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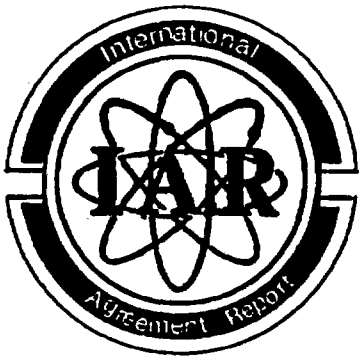

International Agreement Report

Developmental Assessment of RELAP5/MOD3.1 with Separate-Effect and Integral Test Experiments: Model Changes and Options

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ABSTRACT

A summary of modifications and options introduced in RELAP5/MOD3.1 (R5M3.1) is presented and is shown that the predicting capabilities of the modified version of the code are greatly improved, while the general philosophy we followed in arriving at these modifications is also outlined. These changes which are the same ones we implemented in the past in the version 7j of the code, include 2 different heat transfer packages (one of them activated during reflooding), modification of the low mass-flux Groeneveld CHF look-up table and of the dispersed flow interfacial area (and shear) as well as of the criterion for transition into and out from this regime, almost complete elimination of the under-relaxation schemes of the interfacial closure coefficients etc. The modified R5M3.1 code is assessed against a number of separate-effect and integral test experiments and in contrast to the frozen version, is shown to result in physically sound predictions which are much closer to the measurements, while almost all the predicted variables are free of unphysical spurious oscillations. The modifications introduced solve a number of problems associated with the frozen version of the code and result in a version which can be confidently used both for SB-LOCA and LB-LOCA analyses.

CONTENTS

1 INTRODUCTION

During the last decade, under the ICAP (International Code Assessment and Applications Program) and lately the CAMP (Code Assessment and Maintenance Program), there has been an International effort directed towards assessing and improving different thermal-hydraulics reactor transient analysis codes. The aim of this effort is to end up with transient analysis computer codes for PWRs and BWRs (and also for advanced reactors) which can be used for licensing as well as for analyzing different reactor transients. In this effort, the data from a large number of separate-effect and integral test experiments has been utilized for assessing either individual physical models in these codes, or the performance of the codes as integral entities. Clearly, in many situations of interest, the fine details of a physical model are not very important to the over-all predicting capability of the code, and the latter may be determined by other factors like node size, interpolation schemes, finite-differencing scheme etc; as a matter of fact, the line separating the physically faithful and detailed modeling from the practically useful one within the framework of a code is always rather vague and mostly difficult to define and quantify. Nevertheless, the employment of physically sound and realistic models is a necessary requirement for having a **physically robust code** (as opposed to **numerically robust**; though, the two items can sometimes be closely inter-related !) which can be technically defended. Though, the improvement efforts should not only be restricted to the implementation of physically defendable models, but also to other "numerics" related areas like interpolation schemes, old-time averaging (under-relaxation) used to smooth the interfacial closure coefficients between two successive time-steps etc.

During the last few years, extensive assessment [1-4] of the transient thermal hydraulics code RELAP5/MOD3 versions **5m5** and **7j** [5,6] (henceforth to be referred to as R5M3) has been pursued and a number of deficiencies (particularly in relation to physical modeling) and problem areas have been identified, the most striking one being the inability of the code to even remotely capture the physics of reflooding [1,2,4], something which makes the code totally inadequate for LB-LOCA calculations. The main reason for this is the inappropriateness of the post-CHF wall-to-liquid heat transfer logic of the code; in fact, we have shown in the past [1,2] that this logic can be problematic, independently of whether the physical process is reflooding or not. Saying this, we should clearly state at this point that since the policy of the NRC has been during the last few years that R5M3 should not be used for LB-LOCAs (TRAC-PF1 was supposed to be the code for analyzing these transients), it is inevitable that neither a systematic effort was spent by the code developers to implement in the code models which would be appropriate for reflooding, nor extensive studies and assessments of the present capabilities of the code in this area were undertaken. Nevertheless, a number of CAMP member countries using the code have been rightly insisting that R5M3 should be able to model reflooding and that one should be able to use it for analyzing hypothetical LB-LOCA transients.

As a result of this situation, a broad developmental program has been undertaken, and a number of improvements covering a rather extensive area have been made [1,2]. Here, we should add that a number of workers from different countries have also demonstrated that as far as LB-LOCAs are concerned, R5M3 is suffering from a number of shortcomings, the result of this being that they abandoned using the code in favour of RELAP5/MOD2.5, whose ability to model reflooding although by no means perfect, was nevertheless much better than the one of R5M3. This is clearly not an acceptable situation since one could actually try to improve R5M3 by implementing models which would enhance and improve the capabilities of the code in modeling a much wider range of transients rather than reversing back to its predecessor which, admittedly, did perform better than R5M3 for some cases. Though, it is now becoming clear that before moving into applications of the code to advanced reactors, a number of problems with which the code is plagued should be tackled and, if possible, resolved in a clear and satisfactory manner. Although as far as advanced reactors are concerned, these problems are mainly related to modeling heat transfer in the presence of non-condensible rather than the post-CHF heat transfer, ideally, one should have a code whose predictions are reliable independently of whether one analyzes transients in conventional or advanced reactors.

The improvement of the predicting capabilities of a code (which, let us not forget, is supposed to be able to capture a wide variety of phenomena in a very complex system) and the subsequent assessment of a resulting "new version" is not at all a straight-forward or easy task and a systematic and coordinated effort is required if this is to be accomplished in an efficient and scientifically defendable fashion. In particular, it is not only the individual physical models that have to be sound and realistic, but the way they are connected with each other over transition regimes must be consistent and free of discontinuities which, in turn, may excite unwanted oscillations which can adversely affect the final predictions, to the extent that they are no longer representative of the physical models in the code. Hence, there is a host of different problems which have to be addressed and resolved during the development and assessment of a particular code version, some of them (and sometimes, most of them) not being directly related to the way that the code is attempting to model a particular physical process. As far as R5M3.1 is concerned, our task is simplified and our assessment efforts (at least as far as the deficiencies in the heat-transfer related models are concerned) are actually guided by the corresponding efforts we made for improving the version 7j of the code, the reason being that no physical models have been changed between the two versions.

In our general effort to improve R5M3, the following steps were taken when we were developing and testing our modifications:

(a) Assess specific models of the fixed code version by analyzing rather simple separate-effect tests in which these models are dominant. As an example, a boil-off test under no-flow conditions would reveal the appropriateness (or not) of the bubbly/slug interfacial shear model while a bundle reflooding

experiment would test the appropriateness of the post-CHF wall-to-liquid heat transfer, dispersed flow interfacial shear (and area) etc. Though, in the latter example, the task is really more complicated since there will be a number of competing effects influencing the final code predictions of interest.

(b) Whenever there are large deviations from the measurements (eg in the rod surface temperature (RST) histories) and it is clear that the code predictions are **qualitatively** wrong, try to understand the reason(s) that this is happening and propose a solution to the problem. We believe that should this be the case, it makes no sense whatsoever to try to use the code for a reactor calculation; any code predictions would be of no use and any conclusions drawn would be unsubstantiated. Furthermore, it could be that although certain variables are "correctly" predicted, this is clearly coincidental and there are probably large deviations in other variables. Generally, the reason of the deviations may be due to the inappropriateness of a specific physical model but it is also possible (and this is usually the case) that the deviations are due to more than one reasons; in this case, a greater effort is required to understand and resolve the problem and the action to be taken may be more complex.

(c) Having changed the specific model (or models), re-analyze with the new version the series of simple tests and note any resulting improvement. Subsequently, perform with this version integral test calculations (eg LOFT) for which data exists as well as possible hypothetical transients (eg different LB-LOCAs) and examine the code's performance by comparing the predictions of the new and the fixed version. In most cases, improved code performance for a separate-effect test will, one way or an other, also show up in a large system calculation.

(d) Should additional modifications in relation to other physical models are introduced and yet a different code version is created, it is possible that the good predictions of the integral test transients evaluated with the previous code version are adversely affected by the new changes. Hence, the previously evaluated transients should be re-analyzed with the latest code version. Should the new model changes are physical and realistic and not "contradictory" to the other models, the latest code version should also produce results equally good (ideally, even better) as the previous one and this has indeed been the case for almost all modifications we have introduced in the code. Should this not be the case, one should try to understand the reason (or reasons) why this is not so and try to correct it; this is a rather complicated but inescapable step in our approach. In any case, one should certainly not fall into the trap of avoiding making a physically sound change just because the results of other "good" calculations are affected: Sooner or later, one will have to pay the penalty of such an approach.

(e) In our effort of improving the code, one may frequently come across problems and difficulties arising not from the actual physical models, but from the

interpolations used between two different correlations, the error bounds set for advancing the solution, the old-time averaging schemes of the interfacial constitutive coefficients etc. These are the most difficult and challenging problems to deal with and sometimes they even require some compromise between the physics and the "numerics". Though, we have noted a number of cases in which, for example, the implementation of a more physically sound model may result in suppression of unphysical oscillations.

One must certainly admit that the aforementioned procedure can be rather complex and time-consuming. Though, we believe that it is the only realistic path to follow if one is to end up with a code which is reliable, physically robust, easily defendable and can predict complicated transients. Additionally, we strongly believe that code users reporting on generic code deficiencies, should be in a position to further investigate the reason(s) for it and if possible, suggest ways of resolving the problems. This may not always be a straight-forward task since a particular code deficiency may require changes in a number of models; this brings us back to the above tedious **step-by-step** procedure.

It is the aim of this work to show that with some relatively modest effort and by following the aforementioned approach which we have been closely following in the past, one can successfully solve a number of important problems associated with the code, most of which are of a rather **generic nature**, have been frequently reported in the literature, are the main reasons against employing R5M3 for LB-LOCA calculations and, most important, are shared by all versions of R5M3.

In this work, we shall summarize the model changes and code modifications we implemented in R5M3.1 (henceforth to be referred to as frozen version), trying at the same time to explain the reason(s) why it was necessary to make them. Most of these reasons we have already discussed in the past and although whenever possible, we shall refer the reader to the literature, since we want to make this work self-contained, we shall spend some time discussing the different problems and model-changes to some detail. Most of the modifications to be reported here are **identical** to the ones we have implemented in the version 7j of R5M3 and have been extensively discussed in a number of previous works and CAMP presentations; though, there is a small number of new ones which are not actually related to new physical models. These changes which we shall discuss in some detail in section 2, include the implementation of two different wall-to-liquid heat transfer packages and complete removal of the package of the frozen version (which has been shown to be unphysical and problematic, due to the peculiar behaviour of the Chen transition/film boiling heat transfer correlation as a function of the total mass-flux), modification of the low mass-flux limit of the Groeneveld CHF look-up table, modification of the minimum allowed droplet diameter (and hence, the interfacial shear) in dispersed flow and the criterion for transition to this regime, re-activation of the modified Bestion bubbly/slug interfacial shear correlation for low pressures and almost complete elimination of the under-relaxation schemes of the interfacial closure coefficients. In particular, a special wall-to-liquid heat

transfer package is implemented and activated when reflooding is being modelled. In this package, the wall-to-liquid heat transfer coefficient is expressed as a function of the distance from the bottom QF, and as an option, the heat transfer as a function of the distance from the top QF can also be computed, resulting in a downwards propagating top QF. Though, the constants appearing in this latter option are still not set with confidence, awaiting comparison with experimental data. In section 3, we shall compare the predictions of the modified code with the ones of the frozen version as well as with measurements from a number of separate-effect and integral test experiments and wherever possible, we shall briefly comment on the origin of the differences in predictions between the two code versions. The tests we shall be analyzing both with the frozen and modified versions of the code are bottom-flooding experiments in the NEPTUN half-length heater rod bundle at PSI and at the FLECHT-SEASET heater full-length bundle, the Oak Ridge THTF low power film boiling experiment Nr. 3.08.6C, the LOFT LP-LB-1 test (for which case we shall also demonstrate the effect of changing the annular flow interfacial shear model everywhere except the core) and the LOBI SB-LOCA BL34 experiment. Additionally, for the sake of completeness, we shall analyze a hypothetical LB-LOCA in a commercial two-loop 1130 MWth PWR.

Although this work represents a rather extensive summary of work pursued during the last 3 - 4 years, we are in no way claiming that all problems associated with this code are solved, that our proposed solutions are "unique" or that there are no other areas than the ones addressed in this work that attention is required. Additionally, a much wider assessment basis of our modified version of the code is needed; though, the physically sound and consistent results obtained up to now for the cases analyzed show that provided the right physical models are implemented in the code, R5M3 can confidently be used both for LB-LOCA and SB-LOCA analyses. Hence, with the exception of perhaps the ability to model with some codes more geometrical details than with others due to eg their 3-dimensional capabilities (capabilities which one could in principle dispute due to the large volumes one usually employs for the 3-dimensional components), the classification of different codes into "SB-LOCA codes" and "LB-LOCA codes" is, to say the least, conceptually erroneous and misleading and should be abandoned: **The actual physical models are invariant to the code in which they are implemented and apart from possible geometrical limitations, all codes should be able to model the same phenomena and transients.** Finally, we shall conclude in section 4 with some recommendations.

2 MODIFICATIONS AND OPTIONS IN R5M3.1

We shall now very briefly outline the code modifications and model changes we have implemented in R5M3.1; most of them are identical to the ones we have implemented in the version 7j of the code. These are:

(a) The modified Bestion interfacial shear correlation for bubbly/slug flow was re-activated and used for pressures less than 10 bar. It reads [7-9]

$$f_i = \frac{65\,\alpha\,(1-\alpha)^3\,\rho_g}{D_H}\,(C_1 V_g - C_o V_l)^2 \qquad (2.1)$$

where ρ_g and D_H are the steam density and hydraulic diameter, respectively, while the value of the distribution parameter C_o is set equal to 1.2. For pressures greater than 20 bar, the EPRI interfacial shear correlation [5] already in the code is used, and a linear interpolation is used between these two pressures. There is a number of reasons for re-activating the modified Bestion correlation, one of them being that as has already been shown in Ref. 5, it results in very good predictions for low pressures. Additionally, recent work has shown that the highly complex EPRI correlation, due to its dependence on a large number of local variables which are usually oscillating in a transient calculation, may induce a number of undesirable side-problems, one of them being an unacceptably high mass-error. Hence, although the EPRI correlation (which is only one part of the very complex bubbly/slug interfacial shear package of the frozen version of R5M3), due to the fact that it is actually a fit to a large number of data points, is bound to result, in general, in better predictions than other correlations, we would generally recommend employing simpler bubbly/slug interfacial shear correlations like the aforementioned modified Bestion correlation for bundles and for pipes, the one used in codes like TRAC-BF1 [10] (see item **(k)** below).

(b) In order to avoid vapour de-superheating (the interfacial heat transfer from the vapour to the droplets is proportional to the interfacial area per unit volume S_{dr} and hence, inversely proportional to the "average" droplet diameter D_o), the average droplet diameter D'_o defined via the Weber number (We) was increased in R5M3 by the code developers by increasing (We) to 12 (in RELAP5/MOD2.5, it had the value of 3). Additionally, the minimum allowed droplet diameter D'_m in the post-dryout regime is defined (ad-hoc) as a function of the pressure p as follows:

$$\left.\begin{aligned} &D'_m = 0.0025 \ \text{ for } \tilde{p} \le 0.025, \\ &D'_m = 0.0025 - \\ &4.444\,(0.0025 - 0.0002)\,(\tilde{p} - 0.025), \\ &\text{for } 0.025 < \tilde{p} < 0.25, \\ &D'_m = 0.0002 \ \text{ for } \tilde{p} \ge 0.25 \end{aligned}\right\} \qquad (2.2)$$

$\tilde{p} = p/p_{cr}$, p is the pressure and p_{cr} is the critical pressure. The final average droplet diameter D_o used is defined by

$$D_o = \min\left\{D_H,\ \max\left(D'_m, D'_o\right)\right\} \qquad (2.3)$$

where D'_m is defined by eq.(2.2) (with $(We) = 12$). Hence, as can be seen from eq. (2.3), for $p < 5.4$ bar, the average droplet diameter is not allowed

to be less than 0.0025 m. Subsequently, the droplet interfacial area per unit volume S_{dr} is defined by [5,6]

$$S_{dr} = \frac{3.6\,(1-\alpha)}{D_o}. \tag{2.4}$$

In R5M3, the interfacial shear is proportional to S_{dr} and since the droplets are large (small interfacial area), in a number of situations, the droplets cannot be "lifted" by the vapour. Hence, we re-set the Weber number to 3 and assumed a minimum average droplet diameter of 0.0015m. In actual fact, the minimum average droplet diameter observed during the FLECHT-SEASET tests was in the range of 0.0008 m. Though, there is no explicit spacer model in the code (and spacers do certainly play a very important role in the amount of liquid carry-over in bundles particularly during low-flooding rate bottom flooding). Furthermore, we believe that there are geometry-effects contributing to the lower liquid carry-over in rod bundles (as compared to tubes) and these effects cannot be qualified. Finally, in a 2-fluid model code one cannot "rigorously" define an average droplet diameter since there is only one liquid field. Consequently, assuming a smaller minimum average droplet diameter would lead (through the resulting higher interfacial shear) in a liquid carry-over higher than the measurements show. Hence, the assumed value of 0.0015m is based on a compromise and engineering judgement rather than on a rigorous argument.

(c) The logic for selecting the pre- or the post-CHF interfacial closure laws in R5M3 are as follows: One defines

$$P \geq 1 \tag{2.5}$$

where

$$P = \max\,(0,\ \min\,(1,\ P'\,(0.4 - \alpha_B)10)). \tag{2.6a}$$

Now P' is defined by

$$P' = \min\,(1,\ P_{wind}\,T_{gs}) \tag{2.6b}$$

where

$$\left.\begin{aligned} P_{wind} &= 0.06666667 \ \text{ for } \tilde{p} \leq 0.025, \\ P_{wind} &= 0.016666667 \text{ for } \tilde{p} \geq 0.25, \\ P_{wind} &= \text{Interp. for } 0.025 < \tilde{p} < 0.25, \end{aligned}\right\} \tag{2.6c}$$

and

$$T_{gs} = T_g - T_s - 1. \tag{2.6d}$$

α_B is the void fraction for transition from bubbly to slug flow and T_g and T_s are the vapour and saturation temperatures, respectively. If (2.5) is satisfied, the code selects the post-dry out closure laws. The reason for this "indirect"

selection logic is that the interfacial closure laws subroutines are not actually "communicating" with the ones for the wall heat transfer and one would like to have some consistency between the two (which, by the aforementioned indirect procedure, is not always possible). We found that the definition of P' given by (2.6b) with P_{wind} given by (2.6c) is too restrictive. Hence, we modified equation (2.6b) to read (as it was defined in RELAP5/MOD2.5 [8])

$$P' = 1.0000454\,(1 - e^{-0.5\,T_{gs}}) \tag{2.7a}$$

where now (as we modified it in the past in RELAP5/MOD2.5 [8]), if the component in question is a bundle,

$$T_{gs} = T_g - T_s - 29 \tag{2.7b}$$

otherwise, T_{gs} is given by eq. (2.6d). This modification has a surprisingly large effect on a number of important predictions, and this can be easily understood if one realizes that it changes the "points" of transition from wet to dry-wall interfacial shear correlations (and vice versa), which are largely different in magnitude. Here, we should say that the selection of the value 29 in the above equation was made based on inspection of a number of predicted void fraction profiles during reflooding which with the original formulation, were exhibiting discontinuities near the quench front (QF) as in the case of RELAP5/MOD2.5 [8]. Conceptually, decrease (resp. increase) of this value results in employing the post-CHF interfacial closure laws at a lower (resp. higher) vapour temperature.

(d) We have completely removed the post-CHF wall-to-liquid heat transfer package of the code and in its place, we implemented two different packages, one when the physical process is reflooding and the other when it is not; hence, we reversed back to the RELAP5/MOD2 philosophy, but the packages we implemented are different to the ones in RELAP5/MOD2. The reasons for which we believe that the post-CHF heat transfer package of R5M3 is inappropriate and physically erroneous have already been extensively discussed in a series of previous works [1-3]; here, we shall briefly outline the logic of this package.

If the wall temperature T_w is greater than $T_s + 75$ and less than $T_s + 600$, the wall-to-liquid heat transfer coefficient is computed in the code as the maximum between the one given by the Chen transition/film boiling correlation (which has the form $q_{CHF}\exp(-1.34164\min(15, \sqrt{T_w - T_s})\,\Theta) / (T_w - T_s)$, where q_{CHF} is the critical heat flux and $\Theta = \Theta(|G|, \alpha)$ a complicated function of α and the total mass-flux $|G|$), and the modified Bromley correlation (supplemented by a void-fraction dependence), while if $T_w > T_s + 600$, is computed by the latter. As we have previously discussed on a number of different occasions [1,3], this approach is problematic, due to the fact that the Chen transition/film boiling correlation (which is a function of the

total mass flux $|G|$ and the void fraction) exhibits a sharp maximum at $|G| = 271$ kg/m^2/s while it decays fatly for mass-fluxes lower and higher than this value. A direct consequence of this is that, for example, in a bottom flooding experiment with an inlet mass-flux of say $|G| = 271$ kg/m^2/s, the code would predict a much faster quenching of the rods (higher heat transfer coefficient) than for a case with $|G| = 2000$ kg/m^2/s, **an unphysical result contradicting all experimental findings**. Clearly, this problem will also manifest itself in a number of other physical situations, whenever the total mass flux $|G|$ has a value close to 271 kg/m^2/s: This can be the case during the very early stages of a LB-LOCA in which case, as the flow slows-down in the core, the mass-flux is bound to cross this value, at which point the predicted HTC will suddenly greatly increase, while it will again decrease fastly as the mass-flux decrease further.

During reflooding, if the wall heat flux is exceeding the CHF, we implemented in the code a special heat transfer package based on the **empirical** CATHARE film-boiling, wall - to - liquid HTC and modified the wall heat transfer logic in the same way we modified it in TRAC-BF1 [9]. Hence, for the post-CHF wall heat transfer regime, we first define

$$\begin{aligned} \tilde{h}_{wl(FB)} = \max \Big\{ & (f_1 - f_2\, \Delta z_{QF}) \\ & \min\,(1 - \alpha,\, 0.5),\, 0 \Big\} + h_{BR} \sqrt{1 - \alpha} \end{aligned} \tag{2.8a}$$

where $f_1 = 1400$, $f_2 = 1880$, Δz_{QF} is the distance from the QF and h_{BR} is the original unmodified Bromley correlation. Subsequently, we define

$$h_{wl(FB)} = \max \left\{ \tilde{h}_{wl(FB)}, h_{FR} \right\} \tag{2.8b}$$

where now h_{FR} is the Forslund-Rohsenow wall-to-droplets contact HTC [8]. Finally, we define the wall - to - liquid post-CHF HTC by

$$h_{wl} = \max \left\{ h_{wl(FB)}, h_{wl(TB)} \right\} \tag{2.8c}$$

where $h_{wl(TB)}$ is the Weismann transition boiling correlation given by

$$h_{wl(TB)} = h_m e^{-\xi \Delta T} + 4500 \Big(\frac{G}{G_R} \Big)^{0.2} e^{-0.012 \Delta T}. \tag{2.9a}$$

We have chosen for the constant $\xi = 0.03$ and h_m is defined by [6,8]

$$h_m = \frac{q_{CHF}}{T_{CHF} - T_s} \tag{2.9b}$$

$$\Delta T = T_w - T_{CHF} \tag{2.9c}$$

where T_{CHF} and T_s are the wall temperature at CHF and saturation temperature, respectively, G is the total mass-flux and $G_R = 67.8$ kg/m^2/s. Notice that in RELAP5/MOD2, the value of $\xi = 0.04$ was used (although the value quoted in the manual [6] is $\xi = 0.055$); the reasons for decreasing this to 0.03 are explained in Ref. 8. No T_{MIN} is used in this formalism. Furthermore, we ramped the Weismann correlation linearly to 0 for distances between 0.1 m and 0.2 m from the QF, ie

$$h_{wl(TB)} = \begin{cases} h_{wl(TB)} & \Delta z_{QF} \leqq 0.1m \\ 0 & \Delta z_{QF} \geqq 0.2m \\ \text{Interp.} & 0.1m < \Delta z_{QF} < 0.2m. \end{cases} \tag{2.9d}$$

The reason for this is that we want to avoid "spurious" quenching of a node (eg due to a high value attained by $h_{wl(TB)}$ in eq. (2.9a)) if the QF is not in the vicinity of the node.

The aforementioned approach should be applicable if the flooding velocities are not exceeding $0.2 - 0.3$ m/sec, and this is usually the case during normal reflooding; for higher flooding velocities, this formalism is bound to brake down since the post-CHF HTC is certainly also a function of the mass-flux. An analysis of this problem is beyond the scope of this work. Furthermore, we should draw the reader's attention to the fact that with the above formalism, we have not modelled the top QF and its subsequent propagation up-stream: A node will quench only when the **bottom** QF approaches it. The reader should not forget that in the conventional approach followed in almost all transient analysis codes, spontaneous quenching of a node (or quenching of the top nodes) may occur not because a real QF is formed, but just because the surface temperature decreases below a pre-defined artificial temperature, often called T_{MIN}. Effectively, this is more erroneous than admitting that no rigorous physical model for predicting local quenching really exists and only taking the bottom QF as reference.

Before proceeding to the next item, we should say that recently we have actually extended the aforementioned approach and also modelled (at least generically) the heat transfer coefficient as a function of the distance from the top QF; this could be used for modeling top quenching. The way we achieved this can be outlined as follows: We define again

$$\begin{aligned} \tilde{h}^t_{wl(FB)} = & \max \left\{ (f^t_1 - f^t_2 \, \Delta z^t_{QF}) \right. \\ & \left. \min (1 - \alpha, \, 0.5), \, 0 \right\} + h_{BR} \sqrt{1 - \alpha} \end{aligned} \tag{2.10a}$$

where now f^t_1 and f^t_2 are coefficients different to the ones we used before and Δz^t_{QF} is the distance of the fine heat transfer node from the **top** QF and h_{BR} is still the original unmodified Bromley correlation. For the sake of completeness, we have set $f^t_1 = 500(200)$ and $f^t_2 = 5000$, which are showing

that $\tilde{h}^t_{wl(FB)}$ becomes 0 at a distance of 0.12(0.025) m from the top QF. We should stress that these values are **purely intuitive** and can only be (partly) determined after comparison of the code predictions with a number of experiments. Subsequently, like in the bottom QF case, we define

$$h^t_{wl(FB)} = \max\left\{\tilde{h}^t_{wl(FB)}, h_{FR}\right\} \tag{2.10b}$$

where h_{FR} is also the Forslund-Rohsenow wall-to-droplets contact HTC [8]. Finally, we define the wall - to - liquid post-CHF HTC from the top QF by

$$h^t_{wl} = \max\left\{h^t_{wl(FB)}, h^t_{wl(TB)}\right\} \tag{2.10c}$$

where now $h^t_{wl(TB)}$ is a modified Weismann transition boiling correlation (cf Eq. (2.9a)) but with $\xi = 0.05$. For this case, similarly to Eq. (2.9d), we ramp now $h^t_{wl(TB)}$ to 0 for distances which are greater than 0.1 m from the top QF for the case the $f^t_1 = 500$ or greater than 0.025 m for the case that $f^t_1 = 200$. Notice that in trying to model the wall-to-liquid heat transfer coefficient as a function of the distance from the top QF, we have tried to greatly restrict its range of influence in comparison to the range of influence of the bottom QF: This we did by assuming different values for f^t_1 and f^t_2 than for the corresponding ones (f_1 and f_2) for the distance from the top QF, but also by assuming a different value for ξ for the modified Weismann transition boiling correlation. The result of all these is that the effect of the heat transfer due to the top QF is much more concentrated in its vicinity. At the end, for each fine heat transfer node, the maximum between the two heat transfer coefficients (from the top and the bottom QFs) is taken. The inclusion of the top QF in the calculations is activated by an option. The draw back of this approach is that we actually have no way of telling when the top QF will be initiated so that we can later follow its downwards progression by our model; instead, we are relying on the heat transfer logic of the code for initializing the top QF.

(e) If the reflooding trip is not active, we implemented a different wall - to - liquid heat transfer package, which resembles the one of TRAC-BF1 [9,10]. For this case, we define the wall-to-liquid film boiling HTC h_{wl} by

$$h_{wl} = h_{wl(BR)}\,(1 - \alpha) \tag{2.11a}$$

where $h_{wl(BR)}$ is now the <u>modified</u> Bromley correlation. Additionally, we define a T_{MIN} and the transition boiling wall-to-liquid HTC (if $T_w < T_{MIN}$) by the Bjornard quadratic interpolation between the CHF point and the film-boiling wall-to-liquid HTC $h_{wl(FB)}$ ($= h_{wl(BR)}\,(1 - \alpha)$) in the usual way [9,10]

$$h_{wl(TB)} = (1 - \Gamma)\,h_{wl(FB)} + \Gamma\,\frac{q_{CHF}}{T_w - T_l} \tag{2.11b}$$

where

$$\Gamma = \left(\frac{T_w - T_{MIN}}{T_{CHF} - T_{MIN}} \right)^2 \tag{2.11c}$$

and all the symbols have their usual meaning.

For a suitable T_{MIN}, we first used the homogeneous nucleation temperature T_{HN} as used in COBRA-TF, and we have included this in a new subroutine **tmsfb**; T_{HN} is given by

$$T_{HN} = 705.44 - 4.722\ 10^{-2}\ dp + 2.3907\ 10^{-5}\ dp_2 - 5.8193\ 10^{-9}\ dp_3 \tag{2.12a}$$

where

$$dp = 3203.6 - p, \tag{2.12b}$$

$$dp_2 = dp\ dp \tag{2.12c}$$

and

$$dp_3 = dp_2\ dp. \tag{2.12d}$$

Notice that the above expression is in British units and we have to convert it to SI units internally in the code. The expression for T_{HN} given by the above equation results in a rather small temperature. Hence, for the time being, we set $T_{MIN} = 710\ K$ which we believe is a good approximation. In the future, one can actually refine this expression. Here, we should point out that the assumed T_{MIN} value is absolutely crucial on the peak RSTs reached during a LB-LOCA. In particular, a high value of T_{MIN} would result in a later dry-out of the rods at certain elevations and consequently, in an early removal of the stored energy which in turn means lower peak RSTs later in the transient.

(f) It has been explicitly shown [1,3] that during transients, for low mass-fluxes, the Groeneveld look-up table CHF exhibits oscillations which are fed-back into the HTC, hence adversely affecting the predicted RST histories. This is actually exaggerated by the heat-transfer package of the frozen version of the code outlined under **(d)** above, since, as we have already mentioned before, the q_{CHF} enters the HTC through the Chen transition/film boiling correlation which is used for wall-temperatures up to $T_s + 600$. Though, also in our new reflooding heat -transfer package the q_{CHF} enters through the Weismann correlation (cf. eq. (2.9a) - (2.9b)) and influences the post-CHF HTC (though, not to the extent that it influences it in the frozen version of the code). What actually happens is that through this, the oscillations of the q_{CHF} are transferred on the wall-to-liquid HTC which in turn exhibits a highly oscillatory behaviour. We have shown in Ref. 1 - 3 that this "unphysical pseudo-enhancement" of the HTC leads to unphysical RST decreases during analysis of reflooding experiments [4]; this we shall explicitly demonstrate in section 3.1. For this reason, for low mass-fluxes $|G|$, we modified the q_{CHF} in the code as follows:

$$\left.\begin{aligned} q_{CHF} &= q_{CHF,Zu} \text{ for } |G| < 50\ kg/m^2/s, \\ q_{CHF} &= q_{CHF,G_T} \text{ for } |G| > 150\ kg/m^2/s, \\ q_{CHF} &: \text{Interpolation for } 50 \leq |G| \leq 150 \end{aligned}\right\} \quad (2.13)$$

where q_{CHF}, $q_{CHF,Zu}$ and q_{CHF,G_T} are the critical heat flux, the modified Zuber CHF, and the CHF predicted from the Groeneveld tables, respectively.

(g) For the post-dry-out droplets (this includes low pressure reflooding), the interfacial shear coefficient c_{d_T} is restricted in the code by the following condition:

$$c_{d_T} = \min\,(c_{d_T},\ 0.45). \quad (2.14a)$$

This condition severely limits the value that the coefficient c_{d_T} can attain since, even for very large Reynold's numbers, c_{d_T} rarely becomes less than 0.45. The correct form of this condition should read

$$c_{d_T} = \max\,(c_{d_T},\ 0.45). \quad (2.14b)$$

(h) The interfacial closure coefficients are "old-time averaged" (under-relaxed) in order to smooth possible largely different values obtained during flow-regime transitions in the course of a transient [5,10]. The scheme used for under-relaxing the interfacial shear and heat transfer coefficients can be summarized as follows: The new-time under-relaxed coefficient f_i^{n+1} is defined by

$$f_i^{n+1} = f_i' \left(\frac{f_i^n}{f_i'}\right)^R, \quad (2.15)$$

where f_i' is the explicitly evaluated interfacial coefficient. In the version 7j of the code, the exponent R was defined in the following way: Firstly, one defines the function

$$R = \exp\,(-\min\,(20,\ \frac{\Delta t}{\tau})), \quad (2.16a)$$

where τ is a relaxation time-constant whose value depends on a number of conditions, and is either $0.1s$ or $0.5s$ for the interfacial shear coefficient, while for the interfacial heat transfer, it also depends on the liquid subcooling and the vapour superheating. In the R5M3.1, these schemes have been changed and are now very similar to the ones used before in RELAP5/MOD2.5 ie

$$R = f(\Delta t, \Delta_{cour}, \tau', V_g, V_l ...), \quad (2.16b)$$

where $R's$ are now complicated functions of the phasic velocities, time-step, Courant limit etc.

In the case of the version 7j of the code, we have already demonstrated that under certain conditions, the old-time averaging may lead to strongly time-step dependent code predictions [1]. To show this, we considered a numerical benchmark in which we assumed a heater-rod bundle in which there is a continuous liquid flow with an inlet liquid velocity of 0.015 m/s at 4.1 bar, and increased the power of the rod from 0 to 5500 W within 10s. We run the case with the frozen version of the code by assuming two different maximum-allowed time-steps DTMAX, 0.1s and 0.01s. With the former DTMAX, the code predicted no dry-out of the peak axial power elevation while with the latter, predicted a dry-out after 20s ! Hence, we first completely eliminated the interfacial shear under-relaxation scheme by setting $R = 0$ in Eq. (2.15), ie, a relaxation time-constant $\tau = 0$. Subsequently, we eliminated the vapour-to-liquid interface under-relaxation scheme and finally, for the liquid interface-to-liquid heat transfer under-relaxation, we kept the scheme which is already in the code if $f_i' > f_i^n$ while if this is not satisfied, we also by-passed this scheme. An attempt to generalize this and **completely** by-pass it independently of any conditions resulted in numerical problems. In R5M3.1, we also implemented exactly the procedure outlined above and as we shall see in the following section, a number of transients were successfully completed. Though, we should warn the reader that there may be cases for which this (even partial) elimination of the under-relaxation schemes may give problems; this may well be the case with certain SB-LOCA transients which are using relatively large time-steps. Without any doubt, we can say that the most representative code predictions (in as far as the actual closure laws being used as they stand) are obtained when no under-relaxation is used; though, in reality, the elimination of these schemes may result in unacceptably small time-steps and hence, this procedure may not be actually practical. In a recent work we have shown that the elimination of these schemes can be most effectively accomplished without any of the aforementioned undesirable side-effects if higher-order terms are retained in the linearization of the interfacial shear term (see **(j)** below), in conjunction with using a very simple bubbly/slug interfacial shear model (if the component is not a bundle) in place of the highly complex corresponding package in the frozen version of the code.

(i) As an option, we have included in the code the possibility of "upwinding" (instead of cell-length-averaging) [11] some quantities calculated in the interfacial shear subroutine **phantj** and in the subroutine **vexplt** which solves the momentum equations in the semi-implicit hydro-dynamic solution scheme. This option we also implemented in the past in the version 7j of the code and in general, the code predictions obtained by using this option were sometimes a little different to the ones obtained when the standard "cell-length-averaging" approach was used (mainly for system calculations). Though, these differences were not as dramatic as in the case of TRAC-BF1 [11]. These differences are to be expected since the aforementioned procedure is bound to result in a different water distribution, particularly in a large

system calculation. The difference between a cell-length averaged and upwinded variable can be summarized as follows: A cell-length-averaged phasic variable (or product of variables) $(Y_p)^n_{j+\frac{1}{2}}$ at a junction is defined by

$$(Y_p)^n_{j+\frac{1}{2}} = \frac{\Delta x_j\,(Y_p)^n_j + \Delta x_{j+1}\,(Y_p)^n_{j+1}}{\Delta x_j + \Delta x_{j+1}} \tag{2.17}$$

where Δx_j, Δx_{j+1} are the cell lengths of the adjacent cells and $(Y_p)^n_j$ is the value of this variable at the volume k adjacent to the junction $j + \frac{1}{2}$. An upwinded phasic variable (or product of variables) $\tilde{Y}^n_{p,\,j+\frac{1}{2}}$ at a junction is defined by

$$\tilde{Y}^n_{p,\,j+\frac{1}{2}} = (WP)\,(Y_p)^n_j + (WP1)\,(Y_p)^n_{j+1} \tag{2.18a}$$

where

$$(WP) = \begin{cases} 1 & V^n_{p,\,j+\frac{1}{2}} > 0 \\ 0 & V^n_{p,\,j+\frac{1}{2}} \leq 0 \end{cases} \tag{2.18b}$$

and

$$(WP1) = 1 - (WP). \tag{2.18c}$$

$(Y_p)^n_j$ and $(Y_p)^n_{j+1}$ are the cell-centred phasic quantities up-stream and down-stream the junction $(j + \frac{1}{2})$, respectively. **This option is not part of our standard modifications and all cases to be reported in this work were analyzed with this option switched-off.**

(j) We have implemented a linearization procedure for the interfacial shear terms in the momentum equations (both for the semi- and nearly-implicit hydrodynamic solution schemes) as explained in Ref. 1 and 2 (subroutines **vexplt**, **vimplt** and **jchoke**). Briefly, in the code, in the semi-implicit hydrodynamic solution scheme, the new-time (superscript $n+1$) phasic velocities $V^{n+1}_{g,j+\frac{1}{2}}$ and $V^{n+1}_{l,j+\frac{1}{2}}$ at the junctions $j + \frac{1}{2}$ are evaluated at each time-step by solving for each junction, a system of 2 algebraic equations. These new-time velocities are functions of the old-time (superscript n) velocities $V^n_{g,j+\frac{1}{2}}$ and $V^n_{l,j+\frac{1}{2}}$, the convective terms (which are also the old-time ones) etc. The interfacial shear term $f^{n+1}_{i,j+\frac{1}{2}}$ at time-step $n + 1$ is given by (in general)

$$f^{n+1}_{i,j+\frac{1}{2}} = C_{IO}\,(C_1 V^{n+1}_g - C_o V^{n+1}_l)^q_{j+\frac{1}{2}} = C_{IO}(V^{n+1}_{R,j+\frac{1}{2}})^q \tag{2.19a}$$

where

$$C_1 = \frac{1 - \alpha\,C_o}{1 - \alpha}, \tag{2.19b}$$

q is an integer (in the case of RELAP5, $q = 2$ for all flow-regimes), and C_o and C_{IO} are flow-regime dependent coefficients. Clearly, in order to have a linear algebraic system of finite-differenced equations to be solved as described above, it can readily be seen from eq. (2.19a) that one must somehow approximate the interfacial shear term $f^{n+1}_{i,j+\frac{1}{2}}$ in a way that the new-time

phasic velocities enter the 2 algebraic finite-differenced equations linearly. In R5M3, the following simple approximation is used [5,6]

$$f_{i,j+\frac{1}{2}}^{n+1} \simeq C_{IO}(V_{R,j+\frac{1}{2}}^{n+1})\,(V_{R,j+\frac{1}{2}}^{n})^{q-1}. \tag{2.20}$$

Clearly, this approximation is rather crude and could lead to numerical instabilities or time-step reductions (particularly if rather large time-steps are otherwise allowed), since it contains "too much" of the old-time relative velocity difference. (this is particularly true if $q > 2$, as is the case with some correlations in TRAC-BF1 [10]). Alternatively, one can use the approach used in TRAC-BF1 [10] and first expand eq. (2.19a) in a Taylor series in terms of the velocity differences $(V_R^{n+1} - V_R^n)$ (for the sake of notational convenience, we do not show the dependence of $V_{R,j+\frac{1}{2}}^n$ on the junction number). Hence, we can write

$$\begin{aligned}
&f_i^{n+1} = C_{IO}\,(V_R^{n+1})^q = C_{IO}\,(C_1 V_g^{n+1} - C_0 V_l^{n+1})^q \simeq \\
&f_i^n + \left.\frac{\partial f_i}{\partial V_R}\right|^n (V_R^{n+1} - V_R^n) + \left.\frac{\partial^2 f_i}{\partial V_R^2}\right|^n (V_R^{n+1} - V_R^n)^2 + \\
&\left.\frac{\partial^3 f_i}{\partial V_R^3}\right|^n (V_R^{n+1} - V_R^n)^3 + ... = \\
&C_{IO}\Bigg((V_R^n)^q + q(V_R^n)^{q-1}\,(\delta V_R^{n+1,n}) + \frac{q(q-1)}{2}(V_R^n)^{q-2}\,(\delta V_R^{n+1,n})^2 + \\
&\frac{q(q-1)(q-2)}{6}(V_R^n)^{q-3}\,(\delta V_R^{n+1,n})^3 + ...\Bigg) = C_{IO}\Bigg(-(q-1)\,(V_R^n)^q + \\
&q(V_R^n)^{q-1}\,(V_R^{n+1}) + \frac{q(q-1)}{2}(V_R^n)^{q-2}\,(\delta V_R^{n+1,n})^2 + \\
&\frac{q(q-1)(q-2)}{6}(V_R^n)^{q-3}\,(\delta V_R^{n+1,n})^3 + ...\Bigg)
\end{aligned} \tag{2.21a}$$

where

$$\delta V_R^{n+1,n} = (V_R^{n+1} - V_R^n), \tag{2.21b}$$

$$f_i^n = C_{IO}\,(V_R^n)^q. \tag{2.21c}$$

If we now keep only the first two terms in the expansion, we shall have

$$f_{i,j+\frac{1}{2}}^{n+1} \simeq C_{IO}\Big(-(q-1)\,(V_{R,j+\frac{1}{2}}^n)^q + q(V_{R,j+\frac{1}{2}}^n)^{q-1}\,(V_{R,j+\frac{1}{2}}^{n+1})\Big). \tag{2.22}$$

Clearly, in contrast to the approximation given by eq. (2.20) the higher-order linearization approximation given by eq. (2.22) is bound to improve the numerical robustness of the code, either when the nearly-implicit solution

scheme is used (which allows the code to take time-steps greater than the Courant limit), or when the semi-implicit method is used with relatively large allowed time-steps: Much larger changes of the phasic velocities between two successive time-steps can be tolerated without the danger of exciting numerical instabilities [5]. Notice that if we want to keep even higher order terms in the Taylor expansion given by eq. (2.21a), one introduces again non-linearities due to the $(\delta V_R^{n+1,n})^2$ terms. Here, there are two very interesting points on which the reader should be informed:

(a) In a recent unpublished work, we have actually shown that if the simple bubbly/slug interfacial shear correlation for pipes outlined under **(k)** below is used instead of the complex one already in the code, for some transients, if the interfacial shear linearization shown in eq. (2.22) is used instead of the simple approach used in R5M3 (eq. (2.20)), one can actually obtain completely different code predictions during some system calculations, particularly if eg the RSTs are very sensitive to the actual liquid fraction in the core (as is indeed the case for high-pressure transients at axial elevations where the liquid fraction is very small). No significant differences were observed during the analysis of simple, separate-effect tests like the bottom-flooding tests we shall analyze in the following section.

(b) The retention of higher-order non-linear terms in eq. (2.21a) can be achieved by a simple iteration procedure of the 2 finite-differenced algebraic phasic momentum equations in subroutine **vexplt**: Without getting into any detail, the 2 algebraic equations are first solved for the new-time phasic velocities with the **linear** interfacial shear in the form of eq. (2.22); subsequently, the expression

$$f_i^{n,(k+1)'} = C_{IO}\left(\frac{q(q-1)}{2}(V_R^n)^{q-2}\,(\delta V_R^{n+1,n,(k+1)})^2 + \frac{q(q-1)(q-2)}{6}(V_R^n)^{q-3}\,(\delta V_R^{n+1,n,(k+1)})^3 + \ldots\right) \qquad (2.23a)$$

where

$$\delta V_R^{n+1,n,(k+1)} = (V_R^{n+1,(k+1)} - V_R^{n,k}) \qquad (2.23b)$$

(k is the iteration-index) is evaluated with the newly computed phasic velocities and the 2 algebraic equations are solved again with the extra term $f_i^{n,(k+1)'}$ added on the RHS and with the new-time phasic velocities in the place of the old-time ones. The number of iterations is defined by the index k. We have shown that retaining higher-order terms in the linearization of the interfacial shear terms in the way outlined above can also significantly alter the code predictions as compared to the ones obtained when the linearization shown in eq (2.21) is used. The retention

of higher-order terms results in an even more numerically robust code which runs without problems even for cases in which eg it was stopping if the under-relaxation schemes were by-passed as explained under item **(h)** above. Surprisingly enough, the simple iteration scheme mentioned above only marginally increases the running time of the code; though, for many cases, it has the effect of further reducing the mass-error of the code. We have only implemented this iteration procedure in the semi-implicit solution scheme.

In the analysis of transients on which we shall report in the following section, we have only used in our modified code version the linearization as shown in eq. (2.22).

The following 3 items are included in the code as options and are not part of our standard modification package. A preliminary assessment of these options has been performed with very encouraging conclusions about their validity. Though, in this work, we shall only present selected results obtained and conclusions drawn by activating these options which will clearly demonstrate some of their advantages when compared to the ones already in the code. These options are assumed to be valid only if the component is not a bundle.

(k) We implemented in the code the Ishii/Andersen's drift-flux based bubbly/slug (new subroutine **fidisbu**) interfacial shear correlation as in TRAC-BF1 if the component is not a bundle. We shall have

$$C_{IO} = \frac{\rho_l^2 \, \alpha \, (1 - \alpha)^5}{4 \, \sigma}, \tag{2.24a}$$

while the interfacial shear per unit volume will be

$$f_i = \frac{\rho_l^2 \, \alpha \, (1 - \alpha)^5}{4 \, \sigma} \, (C_1 V_g - C_o V_l)^4, \tag{2.24b}$$

where

$$C_o = C_\infty - (C_\infty - 1) \sqrt{\rho_g \, / \, \rho_l}, \tag{2.24c}$$

$$C_\infty = 1 + 0.2 \, (9.81 \, \rho_l \, D_H \, / (|G| + 0.0001))^{0.05} \tag{2.24d}$$

and all the symbols have their usual meaning. For bundles, the modified Bestion correlation is used as described before. Notice that in contrast to the bubbly/slug interfacial shear package in the frozen version of the code (of which the EPRI correlation is a part) which depends on a large number of local variables (which are bound to oscillate in the course of a transient calculation) and conditions, the aforementioned correlation is a very simple and straight-forward expression. Recently, we have shown that this very complexity of the bubbly/slug interfacial shear package in R5M3 is mainly responsible for the creation of an unacceptably high (more than the initial

liquid inventory of the system) mass-error in the analysis of a long (over 10 hours real time) SB-LOCA transient. Furthermore, we showed that by removing the original package and using the above correlation for pipes and the modified Bestion correlation for bundles, the mass-error was reduced to only a very small percentage of the initial liquid inventory of the system; for more details on this work, the interested reader is referred to Ref. 12 and 13. Our whole set of modifications related to the mass-error reduction have also been implemented in the RELAP5 part of the SCDAP code with which a long SB-LOCA transient was also analyzed, with a very small mass-error; in contrast, the same large mass-error problem was encountered when the frozen version of SCDAP was used [14]. Here, we should clearly state that although our modification reduced the mass-error by more than an order of magnitude, the problem is not really solved and the mass-error increases again if pressures lower than 3 bar are considered. We believe that a drastic modification of the solution scheme in the code by introducing iterations is required before the excessive mass-error problem is properly addressed and solved.

(l) As an option, we have implemented the drift-flux based annular flow interfacial shear correlation of TRAC-BF1 [10]. (subroutine **fidisan**). One defines

$$C_o = 1 + \frac{(1 - \alpha)(1 - e)}{(\alpha + a)} \tag{2.25a}$$

where

$$a = \sqrt{\frac{1 + 75(1 - \alpha)}{\sqrt{\alpha}}} \sqrt{\frac{\rho_g}{\rho_l}} \tag{2.25b}$$

e is the fraction of the entrained liquid and is calculated as in TRAC-BF1 in the following way:

$$e = \max\left((xe - 0.03), 0.0\right) / \sqrt{1 + (0.1 + xe)^2} \tag{2.25c}$$

where

$$xe = 10^{-6} \sqrt{(\sqrt{j_g^{*\,10}\, D^{*5}\, Re_l})}\,. \tag{2.25d}$$

$$Re_l = |G_l|\, D_H \,/\, \mu_l\,, \tag{2.25e}$$

$$G_l = (1 - \alpha)\, \rho_l\, V_l\,, \tag{2.25f}$$

$$D^* = D_H \sqrt{9.81\,(\rho_l - \rho_g) \,/\, \sigma}\,, \tag{2.25g}$$

$$j_g^* = |j_g| \,/\, \sqrt{\sqrt{\sigma\, 9.81\,(\rho_f - \rho_g)\,(\rho_g \,/\, (\rho_f - \rho_g))^{.66666}} \,/\, \rho_g^2}\,, \tag{2.25h}$$

and

$$j_g = \alpha\, V_g\,. \tag{2.25i}$$

Then, we shall have

$$C_{IO} = \frac{0.015\, \rho_l\, \alpha\, (a + \alpha)^2}{D_H} \tag{2.26a}$$

and the interfacial shear per unit volume will be

$$f_i = \frac{0.015\,\rho_l\,\alpha\,(a+\alpha)^2}{D_H}\,(C_1V_g - C_oV_l)^2. \qquad (2.26b)$$

Notice that the aforementioned annular flow interfacial shear correlation assumes that the continuous phase is the liquid and hence, the liquid density ρ_l appears as multiplier. This makes it up to two orders of magnitude higher than the ones of Wallis which is in the code and is based on the assumption that the continuous phase is the vapour [9]. Finally, the following values of C_{IO} are used in the phasic momentum equations:

If $1 - \alpha < 0.1$,

$$C_{IO} = 10\,C_{IO}\,(1-\alpha). \qquad (2.27a)$$

If $1 - e < 0.1$,

$$C_{IO} = 10\,C_{IO}. \qquad (2.27b)$$

If $1 - e > 0.1$,

$$C_{IO} = \frac{C_{IO}}{1-e}. \qquad (2.27c)$$

This formulation is identical to the one in TRAC-BF1 and all the symbols are self-explanatory. Notice that also this formulation avoids a strong dependence on local variables (with the exception of the distribution coefficient C_o (and hence, also C_1) which, in any case, is bounded both from below by 1 and from above) and that the interfacial shear coefficient C_{IO} is mainly a function of the void fraction. Though, the corresponding model in R5M3 is also relatively simple. Here, we should mention that the employment of the aforementioned two options (if the component is not a bundle) has resulted in large reductions of the code mass-error (sometimes by more than an order of magnitude) for long SB-LOCA transients, most probably due to the simple mathematical form of these correlations and their lack of dependence on a number of local variables [12,13].

(m) The Ishii dispersed flow interfacial shear correlation (as in TRAC-BF1) was implemented as an option (subroutine **fidishi**). For this flow regime, C_{IO} is defined by

$$C_{IO} = \frac{\alpha\,(1-\alpha)\,(\rho_l - \rho_g)\,g}{V_r^4} \qquad (2.28)$$

and the interfacial shear per unit volume is

$$f_i = \frac{\alpha\,(1-\alpha)\,(\rho_l - \rho_g)\,g}{V_r^4}\,(C_1V_g - C_oV_l)^4\;;\;(C_o = C_1 = 1) \qquad (2.29)$$

where g is the gravity constant and V_r is the equilibrium relative velocity of the droplets calculated in the following way: First, one defines the droplet radius $r_{d,ish}$ calculated from Ishii's equation

$$r_{d,ish} = \frac{0.006\,\sigma}{\rho_g\,(\alpha\,V_g)^2}\left(\frac{\rho_l}{\rho_g}\right)\left(\frac{\mu_g\,Re_g}{\mu_l}\right)^{0.3333} \tag{2.30}$$

where V_g is the steam velocity and Re_g the steam Reynold's number. Next the Ishii's critical drop size is given by

$$r_{d,crit} = 2\left(\frac{2\,\sigma}{g\,(\rho_l - \rho_g)}\right)^{0.5}\left(\frac{\mu_g}{(\rho_g\,\sigma\,(\frac{\sigma}{g\,(\rho_l-\rho_g)})^{0.5})^{0.5}}\right)^{0.3333}. \tag{2.31}$$

We now define two equilibrium relative velocities V_r for the droplets, one in the "wake" region and one for the "distorted" drops, $V_{r(w)}$ and $V_{r(d)}$, respectively

$$V_{r(w)} = 0.5\,r_d\,\alpha^{1.5}\left(\frac{((\rho_l - \rho_g)\,g)^2}{\mu_g\,\rho_g}\right)^{0.3333} \tag{2.32a}$$

and

$$V_{r(d)} = \sqrt{2}\,\alpha^{1.5}\left(\frac{\sigma\,g\,(\rho_l - \rho_g)}{\rho_g^2}\right)^{0.25} \tag{2.32b}$$

where

$$r_d = \max\left(\min\left(r_{d,ish},\ 2\sqrt{\frac{\sigma}{g\,(\rho_l - \rho_g)}}\right),\ 0.0001\right). \tag{2.32c}$$

Finally, if $r_{d,ish} > r_d$, $V_{r(d)}$ is used as the relative velocity of the droplets in eq. (2.28) otherwise, $V_{r(w)}$ is used. In eq. (2.19a), $q = 4$. Preliminary assessment of this option resulted in over-estimation of the liquid carry-over during low flooding rate bottom flooding experiments: Clearly, the aforementioned correlation over-estimates the interfacial shear. This in itself agrees with the statements we made at the end of item **(b)** above, ie, for a number of reasons, one has to assume in bundles a lower interfacial shear than in tubes in order to avoid excessive liquid carry-over. Effectively, by doing this, we are somehow compensating for effects like wetting of spacers (and hence, decrease of the liquid carry-over) or geometry-effects which are not accounted for in the codes.

(n) Similarly to our work on the version 7j of the code, a number of additional **options** have been implemented in R5M3.1 like the first upwind scheme in the convective terms of the momentum equations and the inclusion of the spatial derivatives in the virtual mass term which read [5]

$$F_{vm,g} = -\,k_{vm}\,\alpha_g\alpha_l\rho\left(\frac{\partial(V_g - V_l)}{\partial t} + V_l\,\frac{\partial V_g}{\partial z} - V_g\,\frac{\partial V_l}{\partial z}\right) \tag{2.33}$$

(where $\rho = \alpha_g\,\rho_g + \alpha_l\,\rho_l$) for the steam momentum equation (and similarly for the liquid, with the subscripts g and l interchanged). With the version 7j of the code, we have tried to assess a number of these options and compare the code predictions with the option(s) switched on and off. Though, we shall not comment on these side efforts in this work.

3 ASSESSMENT OF THE MODIFIED CODE AND COMPARISON WITH THE FROZEN VERSION

We shall now assess the modified code by comparing its predictions with measurements from a number of separate-effect and integral test experiments, as well as with the predictions of the frozen version. For all the case analyzed, the top QF option was not activated; though, activation of this option has shown that the model indeed works properly and predicts (for some cases) a gradual quenching of the nodes from the top due to a downwards propagating QF.

We shall present and discuss the following cases: A constant inlet liquid velocity bottom flooding experiments in the heater rod bundle NEPTUN at PSI, as well as a similar FLECHT-SEASET test, the Oak Ridge THTF low power film boiling experiment Nr. 3.08.6C, the LOFT LP-LB-1 experiment, a hypothetical 200% LB-LOCA calculation in a two loop 1130 MWth commercial PWR and the LOBI SB-LOCA BL34 experiment. Finally, we shall demonstrate that in principle, our top QF logic is functioning and we shall show how it affects the RSTs in the upper regions of NEPTUN and the LOFT when compared to the corresponding cases in which the top QF logic is not activated. A number of boil-off tests in NEPTUN [15] were also analyzed [1,16] but we shall not show or discuss the results here other than saying that although the frozen version of the code over-predicted the amount of water expelled and hence, predicted an earlier dry-out of all axial rod positions, our modified code with the modified Bestion correlation [7] re-activated resulted in excellent agreement between measurements and predictions, a result which is not surprising.

3.1 The NEPTUN and FLECHT-SEASET bottom flooding tests

We shall start by briefly discussing the set of NEPTUN [15] and FLECHT-SEASET bottom flooding experiments. The nodalizations used in both of these tests are the same with the ones reported in Ref. 8. Whenever possible, the code predictions will be compared to the measurements.

The initial conditions of the bottom flooding NEPTUN experiment on which we shall report are [1,4]

Nr. 5036 (P = 4.1 bar, ΔT_s = 10 K, V_{IN} = 0.015 m/s),

while the initial conditions of the FLECHT Test Nr. 31701 are

FLECHT Test Nr. 31701 (P = 2.7 bar, ΔT_s = 79 K, V_{IN} = 0.15

m/s)

In Fig. 1, we show the measured and predicted RSTs and predicted total HTCs at axial elevations of 0.714 m and 0.946 m both by the frozen [1,2,4] and modified versions of the code for the NEPTUN bottom flooding experiment (the measured HTCs are not shown in the figures; they are very close to the ones predicted by the modified code). Clearly, the unphysical modeling of the HTC in the code and, primarily, the low mass-flux CHF behaviour of the Groeneveld look-up tables as explained under **(f)** in the previous section, result in a highly oscillatory total HTC and hence, to unphysical code predictions for the RSTs: This "pseudo-enhancement" of the HTC leads to the unphysical fast decrease of the RSTs which decrease to saturation in an almost continuous fashion (Fig. 1 A); in contrast, at the higher axial elevation (Fig. 1 B), after the initial decrease, the RST stays constant. Not surprisingly, other variables like void fraction and steam velocities also exhibited a highly oscillatory behaviour when calculated by the frozen version [1,4]. On the other hand, the modified code predicts the RST histories well and the predicted HTC are very smooth. indeed. This is also the case for other variables like void fraction and steam velocities. In particular, as one can see in Fig.2, both the void fractions at different axial elevations and the collapsed liquid level in the core computed by the modified version were very close to the experimental measurements. The elimination of the unphysical oscillations is primarily the result modifying the low-mass flux behaviour of the q_{CHF} obtained by the Groeneveld tables and not of the new heat transfer package used during reflooding; the merits of the new heat transfer package is that it actually takes into account (empirically) the experimentally observed dependence of the heat transfer as a function of the distance from the QF; this is bound to result in simulations in which the predicted RSTs exhibit the right trends, particularly in the vicinity of the QF. Although not shown in this work, a number of other (five of them) bottom flooding experiments in the NEPTUN facility were analyzed with both versions of the code and while the frozen version was always resulting in unphysical and erroneous predictions [1-4], the modified code predicted the measured variables (RSTs, Collapsed Liquid Levels etc) very well indeed.

In Fig. 3, we show the measured and predicted RST histories at axial elevations of 2.01 m and 2.56 m for the FLECHT-SEASET test. Also here, one can see that the predictions obtained by the modified version are closer to the measurements and are more physically sound; generally, the inclusion in the code of an empirical wall-to-liquid heat transfer correlation similar to the one in CATHARE HTC with its explicit dependence on the distance from the QF is primarily responsible for this good agreement between measurements and predictions obtained by the modified code. Two more FLECHT-SEASET bottom flooding experiments were also analyzed with both versions and there was always good agreement between measurements and predictions of the modified code; in contrast, the predictions obtained by the frozen version were always plagued by the problems we discussed in the previous section. Hence, we can safely say that as far as reflooding is concerned, the improvements brought about by our new wall-to-liquid

heat-transfer package as well as by the other modifications which are of importance during this process seem to be of general validity.

3.2 The THTF 3.08.6C test

The Oak Ridge THTF test Nr. 3.08.6C is a low-power film-boiling experiment with an imposed inlet mass flow. From $t = 0\ s$ to $t = 50\ s$ the bundle power is 2.4 MW while from 50 s to 80 s, the time-power (in MW) pairs are (51.7,8.1), (52.4, 8.0), (71.0,8.0), (74.8,3.2) and (80.0, 3.1). The imposed liquid mass flow at the inlet was 7.0 kg/s from $t = 0\ s$ to $t = 50.\ s$, and gradually decreased to 0.77 kg/s at $t = 80\ s$. The inlet pressure and liquid temperatures were at $t = 0\ s$ 128 bar and 537.5 K, respectively, while at $t = 80\ s$ were 63.7 bar and 541 K. The facility was modelled by 45 volumes of length varying from 0.104 m to 0.04 m [17].

The case was analyzed both with the frozen [17] and our modified version of the code, and by using both the semi- and nearly-implicit hydrodynamic solution schemes. No reflooding heat transfer package is activated in these runs; hence, one can partly utilize this case for assessing the new wall-to-liquid heat transfer package used when there is no reflooding. Furthermore, we shall show that our modifications (and, most probably, the higher-order linearization scheme of the interfacial shear term in the phasic momentum equations) have a "smoothing" effect on the predictions obtained by using the Courant-violating nearly-implicit numerics.

In Fig. 4A and 4B, we show the measured RST histories at two different axial elevations and compare them to the ones predicted by the frozen and modified versions of the code, when the standard semi-implicit scheme is used. At level F, both versions predict an earlier CHF [17] and under-estimate the RSTs later in the transient; though, the RSTs predicted by the modified code are for almost the whole time "parallel" to the measured ones, while the frozen version, under-estimates the HTC after approximately 60 s. At the higher axial elevation (Fig. 4B), both versions predict very similar RSTs. The situation is basically much different when the nearly-implicit solution scheme is used: One can see from Fig. 4C and 4D that although the RSTs predicted by the modified version of the code are less close to the measured ones than the ones obtained when the semi-implicit scheme was used, the ones obtained by using the frozen version of the code are exhibiting a totally unphysical behaviour, indicating that some auxiliary variables are oscillating and are inducing RST oscillations.

Generally, we can say that the modified code predicts the RSTs of this test a little better than the frozen version and hence, our new heat transfer package used when the reflooding is not activated seems to perform well; though, when the nearly-implicit scheme is used, the predictions of the modified code are certainly much better and more physically sound. The reason that there are relatively large

differences between the predictions obtained when the two different hydro-dynamic solution schemes are used is a subject on its own and we shall not elaborate on it in this work. Concluding this sub-section, we should say that we have also analyzed the THTF test 3.03.6AR with both versions of the code: In contrast to the measurements which show at different axial positions a dry-out, a subsequent RST increase and a subsequent decrease of the RSTs at a very slow rate (up to the time of 20 s, the RSTs are between 750 K and 850 K), the frozen version of the code predicts that the temperatures start decreasing fastly at approximately 10 s, probably due to the Chen transition/film boiling correlation. In contrast, the RSTs predicted by the modified code follow the experimental RSTs (for most axial elevations) very closely.

3.3 The LOFT LP-LB-1 test

We shall continue in this and the following sub-sections our comparison of the predictions obtained by the two versions of the code by considering integral test experiments as well as a hypothetical LB-LOCA in a two-loop commercial PWR. Firstly, in Fig. 5, we present 2 sets of measured and predicted RSTs for the LOFT LP-LB-1 test at axial elevations of 27 and 31 inches; the test was analyzed using the input deck of Ref. 18. Here, apart from the analysis performed with the frozen version of the code, we analyzed it with our modified version but also with the version in which the options described under **(k)** and **(l)** above were activated if the component was not a bundle. From these figures, the following conclusions can readily be drawn:

(a) The frozen version of the code is not capable of capturing the trends of the measured RSTs even qualitatively.

(b) The modified code predicts the measured RSTs better than the frozen version; though, even the modified version under-predicts the peak RSTs by approximately 200 K. Clearly, this is most probably due to the fact that the amount of liquid in the core at the beginning of the reflooding phase is over-predicted.

(c) When the options **(k)** and **(l)** of the previous section are switched-on, the predicted RSTs are now higher than before and hence, closer to the measured ones. In fact, the differences between measured and predicted peak RSTs are now in the range of 50 K. We have actually verified that this difference can be almost exclusively attributed to using a different annular flow interfacial shear in the pipes (item **(l)**). This is an indication that the differences between the measured and predicted RSTs by our modified version are most probably due to hydraulic (liquid-distribution) rather than heat-transfer reasons. Additionally, these differences demonstrate in a very clear fashion the sensitivity of the code predictions during system calculations to the selection

of a particular interfacial shear correlation in one flow regime even in regions other than the core.

3.4 A hypothetical LB-LOCA in a two-loop 1130 MWth commercial PWR

As a next test of the way that our model changes affect the predicting capabilities of the code during the analysis of a LB-LOCAs, we analyzed a two-loop 1130 MWth commercial PWR hypothetical 200 % LB-LOCA transient with the input deck also supplied to us by Lübbesmeyer [19]. Since our main aim here is to present and discuss the differences in predicted RST histories between the frozen and our modified version of the code, we shall avoid any discussion in relation to plant modeling and nodalization other than saying that the core was divided into a low, medium and high power regions while in the latter, a hot rod was assumed. In Fig. 6, we compare the RST predictions of the modified and frozen versions for the hot rod in the high power region at the peak-power axial elevation. One can clearly see that the predictions of the frozen version exhibit a sharp RST decrease at approximately 7.5s after the break opens, due to the problematic Chen transition/film boiling correlation which attains a very high value at $|G| = 271\ kg/m^2/s$ and when later $|G|$ decreases, the RSTs start increasing again. A consequence of this is that the peak RSTs predicted by the frozen version of the code are much lower than the ones predicted by the modified version since with the former, a large amount of stored energy is removed during the first 12s of the transient, due to the high value attained by the Chen transition/film boiling wall-to-liquid heat transfer correlation. Sensitivity studies have also shown the dependence of the peak clad temperature at certain elevations on the assumed value of T_{MIN} which is used on the heat transfer package activated when there is no reflooding (see item (e) in section 2).

3.5 The LOBI SB-LOCA BL34 test

Concluding this section, we shall present some results from the analysis of the LOBI SB-LOCA BL34 experiment [20] for which the reflooding option is not activated. By doing this, we shall try to assess the way (if any) that our modifications (and in particular, the new heat transfer package used when the reflooding is not activated) affects the code predictions for a SB-LOCA calculation. Though, we must stress once more that a much wider assessment of the modified code with a number of SB-LOCA tests is needed before we can say that we are confident that our version of the code performs better than the frozen version also for SB-LOCA transients; this is a necessary procedure for further qualifying our modifications.

In Fig. 7, we show the measured and predicted RST histories at two axial elevations, as well as the collapsed level in the core. Notice that neither the

modified code nor the frozen version predict the first dry-out observed in the measurements. Both at level 5 and level 8, the modified code predicts the RSTs significantly better than the frozen version: In particular, at level 5, the dry-out time is predicted better by the modified code but the peak RST is under-estimated. At level 8, the modified code predicts an earlier dry-out but the RST history is closer to the measurements than the one predicted by the frozen version. Finally, the collapsed core level is predicted much better by the modified code, although both versions are over-predicting the initial level collapse. Hence, also for this case, our modified code performs better than the frozen version and our new wall-to-liquid heat transfer package used when the reflooding is not active seems to work well and without any problems.

Finally, in Fig. 8, we demonstrate the ability of the modified code to model top quenching should this option is activated. In particular, in (A) and (B), we show the predicted RSTs for the 2 top nodes at elevations of 1.41 m and 1.642 m, respectively, for the NEPTUN experiment Nr. 5050 by the version in which the the top quench model is activated and compare them to the case for which it is not. Similar plots are shown in (C) for the axial elevation of 49 inches in the LOFT for the LP-LB-1 test. For the NEPTUN case, the measurements actually show that the nodes in (A) and (B) are quenching at approximately 43 s and 21 s, respectively. Hence, our top QF model which, as we already mentioned before, cannot predict the time that the top QF is initiated, predicts an earlier quenching of these two elevations. Finally, for the LOFT test, the earlier quenching of this elevation when the model is activated is also evident. The measurements actually show a quenching at approximately 16 s followed by a new heat-up at 20 s, a new RST drop at 40 s and a final quenching at approximately 52 s. Clearly, our model (or any other model) cannot reproduce this behaviour. Hence, both for the NEPTUN separate-effect test and for the LOFT integral test, the activation of the top quench option in the code results in predicting a downwards propagating QF. We should make clear here once more that we are assessing the capability of the modified code to generally predict the top quenching rather than claiming that we can achieve quantitative agreement with measurements: For this, as we have already mentioned before, one has to adjust the coefficients of the model by utilizing a large number of experiments.

4 CONCLUDING DISCUSSIONS

In this work, we have outlined a number of modifications, model changes and options introduced in R5M3.1 which result in better and more physically sound code predictions. These modifications include items like modeling of the wall-to-liquid heat transfer, interfacial shear, transition criteria from the pre- to the post-dry out interfacial closure laws (and vice versa), modification of the low mass-flux limit of the CHF predicted by the Groeneveld look-up table, partial removal of the under-relaxation schemes of the interfacial closure coefficients etc. Clearly,

most of these modifications are **generic**, since they are altering some of the basic physical models in the code. Additionally, a number of options were introduced in the code, like the possibility of using the same interfacial shear correlations as in TRAC-BF1. Without any doubt, there is a number of other areas on which attention must be paid and a careful assessment and validation of other models along the lines summarized in the Introduction should be pursued; this, as we have already mentioned, is not only restricted to the actual physical correlations, but also to "numerics-related" items like under-relaxation schemes, flow-regime transition smoothing etc. which as far as code predictions are concerned, they may overwhelm the physical models.

In relation to our particular fields of interest and as far as physical modeling is concerned, the main code deficiencies we identified were in the modeling of the wall-to-liquid heat transfer coefficient in the post-CHF wall heat transfer regime (this being the case independently of whether one is modeling reflooding or not), the adverse effects of the oscillatory behaviour of the CHF predicted from the Groeneveld look-up table (mainly for low mass-fluxes), the unphysically small dispersed flow interfacial shear at lower pressures arising from the assumed unphysically large minimum average droplet diameter and the false condition imposed on the interfacial shear coefficient (both of which greatly affect the code predictions during reflooding), the effect of the under-relaxation schemes of the interfacial closure coefficients (particularly in the presence of "numerical" oscillations), as well as a number of other minor points which, nevertheless, can show-up and become important under certain conditions.

A number of separate-effect and integral test experiments were analyzed with the modified version of the code, and the predictions were compared with the ones obtained by the frozen version and (when possible) with the measurements. Through the course of our work, we closely monitored not only the "observable" variables like RSTs or liquid levels, but also the "non-observable" ones like liquid velocities, steam velocities etc, and looked carefully at possible unphysical large-amplitude oscillations in these quantities; this could indicate a possible modeling deficiency and is bound to affect the predictions of the variables of interest (eg RSTs).

Without any exception, the predictions of the modified code were much closer to the measurements, in most cases free of unphysical oscillations, and always more physically and conceptually sound than the ones of the frozen version. We attribute these improvements to the more realistic and physically sound modeling of a number of processes and constitutive relations in the modified version of the code. The tests analyzed by both code versions included bottom reflooding in a heater rod bundle at PSI and in the FLECHT-SEASET bundle, the Oak Ridge THTF low power film boiling experiment Nr. 3.08.6C, the LOFT LB-LOCA LP-LB-1, a hypothetical 200% LB-LOCA in a two-loop 1130 MWth commercial PWR and the LOBI BL34 SB-LOCA test. Furthermore, we demonstrated that our newly implemented "top QF model" performs as expected, although we have actually no

way of initializing it and for this, we can only rely on the internal logic of the code. Clearly, although we feel confident that at least as far as reflooding is concerned, our modifications really capture the physics of the process and as such we can consider them as being of quite general validity, a much wider assessment with a large number of different transients is required before one can make general and definite statements about the capability of our modified code to correctly capture (and predict) the phenomena in system calculations not only during LB-LOCA's (an ability which we have already clearly demonstrated and on which we have spent considerable effort), but also during other transients. In particular, it would be of interest if cases analyzed with the frozen version of the code for which unphysical predictions have been obtained and reported, could be re-analyzed with our modified version. Though, since our physical model changes are mainly related to wall heat-transfer and interfacial shear (out of which the former is predominantly affecting the core response), we do not think that will have any pronounced effect in the analysis on Chapter 15 transients (malfunction of components etc).

An area which we also discussed in this work (though admittedly not to a great extent) is the effect of the very complex bubbly/slug interfacial shear package of the frozen version of the code on the unacceptably high large mass-error observed in the analysis of long SB-LOCA transients: We have shown that the complexity of this package is mainly responsible for this and the implementation in the code of the simple Ishii/Andersen model of TRAC-BF1 (if the component is not a bundle; in the bundle, the modified Bestion correlation is used) solves this problem not only in R5M3 but also in SCDAP. Clearly, the important lesson from this is that one should as much as possible avoid introducing very complex models and correlations in these codes, even if outside the code environment, they are proved to be superior to other, simpler models: In the complex environment of the code, the complexity of a model may excite local oscillations which can eventually adversely affect the final code predictions. Finally, we touched upon the influence of the degree of linearization of the interfacial shear term on the code predictions; although we did not actually show any concrete examples, we stated that our recent investigations have shown that under certain conditions, this effect can be quite profound. Saying this, we should stress once more that a drastic modification of the solution scheme in the code by introducing iterations is required before the excessive mass-error problem is properly addressed and solved.

Concluding, we should repeat that as we stated in the Introduction, we are not claiming that all problems associated with R5M3 are solved or that there are no other areas than the ones addressed in this work that attention is required. We believe that the process of improving the codes in general and in particular, R5M3, may actually have "never-ending tendencies", since there will always be transients which will exhibit some "unforseen peculiarity" which, somehow, would be desirable to "include" in the code in the form of a special model. In this respect, the effort to improve the predicting capabilities of the code is limited to doing one's best to correctly model the well-understood physical processes in a consistent and clear fashion, and in trying to avoid "numerics-related" problems like excitement

of unwanted oscillations or large mass-errors which may not only adversely affect the code predictions, but may also result in termination of a particular run due to bad convergence of the numerical solution or to water property error. Clearly, the former goal can be more easily achieved than the latter, since in a complicated entity like a transient thermal-hydraulics code, there may be a large number of competing effects which may excite unwanted oscillations. Independently of the various difficulties which may be encountered in the process of improving R5M3, we hope that we have clearly demonstrated in this work that with some systematic and co-ordinated effort and by following the procedure outlined in the Introduction, R5M3 can indeed be greatly improved (not only from the point of view of physical modeling) and confidently used also for LB-LOCAs. We certainly hope that in the future we shall be able to test (and hopefully, further qualify) our modified version of R5M3.1 with transients which were not successfully calculated with the frozen version of the code and assess the extent to which our modifications are improving its general predicting capability. Clearly, this is the only sensible way to realize the goal of gradually and systematically improving R5M3.1 in accordance with the philosophy outlined in the Introduction.

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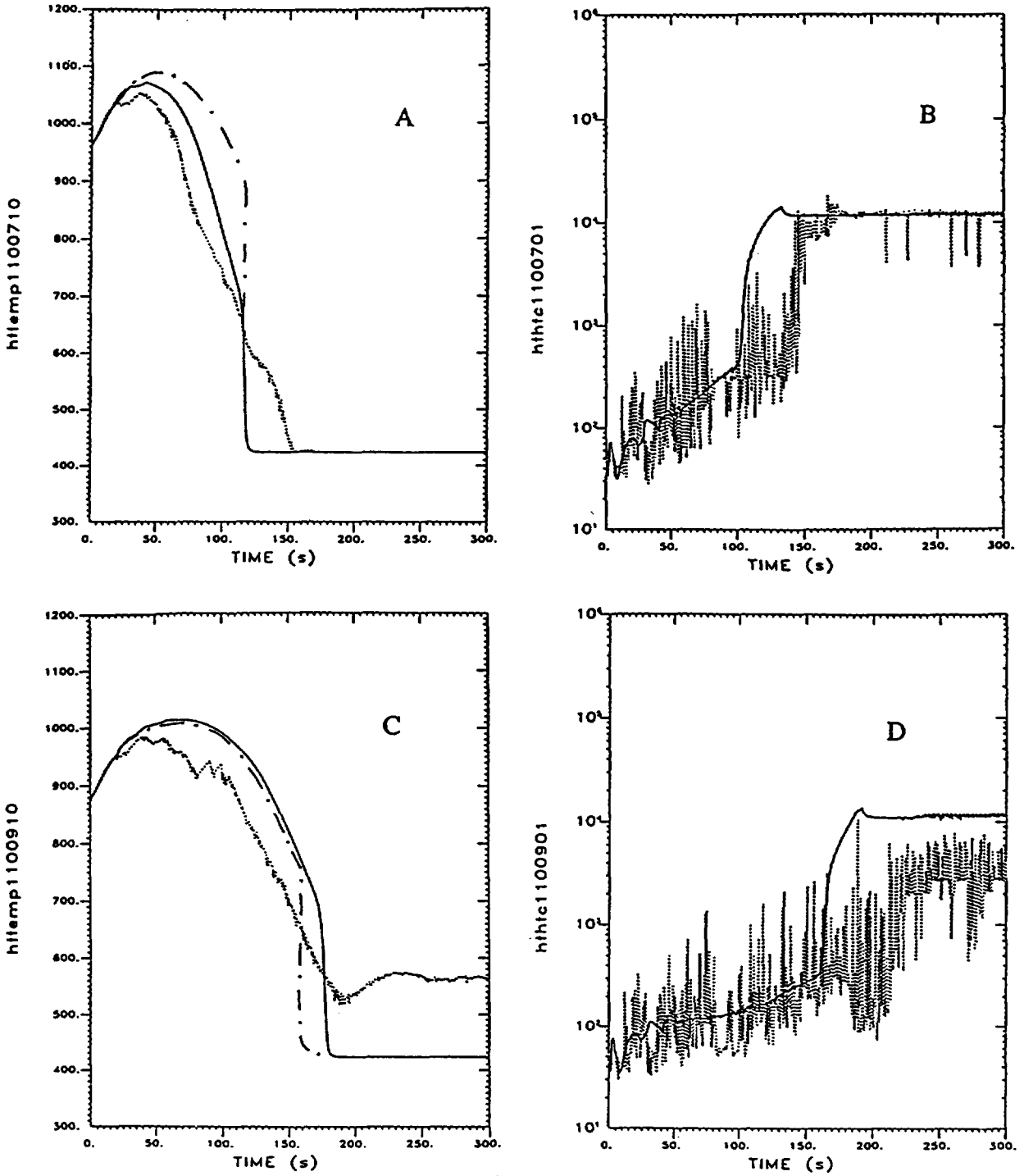

Fig.1 NEPTUN bottom flooding exp. Nr. 5036. RST (K) and HTC ($W/m^2/K$) at axial elevation 0.714 m (A, B) and 0.946 m (C, D). (——): Modified R5M3; (.....): R5M3 frozen version; (-.-.-): measurements.

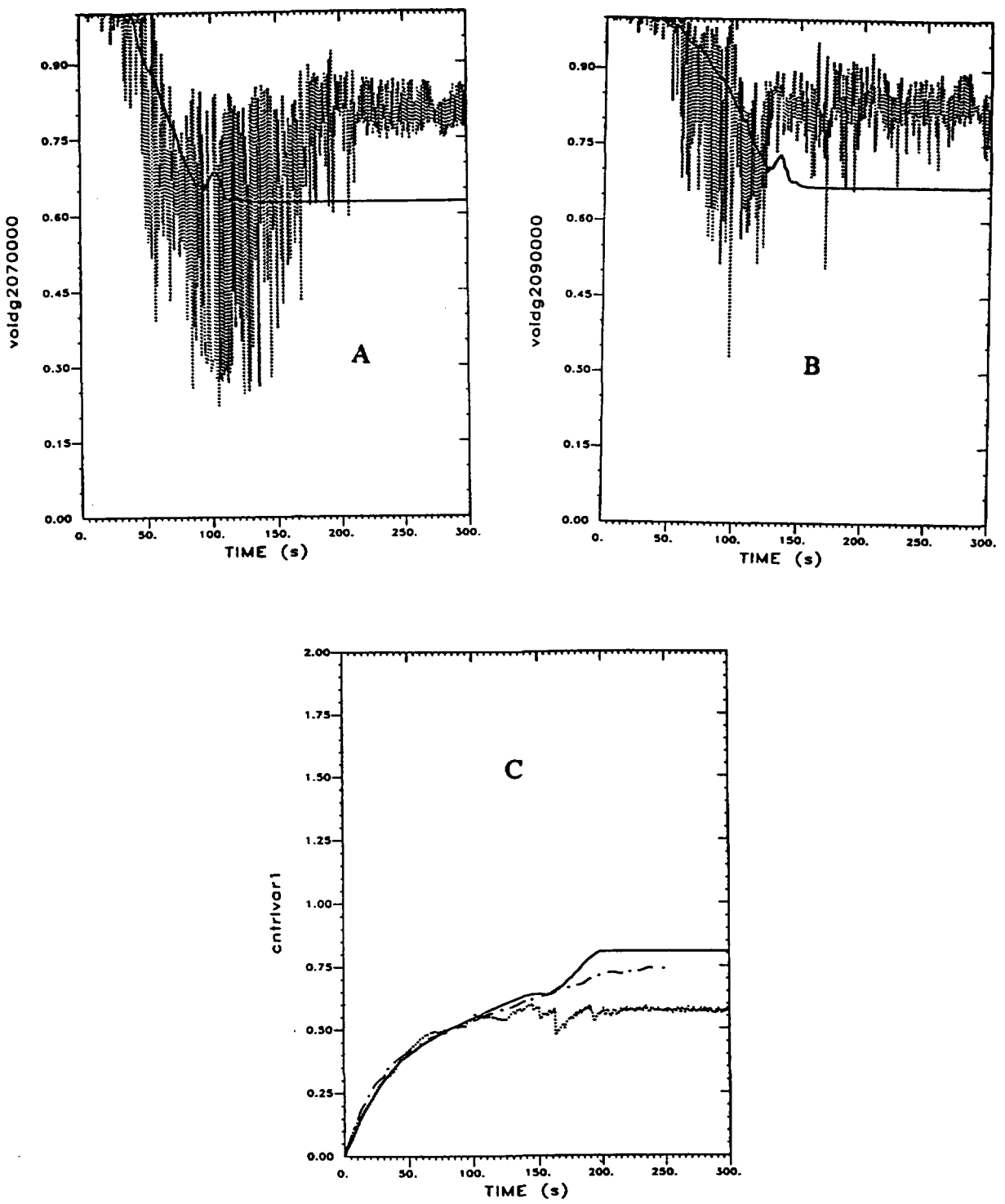

Fig. 2 NEPTUN bottom flooding exp. Nr. 5036. Predicted void fractions at two axial levels (A and B) and collapsed liquid level (m) in the core (C). (———): modified R5M3; (.....): R5M3 frozen version; (-.-.-.-.): measurements.

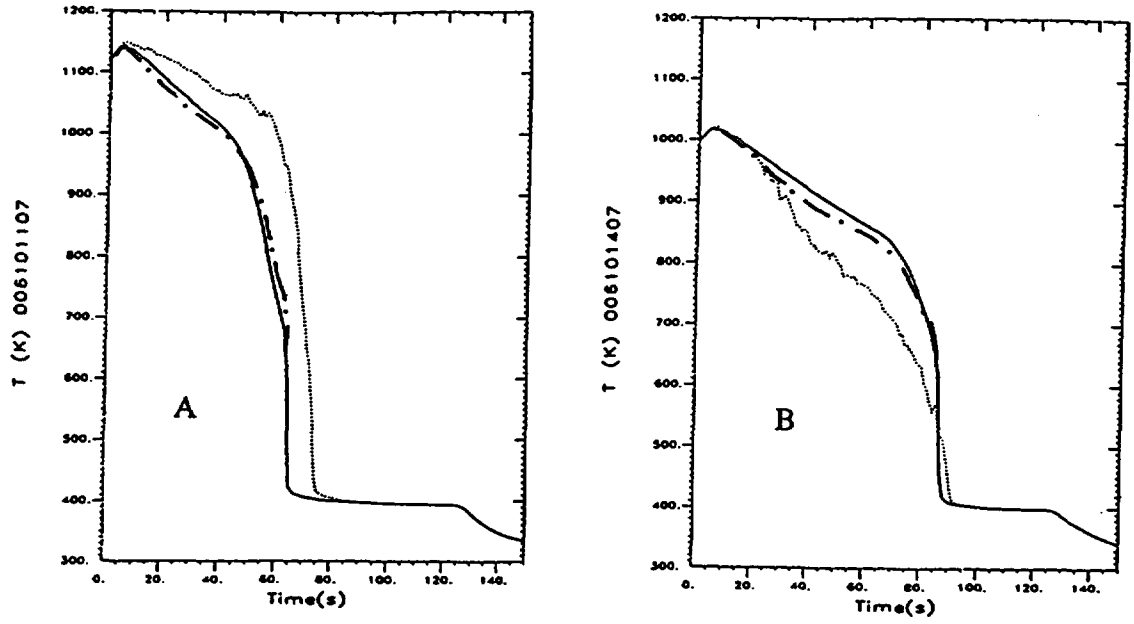

Fig.3 FLECHT-SEASET test Nr. 31701 Predicted RST (K) histories at axial elevations of elevations of 2.01 m (A) and 2.56 m (B). (——): modified R5M3; (.....): R5M3 frozen version; (-.-.-.): measurements.

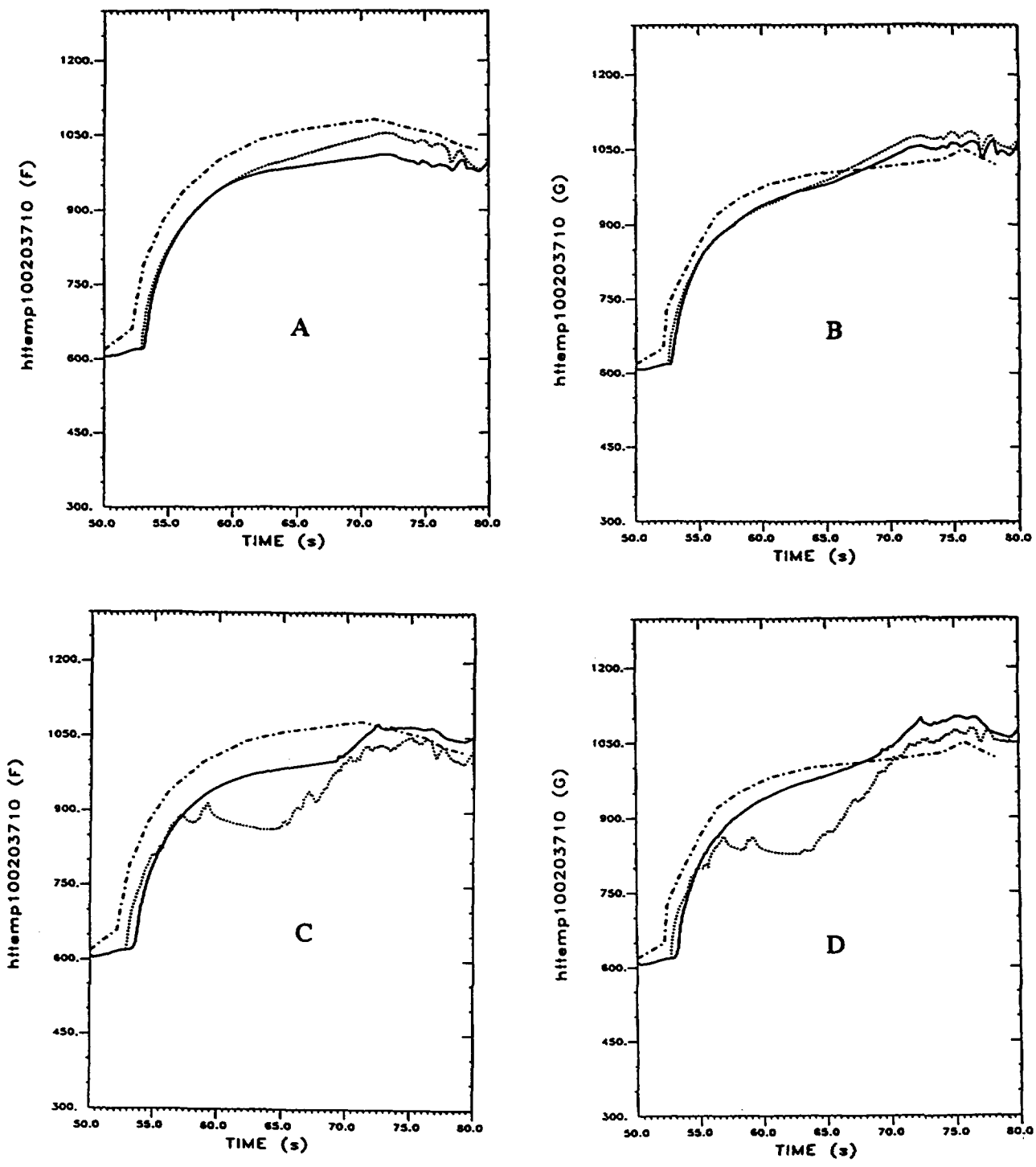

Fig. 4 THTF Test Nr. 3.08.6C. Measured (-.-.-) and predicted RST (K) histories at two axial levels with the semi-implicit (A and B) and nearly-implicit (C and D) solution schemes. (——): modified R5M3; (.....): R5M3 frozen version.

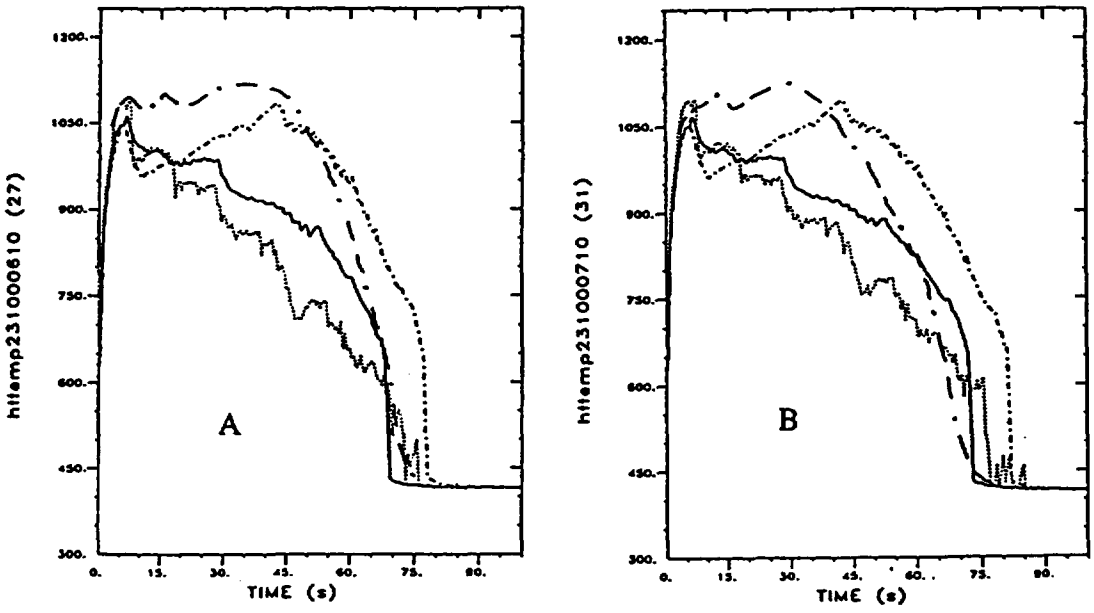

Fig.5 LOFT LP-LB-1 Test. Predicted RST (K) histories at axial elevations of 27 (A) and 31 (B) inches. (——): modified R5M3; (.....): R5M3 frozen version; (-.-.-.): modified R5M3 + Andersen's wet wall shear for pipes; (- -.- -.): measurements.

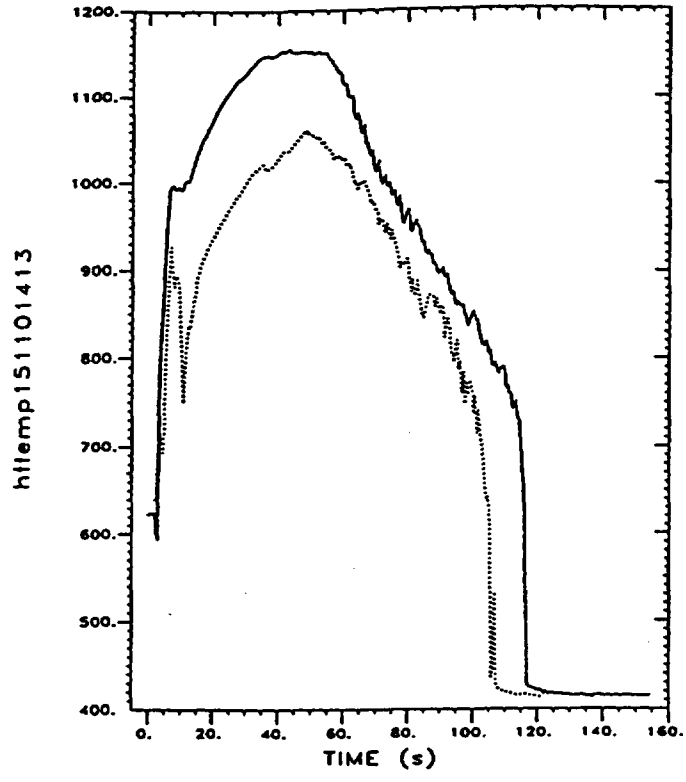

Fig.6 Predicted peak RSTs (K) for a hypothetical LB-LOCA in a Commercial PWR. (——): modified R5M3; (.....): R5M3 frozen version.

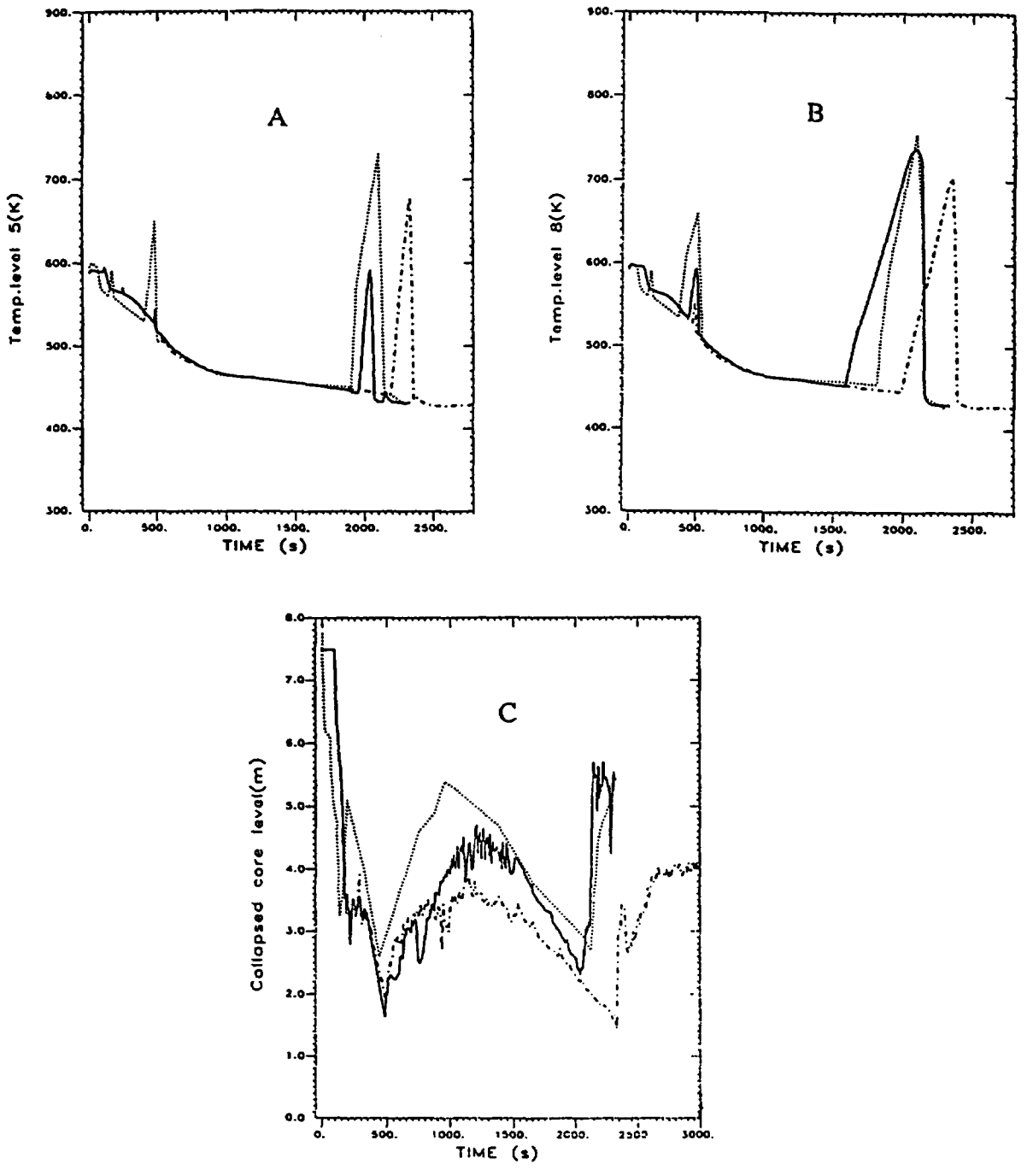

Fig. 7 LOBI BL34 SB-LOCA test. Predicted RST histories at two axial levels (A and B) and collapsed level in the core (C). (——): modified R5M3; (-.-.-.-.): R5M3 frozen version; (.....): measurements.

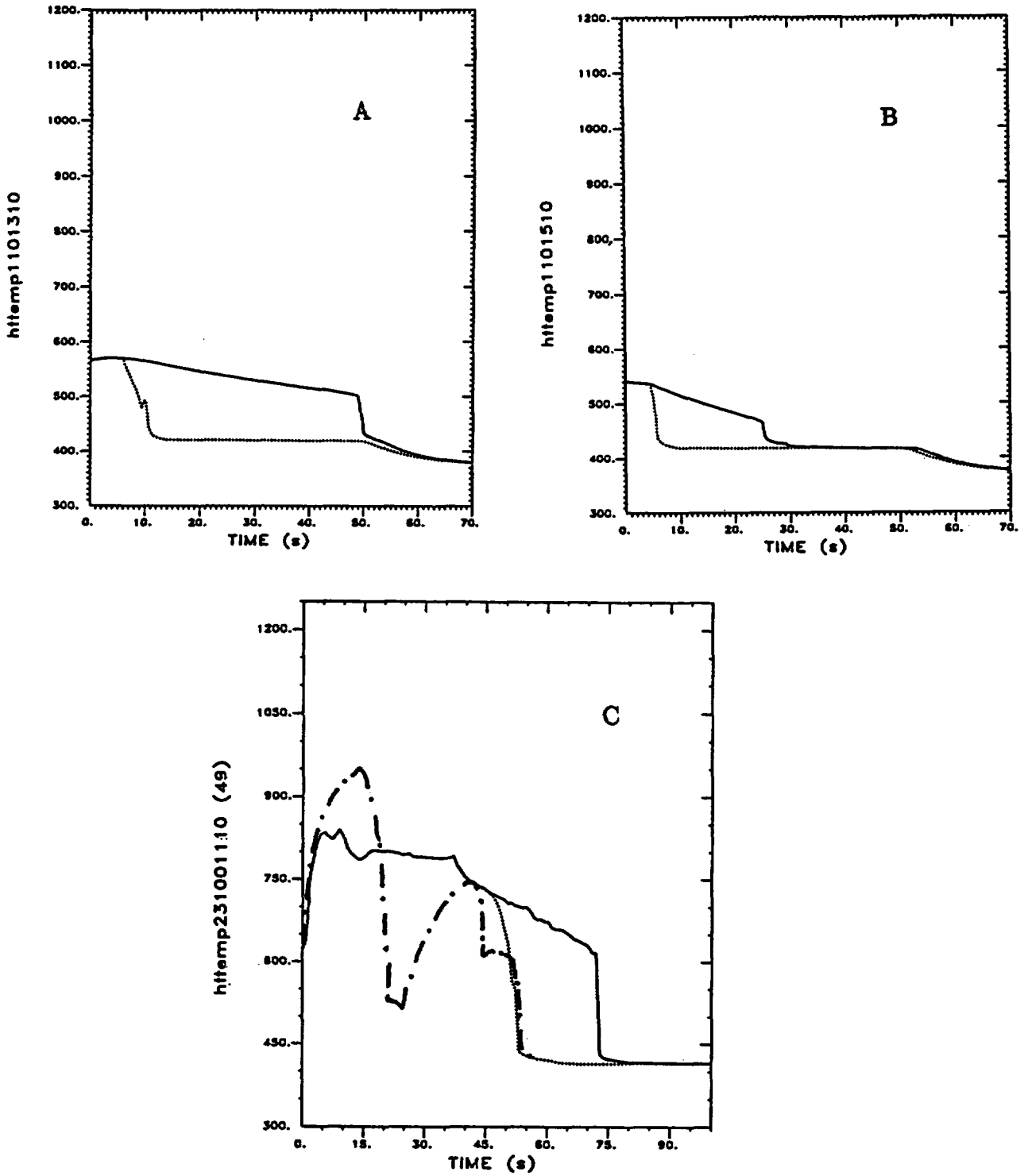

Fig. 8 Predicted RSTs in the upper regions of bundles during reflooding if the top QF model is activated (.....) or not (——). (A) and (B): NEPTUN bottom flooding exp. Nr. 5050 at axial elevations 1.41 m (A) and 1.647 m (B). (C) Predicted and measured (-.-.-.) RSTs at the 49 inches upper elevations in the LOFT bundle for the LP-LB-1 test.

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10. SUPPLEMENTARY NOTES

J. Uhle, NRC Project Manager

11. ABSTRACT *(200 words or less)*

A summary of modifications and options introduced in RELAP5/MOD3.1 (R5M3.1) is presented and is shown that the predicting capabilities of the modified version of the code are greatly improved, while the general philosophy we followed in arriving at these modifications is also outlined. These changes which are the same ones we implemented in the past version 7j of the code, include 2 different heat transfer packages (one of them activated during reflooding), modification of the low mass-flux Groeneveld CHF look-up table and of the dispersed flow interfacial area (and shear) as well as of the criterion for transition into and out from this regime, almost complete elimination of the under-relaxation schemes of the interfacial closure coefficients etc. The modified R5M3.1 code is assessed against a number of separate-effect and integral test experiments and in contrast to the frozen version, is shown to result in physically sound predictions which are much closer to the measurements, while almost all the predicted variables are free of unphysical spurious oscillations. The modifications introduced solve a number of problems associated with the frozen version of the code and result in a version which can be confidently used for both SB-LOCA and LB-LOCA analyses.

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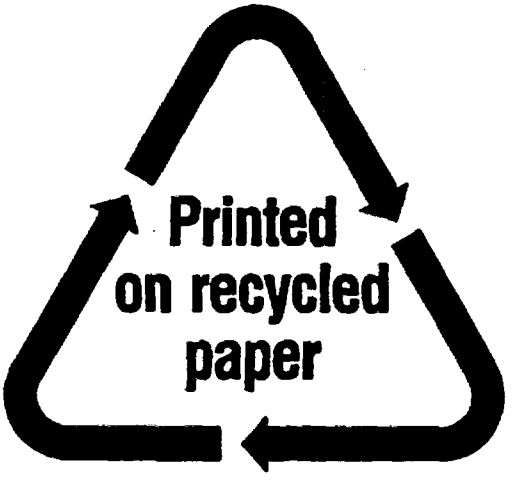

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