coolant channel temperatures the parameters ΔQ_{TWkn} and thus Q_{TWk} have (in the steady state calculation) to be determined in a recursive way. Further uncertainties in the heat transfer coefficients, for example due to the complicated geometry situation (U-tube bends, spacers etc.) can be compensated (within the steady state calculation) by introducing a correction term ε_{QTW} which has to be determined from the given nominal power $Q_{NOM,0}$ by setting $Q_0=Q_{NOM,0}$, thereby assuming this term to remain unchanged at transient conditions.

4.2 PRIMARY AND SECONDARY HEAT EXHANGE REGION

The heat exchange (or evaporator) region (HEX) is assumed to consist of a number of equivalent vertical U-tubes (N_{TUBES}) of the (average) length of $2z_{HXU}$. The secondary side (with the total length z_{HX} and cross section A_2) can be subdivided into N_{ZHX} (maximal 7) equidistant nodes with the nodal length Δz_{HX}. The primary nodes have the same length except the two upper nodes (Δz_{HXU}) which take the bend of the U-tubes into account.

In the here presented advanced UTSG-3 version the wanted differential (and constitutive) eqs. for the primary and secondary HEX (and riser region) are now automatically determined by the coolant channel module CCM ([11,12]). As already explained in chapter 2 for this purpose (besides the initial conditions) only a number of easily available boundary conditions have to be provided to each CCM module (distinguished by their key numbers KEYBC).These are

- Primary and secondary entrance temperatures (T_{1E} and T_{2E}), mass flows (G_{1E} and G_{2E}) and pressures (P_{1E} and P_{2E})
- heat power profile along the primary and secondary HEX side (the mean nodal power values and the power densities at both sides of the HEX entrance)

and (in the steady state case)

- the total nominal heat power $Q_{NOM,0}$ needed for normalization purposes.

Knowing the primary and secondary nodal HEX fluid temperatures the corresponding nodal primary and secondary heat flux values (into and out of a single U-tube) can then be determined (chapter 4.1) and thus also the nodal heat power terms being needed as input to the coolant channel module CCM.

4.3 RISER (with STEAM SEPARATOR)

The riser (index R) is assumed to be represented by a coolant channel (with a cross section A_R and a length z_R) which can be subdivided into (maximal 5) N_{RT} nodes. Applying CCM (and setting K_{EYBC}=3) the wanted time derivatives and characteristic parameters can now be determined in a similar way as for the secondary HEX region taking into account that the riser entrance values are usually equal to the corresponding HEX outlet parameters. Only the entrance void fraction α_{RE} will be different from the HEX outlet value α_{2H} because of $A_R \neq A_2$. Hence, α_{RE} is determined automatically in CCM due to the fact that $G_{SRE}=G_{2H}$ by applying the inverse drift-flux correlation MDS.

At special situations the riser can start to dry-out with the superheating boundary z_{RSPH} (= mixture level) to be provided by CCM. This boundary can even move into the secondary HEX region (z_{2SPH}). Usually the overall parameter z_{SPH} will be equal to $z_{HX}+z_{RSPH}$ but it can be also $z_{SPH} = z_{2SPH}$ if $z_{2SPH} < z_{HX.}$ Similar considerations can be done for the boiling boundary z_{BB} ($=z_{2B}$ but $z_{BB} = z_{RB}$ if $z_{2B} \geq z_{HX}$).

At riser outlet (see fig.1) a separator (index SP) is assumed to be situated showing an ideal separation effect, i.e., the total riser outlet steam mass flow G_{SRA} is directed to the top plenum, the corresponding water term to the DCM. The separator adds to the overall pressure decrease terms an additional (friction) contribution (ΔP_{FRSP}) which can be treated in the same way as done with the additional terms of chapter 3.2, assuming its steady state value $\Delta P_{FRSP,0}$ being given as input or that this term is already included in the overall additional part (In the same form contributions of the tube bows within the secondary HEX region can be treated).

4.4 DOWNCOMER AND FEEDWATER SYSTEM

The downcomer (DCM) is assumed to be subdivided into an upper and lower section (indices DL and DU). The entrance of the feedwater line (at the position z_{FWTE}, with the feedwater mass flow G_{FW} and an enthalpy h_{FW} or temperature T_{FW}) marks the begin of the lower DCM section. The upper DCM section (with the volume $V_{DU,}$ the length z_{DU} and water level z_{WDU}=z_{WD}-z_{DL}) is added to the top plenum. The lower section consists of an annulus with an outer and inner radius r_{DA} and r_{DI} , a flow area A_{DL}, the length z_{DL} and the volume V_{DL}. Saturated water (with the enthalpy $h_{WDE}=h^{/}_{DE}$) leaving the top plenum and entering the lower DCM section with the mass flow G_{WDE} will be mixed at FW and thus DL entrance with the feedwater and flow with the mass flow G_{WTAD}=G_{WDE}-G_{FW} (and the enthalpy h_{WDE}). Thereby it has to be noted, if looking at the steady state energy balance condition,

$$Q_{SINP,0} = G_{FWS,0}[h^{//}(P_{2,0})- h_{FW,0}] \qquad (46)$$

that only 3 of the 4 operational values Q_{SINP0}, $G_{FWS,0}$, $h_{FW,0}$ and $P_{2,0}$ are required (otherwise the problem would be overestimated). Hence, one of the 4 input parameters has to be neglected and calculated directly from the above energy balance relation.

The change in water volume V_{WDU} and thus movement of the water level within the upper DCM part above feedwater entrance is governed by the difference (and eventually deficit) between the entering (saturated) water coming from the top plenum (G_{WTAD} and h_{STAD}) and subcooled water from the feedwater system and water leaving the downcomer (due to natural circulation). If this water volume reaches zero (V_{WDU}=0), partial DCM dry-out can be stated (L_{DCDRY}=1).

According to the natural circulation flow the enthalpy front will move downwards along the DCL. Each element of it will reach after a certain delay time the DCM outlet (G_{WDA}, h_{WDA}, T_{WDA}) and then be yielded (immediately) to the entrance of the HEX region (G_{2E}= G_{WDA}, h_{2E} =h_{WDA}, T_{2E}= T_{WDA}). Different to other approaches this movement (along the lower DCM section) will not be described by a set of ODE-s but simulated by an analytical approach. Thereby the lower DCM section will be assumed to be subdivided into a number of (maximal 50) nodes. The enthalpies at each DCL node can then be determining by estimating for each of these nodes the length of the movement of the enthalpy front during each time step Δt. Taking these new positions as the basic points of a polygon the corresponding enthalpy changes in the original node boundaries (and thus also at DCM outlet) can then be determined by interpolation. No smearing effects due to the movement of the enthalpy front along the DCM has to be expected (See, for example, fig.2C). The corresponding mass flow parameters along the DCM and thus also its entrance (G_{WDE}) or, in case of DCM dry-out, at the upper end of the water column (G_{WDCN}) can then also be estimated starting from the given DCM outlet mass flow (G_{WDA}=G_{2E}).

If the secondary system pressure (P_{2SYS}) and thus the saturation water enthalpy fall below the water enthalpy values of some of these nodes, flashing is initiated. It will then be assumed that the corresponding amount of steam mass is transported to the next higher node, heating-up there eventually the nodal mass of water. If this water is at or has already reached saturation conditions, the corresponding steam mass (or heat content) is transported to the next node, etc. By this very special procedure finally the water mass disappearing from the water column (and being taken into account at the calculation of G_{WZWDL} and steam mass flow G_{SDTE} (=$-G_{STAD}$) entering the top plenum can be estimated quite satisfactory.

On the other side in the case of a partial dry-out of the DCM (with z_{WDL} < z_{DL} and V_{SDL}=$A_{DL}z_{WDL}$ > 0) a certain amount of steam within the steam part of DCL will condense to saturated water (G_{WDCND} with h_{WDCND}) if at this situation cold feedwater is injected into this compartment. Thereby it will be assumed that only a part of the (subcooled) feedwater, i.e., $G_{FWSUB}= (1- z_{WDL}/z_{DL})\varepsilon_{MIX}G_{FW}$, is mixed with the steam and condensing it. The remaining part ($G_{FWSUB} = G_{FW}-G_{FWSAT}$) will fall unperturbed to the top of the water column. The resulting condensed water mass flow G_{WDCND} is added to the water column, its steam part subtracted from the extended top plenum steam volume. (The mixing factor $\varepsilon_{MIX} = z_{DLMIX}/z_{DL}$ given by input states at which length z_{DLMIX} the total feedwater has reacted).

The total change in water (and thus also steam) volume (if L_{DCDRY}=1) can then be derived from the balance of in- and outgoing water mass flows at the top of the water column

$$\dot{V}_{WDL} = (G_{FW}+G_{WTAD}+G_{WDCND} - G_{WDCN})/\rho_{WDLM} \;>\; \dot{V}_{WDLIM} \;\approx -V_{WDUB}/\Delta t \text{ and } G_{WTAD}=0 \quad \text{or}$$
$$= -\dot{V}_{SDL} = 0 \text{ with } G_{WTAD} = G_{WDCN} - G_{FW} \qquad \text{(if } L_{DCDRY} = 1 \text{ or } 0) \qquad (47)$$

(ρ_{WDLM} = average water density along DCL water column, V_{WDUB}= volume at begin of time step)

4.5 TOP PLENUM WITH ITS MAIN STEAM SYSTEM.

The top plenum (index T) with its total, steam and water volumes $V_T = V_{ST} + V_{WT}$ and the system pressure $P_T=P_{2SYS}=P_2$ will be assumed to consist of the steam crest together with the entire main steam system and (as mentioned above) the upper DCM section ($V_{DU}=V_{WDU}+V_{SDU}$). The water volume is thus equal to the volume of the upper DCM section ($V_{WT}=V_{WDU}$). The main steam volume V_{SMN} is counted as volume of the steam pipe line from the isolation valve to the turbine (Hence, in the case of a closure of the isolation valve the corresponding steam and thus total top plenum volumes have of course to be diminished by V_{SMN}).

Caused by the natural circulation flow and the ceasing injection of feedwater it can happen that the TPL (and thus DU) water volume V_{WDU} diminishes, i.e. the DCM water level z_{WD} falls below the feedwater injection position z_{DL}, dry-out of the DCM can be stated (L_{DCDRY}=1). Hence, the originally constant TPL volume V_T has to be extended to the now time dependent parameter V_{TEX} and it has to be taken into account that

$$V_{TEX}=V_T+V_{SDL}=V_{ST}+V_{WT} \text{ with } V_{SDL}=0,\; \dot{V}_{TEX}=\dot{V}_T=0 \text{ and } \dot{V}_{SDL}= \dot{V}_{WDL}=0 \quad \text{(if } L_{DCDRY}=0)$$
$$= V_{ST} = V_T + V_{SDL} ,\; \dot{V}_T = \dot{V}_{TEX} = \dot{V}_{ST} = \dot{V}_{SDL}= \dot{V}_{WDL} \text{ and } \dot{V}_{WT}=0 \qquad \text{(if } L_{DCDRY}=1) \quad (48)$$

The term $V_{SDL}=V_{DL}-V_{WDL}$ depends on the amount of water and steam (G_{WTDE}, G_{STDE}) leaving the TPL in direction lower DCM entrance (or, if having a negative sign, entering it) and the corresponding amount G_{2E} leaving the DCM at its outlet due to natural-circulation flow.

The two-phase flow mixture coming from the riser (and being separated by an 'ideal' separator into its phases) enters with the mass flows $G_{WTE}=G_{WRA}$ and $G_{STE}=G_{SRA}$ and their enthalpies $h_{WTE}=h_{WRA}$ and $h_{STE}=h_{SRA}$ the top plenum (Note: h_{WTE} or h_{STE} can be also sub-cooled water or superheated steam). The steam leaving the top plenum (with the steam mass flow G_{SMN} and the steam enthalpy h_{SMN}) will be governed by the main steam system, consisting of a number of relief and safety valves, isolation, bypass, turbine trip and control valves and a steam turbine (For more details see [5]). At normal situations the water ($G_{WTAD}=G_{WDE}-G_{FW}$ with $G_{WDE}\approx G_{WDA}=G_{2E}$ and the enthalpy h_{WTAD}) leaving the top plenum in direction to the lower DCM section is dependent on the natural circulation and feed water mass flows.

The dynamic situation of the characteristic parameters within the TPL is governed by the generally (i.e., for both dry-out situations) valid balance of in- and outgoing water and steam mass flow contributions:

$$\Delta G_{ST} = G_{STE} - G_{STA} \text{ with } G_{STE} = G_{SRA} \text{ or } = G_{SRA} - G_{STAD} \qquad \text{(if } G_{STAD} > \text{ or } < 0)$$
$$\text{and } G_{STA} = G_{SMN} \text{ or } = G_{SMN} + G_{STAD} \qquad \text{(if } G_{STAD} < \text{ or } > 0) \qquad (49)$$
$$\Delta G_{WT} = G_{WTE} - G_{WTA} \text{ with } G_{WTE} = G_{WRA} \text{ or } = G_{WRA} - G_{WTAD} \qquad \text{(if } G_{WTAD} > \text{ or } < 0)$$
$$\text{and } G_{WTA} = G_{WTAD} \text{ or } = 0 \qquad \text{(if } G_{WTAD} > \text{ or } < 0) \qquad (50)$$

The two-phase flow mixture coming from the riser (and being separated by an 'ideal' separator into its phases) enters the TPL with the mass flows $G_{WTE}=G_{WRA}$ and $G_{STE}=G_{SRA}$ and their enthalpies $h_{WTE}=h_{WRA}$ and $h_{STE}=h_{SRA}$ (Note: h_{WTE} or h_{STE} can be also sub-cooled water or superheated steam). In case of DCM dry-out eventually also the term G_{STAD} = -G_{STDE} (if $G_{STDE} < 0$) coming from flashing has to be added to G_{STE}. Otherwise G_{STDE} is leaving the DCM.

The steam leaving the top plenum (with the steam mass flow G_{SMN} and the steam enthalpy h_{SMN}) will be governed by the main steam system (with a steam pipe line, a steam collector and a steam turbine) consisting of a number of relief and safety valves, isolation, bypass, turbine trip and control valves in a more-loop representation. by the situation within the steam collector (For more details see [5]). The steam collector can, in case of a multi-loop representation [1], in turn be influenced by an eventually non-symmetric perturbation of the different loops.

Only at non-dry-out situations water (with G_{WTAD} and the enthalpy $h_{WTAD}= h^{/}$) can leave the top plenum (in direction to the lower DCM section), dependent on the natural circulation and G_{FW}.

The ODE-s for the change in system pressure ($P_2=P_{2SYS}$), steam and water volumes (V_{ST} and V_{WT}) within the top plenum can then be derived from corresponding conservation eqs. for mass and energy and volume

$$\tfrac{d}{dt}\,[V_{ST}\,\rho^{//} + V_{WT}\,\rho^{/}] \qquad = \Delta G_{ST} + \Delta G_{WT} = \Delta G_{T} \qquad (51)$$

$$\tfrac{d}{dt}\,[V_{ST}\,\rho^{//}\,h^{//} + V_{WT}\,\rho^{/}h^{/}] - V_{TEX}\,\dot{P}_2 = h^{//}\,\Delta G_{ST} + h^{/}\Delta G_{WT} + Q_{TPL} \qquad (52)$$

Thereby a (power) term Q_{TPL} has been introduced which takes care of all contributions coming from superheating or sub-cooled conditions

$$Q_{TPL} = (h_{STE} - h^{//})\,G_{STE} - (h^{/} - h_{WTE})\,G_{WTE} \qquad (53)$$

This yields, if considering the interrelations given in eq.(48), finally the results

$$\dot{V}_{ST} = -\,[(1\text{-}C_{VP})\Delta G_T h^{//} + C_{VP}\,(\Delta G_{WT}\,h_{SW} - Q_{TPL})\,] / (C_{VST}\,\rho^{//}\,h^{//}) \qquad (L_{DCDRY} = 0 \text{ or } = 1) \qquad (54)$$

$$\dot{P}_2 = [\Delta G_T - \rho^{//}\,\dot{V}_{ST} - \rho^{/}\,\dot{V}_{WT}] / B_{VP} \qquad (L_{DCDRY} = 0 \text{ or } = 1) \qquad (55)$$

with the coefficients

$$B_{VP} = V_{WT}\rho^{/P} + V_{ST}\,\rho^{//P} \qquad (56)$$

$$C_{VP} = h^{//}\,B_{VP} / [V_{ST}\,(\rho^{//P}h^{//} + \rho^{//}h^{//P}) + V_{WT}\,(\rho^{/P}h^{/} + \rho^{/}h^{/P}) - V_{TEX}] \qquad (57)$$

$$C_{VST} = C_{VP}\text{-}1 + (1 - C_{VP}\,h^{/}/\,h^{//})(\rho^{/}/\rho^{//}) \text{ or } = C_{VP} - 1 \qquad (L_{DCDRY} = 0 \text{ or } 1) \qquad (58)$$

Since at dry-out conditions $\dot{V}_{ST} = -\dot{V}_{WDL}$ the term $\dot{V}_{ST}$ follows from eq.(47), the comparison with eq.(54) can then be used to determine ΔG_{ST}, G_{STE} and thus a better G_{SDTE} (flashing and condensation effects already included)

After the integration procedure the movement of the water level (z_{WL}) along the lower DCM section can then be determined from the knowledge of $V_{WDU} = V_{WT}$, or, in case of a DCM dryout, of V_{SDL}. Hence,

$z_{WL} = z_{WDU} + z_{DL}$ with z_{WDU} being a function of V_{WDU}, e.g., $z_{WDU} = V_{WDU}/A_{DU}$ (if L_{DCDRY} =0)

$z_{WL} = z_{DL} - V_{SDL}/A_{DL}$ (if L_{DCDRY} =1) (59)

4.6 NATURAL CIRCULATION ALONG SECONDARY LOOP

As explained in chapter 3.2, the time-derivative for the overall natural circulation mass flow, for example at the entrance to the HEX region (G_{2E}), follows from the facts that the sum of all pressure increase terms along the entire loop must be zero and that the time-derivatives along the entire loop can be assumed to be equal, i.e.,

$$\dot{G}_{2E} \cong d_{G2E} (\Delta P_{STH} - \Delta P_{ACC} + \Delta P_{XIN} + \Delta P_{FR} + \Delta P_Z) \text{ with } 1/d_{G2E} = z_{HX}/A_2 + z_R/A_R + z_{DU}/A_{DU} + z_{WDL}/A_{DL} \quad (60)$$

The HEX and riser pressure difference terms are provided by the coolant channel code package CCM. The corresponding terms for the DCM region have to be derived in a similar way. This means that at steady state the coefficients f_{DADD0} and f_{DFFMP0}, needed for the calculation of transient terms ΔP_{DADD} and ΔP_{DZFR}, are determining from the given overall pressure difference value $\Delta P_{DAE,0} = P_{DE,0} - P_{DA,0} = P_{2,0} - P_{2E,0}$. Knowing now the time-derivatives at each position of the loop then in the transient case the terms ΔP_{GHAE}, ΔP_{GRAE} and ΔP_{GDAE} and thus also the total pressure increase terms ΔP_{HAE}, ΔP_{RAE} and ΔP_{DAE} along the HEX, riser and DCM regions can be determined and, finally, in relation with the system pressure P_{2SYS}, their absolute pressure parameters.

5. DIGITAL CODE UTSG-3

The digital code UTSG-2 [5,8] has, both in a stand-alone manner or as a part of more complex transient codes (e.g., the thermal-hydraulic GRS system code ATHLET [2]), for many years been successfully applied by different research institutes for the simulation of the thermal- and hydrodynamic steady state and transient behaviour of the primary and secondary side of a vertical natural-circulation U-tube steam generator together with its main steam system and, when applied in a combination with the comprehensive modular code ATHLET, the multi-loop construction of a PWR (e.g. [1]). To establish the theoretical background of the UTSG code the primary and secondary heat exchange regions and the riser section had to be subdivided into a number of nodes. At the advanced UTSG-3 version the corresponding nodal time-derivatives and characteristic parameters of this three channel regions are now, as already pointed out, automatically provided by the coolant channel module CCM. The other elements of the UTSG-2 code will (as presented before) be updated to the newest demands on such a code.

Hence, starting from the previous digital code UTSG-2 and, based on the theory presented above by taking into account the changes in the theoretical background, the advanced digital code version UTSG-3 could be established. The non-linear set of steady state algebraic eqs. will be solved in a recursive way yielding the parameters being needed as starting values for the transient calculations. The integration of the set of ODE-s of first order (together with the corresponding constitutive eqs.) yields finally the wanted characteristic parameters.

<u>Needed input data to the code:</u>

Operation conditions, geometry data, characteristic data of the valves, basic points of polygons describing the outside-perturbation signals (if they are not provided as BOP signals described by the GCSM procedure), and option values governing the wanted output.

<u>Possible perturbations of the system from outside:</u>

- Inlet water temperature (T_{1E}), mass flow (G_{1E}) and pressure (P_{1E}) on the primary side. If UTSG-3 is used in combination with a code for NPP-s, e.g. ATHLET [2], these parameters are then provided by the main code, the resulting outlet parameters being transferred again back to the main code.
- Steam mass flow (G_{SMN}) into the main steam system caused by the feedback from the turbine and actions from different valves.
- Feedwater mass flow (G_{FW}) and temperature (T_{FW}) (or enthalpy h_{FW}) as a result of different feedwater actions.
- If used in combination with balance-of-plant (BOP) actions the perturbation parameters are provided with the corresponding BOP signals.

<u>Final system of state and differential eqs.:</u>

If simulating the heat transfer through a tube wall either by a single layer (N_{RT}=1) or describing it by overall heat transfer coefficients (N_{RT}=0), subdividing the HEX region axially into maximal 7 nodes (N_{ZHX}=7) and the riser into maximal 5 nodes (N_T=5) and taking into account a moving boiling and eventually also superheating boundary (z_{2B} and z_{2SPH}) the theoretical model consists of a set of maximal 53 (or 38, if choosing N_{RT}=0) non-linear ODE-s of 1-st order for the variables

$$T_{1M}(i_M,k),\ T_{2M}(i_M),\ (T_{TWE} \text{ and } T_{TWM}(i,k) \text{ if } N_{RT}=1), \alpha_{2M}(i_M),\ \alpha_{RM}(i_{RM}),\ z_{2B},\ z_{2SPH}, P_2, V_{WT}, G_{2E} \qquad (61)$$

$$(\text{with } i=1,..,N_{ZHX} \leq 7,\ i_M=i-\tfrac{1}{2},\ i_{RM}=1,..,N_{RT} \leq 5,\ k=1,2\text{: parallel and counter flow})$$

and a number of state eqs. (total and nodal power terms, turbine power, nodal heat power fluxes, total and nodal steam mass flow, steam mass flow into main steam system, i.e. into steam relief and safety valves and into steam turbine, total and nodal pressure drops, steam volumes along HEX, riser, top plenum and DCM, dry-out boundary and movement of the enthalpy front along the DCM, etc.)

Solution method.:

Solving now this set of ODE-s (together with the corresponding constitutive contributions) directly (together with the momentum balance contributions) has the effect that by using an explicit integration procedure the computation has to be performed because of the very fast pressure wave propagation with very small time steps ('stiff equation system'). Hence, high CPU values can be expected. Using an implicit-explicit integration procedure (see, e.g. the routine FEBE, Hofer [16]). this situation can partially be improved, the computing time being, however, still very disagreeable. As already pointed out, this time-consuming procedure could be circumvented by an approach which takes advantage of the fact that under the most circumstances the mass and energy balance eqs. can be treated separately from momentum balance without loosing essentially on accuracy. (For more details see Hoeld [12]).

The resulting set of eqs. can be combined with other sets of ODE-s and algebraic eqs. coming from additional parts of a complex model, e.g., from other basic channels which represent different thermal-hydraulic objects within an entire closed loop or a system of parallel channels, from heat transfer or nuclear kinetics considerations etc.

6. UTSG-3 TEST CALCULATION

To demonstrate the properties and validity of the advanced code version UTSG-3 a number of ATHLET/UTSG-2 calculations (by taking them as benchmarks) have been successfully post-calculated by the UTSG-3 code, see for example [3,4], thus contributing to the verification process of the newest code version and also of the coolant channel module CCM. The corresponding UTSG-3 calculations had then obviously to be based on the same input data set, despite of the fact that the philosophy about the balance-of-plant (BOP) actions in a NPP may have partially changed.

As an example the process sequence of an UTSG-3 stand-alone calculation of the case 'loss of main feedwater at a PWR NPP (at nominal conditions) with turbine trip and scram' will be presented. Some selected characteristic parameters are plotted in the figs.2A-2H. The calculation is based on an ATHLET/UTSG-2 calculation (Hoeld [9]) which has been performed in connection with the establishment of an general standard input data set for the ATHLET/UTSG-2 code, using the general control simulation language GCSM of ATHLET for the description of BOP actions.

The following transient behaviour of the most characteristic parameters can be observed:

- As initiating event the switch-off of all 2 (plus 1 reserve) main feedwater pumps had been assumed.
- The abrupt coast-down in main feedwater (falling within 4 s to zero, fig.2B) created the signal 'low-feedwater-flow' which in turn caused a number of BOP actions.
- In the first phase of the transient a signal for reactor power limitation (RELEB) is initiated, causing due to an adequate drop of control rods a reduction of the nuclear kinetic and thermal reactor power (and thus HEX power, fig.2E) to about 50%, resulting in a corresponding reduction of the primary HEX entrance temperature (fig.2C) and primary system pressure (fig.2C). The primary coolant mass flow remains almost unchanged (fig.2A).
- Simultaneously, a decrease in steam turbine power to about 55 % (fig.2E) is initiated by reducing, due to the 'maximum pressure control' procedure, in a controlled way the steam mass flow through the turbine-control valve (fig. 2B) yielding consequently to an (at the begin very steep) increase in secondary system pressure (fig.2D) and thus saturation temperature (fig.2C). The corresponding

pressure set point curve is a function of the part-load diagram, is limited by a maximal increase rate of 2.0 MPa/min after having reached 7.7 MPa and is kept below 8.0 MPa.

- The temperature at DCM entrance is a mixture of saturated water coming from the riser/separator and the injected feedwater (with a temperature of 218 ^{0}C). Hence, the switch-off of the main feedwater pumps and increase in saturation temperature yielded to an abrupt increase in DCM entrance temperature, reaching saturation conditions. Its temperature (or enthalpy) front (fig.2C) is moving (due to the natural circulation) along the DCM until it reaches (at the begin of the transient after about 8 s) the HEX entrance.
- The decrease in feedwater flow causes also a decrease in sub-cooling power at feedwater and HEX entrance (fig.2E) and thus also a decrease in boiling boundary (fig.2G).
- From fig.2F the transient behaviour of the corresponding local and total HEX and riser steam void fractions can be seen.
- Due to the deficit in incoming (feedwater) and outgoing (steam) masses the downcomer starts to dry-out (see water level in fig.2G), crossing at about 45 s the position of feedwater nozzle. This means that in the model it had to be taken into account that feedwater, being injected after this time point, will act partially with the increasing steam content of the lower DCM section (i.e., will condense a part of it). The falling DCM water level causes at about 10.2 m (with a delay of about 10 s) the activation of the auxiliary FW pumps (fig.2B) and at 9.0 m a turbine trip (TUTRI) and a reactor scram, switching down the turbine and reactor power (fig.2E) (leaving only the power decay heating term), withdrawing, however, power from the primary loop due to steam removal through the bypass valve.
- Steam mass flow through the bypass valve (being a part of the main steam system, fig.2B) is governed by the ‘partial cool-down procedure’, acting at first in combination with the maximum pressure control. It should be noted that the difference between the secondary system pressure and the pressure at channel entrance (i.e. HEX entrance or DCM outlet) is an important basis for parallel channel assemblies. This difference stays in the first phase of the transient almost unchanged as long as no essential changes in the DCM (temperature, dry-out) appear. Despite of the turbine trip steam is now removed by the bypass valves. Hence the pressure difference between HEX outlet and steam collector entrance is still present but diminishing. This parameter plays an important role for the determination of different in- and outflows into the steam collector in case of a multi-loop application of the ATHLET/UTSG code [1].
- The natural-circulation flow (e.g., at secondary HEX, fig.2A) is continuously decreasing, stagnant flow will, however, not be reached because of steam removal by the bypass valves.
- UTSG calculations have always been accompanied with strong quality control measures. The control of the actual masses being present at any time in the HEX, riser/separator, top plenum and downcomer regions with masses which should be expected due to the balance of in- and outgoing masses can give valuable hints to the quality of the calculations and thus the validity of the code. In fig.2H the total mass content along the secondary loop is split into its contributions from different regions and shows excellent agreement. This procedure has been a most valuable tool during the construction of the theoretical model.

The calculations showed, as expected, no noticeable differences in comparison with calculations with the code ATHLET/UTSG-2. That means, that the broad experience with the almost 20 years of UTSG application and the many verification runs with the ATHLET/UTSG-2 code combination could be transferred directly to this code and the module CCM. (See, for example, the case of a loss of one out of four main coolant pumps (see Bencik et al. [1]) within the series of posttest-calculations of start-up tests of a German PWR NPP).

CONCLUSIONS

The presented model fulfils the aim of the project to construct a reliable module which is more flexible with respect to the existing codes, can easier be handled (see the possibility of an automatic subdivision of a BC into SC-s) and has a much higher potential for further applications (e.g, if using in for parallel channel assemblies etc.).

The procedure PAX and the drift flux correlation package are a central part of the theoretical model and module CCM and thus also of the advanced code version UTSG-3. The approximation procedure has to provide the model, apart from the above feature, also with gradients of the resulting approximation function needed for the determination of the time-derivatives of coolant temperature and void fraction and governs the movement of SC (= boiling or mixture) boundaries across BC node boundaries whereas an adequate drift-flux correlation package states in which way co- and countercurrent flow in vertical, inclined or even horizontal coolant channels can be treated. In both cases it has, due to the availability of different input parameters, be distinguished between a steady state or a transient case and both methods had to be submitted to a thorough test phase outside of the code before being implemented into the code.

On hand of its application within the UTSG-3 concept it could be demonstrated that the presented theoretical drift-flux based thermal-hydraulic coolant channel model and the resulting module CCM can be a valuable element for the construction of complex assemblies of pipes and junctions. Simultaneously, it could be build a bridge to the verification status of the widely used UTSG-2 code. Experiences with other application cases will help to mature the present CCM module. As it turned-out the method to discretize PDE-s and connect the resulting mean and boundary nodal functions by means of the PAX procedure can be of general interest for similar projects too.

The knowledge of some characteristic parameters of a U-tube steam generator allows also to establish some normalization procedures in order adjust the code to the real situation. Taking the steady state heat power as a nominal power helps to compensate the uncertainties in the determination of the heat transfer coefficients and the exact number of the U-tubes, the steady state natural circulation mass flow allows to adjust the pressure decrease over the entire secondary loop (see chapters 3.2 and 4.6) and, finally, from the given steady state sub-cooled power (being dependent on total power and pressure) an overestimation in feedwater entrance enthalpy or mass flow parameters can be avoided (chapter 4.4).

Several measures been installed to control during a computational run continuously the quality of the calculated results. By determining (parallel to the normal calculation) a number of auxiliary parameters should then allow to judge the quality of the run.. Besides a high number of test-prints and the presentation of pseudo-stationary characteristic parameters (boiling boundary, heat content values along the primary and secondary loop) it turned out to be a very important tool the comparison of the actual steam and water mass contents within the HEX, the riser and the DCM region with the mass contents as being determined from the balance between the in- and outgoing mass flows. Already small deviations can give a hint that some uncertainties in the model or the realization in the code exist (see fig. 2H).

NOMENCLATURE

A_{Nn}, A_{Mn}	m^2	Cross sectional area (node boundary, mean value)
C	–	Dimensionless constant
C_0	–	Phase distribution parameter
d_{HW}	m	Hydraulic diameter
f_{ADD0}, f_{FMP0}	–	Additive and multiplicative friction coefficients
G ,G_S,G_W	kg/s	Total, steam and water mass flow
h, h^P , c_P= h^T	J/kg, m³/kg, J/(kgC)	Specific enthalpy and their partial derivatives with respect to pressure and temperature (= specific heat)
K_{EYBC}	-	Characteristic key number of each BC
L_{FTYPE}= 0,1 or 2	-	SC with saturated water/steam mixture, sub- cooled water or superheated steam
N_{BT}, N_{CT}, N_{RT},	-	Total number of BC or SC nodes, of radial U-tube layers,

N_{TUBES}, N_{ZHX}		of U-tubes and of HEX nodes
P, $\Delta P = P_A - P_E$	$Pa=Ws/m^3 = kg/(ms^2)$	Pressure and pressure difference (in flow direction)
Q_k, Q_{NOM}, ΔQ_{kn}	W	Total, nominal and nodal power into (!!) channel k
q_{SFkn}	W/m^2	Local nodal heat flux in- and out of all U-tubes (k=1,2),
$q_{TWkn}=q_{SFkn}U_{kn}/A_{kn} = q_{SFkn} A_{SFkn}/V_{kn}$	W/m^3	Local nodal power density into and out of all tubes
r, $\Delta r = r_2 - r_1$	m	Radial U-tube variable and thickness
T, t	C, s	Temperature, time
U_{TW}	m	(Heated) perimeter of a single U-tube
$V_{Bn}=0.5(A_{Bn}+A_{Bn-1})\Delta z$	m^3	Nodal BC volume
v	m/s	Velocity
$X=G_S/G$	-	Steam quality
z, $\Delta z_{Nn}=z_{Nn}-z_{Nn-1}$	m	Local variable, SC node length ($z_{Nn-1}=z_{CE}$ at n=0)
z_{BA}, z_{BE}, z_{CA}, z_{CE},	m	BC and SC outlet and entrance positions
α		Void fraction
α_{SF}	$W/(m^2C)$	Heat transfer coefficient along tube wall
ε_{DPZ}	–	Coefficient for choice of additional friction
ε_{QTW}	–	Correction factor with respect to $Q_{NOM,0}$
ε_{TW}	m	Absolute roughness of tube wall (ε_{TW}/d_{HW}=rel.value)
λ_{TW}	W/(mC)	Heat conductivity along tube wall
ρ, ρ^P , ρ^T	kg/m^3, kg/J, $kg/(m^3C)$	Density and their partial derivatives with respect to (system) pressure and temperature
∂		Partial derivative

Subscripts

0, 0 (=E)	Steady state or entrance to the HEX region (U-tubes)
A, E	Outlet, entrance
B, S	Basic or subchannel
A,F,Z,S,X	Acceleration, direct and additional friction, static head or external pressure difference (if in connection with ΔP)
HEX, R,T,MN, DU,DL,TAD	Heat exchanger (evaporator), riser/separator, top plenum, main steam system, upper and lower DCM part, out of TPL to DCM
k=1,2	Primary and secondary loop (containing all tubes)
Mn, BMk	Mean values over SC or BC nodes
Nn, BNk	SC or BC node boundaries
D	Drift
R	Relative
S, W	Steam, water
P, T	Derivative at constant pressure or temperature
TW, TWk	Tube wall and primary or secondary tube wall surface of a single U-tube

Superscripts

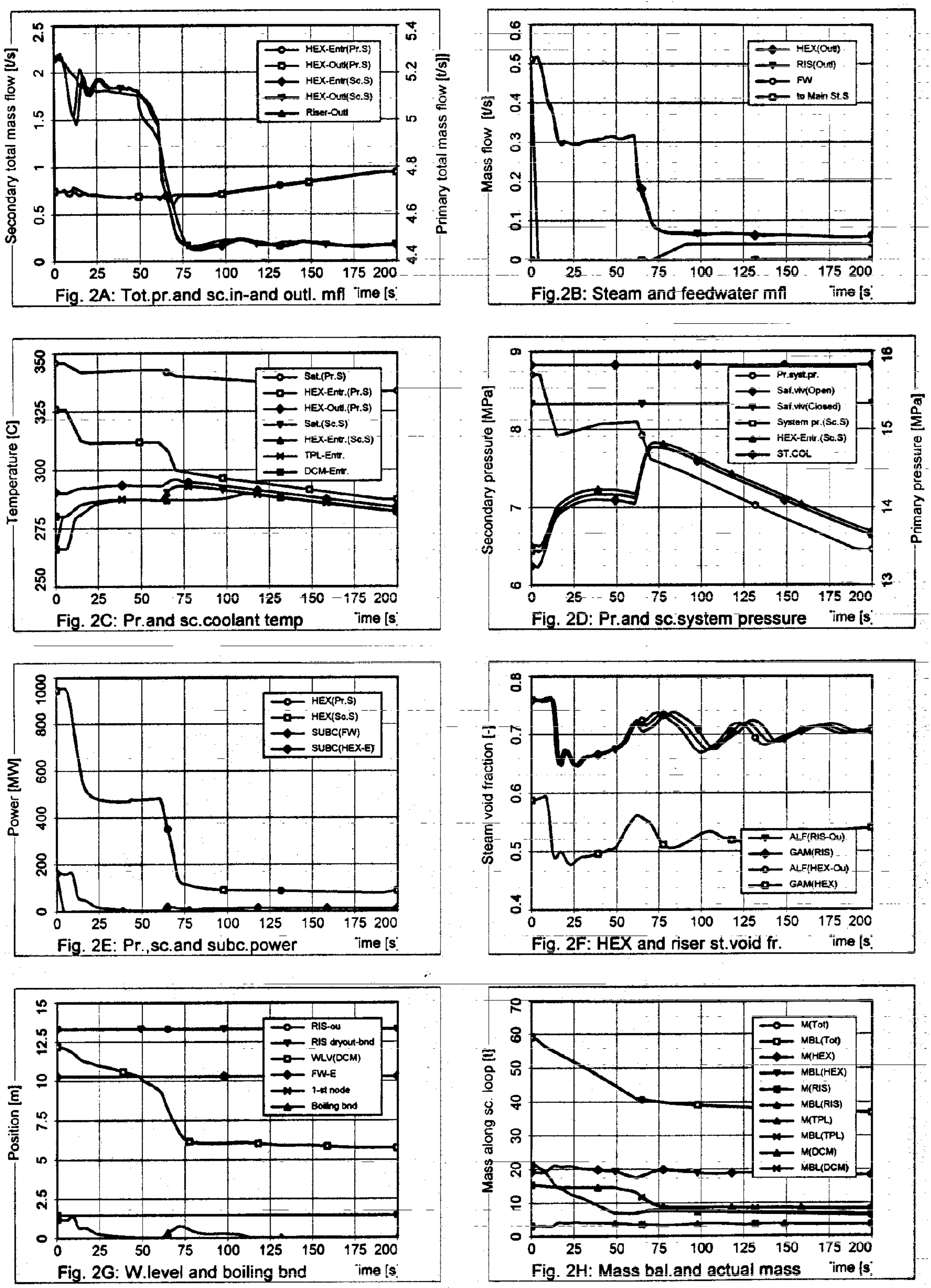
/, //	Saturated water or steam
P, T	Partial derivatives with respect to P or T
(G_S), (α), (r)	Partial derivatives with respect to G_S, α or r

REFERENCES

1. V. Bencik, A.Hoeld, " Steam collector and main steam system in a multiloop PWR NPP representation", *Proc. of 1993 Simulation* MultiConf (Simulators Intern. X), March 29-April 1 1993, Arlington/Washington, Simul.Ser. vol.25(4), 223-229, 1993
2. J. Burwell et al.. "The thermohydr. code ATHLET for analysis of PWR and SWR systems", *Proc.of 4-th* Int.Top. Meeting on Nucl.R.Th.-Hydraulics,NURETH–4, Karlsruhe, Oct.10-13, 1989
3. L.Haar, J.S.Gallagher, G.S.,Kell, U.Grigull,U, "*NBS/NRC* Wasserdampftafeln", Springer-Verlag, 1988 (See also Transl. '*NBS/NRC Steam Tables*'. Hemisphere Publ. Co, 1984).
4. C.Hirsch, "*Numerical computation of internal and external flows", vol. 2, 'Computational* Methods for Inviscid and Viscous Flows*"*, J.Wiley&Sons, Nov.1991.
5. A.Hoeld, " A theoretical model for the calculation of large transients in nuclear natural circulation U-tube steam generators (Digital code UTSG)", *Nucl. Eng. Des*., 47, 1 (1978)
6. A.Hoeld, "HETRAC: A heat transfer coefficients package for NPP-s at normal operational conditions". GRS-A-1446 (May 1988)
7. A.Hoeld, "Calculation of the time behavior of a PWR NPP during a loss of a feedwater ATWS case". *Proc.of* the Int. ENS/ANS Conference on *'Thermal* Reactor Safety*' ('NUCSAFE 88'),* 2-7 Oct.1988, Avignon, France. Proc.1037-1047.
8. A.Hoeld, "UTSG-2. A theoretical model describing the transient behaviour of a PWR natural circulation U-tube steam generator", Nuclear Techn. 90, 98-118, April 1990.
9. A.Hoeld, "The code ATHLET as a valuable tool for the analysis of transients with or without scram in PWR-s", *Proc SCS Eastern* Mtconf., 23-26 April, 1990, Nashville, Tennessee, 8-74.
10. A.Hoeld,A., "Thermodynamisches Stoffwertpaket MPPWS für Wasser und Dampf. GRS, Technische Notiz, HOD-TN-03, August 1994
11. A.Hoeld, "The drift-flux based thermal-hydraulic coolant channel model and its realization in the code package CCM"*,* 8-th Int.Conf.on Nucl.Eng.,ICONE-8049,April 2-6, 2000, Baltimore, USA.
12. A.Hoeld, "The module CCM for the simulation of the thermal-hydraulic situation within a coolant channel".*Int.Conf.'Nuclear* Enrg in Central Europe 2000, September 11-14, 2000, Bled, Slovenia.
13. A.Hoeld, "The drift-flux correlation package MDS".*9-th* Int.Conf.on Nucl.Eng.,ICONE-789,April 8-12,2001,Nice, France.
14. A.Hoeld, "The consideration of entrainment in the drift-flux correlation package MDS", *10-th* Int.Conf.on Nucl.Eng.,ICONE10-22692,April 14-18,2002, Arlington/Washington, USA
15. A.Hoeld, E.Jakubowska, J.E.Miro, H.G. Sonnenburg, "A comparative study on slip and drift-flux correlations".GRS-A-1879 (Jan.1992)
16. E.Hofer, "An A(α)-stable variable order ODE-solver", *Topical Meeting on Advances in Math.* Methods, Munich, April 1981
17. M.Ishii, M.A.Grolmes, "Inception Criteria for Droplet Entrainment in Two-Phase Concurrent Film Flow". *A.I.Ch.E. Journal* 21 (2), 308-318, March 1975.
18. M.Ishii,M., K.Mishima, "Study of Two-fluid Model and Interfacial Area", *NUREG/CR-1873* (ANL-80-111), Dec. 1980.
19. M.Ishii, K.Mishima,K., "Droplet Entrainment Correlation in Annular Two-phase Flow", *Int.J.Heat* Mass Transfer 32(10),1835-1846,1989.
20. R.C. Martinelli,R.C.,Nelson,D.B.,1948. "Prediction of pressure drop during forced-circulation of boiling water",*Trans. ASME* 70, 695 (1948).
21. N.L.F. Moody, "Friction factors for pipe flow", *Trans. ASME,* 66, 671 (1948), (See also *VDI-*W*ärmeatlas, 7.Auflage*, 1994, VDI - Verlag).
22. H.G. Sonnenburg, „Full-Range Drift-Flux Model based on the Combination of Drift-Flux Theory with Envelope Theory", *Int. NURETH-4* Conf., Oct. 10-13, 1989, Karlsruhe, pp. 1003-1009.
23. E.Schmidt,U.Grigull (ed.), "*Properties of water and steam in SI-units*", Springer-V., 1982

UTSG-3 (CCM) STAND-ALONE CALCULATION

Loss of main feedw(but EMFW),TUTRI/SCRAM,max.pr.c,prt.coold.



Figs. 2A-2H