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International Agreement Report

Assessment of Interphase Drag Correlations in the RELAP5/MOD2 and TRAC-PF1/MOD2 Codes

Prepared by
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Office of Nuclear Regulatory Research
U.S. Nuclear Regulatory Commission
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Abstract: An assessment is carried out of the interphase drag correlations used in modelling vertical two-phase flows in the advanced thermalhydraulic codes RELAP5/Mod2 and TRAC-PF1/Mod1. The assessment is performed by using code models to calculate void fraction in fully developed steam-water flows, and comparing results with predictions of standard correlations and test data. The study is restricted to the bubbly and slug flow regimes (void fractions below 0.75).

For upflows, at pressures of interest in PWR small break LOCA and transient analysis the performance of the code models is generally satisfactory. Exceptions are (i) small hydraulic diameter channels at low pressure ($p \leqslant 4$ MPa) (ii) large pipe diameters at void fractions exceeding 0.5; in these cases void fraction errors are outside normal uncertainty ranges.

For downflows, the code models give good agreement with limited available void fraction data.

The numerical results given in this paper allow a rapid estimate to be made of void fraction errors likely to arise in a particular code application due to deficiencies in interphase drag modelling.

Executive Summary

In some small break loss-of-coolant accidents (LOCAs) and pressurised transients in PWRs, system behaviour depends strongly on the void fraction in vertical loop components. For example, when the reactor core is partially uncovered, the boildown rate is influenced by the void fraction which determines the continuous liquid level. Similarly, the void fraction in the core and other vertical flow paths strongly influences the duration of core dry-out when core uncovering is caused by a balance of hydrostatic forces. To provide an accurate numerical simulation of these situations it is necessary to model properly the interphase relative motion (slip) in the vertical loop components.

RELAP5/Mod2 [1] and TRAC-PF1/Mod1 [2] are advanced thermalhydraulic codes presently being used in the UK for PWR fault transient analysis. Both codes employ a two-fluid model in which separate momentum equations are solved for the gas and liquid phases. Flow-regime dependent constitutive equations are specified to model interphase momentum transfer.

In mixed flow regimes such as bubbly, slug, and churn flow, it is generally accepted that interphase slip is made up of separate contributions from the motion of gas bubbles relative to the surrounding liquid ('local' slip) and from the non-uniform profiles of void fraction and gas velocity over the pipe area ('profile' slip). The one-dimensional two-fluid models in RELAP5/Mod2 and TRAC-PF1/Mod1 assume a uniform profile of void fraction and steam velocity over the pipe area; therefore profile slip is not modelled explicitly. Instead profile effects are modelled indirectly by using empirically based interphase drag coefficients. However, since the processes producing the interphase slip are not fully simulated, the accuracy of this approach is questionable.

The present note describes an assessment of the performance of the interphase drag relationships in RELAP5/Mod2 and TRAC-PF1/Mod1, in modelling vertical flows. The method used to assess the drag relationships is to apply them to calculate the void fraction in steady, fully-developed vertical steam-water flows under conditions of typical interest in reactor transient analysis. Results are then compared with the void fraction predicted by standard empirical correlations, or with test data.

Graphical results are presented which can be used for a rapid estimation of the void fraction error which is likely to arise in a particular application of RELAP5/Mod2 or TRAC-PF1/Mod1 due to deficiencies in the interphase drag modelling. The main findings can be summarised as follows:

(i) the interphase drag models in RELAP5/Mod2 and TRAC-PF1/Mod1 perform comparably well in modelling vertical flows;

(ii) errors in the two-phase mixture density increase with decreasing liquid flux, increasing steam flux, increasing pipe size and decreasing pressure;

(iii) for upflow, at the pressure of interest in modelling small break LOCAs and transients in PWRs ($p > 4$MPa), the errors in two-phase mixture density are not grossly different from errors normally expected in applying standard correlations for void fraction. Exceptions are large pipe sizes at void fractions exceeding 0.5, and small pipe sizes at low pressure ($p \lesssim 4$ MPa), where errors become large.

For downflow the code models perform very well in comparison with the limited void fraction data available.

Finally, it is noted that the paper examines the modelling equations themselves, rather than their numerical implementation in the codes. In practice the numerical value of the interphase drag force is calculated by combining information from upstream and downstream volumes. Therefore, computed values are sensitive to cell size and positioning of node boundaries. Additional comparisons with actual code calculations are needed to assess the implementation of the models within the codes.

Notation

A	Pipe area
D_h	Hydraulic diameter
F_{wk}	Force on phase k per unit flow volume due to wall shear
$f_{g\ell}$	Interphase drag coefficient
g	Acceleration due to gravity
G	Mass flux
j	Volumetric flux
M^{d}_{kz}	Interfacial drag force on phase k per unit flow volume
p	Pressure
u	Velocity
z	Axial co-ordinate
α	Volumetric concentration
Δu	Relative velocity ($= u_g - u_\ell$)
Γ	Mass generation rate per unit flow volume
σ	Surface Tension
ρ	Density
$\Delta\rho$	Density difference $= (\rho_\ell - \rho_g)$
θ	Inclination angle of pipe

Subscripts

g	Property of gas phase
ℓ	Property of liquid phase
i	Property of gas-liquid interface
$g\ell$	Difference between gas and liquid phase value

1. INTRODUCTION

In some small break loss-of-coolant accidents (LOCAs) and pressurised transients in PWRs, system behaviour depends strongly on the void fraction in vertical loop components. For example, when the reactor core is partially uncovered, the boildown rate is influenced by the void fraction which determines the continuous liquid level. Similarly, the void fraction in the core and other vertical flow paths strongly influences the duration of core dry-out when core uncovering is caused by a balance of hydrostatic forces. To provide an accurate numerical simulation of these situations it is necessary to model properly the interphase relative motion (slip) in the vertical loop components.

RELAP5/Mod2 [1] and TRAC-PF1/Mod1 [2] are advanced thermalhydraulic codes presently being used in the UK for PWR fault transient analysis. Both codes employ a two-fluid model in which separate momentum equations are solved for the gas and liquid phases. Flow-regime dependent constitutive equations are specified to model interphase momentum transfer.

In mixed flow regimes such as bubbly, slug, and churn flow, it is generally accepted that interphase slip is made up of separate contributions from the motion of gas bubbles relative to the surrounding liquid ('local' slip) and from the non-uniform profiles of void fraction and gas velocity over the pipe area ('profile' slip) (see e.g. ref. [3]). The one-dimensional two-fluid models in RELAP5/Mod2 and TRAC-PF1/Mod1 assume a uniform profile of α_g and u_g over the pipe area: therefore profile slip is not modelled explicitly. Instead profile effects are modelled indirectly by using empirically based interphase drag coefficients. However, since the processes producing the interphase slip are not fully simulated, the accuracy of this approach is questionable.

The present note describes an assessment of the performance of the interphase drag relationships in RELAP5/Mod2 and TRAC-PF1/Mod 1, in modelling vertical flows. The method used to assess the drag relationships is to apply them to calculate the void fraction in steady, fully-developed vertical steam-water flows under conditions of typical interest in reactor transient analysis. Results are then compared with the void fraction predicted by standard empirical correlations, or with test data. The adequacy of the interphase drag models is reviewed in the light of these comparisons. The assessment is confined to bubbly and slug flow conditions (α_g <0.75).

2. SELECTION OF VOID FRACTION CORRELATIONS FOR CODE ASSESSMENT

Correlations were selected from the literature to provide 'best estimate' void fraction predictions for comparison with the void fractions calculated with the code models. Selection of appropriate models for upflow and downflow is described below:

2.1 Co-current Upflow

There are extensive void fraction data available for co-current upflow of steam-water and air-water mixtures, and a number of void fraction correlations have been proposed in the literature. For the present application a 'best-estimate' model was developed by combining the correlations of Wilson et al [4] and Rooney [5].

The Wilson correlation has the functional form

$$\alpha = f(j_g, j_\ell, \rho_g, \rho_\ell, \sigma, D) \qquad (1)$$

where the function f is defined in Appendix A, section A1.0.

The Wilson correlation is based on steam-water data for pressures in the range 2.0 - 13.8 MPa and pipe diameters between 100 - 914 mm [4, 6]. However in ref [7] the correlation was tested against steam-water level-swell data obtained in the THETIS rod bundle facility ($D_h = 9.12$mm) at pressures between 0.2 and 4.0 MPa. In that study it was found to perform well if the dimension D in the correlation was equated with the hydraulic diameter of the rod bundle. Thus the correlation is considered reliable for PWR core conditions, despite the fact that this geometry is outside the range of its original data-base.

For higher flows ($Ku_g > 10$) the Wilson correlation falls outside its range of validity. Then the Rooney correlation [5] which has the form,

$$\alpha_g = \frac{1}{C_o} \frac{j_g}{j} \qquad (2)$$

is used. The parameter C_o in equation(2) is pressure and flow dependent and is defined in Appendix A section A2.0.

Well established correlations of the same form as equation(2) have been given by Bankoff [8] and by Armand and Treschev [9] with slightly different specifications for the parameter C_o; the Rooney correlation has been selected in preference to these correlations on the basis of its marginally better performance in comparison with an HTFS data base [10].

The 'best estimate' correlation of void fraction for upward flow used in this assessment combines the Wilson and Rooney correlations according to:

$$\alpha_g = \min\ (\text{Wilson, Rooney}) \qquad (3)$$

Equation(3) is found to satisfy the limits of validity of the Wilson correlation.

Equation (3) was used as a basis for assessing the performance of the interphase drag equations used in the codes in upflow. However it must be recognised that there is considerable uncertainty in void fraction predictions obtained from any empirical correlation. Ref [10] compared several commonly used void correlations with steam-water and air-water data from various sources. Even the most successful correlations give RMS errors in two-phase mixture density in the range 17 - 30%.

In order to give an indication of these uncertainties, comparisons were made between predictions of eq(3) and predictions of some alternative correlations in common use. The following correlations were selected for this purpose:

(i) EPRI correlation [11]. This correlation has been validated against an extensive database although the bulk of comparisons presented by the developers is for small pipe sizes;

(ii) Zuber - Findlay correlation [3]. This model has a simple form and has been widely used for modelling vertical flows in open pipes;

(iii) Cunningham - Yeh correlation [13]. This model was developed from voidage data in a large PWR-type pin bundle under static boildown conditions.

The forms of these correlations are given in Appendix A.

2.2 Cocurrent Downflow

For co-current downflow very few void fraction data are available and no well established correlations are known to the present authors. Accordingly, the performance of the code models was assessed against the data of Petrick [12]. Petrick's data are for steam/water mixtures in a 49mm pipe at pressures of 4.1, 7.0 and 10.3 MPa, with gas and liquid superficial velocities up to about 1.5 m/s. Comparison with these data provides a limited test of the code models at small diameters and low to moderate flows. It appears that no data are available for larger pipe sizes.

3. ASSESSMENT OF INTERPHASE DRAG MODELS IN TRAC-PF1/MOD1 and RELAP5/MOD2

To assess the interphase drag models in the codes, the drag equations are first used to develop relationships between the void fraction and the phase flow-rates, for the case of a steady fully developed steam-water flow in a uniform area vertical pipe. The void fractions obtained from these relationships are then compared (i) with predictions of the best-estimate empirical correlation for upflow described in section 2 and (ii) with limited available data for downflow.

3.1 Development of Equation for Void Fraction

The momentum equations in the RELAP5/Mod2 and TRAC-PF1/Mod1 models can be expressed in the following general form

$$\frac{\partial}{\partial z}\left(\alpha_k \rho_k u_k\right) + \frac{1}{A} \frac{\partial}{\partial z}\left(A\, \alpha_k \rho_k u_k^{\,2}\right)$$

$$= -\alpha_k \rho_k g \sin\theta - \alpha_k \frac{\partial p}{\partial z} + M^d_{kz} + F_{wk}$$

$$+ u_{ki}\, \Gamma_k \qquad (4)$$

where $\theta = \pi/2$ for upflow and $-\pi/2$ for downflow. In both codes, the interfacial drag force per unit flow volume is represented by an equation of the form:

$$M_{gz}{}^{d} = -M_{\ell z}{}^{d} = -f_{g\ell}\ \Delta u \mid \Delta u \mid \qquad (5)$$

where $f_{g\ell}$ is a generalised interphase drag coefficient and $\Delta u = u_g - u_\ell$

For steady, fully developed flow the derivative terms in the left hand side of (4) can be set to zero. Eliminating (dp/dz) between the resultant equations for the gas and liquid phases, neglecting momentum exchange between the phases due to mass transfer (which is usually small), and using equ(5), we arrive at the following algebraic equation for the relative velocity.

$$\Delta u|\Delta u| = (g\sin\theta\ \alpha_g \alpha_\ell \Delta\rho - \alpha_g F_{w\ell} + \alpha_\ell F_{wg})/f_{g\ell} \qquad (6)$$

Since uniform flow is assumed Δu is related to the phase k superficial velocities by the equation:

$$j_g/\alpha_g - j_\ell/\alpha_\ell = \Delta u \qquad (7)$$

If formulations for the interphase and wall friction terms $f_{g\ell}$, F_{wk} are specified, equations (6) and (7) can be used to calculate the void fraction in terms of the phase superficial velocities j_g and j_ℓ.

3.2 Comparisons with Empirical Void Correlations and Measurements

The formulations for interphase drag and wall friction used in RELAP5/Mod2 and TRAC-PF1/Mod1, are given in Appendix B and Appendix C. Short computer programmes were written to solve equations (5)-(7) using these interphase/wall friction relationships.

3.2.1 Comparisons for Vertical Upflow

Results of the calculations for vertical upflow are shown in Figs. 1 and 2. The dashed lines are the predictions obtained in the RELAP5 and TRAC-PF1 models. The solid lines are the corresponding predictions of the Wilson-Rooney best-estimate void correlation described in section 2.1. Calculations are given for $D_h = 0.01$m and 1.0m (nominal values for the reactor core and main loop pipework respectively) at pressures of 10.0, 4.0 and 1.0 MPa. (Note that the pressure range 4.0 - 10.0 MPa is the range of chief interest for PWR small break LOCA analysis. At lower pressures emergency cooling water is injected from accumulators, leading to rapid system refilling.)

Inspection of the Figures shows reasonably good correspondence between the RELAP5 and TRAC results and the Wilson-Rooney correlation for moderate and high liquid flow-rates and small hydraulic diameters. Discrepancies are largest for low pressures, large pipe diameters, low values of j_ℓ and high values of j_g.

Since two-phase density rather than void fraction is the quantity of physical interest in modelling an uncovered core, it is more meaningful to discuss density errors than errors in void fraction. Figure 3 plots percentage error in the density against the void fraction, for the limit of zero liquid flow-rate ($j_\ell = 0$) where errors tend to be largest. The percentage error in the two-phase density is defined as

$$\frac{\rho_2 - \rho_1}{\rho_1} \times 100 \qquad (8)$$

Where ρ_1 is the density calculated with the Wilson-Rooney correlation and ρ_2 is the density calculated with RELAP5 or TRAC models. The abscissa is the void fraction predicted by the Wilson-Rooney correlation.

Fig. 3 shows that the 'density errors' obtained using the RELAP5 and TRAC models are of comparable magnitude.

Figs.4 and 5 compare the 'density errors' obtained using the code models with the 'errors' obtained using the other correlations for α_g in vertical upflow, listed in section 2.1. The calculations are for $j_\ell = 0$, and pressures of 4.0 and 10.0 MPa, which span the pressure range of chief interest in PWR small break LOCA analysis. The 'density errors' associated with each correlation are obtained from equ(8) by making ρ_2 equal to mixture density calculated with the correlation, and ρ_1 the mixture density calculated with the Wilson-Rooney model.

Examination of Figs.3-5 leads to the following observations:

(1) for small hydraulic diameters (characteristic of the reactor core) the code models perform well at a pressure of 10MPa. At lower pressure the void fraction is increasingly over-predicted. At 4.0 MPa, density errors of 30-40% are apparent at void fractions of about 0.5, which are significantly outside the uncertainty range of ±15% indicated by the correlations.

(2) for large hydraulic diameters the code models perform well at all pressures for $\alpha_g < 0.5$. However for higher values of α_g, the density errors become large.

3.2.2 Comparisons for Downflow

As noted in section 2.2, there appear to be no reliable correlations for void fraction in vertical downflow. Therefore void predictions obtained with the RELAP5/Mod2 and TRAC-PF1 models were compared directly with the test data of Petrick [12]. Comparisons are shown in Fig. 5 for tests at a pressure of 7.0 MPa (1000 psia). Agreement is very good in both cases, with the RELAP5 models performing somewhat better than the TRAC models overall, particularly at high void fractions. Similar comparisons were made with data at pressures of 4.1 and 10.3 MPa and similar conclusions were reached.

4. DISCUSSION AND CONCLUSIONS

The numerical calculations in Figs 1-3 can be used for a rapid estimation of the void fraction error which is likely to arise in a particular application of RELAP5/Mod 2 or TRAC-PF1/Mod 1 due to deficiencies in the interphase drag modelling. In assessing the magnitude and significance of the errors, our main conclusions are as follows.

(i) the interphase drag models in RELAP5/Mod 2 and TRACPF1/ Mod 1 perform comparably well in modelling vertical flows;

(ii) errors in the two-phase mixture density increase with decreasing j_ℓ, increasing j_g, increasing pipe size and decreasing pressure;

(iii) for upflow, at the pressure of interest in modelling small break LOCAs and transients in PWRs ($p > 4$MPa), the errors in two-phase mixture density are not grossly different from errors normally expected in applying standard correlations for void fraction. Exceptions are large pipe sizes at void fractions exceeding 0.5, and small pipe sizes at low pressure ($p < 4$ Mpa), where errors become large).

For downflow the code models perform very well in comparison with the limited void fraction data available.

The present paper has examined the modelling equations themselves, rather than their numerical implementation in the codes. In practice the numerical value of the interphase drag force is calculated by combining information from upstream and downstream volumes. Therefore, computed values are sensitive to cell size and positioning of node boundaries. Comparisons with actual test data are needed to assess the implementation of the models within the codes.

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APPENDIX A

Description of Void Fraction Correlations used for Code Assessment

A1.0 Wilson correlation [4]

This correlation is based on steam-water data in pipes of 100 mm and 480 mm diameter at pressures between 2.0 and 5.5 MPa [6], and additional data obtained in 460 mm and 914 mm diameter pipes at pressures between 4.1 and 13.8 MPa [4]. The tests were done with either zero liquid flow-rate (stagnant pool type conditions) or at small liquid flow-rates established by natural circulation. The correlation has the following form.

$$c = C_1 \left(\frac{\rho_g}{\rho_\ell}\right)^{C_2} Ku_g^{C_3} \left(\frac{\sqrt{\sigma/g\rho_{\ell g}}}{D_h}\right)^{C_4} \left(\frac{j_g}{j}\right)^{C_5} \qquad \text{(A1)}$$

where

$C_1 = 0.564$, $C_2 = 0.12$, $C_3 = 0.67$, $C_4 = 0.1$, $C_5 = 0.6$ for $Ku_g < 1.5$

$C_1 = 0.619$, $C_2 = 0.12$, $C_3 = 0.47$, $C_4 = 0.1$, $C_5 = 0.6$ for $Ku_g > 1.5$

A2.0 Rooney correlation [3]

This correlation has the form

$$\alpha_g = \frac{1}{C_o} \frac{j_g}{j} \qquad \text{(A2)}$$

The parameter C_o is a function of flow-rate defined in the following table:

U_R/j	C_o
0.0	1.00
0.006	1.050
0.013	1.100
0.021	1.150
0.031	1.200
0.047	1.250
0.060	1.275
0.010	1.300
0.140	1.300

where

$$U_R = (\sigma g \,\Delta\rho / \rho_\ell^2)^{0.25}$$

In ref.[10] the Rooney correlation was compared against the HTFS database for steam-water flow in vertical tubes, (tube diameters in the range 9 - 170 mm and pressures in the range 3.0 - 16.5 MPa). The correlation was shown to perform satisfactorily.

A3.0 EPRI Drift Flux correlation [11].

The EPRI correlation has the form

$$\alpha_g = j_g/(C_o j + V_{gj}) \tag{A3}$$

where C_o and V_{gj} are functions of flow properties, including void fraction.

The EPRI correlation was developed from a large database of steam-water data. The database covered the following conditions:

$G_l = 0$	$0.1 < D_h < 0.2m$	$0.1 < p < 0.4$ MPa
$G_l = 0$	$D_h = 0.01m$	$0.1 < p < 0.8$ MPa
$G_l = 0$	$D_h = 0.456m$	$4.0 < p < 14.0$ MPa
$100 < G_l < 300\ kgm^{-2}s^{-1}$	$D_h = 0.168m$	$8.0 < p < 16.0$ MPa
$500 < G_l < 2000\ kgm^{-2}s^{-1}$	$0.01 < D_h < 0.05m$	$3.0 < p < 8.5$ MPa

A4.0 Zuber - Findlay Correlation [3]

The Zuber-Findlay correlation for vertical churn-turbulent flow has the form

$$\alpha_g = j_g/[C_o j + 1.53\ (\sigma g\ \Delta\rho/\rho_l^2)^{0.25}] \tag{A4}$$

Values for the profile parameter C_o were given in the range 1.1 - 1.6; a value of 1.2 has been used in the present work.

The Zuber-Findlay model is simple to apply and is widely used in two-phase flow analysis. An equation approximating to eq(A4) was tested against the HTFS steam-water database in ref.[10] and was found to perform reasonably well.

A5.0 Cunningham-Yeh correlation [13]

This equation was developed from voidage measurements in a large electrically heated PWR - type pin bundle. The equation has the same form as (A1) but with revised constants, (C_n).

$C_1 = 0.696$, $C_2 = 0.24$, $C_3 = 0.67$, $C_4 = 0$, $C_5 = 0$ for $Ku_g < 1.53$

$C_1 = 0.757$, $C_2 = 0.24$, $C_3 = 0.47$, $C_4 = 0$, $C_5 = 0$ for $Ku_g > 1.53$

APPENDIX B

Description of the RELAP5/Mod2 constitutive models for wall and interphase friction for vertical flow

B1 Interphase Drag Models

The RELAP5/Mod2 flow regime map for vertical flow is divided into regions of bubbly flow, slug flow and annular mist flow as shown in Fig. A1. Transitional zones are placed between these regimes to ensure smoothly varying functions. Below we give the equations used for $f_{g\ell}$ in the bubbly and slug flow regimes. These equations have been taken from a source code listing of RELAP5/Mod2 Cycle 36.00 and differ in some respects from the equations described in the code manual [1]. In some cases, parameters defined below are constrained to lie within prescribed ranges in the actual RELAP5 coding. For the sake of brevity, these ranges are not indicated in all cases below.

(a) Bubble Flow Region

The bubble flow region is defined as the region bounded by the void fraction limits:

$$0 < \alpha_g < \alpha_{BS}$$

Where

$\alpha_{BS} = \alpha_L \qquad G < 2000 \text{ kg m}^{-2}\text{S}^{-1}$

$\alpha_{BS} = \alpha_L + 10^{-3}\,(G-2000)\,(0.5-\alpha_L) \qquad 2000 < G < 3000 \text{ kg m}^{-2}\text{S}^{-1}$

$\alpha_{BS} = 0.5 \qquad G > 3000 \text{ kg m}^{-2}\text{S}^{-1}$

and

$$\alpha_L = 0.25 \text{ Min } \{1.0,\ (D^*/22.2)^8\}$$

The drag coefficient in bubbly flow is defined as follows:

$$f_{g\ell} = 0.125\ \rho_\ell\, a_{g\ell}\, C_D$$

Where

$a_{g\ell} = 3.6\ \alpha_g/d_o$

$C_D = 24\,(1 + 0.1\ \text{Re}_p^{0.75})/\ \text{Re}_p$

$\text{Re}_p = \Delta u\ d_o\ \rho_\ell\ (1-\alpha_g)/\mu_\ell$

$d_o = 5\sigma/\rho_\ell\ \Delta u^2$

(b) Slug Flow Regime

The slug flow regime is defined as the region within void fraction limits

$$\alpha_{BS} < \alpha_g < \alpha_{SA}$$

Where

$$\alpha_{SA} = \text{Max}\{0.75,\ 1.4\ (\sigma g\ \Delta\rho/\rho_g^{\ 2}\ u_g^{\ 4})^{0.25}\}$$

and α_{BS} is defined as in (a) above.

The drag coefficient in slug flow is calculated from the equation:

$$f_{g\ell} = (1 - \alpha_b)\ f_B + f_s$$

Where

$\alpha_b = (\alpha_g - \alpha_{gs})/(1 - \alpha_{gs})$

$\alpha_{gs} = \alpha_{BS} \exp\{-8\ (\alpha_g - \alpha_{BS})/(\alpha_{SA} - \alpha_{BS})\}$

$f_B = 0.125\ \rho_\ell\ a_{g\ell B}\ C_{DB}$

$f_s = 5.51\ \rho_\ell\ (1 - \alpha_b)^3\ \alpha_b/D_h$

$a_{g\ell B} = 3.6\ \alpha_{gs}/d_o$

$d_o = 5\sigma/\ \rho_\ell\ (\Delta u')^2$

$\Delta u' = \Delta u\ \alpha_{gs}/\alpha_{BS}$

$C_{DB} = 40\ (1 + 0.075\ Re_B^{0.75})/Re_B$

$Re_B = d_o\ \rho_\ell\ \Delta u/(1 - \alpha_{gs})/\mu_\ell$

(c) Annular Mist Flow Regime

The lower boundary of the annular mist region is defined by the void fraction limit

$$\alpha_g = \alpha_{SA}$$

where α_{SA} is defined in (b) above. The drag coefficient in annular mist flow is calculated from the equation

$$f_{g\ell} = (1 - \alpha_{\ell F})\ f_D + f_F$$

Where

$\alpha_{\ell F} = e^{-s} \{1 - 10^{-4} (\alpha_\ell u_\ell \rho_\ell D/\mu_\ell)^{0.25}\}$

$s = 7.5 \times 10^{-5} (\alpha_g u_g/u_c)^6$

$u_c = 2.5 (\sigma \Delta\rho/\rho_g)^{0.25}$

$f_D = 0.125 \rho_g a_{g\ell D} C_{DD}$

$a_{g\ell D} = 3.6 \alpha_{\ell D}/d_{OD}$

$\alpha_{\ell D} = (\alpha_\ell - \alpha_{\ell F})/(1 - \alpha_{\ell F})$

$1/d_{OD} = \rho_g (\Delta u'')^2/(1.5\sigma)$

$\Delta u'' = \Delta u (\alpha_\ell - \alpha_{\ell F})/\alpha_\ell$

$C_{DD} = 40 (1 + 0.074 Re_D^{0.75})/Re_D$

$Re_D = d_{OD} \Delta u'' \rho_g (1 - \alpha_{\ell D})^{5/2}/\mu_g$

$f_F = 0.5 S_A \rho_g f_i$

$S_A = 6.1192775 \alpha_{\ell F}^{0.125} (1 - \alpha_{\ell F}/D)$

$f_i = 0.005 + A (\delta^*)^B$

$\log_{10} A = -0.56 + 9.07/D^*$

$B = 1.63 + 4.74/D^*$

$D^* = D (g\Delta\rho/\sigma)^{\frac{1}{2}}$

$\delta^* = 0.5 D^* (1 - \sqrt{1 - \alpha_{\ell F}})$

An interpolation zone is defined in the region $(\alpha_{SA} - 0.05) < \alpha_g < \alpha_{SA}$

In this zone $f_{g\ell}$ is calculated by interpolating between the values for slug flow and annular mist flow, to ensure a smooth transition.

B2 Wall Friction Models

RELAP5/Mod2 has a complex method for calculating the wall friction source terms F_{wg} and $F_{w\ell}$. The formulation described below has been taken from the code manual and has not been verified against source code listing. According to the code manual, in all flow regimes, the source terms are given by the equation (see equations (224) and (225) of ref.[1]).

$$F_{wk} = \frac{-\lambda_k' \rho_k j_k|j_k|}{2D} \frac{\alpha_{kw} \lambda_k \rho_k u_k^2}{\{\alpha_{gw} \lambda_g \rho_g u_g^2 + \alpha_{\ell w} \lambda_\ell \rho_\ell u_\ell^2\}} \cdot \phi_k^2$$

Where λ_k and λ_k' are friction factors which depend on Reynolds number and tube wall roughness. λ_k is calculated based on the "mixture" Reynolds number

$$Re_k = \rho_k u_k D_h (\alpha_k/\alpha_{kw})/\mu_k$$

ϕ_k^2 is a two-phase multiplier calculated from the HTFS correlation, which is expressed as:

$$\phi_\ell^2 = 1 + \frac{C}{X} + \frac{1}{X}^2$$

$$\phi_g^2 = X^2 + CX + 1$$

Here X is the Martinelli parameter defined as:

$$X^2 = \phi_g^2/\phi_\ell^2 = (\lambda_g' \rho_g j_g^2)/(\lambda_\ell' \rho_\ell j_\ell^2)$$

and

$$C = \text{max.} \{2.0, (-2.0 + f_1(G)T_1)\}$$

where

$$f_1(G) = 28 - 0.3G$$

$$T_1 = \exp [-(\log_{10} \Lambda + 2.5)^2/(2.4 - G(10^{-4}))]$$

$$\Lambda = \rho_g/\rho_\ell (\mu_\ell/\mu_g)^{0.2}$$

$$G = \rho_g j_g + \rho_\ell j_\ell$$

α_{kw} represents the fraction of the pipe wall perimeter in contact with phase k. In the bubbly flow regions it is assumed that

$$\alpha_{kw} = \alpha_k$$

In the slug flow regions it is assumed that:

$$\alpha_{gw} = \alpha_{gs} \text{ and } \alpha_{\ell w} = 1-\alpha_{gs}$$

where α_{gs} is defined in section A1(b). In the annular-mist flow regime it is assumed that:

$$\alpha_{\ell w} = \alpha_{\ell F}^{0.25} \text{ and } \alpha_{gw} = 1-\alpha_{\ell F}^{0.25}$$

where $\alpha_{\ell F}$ is defined in section A1(c).

APPENDIX C

Description of the TRAC-PF1/Mod1 constitutive models for wall and interphase friction

C1.0 Interphase Drag Models

(a) Bubbly and Slug Flow

Evaluation of the interphase friction coefficient $f_{g\ell}$ is based on the flow regime map shown in Fig.A1. Here the upper limit of the bubbly flow regime is shown at $\alpha = 0.25$ as coded in subroutine FEMOM, not at $\alpha = 0.3$ as stated in the manual. In both bubbly and slug flows the vapour phase is assumed to be in the form of non-interacting spherical bubbles, and the drag force is expressed as the product of a cross-sectional area and a drag coefficient. Thus for bubble diameter D_b and drag coefficient C_b

$$f_{g\ell} = 0.75\ \alpha_g \rho_\ell C_b / D_b$$

The bubbly and slug regimes differ only in the evaluation of bubble diameter. Two basic diameters are calculated; firstly a Weber number controlled diameter

$$D_b' = \frac{We_b \sigma}{\rho_\ell V_{rb}^2}$$

where $We_b = 7.5$ and $V_{rb} = \max(0.1, |u_g - u_\ell|)$, and secondly a diameter equal to the local hydraulic diameter, D_h. Within the range $0 < \alpha < 0.5$ a weighted average of these two values is taken:-

$$D_b = \max\left[((1 - X)D_b' + XD_h),\ 10^{-4}\mathrm{m}\right]$$

where

$$X = (3 - 2X')(X')^2,$$

$$X' = X''\exp\left[-(G-2000)/700\right]$$

and

$$X'' = \max\left[0, \min(1, 4(\alpha_g - 0.25))\right]$$

Thus for the region shown as bubbly flow in Fig. 1, $D_b = D_b'$ is taken, and within the 'slug flow' region, D_b is interpolated between D_b' and D_h, which is reached at $\alpha_g = 0.5$.

The drag coefficient is of a commonly used form developed from measurements of the terminal velocity of spherical particles falling through a stagnant liquid. The equations are:

$C_b = 240 \qquad Re_b < .1031$

$C_b = (24/Re_b)(1 + 0.15Re_b^{.687}) \qquad 0.1031 < Re_b < 989$

$C_b = 0.44 \qquad Re_b > 989$

Where

$Re_b = V_{rb}\rho_\ell D_b/\mu_\ell.$

(b) <u>Annular-Mist Flow</u>

For void fractions $\alpha_g > 0.75$ annular-mist flow is assumed, with no contribution from bubbly/slug flow. Inter-facial shear contributions from droplets and film are evaluated separately. Firstly the fraction of liquid entrained as droplets (E) is calculated from the equation:

$$E = \max\left[1-\exp(V_E - V_g)/V_E,\ 7.75 \times 10^{-7}\, We_e^{\ 1.25} Re_\ell^{\ 0.25}\right]$$

where

$$V_E = 2.33\left[\frac{\Delta\rho\sigma We_d}{\rho_g}\right]^{0.25} \quad \text{with} \quad We_d = 4,$$

and

$$We_e = \left(\frac{D_h\rho_g j_g^{\ 2}}{\sigma}\right)\left(\frac{\Delta\rho}{\rho_g}\right)^{0.33}, \qquad Re_\ell = \left(\frac{j_\ell\rho_\ell D_h}{\mu_\ell}\right)$$

Here the critical velocity for entrainment, V_E, is equal to the vapour velocity needed to just levitate a drop whose size is governed by We_d, given a drag coefficient ($C_d = 0.44$) appropriate to high droplet Reynolds numbers. The droplet drag is calculated in the same way as the bubble drag, viz.

$$D_d = \frac{We_d\sigma}{\rho_\ell V_r^2}$$

Where $We_d = 4$ and $V_r = \max\ (0.1,\ |u_g - u_\ell|)$, and

$$f_{g\ell d} = 0.75\ \alpha_\ell\rho_g C_d/D_d$$

where C_d is given by the same drag coefficient relations as for bubbles except that the droplet Reynolds number ($Re_d = D_d V_r \rho_g/\mu_g$) is used.

The film shear is calculated using the following equation for the interfacial shear stress coefficient

$$c_f = 0.005[1 + 75\ (1-E)\ (1-\alpha_g)]$$

and an approximation to the film area given by:

$$a_{if} = \frac{4}{D_h}\ (1-E) \qquad \alpha_g < \alpha_f$$

$$a_{if} = \frac{4}{D_h}\ \frac{1-\alpha_g}{1-\alpha_f}\ (1-E) \qquad \alpha_g > \alpha_f$$

Where $\alpha_f = 1 - \dfrac{4\delta}{D_h}$

and $\delta = 2.55 \times 10^{-4}$m is a minimum allowed thickness. The total film shear coefficient is then $f_{g\ell f} = a_{if} c_f \rho_g/2$

and the total annular-mist drag coefficient is calculated as

$$f_{g\ell} = f_{g\ell d} + f_{g\ell f}$$

In the interpolated region $0.5 < \alpha_g < 0.75$ the bubbly/slug and annular-mist coefficients are combined using linear interpolation using a weighting factor:

$$F = (4\alpha_g - 2)^2(7 - 8\alpha_g).$$

In the transition zone the interphase friction coefficient $f_{g\ell}$ is calculated based on a bubble size equal to D_h, and a modified bubble velocity is used which transitions

from $V_{rb} = \max\ (0.1,\ |u_g - u_\ell|)$ at $\alpha_g = 0.5$

to $V_{rb} = 2.33\ \sigma\ We_b(\rho_\ell - \rho_g)^{0.25}/\rho_\ell$ for $\alpha > 0.55$

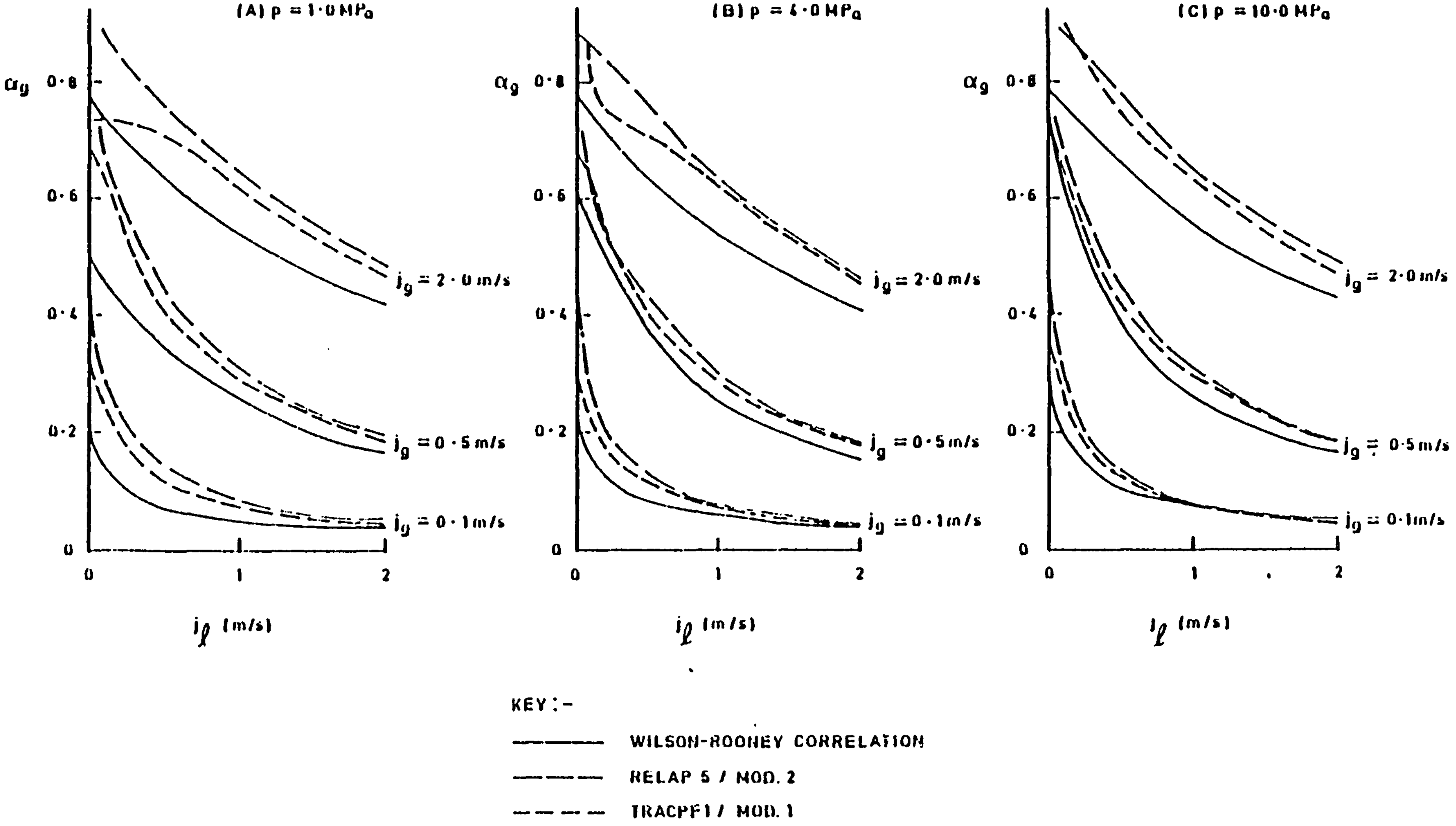


FIG.1 COMPARISON OF VOID FRACTION CALCULATIONS FOR D_h =0·01m

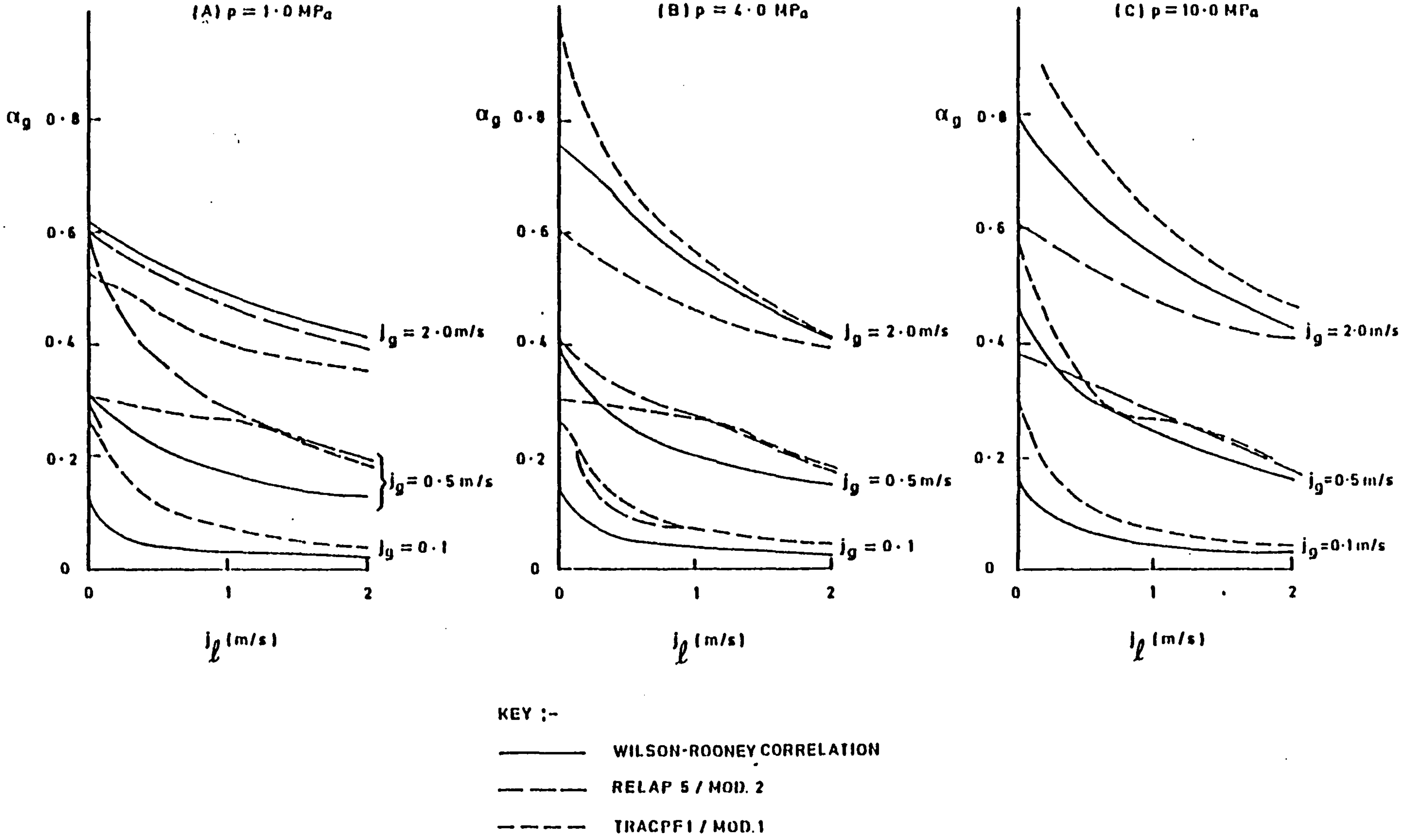
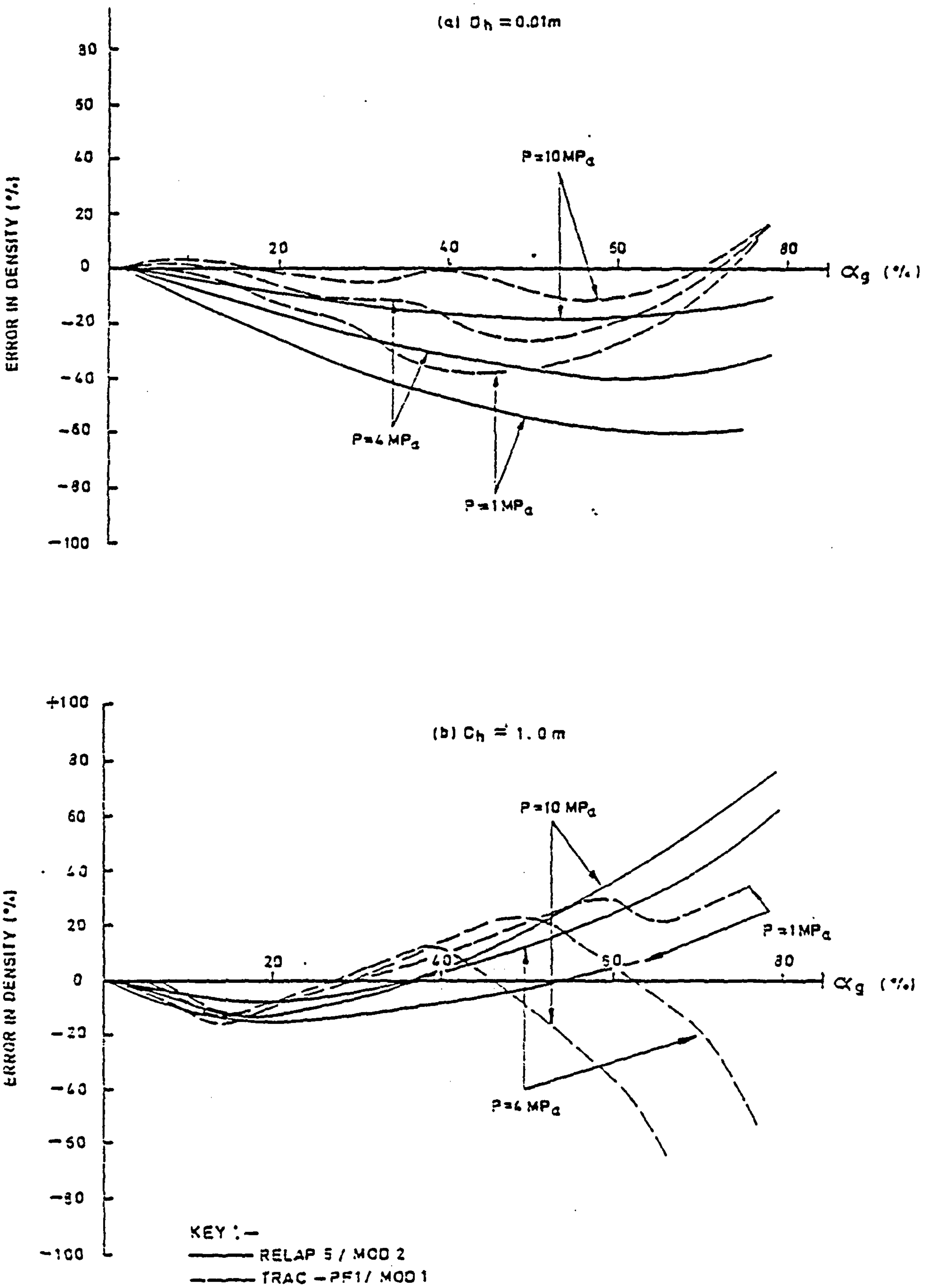
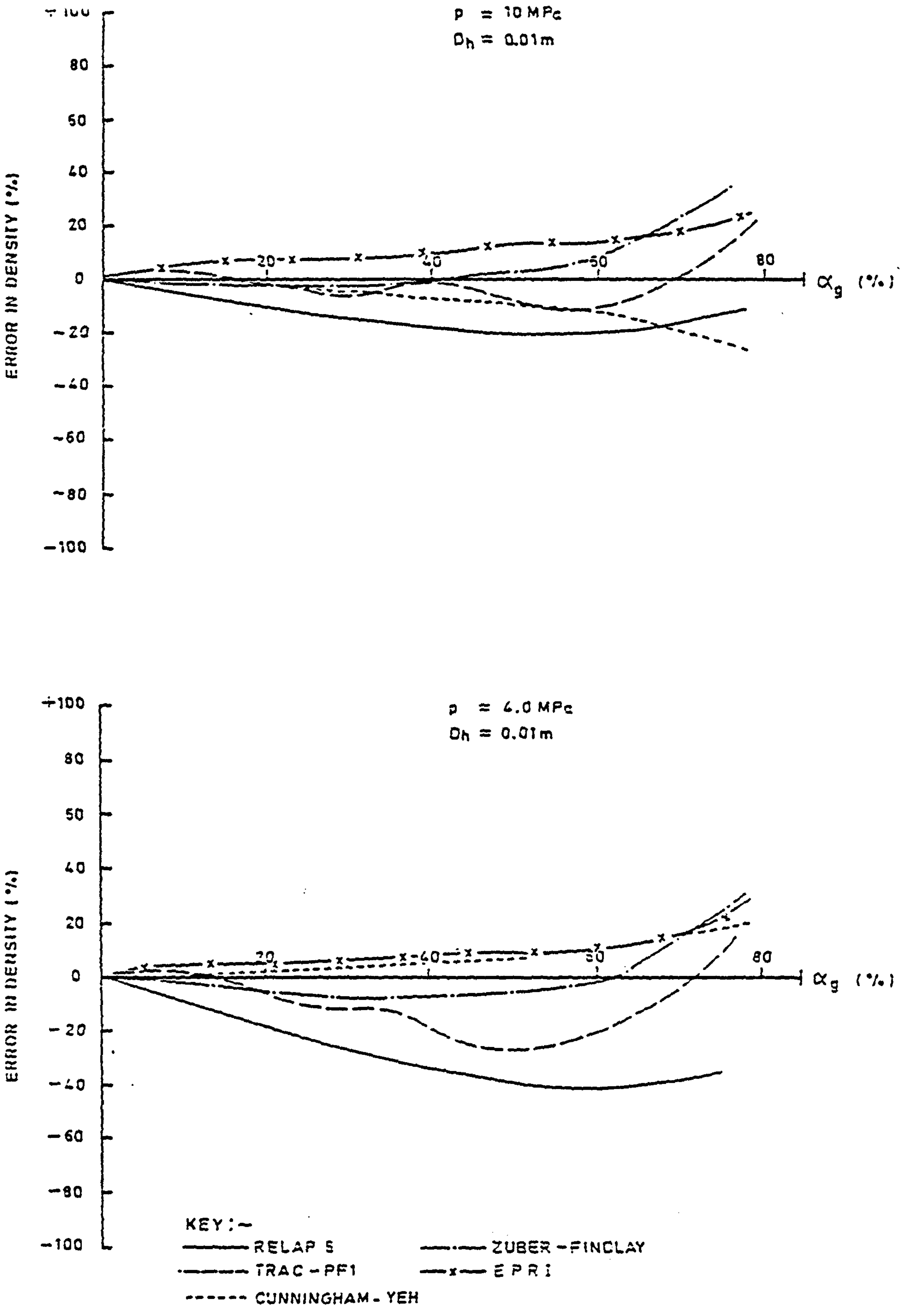


FIG.2 COMPARISON OF VOID FRACTION CALCULATIONS FOR $D_h = 1 \cdot 0$ m

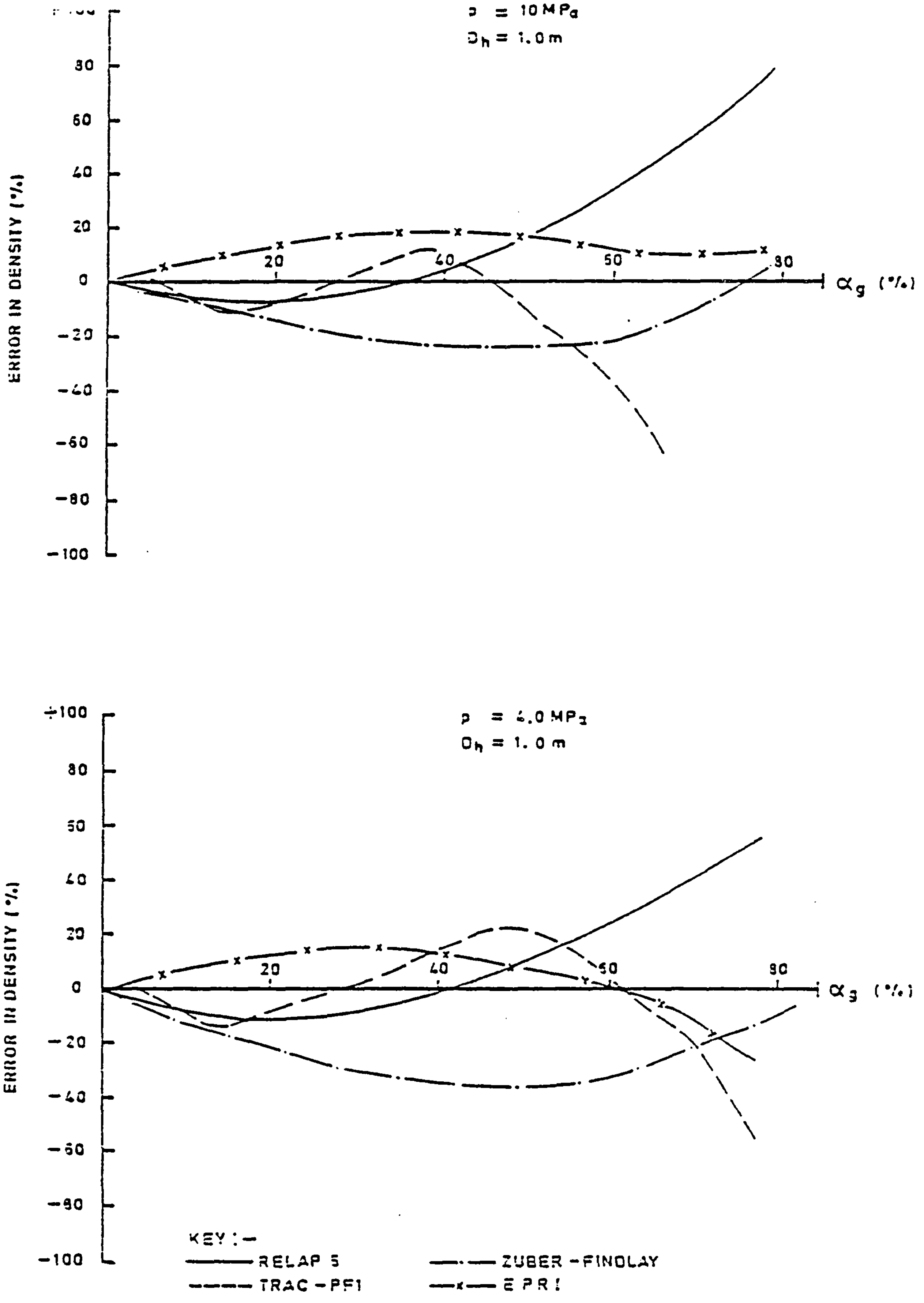


ERRORS IN MEAN TWO-PHASE MIXTURE DENSITY FOR $j_\ell = 0$

FIG. 3



COMPARISON OF CORRELATIONS FOR $j_\ell = 0, D_h = 0.01m$ FIG. 4



COMPARISON OF CORRELATIONS FOR $j_\ell = 0, D_h = 1.0m$ FIG. 3

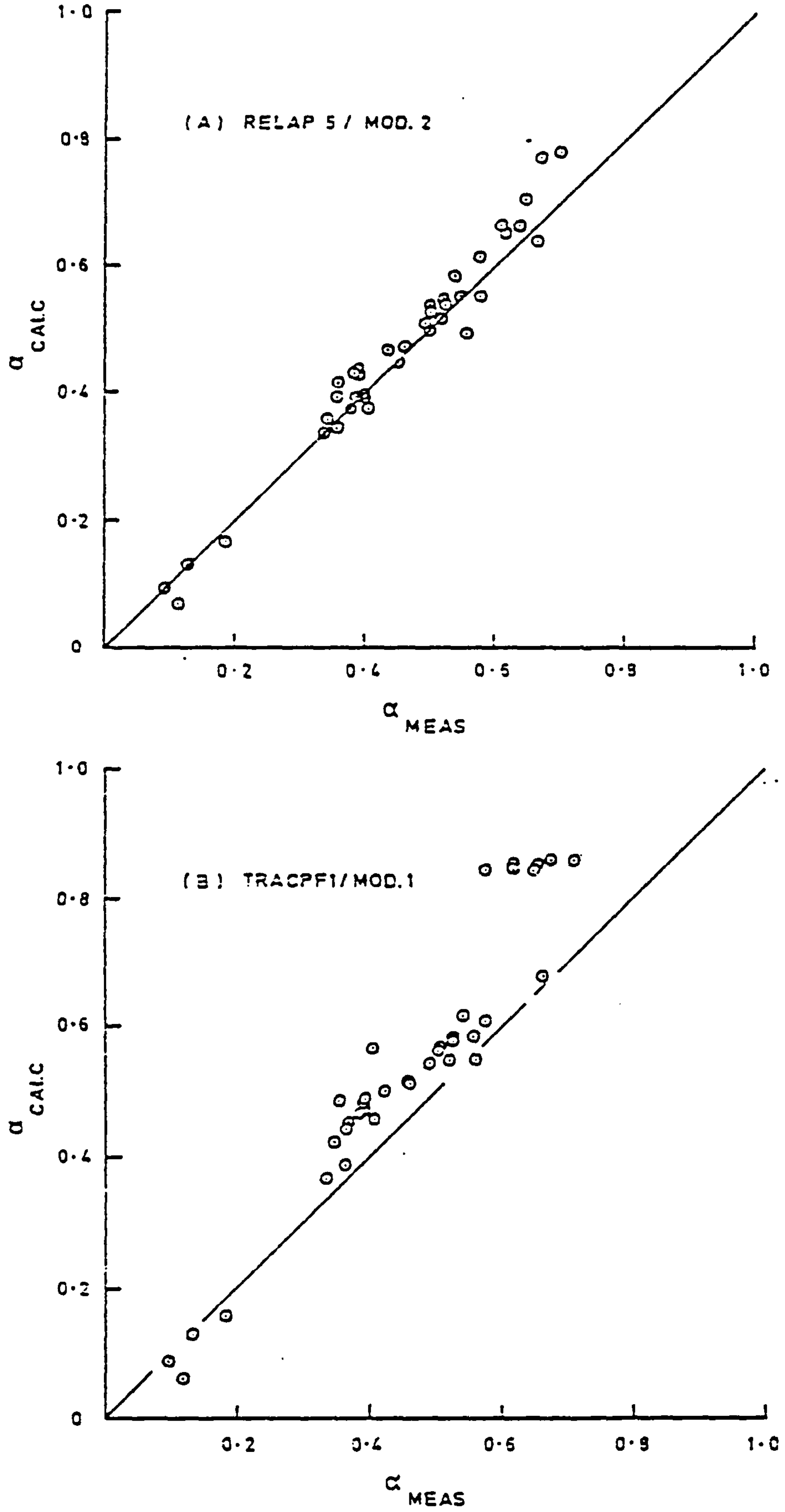


FIG. 6 COMPARISON OF MEASURED AND PREDICTED VOID FRACTIONS PETRICK DOWNFLOW DATA ($p = 7OMPa$ $D_h = 49mm$)

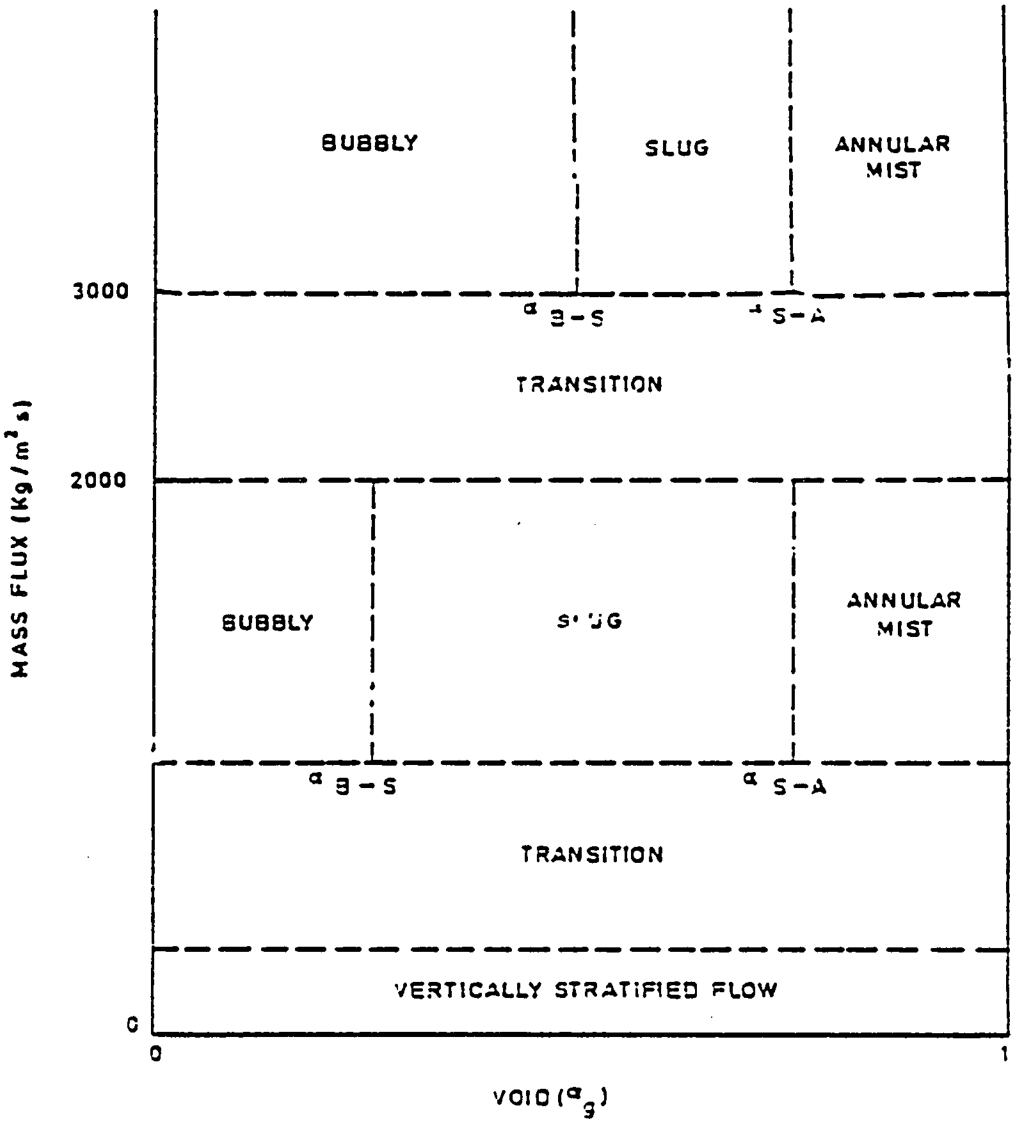


FIG. A1 VERTICAL FLOW REGIME MAP IN RELAP 5/MOD. 2

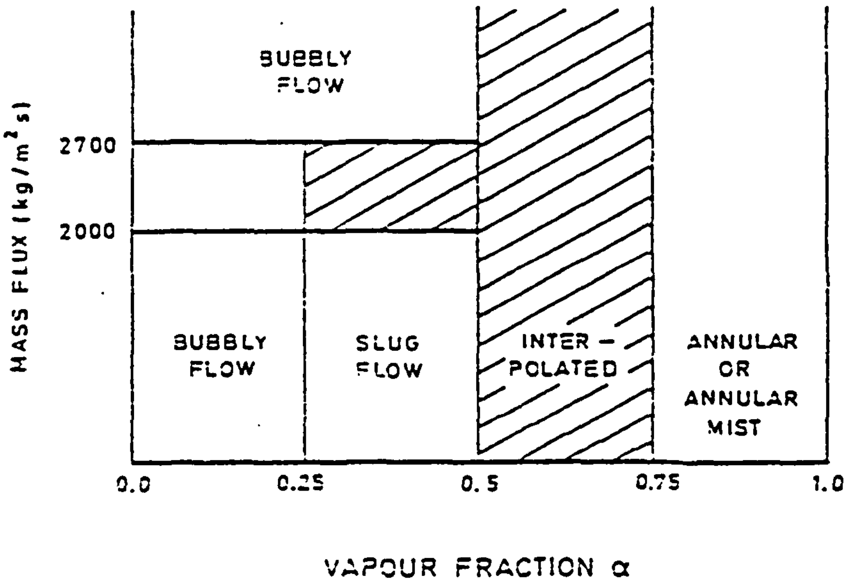


FIG. B1 FLOW-REGIME MAP FOR ONE-DIMENSIONAL HYDRODYNAMICS IN TRACPF1/MOD.1

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An assessment is carried out of the interphase drag correlations used in modelling vertical two-phase flows in the advanced thermal hydraulic codes RELAP5/MOD2 and TRAC-PF1/MOD1. The assessment is performed by using code models to calculate void fraction in fully developed steam-water flows, and comparing results with predictions of standard correlations and test data. The study is restricted to the bubbly and slug flow regimes (void fractions below 0.75).

The numerical results given in this paper allow a rapid estimate to be made of void fraction errors likely to arise in a particular code application due to deficiencies in interphase drag modelling.

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