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International Agreement Report

Preliminary Assessment of PWR Steam Generator Modelling in RELAP5/MOD3

Prepared by
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Office of Nuclear Regulatory Research
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NOTICE

PRELIMINARY ASSESSMENT OF PWR STEAM GENERATOR MODELLING IN RELAP5/MOD3

R J Preece and J M Putney

Addressee: Mr P Lightfoot, NE PPG

SUMMARY

A preliminary assessment of Steam Generator (SG) modelling in the PWR thermal-hydraulic code RELAP5/MOD3 is presented. The study is based on calculations against a series of steady-state commissioning tests carried out on the Wolf Creek PWR over a range of load conditions. Data from the tests are used to assess the modelling of primary to secondary side heat transfer and, in particular, to examine the effect of reverting to the standard form of the Chen heat transfer correlation in place of the modified form applied in RELAP5/MOD2. Comparisons between the two versions of the code are also used to show how the new interphase drag model in RELAP5/MOD3 affects the calculation of SG liquid inventory and the void fraction profile in the riser.

Conclusions

1. Like RELAP5/MOD2, RELAP5/MOD3 under-predicts SG heat transfer for all load conditions examined (36% - 99%). If the code is initialised with the correct primary side conditions, this is reflected by an under-prediction of the secondary side pressure. The deficiency can be attributed to the inappropriate use of Chen correlation, which was developed using data from flows in tubes and annuli, to calculate the boiling heat transfer coefficient on the secondary side of the U-tube bundle. The errors in SG heat transfer are worse in MOD3 because the correlation is applied in its standard form, whereas MOD2 incorporates a modification which enhances heat transfer as the void fraction reduces.

2. For both versions of the code, the error in secondary side pressure tends to reduce as the reactor power reduces. The error in the MOD3 prediction is typically around 0.6 bar higher than the MOD2 error, and is about 4 bar at full load conditions.

3. A number of studies performed elsewhere have shown that RELAP5/MOD2 under-predicts SG secondary side liquid inventory if the downcomer level and recirculation ratio are calculated correctly. The deficiency has been attributed to an over-prediction of void fraction in the SG bundle, caused by an over-prediction of the interphase drag force. The Wolf Creek calculations performed here indicate that the new interphase drag models in RELAP5/MOD3 result in a 25% increase in SG inventory at full load conditions. The new models also lead to changes in the void fraction profiles predicted for the SG riser which can be explained in terms of the variations in SG geometry. Unfortunately, no Wolf Creek data are available to check the MOD3 results in these areas.

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1 INTRODUCTION

A previous Report (Putney and Preece, 1991) described an assessment of steam generator (SG) modelling in the PWR thermal-hydraulics code RELAP5/MOD2 (Ransom, Wagner, Trapp, Johnsen, Miller, Kiser and Riemke, 1987) - which is currently being used by Nuclear Electric for the analysis of small break LOCAs and intact primary circuit fault transients in the Sizewell 'B' PWR. The assessment was based on a review of code assessment calculations performed in the UK and elsewhere, detailed calculations against a series of commissioning tests carried out on the Wolf Creek PWR and analytical investigations of the phenomena involved in normal and abnormal SG operation. A number of modelling deficiencies were identified and their implications for PWR safety analysis were discussed. The most significant deficiencies were associated with the modelling of primary to secondary side heat transfer, secondary side liquid inventory and level behaviour. These deficiencies were traced to errors in the calculation of boiling heat transfer and interphase drag on the secondary side of the U-tube bundle.

In January 1990, the RELAP5/MOD3 code was released as a potential successor to RELAP5/MOD2. Amongst other things, the new version of the code includes model improvements in the area of interphase drag and uses a revised correlation to represent boiling heat transfer. Changes in the predictions of PWR SG behaviour may therefore be expected.

The present Report describes a preliminary assessment of SG modelling in RELAP5/MOD3, based on calculations against the Wolf Creek commissioning tests considered in the MOD2 assessment study.

The Wolf Creek PWR is one of two fully operational PWRs based on the Westinghouse SNUPPS design, which is the basis for the Sizewell 'B' PWR. The test data used for the present study were obtained during commissioning trials on the station involving measurements on a specially instrumented SG operating under steady-state conditions at various load conditions (36 - 99% full power). The calculations were performed using RELAP5/MOD3 version 5m5 running on an IBM RS6000 work station.

2 BACKGROUND TO RELAP5/MOD3

RELAP5/MOD3 contains a number of modelling changes from the MOD2 version of the code. Those affecting SG modelling concern the interphase drag and boiling heat transfer models.

2.1 Interphase Drag Model

The modelling of interphase drag in RELAP5/MOD2 has been the subject of much criticism in the past and significant changes were introduced in the MOD3 version of the code.

The deficiencies in the MOD2 models that affect SG modelling may be summarised as follows

1. For low flow conditions in rod/tube bundles, there is considerable experimental and theoretical evidence to show that RELAP5/MOD2 over-predicts the interphase drag force and thus void fraction Putney (1988; 1989b). The deficiency arises because the code assumes that the flow regime that exists in a bundle is identical to that in a tube having the same hydraulic diameter. As a consequence, a slug flow model is used for the interphase drag calculation until a transition to annular flow is predicted to occur. Observations made by Bestion (1985) at low void fraction however, indicate a quite different flow regime for a bundle, in which

the vapour and liquid phases have a tendency to occupy separate flow paths. Also, at higher void fractions, Venkateswararao, Semiat and Dukler (1982) found that under certain circumstances, large Taylor bubbles can occupy several subchannels. As a result, and confirmed by measurements, the interfacial drag force in a bundle is much less than that associated with slug flow. This fundamental deficiency in the code is exacerbated by the use of a slug flow model which neglects the effects of profile slip.

2. The code's slug flow model is also inappropriate for the case of low flow in a large diameter pipe (e.g. the unheated part of the SG riser), because the Taylor bubbles are assumed to have a diameter equal to the pipe diameter. In reality however, bubbles of this size cannot be sustained due to interfacial stability and they disintegrate into cap bubbles (Kataoka and Ishii, 1987). Since the specific area of a Taylor bubble is inversely proportional to its diameter, the assumption of slug flow in this situation can lead to an **under-prediction** of interphase drag when the proportion of vapour in the Taylor bubble (relative to the small bubbles) becomes significant - and the consequences of neglecting profile slip are outweighed. This effect is clearly demonstrated by Putney (1988; 1989b).

The review of RELAP5/MOD2 assessment calculations performed by Putney and Preece (1991) found clear evidence to show that RELAP5/MOD2 under-predicts SG liquid inventory under steady-state conditions, when the recirculation ratio and downcomer level are calculated correctly. The deficiency was attributed to an over-prediction of void fraction in the bundle region, caused by the over-prediction of the interphase drag force. It was noted however, that the over-prediction of void fraction in the bundle could be compensated to some extent by an under-prediction in the unheated riser (which consists of a circular channel having a diameter of around 3.6 m). This effect is unlikely to be present at full power conditions as annular flow is generally predicted to exist in the unheated region (and thus deficiencies in the slug flow model will have no effect), but could become important at lower power conditions.

RELAP5/MOD3 includes improved interphase drag models for low flow conditions in both rod bundles and large diameter pipes (Putney, 1989b). Both models are based on drift flux correlations derived from experimental data appropriate to the conditions concerned - the former on the EPRI drift flux correlation (Chexal and Lellouche, 1986), the latter on the Kataoka-Ishii correlation (Kataoka and Ishii, 1987). The code may therefore be expected to provide a better prediction of SG liquid inventory than RELAP5/MOD3.

Other changes related to interphase drag modelling introduced in RELAP5/MOD3 include a move to a junction based drag calculation (rather than volume averaged) (Riemke, 1989), and modifications to the annular flow transition criteria (Putney, 1989a). These changes however, are unlikely to have a significant effect on the calculation of SG inventory.

The present study compares RELAP5/MOD3 calculations of steady-state SG behaviour against data obtained from a series of commissioning tests carried out on the Wolf Creek PWR. Unfortunately, these tests did not include measurements of SG liquid inventory. However, the code's predictions of this parameter have been compared with those obtained from RELAP5/MOD2 to show the changes resulting from the new interphase drag models.

2.2 Boiling Heat Transfer

For saturated and subcooled boiling, RELAP5/MOD2 applies a **modified** form of the Chen correlation (Chen, 1966) to calculate the wall to fluid heat transfer coefficient. The principal modification introduced has the effect of enhancing heat transfer as the void fraction reduces. This modification has been removed in RELAP5/MOD3.

For saturated boiling (i.e. saturated nucleate boiling and forced convection vaporization), the standard form of the Chen correlation is

$$q''_w = H_{mic}(T_w - T_s) + H_{mac}(T_w - T_f) \tag{1}$$

where

$$H_{mic} = \left[\frac{0.00122\, k_f^{0.79}\, C_{pf}^{0.45}\, \rho_f^{0.49}\, \Delta T_{sat}^{0.24}\, \Delta P_{sat}^{0.75}}{\sigma^{0.5}\, \mu_f^{0.29}\, h_{gf}^{0.24}\, \rho_g^{0.24}}\right] S \tag{2}$$

$$H_{mac} = \left[0.023\, \frac{k_g}{D_e}\, Pr_f^{0.4}\, Re_f^{0.8}\right] F \tag{3}$$

$$Re_f = \frac{(1 - x)\, G\, D_e}{\mu_f} \tag{4}$$

$$\begin{aligned} F &= 1.0 & X_{tt}^{-1} &\le 0.1 \\ &= 2.35\,(X_{tt}^{-2} + 0.213)^{0.736} & X_{tt}^{-1} &> 0.1 \end{aligned} \tag{5}$$

$$X_{tt}^{-1} = \left(\frac{x}{1 - x}\right)^{0.9} \left(\frac{\rho_f}{\rho_g}\right)^{0.5} \left(\frac{\mu_g}{\mu_f}\right)^{0.1} \quad \text{Martinelli flow parameter} \tag{6}$$

$$\begin{aligned} S &= [1 + 0.12\,(Re_{TP})^{1.14}]^{-1} & Re_{TP} &< 32.5 \\ &= [1 + 0.52\,(Re_{TP})^{0.78}]^{-1} & 32.5 \le Re_{TP} &< 70.0 \\ &= 0.1 & Re_{TP} &\ge 70.0 \end{aligned} \tag{7}$$

$$Re_{TP} = \frac{(1 - x)\, G\, D_e}{\mu_f}\, F^{1.25} \times 10^{-4}$$

and the rest of the notation is defined in the Nomenclature Section. Note that for saturated boiling, $T_f = T_s$ (essentially), and thus equation (2) may be written

$$q''_w = (H_{mic} + H_{mac})(T_w - T_s) \tag{8}$$

The term H_{mic} in the Chen correlation represents the contribution to heat transfer due to nucleate boiling, or microconvection, and is derived from the Forster-Zuber correlation for pool boiling (Forster and Zuber, 1955). The factor S in equation (3) accounts for a suppression of nucleate boiling under forced convection conditions and increasing flow rate, due to a reduction in the thermal boundary layer thickness and, consequently, a degradation of the conditions for growth of vapour bubbles. The term H_{mac} in the Chen correlation represents the contribution from single phase convection, or macroconvection, and is based on the Dittus-Boelter (Dittus and Boelter, 1930) equation with a modifying factor F to account for an enhancement of heat transfer due to an increase in the mixture velocity.

For subcooled boiling, the Chen correlation is generally applied with the following modifications

$$F = 1, \qquad Re_{TP} = \frac{G\, D_e}{\mu_f} \tag{9}$$

Also, in equation (1), T_f is no longer equal to T_s.

The principal modification to the Chen correlation introduced in RELAP5/MOD2 is to replace the suppression factor S by the modified suppression factor, S^*:

$$\begin{aligned} S^* &= 1 & \alpha &\le 0.3 \\ &= 1 - 2(1 - S)(\alpha - 0.3) & 0.3 < \alpha &\le 0.8 \\ &= S & \alpha &> 0.8 \end{aligned} \tag{10}$$

This has the effect of increasing heat transfer for void fractions below 0.8.

The code assessment exercise performed by Putney and Preece (1991) clearly established that RELAP5/MOD2 under-predicts SG heat transfer under steady-state normal operating and start-up conditions. If the code is initialised with the correct primary side conditions, this will be reflected by an under-prediction of the secondary side pressure. The effect is seen in both plant and rig calculations, although it tends to reduce as both reactor power and scale reduce. The deficiency was attributed to the inappropriate use of Chen correlation to calculate the boiling heat transfer coefficient on the secondary side of the U-tube bundle. In particular, the correlation was developed using data from flows in tubes and annuli. The evidence available however, indicates that boiling heat transfer in a full size SG bundle at full load conditions, is much greater than that in a tube having an equivalent heated diameter. Although, as shown above, RELAP5/MOD2 incorporates a modification to this correlation which enhances heat transfer as the void fraction reduces, the enhancement is not sufficient for plant calculations. (It also exacerbates an incorrect trend with bundle elevation.)

In RELAP5/MOD3, the suppression factor in the Chen correlation is applied in its standard form. The reason for doing this is not clear, apart from a general desire by the code developers to return all correlations to their literature form if the reasons for modifications are not known. As a result of this change, the tendency for the code to under-predict SG heat transfer can be expected to be worse than that associated with RELAP5/MOD2. This effect is investigated in Section 5.1 using the Wolf Creek data.

3 WOLF CREEK COMMISSIONING TESTS

3.1 Description of Tests

In 1978 Westinghouse and the SNUPPS utilities agreed to sponsor a joint test programme to provide detailed measurements of the thermal- hydraulic performance characteristics of one of the first Model 'F' SGs to go into commercial service. The Wolf Creek generating station was chosen to be the site of this test programme.

Wolf Creek is one of two fully operational PWRs based on the Westinghouse Standardised Nuclear Unit Power Plant System (SNUPPS) design, which is the basis for the Sizewell 'B' PWR. The plant consists of 4 loops and has a rated thermal power of 3425 MW. The SGs are Westinghouse U-type Model 'F'. The Nuclear Steam Supply System (NSSS) is essentially the same as Sizewell 'B', although there are some differences in the Balance Of Plant (BOP).

During the commissioning tests measurements were concentrated on a single steam generator which had been constructed with thirteen additional ports to take special instrumentation for internal secondary side measurements. The data recorded (Curlee and Preece, 1987) for each test condition represent plant measurements averaged over 10 minutes when the plant was operating under nominal steady state conditions. This covers loads from 23% to full load, together with a number of loads below 10%. Unfortunately, due to feed flow instability problems, it was not possible to achieve steady state conditions in the load range up to 23%.

Details of the measurements made in the tests and the parameters derived from them are given by Putney and Preece (1991). Parameters relevant to the present study include

Primary side	reactor power pressurizer pressure hot and cold leg temperatures fluid flow rate
Secondary side	SG exit pressure feedwater flow and inlet temperature steam flow downcomer water level recirculation ratio

The SG exit pressure corresponds to the pressure at the inlet to the steam flow restrictor nozzle in the upper dome of the SG, and provides an important quantity for assessing SG heat transfer modelling in RELAP5. This parameter was obtained from measurements of the steam pressure in the steam main outside the containment boundary, after taking account of the steam line losses. The pressure measurements were normally made using three equi-spaced pressure transducers. However, for some of the earlier tests, these were supplemented by measurements from more accurate direct-reading Heise gauges mounted close by. For the present study, the test runs containing the Heise gauge values are the preferred ones because of the relatively high precision obtainable ($\pm$0.1bar compared with $\pm$0.45bar for the pressure transducers). From standard error analysis, Putney and Preece (1991) found that the uncertainty in the steam exit pressure was $\pm$0.143 % at 36.1 % reactor power, increasing to $\pm$0.147 % at full power.

Other parameters of interest which were derived from the plant measurements were the primary fluid flow and the recirculation ratio. The primary fluid flow was deduced from measurements of the hot and cold leg temperatures and the reactor power (NSSS trend block data, process and control instrumentation). However, because the cold leg temperature was measured downstream of the reactor coolant pump, a temperature correction was required to account for the heat input from pumping. The calculation of the circulation ratio, which was based on mass flow rather than enthalpy flow, took due account of the blowdown flow.

3.2 Tests Analysed

From the datasets recorded by Curlee and Preece (1987), five test cases were selected for detailed analysis with RELAP5/MOD3. The operating conditions covered by these tests are summarised in Table 1. The values given in the table also include the input values required to run RELAP5.

The tests selected for analysis were identical to those used by Putney and Preece (1991) for their RELAP5/MOD2 assessment, and covered a wide range of load conditions (36%-99% of full rated power). Only tests in which steam pressures were measured using the Heise gauge were used. Very low load conditions were avoided because of the associated control system instability, which placed in doubt the achievement of steady-state conditions.

4 RELAP5 MODEL

For each of the tests selected, RELAP5/MOD3 was applied to simulate the thermal-hydraulic performance of a single SG. The input decks used for the analyses were created from the MOD2 decks used in the previous study by running the automatic conversion program RICO (Ward, 1990)[1]. Additional changes were introduced manually to set the junction hydraulic diameters equal to the hydraulic diameters of their upstream volumes (as the default setting was not always appropriate), and to invoke the rod bundle interphase drag model on the secondary side of the U-tube bundle (including the U-bend). It was also noted that RICO had set the axial boiling factors on the 'additional' heat structure boundary cards to be 0.0, whereas the default value suggested in the input data description is 1.0. However, when the problem was run, RELAP5/MOD3 overrode these values and set them to 0.0.

The nodalisation scheme used for the calculations is shown in Fig. 1 and encompasses the primary side pump suction leg and hot leg elements, the secondary side main feed and the SG steam main element. Excluded from the present model are the main steam inlet valve, steam relief valves and the power operated relief valve. The auxiliary feed is also omitted as is the primary side recirculation pump.

In the model, the primary side flow and hot leg temperature are represented by a time dependent junction, TDJ, and a time dependent volume, TDV, connected to the steam generator inlet plenum. Pressure within the TDV is set equal to that in the inlet plenum. The pump suction pressure is represented by a TDV connected to the steam generator outlet plenum.

On the secondary side, the main feedwater flowrate and temperature are represented by a TDJ and TDV[2]. The main feed flow through the TDJ is varied to achieve the desired water level in the steam generator. To do this the adjusted flow is set equal to the specified flow in addition to a term based on the level error. The steady state steam outlet flow is represented by a TDJ connecting the top of the steam dome to a dummy steam main TDV, with steam flow set equal to the specified main feed flow. With this arrangement the dummy steam main pressure has no effect on the steam generator performance.

The required circulation ratio is achieved by adjusting the downcomer flow. The downcomer flow is itself controlled by adjusting the friction losses at the junction between the downcomer and riser by varying the junction flow area.

The boundary conditions for the SG model are therefore specified as follows:

1. Hot leg temperature
2. Primary flow
3. Cold leg pressure
4. Feed water temperature
5. Feed water flow
6. Desired SG narrow range water level
7. Desired downcomer flow

These may be obtained from Table 1

The calculations were performed using RELAP5/MOD3 version 5m5 running on an IBM RS6000 workstation at NPTEC.

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[1] The MOD2 deck had been derived from the two-loop Sizewell 'B' whole plant deck described by Harwood (1986). This was felt to be an acceptable approach as the geometric and material characteristics of the Wolf Creek and Sizewell 'B' SGs are essentially identical for simulation purposes. The deck was created by extracting the data cards for the broken loop SG and introducing appropriate boundary conditions to represent the rest of the NSSS.

[2] The feedwater pressure is set to a nominal value, which was felt to be adequate, as this pressure only affects the local fluid properties.

As with the earlier RELAP5/MOD2 calculations, the initial calculations with RELAP5/MOD3 were made using a requested time step of 0.25s. Both the MOD2 and MOD3 calculations showed a Courant time step of around 0.2s. However, whereas the MOD2 time step controller obeyed this limit and reduced the calculational time step to 0.125s, the MOD3 calculation allowed the requested time step to be used. For an 81% load test, the larger time step in the MOD3 calculation resulted in large oscillations in the predicted values of several SG parameters - e.g. the downcomer flow varied between 1400 and 2000 kg s^{-1}. These oscillations were subsequently eliminated completely by imposing a maximum (requested) time step of 0.125s. Consequently, it was decided to force the MOD3 calculations to use the same time step as the MOD2 calculations.

The only other difference in the manner in which the RELAP5/MOD2 and RELAP5/MOD3 calculations were performed concerns the adjustment of flow area at the junction between the downcomer and riser to achieve the desired circulation flow. In the MOD2 calculations, this was done automatically using a control system. Due to numerical difficulties, this approach was not successful in the MOD3 calculations, and the appropriate flow area had to be determined by trial and error.

5 CODE PERFORMANCE

5.1 Steam Pressure

The difference between the calculated steam pressure in the SG dome and that deduced from the Wolf Creek measurements is shown in Fig. 2 as a function of reactor power. The calculated steam pressure in this case is that evaluated at the nodal element 612-3 in Fig. 1, which is compared with the plant value corresponding to the inlet plane to the steam flow restrictor nozzle. Two curves are shown, corresponding to the RELAP5/MOD3 and MOD2 predictions. In both cases, the SG pressure is under-predicted, implying an under-prediction of primary to secondary side heat transfer, with the error in pressure increasing monotonically with load. As expected the use of the standard Chen heat transfer correlation in MOD3 results in a greater under-prediction of. pressure than MOD2. At full load conditions, MOD3 under-predicts the pressure by almost 4 bar, with the error in pressure dropping to about 1 bar at 36% load. This error is consistently about 0.6 bar greater than the MOD2 error.

5.2 Liquid Inventory

As discussed earlier, the interphase drag model used in RELAP5/MOD2 results in an under-prediction of SG liquid inventory under steady-state conditions, when the recirculation ratio and downcomer level are calculated correctly. The effect of the new interphase drag model in MOD3 can be seen in Fig. 3, which shows the ratio of the SG liquid masses obtained from the MOD2 and MOD3 calculations. For the 73%, 81% and 99% load cases, the new interphase drag model results in an increase in inventory of around 25%. For the 51% and 36% load cases however, there is little change in the predicted inventory.

The trends shown in Fig. 3 can partly be explained by the fact that as the reactor power reduces, so does the SG vapour generation rate, and thus errors in interphase drag modelling will have an smaller impact on the predicted SG inventory. The errors in the MOD2 interphase drag modelling in bundles are also less significant at lower void fractions.

A more detailed examination of the RELAP5/MOD2 results reveals that, for the 36% and 51% load cases, slug flow was predicted to occur in the unheated section of the

SG riser (nodal element 604-6 in Fig. 1) and the separator inlet volume (nodal element 606). At the higher load conditions however, annular-mist flow was predicted in both cases. In the RELAP5 model, the unheated riser section is represented as a circular channel of length 2.9 m having a hydraulic diameter of 3.6 m; while the separator inlet is represent as a circular channel of length 1.4 m having a hydraulic diameter of 0.5 m. As discussed in Section 2.1, the assumption of slug flow in such volumes could result in an under-prediction of the local void fraction, which for the inventory calculation, would tend to compensate for the over-prediction of void fraction in the bundle. Although this effect could also explain the trend shown in Fig. 3, Section 5.3 shows that the void fraction profiles calculated for the SG riser do not support this conclusion (the low vapour generation rate would appear to be the dominant effect). However, as also discussed in Section 5.3, for the lower load conditions, RELAP5/MOD2 does tend to under-predict void fraction in the upper part of the heated bundle, and this could be linked to the assumption of slug flow in the unheated riser and the use of volume averaging to calculate the interphase drag force at a junction. Moreover, for intermediate load conditions between the 51% and 73% load cases examined here, a cancellation of void fraction errors could well lead to a reasonable prediction of SG inventory.

5.3 Void Fraction

The influence of the new interphase drag model in RELAP5/MOD3 is also reflected in the void fractions predicted by the two versions of the code for SG riser. These are shown in Fig. 4 to Fig. 8. Not only are the magnitude of the void fraction predictions different, but the void profiles are also quite different.

For the higher load conditions, 73% and above, the voidage predicted by MOD3 becomes progressively lower than that predicted by MOD2 as the elevation above the tube sheet increases. Moreover, at the higher elevations, MOD3 predicts a reduction in voidage with increasing elevation, resulting in a convex-type profile, while MOD2 predicts a continual increase in voidage.

More detailed examination of the results reveals that, for the 73% load case, both codes predict a transition from bubbly-slug[3] to annular-mist flow between the 4th and 5th nodes in the riser [4], and for the 99% load case, the transition is predicted to take place between nodes 3 and 4. For the 81% load case, RELAP5/MOD2 predicts a transition between nodes 3 and 4, while RELAP5/MOD3 predicts a transition between node 4 and 5. Interestingly, in all cases, RELAP5/MOD3 (but not RELAP5/MOD2) predicts a transition back to bubbly-slug flow as the mixture enters the unheated section of the riser and the hydraulic diameter increases from 1.7 cm to 3.6 m - with a return to annular flow when the mixture enters the separator inlet region (hydraulic diameter of 0.5 m). Physically, this implies that as the channel width increases, the vapour confined within annular core redistributes itself into cap bubbles near the maximum stable size (which is physically reasonable). As a result of this transition, RELAP5/MOD3 predicts a reduction in the local void fraction, indicating that the consequent increase in specific interfacial area (i.e interfacial area per unit volume) causes an overall increase in interphase drag. The slight reduction in void fraction in passing from nodes 4 to 5 in the MOD3 calculations is probably due to a reduction in flow area in node 5 arising from the presence of the U-bend, causing an increase in the mixture flow rate.

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[3] In RELAP5/MOD3 solution, the interphase drag model applied in the bubbly-slug regime is based on the EPRI drift flux model in the bundle region and the Kataoka-Ishii model in the unheated riser. In the RELAP5/MOD2 solution, a slug flow model is applied in the bubbly-slug regime for all regions of the riser.

[4] Nodes 1 - 4 represent the straight section of the heated bundle, node 5 represents the U-bend and node 6 represents the unheated section of the bundle.

For the 36% and 51% load cases, both codes predict slug flow throughout the riser, although RELAP5/MOD3 still predicts a reduction in voidage as the mixture enters the unheated section of the riser (and the vapour 'bubbles' confined within the channels expand to form cap bubbles). The void fractions predicted by each code are very similar over the first three nodes, but then the MOD2 voidage falls below the MOD3 value and stays relatively constant. On the surface, the results do not appear to indicate a significant under-prediction of void fraction by MOD2 in the unheated section of the riser, due to the inappropriate assumption of slug flow (see Section 5.2). However, it is possible that the volume-averaged calculation of interphase drag at each junction in MOD2 has caused this effect to be smeared over the upstream nodes - and this could be the reason why the voidage profile above node 3 is relatively flat.

The void fraction predictions shown in Fig. 4 to Fig. 8 are are consistent with the trends shown in Fig. 3.

6 CONCLUSIONS

1. Like RELAP5/MOD2, RELAP5/MOD3 under-predicts SG heat transfer for all load conditions examined (36% - 99%). If the code is initialised with the correct primary side conditions, this is reflected by an under-prediction of the secondary side pressure. The deficiency can be attributed to the inappropriate use of Chen correlation, which was developed using data from flows in tubes and annuli, to calculate the boiling heat transfer coefficient on the secondary side of the U-tube bundle. The errors in SG heat transfer are worse in MOD3 because the correlation is applied in its standard form, whereas MOD2 incorporates a modification which enhances heat transfer as the void fraction reduces.

2. For both versions of the code, the error in secondary side pressure tends to reduce as the reactor power reduces. The error in the MOD3 prediction is typically around 0.6 bar higher than the MOD2 error, and is about 4 bar at full load conditions.

3. A number of studies performed elsewhere have shown that RELAP5/MOD2 under-predicts SG secondary side liquid inventory if the downcomer level and recirculation ratio are calculated correctly. The deficiency has been attributed to an over-prediction of void fraction in the SG bundle, caused by an over-prediction of the interphase drag force. The Wolf Creek calculations performed here indicate that the new interphase drag models in RELAP5/MOD3 result in a 25% increase in SG inventory at full load conditions. The new models also lead to changes in the void fraction profiles predicted for the SG riser which can be explained in terms of the variations in SG geometry. Unfortunately, no Wolf Creek data are available to check the MOD3 results in these areas.

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8 NOMENCLATURE

The following lists the general notation used throughout the Report. Other symbols used are defined when they appear.

All variables relate to a 1D (channel averaged) flow. SI units are assumed throughout.

k	Phase index = g(vapour)/f(liquid)
α_k	Volume fraction ($\alpha \equiv \alpha_g$)
ρ_k	Density
h_{ks}	Specific enthalpy at saturation conditions
h_{gf}	Latent heat of vaporisation ($h_{gf} = h_{gs} - h_{fs}$)
T_k	Temperature
T_s	Saturation temperature
T_w	Wall temperature
ΔT_{sat}	$= T_w - T_s$
ΔP_{sat}	Difference between saturation pressure at T_w and saturation pressure at T_s
q''_w	Wall heat flux
μ_k	Dynamic viscosity
k_k	Thermal conductivity
C_{pk}	Specific heat at constant pressure
Pr_k	Prandtl number ($= C_{pk}\,\mu_k / k_k$)
σ	Surface tension
v_k	Velocity
j_k	Superficial velocity ($j_k = \alpha_k v_k$)
G	Mixture mass flux ($G = \rho_g\, j_g + \rho_f\, j_f$)
x	Quality
D_e	Equivalent heated diameter

Test Run Number	WC046	WC047	WC049	WC051	WC068
Reactor Power, %	**36.1**	**50.5**	**73.3**	**81.4**	**98.9**
Feedwater temperature, K	452.6	467.3	484.8	489.3	496.9
Feedwater flow, kg/s	152.5	221.5	335.3	376.6	467.1
Total downcomer flow, kg/s	1554.9	1642.7	1686.6	1680.5	1684.4
Blowdown flow, kg/s	5.23	5.14	5.03	5.36	3.91
Primary hot leg temperature, K	576.1	581.5	589.4	591.9	597.0
Primary fluid pressure, bar	154.77	154.77	155.18	155.18	155.32
Primary fluid flow, kg/s	4592.0	4675.3	4666.0	4612.8	4745.0
SG water level, %	50	50	50	50	49
Steam nozzle inlet pressure, bar	71.39	71.87	70.69	70.10	70.61
Primary pump suction leg temperature, K	563.6	564.6	565.4	565.1	566.1

Table 1. Wolf Creek SG Data

In the above table the effective values of the feed flow and downcomer flow are the measured plant values reduced by the value of the blowdown flow.

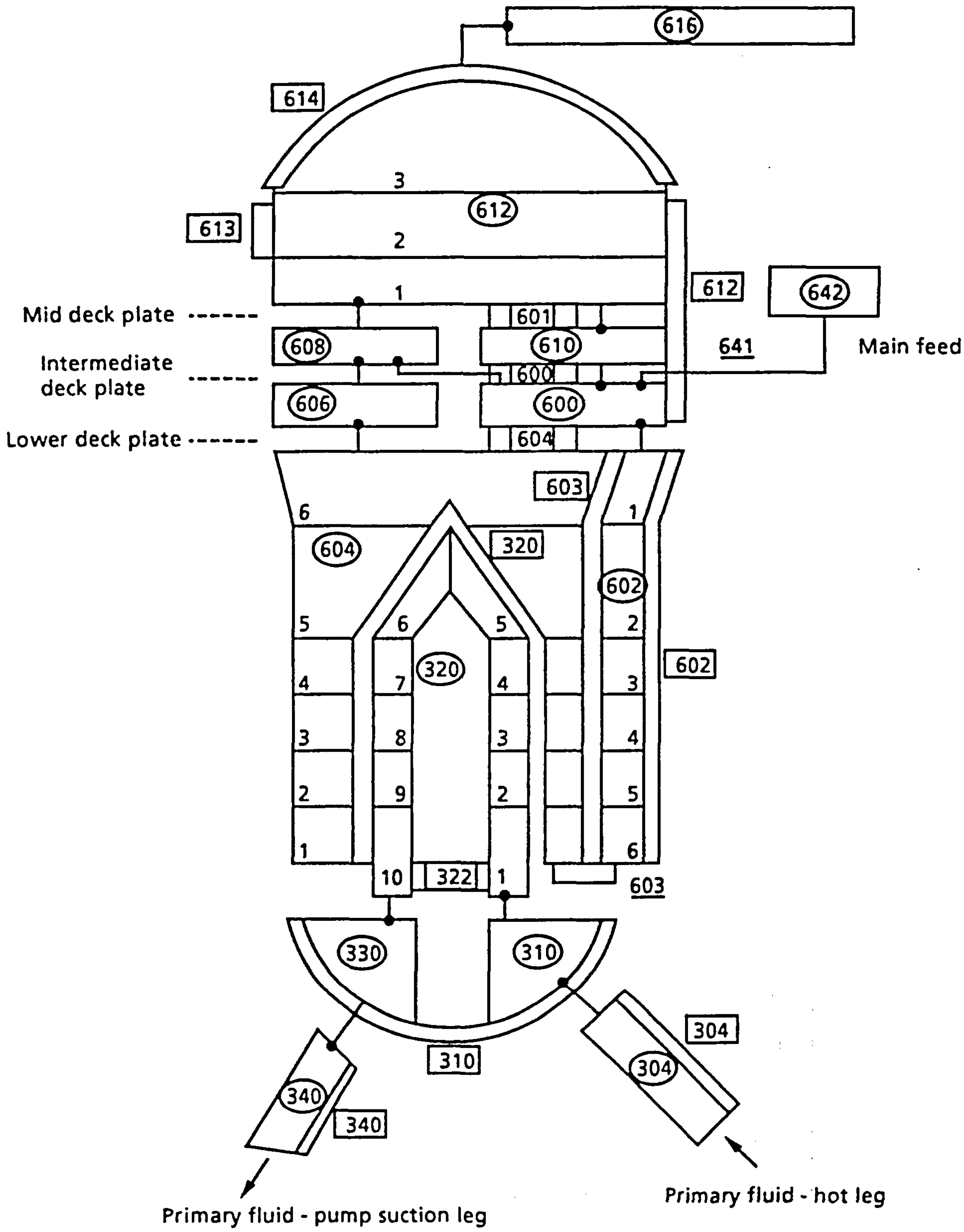


Figure 1 Nodalisation of the model 'F' steam generator

JMP/CPR(8.8.91)0539/1

Fig. 2. Errors in Calculated SG Dome Pressure

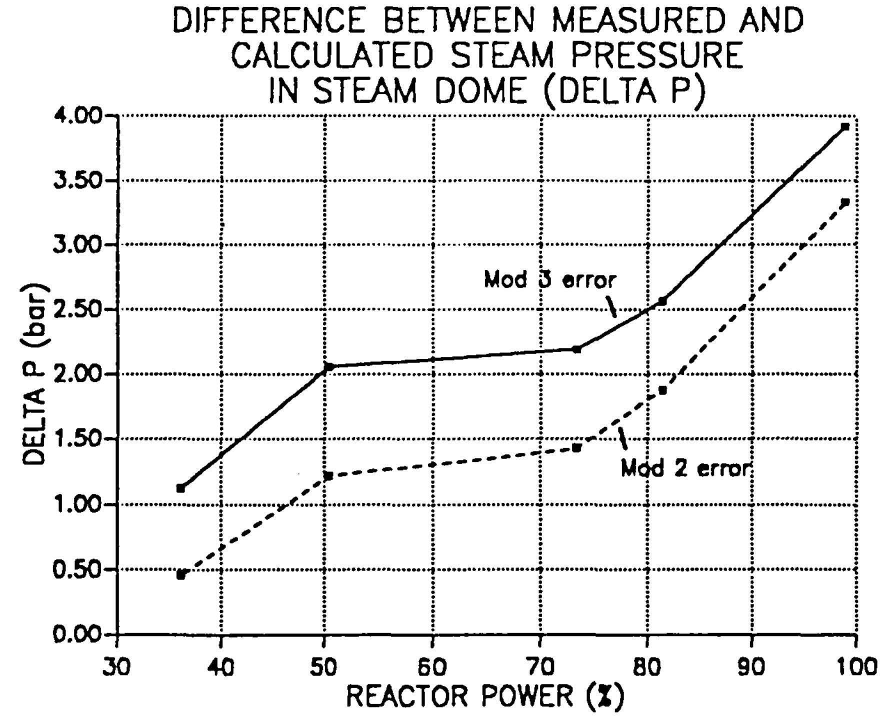


Fig. 3. SG Liquid Mass Ratio, MOD3/MOD2

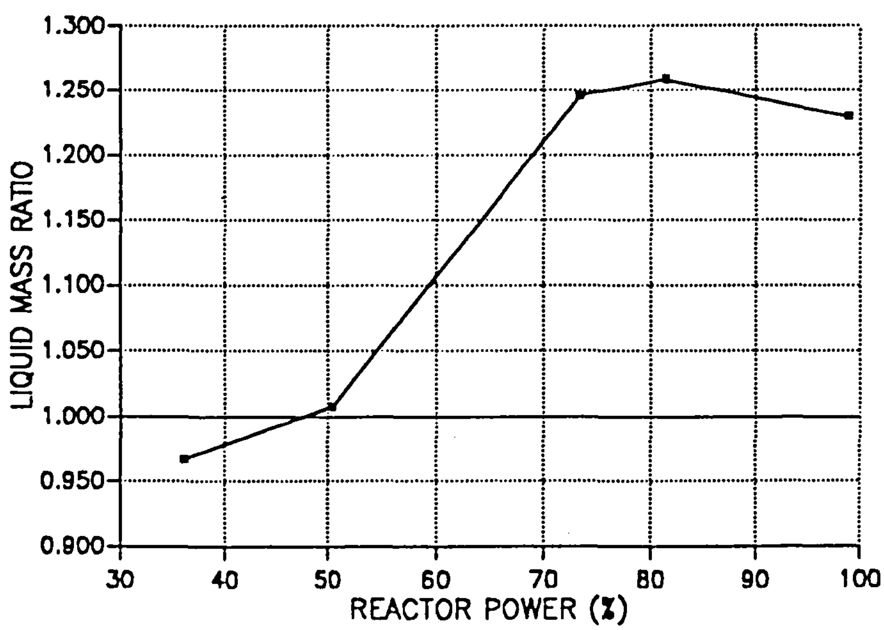


Fig. 4. Void Profiles in SG Riser at 99% Load

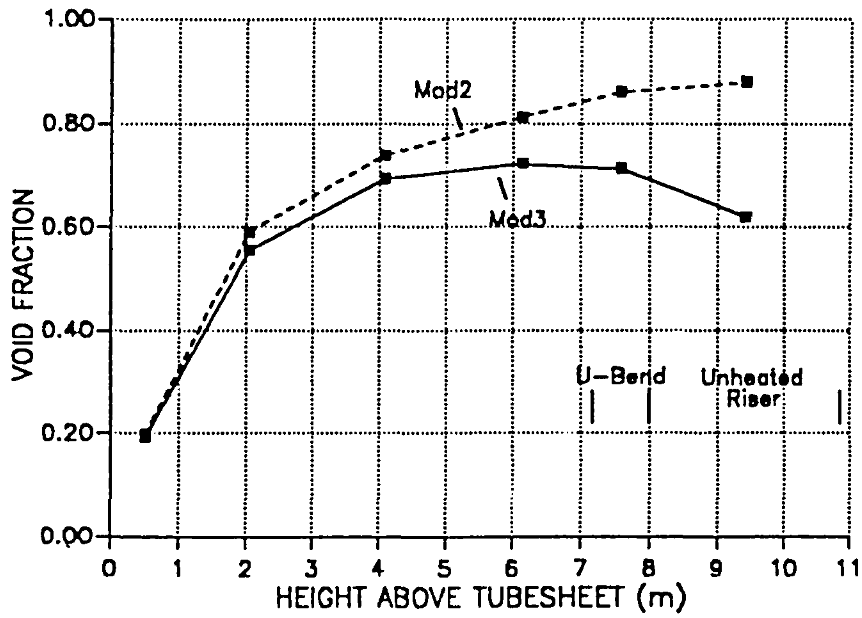


Fig. 5. Void Profiles in SG Riser at 81% Load

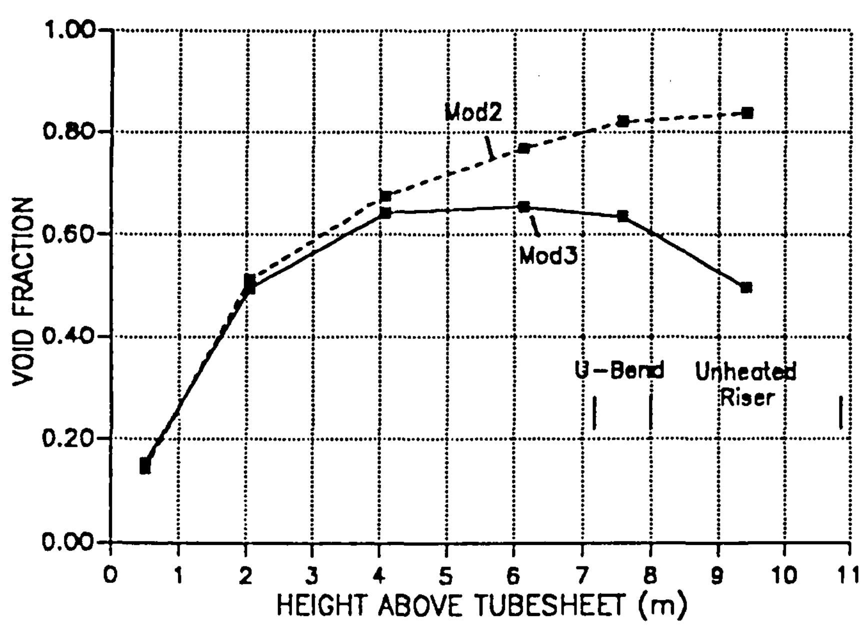


Fig. 6. Void Profiles in SG Riser at 73% Load

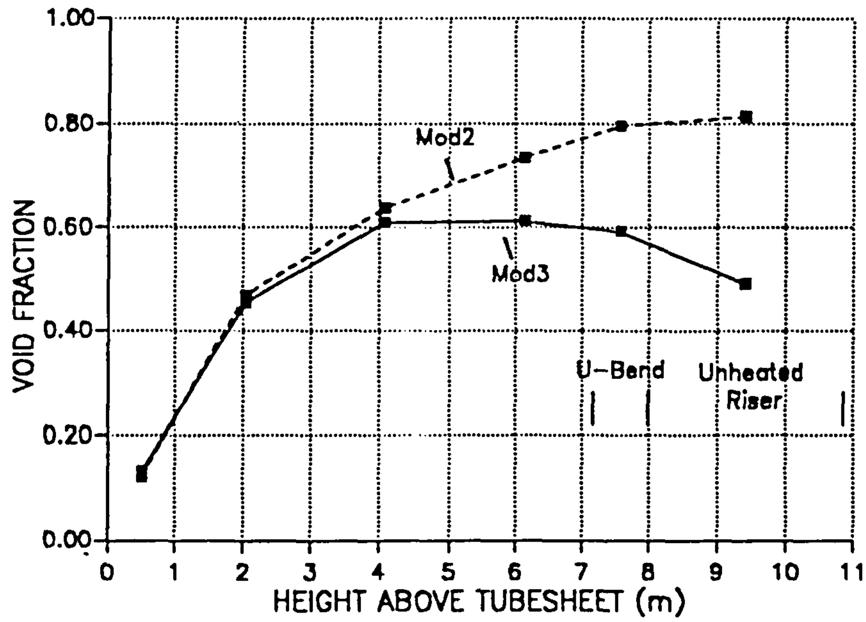


Fig. 7. Void Profiles in SG Riser at 51% Load

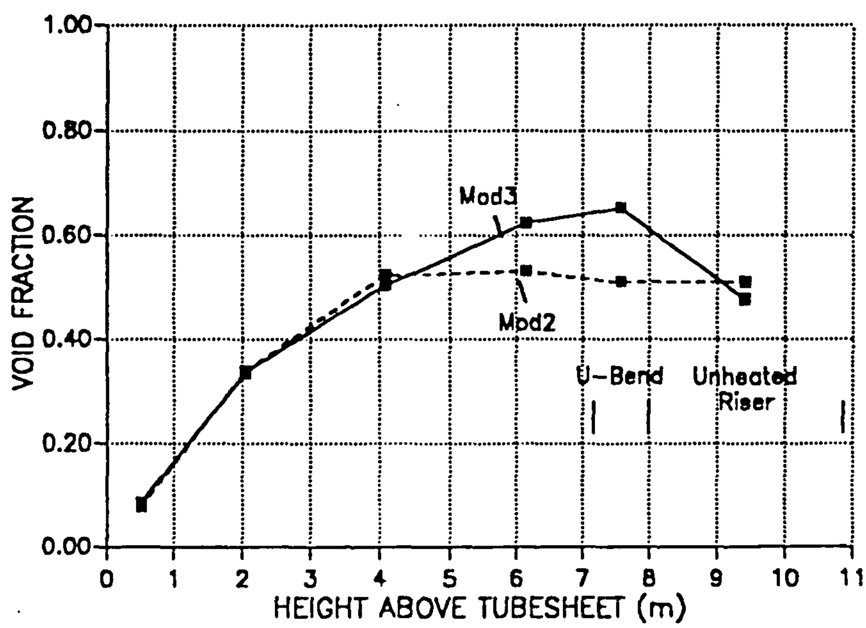
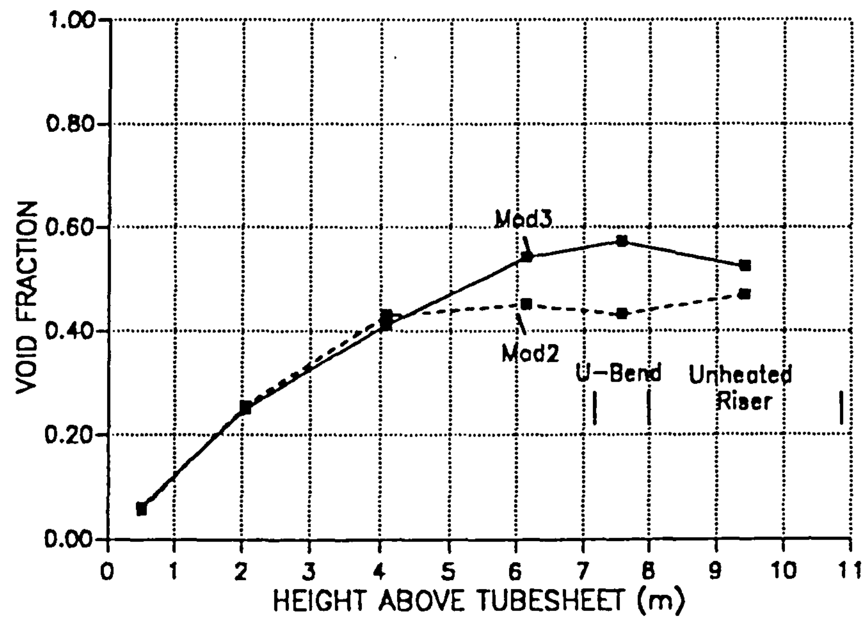


Fig. 8. Void Profiles in SG Riser at 36% Load



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11. ABSTRACT *(200 words or less)*

A preliminary assessment of Steam Generator (SG) modelling in the PWR thermal-hydraulic code RELAP5/MOD3 is presented. The study is based on calculations against a series of steady-state commissioning tests carried out on the Wolf Creek PWR over a range of load conditions. Data from the tests are used to assess the modelling of primary to secondary side heat transfer and, in particular, to examine the effect of reverting to the standard form of the Chen heat transfer correlation in place of the modified form applied in RELAP5/MOD2. Comparisons between the two versions of the code are also used to show how the new interphase drag model in RELAP5/MOD3 affects the calculation of SG liquid inventory and the void fraction profile in the riser.

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