HEAT TRANSFER -Pittsburgh 1987

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FOREWORD

The AIChE Symposium Series Volume contains almost all of the papers presented in the AIChE sessions at the 24th National Heat Transfer Conference held August 9—12, 1987 in Pittsburgh, Pennsylvania. As in previous years, beginning with "Heat Transfer - San Diego 1979," the volume is available at the meeting site. Abstracts or summaries of several AIChE papers are also included.

The papers for the ASME/AIChE cosponsored session "Non-Equilibrium Transient Phenomena" appear in a separate Symposium Volume published by ASME and also available at the meeting site.

The sections in this volume are arranged according to the Conference Sessions, "Process Heat Transfer," "Numerical Simulation of Multiphase Flow and Heat Transfer," "Hazardous Waste Onsite Disposal," "Heat Transfer Aspects of Advanced Reactors," "High Temperature Heat Transfer," "General Heat Transfer in Solor Energy," "Thermal Hydraulic and Phase Change Phenomena," "Analysis of Multicomponent Flow and Heat Transfer," "General Heat Transfer in Solar Energy," "Thermal Hydraulic and Phase Change Phenomena," "Analysis of Multicomponent Flow and Heat Transfer," "General Papers," and "Heat Transfer Aspects of Severe Reactor Accidents." The last session was cosponsored by the American Nuclear Society, which participated in the National Heat Transfer Conference for the first time, Denver 1986.

The preparation of this Symposium Volume went exceptionally smooth this year thanks in great part to the hard work and cooperation of the Session Chairmen and Co-Chairmen whose names are listed in the various sections. They assembled and obtained reviews, and edited the papers for their respective sessions. Another reason for the smooth preparation was the hard work of Maura Mullen and Bill Buchler of the AIChE publication staff who produced and printed the Symposium Volume. They cooperated in revising and clarifying the information for preparation of papers. Special thanks are due to the AIChE Program Chairman Paul E. Minton who organized the AIChE Session and whose ceaseless attention to detail and timing helped decisively to draw together the entire program in a timely fashion.

Finally Ralph P. Stein, a past Editor of several of the Symposium Volumes in this series, is to be thanked for preparing and making available to the AIChE session organizers the Paper Reviewer Database Search Program. This program contains almost 200 reviewers who voluntarily filled out the prospective Paper Reviewer Form over the last year. My experience in using these reviewers showed that they are responsive and responsible reviewers, greatly speeding up the review process. All the reviewers for the papers are acknowledged and their names are listed on the next page.

Robert W. Lyczkowski, Editor

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VOID FRACTIONS AND TWO-PHASE FRICTION MULTIPLIERS IN A HORIZONTAL TUBE BUNDLE

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An experimental investigation has been made on void fractions and frictional pressure drops in two-phase vertical crossflows in a horizontal tube bundle. Quick-closing plate valves were used to isolate the tube bundle, allowing the measurement of a volume-average void fraction. The void fractions were found to increase with increasing mass velocity for a fixed quality level. The two-phase friction multiplier increased with increasing mass velocity for a fixed value of the Martinelli parameter in both slug and spray flow, while decreasing with increasing mass velocity in bubbly flows.

INTRODUCTION

The two-phase heat transfer and pressure drop literature is extensive for intube and parallel flow geometries. However, only limited information is available for use in vertical, crossflow boiling. This paper is concerned with the evaluation of the pressure drop in a two-phase crossflow over a horizontal tube bundle and the two main parameters needed to predict the total pressure drop: void fraction and two-phase friction multiplier.

Although there have been no direct measurements of void fractions in tube bundles, Kondo and Nakajima (1) have taken indirect void fraction measurements in vertical air-water upflows across horizontal tube bundles. The data showed that the void fraction increased with superficial gas velocity, but the superficial liquid velocity had no effect on the void fraction. The results also showed that the number of tube rows affected the void fraction which could be the result of inlet effects from the mixing section, exit effects or both. The only other void fraction measurements have been for horizontal two-phase flows through tube bundles (e.g., (2)) and cannot be applied to vertical flows. With only limited data available on shellside void fractions, researchers have relied on intube models (3,4,5,6,7,8). However, no justification is given for this procedure. Leong and Cornwell (8) and Whalley and Butterworth (9) have used the homogeneous model.

More attention has been given to the shellside two-phase friction multiplier. Various investigators (2,3,5,7,9-16) have developed two-phase friction multiplier models but have either used an intube void fraction model, have used the homogeneous model or have not identified the void fraction model used to reduce the experimental data. A critical review of both two-phase friction multiplier data and models was performed by Ishihara et al. (13) in which several existing correlations (14,15,16) were evaluated. In general, it was concluded that all of the correlations predicted the shear controlled or high pressure drop data better than the low pressure drop data. The Martinelli separated flow model was then used by Ishihara et al. to develop a new correlation which predicted the shear controlled flow data for $X_{tt} < 0.2$ with good results; however for X_{tt} greater than 0.2 deviations were quite large, exceeding 60 percent. To improve the correlation in this range, Ishihara et al. (13) concluded that by categorizing the data according to flow pattern and then obtaining the best curve

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fit for each pattern, an improvement in predictions could be made.

As indicated in the literature survey (A much more detailed review can be found in (17)), there are no models capable of predicting the void fraction or two-phase friction multiplier in horizontal tube bundles in vertical crossflow. Hence, the main objective of this study was to design an experiment in which the void fraction and the total pressure drop occurring in adiabatic two-phase flows could be measured.

EXPERIMENTAL APPARATUS AND PROCEDURE

Air-water mixtures were used to model the two-phase adiabatic flows. Flow control valves were located upstream of the air turbine flow meter and downstream of the water turbine flow meter. For both the air and water rotameters these valves were located upstream. A control valve, located in the exhaust piping, was used to control the back-pressure on the test section. It was necessary to be able to instantaneously stop the flow of air and water to the test section. Hence, normally closed and open electrically-operated solenoid valves were used in both the air and water lines. The pressure level in the tube bundle and in the air flow meter were measured with Bourdon tube pressure gauges. Pressure drops in the test section were measured with five U-tube manometers. Each manometer was modified such that it could be inclined from a vertical position for improved accuracy during low pressure drop measurements. The air temperature in the air flow meter and the air-water mixture temperature were measured with copper-constantan thermocouples.

The test section consisted of a vertical rectangular channel. Solenoid-driven plate valves immediately upstream and downstream from the first and last rows of tubes in the bundle respectively, were used to isolate the tube bundle from the inlet mixing section and the exit section. These plates slid through slots in the channel wall, were connected by a series of linkages and were driven by a combination of an electric solenoid and springs. This combination provided enough force to quickly close the valves and to maintain enough force after closing to effectively seal the channel. The valves' action were synchronized through the use of a six-bar linkage. Microswitches attached to the valves permitted the closing time of the valves to be measured. The mixing section consisted of an inlet nozzle at the entrance to the test section and a series of three flow straighteners and mixers. The section containing the tube bundle model was fitted with 27 rows of 7.94 mm diameter tubes with three full and two half tubes in each row. Tube length was 82.6 mm. The inline, square array had a pitch-to-diameter ratio of 1.3. To reduce bypass leakage and to minimize wall effects, the two walls parallel to the tube bank were machined such that the two outside columns of tubes had only one half the tube diameter exposed to the flow. Pressure taps were located in the side walls, so that five pressure drops (across six, five, five, five and six rows) could be measured.

Single-phase pressure drop tests were taken with water to verify the experimental apparatus and procedure and to reduce the two-phase results. To determine the effects of mass velocity on the void fraction and the two-phase friction multiplier, the inlet quality was varied over a range of values for a fixed total mass velocity. Upon achieving a steady-state condition, all temperatures, pressures, pressure drops and flows were recorded. In all of the air-water tests, any air accumulation in the manometer lines was purged prior to recording the deflections so that accurate pressure drop measurements would be obtained.

After recording all of the flow data, the final step required closing the flow isolation plate valves so that the void fraction measurement could be made. The water which was trapped between the plate valves settled to some level; both this level and the number of tube rows submerged to the nearest one quarter of a tube were recorded. Any water clinging to the tube walls above the water level was taken into account when calculating the void fraction. To estimate the amount of water clinging to the tubes, a series of calibration tests were performed. These data were combined to estimate the void fraction in the tube bundle. Details of this are given in (17). To determine if the measured void fraction was affected by the plate valve closing time, measurements were made at various values of closing time. No significant variation in void fraction was measured for closing times from 0.035 to 0.50 sec.

The single-phase friction factor was calculated with

$$f_{\ell t} = 2\Delta P_{F1\phi}\rho_\ell/MG^2_{\ell t} \qquad (1)$$

Because of the possibility of inlet and exit effects, the single-phase friction factors were computed between the second and third, third and fourth and fourth and fifth pressure taps and then averaged.

As in the calculation of the single-phase friction factor, the adiabatic two-phase friction multiplier was determined using pressure drop data between the second and fifth pressure taps. The two-phase friction multiplier based on the liquid phase flowing alone, ϕ_ℓ^2, is:

$$\phi_\ell^2 = \frac{\Delta P_{F2\phi}}{\Delta P_{F\ell 1\phi}} = \frac{2\rho_\ell \Delta P_{F2\phi}}{Mf_\ell G_\ell^2} \qquad (2)$$

The determination of the two-phase frictional pressure drop required that the acceleration and gravitational pressure drop components be subtracted from the total pressure drop. These two components were calculated using the measured void fraction.

The raw and reduced data for all tests and details of the apparatus and procedures can be found in (17). The nominal range of experimental conditions covered in this investigation were: $3.0 \times 10^{-4} \le x \le 0.68$; $55 \le G \le 680$ kg/m^{2}s; $1 \le P \le 3$ atm. The mixture temperature was about 10°C for all tests. Uncertainties for the majority of the experimental data, as estimated through a propagation-of-error analysis, are suggested to be: G, ± 3%; x, ± 7%; α, ± 4%; ϕ_ℓ^2, ± 4%. At low qualities and high mass velocities, the uncertainties in α and ϕ_ℓ^2 could be substantially greater than these values.

EXPERIMENTAL RESULTS AND ANALYSIS

Single-Phase Friction Factor

The single-phase friction factors for Reynolds numbers from 200 to 10,000 were compared to the ESDU (18) and Zhukauskas (19) correlations and the data of both Frass and Ozisik (20) and Kays and London (21). Generally, there was close agreement between sources and the data for Re > 1000; however, deviations between the data and the two correlations for Re < 1000 was as high as 75 percent. This was attributed to equipment limitations in measuring the very small pressure drops in this Reynolds number range. Except for this, it was concluded that because there generally was close agreement between the data and the correlations, the tube bundle model used in this study was representative of the actual behavior encountered in larger bundles. To accurately represent the single-phase friction factor, a three part Blasius-type friction factor model was used to correlate the data.

Void Fraction

It was speculated that by comparing the measured adiabatic void fraction data to the homogeneous void fraction model the type of flow model (separated or homogeneous) required to reduce the total pressure drop would be clearly indicated. In Figure 1, the void fraction data and the homogeneous model at pressures of one and three atmospheres have been plotted. As can be seen, the homogeneous void fraction model dramatically overpredicts the void fraction data for all quality and mass velocity levels. Although the general trends between the homogeneous model and the data are the same, the poor agreement indicates that the homogeneous flow model is not applicable. Thus, the separated flow model must be used. There are mass velocity effects in the void fraction data with trends similar to those of intube flow.

In the data, there was some scatter which may be attributed in part to pressure level. To eliminate the effects of pressure, a reduced void fraction was used which is the ratio of the measured void fraction to the homogeneous void fraction, evaluated at the same conditions. In Figure 2, the reduced void fraction clearly shows the mass velocity trends. As the mass velocity increases the reduced void fraction also increases; hence, the actual void fraction begins to approach that of the homogeneous model. Note also that as quality approaches unity the reduced void fraction tends to approach unity. However, as the quality approaches zero if the trend in the data is extrapolated it appears that the reduced void fraction will approach zero which is not possible. Based on the physics of the process, an argument can be made that as the quality tends towards zero, the reduced void fraction will approach unity after some minimum value is reached. If it is assumed that at very low qualities the gas phase is present in the form of very small bubbles, the flow will behave essentially as a homogeneous flow, the actual void fraction will approach the homogeneous void fraction, and, hence, the reduced void fraction will approach unity. The critical quality, after which for lower qualities the reduced void fraction would begin to increase, would tend to be higher for higher mass velocities since at higher mass velocities the flow would behave more homogeneous (e.g., see data for G = 683 kg/m²s). This trend is suggested based on the observation of only a few data points and physical reasoning. Additional data are required to confirm these trends. Because of the lack of data it was impossible to fix the various combinations of quality and mass velocity at which the minima in the reduced void fraction curves occur. Thus, the minimum value of the reduced void fraction was assumed, somewhat arbitrarily for correlation purposes, to never be less than 0.1 for any combination of mass velocity and quality.

Utilizing the boundary condition at x = 1.0, $\alpha/\alpha_H = 1$, the reduced void fraction data were correlated as:

$$\alpha/\alpha_H = 1 + 0.360\ G^{-0.191}\ \ln x \quad (3)$$

This is a dimensional equation in G where Equation (3) requires the mass velocity to have units of kg/m^{2}s. Using the restriction on the reduced void fraction given above, the final void fraction model consisted of two parts. If the reduced void fraction predicted by Equation (3) was less than 0.1 then $\alpha/\alpha_H = 0.1$, otherwise Equation (3) would define the reduced void fraction.

A comparison of the final two-part void fraction model to the data resulted in an average absolute deviation between the predictions and the experimental data of 10.3 percent, with 98 of the 108 data predicted with a deviation of less that ± 20 percent. Note that for this curve fit, two data points were rejected by applying Chauvenet's criterion (22). (Chauvenet's criterion is a technique by which outlying points can be rejected from the data set.) Figure 3 shows a plot comparing the void fraction model to the data. In general, the agreement between the model and the data improved for qualities greater than 2 x 10^{-2}.

Two-Phase Friction Multiplier

To determine if the mass velocity had an effect on the two-phase friction multiplier similar to that on the void fraction, the data were plotted in such a way as to (ideally) eliminate all effects except those associated with the mass velocity. The values of ϕ_ℓ^2 were plotted against the Martinelli parameter evaluated using the value of the Blasius exponent appropriate for each test run. The values of ϕ_ℓ^2 were also plotted (Figure 4) against the Martinelli parameter with m = 0.2. Comparison of these two plotting schemes showed that when m = 0.2 the two-phase friction multiplier data exhibited far less scatter than when m was allowed to vary. The use of a constant value of m to evaluate a diverse data bank, where it is likely that a range of values of m would be more applicable, has been done by Ishihara et al. (13). In that study the authors assumed that m = 0.2 would not be a bad approximation for most tube layouts over the range of $10^3 \leq Re \leq 10^5$. This approach was also adopted in the present study of the two-phase friction multiplier data both because of convenience and more well-behaved data curves.

As shown in Figure 4, the values of ϕ_ℓ^2 are shown to increase with increasing mass velocity at a given value of χ_{tt} up to a value of $\chi_{tt} \approx 0.9$, after which a crossover occurs and the ϕ_ℓ^2 data then decrease with increasing mass velocity. The Ishihara et al. (13) correlation, given in Equation 4 with

$$\phi_\ell^2 = 1 + C/\chi_{tt} + 1/\chi_{tt}^2 \quad (4)$$

m = 0.2 in the Martinelli parameter and C = 8, predicted the general trend in the data. However, the use of C = 8 as suggested by Ishihara et al. (13) did not result in a good representation of the data; the average absolute deviation between the predictions of the two-phase friction multiplier and the experimental data was 41 percent and the data were overpredicted by an average of 17 percent. Assuming that a Martinelli model could be used to correlate the present ϕ_ℓ^2 data, Equation 4 was solved for the C-factor. As shown in Figure 5 the mass velocity effects as well as the crossover point are now clearly visible. It is interesting to note that at the crossover point, the C-factor is approximately equal to eight, the same value obtained by Ishihara et al. (13). Although the mass velocity effects on the C-factor are clearly visible, the reason for the different trends in the data (both increasing and decreasing values of ϕ_ℓ^2 with increasing mass velocity) at different flow conditions is not readily explainable. However, the two-phase friction multiplier has been observed (e.g., (10,15) to be a function of flow pattern. It is likely that the different data trends are the result of a change in flow pattern.

To observe the effects of flow pattern on the two-phase friction multiplier, the C-factors were separated according to what flow pattern was present. While visibility to the tube bundle was somewhat limited because of the hardware used in the void measurement, the flowing mixtures generally could be classified as either a bubbly, slug or spray flow. Because of the difficulties associated with quantifying the flow patterns the flow pattern map developed by Grant and Chisholm (2) was used to classify the flows. Because of uncertainties in flow pattern transitions, any data point that was within 15 percent of a transition curve was, for data manipulation purposes only, assumed to be in both flow patterns. As a result, some data were plotted more than once allowing data trends to be more easily established. Using this breakdown, the C-factors plotted against χ_{tt} for each flow pattern can be found in Figure 6. The C-factor plotted in this fashion clearly exhibit the dependence of mass velocity and flow pattern on the two-phase friction multiplier. For the data in both spray and slug flow patterns, ϕ_ℓ^2 increased with increasing mass velocity for a given value of χ_{tt}. However, in the bubbly flow pattern, ϕ_ℓ^2 decreased with increasing mass velocity. The fact that this trend is evident in this bubbly flow data explains the crossover effect observed in Figures 4 and 5.

To correlate the two-phase friction multiplier data, the C-factor for each flow pattern was expressed as a function of χ_{tt} and G where:

$$C = (C_1 G^{C_2}) \ln \chi_{tt} + C_3 G^{C_4} \qquad (5)$$

In the Ishihara et al. (13) model the only coefficient which was subject to adjustment was that on the $1/\chi_{tt}$ term; the $1/\chi_{tt}^2$ term had a coefficient of unity. The C-factors in each flow pattern were initially correlated using Equation 4 without a coefficient on the $1/\chi_{tt}^2$ term. This was the form of the C-factor plotted in Figure 6. However, by introducing an additional coefficient, C_5, on the $1/\chi_{tt}^2$ term, a significant improvement could be made compared to the original form of the Ishihara et al. (13) model with the C-factor given by Equation 5. The final form of the correlation was:

$$\phi_\ell^2 = 1 + C/\chi_{tt} + C_5/\chi_{tt}^2 \qquad (6)$$

Table 1 shows the resulting curve fits, while Table 2 shows the comparison of each correlation to the experimental data. As can be seen the agreement is good. Since Equation 5 is a dimensional equation in mass velocity, the G terms were required to have units of kg/m^2s. A scatter band plot of the two-phase friction multiplier data is shown in Figure 7. It was found that the two-phase friction multiplier correlations for the slug and spray flow patterns would give values of ϕ_ℓ^2 which were less than unity if G $\lesssim$ 43 kg/m^2s; no problem was found with the bubbly flow correlation. Hence, the present spray and slug flow correlations were restricted to flow conditions where G $\geq$ 43 kg/m^2s. For any mass velocity less than 43 kg/m^2s it was decided that the Ishihara et al. (13) model be applied.

CONCLUSIONS

In an experimental investigation void fractions and pressure drops were measured for two-phase vertical crossflow in a horizontal tube bundle. Adiabatic air-water two-phase flows were tested over a large range of qualities and mass velocities. The data were used to develop correlations for the void fraction and two-phase friction multiplier. Based on the results of this study, the following conclusions have been drawn:

1. Quick-closing plate valves can effectively be utilized to isolate two-phase flows in a tube bundle so that a volume-average void fraction measurement can be made.

2. Void fractions were found to be a strong function of mass velocity where an increase in mass velocity led to an increase in void fraction for a given quality.

3. The two-phase friction multiplier was found to be a function of both mass velocity and flow pattern. For slug and spray flows the two-phase friction multiplier, for a given value of χ_{tt}, was found to increase with increasing mass velocity, but was found to decrease with increasing mass velocity in bubbly flows.

Additional work is underway to compare the void fraction and two-phase friction multiplier models to diabatic data. It is hoped that the correlations can be nondimensionalized through comparison to these data.

ACKNOWLEDGEMENTS

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NOTATION

C	C-factor
C_1 to C_5	correlation parameters in Equations 5 and 6
D	tube diameter (m)
f	single-phase friction factor defined in Equation 1
G	mass velocity based on minimum flow area (kg/m^2s)
g	gravitational constant (9.806 m/s^2)
M	number of tube rows between pressure taps
m	exponent in Blasius type friction factor equation
N	number of data
ΔP	pressure drop (kPa)
P	pressure (kPa)
x	quality

Greek Symbols

α	void fraction
α_H	$1/(1 + ((1-x)/x)(\rho_v/\rho_\ell))$ homogeneous void fraction
μ	viscosity (kg/m s)
ρ	density (kg/m^3)
ϕ^2	two-phase friction multiplier
χ_{tt}^2	$((1-x)/x)^{2-m}\ (\rho_v/\rho_\ell)(\mu_\ell/\mu_v)^m$

Subscripts

F	friction
ℓ	liquid phase only
ℓt	total flow assumed liquid
v	vapor phase only
1ϕ	single-phase
2ϕ	two-phase

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TABLE 1. Correlation results for the liquid-only two-phase friction multiplier. (Equations 5 and 6)

Flow Pattern	Bubbly	Slug	Spray
C_1	7.34×10^{-6}	81.4	1180.
C_2	1.51	-0.643	-1.50
C_3	10.7	3.12	3.87
C_4	-0.057	0.233	0.207
C_5	0.774	1.09	0.205

Table 2. Comparison of the two-phase friction multiplier model to the experimental data.

Flow pattern	N	AAD*	R**
Bubbly	37	0.110	0.961
Slug	65	0.169	1.040
Spray	7	0.070	0.970
All flow patterns combined	109	0.143	0.999

$*AAD = (\Sigma|\phi^2_{\ell,Pre}/\phi^2_{\ell,Exp} - 1|)/N$

$**R = (\Sigma(\phi^2_{\ell,Pre}/\phi^2_{\ell,Exp}))/N$

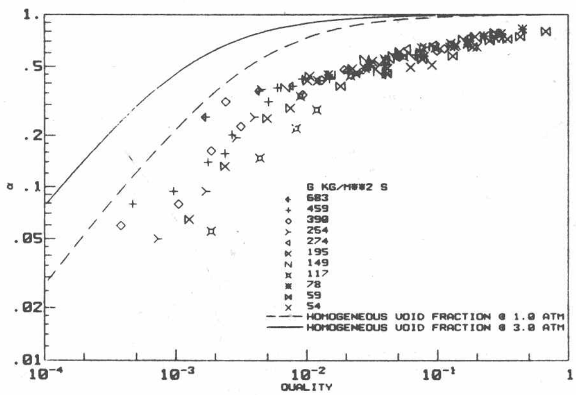

FIGURE 1. Void Fraction data from present study.

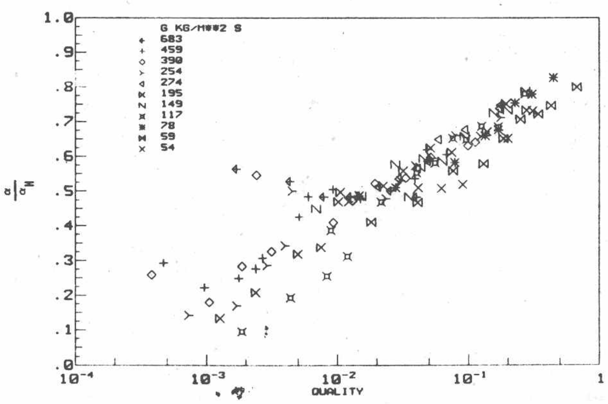

FIGURE 2. Effects of mass velocity on the reduced void fraction.

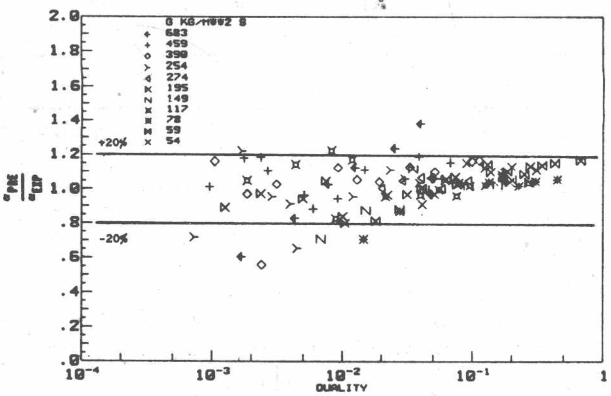

FIGURE 3. Comparison of predicted and experimental adiabatic void fraction data.

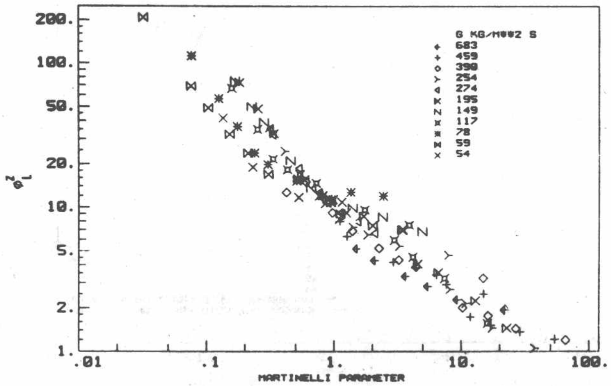

FIGURE 4. Liquid-only two-phase friction multiplier data.

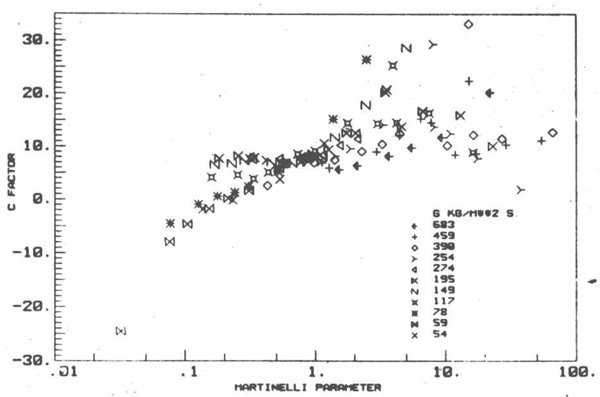

FIGURE 5. C factors reduced using Martinelli-type model.

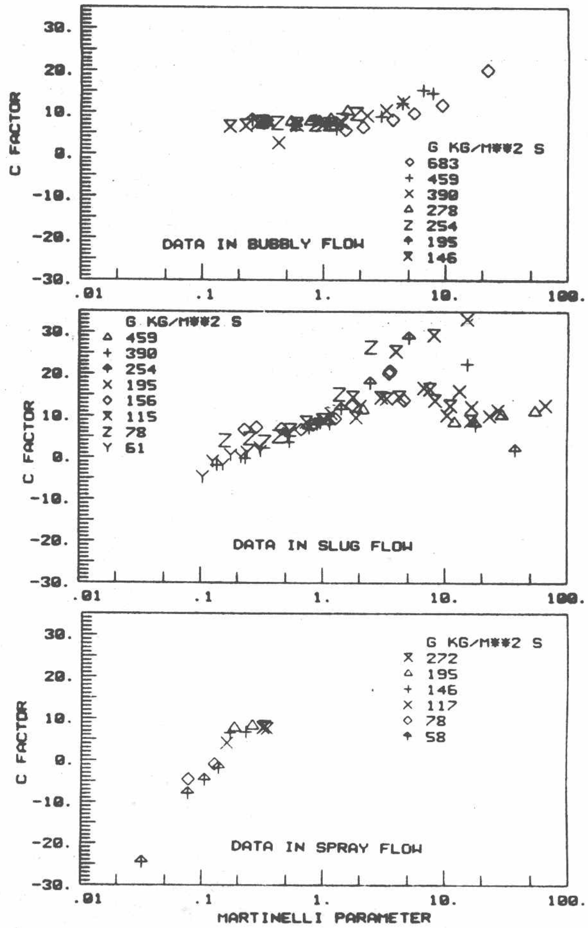

FIGURE 6. C factors for all flow patterns.

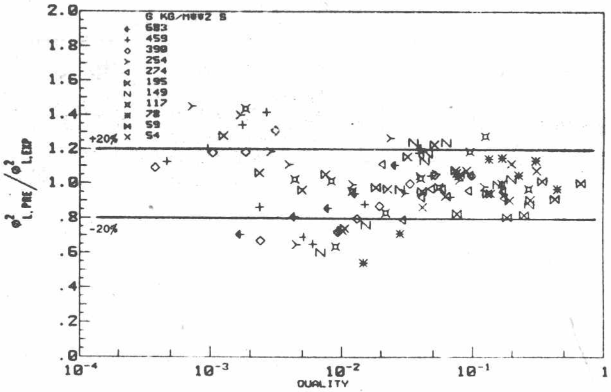

FIGURE 7. Comparison of predicted and experimental adiabatic liquid-only two-phase friction multiplier data.