**THE SPOUT OF AIR JETS UPWARDLY INJECTED
INTO A WATER BATH**

By

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Abstract

The spout formed at the free surface of a gas-stirred liquid has received little attention even though it has both theoretical and practical significance. In steelmaking ladles, for example, the spout is the site of strong metal-slag-air mixing which affects: the kinetics of reactions at the slag-metal interface, the absorption of oxygen by the bath and the temperature drop of the bath. Notwithstanding its importance, the spout is usually neglected in flow models of gas-stirred baths because it has not been characterized quantitatively; assumption of a flat top surface, however, reduces the accuracy of the velocity and kinetic energy predictions, particularly close to the spout region.

Thus in this study, the spout of upwardly injected gas jets in water was characterized experimentally in terms of gas fraction, bubble frequency and axial velocity distributions. The measurements were made with a two-element electroresistivity probe coupled to a microcomputer. Special hardware and software were developed to analyze the signals generated by contact of the bubbles with the sensor, in real time, for the turbulent flow conditions prevailing in the jet plume and spout. Correlations of the gas fraction with axial and radial position for different gas flow rates have been established from the measurements. The dimensions of the spout were obtained from time-exposure photographs; when compared with the gas fraction measurements, the spout boundary always corresponded to values ranging from 0.82 to 0.86. The radial profiles of bubble frequency at different levels in the spout have a bell shape; the bubble frequency decreases with increasing height. The velocity of the bubbles in the spout drops linearly with increasing axial position. Measurements of bath velocity near the walls of the vessel were also conducted with a laser doppler velocimeter for comparison

to model predictions.

The gas fraction data obtained for the spout then were incorporated into a mathematical model of turbulent recirculatory flow with which predictions of velocity, kinetic energy and effective viscosity in the bath were made. Predictions of the model were compared with the experimental measurements as well as with predictions assuming a flat bath surface (no spout); and the importance of incorporating the spout thus was demonstrated. The variation of the total kinetic energy in the spout with gas flow rate was determined. The energy increased with flow rate, as expected, but at a critical value, the rate of increase abruptly rose. Based on photographs taken of the gas/liquid dispersion, the increased spout kinetic energy appears to be related to the location of bubble break-up and possibly to gas channeling. At lower flow rates below the critical value, the bubble break-up occurs relatively close to the nozzle, whereas at higher flow rates bubble disintegration is nearer to the surface. At the lower flow rates the gas/liquid interaction was maximum which promoted the gas/liquid momentum transfer. Moreover, at the higher flow rates the gas dispersion was observed intermittently to be a continuous chain of large envelopes which could permit a fraction of the gas to channel through the bath for a considerable distance. The channeling phenomenon could lead to an inefficient gas/liquid energy transfer resulting in a reduced efficiency of bath mixing and enhanced energy release at the surface. These results can explain the observations of previous investigators who found that beyond a critical gas injection rate, the rate of decrease of mixing time with flow rate decreased. The metallurgical consequences of the spout and its influence on the flow field, especially in the near-surface region, have been highlighted, thus unveiling the practical bearing of the spout on the gas injection process.

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List of Symbols

A	Parameter in Eq.(5.2).
C_1, C_2, C_d	Constants in the $k - \epsilon$ turbulence model.
d_o	Orifice diameter, mm.
f_b	Local bubble frequency, s^{-1} .
Fr	Modified Froude number $= \frac{Q_o^2 \rho_G}{g d_o^5 (\rho_L - \rho_G)}$
g	Acceleration due to gravity, (9.81 m/s^2) .
G	Generation term in the turbulence model.
h_b, h_{max}	Bath depth; maximum height of the spout, mm.
k	Turbulent kinetic energy, Joules/kg.
K_s, K_1, K_2	Desulphurization rate constant; Constants in Eq.(5.3).
K_3, K_4, K_5	Constants in Eq.(5.8).
n	Parameter in Eq.(5.2); subindex in Eq.(4.1) indicating class interval with the largest central value in bubble velocity histogram.
P, P_{atm}	Pressure; atmospheric pressure, kg/m s^2 .
Q, Q_o	Gas flow rate at STP; gas flow rate at given conditions (atmospheric pressure plus static head of water and 20°C), cm^3s^{-1} .
r	Distance in the radial direction, mm.
r_o, r_p	Core radius; statistic defined in Eq.(4.1).
$r_{\alpha max/2}$	Half-value radius, mm.

T_L	Temperature of the liquid.
τ	Mixing time.
U_{ax}	Vertical velocity of the fluid at the static level of the bath, along the axis.
U_b	Local bubble velocity, m/s.
U_r	Radial component of mean velocity, m/s.
U_z	Axial component of mean velocity, m/s.
W	Weight of the bath.
x	Non-dimensionalized axial distance $= z(\frac{g}{Q_0^2})^{1/5}$
z	Vertical distance from the nozzle, mm.
α	Local time-averaged gas fraction.
α_{maz}	Local gas fraction at plume centreline.
α_{min}	Local gas fraction at spout centreline.
ϵ	Dissipation rate of turbulent kinetic energy, J/kg s.
ε	Mixing power density.
ε_s	Specific input power density, Watts/kg.
ρ_L, ρ_G	Density of liquid; density of gas at orifice, kg/m ³ .
σ_k	Effective Prandtl number for k .
σ_ϵ	Effective Prandtl number for ϵ .
μ	Molecular viscosity, kg/m s.
μ_{eff}	Effective viscosity, kg/m s.
μ_t	Turbulent viscosity, kg/m s.
ν	Turbulent kinematic viscosity, m ² /s.

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Chapter 1

Introduction

The importance of ladle metallurgy in steelmaking has now become established, as the demand for a better quality, clean steel increases. Ladle metallurgy involves various operations including homogenization with respect to temperature and chemical composition, desulphurization, deoxidation, injection of alloying elements and removal of inclusions. In order to conduct these operations, submerged gas injection is the most widespread technique that has been utilized. Submerged gas injection can be achieved by various means through a tuyere, porous plug or lance; the gas may be injected from the bottom, top, laterally or in a combined mode. Although submerged gas injection techniques have been widely adopted owing to the efficient mixing of the bulk liquid, the large surface area produced for reaction, easy control of process variables such as gas flow rate, relative simplicity and low capital costs, a number of problems remain. These include severe bath slopping, refractory wear, poor utilization of certain additions, reoxidation and excessive cooling of the bath.

To improve our understanding of these injection phenomena, physical and mathematical modelling studies, which can give a clearer insight into the complex nature of the fluid flow in these systems, are necessary. A review of the literature, presented in the next chapter, shows that a gas-stirred melt, with a centrally placed nozzle at the bottom of the vessel has been extensively studied. Physical modelling has given a qualitative picture of the fluid flow in the bath, and experimental measurements have helped identify regions of high turbulence. Mathematical modelling has made it

possible to make quantitative predictions regarding mixing and homogenization times as well as regions of high turbulent kinetic energy which lead to accelerated refractory wear. Over the years the mathematical models being developed have improved greatly. The turbulence in the bath has been studied utilizing the $k - \epsilon$ model; the two-phase plume has been experimentally investigated and correlations, which can be used in models, have been developed to represent this region [1,2,3]. These are some of the difficult aspects in mathematically modelling turbulent recirculatory flow in a gas-stirred liquid.

There are several areas in these systems which have not been addressed, one of which is the free surface of the liquid; this is the focus of attention in the present study. This free surface, which has a significant bearing on the process, generally has been assumed to be flat in mathematical models. This is a convenient but incorrect representation which can lead to inaccurate model predictions especially near the top of the bath. The free surface is important from a practical viewpoint because excessive turbulence in this region can lead to bath slopping and opening up of the slag layer which exposes the metal to reoxidation and heat losses. Moreover at the slag-metal interface, important reactions like desulphurization and deoxidation occur, and the refractories near the free surface region undergo accelerated wear. These phenomena can be better understood if the free surface is correctly represented in the mathematical model of the turbulent recirculatory flow in a gas-stirred liquid. This may permit better control of some of the process variables such as flow rate which can directly affect the phenomena occurring at the free surface discussed above as well as the efficiency of the overall gas-injection process.

The aim of this investigation was to improve our understanding of the free surface, in particular the spout formed in a gas-stirred water bath. In the study, the gas-fraction distribution in the spout was characterized experimentally, and the data obtained then

were incorporated into a mathematical model to make fluid flow predictions for the bath. Hence the importance of incorporating the free surface in a recirculatory flow model for a gas-stirred liquid bath has been examined.

Chapter 2

Literature Review

2.1 Physical and Mathematical Modelling Studies

The gas-stirred liquid bath has been modelled by several investigators, in an attempt to comprehend the hydrodynamics of the system. Szekely and Asai [4,5] modelled the turbulent recirculatory flow in a gas-stirred ladle. Turbulence was simulated by the Kolmogorov-Prandtl one-equation model and qualitative agreement was found between model predictions and experimental observations. Szekely et al. [6] undertook two-dimensional aqueous modelling of an argon-stirred ladle, in which the gas jet was modelled as a solid wall moving upwards at constant velocity; the turbulence was simulated using the $k - W$ model; qualitative agreement was found between measurements and model-predicted velocities [7]. Deb Roy et al. [8] and Grevet et al. [9] reported two-dimensional computations of argon bubbling in vessels, based on buoyancy-driven flow models, in which the plume was assumed to be cylindrical; average plume void fractions were estimated for both slip and non-slip conditions. For the slip condition, the gas was assumed to move with a “terminal velocity” relative to the liquid. Salcudean et al. [10] performed calculations for steady-state, buoyancy-induced, three-dimensional flow to simulate gas bubbling in ladles. The $k - \epsilon$ model was used to represent turbulence in the bath. Salcudean et al. [12] developed a computational scheme for predicting flow fields and heat-transfer phenomena associated with asymmetric gas-driven flows

in systems of cylindrical geometry. Mass, momentum and energy conservation equations were solved numerically based on the Marker-and-Cell technique [13]. Specific solutions were obtained for direct comparison with other two-dimensional model predictions. Flow patterns for centered and off-centered, conical plumes were presented. The convective effect on the temperature fields in an initially stagnant and fully stratified fluid due to gas injection at the bottom surface, was illustrated. Salcudean et al. [14] presented a three-dimensional model to simulate the transient temperature field and homogenization time for a gas-stirred liquid in a cylindrical vessel. Velocity fields, temperature distributions and mixing times were obtained using the algebraic models of Pun and Spalding [11], and of Sahai and Guthrie [15] and the $k - \epsilon$ turbulence model. The velocity fields and mixing times were compared with experimental data. Sahai and Guthrie [16,17] developed a generalized expression for axisymmetric gas stirred systems; plume velocities and the average speed of liquid recirculation were related to vessel diameter, gas flow rate and liquid depth with the aid of an energy balance technique. In all the models mentioned above, the spout formation was not considered and the free surface was assumed to be flat. Tse-Chiang et al. [18] studied fluid flow in a 60 t melt stirred by argon. Direct measurements in liquid steel were conducted from which the surface velocity was found to vary with the gas flow rate to the power of $1/3$.

Sano and Mori [19] proposed a simple model to characterize fluid flow in a liquid bath agitated by gas bubbles. The model postulated that the bath consists of two zones, a central bubble plume moving upward and a liquid annular zone moving downward. The analysis was based on a steady-state energy balance for the plume, which states that the rate of energy dissipation due to liquid recirculation is equal to the rate of energy input. They assumed that the rate of energy dissipation is equal to the difference between the rate of kinetic energy associated with the liquid moving upward and that

of the liquid moving downward plus an energy dissipation due to the bubble slip. The equations then derived permitted the calculation of the liquid velocity in the plume zone, the liquid recirculation velocity and the mixing time as a function of gas flow rate, bath depth and cross-sectional area of the plume. The model predicted that the liquid velocity in the plume was directly proportional to the gas flow rate to the power of $1/3$.

Tacke et al. [20] applied an integral-profile technique to study vertical gas injection in water and mercury. This technique involved the utilization of the conservation equations of mass for the gas and liquid and the conservation equation for the vertical momentum in integrated form. The profiles of gas concentration and liquid velocity were taken to be Gaussian and the velocity difference between the rising bubbles and the liquid was introduced. The set of differential equations that resulted from the analysis was solved for a region separated from the nozzle where the maximum gas fraction had decreased to fifty percent. Reasonable predictions of axial bubble velocity profiles were obtained.

Haida and Brimacombe [21] conducted water-model experiments involving gas injection through a single axial tuyere located at the bottom of a cylindrical vessel. They measured the shear stress on the bottom surface of the vessel by an electrochemical technique, and confirmed the presence of a maximum in the variation of the spatial average shear stress with increasing gas flow rate. They attributed it to the phenomenon of "channelling" in the plume region which lowers the efficiency of gas/liquid energy transfer.

A comprehensive review of the literature has revealed that the spout of a submerged gas jet, has received little attention by previous researchers. Gray et al. [22] have qualitatively described the formation of two important regions in the upper portion of a gas-stirred bath, namely a disengagement zone and a splash zone. Splashing

and spitting were studied by Paul and Ghosh [23] for various tuyere arrangements in physical models of the LD and Q-BOP processes and comparisons between them were drawn. The LD process was found to exhibit greater splashing and spitting. Also, the influence of different injection positions and design of injectors on splashing was investigated by Robertson and Sabharwal [24].

Modelling of transient, free-surface liquid flow utilizing computer techniques such as MAC and SOLA has been applied to the filling of moulds with molten metal by Stoehr [25]. Fluid flow modelling in a water bath, with gas injection through horizontal tuyeres, has been conducted by Shook [26]. The free-surface profiles were obtained by digitizing the traces of the experimental surfaces; the velocity measurements in the bath were made with a laser doppler velocimeter. It was observed that the mean cell kinetic energy increased with flow rate up to $1500 \text{ cm}^3\text{s}^{-1}$, beyond which it decreased abruptly. This phenomenon was found to occur even when the surface cells were not included in the calculation of the kinetic energy, indicating that at higher flow rates, the bulk of the liquid in the bath had received less energy from the incoming gas. Photographs of the gas/liquid dispersions were taken on either side of the transition flow rate mentioned above; the photographs at the flow rate smaller than the critical flow rate showed the presence of distinct bubbles, which did not interfere with each other. At a flow rate higher than the critical flow rate, the photographs indicated the presence of bubbles which coalesced continuously, and the gas channelled its way to the surface. This phenomenon was ascribed to be the cause of reduced efficiency of energy transfer.

Recently, Mazumdar et al. [27] employed a physical model of oil and water to simulate slag and steel respectively. Measurements of the total specific kinetic energy of motion in the bath indicated that there was an energy decrease in the recirculating liquid which was enhanced further with increasing flow rate; they proposed three

modes of energy dissipation to account for the loss of energy in the bath: 'slag' droplet creation, 'slag' droplet suspension and 'slag/metal' interface distortion. First-principle calculations were made which indicated that the first two modes involved a small magnitude of energy dissipation. Only the third mode was large enough to account for the energy decrease in the bath but even then it was still only half the required value; hence other modes accounting for the energy decrease in the bath have to be found. The mechanism responsible for the energy decrease in the bath has to be identified, since it leads to a reduced efficiency of mixing of alloying elements and their dissolution as well as an inefficient homogenization of bath temperature.

2.2 Mass-Transfer Studies

The mass-transfer coefficient at slag-metal interfaces stirred by injected gas has been characterized by several investigators [28,29] in different physical models. The relationship between the mass-transfer parameter (mass-transfer coefficient $\times$ interfacial area), K , and flow rate, Q , has been determined to be $K \propto Q^n$. The value of the exponent, n , was found to increase abruptly at a critical flow rate. Hirasawa et al. [30] found that only at very high flow rates does the interfacial area play an important role; at other times, the dominant factor is the metal-side mass-transfer coefficient which, in turn, is governed by the velocity distribution in the spout region. Kim and Fruehan [31] studied liquid/liquid mass transfer with physical models involving oil and water. They found that the recirculation and the entraining depth of oil droplets in the liquid bath are the major parameters which increase mass transfer. This emphasizes again the need to understand the spout region and also to characterize the velocity distribution at the interface accurately.

2.3 Mixing-Time Studies

The mixing time of refining vessels stirred by gas injection, at various gas flow rates, was studied by Asai et al. [32]. They observed that the mixing time decreased with flow rate according to the relationship: $\tau \propto \epsilon^{-0.68}$, up to a critical mixing power density beyond which the relationship changed to: $\tau \propto \epsilon^{-0.32}$, which indicated that the mixing in the bath had become less efficient. They also found that as the bath height decreased, the critical mixing power density at which the mixing became less efficient, decreased; thus at the same input power density the mixing process was more efficient in vessels with greater bath heights.

Mazumdar and Guthrie [33] measured the mixing time in a water model of a gas-stirred ladle. They observed that the mixing time decreased with gas flow rate as: $\tau \propto \epsilon^{-0.48}$, up to a critical gas flow rate; beyond which the dependence of the mixing time on input power density changed to: $\tau \propto \epsilon^{-0.34}$. These observations are similar to those reported by Asai et al. [32], confirming that beyond a critical flow rate the efficiency of mixing in the bath decreased.

Chapter 3

Objectives of the Present Work

The literature review has indicated that little work has been done to characterize quantitatively the spout formed at the free surface of a gas-stirred water bath. Hence the goal of the present work was to undertake this task experimentally, and then to incorporate the results into a mathematical model, to predict the velocity, turbulent kinetic energy and viscosity distributions in the water bath, and to assess the effect of the spout on metallurgical processes.

Consequently the objectives of this investigation were as follows:

1. To determine time-averaged spout dimensions photographically, at different flow rates.
2. To characterize the dynamics of the spout by high-speed photography.
3. To determine the structural development of the air-water spout, by making detailed measurements of the local gas-phase properties-void fraction and bubble frequency-with the aid of a computer-aided electroresistivity sensor, developed by Castillejos and Brimacombe [1,2,3].
4. To develop an empirical correlation to represent the gas void fraction distribution in the spout.
5. To incorporate the empirical correlation for the spout, in a mathematical model of a gas-stirred water bath, to make model predictions regarding the velocity,

kinetic energy and viscosity distribution in the bath.

6. To compare the predictions of the present model with the flat surface model, and with measurements.
7. To relate the findings of this study to experimental observations of some previous investigators and to industrial ladle metallurgy practices.

Chapter 4

Experimental

4.1 Injection System

The apparatus employed in the study of air-water spouts is illustrated schematically in Fig. 4.1. The equipment consisted of a ladle-shaped vessel, which was a 1/6th-scale model of a 150-tonne ladle. The bath was de-ionized water which was slightly acidified to facilitate the electroresistivity probe measurements. An air delivery system was installed to supply air to the nozzle.

The vessel was fabricated from plexiglas plate and had an internal diameter of 500 mm with a height of 900 mm. A baffle, consisting of a plexiglas ring with an external diameter of 496 mm and an internal diameter of 300 mm, was mounted ~ 5 mm above the static level of the bath. This decreased the extent of periodic swaying of the plume and hence of the spout, relative to the vessel axis, and thus permitted reproducible measurements. The internal diameter of the baffle did not interfere with the spout, even at the maximum flow rate investigated. This was confirmed by raising the baffle farther from the surface and repeating measurements with the electroresistivity probe; the same results were obtained.

The nozzle employed had a straight bore and was located at the centre of, and flush with, the bottom of the vessel. The lower end of the nozzle was connected to the air supply through a needle valve to control flow. Constant flow conditions were achieved by means of a high pressure drop across the valve and a small subnozzle volume. The air

flow rate was monitored using rotameters in which the pressure was held constant at 62 kPag (9 psig) in all the experiments. The water and air used were filtered to maintain a clean bath which prevented measurement problems arising from electrochemical effects and changes in fluid resistivity.

4.2 Electroresistivity Probe and Data Acquisition

The measuring system was based on a double-contact electroresistivity probe coupled to a microcomputer through a conditioning circuit and a counter-timer parallel input/output interface [1,2,3]. To minimize interference with the flow, the probe diameter was kept small at 3.5 mm. Structural rigidity, to withstand vibrations and buffeting of the turbulent flow, was imparted to the probe by bracing it with four symmetrically positioned wires. Details of the probe design have been given by Castillejos and Brimacombe [1,2,3].

The probe was located accurately at different positions in the spout with the aid of a two-dimensional traversing device, again described in earlier papers [1,2,3]. Measurements of the radial profiles of gas fraction and bubble frequency, as well as of axial profiles of centreline velocity, were performed from 40 mm below the static level of the bath (no injection) to the boundary of the spout at axial intervals of 10 mm; in the radial direction, measurements were made every 5 mm.

The electronic circuit and software were developed specially to perform signal analysis in real time. The analysis, based on pattern recognition, allows the simultaneous acquisition of the data required to calculate the local spout properties and ensures, to a large degree, that the velocities measured are uniquely related to the transit of bubbles moving vertically, and undisturbed, from the lower to the upper electrode.

Some uncertainty remains owing to the impossibility of accurately determining the

direction of movement of a bubble with respect to the probe using a double-contact sensor [1,2,3]. This problem manifested itself by the presence of a small number of unreasonably high velocities in the bubble velocity spectrum. A rational method for neglecting the large "velocities" was found by examining their associated pierced lengths. Visual observation of the bubbles from films recorded with a high-speed camera permitted the determination of reasonable upper limits for the pierced length. In the spout region the maximum acceptable pierced length was 70 mm. This limit was fixed in the treatment of all the data collected. It was seen that the large "velocities" measured were, in general, related to "bubbles" with pierced lengths larger than those observed; consequently their probability of occurrence was very small and it was justifiable to neglect these velocities without unduly introducing bias in the data treatment.

After neglecting these unreasonable values, a very few "velocity" values still remained that were markedly different from the bulk of the data. Johnson and Leone [34] and Barnett and Lewis [35] have discussed a number of tests of significance to decide whether isolated observations, called outliers, can be regarded as being too doubtful to be accepted as part of a sample. In this work the Dixon Statistic [35] for normal samples was applied,

$$r_p = \frac{U_{b,n} - U_{b,n-2}}{U_{b,n} - U_{b,3}} \quad (4.1)$$

The observation was neglected, that is removed from the sample, if the test criterion was significant. In this study, from the table of critical values for testing outliers [35], the test was taken to be significant to 95 percent if the value for r_p exceeded 0.30. As Johnson and Leone [34] have recommended, the testing procedure was applied sequentially removing one outlier at a time until no further significant values of r_p resulted.

A complication that arose in this study was that owing to periodic motion of the

spout, the electroresistivity probe could be immersed, at a given instant in the gas-liquid zone or be in contact with gas only. This situation, which was not encountered in the earlier study [1,2,3] on the jet plume, necessitated a modification to the software such that the timer capacity was increased beyond the existing value of ~ 65.5 ms so that the timer could record larger time intervals. The periodic immersion of the probe in the oscillating spout also made it difficult to perform mass balances on the air as done in the earlier study [1,2,3], for the purpose of testing the consistency and accuracy of the measurements.

4.3 Photographic Observations

High-speed photographs of the spout were taken with a Hycam K2054E camera at 500 frames per second and a wide-angle lens with a focal length of 26 mm. Black and white film having 400ASA was used. The vessel was lighted from the back as well as from the sides through translucent paper which acted as a diffuser.

The time-exposure photographs of the spout, under the conditions investigated, were taken with a Yashica camera, with an f stop of 11, and a shutter speed of 1 s. Black and white film having 32ASA was used in conjunction with a lens of focal length 50 mm.

Photographs of the entire dispersion, that is the plume and the spout, were taken with a motor driven Contax camera, at the rate of ~ 5 pictures per second, with an f stop of 8, and a shutter speed of 1/500th of a second. Black and white film having 125ASA was selected together with a lens of focal length 50 mm.

4.4 Laser Doppler Velocimetry

The laser-doppler velocimeter(LDV), used to measure bath velocities, consisted of a 15mW Spectra-Physics Helium-Neon laser connected to optics and data-collection electronic equipment manufactured by TSI Inc. The detailed operation of the LDV has been outlined elsewhere [36,37]. The velocity of the water in the vessel was measured within a region 1.8 cm long, defined by the intersection of the two laser beams. The value recorded as the water velocity at a particular point in the vessel was an average of 512 separate velocity readings taken from anywhere within this 1.8 cm measuring length. The LDV was capable of measuring one component of the fluid velocity at a time, either horizontal or vertical, but the orientation of the refracting optics could be altered to allow the measurement of the other component later; care was taken to obtain steady-state conditions in the bath each time measurements were made.

The entire laser-doppler apparatus was mounted on a computer-controlled traversing table, which was constructed so that it could position the measuring volume of the LDV at any location in the vessel, with an error of less than 1.0 mm. The apparatus was coupled to an Apple IIe microcomputer for data collection.

4.5 Experimental Conditions Investigated

The experimental variable of main interest was the gas flow rate because it determines the extent of buoyancy-induced flow, which is an important parameter affecting the spout. Measurements were carried out at four flow rates: 371, 876, 1257 and 1630 $\text{Ncm}^3\text{s}^{-1}$. The nozzle diameter was fixed at 6.35 mm; the static level of the bath was maintained at 400 mm. The experimental conditions under which measurements were carried out are shown in Table 4.1; photographs of the gas/liquid dispersion were also taken at flow rates higher than those indicated in Table 4.1.

Table 4.1: Experimental conditions of the study.

Orifice diameter d_o (mm)	Bath depth h_b (mm)	Gas flow rate Q (Ncm ³ s ⁻¹)			
6.35	400	371	876	1257	1630

Figure 4.1: Schematic diagram of experimental facility.

Chapter 5

Observations and Experimental Correlation

5.1 High Speed Cinematographic Observation of the Spout

Typical sequences of high-speed motion pictures of a spout taken at 500 frames per second are shown in Figs. 5.2 and 5.3. These reveal that the spout under study moves outward periodically from the axis of the jet toward the walls of the vessel and then back toward the centre. The time taken by the spout to complete one such cycle is ~ 270 to 540 ms. In Fig. 5.2 it can be observed that the height of the spout in the cycle remains approximately constant. However in Fig. 5.3, while moving in a similar manner radially, the height of the spout drops then rises back to its original location half way through the cycle, that is near the baffle. Through the latter half of the cycle, the spout height sometimes remains constant or drops in which case it regains its original position at the vessel axis. The frequency of horizontal oscillation of the spout is ~ 2 to 4 s^{-1} and that of vertical oscillation is ~ 3 to 4 s^{-1} . Owing to the oscillatory nature of the spout, a gas fraction value of unity can never be obtained at its boundary as will be discussed in the following section.

5.2 Boundary of the Spout

In order to characterize the spout on a steady-state basis, for subsequent incorporation into flow model calculations, it is useful first to determine the time-averaged spout boundary. This can be obtained by taking time-exposure photographs of the spout, as

shown in Fig. 5.4. From these the height of the spout at different radial positions was obtained by digitization of the observed spout surface. From the preceding section, the boundary of the spout thus represents the surface within which the spout spends a major fraction of its total time. When comparisons are made between the boundary obtained from the time-exposure photographs and from gas-fraction measurements, as will be discussed in the following section, it is seen that the boundary of the spout corresponded to gas-fraction values within a fixed range, for all the flow rates investigated. The value of gas fraction at the steady-state boundary, was less than unity as expected, due to the oscillatory nature of the spout.

5.3 Gas-Fraction Profiles

Typical results of the radial time-averaged gas-fraction distribution are plotted in Figs. 5.5 to 5.7. The profiles in the spout are seen to be the reverse of those found in the plume [1,2,3] in that the minimum gas fraction is found at the axis; in the plume the maximum gas fraction is found at the axis. The gas fraction in the spout not only increases radially but also with height above the nozzle. This is an expected result since at the boundary of the spout, a gas fraction value of unity should be approached.

Figures 5.8 to 5.10 show typical maps of the gas fraction in a vertical plane passing through the centreline of the spout. The maps consist of iso-gas-fraction curves expressed as a percentage. These reveal that, as expected, for an axisymmetrical air jet issuing vertically upward in water, the gas-liquid spout has the form of a dome. Comparison of the gas-fraction profiles with the time exposure photographs, Fig. 5.4, reveals that the boundary of the spout always corresponds to a gas fraction in the range of 0.82 to 0.86 for the three flow rates studied. In view of the oscillatory motion of the spout, Figs. 5.2 and 5.3, this finding is not surprising. Quantitative determination of

the spout shape(radius vs height) will be described in the following section.

5.3.1 General Equation for Gas Fraction in the Spout

The gas-fraction profiles at different levels in the spout are seen to be similar when they are normalized by plotting the ratio of the minimum value of the gas fraction at a particular level, α_{min} , to the local gas fraction, against the reduced radial distance $r(g/Q_o^2)^{1/5}$. The factor $(g/Q_o^2)^{1/5}$ has dimensions of $(length)^{-1}$. The normalized gas-fraction profiles at different levels in the spout for the three flow rates are shown in Figs. 5.11 to 5.13 respectively. Under all the conditions studied, the experimental reduced gas-fraction distributions could be approximated satisfactorily by the following equation, which was obtained by carrying out regression analysis

$$\frac{\alpha_{min}}{\alpha} = A \exp(n[r(\frac{g}{Q_o^2})^{1/5}]) \quad (5.2)$$

where A and n are constants for a particular level in the spout and a particular gas flow rate. The gas-fraction values predicted by the above experimental correlation were carefully checked against those measured at $1630 \text{ Ncm}^3\text{s}^{-1}$ and a good agreement was found. Hence Eq.(5.2) represents correctly the gas-fraction distribution even at high flow rates. Equation (5.2) is in good agreement with the experimental data for virtually the entire spout, except in the region close to the axis, which is referred to as the "core region", where the gas fraction maintains a constant minimum value, α_{min} , at a particular level in the spout. The radius of the spout at any level can be determined from Eq.(5.2) by substituting the limiting value for gas fraction at the boundary of the spout, 0.86.

In order to utilize Eq.(5.2), the parameters A and n have to be determined as a function of position and flow rate. Thus the following expressions for the variation of

the constants with axial position and modified Froude number were obtained

$$A = [K_1(x + K_2)^2 + 1]^{-1} \quad (5.3)$$

where $x = z(g/Q_o^2)^{1/5}$, the non-dimensionalized axial distance

$$K_1 = -0.10(Fr)^{0.56} \quad (5.4)$$

$$K_2 = -17.94(Fr)^{-0.20} \quad (5.5)$$

$$n = 0.62x - 10.73(Fr)^{-0.17} \quad (5.6)$$

where $Fr = \text{modified Froude number} = \frac{Q_o^2 \rho_G}{g d_o^5 (\rho_L - \rho_G)}$. The dependence of A and n on reduced axial distance from the nozzle is shown in Figs. 5.14 and 5.15 respectively for the spout.

The dimensions of the "core region" can be represented in terms of a radius, r_o . The dependence of r_o and axial gas fraction, α_{min} , on height above the nozzle and gas flow rate, can be expressed in terms of the following equations

$$r_o \left(\frac{g}{Q_o^2} \right)^{1/5} = -0.48x + 8.3(Fr)^{-0.18} \quad (5.7)$$

where

$r_o \left(\frac{g}{Q_o^2} \right)^{1/5} =$ the non-dimensionalized core radius

$x = z(g/Q_o^2)^{1/5}$ is the non-dimensionalized axial distance

$Fr =$ modified Froude number

$$\alpha_{min} = K_3 + K_4x + K_5/x \quad (5.8)$$

where $\alpha_{min} =$ axial gas fraction expressed as a percentage.

$$K_3 = -2.32(10^4)(Fr)^{-0.71} \quad (5.9)$$

$$K_4 = 7.10(10^2)(Fr)^{-0.47} \quad (5.10)$$

$$K_5 = 1.90(10^5)(Fr)^{-0.95} \quad (5.11)$$

The change of axial gas fraction in the spout with distance from the nozzle is shown in Fig. 5.16. The dependence of r_o and α_{min} on reduced axial distance from the nozzle is shown in Figs. 5.17 and 5.18 respectively. Normalized radial gas-fraction profiles at a constant level in the spout, for different flow rates, are shown in Figs. 5.19 to 5.24.

5.4 Bubble-Frequency Profiles

Radial profiles of bubble frequency in the spout, at different axial distance from the nozzle, are shown in Figs. 5.25 to 5.27. The bubble frequency distribution exhibits a bell shape, with the bubble frequency decreasing with increasing height. At the static level of the bath, for all the cases examined, the bubble frequency was only weakly dependent on radial position, as shown in Fig. 5.28.

At the boundary of the spout, the measured frequency lies in the range of 2 to 4 s⁻¹, which is in agreement with the frequency of horizontal and vertical oscillations of the spout observed in the high-speed tracings, illustrated in Figs. 5.2 and 5.3 respectively. Figures 5.29 to 5.31 show typical maps of the bubble frequency in a vertical plane passing through the centreline of the spout for the three gas flow rates. These maps indicate that even at the boundary of the spout, the frequency drops only to 2 to 4 s⁻¹; due to the oscillatory nature of the spout, the frequency at the spout boundary does not drop to zero, just as the gas fraction rarely attains a value of unity.

5.5 Profiles of Axial Velocity

Profiles of axial velocity in the spout are shown in Fig. 5.32. Thus the velocity decreases with increasing axial distance in the spout in contrast to the plume where, beyond a certain height above the nozzle, the axial velocity is practically uniform. This is due to the fact that in the plume the buoyancy force accelerating the fluid is about balanced by momentum diffusion which tends to slow the fluid down resulting in a close to uniform plume velocity. At the surface, on the other hand, the kinetic energy of the fluid is transformed into potential energy thereby raising the level of the fluid, reducing the axial velocity and forming the spout.

Figure 5.2: Tracings of high-speed photographs of the spout, with its height remaining constant during the cycle.

Figure 5.3: Tracings of high-speed photographs of the spout, with its height varying during the cycle.

(a)

50 mm

(b)

50 mm

Figure 5.4: Time-exposure photographs of the spout (a) $Q = 876$ and (b) $Q = 1257$ $\text{Ncm}^3\text{s}^{-1}$.

Figure 5.5: Radial gas-fraction profiles at different axial distances from the nozzle in the spout, for $Q = 371 \text{ Ncm}^3 \text{ s}^{-1}$.

Figure 5.6: Radial gas-fraction profiles at different axial distances from the nozzle in the spout, for $Q = 876 \text{ Ncm}^3 \text{ s}^{-1}$.

Figure 5.7: Radial gas-fraction profiles at different axial distances from the nozzle in the spout, for $Q = 1257 \text{ Ncm}^3 \text{ s}^{-1}$.

Figure 5.8: Contours of the gas void fraction in a vertical plane passing through the centreline of the spout, for $Q = 371 \text{ Ncm}^3 \text{ s}^{-1}$.

Figure 5.9: Contours of the gas void fraction in a vertical plane passing through the centreline of the spout, for $Q = 876 \text{ Ncm}^3 \text{ s}^{-1}$.

Figure 5.10: Contours of the gas void fraction in a vertical plane passing through the centreline of the spout, for $Q = 1257 \text{ Ncm}^3 \text{ s}^{-1}$.

Figure 5.11: Normalized radial gas-fraction profiles in the spout at different axial distances from the nozzle, for $Q = 371 \text{ Ncm}^3 \text{ s}^{-1}$.

Figure 5.12: Normalized radial gas-fraction profiles in the spout at different axial distances from the nozzle, for $Q = 876 \text{ Ncm}^3 \text{ s}^{-1}$.

Figure 5.13: Normalized radial gas-fraction profiles in the spout at different axial distances from the nozzle, for $Q = 1257 \text{ N cm}^3 \text{ s}^{-1}$.

Figure 5.14: Variation of the parameter "A" in Eq.(5.2) with reduced axial distance from the nozzle, for the spout.

Figure 5.15: Variation of the parameter “n” in Eq.(5.2) with reduced axial distance from the nozzle, for the spout.

Figure 5.16: Variation of axial gas fraction with distance from the nozzle.

Figure 5.17: Variation of the core radius, r_o , with reduced axial distance from the nozzle in the spout.

Figure 5.18: Variation of the gas fraction at the spout centreline, α_{min} , with reduced axial distance from the nozzle in the spout.

Figure 5.19: Normalized radial gas-fraction profiles in the spout for different flow rates at 400 mm from the orifice.

Figure 5.20: Normalized radial gas-fraction profiles in the spout for different flow rates at 410 mm from the orifice.

Figure 5.21: Normalized radial gas-fraction profiles in the spout for different flow rates at 420 mm from the orifice.

Figure 5.22: Normalized radial gas-fraction profiles in the spout for different flow rates at 430 mm from the orifice.

Figure 5.23: Normalized radial gas-fraction profiles in the spout for different flow rates at 440 mm from the orifice.

Figure 5.24: Normalized radial gas-fraction profiles in the spout for different flow rates at 450 mm from the orifice.

Figure 5.25: Radial bubble-frequency profiles in the spout at different axial distances from the nozzle, for $Q = 371 \text{ Ncm}^3 \text{ s}^{-1}$.

Figure 5.26: Radial bubble-frequency profiles in the spout at different axial distances from the nozzle, for $Q = 876 \text{ N cm}^3 \text{ s}^{-1}$.

Figure 5.27: Radial bubble-frequency profiles in the spout at different axial distances from the nozzle, for $Q = 1257 \text{ Ncm}^3 \text{ s}^{-1}$.

Figure 5.28: Radial bubble-frequency profiles at the static level of the bath.

Figure 5.29: Contours of the bubble frequency in a vertical plane passing through the centreline of the spout, for $Q = 371 \text{ Ncm}^3\text{s}^{-1}$.

Figure 5.30: Contours of the bubble frequency in a vertical plane passing through the centreline of the spout, for $Q = 876 \text{ N cm}^3 \text{ s}^{-1}$.

Figure 5.31: Contours of the bubble frequency in a vertical plane passing through the centreline of the spout, for $Q = 1257 \text{ Ncm}^3 \text{ s}^{-1}$.

Figure 5.32: Profiles of axial velocity in the spout for the three gas flow rates.

Chapter 6

Mathematical Model

6.1 Governing Equations

The turbulent recirculatory flow in a water bath, stirred with gas discharging from a bottom-centered nozzle, is assumed to be axisymmetric and buoyancy driven. The turbulence was simulated using the $k - \epsilon$ differential model. Two transport equations are written for the turbulent kinetic energy “ k ”, and the energy dissipation rate, “ ϵ ” [38]. The two-phase plume was represented by an experimental correlation for gas-fraction distribution developed by Castillejos and Brimacombe [1,2,3]. The spout at the free surface is represented by the experimental correlation developed in the present study. The spatially variable density of the jet in the model was written as [39]:

$$\rho = \alpha \rho_G + (1 - \alpha) \rho_L \quad (6.12)$$

where

- α = local time-averaged gas-fraction
- ρ_G = density of gas
- ρ_L = density of liquid
- ρ = spatially variable density

The following equations were written for the steady-state, axi-symmetrical flow in cylindrical coordinates:

Equation of continuity:

$$\frac{1}{r} \frac{\partial}{\partial r}(\rho r U_r) + \frac{\partial}{\partial z}(\rho U_z) = 0 \quad (6.13)$$

Equation of motion in axial direction:

$$\begin{aligned} \frac{1}{r} \frac{\partial}{\partial r}(\rho r U_r U_z) + \frac{\partial}{\partial z}(\rho U_z^2) = & -\frac{\partial P}{\partial z} + \frac{1}{r} \frac{\partial}{\partial r}[r \mu_{eff} \frac{\partial U_z}{\partial r}] \\ & + 2 \frac{\partial}{\partial z}[\mu_{eff} \frac{\partial U_z}{\partial z}] + \frac{1}{r} \frac{\partial}{\partial r}[\mu_{eff} r \frac{\partial U_r}{\partial z}] + \rho_L g \alpha \end{aligned} \quad (6.14)$$

Equation of motion in radial direction:

$$\begin{aligned} \frac{1}{r} \frac{\partial}{\partial r}(\rho r U_r^2) + \frac{\partial}{\partial z}(\rho U_r U_z) = & -\frac{\partial P}{\partial r} + \frac{2}{r} \frac{\partial}{\partial r}[r \mu_{eff} \frac{\partial U_r}{\partial r}] \\ & + \frac{\partial}{\partial z}[\mu_{eff} \frac{\partial U_r}{\partial z}] + \frac{\partial}{\partial z}[\mu_{eff} \frac{\partial U_z}{\partial r}] - \frac{U_r \mu_{eff}}{r^2} \end{aligned} \quad (6.15)$$

Equation for turbulent kinetic energy:

$$\begin{aligned} \frac{1}{r} \frac{\partial}{\partial r}(\rho r U_r k) + \frac{\partial}{\partial z}(\rho U_z k) = & \frac{1}{r} \frac{\partial}{\partial r}[r \frac{\mu_{eff}}{\sigma_k} \frac{\partial k}{\partial r}] \\ & + \frac{\partial}{\partial z}[\frac{\mu_{eff}}{\sigma_k} \frac{\partial k}{\partial z}] + G - \rho \epsilon \end{aligned} \quad (6.16)$$

where σ_k is the effective Prandtl number for k and G is the generation of turbulent kinetic energy expressed as follows:

$$G = \mu_t [2((\frac{\partial U_r}{\partial r})^2 + (\frac{\partial U_z}{\partial z})^2 + (\frac{U_r}{r})^2) + (\frac{\partial U_r}{\partial z} + \frac{\partial U_z}{\partial r})^2] \quad (6.17)$$

Equation for dissipation rate of turbulent kinetic energy:

$$\begin{aligned} \frac{1}{r} \frac{\partial}{\partial r} (\rho r U_r \epsilon) + \frac{\partial}{\partial z} (\rho U_z \epsilon) &= \frac{1}{r} \frac{\partial}{\partial r} \left[r \frac{\mu_{eff}}{\sigma_\epsilon} \frac{\partial \epsilon}{\partial r} \right] \\ &+ \frac{\partial}{\partial z} \left[\frac{\mu_{eff}}{\sigma_\epsilon} \frac{\partial \epsilon}{\partial z} \right] + \frac{C_1 \epsilon G}{k} - \frac{C_2 \rho \epsilon^2}{k} \end{aligned} \quad (6.18)$$

where σ_ϵ is the effective Prandtl number for ϵ and C_1 and C_2 are constants.

The effective viscosity, μ_{eff} is given as;

$$\mu_{eff} = \mu + \mu_t \quad (6.19)$$

$$\mu_t = \frac{C_d \rho k^2}{\epsilon} \quad (6.20)$$

After Launder and Spalding [40], the values given in Table 6.2 were assigned to the above-mentioned constants in the model; a variation in the values of these constants was explored and no significant effect on the results was observed.

Table 6.2: Values of constants in $k - \epsilon$ Turbulence Model.

C_1	C_2	C_d	σ_k	σ_ϵ
1.43	1.92	0.09	1.00	1.30

6.2 Two-Phase Plume

Since the flow is buoyancy dominated, it is important to have an accurate representation of the two-phase plume. In the present model, an experimental correlation to represent the gas volume fraction in the two-phase plume developed by Castillejos and Brimacombe [1,2,3] was used. The correlation is as follows:

$$\frac{\alpha}{\alpha_{maz}} = \exp[-0.7(\frac{r}{r_{\alpha maz/2}})^{2.4}] \quad (6.21)$$

$$r_{\alpha maz/2}(\frac{g}{Q_o^2})^{1/5} = 0.243[(\frac{gd_o^5(\rho_L - \rho_G)}{Q_o^2\rho_G})^{0.184}(\frac{z}{d_o})^{0.48}] \quad (6.22)$$

For $N \geq 4$:

$$\alpha_{maz} = 293.77[(\frac{gd_o^5(\rho_L - \rho_G)}{Q_o^2\rho_G})^{0.269}(\frac{z}{d_o})^{0.993}]^{-1} \quad (6.23)$$

For $N < 4$:

$$\alpha_{maz} = 100[(\frac{gd_o^5(\rho_L - \rho_G)}{Q_o^2\rho_G})^{0.269}(\frac{z}{d_o})^{0.993}]^{-0.22} \quad (6.24)$$

N is defined as:

$$N = (\frac{gd_o^5(\rho_L - \rho_G)}{Q_o^2\rho_G})^{0.269}(\frac{z}{d_o})^{0.993} \quad (6.25)$$

6.3 Two-Phase Spout

The correct representation of the spout formed at the free surface is essential for accurate prediction of fluid flow in a gas-stirred vessel, since very high velocities and intense turbulence have been observed near the free surface in the bath. The gas volume fraction in the spout at the free surface is represented by an experimental correlation, involving different flow rates, as mentioned in the previous chapter. The pressure condition at the free surface was simulated by incorporating a surface routine [41,42] into the computer program.

6.4 Boundary Conditions

As shown in Fig. 6.33, the boundary conditions for the governing equations are:

At the axis:

$$U_r = 0 \quad \frac{\partial U_z}{\partial r} = 0 \quad \frac{\partial k}{\partial r} = 0 \quad \frac{\partial \epsilon}{\partial r} = 0$$

At the spout boundary:

$$P = P_{atm}$$

Outside the spout boundary:

$$U_z = 0 \quad U_r = 0 \quad k = 0 \quad \epsilon = 0$$

At the walls:

$$U_r = 0 \quad U_z = 0$$

In order to characterize the steep variation in flow properties close to the solid walls, wall functions [43] were used for momentum(U_r, U_z) and scalar(k, ϵ) transport equations. A one-dimensional Couette flow was assumed near the boundary. Thus the velocity profile in the turbulent region is given by the universal log law. It was also assumed that equilibrium conditions exist in the near-wall region, so that the turbulent kinetic energy generation is equal to its dissipation.

6.5 Numerical Calculations

The governing equations were discretized using a control volume method and a hybrid differentiation scheme [44]. The system of equations was solved by the SIMPLE algorithm of Patankar and Spalding [45] incorporated in the TEACH-2E computer code [46]; a grid of 34 x 17 was used. In the axial direction a cell size of 5 mm was maintained in the spout and in the bulk liquid a cell size in the range of 5 to 15 mm was used; whereas in the radial direction the cell size varied in the range of 5 to 20 mm. A convergence criterion was set so that the sum, over the entire grid, of the absolute residuals for each of the mass and momentum discretized equations, was lower than 10^{-5} . The non-dimensionalization factors were the mass flow rate and the momentum of the gas at the inlet, for mass and momentum respectively.

Figure 6.33: Schematic of the gas-stirred bath illustrating the boundary conditions used in the mathematical model.

Chapter 7

Model Predictions and Discussion

The predicted velocity profiles, for the three different flow rates investigated are presented in Figs. 7.34, 7.35 and 7.36 respectively. These show that with increasing flow rate, the size of the spout and the magnitude of the velocity in the spout increases.

7.1 Comparison between Model Predictions and Measurements

Figures 7.37 to 7.39 show for the three flow rates investigated, the near-wall velocities, as measured by laser doppler velocimetry, as predicted by the flat surface model (no spout) and the present model (including spout). Similar values for velocities near the walls of the vessel have been reported by Tse-Chiang et al. [18]. The agreement between the present model and measurements is good, whereas the flat-surface model overestimates the velocities. This is understandable because the flat-surface, free-slip model does not allow for the dissipation of kinetic energy in the free surface region. Thus the kinetic energy is over-estimated at the near-wall region.

Figures 7.40 to 7.42 show a comparison between axial velocities measured by the electroresistivity probe and predicted by the two models. The flat-surface model cannot predict velocities beyond a certain height in the bath, since it does not consider the formation of the spout at the free surface. Discrepancies between the measurements and the predictions of both models can be observed. The differences arise because the electroresistivity probe measures the velocity of the gas bubbles moving vertically upwards, whereas the model assumes a homogenous, single-phase fluid with a spatially

variable density. The model does not account for the relative velocity of the gas with respect to the liquid. Thus, the largest discrepancy is observed near the nozzle, caused probably by the assumption of zero gas jet momentum at the orifice. This assumption affects only a small region near the nozzle; since the kinetic energy of the gas is important only close to the nozzle. As the distance above the nozzle increases, the discrepancy is almost constant, since the axial velocity remains essentially unchanged in this region. In the near-surface region, the discrepancy between the present model and the measurements decreases since the gas bubbles undergo a deceleration in the spout. The flat-surface model underestimates the velocities in the near-surface region because the assumption of a non-slip wall requires zero normal velocity so that the fluid is forced to turn and flow radially outward toward the vessel wall.

The present model predicts the velocity to be maximum at the axis and to decrease with radial distance, in the near-surface region. A similar radial distribution for the near-surface velocities has been measured by Tse-Chiang et al. [18] at different flow rates. The flat-surface model, on the other hand, predicts an almost uniform near-surface velocity along the radius. The flat-surface model thus underestimates the near-surface velocity at the axis and overestimates the velocity at the wall. The near-surface velocities predicted by the flat-surface model are close to horizontal, whereas those predicted by the present model have a comparatively large axial component and they are seen to deflect downwards, while moving radially outwards, at an angle of thirty to fifty degrees depending on the vertical location of the velocity vector.

The incorrect velocity prediction by the flat surface model can have several consequences:

1. The velocity distribution obtained from the model is necessary to determine the

distribution of temperature and chemical composition in the bath, and to predict the times required for temperature and composition homogenization. Hence, an incorrect velocity distribution will lead to inaccurate predictions for both.

2. The overestimation of velocities in the region near the wall, especially close to the free surface, will result in predictions of locally high turbulence. Consequently the excessive erosion of refractories in that region may be attributed wrongly to high turbulence, rather than to the presence of a highly corrosive slag layer adjacent to the refractories.
3. The near-surface velocity distribution is of foremost importance in controlling the rate kinetics of various reactions like desulphurization and deoxidation taking place at the slag-metal interface, since the velocity of the liquid below the surface determines the rate at which the reactants will be transported toward the interface. Therefore a flat surface model can not predict correctly the rate of these reactions. This is because in these systems the reaction is usually controlled by the mass-transfer rate.

7.2 Profiles of Turbulent Kinetic Energy and Viscosity Distribution

The maps of predicted turbulent kinetic energy in the plume (from the bottom of the vessel to the static level of the bath) are shown in Figs. 7.43 to 7.45. These contours show maximum turbulent kinetic energy at a small distance from the axis, where the velocity gradients are very large. The turbulent kinetic energy is seen to be comparatively high in the near-surface region close to the axis, and low in the near-wall region. For the spout, the maps of turbulent kinetic energy are shown in Figs. 7.46 to 7.48. These profiles indicate that the region of maximum turbulent kinetic energy is located close to the boundary of the spout. Close to the axis of the spout the kinetic

energy is an order of magnitude smaller than that close to the boundary; hence it has not been shown on the contours.

The profiles of effective viscosity, non-dimensionalized with molecular viscosity, in the plume are shown in Figs. 7.49 to 7.51. These show that the effective viscosity increases with height above the nozzle, and attains a maximum value at the near-surface region (the static level of the bath) at a small distance from the axis. The effective viscosity decreases with radial position; and in the region close to the wall is an order of magnitude smaller than that at the axis. Profiles of effective viscosity, non-dimensionalized with molecular viscosity, in the spout are shown in Figs. 7.52 to 7.54. These profiles show that the region of maximum effective viscosity is located at the near-surface region (the static level of the bath), close to the axis. The effective viscosity decreases with height above the nozzle as well as with radial distance from the axis; hence the minimum viscosity was found close to the boundary of the spout. This result is expected since beyond the spout the turbulent kinetic energy must drop to zero, and hence the effective viscosity will approach the value of laminar viscosity at the boundary of the spout.

The present results have been compared with predictions by Woo et al. [47]. Good agreement was observed in the central region of the bath. Near the wall Woo et al. [47] have predicted higher values of turbulent kinetic energy and effective viscosity than the present results. This is due to their assumption of a flat, free-slip top surface.

7.3 Turbulent Kinetic Energy of the Spout

The turbulent kinetic energy of the spout is a key parameter and has important metallurgical consequences; as will be shown in the next chapter. In order to quantify the effect of the spout on the metallurgical processes occurring in a gas-stirred vessel, the

variation of total turbulent kinetic energy in the spout with flow rate was analysed. The total turbulent kinetic energy in the spout was obtained by summation of turbulent kinetic energies at all nodal points within the flow domain. Its variation as a function of gas flow rate is illustrated in Fig. 7.55. It can be seen that the slope increases abruptly at a "critical" value of the gas flow rate, which is $1350 \text{ cm}^3\text{s}^{-1}$. Beyond the critical flow rate, a small increase in flow rate can lead to a large rise in the level of turbulence at the free surface. In a steel ladle this could accelerate reoxidation and moreover reduce the efficiency of temperature homogenization, dissolution and mixing of alloying additions as will be shown later in this chapter. Thus in order to operate a gas-stirring vessel in an optimum manner, the flow rate range of reduced efficiency of bath recirculation, associated with excessive turbulence at the free surface, must be identified and consequently avoided.

7.4 Potential Energy of the Spout

The spout is formed at the free surface due to the kinetic energy of the fluid present at the static level of the bath, which is transformed into the potential energy of the spout. Hence the maximum height of the spout, which is observed to be at the axis, can be determined as follows (the energy losses through diffusion are assumed to be negligible and a constant mixture density is considered):

$$h_{maz} = \frac{U_{az}^2}{2g} \quad (7.26)$$

where

h_{maz} = maximum height of the spout.

U_{az} = vertical velocity of the fluid at the static level
of the bath, along the axis.

g = acceleration due to gravity.

The h_{max} is an important parameter from a practical point of view, in ladle furnaces where electrode reheating is conducted. The value of h_{max} should be smaller than the height at which the electrode is placed above the bath in order to avoid short circuiting between the metal and the electrode.

The variation of $2gh_{max}$, obtained from photographs of the spout, with flow rate is shown in Fig. 7.56. The relationship obtained is similar to that of total turbulent kinetic energy in the spout. A comparison between Fig. 7.56 and Fig. 7.55 shows that the abrupt increase of $2gh_{max}$ and of the turbulent kinetic energy occur at approximately the same flow rates. Hence $2gh_{max}$ also can be considered as a measure of the level of turbulence at the surface, since its variation reveals two distinct flow rate ranges. As indicated before, if in a ladle process, reoxidation is to be minimized, the higher flow rate range must be avoided, to prevent excessive turbulence at the free surface.

Mazumdar et al. [27] measured the total specific kinetic energy of motion in the bath, and pointed out that there was an energy decrease in the recirculating liquid which was enhanced further with increasing flow rate. They attributed this energy decrease to 'slag/metal' interface distortion; but this mode of energy dissipation could account for only half the required value. Hence there exist other equally important reasons to explain the energy decrease in the bath.

7.5 Proposed Mechanism

The critical gas flow rate in the gas injection process determines the degree of partitioning of gas energy between the bulk liquid and the surface region. It has been observed in this work, that the rate of increase of the spout height increases with flow rate beyond a critical value. At the same time, in other studies, the efficiency of bath mixing has been found to decrease, at higher flow rates, as was mentioned in Chapter

2. These two phenomena are linked, and would certainly depend upon the manner in which the injected gas interacts with the liquid. Mechanisms have been proposed to explain these observed phenomena in a gas-stirred bath.

To explain the phenomena occurring on either side of the "transition" (the critical flow rate), photographs of the gas/liquid dispersions were taken at an interval of one second as shown in Figs. 7.57 and 7.58 respectively. The series of photographs, in both cases, reveal distinct differences between the two flow rate ranges. At the smaller flow rate, Fig. 7.57, the large gas bubbles seen at the bottom of the bath, break up into distinct small bubbles, not far away from the orifice. They are able to interact with the liquid during a large fraction of their total travel time from the orifice to the surface, thus transmitting a major fraction of their total energy to the bath. This leads to efficient mixing. (as well as temperature homogenization, dissolution and dispersion of alloying elements in the bulk of the liquid). In contrast, at the higher flow rate, seen in Fig. 7.58, the bubble breakup does not occur until close to the bath surface. The gas pockets continue to move upwards from the orifice toward the surface, transmitting only a fraction of their energy to the liquid. A major fraction of the total energy content is released close to the surface, resulting in excessive turbulence and a prominent spout at the bath surface. It has been pointed out by Sahai and Guthrie [16] that a dynamic interaction between the gas and the liquid and a constant exchange of liquid between the plume and the single-phase liquid leads to the transfer of energy to the bulk. Such an exchange of liquid is more efficient in the case of the lower flow rates, since the bubbles break up relatively close to the nozzle and hence a greater volume of the bulk liquid is able to penetrate the gas column due to a comparatively earlier initiation of gas/liquid interaction. On the other hand at higher flow rates the intermittent formation of a chain of gas envelopes hinders the frequent penetration of the bulk liquid into the plume.

Channeling of gas could also be responsible for the different phenomena occurring on either side of the critical flow rate. Beyond the critical value, the large gas envelopes are closely spaced so that intermittently, gas channeling or bypass may be possible. This reduces the extent of gas/liquid interaction, and hence reduces the transfer of energy from the gas to the liquid, resulting in a reduced efficiency of bath mixing. Thus the energy of the gas is released close to the surface leading to the formation of a prominent spout.

7.6 Experimental Evidence

7.6.1 Axial Bubble Velocity Variation

Axial bubble velocity measurements conducted by Castillejos and Brimacombe [2,3], shed light on the cause of the gas flow “transition”. At lower flow rates, the velocity is seen to increase up to a certain height above the nozzle, beyond which it begins to decrease. This is due to the fact that distinct bubbles are formed at the orifice and they accelerate in the wake of the preceeding bubbles. At higher flow rates the bubble velocity decreases continuously with distance from the orifice because the gas forms a continuous series of envelopes from the orifice. These lose their momentum and decelerate on their upward path toward the surface. The modified Froude number(Fr) at which the transition in axial bubble velocity variation was observed by Castillejos and Brimacombe [2,3] is the same as the critical Fr observed in the present study.

7.6.2 Axial Bubble Frequency Variation

The axial variation of local bubble frequency for different modified Froude numbers(Fr), as measured by Castillejos and Brimacombe [2,3], is shown in Fig. 7.59. This plot reveals that up to a particular value of Fr , the bubble frequency is always the least

at the smallest Fr at any height above the orifice. But at the higher Fr , this trend is maintained only up to a small distance from the orifice, beyond which, the trend reverses and the bubble frequency begins to decrease at a rapid rate and drops even below the value at the lower Fr . The Fr at which the transition in the variation of bubble frequency occurs, is the same as the critical Fr observed in the present study. These measurements directly support the assumption that at higher flow rates, the gas moves upward in the form of large gas envelopes which do not break into smaller size bubbles until close to the surface; whereas the higher frequency observed at lower flow rates imply that the gas is present in the form of a large number of small size bubbles.

7.6.3 Mixing Time Measurements

Measurements of mixing time in gas-stirred vessels were conducted by Asai et al. [32] as described in Chapter 2. They found that the mixing time decreases with the mixing power density, ϵ , according to the relationship: $\tau \propto \epsilon^{-0.68}$, up to a critical value of ϵ . Beyond this value the rate of drop of mixing time, decreases to $\tau \propto \epsilon^{-0.32}$, indicating that above the critical value of mixing power density, the efficiency of the process has been reduced. This can be attributed again to the large gas envelopes as observed in the present study at high gas flow rates. These gas envelopes do not break up into bubbles until close to the surface thus transmitting only a small fraction of their total energy to the bath. They also found a similar observation for the fluid velocity, which varied with ϵ as $v \propto \epsilon^{0.48}$ up to the critical value of ϵ , which was the same as that observed in the mixing time measurements. At higher values of ϵ the relationship changed to $v \propto \epsilon^{0.24}$.

Figure 7.34: Predicted velocity profile in the bath at $Q = 371 \text{ Ncm}^3 \text{ s}^{-1}$.

Figure 7.35: Predicted velocity profile in the bath at $Q = 876 \text{ Ncm}^3 \text{ s}^{-1}$.

Figure 7.36: Predicted velocity profile in the bath at $Q = 1257 \text{ Ncm}^3\text{s}^{-1}$.

Figure 7.37: Comparison between measurements and model predictions of near-wall bath velocity at $Q = 371 \text{ Ncm}^3 \text{ s}^{-1}$.

Figure 7.38: Comparison between measurements and model predictions of near-wall bath velocity at $Q = 876 \text{ Ncm}^3 \text{ s}^{-1}$.

Figure 7.39: Comparison between measurements and model predictions of near-wall bath velocity at $Q = 1257 \text{ Ncm}^3 \text{ s}^{-1}$.

Figure 7.40: Comparison between measurements and model predictions of axial jet velocity at $Q = 371 \text{ Ncm}^3\text{s}^{-1}$.

Figure 7.41: Comparison between measurements and model predictions of axial jet velocity at $Q = 876 \text{ Ncm}^3 \text{ s}^{-1}$.

Figure 7.42: Comparison between measurements and model predictions of axial jet velocity at $Q = 1257 \text{ Ncm}^3 \text{ s}^{-1}$.

Figure 7.43: Contours of predicted turbulent kinetic energy (Joules/kg) distribution in the plume, at $Q = 371 \text{ Ncm}^3 \text{ s}^{-1}$.

Figure 7.44: Contours of predicted turbulent kinetic energy (Joules/kg) distribution in the plume, at $Q = 876 \text{ Ncm}^3 \text{ s}^{-1}$.

Figure 7.45: Contours of predicted turbulent kinetic energy(Joules/kg) distribution in the plume, at $Q = 1257 \text{ Ncm}^3\text{s}^{-1}$.

Figure 7.46: Contours of predicted turbulent kinetic energy (Joules/kg) distribution in the spout, at $Q = 371 \text{ Ncm}^3 \text{ s}^{-1}$.

Figure 7.47: Contours of predicted turbulent kinetic energy(Joules/kg) distribution in the spout, at $Q = 876 \text{ Ncm}^3\text{s}^{-1}$.

Figure 7.48: Contours of predicted turbulent kinetic energy (Joules/kg) distribution in the spout, at $Q = 1257 \text{ Ncm}^3 \text{ s}^{-1}$.

Figure 7.49: Contours of predicted effective viscosity non-dimensionalized with molecular viscosity in the plume, at $Q = 371 \text{ Ncm}^3\text{s}^{-1}$.

Figure 7.50: Contours of predicted effective viscosity non-dimensionalized with molecular viscosity in the plume, at $Q = 876 \text{ N cm}^3 \text{ s}^{-1}$.

Figure 7.51: Contours of predicted effective viscosity non-dimensionalized with molecular viscosity in the plume, at $Q = 1257 \text{ Ncm}^3 \text{ s}^{-1}$.

Figure 7.52: Contours of predicted effective viscosity non-dimensionalized with molecular viscosity in the spout, at $Q = 371 \text{ Ncm}^3 \text{ s}^{-1}$.

Figure 7.53: Contours of predicted effective viscosity non-dimensionalized with molecular viscosity in the spout, at $Q = 876 \text{ Ncm}^3 \text{ s}^{-1}$.

Figure 7.54: Contours of predicted effective viscosity non-dimensionalized with molecular viscosity in the spout, at $Q = 1257 \text{ Ncm}^3 \text{ s}^{-1}$.

Figure 7.55: The variation of predicted total turbulent kinetic energy in the spout with flow rate.

Figure 7.56: The variation of measured $2gh_{\max}$ with flow rate.

Figure 7.57: Photographs of the gas/liquid dispersions at a flow rate less than the critical value.

Figure 7.58: Photographs of the gas/liquid dispersions at a flow rate higher than the critical value.

Figure 7.59: The variation of local bubble frequency at the axis with height above the nozzle at varying modified Froude number as measured by Castillejos and Brimacombe [2,3].

Chapter 8

Metallurgical Consequences

It has been established in the previous chapter that a critical flow rate exists which divides the gas-injection process into two distinct regimes. The practical consequences of this observation on the metallurgical processes taking place in a gas-stirring operation are important and are described in the following sections. The variables that influence the critical flow rate also have been examined.

8.1 Turbulence at the Surface

The turbulent kinetic energy in the spout is an important parameter, since it determines the extent of turbulence at the free surface, which influences metallurgical phenomena in ladle injection processes.

1. The discharge of gas from the spout can push the slag layer on the metal aside, and expose the underlying metal. This can lead to increased oxygen absorption by the bath [48] and affect negatively the desulphurization process and the production of clean steel.
2. In ladle furnaces with arc heating capability, the turbulence at the free surface can lead to contact between the liquid metal and the electrodes causing short circuiting.

In order to avoid these negative effects, the level of turbulence at the free surface must be kept as low as possible. Thus the operation must be conducted below the

critical flow rate. This is in agreement with the observation by Cramb and Byrne [48], who reported that it was not uncommon to find the total oxygen content of steel increased when the stir rate was high. This phenomenon can be understood on the basis of the mechanism proposed in the previous chapter. When the gas flow rate is high, the extent of gas/liquid interaction in the bath is decreased, causing a decrease in the efficiency of energy transfer from the gas to the bulk liquid. The energy of the gas is released near the free surface, resulting in an excessive turbulence at the surface. This leads to an increase in the oxygen absorption by the bath at the free surface, which causes the total oxygen content of the bath to rise.

8.2 Reactions at the Slag-Metal Interface

Several reactions occurring at the slag-metal interface, in various systems, have been studied, for example desulphurization and deoxidation, the rate of which depend on the extent of turbulence in the free surface region. The variation of the mass-transfer parameter with flow rate (or energy density) for these reactions has been characterized. As described in Chapter 2, the mass transfer was found to increase with flow rate as expected, but at a critical value, the rate of increase abruptly rose. A few examples of this phenomenon are summarized below.

1. The desulphurization rate constant was measured by Carlsson et al. [28] in plant trials, at different energy densities, using low-alloy steel in a 6 ton ladle. The process was conducted with a top slag and bottom gas stirring, and hence the kinetics of the reaction were dependent on the level of turbulence in the free surface region of the bath. It was observed that the rate of increase of the reaction rate constant increased abruptly at a critical energy density, ϵ , of 0.6 kW/ton (the flow rate value was taken at the steel temperature). The dependence of the

desulphurization rate constant on the energy density below the critical value was given as: $K_s \propto \epsilon^{0.3}$ and above the critical value was given as: $K_s \propto \epsilon^{1.3}$.

2. Kim and Fruehan [31] measured the mass transfer between two immiscible liquids, oil and water, simulating slag and steel respectively, in a model of a gas-stirred ladle. The transferring species was thymol which has an equilibrium partition ratio between oil and water similar to that for sulphur between slag and metal. The mass-transfer parameter increased with flow rate, but at a critical value equal to ~ 8 l/min, the rate of increase, accelerated.
3. Hirasawa et al. [30] studied the rate of mass transfer between molten slag and metal with gas stirring. A $Li_2O - SiO_2 - Al_2O_3$ slag-molten copper reaction system for oxidation was selected. The experimental conditions were: gas flow rate of injected argon of 180 to 6000 $cm^3 min^{-1}$, and metal depth of 3.7 cm. The slag depth was between 1.6 and 1.7 cm, although some experiments were also carried out with a slag depth of 1.2 cm. The apparent metal-side mass-transfer coefficient for Si was calculated from the raw data. The mass-transfer coefficient was found to increase with flow rate, as expected, but the rate of increase, abruptly rose at a critical flow rate equal to 50 $cm^3 s^{-1}$.

From the present study and the above mentioned examples, it can be concluded that, in the higher flow rate regime, a small increase in the flow rate can cause a large increase in the extent of turbulence at the free surface. This promotes the kinetics of the reactions at the interface. Therefore it is advisable to operate a process at higher flow rates, if the main objective is to enhance the rate of a reaction occurring at the slag-metal interface. A similar conclusion was reached by Carlsson et al. [28], who studied the kinetics of desulphurization reaction at the slag-metal interface in a gas-stirred ladle. They deduced that the energy density should be above 0.6 kW/metric

ton, in order to achieve acceptable desulphurization results.

8.3 Mixing in the Bath

If an efficient transfer of energy from the gas to the bulk liquid is desired, an injection process must be operated in the lower flow rate regime, as explained in the previous chapter. Therefore a gas flow rate below the critical value is advisable when the objective of the gas-stirring process is to achieve efficient bath mixing, temperature homogenization, dissolution and dispersion of alloying elements and prevention of reoxidation of the bath.

8.4 Variables Influencing the Critical Gas Flow Rate

If the mechanism proposed in section 7.5 is correct, one of the important variables influencing the critical gas flow rate, must be the bath height. As the bath height decreases, the extent of gas/liquid interaction is reduced. Hence momentum transfer and thus the energy transfer from the gas to the liquid becomes less efficient. Such an observation has been made by Asai et al. [32], who measured the mixing time in a gas-stirred bath at various mixing power densities, and at different bath heights. They observed that as the bath height decreased, the critical input power density dropped; thus at lower bath heights the efficiency of mixing decreases at a much smaller flow rate. In other words for the same input power density, the higher bath height proved to be more efficient.

8.5 Critical Value of Specific Input Power Density in the Bath

In order to compare the critical conditions found in the different systems of this study and previous works, the specific power input has been calculated for each. This parameter incorporates the gas injection rate and the bath height together with the density of the liquid. The expression given by Sano and Mori [19] has been adopted; it is given as follows:

$$\epsilon_s = 6.18 \frac{QT_L}{W} \ln \left(1 + \frac{\rho_L g h_b}{1.013 \times 10^5 P} \right) \quad (8.27)$$

In the above equation the flow rate must be expressed as $\text{Nm}^3\text{min}^{-1}$; the temperature (T_L) should be in degree Kelvin, the pressure in atm and the other variables in the MKS system to yield the specific input power density in Watts/kg.

The specific input power density calculated at the critical gas flow rate for the present physical model, and for the system of Hirasawa et al. [30] is presented in Table 8.3. The latter system was one of the few in the literature which involved liquid metal and slag and for which sufficient data was available to compute the power density.

Table 8.3: The variables and the critical specific input power density in two gas-stirred systems.

Variables	Present Study	Hirasawa et al.
Liquid	Water	Molten Copper
h_b	0.4 m	0.037 m
Temperature	20°C	1250°C
Critical flow rate	1350 cm^3s^{-1}	50 cm^3s^{-1}
ϵ_s	0.07	0.09

It can be seen from Table 8.3, that the critical values of the specific input power density for these two completely different systems are closely similar. Hence although

more data is needed, the specific input power density may be the single parameter which characterizes the gas/liquid energy exchange (bulk vs surface) in a gas-stirred bath.

8.6 Critical Flow Rate in an Operating Ladle

If the specific input power density calculated for the critical gas flow rates in the previous section is roughly constant, it can be used to make predictions regarding the value of the critical flow rate in an operating ladle. Data from Bethlehem Steel [49] on operating ladles, used for alloy additions, has been adopted for comparison with the values of flow rates obtained from the critical value of the specific input power density equal to 0.08. Specifically, two ladles of sizes 10 and 170 ton, with bath heights of 1.05 and 3 m respectively, have been considered. The maximum gas injection rates that can be maintained in practice to conduct the mixing efficiently are 0.08 and 0.6 $\text{Nm}^3\text{min}^{-1}$ for the two ladles respectively. Data from Nippon Steel [50] on a ladle mainly used to bring about temperature homogenization in the bath, also has been examined. The capacity of the ladle is 300 ton, with a bath height of 3.5 m; the maximum gas injection rate maintained in practice is 0.7 $\text{Nm}^3\text{min}^{-1}$. The critical flow rate obtained from the present study for the three ladles, is presented in Table 8.4.

It can be seen from Table 8.4 that the calculated critical flow rates are comparable to, but slightly larger than, the maximum flow rates adopted in practice. This is to be expected since operating too close to the critical conditions could result in periodically generating excessive surface turbulence and reduced bath mixing efficiency. From this comparison, it seems clear that the critical value of specific input power density is a useful criterion for delineating the regimes of gas/liquid exchange (bulk vs surface) and has practical significance.

Finally, the critical value of the specific input power density has been applied to calculate the critical flow rate at various bath heights, using Eq.(8.27). The results are shown in Figs. 8.60 and 8.61, for an 80-ton and 250-ton ladle respectively.

Table 8.4: Comparison between critical flow rates used in practice with values calculated from the critical specific input power density.

Variables	Ladle 1	Ladle 2	Ladle 3
Size	10 ton	170 ton	300 ton
h_b	1.05 m	3.0 m	3.5 m
Maximum Q(in practice)	$0.08\text{Nm}^3\text{min}^{-1}$	$0.6\text{Nm}^3\text{min}^{-1}$	$0.7\text{Nm}^3\text{min}^{-1}$
Critical Q calculated	$0.1\text{Nm}^3\text{min}^{-1}$	$0.8\text{Nm}^3\text{min}^{-1}$	$1.3\text{Nm}^3\text{min}^{-1}$

Figure 8.60: Variation of the calculated critical flow rate with bath height for an 80 ton ladle.

Figure 8.61: Variation of the calculated critical flow rate with bath height for a 250 ton ladle.

Chapter 9

Summary and Conclusions

The spout of air jets upwardly injected into a water bath has been characterized quantitatively with a two-element electroresistivity probe. Measurements of gas-fraction, bubble-frequency and axial velocity distributions within the spout were made. Correlations of the gas-fraction with axial and radial position for different gas flow rates were established from the measurements. The dimensions of the spout also were obtained from time-exposure photographs; when compared with the gas fraction measurements, the spout boundary always corresponded to values ranging from 0.82 to 0.86. The frequency of the spout oscillation was obtained from high-speed photographs for comparison to the bubble-frequency measurements at the spout boundary; the bubble-frequency always fell in the range of 2 to 4 s⁻¹.

The gas-fraction correlations then were incorporated into a model for turbulent, recirculatory flow; predictions of velocity, turbulent kinetic energy and effective viscosity were made in the bath. Comparisons of the flat surface model (no spout) and the present model (including the spout) with experimental data were made. The model including the spout predicted the velocities more accurately compared to the flat surface model. The variation of the total turbulent kinetic energy in the spout with flow rate was analysed. The turbulent kinetic energy increased with flow rate as expected, but at a certain critical flow rate, the rate of increase abruptly rose. It was concluded that there were two distinct flow regimes, one characteristic of low flow rates and the other of higher flow rates. Photographs of the gas/liquid dispersions were taken in both

regimes. Based on photographs of the gas/liquid dispersion this behaviour appears to be related to the location of bubble breakup. At lower flow rates the bubbles breakup close to the nozzle whereas at higher flow rates the bubble break-up did not occur until close to the surface. Hence at flow rates below the critical value, the gas/liquid interaction was enhanced which promoted the gas/liquid energy transfer. Moreover, at higher flow rates the gas dispersion intermittently was in the form of a continuous chain of interconnected gas envelopes, which could allow a fraction of the gas to channel through the bath. This would lead to an inefficient gas/liquid energy transfer, resulting in a reduced efficiency of bath mixing. This study thus could explain the findings of some previous investigators who observed that the rate of decrease of mixing time with flow rate, decreased beyond a critical flow rate and also that the total specific kinetic energy of liquid in a gas-stirred bath decreases with increasing flow rate. These observations can be explained on the basis of the present work. Finally considering ladle processes, it can be concluded that, depending on the objective of the gas-stirring process, stirring should be effected in either the lower or the higher flow rate regime. If the aim of the process is to achieve an efficient bath mixing, temperature homogenization, dissolution and dispersion of alloying elements and prevention of reoxidation of the bath, the flow rate should be maintained below the critical value to maximize mixing and minimize energy release at the surface. Flow rates greater than the critical value should be imposed when the goal is to enhance the kinetics of reactions at the slag-metal interface, because then turbulence at the surface is a maximum.

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