INJECTION PHENOMENA AND HEAT TRANSFER IN COPPER CONVERTERS

by

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ABSTRACT

The injection dynamics and related accretion build up, as well as bath motion and heat losses in copper converters, have been investigated. The studies involved physical and mathematical models coupled with plant trials at four copper smelters to examine gas discharge phenomena, bath slopping and heat transfer within the converter.

The laboratory work, performed on a 1/4th scale model of a converter, indicated significant tuyere interaction. Air discontinuously discharges into the bath with a frequency which increases with gas flow rate and is affected by the bath circulation velocity in the tuyere region. Measurements have delineated slopping behaviour in terms of tuyere submergence and the buoyancy power input to the bath.

The industrial trials were conducted in Peirce-Smith, Hoboken and Inspiration converters under normal conditions. A tuyerescope attached to the back of a tuyere permitted the direct observation of accretion growth and the sampling of accretions during blowing. The tests indicated that the copper converter operates under bubbling conditions. Pressure pulses from the tuyeres revealed that in non-ferrous submerged injection processes three regimes of gas-liquid interaction can be identified: bubbling, unstable envelope and channelling.
The relative dominance of each regime is affected by tuyere line erosion, viscosity of the bath and tuyere submergence. Analysis of the accretion samples revealed that accretions in the copper converter form mainly by the solidification of bath at the tuyere tip. Oxygen enriched air does not prevent accretion formation, but seems to produce a softer, easy-to-punch accretion. The type of puncher as well as punching frequency affect conditions inside the tuyere pipe and this could have an influence on accretion formation.

The mathematical heat transfer model indicated that when the converter is out of the stack, heat losses through the mouth of the converter cause the internal surface to cool rapidly which may lead to freezing at the tuyere line and tuyere blockage when blowing is resumed. The temperature gradient, localized to within 60-80 mm of the refractory inside wall, changes markedly within the first minutes of the converter being out of stack. This may generate thermal stresses in the converter wall and contribute to refractory erosion at the tuyere line. Covering the converter mouth during out-of-stack periods significantly reduces the change in temperature gradient at the inside wall as well as heat losses from the converter.
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<td>A</td>
<td>Cross Sectional Area</td>
<td>$m^2$</td>
</tr>
<tr>
<td>c</td>
<td>Specific Heat</td>
<td>$J \ kg^{-1} \ K^{-1}$</td>
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<td>C</td>
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Pr Prandtl Number
Q  Gas Flow Rate  m$^3$s$^{-1}$
q  Heat Transfer Rate  W
R  Radius  m
Re Reynolds Number
s  Space  m
T  Temperature  K
t  Time  s
u  Velocity  m$s^{-1}$
V  Volume  m$^3$

Greek Symbols

α  Partition Factor of $\varepsilon_k$ in Equation (7.1)
β  Bubbling Factor
ρ  Density  kg$m^{-3}$
σ  Surface Tension  N$m^{-2}$
μ  Viscosity  N$s$m$^{-2}$
λ  Reflectance
ε  Emittance
$\varepsilon_k$  Kinetic Power per Unit Mass of Bath  W$kg^{-1}$
$\varepsilon_b$  Buoyancy Power per Unit Mass of Bath  W$kg^{-1}$
$\varepsilon_s$  Stirring Power per Unit Mass of Bath  W$kg^{-1}$
θ  Wetting Angle  degree
Subscripts

b  Bubble

c  Chamber

g  Gas

l  Liquid

M  Model

o  At the Orifice

P  Prototype

S  Surroundings
ACKNOWLEDGEMENTS

I would like to thank most sincerely my supervisor, Dr. Keith Brimacombe. His assistance, guidance and friendship made my stay at UBC one of the happiest periods in my life.

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Finally my gratitude to Camila, Alejandro and Pablo. Their help, understanding and patience constituted the strongest stimulus to initiate and complete this work. Indeed this thesis is also theirs.
El cobre ahí dormido.
Son los cerros del Norte desolado.
Desde arriba
las cumbres del cobre,
cicatrices hurañas,
mantos verdes,
cúpulas carcomidas
por el ímpetu
abrasador del tiempo,
cerca de nosotros la mina:
la mina es sólo el hombre,
no sale
de la tierra el mineral,
sale del pecho humano.

... 
Es hora
de dar el mineral
a los tractores,
a la fecundidad
de la tierra futura,
a la paz del sonido,
a la herramienta
a la máquina clara
y a la vida.
Es hore de dar
la huraña mano abierta del cobre
a todo ser humano.

... 
De los cerros abruptos,
de la altura verde,
saldrá el cobre de Chile,
la cosecha más dura
de mi pueblo,
la corola incendiada,
irradiando la vida
y no la muerte,
propagando la espiga
y no la sangre,
dando a todos los pueblos
nuestro amor desenterrado,
nuestra montaña verde
que al contacto
de la vida y el viento
se transforma
en corazón sangrante,
en piedra roja.

PABLO NERUDA

Copper lies there resting.
Amongst the desolated Northern heights.
From above
the copper hills,
strange scars,
green mantles,
domes undermined
by the ever embracing impetus of time,
near us the mine:
the mine is only man,
minerals do not leave the earth,
they leave the human heart.

... 
It is time
to give the mineral
to tractors,
to the fertility
of the future land,
to the peace of sound,
to tools,
to the lucid machine
and to life.
It is time to give
copper's shy and open hand
to every human being.

... 
From the steep hills,
from the green heights,
copper will leave Chile,
the hardest harvest
of my people,
the burning corolla,
irradiating life
denying death,
giving life to the stem
and not wounds,
giving all peoples
our unearthed love,
our green mountain
touched
by life and wind
transforming
streams of blood,
in red stone.

Translated by Dr. A. Urrello
Dept. of Hispanic and Italian Studies
The University of British Columbia
CHAPTER I

INTRODUCTION: THE COPPER CONVERTING PROCESS

The submerged injection of gas into molten baths has been practiced in both the ferrous and non-ferrous industries for a century. In steelmaking it was first used as early as 1860 in the Bessemer bottom-blown acid process. Bottom-blown steelmaking is increasingly employed today due to the development of the concentric tuyere which allows injected oxygen to be shielded with another gas thus eliminating extremely high temperatures near the bottom refractories. Today in the non-ferrous-industry processes that employ submerged injection, such as the smelting of sulphide concentrates, the zinc slag fuming process, the treatment of tin slags, and the production of copper and nickel are in operation or under study.

The most important application of submerged gas injection in the non-ferrous industry is the converting of copper mattes. The purpose of converting is to remove iron, sulphur and other impurities from matte, thereby producing liquid metallic copper in a crude blister form. This is achieved by oxidizing the molten matte with air. The converting reactions are exothermic and the process is autogeneous.
1.1 History of Copper Converting.

Prior to the development of the converter, copper sulphide ores were smelted and processed into metallic copper by the use of the Welsh process for 'black copper'. The Welsh process, from which reverberatory smelting sprung, was carried out in six separate operations involving the oxidation of some of the copper sulphide to copper oxide which then reacted with the remaining sulphides to produce copper, slag, and \( \text{SO}_2 \). This operation was slow, tedious and expensive.

The primitive ancestor of the conversion of matte into copper through oxidizing the sulphur by means of a blast of air was the 'mabuki' method, practiced in Japan from early times. Modern development had its inception when H. Bessemer pointed out that the removal of the carbon in pig iron could be accomplished by blowing air through the molten mass. The analogy of carbon in pig iron with sulphur in copper-iron sulphides suggested the idea of producing copper from matte in the Bessemer converter. Early experiments were unsuccessful because the liquid copper was cooled by the incoming air, to the extent that the liquid froze and choked the tuyeres. In 1879 J. Hollway made a series of experiments and pointed out that the heat generated by the oxidation of the iron and sulphur is sufficient to maintain the bath in a molten state.
during operation and that the difficulties are lessened when
the active mass is greater. He further pointed out that the
SiO₂ required to form a slag can either be derived from the
siliceous lining of the converter or from siliceous material
thrown into the converter during the progress of the
operation. At the same time P. Manhés and P. David attacked
the problem at the Védennes smelter²,⁵,⁶. They soon discover-
ed that the chief difficulty was the clogging of the tuyeres
by the chilling of the copper and hit on the expedient of
placing the tuyeres horizontally, at such a height that the
blast would not chill the metallic copper as it formed. After
these trials the process was introduced on a larger scale,
with two-3 ton Bessemer converters being used, in one of which
the matte was concentrated up to about 60% copper, then trans-
ferred and blown to blister copper in the second. By 1890 the
advantages of the new method were generally recognized and
upright type converters were in use or under construction in
several places with the method of keeping the tuyeres free
from copper by systematic punching being added to the operation.

Nevertheless, soon it was realized that there is a wide
difference between the Bessemer process applied to iron and
the same operation when employed for the treatment of copper
mattes. In Bessemer steelmaking the bath is homogeneous
during the entire process. On the contrary, during treatment
of copper mattes, as soon as the operation has proceeded so far that there is not sufficient sulphur left three distinct products are found\textsuperscript{7,8}: a slag floating on the surface, a middle constantly decreasing zone of matte, and lowest of all, a constantly increasing layer of metallic copper. As a consequence, in the upright type of converter, it frequently was found that the air was injected into slag, and the charge could not be blown to a finish, yielding copper. This difficulty soon led to the adoption by David and Manhes of a second form of converter, a horizontal cylindrical vessel, with lateral tuyeres, that could be turned around its central longitudinal axis. This cylindrical form of converter was first introduced at Livorno (Leghorn), Italy; in 1891 it was in operation at Jerez Lanteira, Spain\textsuperscript{2,7,9}. It was not until later that the horizontal converter was first employed in North America at the Vivian plant at Sudbury, Ontario\textsuperscript{2}. The first converter of this type in the United States for treating copper mattes was that of the Copper Queen Company\textsuperscript{10}.

Another problem was found during the treatment of copper mattes at this early age of copper converting. The oxidation of the iron formed large quantities of basic slags, which at once attacked the converter lining to satisfy their strong affinity for silica. The necessity for frequent renewal of the lining, typically after 12 blows\textsuperscript{6,7}, gave the driving
force for experiments with a lining which was not attacked chemically by the process. The use of an inactive lining in the form of magnesite brick, and the addition of SiO₂ to slag the FeO formed, was carried out successfully by W. H. Peirce and E. A. C. Smith, in 1909, at the Baltimore Copper Smelting and Rolling Co.¹¹,¹². Although the basic character of the lining has nothing to do chemically with the process, the Peirce-Smith basic converter process was so eminently successful that it replaced acid converting all over the world, and has remained an integral part of nearly all converting operations for three quarters of a century.

1.2 Current Converting Practice

As has been described earlier copper converting is a batch process in which molten matte, predominantly a mixture of cuprous sulphide (Cu₂S) and ferrous sulphide (FeS), is artfully brewed with oxygen and silica to yield three end products: an iron silicate slag, sulphur dioxide, and molten copper. It takes place in two stages both of which involve blowing air into the liquid matte⁸,¹³-¹⁵: the slag-forming stage and the blister-forming or copper-making stage.

At the start of the blowing the phases inside the converter are a bath of molten matte through which air is being blown and
upon which is floating a solid flux, mainly silica. The temperature is between 1100 and 1300 °C. The main reaction occurring during the slag-form stage can be represented as follows:

$$\text{FeS}_1 + \frac{3}{2} \text{O}_2(g) = \text{Fe}_0 + \text{SO}_2(g)$$  \hspace{1cm} (1.1)

The matte is added to the converter in two or more steps, each step being followed by oxidation of much of the FeS from the charge. The resulting slag, liquid fayalite saturated with magnetite, is poured from the converter after each oxidation step and a new matte addition is made.

Toward the end of the slag-forming stage the amount of copper in the converter has gradually increased so that there is sufficient for a final copper-making blow. At this point the FeS content of the matte is about 1%, and the formation of magnetite in the converter as indicated by the reaction:

$$3 \text{Fe}_3\text{O}_4(s) + \text{FeS}(1) = 10 \text{FeO}(1) + \text{SO}_2(g)$$  \hspace{1cm} (1.2)

becomes particularly severe. Although some magnetite is desirable to protect the refractories, an excessive amount leads to viscous slags and to the entrainment of large quantities of matte. A common practice to minimize magnetite formation is to maintain as high a concentration of FeS in
the matte as possible by only partially oxidizing the matte after each matte addition. This practice ensures that a considerable portion of the solid magnetite will be reduced.

During the blister-forming stage, metallic copper is formed in the converter and the remaining sulphur is oxidized to \( \text{SO}_2 \) by a combination of the reactions:

\[
\text{Cu}_2\text{S}(l) + \frac{3}{2} \text{O}_2(g) = \text{Cu}_2\text{O}(s) + \text{SO}_2(g) \quad (1.3)
\]

\[
\text{Cu}_2\text{S}(l) + 2 \text{Cu}_2\text{O}(s) = 6 \text{Cu}(l) + \text{SO}_2(g) \quad (1.4)
\]

\[
\text{Cu}_2\text{S}(l) + \text{O}_2(g) = 2 \text{Cu}(l) + \text{SO}_2(g) \quad (1.5)
\]

The process is carried out until the first trace of \( \text{Cu}_2\text{O} \) appears. Great care is taken to ensure that the copper is not overoxidized, because there is no longer any \( \text{Cu}_2\text{S} \) to reduce the \( \text{Cu}_2\text{O} \) back to copper by reaction (1.4). The final product is molten blister copper (98.5 - 99.5% Cu) containing about 0.05% S, 0.5% O, and minor amounts of other impurities.

Today the converting of copper mattes is almost universally carried out in Peirce-Smith converters. In recent years, however, three novel types of converter have been developed: the Hoboken or syphon type converter\(^{16}\), the top-blown con-
The Peirce-Smith converter\textsuperscript{11,13} is a horizontal cylinder, typically 4 m in diameter and 9 m long, constructed of a steel shell lined with burned magnesite or chrome-magnesite brick. Molten matte is charged to the converter through a large rectangular opening or 'mouth'. Air at low pressure (104 kPag, 8-12 Nm\textsuperscript{3}/s) is side blown into the converter through a single line of 4-6 cm diameter tuyeres, distributed along the length of the vessel. The tuyeres consist of steel pipes imbedded in the refractory and they are connected to a bustle pipe running the length of the converter. There are forty to fifty tuyeres per converter, depending upon their diameter and the size of the reactor. The converter is provided with a rotating mechanism which permits it to be correctly positioned for charging, blowing, and pouring. The large volumes of hot, SO\textsubscript{2}- bearing gases produced during converting are collected by means of a loose-fitting hood above the converter.

Recent concern over air pollution has led to the development of two types of converter, the Hoboken or syphon-type converter\textsuperscript{16}, and the Inspiration converter\textsuperscript{20}. Both reactors represent a method to improve the collection of the converter gases and to prevent their dilution with air. In the Hoboken
converter the gases are drawn off through a flue connected axially to the converter, via a 'goose-neck' or syphon connection. In the Inspiration converter the gases are extracted from the vessel via an 'off-gas mouth' in the mantle of the reactor. Both arrangements, though more complex than the Peirce-Smith configuration, have some useful features. The converters can run with a zero or slightly negative pressure at the charging mouth, preventing $\text{SO}_2$ from escaping and minimizing dilution of the converter gases by infiltrated air. In addition, charging can be carried out during the blow, reducing converting downtime and producing a more constant stream of gases.

1.3 Problems in Copper Converting

In spite of its widespread use, gas injection in the Peirce-Smith converter (or any other submerged-injection-type of reactor) is not a technique free of operational difficulties. Since its inception the process has experienced several problems such as accretion formation, tuyeres blockage, refractory wear, tuyere erosion, splashing and slopping.

During converting, a build up of solidified material or accretion takes place at the tuyere orifice. As a consequence the flow of air through the tuyere gradually becomes
blocked. Periodic clearing or 'punching' of the tuyere by forcing a steel bar through the tuyere, either manually or by a pneumatic/mechanical system, has therefore become a routine practice in every converting operation. Manual punching has largely given way to mechanical/pneumatic punching devices. Two types of mechanical punchers most commonly are used: the Kennecott (4B5) puncher, which is attached to each tuyere and the Gaspé puncher, which is mounted separately from the converter and moves parallel to it on rails. Other innovations in tuyere punching include the development of silencers to reduce the considerable noise which accompanies punching. A review of developments in tuyere punching, and a tabulation of the type of punching systems in use in smelters around the world have been published.

The periodic clearing of the tuyeres, the high temperature of operation, and the turbulent conditions of the bath constitute harsh conditions for the tuyeres and the adjacent refractory brick. Severe refractory erosion at the tuyere line usually appears in the form of a trench running from one tuyere to the next and is the limiting factor in the campaign life of a converter. The lining life today normally does not exceed 100 to 200 days or about 2.25 to 4.5 kg refractory per tonne copper produced. Because of refractory wear and tuyere erosion, a converter is typically down one month in four such
that about 25% of the nominal converter capacity is lost.

The energy of the injected air is not dissipated entirely in mixing the bath. Splashing occurs which causes particles of liquid to be carried out with the gas above the surface of the bath. This results in the build up of accretions at the converter mouth, and dust losses in the flue gas. A large amount of maintenance work to remove accretions resulting from splashing and to replace refractories is required amounting to around 30% of the converter operating cost\textsuperscript{13}. Slopping also takes place in which a standing wave of the molten bath produces massive ejections of liquid through the mouth of the converter. The severity of splashing and slopping represents an ultimate limitation of the air flow rate through the tuyeres. A limiting value of 0.14 Nm\textsuperscript{3}/s per m\textsuperscript{3} of converter volume has been suggested\textsuperscript{23}.

Over the years numerous studies concerning the chemical reactions in copper converting have been conducted. In contrast, remarkably little is understood about the influence of the design and operation of the reactor on the numerous problems underlined above, which have hampered the operation of the converter. From a general perspective, progress in design of the reactor or improvement in its operation will not be achieved without process engineering knowledge. The present
thesis represents an effort to investigate some process engineering aspects of converter operation, such as: gas injection into the bath, accretion growth at the tuyere tip, slopping of the bath and heat losses from the interior of the converter during out-of-stack periods. The study of these subjects has involved physical model laboratory experiments, plant measurements, and the formulation of a mathematical model. The following pages describe the work, its results and recommendations to extend refractory life and to increase converter productivity.
CHAPTER II

LITERATURE REVIEW: STUDIES IN GAS INJECTION

The basic converting operation has remained essentially the same since its implementation. Fluid dynamics and heat flow of the process have not been studied in detail, and the design of reactors as well as the converting practice have been based mainly on operating experience. Such experience varies widely from plant to plant, as can be observed from data reported during the last quarter century\(^23,24,25\). The effect of converting practice on bath movement, splashing, accretion formation at the tuyeres tip, and the life of tuyeres and refractory is not clearly understood. Operational improvements and the development of effective process control have been inhibited through the lack of such understanding.

Little work has been done on the injection process in copper converters. Studies related to the subject have been performed under experimental conditions quite different from those prevailing during industrial converting. The relationship between the gas dispersion process and the bath movement and splashing, and the effect of adjacent tuyeres on bubble coalescence in the tuyere region have not been reported.
At present there are two main approaches used to describe the process of gas injection into liquids. One is based on the similarity between the behaviour of a jet and its time-averaged shape; the other is concerned with the growth, separation and disintegration of the gas filled envelopes that form at the tuyere. The links between them and the influence of the instantaneous conditions on the general behaviour of the process are not clear.

2.1 Studies on Side-Blown Injection

When one fluid is discharged into another, a tangential unstable separation surface is created between the injected fluid and the surrounding medium. The instability of this surface leads to the entrainment of the surrounding medium and causes the jet to expand, forming a characteristic jet cone.

In 1969 Themelis et al.26, on the basis of mass and momentum balances, derived a dimensionless equation describing the trajectory of a gas jet horizontally injected into a liquid, which was found to be a function of the modified Froude number for the gas-liquid system. Using a photographic technique the shape and trajectory of the jet
were measured for an air-water system. It was concluded that the cone angle is a function of the jet fluid properties, and that the increase of the jet diameter is proportional to the horizontal distance from the orifice. Agreement was obtained between the dimensionless equation and experimental results for the air-water system, with a liquid-gas density ratio of nearly nine hundred. Therefore it was suggested that the jet trajectory equation should also be applicable to the case of an air jet injected into liquid matte or copper.

Later Engh and Bertheussen\(^2^7\) modified the model of Themelis et al. by assuming the diameter of the cone to be proportional to the distance along the jet axis rather than the horizontal distance from the orifice. The results made clear that both models provide a satisfactory means of predicting the trajectory of air jets into water.

Spesivtsev et al.\(^2^8,^2^9\) published some data from tests made on both cold and hot models of converters. Their results showed that the penetration of the gas into the liquid is limited by the properties of the liquid, and that the interaction between both fluids has a pulsating character. An equation was proposed describing the horizontal penetration of the jet as a function of the Froude number and the gas-liquid density ratio. It was concluded that to obtain a
longer service life for the converter lining and stable blowing conditions the bath level should be kept constant. The existence of a certain permissible diameter for the converter was suggested.

In addition, experimental work by Oryall and Brimacombe and by Hoefele and Brimacombe has called into question the validity of some of the assumptions upon which both models are based, and on the applicability of the models to the case of air injected into liquid-metal systems.

The experimental results of Oryall and Brimacombe, obtained by horizontally injecting an inert gas into mercury, suggest that:

a) the jet expands rapidly upon discharge from the nozzle, with an expansion angle of about 155 degrees, instead of 20 degrees as was found by Themelis et al. for the air-water system.

b) the jet expansion appears to be confined to the horizontal region of its trajectory, the vertical sections do not show appreciable expansion. The general shape of the jet is more akin to a vertical column than to a cone.

c) while the central streamline only penetrates 5 mm into the fluid, there is considerable backward penetration. The air penetrates a considerable distance behind the nozzle, and the jet looks as if it has been injected vertically upwards.
rather than horizontally.

Hoefele and Brimacombe\textsuperscript{31} working in the laboratory, studied the injection of different gases from a submerged, horizontal tuyere, into water, a zinc-chloride solution, and a mercury bath. Working with actual industrial equipment, tests were performed involving pressure measurements in the tuyeres of a nickel converter under normal low-pressure blowing operation, and under higher pressure conditions. The results of their studies can be summarized as follows:

a) two regimes of flow can be distinguished. At normal low flow rates a bubbling regime predominates. In the industrial tests the bubbling frequency was found to be about 10 to 12 s\textsuperscript{-1}. About half of the bubbling cycle is occupied by bubble growth, while for the other half, liquid surrounds the tuyere tip. At higher flow rates a steady jetting regime predominates, in which gas flows from the tuyere continuously. Pressure changes in the tuyere have been correlated to the individual flow regimes.

b) the flow regimes and the forward penetration of the gas depend on both the modified Froude number of the system and the gas-liquid density ratio. The latter result agrees with the findings of Spesivtsev et al.

c) a jet behaviour diagram was presented illustrating the regimes of bubbling and jetting as a function of the
modified Froude number and the ratio of fluid densities.

d) the bubbling regime has two major disadvantages: the bubbles rise almost vertically from the tuyere tip and impinge on the back wall, contributing to refractory erosion, also between the formation of successive bubbles bath washes over the tuyere tip, accelerating accretion formation and the need for punching. To avoid these problems Hoefele and Brimacombe suggested that the pressure be increased sufficiently to achieve choked flow and steady jetting rather than bubbling conditions.

The concept of high-pressure air injection and punchless converter operation was tested in-plant by Brimacombe et al. 33,34. Four standard tuyeres near the end wall of a converter were replaced by pipes with an I.D. of 19 mm, connected to a high pressure line. Air at 60 psig was injected through the four high-pressure tuyeres at the same time as 15 psig air was blown through the remainder of the tuyeres in an otherwise normal converter campaign. The high-pressure tuyeres operated through eighty-eight charges over a period of eighty-nine days without the need for punching. It was found through the test campaign that accretion formation around the periphery of the tuyere could be controlled by oxygen enrichment.
Fruehan and Martonik\textsuperscript{35}, injecting air into water and glycerol-water solutions, found that the jets do not expand at a constant angle. The shape of the jet was shown to be dependent on the physical properties of the liquid. It was also found that the horizontally injected air does not penetrate into the liquid as far as previously believed on the basis of model calculations. The experimental results were consistent with the interpretation that the gas jet breaks up into a swarm of small bubbles just above the tuyere tip.

The applicability of the models developed by Themelis et al., and Engh and Bertheussen, have also been called into question from the mathematical point of view. It was demonstrated\textsuperscript{36} that both models break down when applied to high liquid-gas density systems, or in other words, when the radius of curvature of the jet axis becomes equal to the radius of the jet. A general approach to model the flow field during the injection of gas into liquid has been outlined by Mc.Kelliget et al.\textsuperscript{37}
2.2 Bubble Formation During Submerged Injection

The fact that air initially discharges into the converter bath in the form of discrete bubbles is of fundamental importance to understanding converter operation. Most of the gas injection steelmaking processes work under jetting conditions. On the contrary, under normal operation, the copper converting process is not a jetting system, but a bubbling system. Thus any successful comprehension of the process requires an understanding of the bubble formation mechanism during converting. Direct observations are obviously difficult due to the opacity of the melts, the elevated temperatures involved, and the highly turbulent characteristics of the bath.

As a consequence, much data have been accumulated on bubble formation in aqueous or low-melting point metal systems. In almost all the published works the gas has been injected into the liquid bath through only one tuyere. Hence direct extrapolation to a metallurgical, multiple-tuyere process can be misleading and yield erroneous conclusions.

When a gas is injected into a fluid of greater density, three different flow regimes may be identified as a function of gas flow rate. At very low gas flow rates, of the order of 1 cm³/s, a single bubble process exists, the bubbling
frequency is proportional to the gas flow rate, and the bubble size is almost constant. For higher gas flow rates, up to about 500 cm$^3$/s, the gas emerges as series of envelopes or bubbles, characterized by liquid bridging of the orifice, the bubble volume increases with gas flow rate, while the frequency of formation remains almost constant. At very high gas flow rates a jetting regime is achieved, in which a continuous gas channel is formed through the liquid.

During bubble formation, the pressure within the bubble decreases due to upward displacement of its centroid, so that the gas flow rate may vary with time. If there is a high pressure drop between the gas reservoir and the orifice, the pressure fluctuation due to forming bubbles is much smaller than the pressure drop between the gas reservoir and the orifice, and in this case the gas flow rate can be considered as constant. If the volume of the reservoir, or chamber volume, is very large compared with the volume of the bubbles being formed, the pressure in the chamber will not significantly change. This corresponds to the other limiting case of bubble formation under constant pressure conditions.

A general model applicable to the formation of bubbles under all kinds of conditions is not available. The many models proposed to describe bubble formation in liquids have been reviewed by Clift et al.$^{45}$ and Kumar and Kuloor$^{46}$. All
are mechanistic and depend on some form of force balance for predicting one or more stages in the bubble growth. In the 'one-stage' models, bubbles originating at the orifice are assumed to grow smoothly until detachment, which occurs when the rear of the bubble passes the orifice. In the 'multiple-stage' models it is assumed that there is a basic change in the growth mechanism at one or more points in the process.

From the metallurgical point of view, the more relevant bubbling processes are those under constant flow and constant bubbling frequency conditions. As the majority of the liquid melts into which a gas is injected exhibit small viscosities (except the treatment of metallurgical slags) the viscous forces are negligible compared with buoyancy and inertial forces.

2.2.1 Bubble Formation in Low Density, Inviscid Liquids

Many models have been proposed\textsuperscript{47-55} to describe bubble growth from a slow steady gas stream injected into a low density, inviscid liquid through only one tuyere or nozzle under stagnant bath and constant flow-rate conditions. The proposed models result in a relationship between bubble volume and gas flow rate of the form:
where the bubbling factor $\beta$ depends on the specific conditions of the gas injection process.

In the models developed by Davidson and coworkers $^{47-49}$ a spherical bubble is assumed to be formed at a point source where the gas is supplied. The rising velocity is determined by a balance between the buoyancy force and the acceleration of the fluid around the bubble, which is carried along with it.

\[
(M - M_b) \, g = \frac{d}{dt} \left[ (M_b + C_a M) \frac{ds}{dt} \right] \tag{2.2}
\]

An 'added mass' or 'virtual mass' contribution has to be included because acceleration of the bubble requires acceleration of the liquid. The mass of the bubble is negligible compared with the mass of the liquid swept along with it. As the flow rate is assumed constant, the final volume of the bubble at detachment can be expressed as:

\[
V_b = \left[ (4 \, C_a) \right]^{3/5} \left( \frac{3}{4} \right)^{1/5} Q^{6/5} \, g^{-3/5} \tag{2.3}
\]

For a sphere moving parallel to a wall, the added mass coefficient has a value of $11/16$; then the bubbling factor in
Equation (2.1) is 1.378, a solution given by Davidson and Schuler\textsuperscript{47,48}. For a nozzle protruding into a fluid $Ca = \frac{1}{2}$, then the $\beta$ coefficient becomes 1.13, a result obtained by Davidson and Harrison\textsuperscript{49}. An empirical expression similar to Equation (2.1), with a bubbling factor of 1.725, had been previously developed by van Krevelen and Hoftijzer\textsuperscript{50} assuming the gas density to be negligible relative to the liquid density.

Kumar and Kuloor developed a model\textsuperscript{51} in which the bubble formation process was assumed to take place in two stages, the expansion stage and the detachment stage. During the expansion stage there is an inertial force due to the expansion rate. As the bubble grows the drag due to expansion decreases whereas the buoyancy force increases continuously. This stage terminates when the buoyancy force equals the downward inertial force. During the detachment stage the buoyancy force is higher than the inertial force. The detachment takes place when the bubble base covers a distance equal to the radius of the bubble from the first stage. After some simplifications, and assuming a spherical bubble forming at a perforation in a flat plate, Kumar and Kuloor's model predicts a bubbling factor of 0.976.

Wraith and coworkers\textsuperscript{53-55} performed extensive observations of submerged nozzle injection. A dispersion process was
observed, consisting of the successive expansion, movement and intermittent severance of bubbles from the source. At a higher level in the liquid the bubbles disintegrated, forming a column of foam rising to the surface. Visual observations when the gas source lies on a horizontal plate showed the growth of a hemispherical envelope pressed to the plate during an early stage of bubble expansion. The hemisphere evolves toward a spheroidal shape as the condition of tangential contact is approached. At the end of the expansion stage, the bubble formation process exhibits a $B$ value of 1.09, thus the bubble volume is smaller than the volume predicted by the models of Davidson and coworkers. This result was verified for low air-injection rates into water. Following the tangential contact, the base of the bubble remains linked to the orifice by a gas-filled stem. The bubble volume grows continuously while it moves upwards. At detachment the bubbling factor is 1.54, a value larger than that predicted by Davidson and coworkers.

An isothermal study of submerged air lancing in water was also performed by Wraith. Coalescence was observed between successive bubbles. When the lance immersion depth was greater than 2.5 times the lance radius, the bubble formation process was adequately described by Equation (2.1) and a $B$ value of 1.138. For immersion depth lower than the above value, the bubble formation mechanism was different. Gas then chan-
nelled directly to the surface through a series of incompletely formed envelopes. Under these conditions the relationship between bubble volume and gas flow rate is characterized by a bubbling factor of 1.453.

Hoefele and Brimacombe, working on a one tuyere model of a copper or nickel converter, indicated that the bubble-formation process can be adequately described by Equation (2.1) with bubbling factor values of 1.57 and 0.88 for mercury and aqueous baths respectively. However, when applied to a new, recently relined industrial converter, Equation (2.1) predicts a bubbling frequency 2 to 3 times lower than the measured value. Bubble volumes under high temperature conditions were also calculated.

A comparison between the predictions of the above models is shown in Figure 2.1, for a gas flow rates between $10^2$ and $10^4$ cm$^3$/s. The discrepancies are not so great, considering the various experimental conditions involved, and the different assumptions made to develop the models.

When the ratio of gas momentum to buoyancy force acting on the system exceeds a critical value, the bubble formation process is significantly affected. Wraith and Chalkley performed model experiments involving energetic gas injection.
Figure 2.1 Bubbling Frequency versus Gas Flow Rate for Different Models. Number in Parenthesis Indicates Reference.
It was found that the effect of gas momentum is to elongate the gas filled envelopes, producing continuous coalescence between successive bubbles. A jet like bubble-stem array was observed, which ultimately breaks down into foam due to basal intrusion.

For submerged vertical injection, if the inertial force is important compared with the buoyancy force, Equation (2.2) has to be transformed to:

\[(M - M_b) g + \rho g u_o^2 A = \frac{d}{dt} \left[ (M_b + C_a M) \frac{ds}{dt} \right] \quad (2.4)\]

The results obtained by Nilmani and Robertson after the integration of Equation (2.4) were compared with the values from Davidson and Schuler's model for the different gas-liquid combinations. Good agreement between both models was observed when the gas-liquid density ratio is small. However, for an air-water system the predictions between the models differed significantly.

2.2.2 Bubble Formation in Liquid Metals

As was mentioned, the previous models have been developed to describe the bubble-formation process from a gas stream injected into a water-like liquid. The predictions of the dif-
different models are in reasonable agreement with experimental measurements. Nevertheless, the validation of such models for the case of gas injection into liquid metals is not conclusive. There are few studies involving bubble measurements in liquid metals, and the comparison between them is difficult due to the many different conditions used in the experiments.

Andreini et al.\textsuperscript{58} discharged argon at low gas flow rates into copper, lead, and tin baths. It was concluded that the bubble size generated for a particular orifice diameter was dependent upon the magnitudes of the orifice Froude and Weber numbers; therefore predictions of bubble size based on empirical regressions cannot be extrapolated from aqueous systems. As in aqueous systems, it was found that the frequency of bubble formation increases with flow rate to some limiting value, above which a constant bubbling frequency occurs, confirming the weak dependence of bubbling frequency on gas flow rate. Similar results have been reported by Berdnikov et al.\textsuperscript{40}.

The wetting characteristics of the liquid represent the principal difference between liquid metals and aqueous systems regarding bubble formation\textsuperscript{43,44,59-61}. For a gas-bubble formation in aqueous or organic systems, when the
liquid wets the nozzle, the bubble forms at the tip of the orifice, that is at the internal nozzle diameter. In contrast, for non-wetting systems the bubble tends to form at the outer nozzle circumference.

Mori and coworkers\textsuperscript{60-71} examined the nitrogen-mercury, nitrogen-silver, and argon-hydrogen–liquid iron systems. It was found that for intermediate flow rates, and small nozzle diameters, the bubble volume is well described by the equation of Davidson and Amick\textsuperscript{72}

\[
d_b = 0.54 \left[ Q d_o^{0.5} \right]^{0.289}
\]

or, in terms of the volume of the bubble:

\[
V_b = 0.08 Q^{0.87} d_o^{0.44}
\]

where \(d_o\) is the outer nozzle diameter, rather than the inner diameter, as in the case of bubble formation from wetted nozzles in water and organic liquids.

Although it was found that the bubble volume is proportional to \(d_o^{0.44}\) for small nozzle diameters, there appeared to be no discernible effect of \(d_o\) for larger orifices. The apparent transition occurred for nozzle diameters between 0.6 and 1.3 cm in the constant frequency regime.
It was also found\textsuperscript{66} that a gradual change from bubbling to jetting takes place in a transitional gas flow rate. The gas flow velocity at which the transition occurs was found to be independent of the orifice diameter, its value being very close to the sonic velocity. It is important to mention that the definition of jetting regime proposed by Mori and coworkers is somewhat different from the jetting regime described by Hoefele and Brimacombe\textsuperscript{31}.

The weak dependence of bubbling frequency on gas flow rate was also pointed out by Irons and Guthrie\textsuperscript{43,44}. They proposed an equation to correlate their results and those of Sano and Mori\textsuperscript{60} and Andreini et al.\textsuperscript{58}

\begin{equation}
V_b = \frac{\pi \sigma}{2 \rho_l g N_c} N_{\text{m}}^m + \left[\frac{\pi^2 \sigma^2}{4 \rho_l^2 g^2 N_c} N_{\text{m}}^m + \frac{\pi^2 K Q^2 d_z^2}{36 g} \right]^{\frac{1}{2}}
\end{equation}

(2.8)

The effect of the chamber volume can be conveniently represented in terms of a dimensionless capacitance group, which for non-wetting systems is defined as:

\begin{equation}
N_c = \frac{4 \rho_l g \sin \theta V_c}{\pi d_0 d_l p_b}
\end{equation}

(2.9)

the capacitance group has a pronounced effect on bubble volume, particularly under low gas flow rate conditions.
Equation (2.8) represents a combination of three separate experimental aspects governing bubbling behaviour in liquid metal systems. At low flow rates surface tension phenomena and chamber volume effects are dominant, i.e. the first two terms. At higher flow rates the inertial, or third term, corresponding to the constant frequency regime, plays the dominant role in determining bubble size.

Sahai and Guthrie\(^7\) developed a mathematical model to predict bubble shapes at their moment of release in water and molten iron. It was found that argon bubbles forming in molten iron are about three times larger than air bubbles formed in water for the same gas flow rates and orifice diameters.

A mathematical model was developed\(^7\) to describe the dynamics of bubble formation at the tuyeres of a copper converter. The effect of heat transfer to the bubble, chemical reaction between the bubble and the bath, and coflowing bath circulation were studied. Temperature, volume and rise velocity for the bubble were predicted as a function of time. The calculated bubble volumes showed discrepancies when compared with previous non-reactive, stagnant and isothermal bath bubble formation models. It was found that the heat transfer and bath circulation have a large influence on the bubble formation process. Heat transfer acts to decrease bubble frequency, while bath circulation has the opposite effect.
2.3 Pressure Fluctuations at the Tuyere

The use of nozzle pressure to characterize submerged injection regimes is now a fairly well established technique. A pressure trace technique has been used by Wraith and coworkers, Mori and coworkers, Hoefele and Brimacombe, Richards and Brimacombe, Farias and Robertson, Gray et al., and McNallan and King.

The theoretical basis for the link between tuyere pressure and bubble formation has been set out by Kupferberg and Jameson and Pinczewski for vertical injection. The pressure inside a bubble growing in an infinite, incompressible liquid is given by:

\[ P_b = P_h + \rho_1 \left[ R \frac{d^2 R}{dt^2} + \frac{3}{2} \left( \frac{dR}{dt} \right)^2 \right] + \frac{2 \sigma}{R} + \frac{4 \mu}{R} \frac{dR}{dt} + \frac{1}{2} \rho_g u_o^2 \cos^2 \theta \]  \hspace{1cm} (2.10)

Where the terms on the right-hand side represent, respectively, the pressure head, liquid inertia, surface tension, viscous contribution and gas momentum. The bubble pressure can be related to the chamber (tuyere system) pressure by:

\[ P_c = P_b + \rho_g K u_o^2 \]  \hspace{1cm} (2.11)
where $K$ is the orifice coefficient. At the commencement of bubble growth, $R$ is small, and $d^2R/dt^2$ and $dR/dt$ are large, (about 300-1000 m/s$^2$ and 2-5 m/s, respectively$^{79}$), so that the pressure inside the bubble and the tuyere is also very large from Equations (2.10) and (2.11). However, as bubble growth continues, $R$ increases, and $d^2R/dt^2$ and $dR/dt$ decrease resulting in a steady drop in pressure which takes longer than the rapid pressure rise. When the bubble becomes detached from the nozzle, the gas supply to it is severed, a new bubble grows and the cycle of change in $P_b$ is repeated. A regular cyclic variation in bubble pressure results if there is regular bubbling.

Mori and coworkers$^{60,62}$ determined the size of argon bubbles in mercury, liquid silver and molten iron by measuring the frequency of bubble formation. For very low frequencies the measurements were made by observing the bubbles arriving at the liquid surface. At higher frequencies the pressure pulses in the gas-supply train were detected with a crystal earphone. The output from the earphone was fed to a syncroscope. The bubble frequency was determined from the distance between two adjacent peaks. No studies were pursued on the shape and amplitude of the pulses.

Farias and Robertson$^{79}$ measured the dynamic pressure in nozzles in $\text{N}_2$-$\text{H}_2$-water systems. The pressures were simultane-
ously recorded in the bubble, in the liquid and in the supply line. At low gas flow rates a very strong pulse associated with initial growth was followed by a damped oscillation of pressure and an oscillating rate of growth during the early stages of bubble formation. For high flow rates, giving nominal Mach numbers up to 4, although the nozzle was nominally choked the flow rate instantaneously decreased at the start of bubble growth due to the high inertial resistance to flow and the corresponding high pressure at the nozzle exit. It was found that the best transducer location is in the supply line itself, even if away from the tuyere tip and that a general purpose transducer with a small dead volume is quite adequate.

Wraith and coworkers\textsuperscript{75,77} have performed detailed studies of the shape of the tuyere pressure pulses during submerged gas injection in water and mercury. At relatively low gas flow rates (26 - 1,270 cm\textsuperscript{3}/s) three principal bubbling processes were observed, which in order of increasing injection rate are labelled triplet formation, bynary coalescence and stem coalescence.\textsuperscript{75}

For high velocity gas injection (up to 0.03 m\textsuperscript{3}/s) different forms of bubble interaction may be distinguished.\textsuperscript{77} Bubbles forming in a sequence are described as discrete if a new bubble starts to grow only after the cut-off stage of the
previous bubble, as shown in Figure 2.2(a). The tuyere pressure is at its minimum at 'a' when the bubble starts to grow following the cut-off of a preceding gas pocket from the orifice. Immediately the pressure rises sharply and almost linearly. With increasing bubble expansion the pressure falls to 'j' which corresponds to bubble detachment. Subsequent formation of a gas neck linking the detached rising bubble to the orifice gives rise to a secondary nozzle pressure pulse 'j-m'. The point 'm' represents the eventual cut-off of the bubble from the orifice. Coalescence occurs when a new bubble starts to grow at the orifice while the previous bubble is between the stages of detachment and cut-off, as shown in Figure 2.2(b). In the jetting regime, a subsequent bubble begins to form at the orifice before the detachment stage of an on-going bubble formation, as shown in Figure 2.2(c). Following the gas pocket 'a-b', a submerged jet develops at the orifice. The pressure signals in the jetting regime lack the sharp peaks characteristic of bubble formation. This is a clear indication that the gas flows through the orifice as a jet column and not as individual bubbles.

In general the pressure fluctuations in high-velocity gas injection are composed of the basic signals of bubbling, coalescence and jetting. Mixed dispersions can be observed up to near-sonic injection velocities with bubbling on the decline in favour of jetting with increasing velocity.
Figure 2.2 Nozzle Pressure Fluctuation (from reference 77)
(a) Discrete Bubble Formation
(b) Bubble Coalescence
Figure 2.2 Nozzle Pressure Fluctuation (from reference 77)
(c) Orifice Jetting.
The pressure trace techniques was also employed by Hoefele and Brimacombe\textsuperscript{31} in the laboratory and in plant. The laboratory work involved the submerged injection of different gases into water, a zinc-chloride solution and a mercury bath. High speed cinematography and pressure measurements in the tuyere were carried out to study the gas discharge into the liquid. The observations and pressure trace for the injection of air into mercury under bubbling conditions are shown in Figure 2.3. As can be seen a smooth decrease in pressure at the tip corresponds to the growth of a bubble while the minimum pressure occurs when the bubble necks off and rises from the tuyere. The minimum is followed by a sharp rise in pressure as the bath flows in around the nozzle to replace the rising gas bubble. The drop in pressure results from the initiation and growth of the next bubble. On the other hand, the pressure oscillations under steady jet conditions were irregular with a smaller amplitude and could not be correlated with gas dynamics at the tuyere tip. Hoefele and Brimacombe also investigated injection dynamics in an operating nickel converter, after reline, by measuring pressure fluctuations in several tuyeres with piezoelectric transducers. The pressure traces recorded during blowing were characterized by distinct, regular pulses separated by intervals of relatively constant, low pressure as can be observed in Figure 2.4\textsuperscript{31,84}. 
Figure 2.3 Pressure Trace During Air Injection into Mercury. Note the Relationship between the Pressure and Stage of Bubble Growth. (from reference 31).
Figure 2.4 Pressure Trace During Air Injection into a Nickel Converter with a New Refractory Lining (from reference 31).
More recently Richards and Brimacombe measured the dynamic tuyere pressure in a slag zinc fuming furnace. A sequence of pressure traces obtained as the furnace was being filled is shown in Figure 2.5. In the empty furnace, Figure 2.5(a), the pressure trace is constant, at about 8 kPag. After 15 tonnes of liquid slag have been charged, Figure 2.5(b), which corresponds to a tuyere submergence of about 160 mm, the constant pressure signal is disturbed by pulses of short duration. With 39 tonnes charged (tuyere submergence of 530 mm), Figure 2.5(c), the pressure trace is characterized by pulses of longer duration which are not separated by lower pressure intervals. When the furnace is fully charged such that the tuyere submergence is about 680 mm, the pressure pulses are slightly more regular with a frequency of 5 to 6 s\(^{-1}\). Most pulses are characterized by a rapid rise in pressure followed by a slower decline. The average back pressure in the tuyere with the furnace full is about 33 kPag. Tests were performed with and without coal feeding to the tuyere; it was found that the pulse frequency was unaffected but the average tuyere pressure increased by about 4 kPa with coal feeding. Presumably this increase results from the greater pressure drop associated with blowing the air-coal mixture over the length of the tuyere.
Figure 2.5 Pressure Traces from the Slag Fuming Furnace (from Reference 86)
(a) Empty Furnace
(b) Tuyere Submergence: 160 mm
(c) Tuyere Submergence: 530 mm
(d) Tuyere Submergence: 680 mm
Vertical Scale: 10 kPa/div
Horizontal Scale: 200 ms/div
2.4 The Bubbling-Jetting Transition

The criterion to be used for defining the transition between bubbling and steady jetting regimes has been a matter of some controversy amongst the researchers working in this area. The different definitions depend on the experimental technique employed, the gas-liquid system under study, the orientation of the nozzle and finally on the objectives of the study under consideration.

Hoefele and Brimacombe\textsuperscript{31} studied the behaviour of gas discharging into a liquid in the laboratory and in plant. Two regimes of flow, bubbling and jetting, were delineated on a jetting behaviour diagram, as observed in Figure 2.6, based on the modified Froude number of the jet and the ratio of the density of the gas to that of the liquid. By interpretation of tuyere pressure traces and high speed films taken simultaneously in the laboratory they distinguished two flow regimes: a 'pulsing' or bubbling regime and a 'steady jetting' regime. They defined a steady jet as essentially one in which there is a gas continuously at the tip of the tuyere and bubbling as the condition in which the gas necks off at the tuyere tip such that periodically liquid washes against the tip. For systems with a low gas-to-liquid density ratio under-expanded flow in the tuyeres is a necessary condition for
Figure 2.6 Jet Behaviour Diagram (from reference 31).
steady jetting. This is not the case for systems with greater gas-to-liquid density ratio in which steady jetting can be achieved under conditions of fully expanded flow in the tuyere.

Mori and coworkers\textsuperscript{66,68,69} studied the injection of nitrogen into mercury and water. A high-speed camera was employed to measure the variation of the base diameter of the gas jet with time. At low gas flow rates they observed that the jets expanded immediately upon discharging, in a pulsating manner, forming bubbles the base diameter of which was much larger than the orifice diameter. They defined this phenomenon as bubbling. With increased gas flow rates they found that a continuous, non-pulsating jet of gas begins to form at the orifice. The base diameter of the gas jet and the orifice diameter coincide over various time ranges during which bubbles are not formed. Thus Mori and coworkers found that the bubbling-jetting transition begins when the gas-flow velocity at the exit of the orifice exceeds the sonic velocity irrespective of the differences in the physical properties of liquids or orifice diameter.

McNallan and King\textsuperscript{83} employed high speed cinematography and the pressure-trace technique to study the injection of gas vertically upward into water and liquid metals. Two flow regimes of jet behaviour were observed: one in which unstable bubbles were produced at the jet nozzle, and one in which a
steady cone of gas emerged from the nozzle. The transition between both regimes was controlled by the mass flow of gas per unit area of nozzle orifice. The steady jet flow regime was predominant in jets where the mass flow per unit area was greater than $40 \, \text{g cm}^{-2} \text{s}^{-1}$.

2.5 Accretion Formation and Tuyere Blockage

Although in use for over seventy years, the copper converter continues to be plagued with problems of tuyere blockage and low refractory life particularly at the tuyere line. The tuyere blockage is caused by the formation of accretions at the tuyere tip. Accretions fall broadly into three categories. Firstly they can be formed inside the pipe, close to the tuyere tip. Secondly they may be pipe shaped (knurdles or horns) with gas flow channels predominantly through the centre. Thirdly they may be porous (mushrooms) with gas flow channels spreading radially outwards from the tuyere exit. The first two categories seem to be the most common during copper converting. An example of a pipe-shaped accretion, taken from an operating Peirce-Smith copper converter is shown in Figure 2.7.
Figure 2.7  Pipe-Shaped Accretion from a Tuyere of a Converter from Horne Smelter.
Tuyere blockage and accretion formation have been the subjects of some studies, but relatively little work has been done directly on the copper converter. As was mentioned, Hoefele and Brimacombe\textsuperscript{31} investigated injection dynamics in a nickel converter. They concluded that for normal operation the converter works under bubbling conditions which is undesirable because between the formation of successive bubbles, liquid washes against the tuyere tip, contributing importantly to accretion build-up.

Another process in which the link between gas flow conditions and tuyere blockage has been established is gaseous deoxidation of blister copper in the anode furnace. In an industrial investigation Gray et al.\textsuperscript{80} also used a pressure transducer to sense fluctuations just upstream from the tuyeres of two anode furnaces at Mt. Isa Mines. The pressure measurements indicated that the gas was discharging as bubbles. In order to reduce pressure fluctuations in the tuyere and the number of tuyere blockages, critical flow orifices, designed to produce sonic velocity through the orifice, were installed upstream from each tuyere. No tuyere blockages occurred while critical flow conditions were maintained in the orifice.
Factors influencing accretion formation and tuyere blockage also have been investigated in the laboratory. Using a room-temperature model, Engh et al. observed the penetration of droplets into a submerged tuyere. The number of droplets propelled into the tuyere was inversely related to the velocity of the gas and was a minimum for horizontal nozzles. Tuyere blockage during the horizontal injection of nitrogen at low flows into liquid cast iron was studied by Davis and Magny. Cylinders of iron, consisting of successive layers like an onion, were found lining the injection tubes. The blockage was considered to be associated with the periodic collapse of bubbles at the tuyere tip which caused droplets of iron to be propelled into the tube. The 'onion skin' appearance of the accretions also suggests that the liquid metal may penetrate as a thin layer around the perimeter of the nozzle as has been observed in the tuyeres of the Bessemer process.

Wood, Schoeberle and Pugh investigated the desulphurization of blast furnace irons in torpedo ladles using pneumatic injection through a lance. Two types of blockage were found to occur. The first was characterized by a laminated build up of hot metal which extended a short distance into the lance. A second type was found to exist only at the nose of the lance and consisted of small metal droplets which built up until they covered the whole of the bore of the lance.
Most recently Boxall et al.\textsuperscript{109} reported a simple experimental technique to simulate knurdle formation and growth by injecting cooled gas into a transparent liquid such as water. Xu et al.\textsuperscript{110,111} also developed an aqueous model to study the formation of ice accretions by injecting supercooled helium into a bath of water. The accretion growth rate was found to be proportional to time, gas flowrate and gas 'supercool' temperature and inversely proportional to liquid 'superheat' temperature.

Accretion formation in both nonferrous and ferrous injection processes also has been investigated mathematically. Using the relaxation technique, Krivsky and Schuhmann\textsuperscript{112} predicted the temperature distribution in the refractory wall surrounding the tuyere pipe in a copper converter. They suggested that the cooling effect of the gas flow in the tuyere was the cause of accretions. Boxall et al.\textsuperscript{109} developed a mathematical model to predict the features of knurdles in their physical model and to allow the relevance of the model studies to real injection processes to be assessed. Preliminary results showed agreement between both models. Sahai and Guthrie\textsuperscript{113} studied some pertinent factors governing the formation and growth of metallic mushrooms during shrouded gas injection operations in steelmaking. They carried out a thermal analysis of the accretion growth, in which the heat
removed by the protective gas equals the heat supplied by the hot metal and the heat required for the solidification of the metal. They found that 60% of the maximum cooling capabilities of the shrouded gases are available for freezing a protective accretion around a tuyere.

Ohguchi and Robertson carried out calculations to estimate the conditions under which pipe-shaped accretions occur in steelmaking furnaces by considering heat transfer both in the refractory around the tuyere and in the molten metal. They also developed a porous-accretion model for heat transfer and accretion formation around a tuyere in order to calculate shapes of mushroom-like accretions. For the pipe-shaped accretions it was found that the thermal conductivity of refractory affects the bottom radius of the accretion, but does not affect its height. Accretion height has a maximum at a particular value of the heat-transfer coefficient between accretion and gas. The accretion grows in the form of a narrow tube increasing in height and rapidly reaches its maximum height; the pipe-shaped accretion then increases in radius, slowly reaching its steady state shape. During accretion growth, the temperature distribution in the refractory changes significantly due to the cooling caused by the gas flow. This leads to considerable thermal stresses in the refractory. For both types of accretion a decrease of superheat or an increase
of gas flow rate increases the size of the accretion. It should be noted that in the ferrous injection processes accretion formation does not lead to tuyere blockage because the gas pressures employed are high; nonetheless, accretion build up around the tuyere exit is important because it locally protects the refractory and extends lining life.

2.6 Bath Surface Movement. Splashing and Slopping

The submerged injection of gas into the bath of the copper converter imparts motion to the liquid which, depending on conditions, may take the form of slopping and/or splashing. Bath slopping is an oscillatory motion of the liquid between the tuyere line and the breast of the converter (presumably the oscillation also could take place from end to end). Bath splashing is the phenomenon in which droplets of matte and slag are torn off the surface layers and ejected into the reactor atmosphere. Both phenomena are undesirable for several reasons: the ejection of bath from the converter and the build up of accretions at the mouth are accelerated and refractory erosion may increase due to thermal shock and wear.

Past practice has endeavoured to control bath behaviour by employing tuyere air flow configurations that are optimum
according to operating experience. This has resulted in limited gas flow rates and hence, limited converter productivity. Studies of copper converter operating data have yielded few patterns; one of note is shown in Figure 2.8, where total air flow exhibits a linear dependence on converter volume. Few investigation of the fluid dynamics during gas injection have been reported, and descriptions of bath movement have been largely qualitative. In most of the studies conducted thus far, emphasis has centered on superficial injection from lances during steelmaking.

In 1948 Kootz and Gille studied converter shapes and blowing conditions for the production of air-refined steel low in nitrogen. During their search for links between blowing conditions and nitrogen in steel they found that the decisive factor is not the depth of the bath, but the relationship between the depth of the bath above the tuyeres and the bath volume. In order to conduct experiments on a large scale they constructed a special converter, strikingly similar to a Peirce-Smith converter, which enabled the bath depth over the tuyeres to be easily changed. The horizontal converter had a length of 5 m, an internal diameter of 2.5 m and held 5 to 10 tonne pig iron. Thus it was found that the blowing properties of the converter were exceptionally bad, especially at greater depths of bath. In order to avoid excessive slopping and
Figure 2.8 Air Injection Rate into a Copper Converter as a Function of Converter Volume. (Reprinted with the permission from 'Copper and Nickel Converters', edited by R.E. Johnson, The Metallurgical Society of AIME, 420 Commonwealth Drive, Warrendale, PA 15086, USA, 1979).
splashing Kootz and Gille pointed out that it would be necessary to work with pressures of not more than 1 atm. They also indicated that the outbursts of slopping and splashing possibly occurred in cycles, associated with the turbulence of the bath of iron in the converter.

To investigate the turbulence phenomena in the bath, Kootz and Gille constructed a vessel with glass end walls, filled with water, to simulate the horizontal converter. They noticed that during blowing the bath becomes strongly agitated at almost every position of the vessel. Two fundamentally different types of bath movements were observed: one during which the whole bath is turbulent, and another during which standing waves are formed. Each type of motion started always at the same angle of immersion of the tuyeres, independently of the degree of filling of the vessel and the volume of air. The bath remained free from strong agitation only when the tuyere line was submerged at a low angle, not exceeding $\pi/12$ rad ($15^\circ$). For immersion angles of $\pi/12$ to $\pi/7$ rad ($15$ to $25^\circ$) a very rapid movement sets in with a low amplitude, too small to give rise to slopping or splashing. When the tuyere line is allowed to be immersed in the range $\pi/7$ to $5\pi/16$ rad ($25$ to $50^\circ$) a slower but considerably stronger bath motion of standing waves takes place, which would lead to slopping and splashing occurring in the large converter. The
nature of the movement may be clearly recognized; in the middle and on both sides of the vessel wave crests and wave throughs alternate with each other, and two nodal points can be observed. If the tuyere line is turned deeper than $5\pi/16$ ($50^\circ$) then the slowest and strongest bath motion appears, whereby the whole bath moves to and fro. This type of bath motion is indeed the most significant for converter slopping and splashing.

In an attempt to reduce splashing in an oxygen-blown open-hearth furnace Li$^{117}$ studied the effect of lance angle, lance height, and jet flow rate on splashing. To this end he constructed a simple model of a fluidized air-water system and an impinging jet. Splash patterns were collected by a piece of filter paper exposed for a given length of time to the splashing. The results indicated that a foam layer present on the top of the liquid plays an important role, producing a damping effect on splashing. This may indicate that the conditions of slag may likewise affect splashing in an actual furnace. Lancing (that is, injection of air with the lance submerged in the bath) generally produces less splashing than jetting (with the lance above the surface). A sharp reduction in splashing occurs as the lance is lowered to touch the bath surface.
Holmes and Thring\textsuperscript{118} put forward three theories describing the mechanism of splashing in the top-blown oxygen converter. The first is that splashing is completely physical and consequently the amount of splashing will depend on the jet momentum. The second is that oxygen is absorbed into the steel and carbon monoxide is then formed explosively causing the splash. The last proposed mechanism is a compromise and claims that splashing is partly physical and partly chemical, the oxygen combining with carbon on the surface. They made some suggestions on how to modify certain similarity criteria to account for any excess splashing taking place owing to chemical action.

Chatterjee and Bradshaw\textsuperscript{119} studied the variables affecting the onset of splashing when a subsonic jet is directed vertically on the surface of various liquids. Splash patterns were obtained by allowing water droplets containing a dye to impinge on a paper disk held above the liquid surface. The volume of liquid torn from the surface was measured by collecting the splashed liquid in a tray situated above the liquid surface. They concluded that for any liquid exposed to a gas jet impinging at its surface, splashing commences at a critical depth of depression of the bath. This critical depth almost solely depends on the liquid properties. An experimental relationship was obtained to predict the critical depth of
depression when the liquid properties are known. The onset of splashing was also related to jet parameters, such as jet momentum and lance height. An increase in the jet momentum or a decrease in the lance height causes the amount of splashing to rise to a maximum value. Further changes caused a decrease in the volume of liquid splashed.

A study to evaluate splashing in the nitrogen-water system during submerged injection was performed by Igwe et al. Two modes of injection were considered, the horizontal nozzle and the submerged lance system involving one or more jets. High speed photographs were taken to qualitatively compare the splash severity under different conditions. At identical gas flow rates, the splashing from the top submerged lance is far less severe and more evenly distributed over the bath surface than the observed in horizontal nozzle injection. The vessel rocked vigorously during side injection, a situation which did not occur in the multiple-orifice top submerged injection. During top-submerged injection it was also observed that as the number of nozzles is increased from one to four, the splash severity at a given total gas flow rate decreases. A slight worsening of the splash was observed as the depth of nozzle submergence increased.
In an air-water model study of bottom-blown converters Etienne\textsuperscript{121} distinguished two types of factors which may influence splashing: geometric factors (design of the bottom, volume of the bath) and physicochemical factors (heating up of the gas, chemical reactions). Two types of models were investigated: an asymmetrical converter without tapping hole and tuyeres distributed in a half bottom, and a model of a symmetrical converter with tapping hole. Quantitative assessment of the amount of splashing was obtained by collecting the water splashes on thick absorbing papers hanging inside the converter. For the asymmetrical converters Etienne found that a calm blow is linked to an intense foaming of the bath. As the number of tuyeres or bath depth decrease the blow becomes rougher, tearing bundles of water away from the bath surface. An increase in the air flow rate enhances the amount of splashing but does not alter the type of behaviour of the bath. For symmetrical converters the motion of the bath depended markedly on its depth and the number of tuyeres. When less than 10 tuyeres are employed heavy splashing is observed, which increases as the bath depth decreases. Blowing with more than 14 tuyeres produces a calm bath even with shallow bath depth. In this case the bath exhibits a tendency to oscillate. The amplitude of these oscillations increases with bath depth.
Model studies on the bath motion and the amount of splashing produced in a sideblown vertical converter at high gas flow rates were performed by Ericsson. A quantitative assessment of the amount of splashing produced was obtained by collecting the water splashes in a ring-formed reservoir placed in the upper part of the vessel. It was found that a number of factors influence bath movement. The projections may originate from mass oscillations that take place in the bath, generally as rotating waves. Another mechanism causing splashing at the greatest air flow rate is due to gas bubbles crossing the bath surface at high speed tearing bundles of water away from the surface. Splashing is increased markedly with increasing gas flow rates. When the tuyeres are inclined directed towards the surface the splashing seems to increase exponentially with gas flow rate. If the tuyeres are directed horizontally or downwards in the bath the importance of the gas flow rate decreases. An increase in bath depth increases splashing, this effect grows with increasing gas flow rates. An increase in tuyere diameter results in less splashing, especially when the tuyeres are directed upwards. In the latter case the amount of splashing even decreases at certain gas flow rates and tuyere diameter.
Robertson and Sabharwal\textsuperscript{105}, working with a water model of a transfer ladle into which air was injected by means of a scaled down lance identified two types of splashing. A 'primary splashing' is caused directly by bubbles arriving at the liquid surface while 'secondary splashing' is a washing effect round the containing walls. Primary splashing will result in accretion formation in the extraction hood over the ladle, while secondary splashing, if vigorous, will result in massive ejections onto the floor. Primary splashing was closely associated with the size of the gas bubbles arriving at the liquid surface in the water model. Secondary splashing is dependent on the interaction between the waves set up on the surface and the walls of the containing vessel, and also depends on the damping produced by the slag/metal medium. Robertson and Sabharwal adopted an ingenious electrical method to quantify primary splashing. A wire mesh was suspended horizontally around the injecting lance, so as to cover most of the liquid surface. An electrical circuit was made each time a splash impinged on the mesh, and was recorded as peaks on a chart recorder. For the assessment of secondary splashing the circuit was completed when splashes made contact with wire rings attached round the circumference of the vessel. Unfortunately due to possible commercial implications Robertson and Sabharwal did not discuss in detail the performance of specific lance designs in relation to splashing.
2.7 Summary

Little is known about the fluid dynamics of submerged injection under actual metallurgical conditions. Although the proposed models of submerged gas jets compare well with air/water experiments, they fail to predict realistically the behaviour of air injected into more dense mercury and other liquid metals. It is clear that the relationship between inertial and buoyancy forces has a great effect on the submerged gas injection process. Also the physical properties of both the liquid and the injected gas are important to define jet cone angle, forward and backward penetration and the shape of the jet.

There are two main flow regimes depending on the driving pressure of the gas. Three different ways have been proposed to define the bubbling-jetting transition. All of them are almost identical when applied to systems having a low gas-to-liquid density ratio, eg. gas-mercury and air-copper mattes. In this case the transition occurs for underexpanded flow conditions. For the case of aqueous systems Mori and coworkers and McNallan and King define the transition at sonic or near sonic velocities of the gas in the orifice, well above the transitional range reported by Hoefele and Brimacombe. Which of the definitions one adopts may depend largely on the
objective of the study under consideration. To investigate how injection conditions could affect nozzle blockage, the continuous-gas criterion may be more appropriate. But if one were examining other aspects of jet behaviour, the no-pulsation criterion may be better. Nevertheless whichever the definition to be adopted it is clear from the operational point of view that the jetting regime is the best to avoid tuyere blockage and to obtain high mass transfer rates and lower refractory wear.

The published models on bubble formation predict with sufficient accuracy the bubble volume for gas injection into liquid metals under low gas flow rates and non-reactive conditions. The relationship between bubble volume and gas flow rate defines two different bubbling regimes, a constant bubble volume regime for very low gas flow rates, and a constant frequency regime for higher flow rates. Nevertheless, when applied to a real industrial process, the models show large discrepancies with measured frequencies and bubble volumes. It seems that heat transfer to the bubble and bath circulation have a major effect on the bubble formation process under actual conditions.

The measurement of tuyere pressure to characterize submerged injection regimes is now a fairly well established
technique, at least when studying tuyere processes. By using this method the bubbling-jetting transition can be studied in detail.

Tuyere blockage and accretion formation have been the subjects of some studies, but relatively little work has been done directly on the copper converter. It is clear that the non ferrous industry has many lessons to learn from its ferrous sister with respect to control of accretion growth for refractory protection. The high pressure injection of air in the copper converter appears as the most interesting development in this area. The mathematical models developed to grow accretions of different shapes work well under steady state, single tuyere, stagnant bath conditions. All the models coincide in that a decrease of superheat or an increase in gas flow rate increase the accretion formation process.

Few investigations about bath movement during injection have been reported, and most of them have been largely qualitatively with emphasis centered on superficial injection from lances during steelmaking. Slopping and splashing have been studied mainly from a physical-mechanical point of view, with almost no emphasis on the effect of gas expansion, and chemical reactions in the converter. The techniques employed to measure splashing are rather crude, while no quantitative technique at all as been proposed to study slopping.
CHAPTER III

OBJECTIVES

Numerous thermodynamic investigations of the chemical reactions in copper converting have been conducted over the years. Thus much is known of the equilibria amongst molten copper, slag and matte phases, and the influence of oxygen potential on copper losses in slag.

In contrast to this state of basic knowledge of equilibria, remarkably little is understood of the rate phenomena in the copper converter, particularly as they are influenced by the design and operation of the reactor. Aspects such as gas discharge dynamics, bath movement within the converter, splashing and heat transfer have only begun to be tackled in a concerted manner. Thus the thrust of this work was to investigate the following process engineering aspects of a converter operation: gas injection into the bath, accretion growth at the tuyere tip, slopping of the bath and heat losses from the interior of the converter during out-of-stack periods. The study of these subjects involved plant measurements, a physical laboratory model and the formulation of a mathematical model.
The main objectives expected to be fulfilled in the course of this work were the following:

a) To study the effect of different injection parameters on bath motion, tuyere injection dynamics and slopping. Observations employing high-speed cinematography of the tuyere region and the bath surface were to be performed in the physical model. The observed events were to be related to measurements of the dynamic gas pressure in the tuyeres.

b) To study some characteristic features of converting under normal blowing conditions and to compare the converting operation and injection characteristics of the different reactors employed in industry (Peirce-Smith, Hoboken and Inspiration converters).

c) To observe accretion build-up while a tuyere is operating. The task was accomplished with the aid of a 'tuyerescope' that facilitated direct observation, by eye or with cameras, of the dynamics of accretion growth at the tuyere tip.

d) To gain knowledge on the effect of variables like bath properties, tuyere submergence, interaction of adjacent tuyeres, and state of the refractories at the tuyere line on injection dynamics.
e) To assess the relative importance of magnetite formation and freezing in the accretion growth process by taking samples of the accretion during operation.

f) To link bath movement and events at the tuyere tip in order to minimize splashing and slopping in the copper converter. Thus, an attempt was to be made to study the influence of injection variables (gas flow rate, tuyere submergence) on bath movement, splashing and slopping.

g) To quantify the influence of out-of-stack time and other converter variables on the temperature distribution in the refractory wall, especially at the tuyere line by means of a mathematical heat transfer model.

h) Finally on the basis of the results from laboratory, industrial and computing work, to make suggestions that may lead to improvements in present gas injection practice in order to increase tuyere and refractory life, and to decrease slopping and accretion build-up.
CHAPTER IV

EXPERIMENTAL TECHNIQUES

As discussed earlier, it was of primary importance to obtain enough information to develop a definite set of operating criteria for copper converters. To accomplish this objective laboratory experiments and industrial tests in four copper smelters were carried out, together with heat transfer calculations. The following sections describe the experimental techniques applied to the acquisition of industrial data and the laboratory work involved.

4.1 Laboratory Experimental Work

The laboratory work was carried out to examine the relation between certain injection parameters and bath behaviour. The high temperature system was simulated by means of a sectional 1/4 scale model of a copper converter. The isothermal model was considered to be the best means to simulate some aspects of the overall bath movement.

4.1.1 The Isothermal Model

A major difficulty encountered in the physical modelling of a gas-liquid metal system lies in the number of physical
parameters involved in the process. It has been pointed out that a complete simulation of a process can be obtained by using 24 dimensionless groups, which evidently is impossible to accomplish from a practical point of view.

To attain fluid flow similitude between two gas-liquid systems, four conditions must be met: dynamic similarity, geometric similarity, kinematic similarity and thermal similarity. To meet thermal similarity requirements, the dimensionless numbers involving heat transfer or convective flow have to be equal in both systems. Thermal similarity appears to be unimportant in modelling copper converting processes since convective forces due to the thermal gradients are considered small relative to other forces acting on the system. The only important thermal effect to be considered is the expansion of the bubble due to heat transfer from the liquid metal to the gas phase. Kinematic similarity is ensured in a model that conforms to dynamic and geometric similarity.

The principal forces to be considered in obtaining dynamic similarity in the converting system are inertial, buoyancy (or gravitational), viscous, and surface tension forces. As indicated by Equation 2-8, the surface tension phenomena and ante-chamber volumes dominate at low flow rates. At higher flow rates, the inertial term plays the dominant
role in determining bubble sizes. Thus two dimensionless parameters which can be used in the modelling process are the modified Froude number, which relates inertial and buoyancy forces, and the Reynolds number, relating inertial and viscous forces.

In the investigation of gas-liquid metal systems, water is usually employed as the modelling liquid because it is easy to handle, readily available, facilitates flow tracing, and its kinematic viscosity is close to that of liquid copper or copper mattes. Table 4.1 compares some physical properties of several fluids. However using water as the modelling liquid for copper mattes, it is impossible to satisfy the two above mentioned dimensionless numbers simultaneously. The partial solution for such a problem lies in knowledge of the behaviour of the systems. It is known from a considerable body of experience in the flow of fluids that the Reynolds number becomes relatively unimportant once fully turbulent flow, or a high value of Reynolds number, has been attained. In model experiments designed to simulate the bath motion in steel-making converters, useful results have been obtained by modelling to obtain similarity with respect to Froude number rather than to Reynolds number.
TABLE 4.1

PHYSICAL PROPERTIES OF SOME FLUIDS

<table>
<thead>
<tr>
<th></th>
<th>DENSITY</th>
<th>VISCOSITY</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>kg m⁻³</td>
<td>N s m⁻²</td>
</tr>
<tr>
<td>Helium (20 C)</td>
<td>0.177</td>
<td>1.9 x 10⁻⁵</td>
</tr>
<tr>
<td>Air (20 C)</td>
<td>1.293</td>
<td>1.8 x 10⁻⁵</td>
</tr>
<tr>
<td>Air-He mixture (88% He)</td>
<td>0.315</td>
<td>1.9 x 10⁻⁵</td>
</tr>
<tr>
<td>Air-He mixture (91% He)</td>
<td>0.281</td>
<td>1.9 x 10⁻⁵</td>
</tr>
<tr>
<td>Air-He mixture (94% He)</td>
<td>0.248</td>
<td>1.9 x 10⁻⁵</td>
</tr>
<tr>
<td>Water (20 C)</td>
<td>1000</td>
<td>1.0 x 10⁻³</td>
</tr>
<tr>
<td>Blister Copper (1200 C)</td>
<td>7800</td>
<td>3.3 x 10⁻³</td>
</tr>
<tr>
<td>Matte 30% Cu (1200 C)</td>
<td>4100</td>
<td>10.0 x 10⁻³</td>
</tr>
<tr>
<td>Matte 50% Cu (1200 C)</td>
<td>4600</td>
<td>10.0 x 10⁻³</td>
</tr>
<tr>
<td>Matte 80% Cu (1200 C)</td>
<td>5200</td>
<td>10.0 x 10⁻³</td>
</tr>
</tbody>
</table>
To simulate some fluid dynamic aspects of a copper converter by means of an isothermal model, three characteristics of the process may be considered. The effect of the relationship between inertial and buoyancy forces has a great influence on the dynamics of gas injection\textsuperscript{26,31,56}. Also during gas injection into a liquid, two regimes of flow can be distinguished, depending on both the modified Froude number of the system and the gas-to-liquid density ratio\textsuperscript{31}. Therefore, similarity based on the modified Froude number criterion appears to be essential to simulate the process. Also the gas-to-liquid density ratio in the prototype and the model should be the same. Then the conditions of an air-matte system may be simulated by injecting a helium-air mixture into water.

It has been suggested\textsuperscript{30} that the refractory wear observed in the area close to the tuyeres in a copper converter may be due to the action of the rising bubbles. It was also indicated\textsuperscript{31} that the bubbles forming at adjacent tuyeres overlap considerably; this interaction explains the uniformity of refractory wear observed along the back wall above the tuyere line. Therefore, to simulate the fluid dynamic contribution to the refractory wear in a copper converter, the ratio between the bubble diameter at the tuyere tip and the distance between adjacent tuyeres should be the same in both the prototype and the model.
The above mentioned factors, coupled to geometric similarity with a selected scale factor, provide a useful frame of reference to develop relations to design an isothermal model of a copper converter.

From the similarity of modified Froude numbers:

\[ \left[ \frac{\rho_g}{\rho_1 - \rho_g} \frac{u_0^2}{d_0 g} \right]_P = \left[ \frac{\rho_g}{\rho_1 - \rho_g} \frac{u_0^2}{d_0 g} \right]_M \]  \hspace{1cm} (4.1)

If the gas-to-liquid density ratio is the same in both the prototype and the model:

\[ \left[ \frac{\rho_g}{\rho_1} \right]_P = \left[ \frac{\rho_g}{\rho_1} \right]_M \]  \hspace{1cm} (4.2)

but, as \( \frac{\rho_g}{\rho_1} \ll 1 \)  \hspace{1cm} Eqs. (4.1) and (4.2) give:

\[ u_{o,M} = u_{o,P} \left( \frac{d_{o,M}}{d_{o,P}} \right)^{\frac{1}{2}} \]  \hspace{1cm} (4.3)

As was mentioned, the ratio between bubble diameter at the tuyere tip and distance between adjacent tuyeres provides another similarity criterion:

\[ \left[ \frac{d_b}{e} \right]_P = \left[ \frac{d_b}{e} \right]_M \]  \hspace{1cm} (4.4)
The bubble diameter at the tuyere tip in the prototype can be given a value from the model developed by Ashman et al.\textsuperscript{74}, the industrial values obtained by Hoefele\textsuperscript{84}, and the industrial measurements to be carried out during the present work.

All the bubble formation models in water-like liquids have been developed assuming stagnant bath conditions and have been tested under experimental conditions involving one tuyere only. It seems unlikely that the bubble diameter in the turbulent, multiple-tuyere water model could be described by Equation (2.1); then the bubble volume in the model has to be defined from dynamic pressure measurements. The bubbling frequency to be measured is related to the gas flow rate as:

\[
V_{b,M} = \frac{Q_M}{f_M}
\]  

or, in terms of the bubble diameter, the tuyere gas velocity and the tuyere diameter:

\[
d_{b,M} = \left[\frac{3}{2} \frac{d_{o,M}^2 u_{o,M}}{f_M} \right]^{1/3}
\]

Then, from Equations (4.3), (4.4) and (4.6) the tuyere diameter to be used in the model is defined by:

\[
d_{o,M} = \left[\frac{2}{3} \frac{f_M}{u_{o,P}}\right]^{2/5} \left[\frac{e_M}{e_P}\right]^{6/5} d_{o,P}^{1/5} d_{b,P}^{6/5}
\]
In Eq. (4.7) the tuyere diameter in the model is expressed as a function of the conditions existing in the prototype, the specific scale ratio to be used, and the bubbling frequency in the model.

The tuyere submergence in the model can be defined by using the ratio between the bubble diameter at the tuyere tip and the tuyere submergence as an additional similarity criterion:

\[
\left[ \frac{d_b}{H} \right]_M = \left[ \frac{d_b}{H} \right]_P \quad (4.8)
\]

Then, from Eqs. (4.3), (4.6) and (4.8) the tuyere submergence in the model can be expressed as:

\[
H_M = \left[ \frac{3}{2} \cdot \frac{u_{o,P}}{f_M d_{o,P}} \right]^{1/3} \cdot \frac{H_P}{d_{b,P}} \cdot d_{o,M}^{5/6} \quad (4.9)
\]

As was mentioned the gas-to-liquid density ratio should be the same in both the prototype and the model; then the air-matte system can be simulated by injecting helium into water. Unfortunately economic considerations owing to consumption of a large amount of gas in the laboratory work prevented the use of helium as the injected gas. Instead it was decided to inject air through the tuyeres of the isothermal water model.
If an air-water system is used to simulate an air-matte system, Eq. (4.2) cannot be used, but the condition \( \rho_g \ll \rho_l \) is still valid. Then Eq. (4.1) becomes:

\[
U_{o,M} = U_{o,P} \left( \frac{d_{o,M}}{d_{o,P}} \frac{\rho_{g,P}}{\rho_{g,M}} \frac{\rho_{l,M}}{\rho_{l,P}} \right)^{1/2}
\]  

(4.10)

Now, from Eqs. (4.10), (4.4) and (4.6) the tuyere diameter in the model is:

\[
d_{o,M} = \left[ \frac{2}{3} \frac{f_M}{u_{o,P}} \right]^{2/5} \left[ e_M \frac{e_P}{e_P} \right]^{6/5} \left[ \frac{d_{o,P}}{d_{o,P}} \frac{\rho_{g,M}}{\rho_{g,P}} \frac{\rho_{l,M}}{\rho_{l,P}} \right]^{1/5} \frac{d_b}{d_P}
\]  

(4.11)

In the same way the tuyere submergence in the model can be expressed in terms of Eqs. (4.10), (4.6) and (4.8):

\[
H_M = \left[ \frac{3}{2} \frac{u_{o,P}}{f_M d_{o,P}} \right]^{1/3} \left[ \frac{H_P}{d_b} \right]^{1/6} \left[ \frac{\rho_{g,P}}{\rho_{g,M}} \frac{\rho_{l,M}}{\rho_{l,P}} \right] \frac{d_{o,M}}{d_{o,P}}
\]  

(4.12)

4.1.2 Experimental Apparatus

The apparatus employed in the laboratory experiments is illustrated schematically in Figure 4.1. It consisted of a converter-shaped vessel, a gas-delivery system to supply air to the tuyeres and a fast-response piezoelectric transducer.
1. Compressor
2. Globe Valve
3. Pressure Gage
4. Plate Orifice
5. Tuyere Manifold
6. Tuyere
7. Plexiglas Converter
8. Pressure Transducer
9. Signal Amplifier
10. FM Tape Recorder
11. Storage Oscilloscope
12. High-Speed Camera

Figure 4.1 Schematic of the Laboratory Apparatus.
coupled to a four-channel FM tape recorder and to a storage oscilloscope to measure and record the pressure along the tuyere. A high-speed camera to film events taking place in the tuyere region was also employed.

4.1.2.1 Converter-Shaped Vessel

To obtain reliable information about the process, it was decided to build a 1/4 scale, sectional Plexiglas model of a 13 x 30 Peirce-Smith copper converter. A smaller model could not be used because the tuyere diameter would be less than 10mm, and under these conditions the surface tension effect would become important with respect to buoyancy and inertial forces. A section containing five tuyeres was constructed.

In North America there are seventeen plants using sixty-one 13 x 30 Peirce-Smith converters. From the data shown in Table 4.2 an 'average' converter can be defined, as one having 44 tuyeres with a diameter of 49mm. The average gas blowing rate and blowing pressure are 9.1 Nm³/s and 9.13 x 10⁴ Pa, respectively. Table 4.3 shows other characteristics of the average converter and three plants which are of interest.
### Table 4.2

**Details of Copper Converter Practice in North America**

<table>
<thead>
<tr>
<th>PLANT</th>
<th>13' x 30' Converters</th>
<th>Number of Tuyeres</th>
<th>Tuyere Diameter, mm</th>
<th>Blowing Rate m³/s STP</th>
<th>Blowing Pressure Pa x 10⁻⁴</th>
<th>Tuyere Submergence, mm</th>
</tr>
</thead>
<tbody>
<tr>
<td>Falconbridge</td>
<td>4</td>
<td>-</td>
<td>-</td>
<td>8.5</td>
<td>-</td>
<td>381</td>
</tr>
<tr>
<td>Gaspe</td>
<td>2</td>
<td>50</td>
<td>48</td>
<td>10.4</td>
<td>10.4</td>
<td>922</td>
</tr>
<tr>
<td>Hudson Bay</td>
<td>3</td>
<td>39*</td>
<td>50</td>
<td>8.5</td>
<td>9.7</td>
<td>762</td>
</tr>
<tr>
<td>Noranda</td>
<td>4</td>
<td>48</td>
<td>48</td>
<td>11.8</td>
<td>10.4</td>
<td>1041</td>
</tr>
<tr>
<td>Ajo</td>
<td>3</td>
<td>52</td>
<td>43</td>
<td>7.8</td>
<td>8.3</td>
<td>558</td>
</tr>
<tr>
<td>Douglas</td>
<td>5</td>
<td>47</td>
<td>43</td>
<td>7.8</td>
<td>9.7</td>
<td>304</td>
</tr>
<tr>
<td>Morenci</td>
<td>9</td>
<td>37*</td>
<td>59*</td>
<td>11.3</td>
<td>9.0</td>
<td>609</td>
</tr>
<tr>
<td>Hidalgo</td>
<td>3</td>
<td>52</td>
<td>48</td>
<td>8.5</td>
<td>6.9</td>
<td>457</td>
</tr>
<tr>
<td>White Pine</td>
<td>2</td>
<td>42</td>
<td>48</td>
<td>8.5</td>
<td>9.7</td>
<td>215</td>
</tr>
<tr>
<td>Ray</td>
<td>3</td>
<td>42</td>
<td>50</td>
<td>9.7</td>
<td>10.3</td>
<td>406</td>
</tr>
<tr>
<td>Nevada</td>
<td>1</td>
<td>43</td>
<td>50</td>
<td>9.0</td>
<td>9.7</td>
<td>457</td>
</tr>
<tr>
<td>Chino</td>
<td>4</td>
<td>48</td>
<td>48</td>
<td>9.0</td>
<td>9.7</td>
<td>393</td>
</tr>
<tr>
<td>Utah</td>
<td>8</td>
<td>48*</td>
<td>50</td>
<td>8.5</td>
<td>10.3</td>
<td>533</td>
</tr>
<tr>
<td>Anaconda</td>
<td>2</td>
<td>48</td>
<td>43</td>
<td>10.2</td>
<td>11.0</td>
<td>584</td>
</tr>
<tr>
<td>El Paso</td>
<td>3</td>
<td>40</td>
<td>43</td>
<td>8.5</td>
<td>10.3</td>
<td>533</td>
</tr>
<tr>
<td>Hayden</td>
<td>1</td>
<td>49</td>
<td>43</td>
<td>11.3</td>
<td>10.3</td>
<td>508</td>
</tr>
<tr>
<td>Tacoma</td>
<td>3</td>
<td>42</td>
<td>40</td>
<td>8.3</td>
<td>10.3</td>
<td>457</td>
</tr>
</tbody>
</table>

* Averaged value.
### TABLE 4-3

**CHARACTERISTICS OF THE AVERAGE 13 x 30 CONVERTER AND THREE OTHER PLANTS**

<table>
<thead>
<tr>
<th></th>
<th>Average</th>
<th>Tacoma</th>
<th>Noranda</th>
<th>Utah</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Number of tuyeres</strong></td>
<td>44</td>
<td>42</td>
<td>48</td>
<td>48</td>
</tr>
<tr>
<td><strong>Tuyere diameter, mm</strong></td>
<td>49</td>
<td>41</td>
<td>48</td>
<td>50</td>
</tr>
<tr>
<td><strong>Tuyere submergence, mm</strong></td>
<td>540</td>
<td>457</td>
<td>1041</td>
<td>534</td>
</tr>
<tr>
<td><strong>Gas flow rate, Nm³/s</strong></td>
<td>9.1</td>
<td>8.3</td>
<td>11.8</td>
<td>8.5</td>
</tr>
<tr>
<td><strong>Tuyere gas velocity, m/s</strong></td>
<td>109.7</td>
<td>149.7</td>
<td>135.9</td>
<td>87.4</td>
</tr>
<tr>
<td><strong>Tuyere spacing, mm</strong></td>
<td>195</td>
<td>205</td>
<td>179</td>
<td>179</td>
</tr>
<tr>
<td><strong>Modified Froude number (1)</strong></td>
<td>7.0</td>
<td>15.7</td>
<td>11.0</td>
<td>4.3</td>
</tr>
<tr>
<td><strong>Reynolds number (1)</strong></td>
<td>$3.9 \times 10^5$</td>
<td>$4.4 \times 10^5$</td>
<td>$4.7 \times 10^5$</td>
<td>$3.2 \times 10^5$</td>
</tr>
<tr>
<td><strong>Converter bubble volume, m³ (2)</strong></td>
<td>$3.4 \times 10^{-2}$</td>
<td>$3.2 \times 10^{-2}$</td>
<td>$4.3 \times 10^{-2}$</td>
<td>$2.6 \times 10^{-2}$</td>
</tr>
<tr>
<td><strong>Bubble diameter @ tuyere, mm (2)</strong></td>
<td>402</td>
<td>394</td>
<td>435</td>
<td>368</td>
</tr>
<tr>
<td><strong>Bubble diameter</strong></td>
<td>2.06</td>
<td>1.92</td>
<td>2.43</td>
<td>2.06</td>
</tr>
<tr>
<td><strong>Tuyere spacing</strong></td>
<td>1.34</td>
<td>1.16</td>
<td>2.39</td>
<td>1.45</td>
</tr>
<tr>
<td><strong>Tuyere submergence</strong></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

(1) Defined @ 25° C for a 50% Cu matte.

(2) Calculated from Asham et al. model.
As just described, a 1/4\textsuperscript{th} scale Plexiglas model was constructed to simulate the Peirce-Smith prototype. The linear dimensions in the prototype were scaled down according to the scale factor, with the exception of the tuyere diameter, which has to be dimensioned as stated by Equation (4.7) if a helium-water system is used, or by Equation (4.11) if air is injected into water.

The converter-shaped vessel was made from a Plexiglas plate 9.5 mm thick. The internal diameter of the model was 850 mm, and its length 270 mm. The end walls of the tank were also made from 9.5 mm thick transparent Plexiglas plate. The top of the vessel was provided with a 85 x 300 mm opening to discharge air. Figure 4.2 shows some views of the Plexiglas converter-shaped vessel employed during the laboratory work.

4.1.2.2 Tuyeres

The five tuyeres in the model were held in place by means of an assembly as shown in Figure 4.3. The assembly consisted of a 58 x 70 x 248 mm Plexiglas rectangular block in which a 50 mm diameter hole was drilled. Into this hole was inserted a 330 mm long Plexiglas bar in which five threaded 7/8 NF holes, 52 mm apart had been drilled. The tuyeres then screwed into the threaded holes. The Plexiglas
Figure 4.2 The Converter-Shaped Model
(a) Frontal View
(b) View Showing the Tuyere Assembly.
Figure 4.3 The Tuyere Assembly.
bar could be rotated in order to change the inclination of the tuyeres in the model. Figure 4.2(b) shows a photograph of the tuyere assembly with the tuyeres in place.

The tuyeres were made from 22.2-mm diameter brass rod in which a hole was drilled axially. The external surface was threaded with a 7/8 NF thread to allow the tuyeres to be screwed into the tuyere assembly. Two tuyere diameters, 16 mm and 12 mm, were used through the laboratory experiments.

4.1.2.3 **Gas-Delivery System**

The source of air used in the current work was a Sutorbilt 4 MF air compressor, with a capacity of 0.08 m$^3$/s (165 CFM) at a gauge pressure of 48.3 kPa (7 psig). A globe valve was used to control the gas flow rate which was measured using a plate orifice. Bourdon-type pressure gages placed at both the entrance and exit of the plate orifice allowed the gas flow rate to be accurately determined, as indicated in Appendix I. At the exit from the plate orifice the air entered a 510 mm long cylindrical manifold 75 mm in diameter for distribution to the tuyeres in the vessel. To calculate the gas flow rate at the tuyere exit, both the atmospheric pressure and the static head of water were considered.
4.1.2.4 **Pressure Measurements**

In order to measure the nozzle pressure oscillations, the back of the tuyere was fitted with a T-connection, as shown in Figure 4.4. The pressure was measured with a fast-response, National Semiconductor (LX 1810 CBZ) piezoelectric transducer. The output signal from the transducer was intermittently recorded on a Tektronik storage oscilloscope (Type 564), and continuously recorded on a four-channel FM tape recorder Tandberg (T1R-115). The signals from the recorder were subsequently played back on the oscilloscope and a Honeywell Visicorder Oscillograph (Model 1508 A) via a Dual HI/LO Rockland filter (Model 442) to eliminate the ambient electric noise. The pressure traces observed on the oscilloscope were photographed with a Polaroid oscilloscope camera.

4.1.2.5 **High-Speed Cinematography**

High-speed films of the tuyere line region were taken using a Hycam camera (Model K2054E). Most films were taken at a speed of 400 frames per second, but speeds of 800 frames per second were also used. Black-and-white 7277 Kodak film (320 ASA) and 4-X 7224 (400 ASA) Kodak film were used. Illumination was provided by a PAMOTOR (Model 8500 C) lamp with a total power of 2400 W. The films were taken from below the
Air from manifold

Figure 4.4 The Tuyere-Pressure Transducer System.
the tuyere region of the bath with the help of a mirror which can be observed in Figure 4.2.

4.1.3 Conditions for the Tests and General Procedure

The tests were carried out under conditions that replicated as closely as possible those prevailing in industrial converters. Table 4.4 shows the scaled-down values which should be used to simulate the injection process in the 'average' converter as well as in three other smelters in Canada and the U.S. The values were calculated for the helium-water and the air-water systems. In all the experiments tap water was used. The calculations were performed assuming a bubbling frequency of $14\ \text{s}^{-1}$ for the aqueous system and a 50 pct. Cu matte in the industrial reactor. The following parameters were studied in the air-water tests:

i) Tuyere diameter  
ii) Tuyere submergence  
iii) Gas flow rate  
iv) Percent filling  
v) Tuyere spacing (by varying the number of tuyeres in the model)

The range of variables is summarized in Table 4.5.
TABLE 4.4

SCALED-DOWN CHARACTERISTICS FOR DIFFERENT MODELS*

<table>
<thead>
<tr>
<th></th>
<th>AVERAGE</th>
<th></th>
<th></th>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Helium</td>
<td>Air</td>
<td>Helium</td>
<td>Air</td>
<td>Helium</td>
</tr>
<tr>
<td>Tuyere Diameter, mm</td>
<td>13</td>
<td>18</td>
<td>11</td>
<td>15</td>
<td>13</td>
</tr>
<tr>
<td>Tuyere Submergence,  mm</td>
<td>135</td>
<td>175</td>
<td>114</td>
<td>147</td>
<td>260</td>
</tr>
<tr>
<td>Tuyere Velocity, m/s</td>
<td>56.5</td>
<td>30.7</td>
<td>76.8</td>
<td>41.7</td>
<td>70.7</td>
</tr>
<tr>
<td>Tuyere Gas Flow Rate, Nl/s</td>
<td>7.5</td>
<td>7.6</td>
<td>7.0</td>
<td>7.0</td>
<td>9.4</td>
</tr>
<tr>
<td>Tuyere Spacing, mm</td>
<td>49</td>
<td>52</td>
<td>45</td>
<td>45</td>
<td>45</td>
</tr>
</tbody>
</table>

* Calculated assuming a bubbling frequency of 14 s⁻¹ in the model.

Scaled-down values for a 50 % Cu matte.
| TABLE 4.5 |

**RANGE OF VARIABLES**

<table>
<thead>
<tr>
<th>Description</th>
<th>Values</th>
</tr>
</thead>
<tbody>
<tr>
<td>Tuyere Diameter, mm</td>
<td>12 and 16</td>
</tr>
<tr>
<td>Gas Flow Rate, 1/s</td>
<td>0 to 50</td>
</tr>
<tr>
<td>Pct. Filling</td>
<td>30, 35, 40, 45</td>
</tr>
<tr>
<td>Tuyere Submergence, mm</td>
<td>60 to 200</td>
</tr>
<tr>
<td>Number of Tuyeres</td>
<td>1, 3, 5</td>
</tr>
</tbody>
</table>
The general procedure to carry out the tests was as follows. First a set of injection conditions was chosen. The gas was turned on and when steady state conditions were reached (it usually took a few seconds) the pressure was measured in the three pressure gages in the system for further calculation of the air flow rate. Then the tuyere pressure oscillations were recorded with the FM tape recorder as well as with the storage oscilloscope and Polaroid photographs. If a movie picture was desired the vessel was illuminated and high-speed films were taken.

To simulate the effect of tuyere line erosion on fluid dynamic events in the tuyere region two 60 mm deflectors were used in some of the experiments, as can be seen in Figure 4.2 (b). The deflectors were placed at right angles to the inside wall of the model along the entire length of the tuyere line, 35 mm above and 25 mm below the tuyere centerline.

4.2 Industrial Work

The injection of air into water carried out during the present laboratory experimental work, differs from industrial practice in many ways. Firstly, the air injected in the laboratory was approximately at the same temperature as that of the bath (room temperature). This is not the case in a copper converter in which air is injected at room temperature into a liquid at about 1200 C. Thus industrial jets are non isothermal
and considerable expansion of the gas close to the tuyere region must be expected. Secondly, the air injected during the laboratory work did not react with the water. On the other hand the objective of injecting air into the bath in a copper converter is to produce chemical reactions between the injected gas and the melt. These reactions are exothermic and change the volume and composition of the injected gas and the bath. Finally, the laboratory work involved a single-phase, homogeneous liquid. In contrast during copper converting, three distinct liquids can be found: slag floating on the surface, a middle zone of matte, and metallic copper at the bottom. Therefore different flow patterns can be generated in the melt, which in turn may affect the jet behaviour.

Owing to these differences between the air-water and the copper converting systems, several industrial tests under normal operating conditions were considered desirable in order to link the laboratory results with metallurgical processes.

4.2.1 Smelters Selected for the Tests

The industrial studies were undertaken to check the conclusions reached in the laboratory work and to further the understanding of gas discharge dynamics and accretion growth in an operating converter. To achieve this, an agreement was
made with four copper smelters in North America to perform industrial tests under normal operating conditions: the ASARCO Smelter in Tacoma, Washington; the NORANDA Smelter in Noranda, Quebec; the KENNECOTT Utah Smelter in Salt Lake City, Utah; and the INSPIRATION Smelter, in Miami, Arizona.

The experiments at the Tacoma smelter were performed on a 13 x 30 Peirce-Smith converter. At the time of the tests the refractory lining of the converter had endured 254 charges. The tuyeres selected for study in the converter were No. 5 and No.6 near one end of the reactor.

At the Noranda Horne smelter the study was carried out on the tuyeres of both Peirce-Smith converters and the Noranda reactor. Two converters were monitored: a relatively old converter (Converter No.6) almost at the end of its working period and a 'middle-aged' converter (Converter No.4). On each a full converter cycle was studied. For the Noranda reactor, which is essentially continuous, the test period was about four hours.

The tests at the Utah smelter were performed on the tuyeres of two 13 x 30 Peirce-Smith converters: a relatively old converter (Converter No.2) and a newly relined converter (Converter No.3). On Converter No.2 almost a full converter cycle was studied. With the new converter, four cycles were
followed (Charges 1, 3, 6, and 12). In both reactors two tuyeres were monitored, most of the time simultaneously, Tuyere No. 25 at the centre of the converter, and Tuyeres No. 6 or 7, close to the end of the tuyere line.

The Inspiration smelter work involved two sets of trials. During the first trial, cinematographic observations of the bath movement were performed at two copper converters, a Hoboken-type (Converter No. 4) and the Inspiration converter. For the second set of trials studies were performed at the tuyeres of the Inspiration converter during Charges 17 and 18 and at the Hoboken converter (Converter No2) during charge 22. In both converters full converter cycles were studied. Two tuyeres were monitored, most of the time simultaneously, one near the mouth of the converter (Tuyeres No. 5 in the Hoboken converter and No. 7 in the Inspiration converter) and the other at the middle of the tuyere line (tuyere No. 21 in converter No. 2 and Tuyere No. 27 for the Inspiration converter). Tables 4.2 and 4.3 show operating data, as compiled by Johnson et al.23, for the 13 x 30 Peirce-Smith copper converters monitored in the course of the present work. Table 4.6 compares other converter characteristics in the four smelters under consideration.
### TABLE 4.6

**CONDITIONS FOR THE INDUSTRIAL TRIALS**

<table>
<thead>
<tr>
<th>SMELTER</th>
<th>TYPE OF CONVERTER</th>
<th>CHARGE MONITORED</th>
<th>OXYGEN ENRICHMENT</th>
<th>PUNCHING EQUIPMENT</th>
</tr>
</thead>
<tbody>
<tr>
<td>TACOMA</td>
<td>13 x 30 PEIRCE-SMITH</td>
<td>254</td>
<td>YES</td>
<td>GASPE</td>
</tr>
<tr>
<td>HORNE</td>
<td>13 x 30 PEIRCE-SMITH</td>
<td>64 and 195</td>
<td>NO</td>
<td>GASPE</td>
</tr>
<tr>
<td>UTAH</td>
<td>13 x 30 PEIRCE-SMITH</td>
<td>1, 3, 6, 12 and 60</td>
<td>YES</td>
<td>KENNECOTT 4B5</td>
</tr>
<tr>
<td>INSPIRATION</td>
<td>HOBOKEN INSPIRATION</td>
<td>22</td>
<td>NO</td>
<td>KENNECOTT 4B5</td>
</tr>
</tbody>
</table>
4.2.2 Scope of the Industrial Tests

As was mentioned, a major objective in the industrial investigation was to observe accretion build-up while a tuyere was operating. The task was accomplished with the aid of a "tuyerescope" that during operation was attached to the back of the tuyere under study. The use of such a device is not new since "endoscopes" have been employed to investigate movement and combustion of coke in the raceway of blast furnaces\textsuperscript{90-94}; however their use to study tuyere phenomena in the copper converting process is believed to be novel.

In order to gain knowledge on the effect of some operating variables on injection dynamics the tuyere pressure measurement technique, first developed by Hoefele and Brimacombe\textsuperscript{31} in a nickel converter, was extended to the copper converter. In addition, samples of accretion were sought during operation to assess the relative importance of magnetite formation and freezing in the accretion growth process. Finally, it was also intended to perform cinematographic observations of the bath surface inside the converter; for this advantage was taken of the particular features of the syphon-type reactors operating in the Inspiration smelter.
4.2.3 Equipment and Procedure

The tuyerescope used to observe tuyere phenomena in the copper converter is shown in Figure 4.5. It was constructed of a 130-mm length of 33-mm diameter stainless steel pipe one end of which was covered by a removable Plexiglas window. This tuyerescope was designed to be inserted quickly into the tuyere immediately after punching and to be removed once the tuyere had blocked. Insertion of the tuyerescope into the back of the tuyere, as in the case of punching, forced the ball valve up into the ball race in the tuyere body as shown in Figure 4.5. A simple lock-fit device was employed to secure the tuyerescope in place and to prevent air leakage. Thus the tuyerescope facilitated direct observation, by eye or with cameras, of the dynamics of accretion growth at the tuyere tip while blowing proceeded.

Focusing on the tip of the tuyere, photographs were taken with cameras individually mounted at the back of the tuyerescope. In the Tacoma smelter tests, motion pictures were taken with a standard Super 8 camera at 25 fps using 25 ASA Kodachrome 40 color film. For the trials in the other smelters, a 16 mm Beaulieu R16 movie camera, to which a Sony TV zoom lens (f = 20–80 mm, 1:25) was attached, was employed. Both color (Ektachrome 7256, 64 ASA) and black and white
Figure 4.5 The Tuyerescope
(Kodak 4-X 7277, 400 ASA) film were utilized. The most detail was obtained with the 64 ASA film. Still shots were also made with a motor driven 35 mm camera, usually at f5.0 and 1/4 to 1/125 s (depending on the coverage of the tuyere tip by accretions) using 64 or 400 ASA color film. The 64 ASA film was found to give the finest detail. No filters were used, and exposure settings were determined with a Pentax spot light meter.

Samples of accretion growing at the tuyere tip were taken. For this purpose a 9.5 mm hole was drilled in the Plexiglas window of the tuyerescope and a probe, consisting of a 1.3 m length of 64 mm rod, was inserted. The last 15 mm of the rod was bent at a right angle to form a hook. With the probe inserted through the window it was possible to observe the hook and manipulate it to take accretion samples.

Pressure fluctuations at the tuyere tip in the copper converter were measured by inserting a 9.5 mm o.d. pipe through the Plexiglas window of the tuyerescope. Pressure in the pipe was sensed with the same pressure transducer of the laboratory work, which was connected via an amplifier to a storage oscilloscope and an FM tape recorder as shown in Figure 4.6.
Figure 4.6 Apparatus to Measure Pressure Fluctuations in the Tuyeres of a Copper Converter.
To study the bath surface movement inside the converter, cinematographic observations were performed during two sets of trials at the Inspiration smelter. Two converters were monitored, a Hoboken-type and the Inspiration converter. In both cases the observations were carried out during the slag making blow. With the Inspiration converter the air flow rate into the reactor was about 12.3 - 13.7 Nm$^3$/s, the tuyere air pressure 11.0 x $10^4$ Pa (16 psig) and the bath temperature about 1180 - 1230°C. The operating conditions at the Hoboken converter were 8.0 - 10.4 Nm$^3$/s, 10.3 - 11.0 x $10^4$ Pa and 1150 - 1210°C, respectively. The cinematographic work was performed with the 16 mm Beaulieu camera, to which a Sony TV zoom lens was attached. Daylight Ektachrome 7256 (64 ASA) film was used.

During the first set of trials the movie pictures were obtained with the camera placed on the sampling platform close to the converter, in such a way that it was possible to observe events taking place mainly in the non-spout single-phase region of the bath. No changes in the operating variables (tuyere submergence, rate of gas flow) were attempted. For the second trial it was intended to carry out observations of the spout or two-phase region in the converter line. To perform this study it was necessary to have an appropriate view port through which motion and still pictures
could be taken. A possible location for this view port was the opening in the side wall of the Inspiration converter, through which burners were formerly held in place but now are mudded over. Two attempts were carried out to implement a view port by mudding a glass window about 200 mm in diameter over the opening. The first glass was put in place during a converter turn-around. Nevertheless, about 5 minutes after blowing was started the window became covered with droplets of liquid which adhered to it, making visualization of the bath surface practically impossible. A second attempt was made to install a second glass during an interval between blowing in order to try to make observations immediately after blowing was resumed. Unfortunately this time the glass fell and broke after which no further trials were pursued.
5.1 Laboratory Results

This section presents the results obtained with the sectional 1/4th-scale model of the copper converter. The air-water system was employed to study a number of variables such as tuyere diameter, gas flow rate, pct. filling, tuyere submergence, and tuyere spacing. Table 4.5 summarizes the range of the variables under consideration. The experimental procedure followed to carry out the tests is described in section 4.1.3.

5.1.1 Dynamic Pressure Measurements

The pressure pulses from the tuyeres in the model were tape recorded or photographed directly from the screen of a storage oscilloscope. Two characteristics of the pressure signals were examined and compared: the frequency of the pulses and the shape of the traces. To calculate the value of the pulse frequency and its standard deviation the recorded signals were played back on the oscilloscope to measure the pulse frequency. About 50 to 100 counts were performed for each set of blowing conditions. Following the frequency
measurements, a representative trace of the pressure signals was photographed by means of the oscilloscope camera.

5.1.1.1 Effect of the Air Flow Rate

The frequency of the pressure pulses changes with the total flow rate of the injected air as can be observed in Figures 5.1 and 5.2 showing measurements in the laboratory converter model with five, 16-mm and 12-mm diameter tuyeres, respectively. When no deflectors are positioned close to the tuyere line in the model, the frequency of the pulses at low rates (5,000 to 9,000 cm$^3$/s, or Froude numbers of about 0.4 to 2.5) is about 13 to 14 s$^{-1}$ for both tuyere diameters. An increase in the gas flow rate produces an almost linear increase in the pulse frequency in both cases. With 16-mm tuyeres in place the frequency is about 20 s$^{-1}$ for air flow rates and Froude numbers of the order of 40,000 cm$^3$/s and 13, respectively. For the case of smaller tuyeres, the frequency of 20 s$^{-1}$ is reached at flow rates of about 24,000 cm$^3$/s, or Froude numbers in the range of 14.

The overall effect of placing the two deflectors along the tuyere line in the model is a reduction in the frequency of the pressure signals. Figure 5.3 shows this frequency as a function of the modified Froude number of the system, with
No deflectors

Two deflectors

Tuyere diameter: 16 mm
submergence: 170 mm
spacing: 50 mm
42% fill

Figure 5.1 Bubbling Frequency versus Total Air Flow Rate in the Model with Five 16-mm Tuyeres. Bands indicate Standard Deviation.
Figure 5.2 Bubbling Frequency versus Total Air Flow Rate in the Model with Five 12-mm Tuyeres. Bands Indicate Standard Deviation.
Figure 5.3 Bubbling Frequency versus Modified Tuyere Froude Number in the Model with Five Tuyeres.
and without deflectors in place, for both tuyere diameters. With a low Froude number the decline in frequency is about 6 to 7 pulses per second, a much more pronounced reduction than for the case of higher values of the Froude number, in which case the frequency declines no more than 4 pulses per second.

The shape of the pressure pulses is also influenced by the air flow rate. The effect of gas flow rate on pressure pulses is shown in Figure 5.4 for the discharge of air through five 16-mm dia. tuyeres. At a flow rate of 12,500 cm$^3$/s, or a Froude number of 1.27, (Figure 5.4 (a)), the pulses are characterized by a relatively sharp increase in pressure until a maximum is reached, immediately followed by a more gentle decline. The amplitude of the oscillations is about 2.4 - 2.8 kPa. At a flow rate of 33,700 cm$^3$/s, or a Froude number of 9.25, Figure 5.4(b), the traces are more symmetrical, and the amplitude of the pressure pulses becomes more irregular varying between 2.8 and 6.0 kPa. At a higher flow rate of 45,200 cm$^3$/s (Froude number 16.6), Figure 5.4(c), the pressure pulses are even sharper and more symmetrical than before, and are now spaced by intervals of constant low pressure signals. The amplitude of the oscillations varies from 2.8 to 5.2 kPa.
Figure 5.4 Pressure Traces in the Model as a Function of Gas Flow Rate.
Five 16-mm Tuyeres, 40 pct. filling.
(a) 12.5 l/s, (b) 33.7 l/s, (c) 47.2 l/s.
Vertical Scale: 0.18 kPa/div
Horizontal Scale: 50 ms/div
5.1.1.2 Effect of Tuyere Spacing

To study the effect of the distance between tuyeres, several experiments were carried out injecting air through three tuyeres in the laboratory model, instead of using five tuyeres as before. This results in an increase in the tuyere spacing from 50 mm for the five-tuyere situation to 100 mm when air is injected through three tuyeres only.

The effect of tuyere spacing can be observed by comparing Figure 5.1 with Figure 5.5 which shows measurements in the model using three 16-mm tuyeres. For a tuyere spacing of 100 mm the frequency of the pressure pulses is much less affected by gas flow rate than in the five-tuyere configuration. In the case of the former the frequency changes from 14 s\(^{-1}\) at an air flow rate of 7,000 cm\(^3\)/s to 18 s\(^{-1}\) when the flow rate is increased to 43,000 cm\(^3\)/s. The same variation in total gas flow rate when the tuyeres are closer together produces changes in frequency from 13 s\(^{-1}\) to about 21 s\(^{-1}\).

Figure 5.6 shows the variation in the frequency of the pulses in terms of the modified Froude number for the three-tuyere configuration. Again the frequency is much less sensitive to Froude number in this case than for a tuyere spacing of 50 mm, as shown in Figure 5.3. For a 100 mm spacing, changes in the modified Froude number from 2 to 40 produce an increase in the pulse frequency of less than 4 pulses per second.
Figure 5.5  Bubbling Frequency versus Total Air Flow Rate in the Model with Three 16-mm Tuyeres. Bands Indicate Standard Deviation.
Figure 5.6 Bubbling Frequency versus Modified Tuyere Froude Number in the Model with Three Tuyeres.

\[ Fr = \frac{\rho_g u_0^2}{g(\rho_l - \rho_g)d_o} \]
5.1.1.3 Tuyere Interaction

To ascertain whether tuyere interaction takes place or not, two tuyeres were monitored simultaneously in the converter model. Figure 5.7 shows representative traces from piezoelectric transducers placed in the central tuyere of the model (Channel 1) and in an adjacent tuyere (Channel 2), 50 mm apart from the central tuyere. In Figure 5.7(a) an almost complete correspondence between the two signals is observed. Each pressure peak in the central tuyere is mirrored in the traces from the adjacent tuyere, therefore the tuyeres are interacting. This indicates that the same type of fluid dynamic events are taking place in the central and the immediately adjacent tuyere. Twenty seconds later, Figure 5.7(b), the signals from both tuyeres are somewhat different from each other. Only a slight correspondence is observed during the initial 200 ms of the signals, after which the events taking place in the central tuyere are clearly different from those occurring in the lateral tuyere. No interaction is found; and the tuyeres are working independently.
Figure 5.7 Simultaneous Pressure Traces from Two Adjacent Tuyeres in the Model.
(a) Tuyeres are Interacting
(b) Twenty Seconds Later, No Interaction is Observed.
5.1.1.4 Effect of Tuyere Submergence

A sequence of pressure traces obtained as the laboratory converter model was being filled with water is shown in Figure 5.8. In this way it was possible to study the effect of tuyere submergence on the injection dynamics in the tuyere region. The Plexiglas model was placed with its tuyere line 180 mm from the bottom of the model and the air flow rate was kept constant at 22 l/s during the complete measuring sequence. Thus as long as the water level is below the tuyere line in the model the pressure trace is constant, Figure 5.8(a), as expected for the homogeneous discharge of air into air. For a tuyere submergence of about 20 mm or 21 pct. fill, Figure 5.8(b), the pressure signals are characterized by irregular pulses of relatively short duration spaced by intervals of low pressure. For a tuyere submergence of 90 mm (30 pct. fill), Figure 5.8(c), the pulses become more symmetrical and less disorganized as compared with the previous signals. For a tuyere submergence of 130 mm (37 pct. fill) the pulses occupy longer time periods although intervals of low pressure can still be observed, Figure 5.8 (d).
Figure 5.8 Pressure Traces in a Tuyere of the Model During Charging.
(a) Tuyere Submergence: 0 mm
(b) Tuyere Submergence: 20 mm
(c) Tuyere Submergence: 90 mm
(d) Tuyere Submergence: 130 mm
Horizontal Scale: 50 ms/div
Vertical Scale: 0.18 kPa/div in Channel 1
Signal in Channel 2 Filtered at 60 Hz.
5.1.2 High-Speed Cinematography

Two different aspects of the injection of air in the water model were studied with the aid of high-speed cinematography. During blowing, the air-matte interaction in the tuyere region influences the state of the refractory and the formation of accretions at the tuyere tip. Therefore it was considered important to observe events taking place in the tuyere region of the model and relate them to the dynamic pressure measurements in the tuyeres of both the laboratory model and operating copper converters. The high-speed cinematographic work technique was also employed to investigate the conditions under which slopping takes place in the copper converter$^{102,103}$.

In both cases frame-by-frame analysis of the high-speed films was conducted using a film analyzer and digitizer. Also, individual photographs from the films were obtained to illustrate the different events taking place during the injection of air into water.

5.1.2.1 Observations at the Tuyere Line

Analysis of the high-speed films showed that the tuyeres
may work independently or may interact. Figure 5.9 shows two photographs taken from a high-speed film of air injected into water at a flow rate of 11.5 l/s, equivalent to a modified Froude number of 1.1. In Figure 5.9(a) the three tuyeres at the left of the tuyere line are seen to produce large bubbles, each tuyere acting independently from the others. In this case it is easy to identify which tuyere is discharging gas; the tips of the other two tuyeres in the model are covered by a gas packet or envelope, which makes impossible to discern the formation of bubbles in those tuyeres. In this case there is a gas path connecting the exit of the tuyeres. Twenty seconds later, Figure 5.9(b), the situation has almost completely reversed. Now the left region of the tuyere line is covered by a large envelope of gas. On the other side, the tuyeres are generating bubbles more independently with less interaction than before. Thus the injection process at the tuyere line is characterized by the generation of an unstable gas-filled packet or envelope, which at certain instants may cover several tuyeres simultaneously, making possible the interaction between them. This unstable envelope breaks down in a matter of seconds, so that the tuyeres which were interacting an instant before start to operate as independent tuyeres and viceversa.
Figure 5.9  High-Speed Film Photographs from the Tuyere Line of the Model
(a) Left-Hand Side Tuyeres Covered by a Gas Envelope
(b) Two Seconds Later, the Envelope Breaks Down.
5.1.2.2 Slopping Observations

As stated earlier the injection of gas into the bath imparts motion to the liquid which, depending on conditions, may take the form of slopping. High-speed cinematographic observations and tuyere pressure measurements were carried out to study bath movement and fluid dynamic events close to the tuyere region.

The high-speed cinematographic work showed that during horizontal submerged injection two regions can be discerned at the surface level of the bath, as can be observed in Figure 4.2. Above the tuyere line a spout or two-phase region exists, covering about 20-40% of the surface level of the bath, characterized by a strong agitation of the surface, as the air bubbles disengage from the tuyere tip. This region is thought to be the primary source of splashing and build-up of accretions at the converter mouth. The remainder of the bath surface consists of a single-phase region with much less agitation which contributes little to splashing, except for the case of shallow tuyere submergence; then injection is accompanied by slopping of the bath and enhanced entrainment and splashing. As described in Section 5.2.3, in the single-phase zone in an operating copper converter the bath surface moves mainly from the region above the tuyere line toward the opposite wall. Superimposed on this bath movement, there is evidence of a strong splashing toward the mouth of the reactor...
5.1.3 Slopping Measurements

Bath slopping is an oscillatory motion of the liquid between the tuyere line and the breast of the converter. To investigate the influence of variables such as air injection rate, tuyere submergence and pct. filling of the converter on slopping Jorgensen et al.\textsuperscript{103} carried out slopping measurements in the sectional $1/4^{th}$ scale model of the copper converter. Slopping and non-slopping of the bath were determined under various injection conditions by visual observation, high-speed cinematography and by measuring the dynamic pressure in the central tuyere with the piezoelectric transducer described earlier.

Typical observations of non-slopping and slopping are shown in Figures 5.10(a) and 5.11(a) respectively. As expected when the bath is not slopping, the liquid surface is horizontal with waves moving between the jet spout and the opposite wall. However under slopping conditions, the bath surface moves back and forth with a frequency of about 1 Hz, and some of the injected gas moves to the opposite wall. During slopping, periodic pulses of sound could be heard from the air jet, in concert with the rise and fall of liquid above the tuyere line. The pressure traces recorded on the screen
Figure 5.10 Model Under Non-Slopping Conditions.
(a) Tuyere Submergence: 130 mm
   Air Flow Rate: 3.7 l/s
   40 pct. filling
(b) Pressure Trace from Central Tuyere
   Horizontal Scale: 0.5 s/div
   Vertical Scale: 0.18 kPa/div
Figure 5.11 Model Under Slopping Conditions
(a) Tuyere Submergence: 130 mm
    Air Flow Rate: 20 l/s
    40 pct. filling
(b) Pressure Trace from Central Tuyere
    Horizontal Scale: 0.5 s/div
    Vertical Scale: 0.18 kPa/div
of the oscilloscope, Figures 5.10(b) and 5.11(b), also clearly reveal the state of bath slopping. Under non-slopping conditions the pressure fluctuates, due to the formation of bubbles, about a constant mean value whereas bath slopping causes a 1 Hz oscillation to be superimposed on the signal. The pressure signals were particularly helpful in delineating the transition from non-slopping to slopping.

Jorgensen et al.\textsuperscript{103} conducted experiments with varying pct. filling and tuyere submergence to determine the critical air flow rate, above which bath slopping prevailed, for each set of conditions. An attempt was then made to correlate the critical air flow rates in terms of kinetic and buoyancy power into the bath from the injected air. In a physical-model study conducted earlier by Haida and Brimacombe\textsuperscript{104}, the mixing time in hot-metal ladles and torpedo cars has been correlated, and scaled-up to full size, successfully with buoyancy power while kinetic power was found to have a minor influence. Thus the kinetic power and buoyancy power per unit mass of bath were calculated from the following respective relationships\textsuperscript{104}:

\begin{align}
\epsilon_k &= \frac{1}{2} \frac{p \ u_o Q}{M_{\text{bath}}} \\
\epsilon_b &= \frac{2 Q P_a \left[ \ln \left( \frac{P_a + \rho_1 g h}{P_a} \right) \right]}{M_{\text{bath}}} 
\end{align}

for the critical flow rates determined in the experiments. It was found that the critical flow could not be correlated with the kinetic power, but a good correlation was obtained in terms of buoyancy power input. Indeed the critical-flow data from all the experiments can be represented on a plot of buoyancy power per unit mass of bath against tuyere submergence as shown in Figure 5.12\textsuperscript{102}. The least-squares fit line passing through the data delineates regions of slopping and non-slopping so that Figure 5.12 is effectively a 'slopping-behaviour' diagram. It is seen that more buoyancy energy, e.g. greater air flow rate, can be imparted to the bath without slopping when the tuyere submergence is large.

To relate these physical model results to converter operation, data on operating converters have been taken from the world-wide survey conducted by Johnson et al.\textsuperscript{23}; the buoyancy power (calculated at standard conditions) and tuyere submergence for the different smelters has been determined and plotted on the slopping-behaviour diagram shown in Figure 5.13. The critical slopping line, shown in Figure 5.12, has been extrapolated to converter tuyere submergences and also is presented in Figure 5.13. It is seen that most of the converter operations are reasonably close to the critical slopping line.
Figure 5.12 Critical Flow Data from Physical Model Experiments on Slopping Behaviour Diagram.
Figure 5.13 Buoyancy Power Against Tuyere Submergence for Different Smelters. Solid Line is Extrapolation from Figure 5.12 (Data-points from reference 23)
5.2 **Industrial Results**

The results obtained during the trials at the four smelters studied in this work are presented in this section. The conditions for the tests are summarized in Tables 4.2, 4.3 and 4.6. The number of tests was limited realistically by the duration of a copper converter cycle and the difficult conditions that are typical of a smelter. However, the results obtained at each smelter were consistent with the results from the others and also with what was expected from the results of the laboratory work.

5.2.1 **Dynamic Pressure in the Tuyeres**

Compared to the pressure traces obtained in the laboratory experiments, the pressure signals recorded during the industrial tests were noisier, mainly due to the rest of the electrical equipment operating in the converter aisle. To eliminate part of this ambient noise, most of the signals from the tape recorder were analyzed after being filtered as described in Section 4.1.2.4.
5.2.1.1 Effect of the State of the Refractory at the Tuyere Region

Tuyere pressure measurements commencing with the first charge of a freshly relined converter revealed an interesting change of pulse frequency with increasing number of charges processed by the converter, as shown in Figure 5.14. During the first charge, the pulse frequency was about 14 s\(^{-1}\), a value close to the frequency measured by Hoefele and Brimacombe\(^3\) at the tuyeres of a Peirce-Smith nickel converter during the first charge of its campaign. By the third charge of the copper converter the pulse frequency dropped to 8 s\(^{-1}\) and to about 7 s\(^{-1}\) for Charges 6 and 12 respectively; and thereafter there was a slow decline to a steady value of 4 to 6 pulses per second.

It is important to mention that in the smelter in which most of these measurements were made, the tuyere pipes are protruding into the converter 150 mm or more beyond the lining after new refractory has been installed, as can be observed in Figure 5.15. The same situation prevails at the Thompson Smelter. Coincidentally within about three charges the pipes burn back to become flush with the inside wall, but no visible erosion (or damage) of the refractory is observed. Appendix II shows the shape of the pressure pulses during different charges of the converter being monitored.
Figure 5.14 'Bubbling' Frequency from Different Copper and Nickel Converters versus Number of Charges in the Campaign.
Figure 5.15 Tuyere Pipes in a Converter from Utah Smelter.
(Photographs courtesy of Dr. A. Weddick)
(a) Pipes Protruding after Reline
(b) Pipes after Third Charge
5.2.1.2 Effect of the Tuyere Submergence

A typical pressure trace measured at the tuyere tip in the copper converter at Tacoma is shown in Figure 5.16, for two tuyere submergences: 300 mm (normal practice) and 500 mm. The measurements were made during the second slagmaking blow of Charge 254. Thus, with the shallow tuyere submergence, Figure 5.16 (a), a relatively constant, low-pressure signal interrupted periodically by pulses of short duration is observed. The pulse frequency is close to 4 s⁻¹. With the deeper tuyere submergence of 500 mm, Figure 5.16 (b), pulses of greater duration but the same frequency are observed. Again, the pulses are separated by intervals of constant lower pressure.

5.2.1.3 Effect of Tuyere Blockage

As was mentioned, the tests at the Utah smelter were performed at the tuyeres of two Peirce-Smith converters. In both reactors two tuyeres were monitored, most of the time simultaneously. During the bulk of the observation period the tuyeres under study remained open. Nevertheless, when tuyere blockage took place the pressure pulses from the tuyeres being monitored showed some interesting features, as can be observed in Figure 5.17, which shows pressure pulses from Tuyeres No. 6.
Figure 5.16 Pressure Traces During Charge 254 in a Copper Converter from Tacoma.
(a) Tuyere Submergence: 300 mm  (b) Tuyere Submergence: 500 mm
Vertical Scale: 14 kPa/div
Horizontal Scale: 100 ms/div
and No. 25 (Channels 1 and 2, respectively) of Utah Converter No. 2. In Figure 5.17 (a) the pulses are coming from tuyeres totally open, as could be observed through the Plexiglas window of the tuyerescope. The pressure traces are characterized by pulses with an amplitude of about 7 to 14 kPa and a frequency of 4 to 5 s$^{-1}$. Seven minutes later Tuyere No. 6 was observed to be blocked, while Tuyere No. 25 remained open. The tuyere pressure traces under these conditions can be observed in Figure 5.17 (b). For the open tuyere the pressure traces remain essentially the same as before, while the signals from the blocked tuyere change drastically to a flat trace, without pulses. Three minutes later the accretions blocking tuyere No. 6 dislodged by themselves, and again the signals from both tuyeres are characterized by regular pulses, as can be observed in Figure 5.17 (c). This suggests that the pressure signals from individual tuyeres may be used to detect tuyere blockage.

It is also important to mention the effect of the tuyere punching on the amplitude of the pressure pulses. Immediately after punching the pressure traces had a greater amplitude, which then decreases as the next punching cycle was approached. Similar results were obtained previously by Hoefele and Brimacombe$^{31}$ in a nickel converter.
Figure 5.17 Simultaneous Pressure Traces from Two Tuyeres in a Copper Converter. Tuyere No. 6 in Channel 1 and Tuyere No. 25 in Channel 2. 
(a) Both Tuyeres are Open 
(b) Seven Minutes Later, Tuyere No. 6 is Blocked, Tuyere No. 25 is Open 
(c) Three Minutes Later, Both Tuyeres are Open Again. 
Vertical Scale: 4.8 kPa/div 
Horizontal Scale: 100 ms/div
5.2.2 Accretion Growth at the Tuyere Tip

As stated earlier the tuyerescope provided an excellent view of the bath and the growth of accretions, and made possible the sampling of the material forming the accretion while it was growing at the tuyere tip.

5.2.2.1 Dynamics of Accretion Growth

Typical accretion growth in the copper converter, depicted by a sequence of photographs taken with the 35 mm camera, is shown in Figure 5.18. These photographs were taken during the second slagmaking blow of Charge 254 of a converter from the Tacoma Smelter. The oxygen content of the enriched air was 26 pct and the matte grade was 43 pct Cu. Figure 5.18 (a) shows the tuyere nearly covered with an accretion which within seconds of the photograph being taken, spontaneously dislodged. The ensuing photographs in the sequence reveal the growth of the next accretion over a time span of about 180 seconds whereupon the tuyere again was nearly covered. Typically, the time for complete coverage of the tuyere was 60 to 180 seconds at this stage of the converter cycle. In Figures 5.18 (b) to (f) the accretion can be seen to grow primarily from the bottom of the horizontal tuyere. Initially, the accretion is crescent-shaped extending from the 3 to 9 o'clock
Figure 5.18 Dynamics of Accretion Formation in a Converter at Tacoma Smelter.
(a) Tuyere is Nearly Blocked
(b) Accretion Dislodged Spontaneously
(c)-(f) Accretion Growth Resumes
position of the tuyere; the accretion continues to grow upward until, as seen in Figure 5.18 (a) and (f), the uncovered region of the tuyere tip has been reduced to a small area at the 1 o'clock position. In Figures 5.18 (e) and (f) the upper region of the accretion is seen to be brighter, i.e., hotter, than that near the bottom of the tuyere; in addition, striations which are roughly concentric with the circular tuyere are visible. Visual observation and the cinematography confirmed these general features of accretion formation and also showed that, from time to time, protruding parts of an accretion would disappear then reappear suddenly. At first it was thought that this phenomenon was caused by rapid melting and freezing, but it is more likely a result of temporary immersion of the protuberances as bath washes briefly against the accretion.

5.2.2.2 Effect of Oxygen Enrichment of the Blast

The photographic work in the tuyeres of the different converters indicated that the mechanism of accretion growth is affected by the oxygen enrichment of the blast. Figure 5.19 shows accretion formation during the first slagmaking blow in a converter at the Noranda Smelter, where oxygen enrichment of the air is not used. As can be observed, early in the cycle the accretion tends to build up gradually, mainly
Figure 5.19 Accretion Formation During First Slagmaking Blow in a Copper Converter at Horne Smelter.
upward from the bottom of the tuyere without remelting of the solidified material. Late in the converter cycle the build-up of accretions is faster, but with some remelting back to the bath. In Figure 5.19 (a) the tuyere is about half-blocked with solidified material growing from the bottom and the upper part of the tuyere tip. Ten and twenty seconds later, Figures 5.19 (b) and 5.9 (c), some remelting of the accretion material back to the bath is observed. Sixty seconds later, Figure 5.19 (d), the accretion growth process has started again and about 70 pct. of the tuyere tip area is blocked. During the entire cycle at Noranda the accretions were observed to be more dense and stable, as compared with the accretions formed at Tacoma which are porous and can be removed easily by punching.

The effect of oxygen-enrichment can be further elucidated by referring to the accretion formation process at Utah smelter, where oxygen-enriched air is used. Figure 5.20 depicts a sequence of photographs taken during the slagmaking blow of Charge 1 of a converter from the Utah smelter. As in Tacoma the oxygen content of the enriched air was 26 pct. The time between each photograph was 20 seconds. It is seen that the accretion grows primarily from the bottom of the tuyere. As in Tacoma the accretion is initially crescent shaped and continues to grow until the uncovered region of the tuyere
Figure 5.20 Accretion Formation During Slagmaking Blow in a Converter at Utah Smelter.
tip is reduced to a small area in the upper region of the tuyere. As in the case of the Tacoma smelter, the accretions were porous and could be removed easily by punching.

5.2.2.3 Analysis of the Accretion Samples

Samples of accretion, which were obtained with some difficulty, were studied by X-Ray Diffraction and Scanning Electron Microscopy. Samples of accretion formation taken during the first slagmaking blow at Tacoma, during the third slagmaking blow at Noranda, and at the end of the slagmaking blow, and during the coppermaking blow at the Utah smelter were analyzed. For all the samples obtained during slagmaking blows, the major constituent of the accretion was found to be $\text{Cu}_1.96\text{S}$, a high-temperature phase which is metastable at room temperature. Other compounds in the samples were identified as various copper-iron sulphides ($\text{FeS}_2$, $\text{Cu}_5\text{FeS}_4$, $\text{Cu}_5\text{FeS}_6$, etc). No magnetite was found even though special care was taken to detect its presence. This suggests a freezing mechanism of the bath for the accretion growth, rather than a magnetite precipitation mechanism due to local over oxidation of the bath.

Figure 5.21 shows a S.E.M. photograph of the material from the third slagmaking blow at Noranda. The analysis of the coppermaking accretions from the Utah Smelter indicated that the major components of the sample were metallic copper and $\text{Cu}_1.96\text{S}$. It
Figure 5.21 S.E.M. Photograph of the Accretion Sample from Slagmaking Blow in a Copper Converter at Horne Smelter.
should be mentioned that during the sampling of this last specimen it was not possible to perceive clearly if this sample represented accretions growing at the end of the tuyere or simply liquid bath freezing at the tip of the sampling probe.

5.2.2.4 Influence of the 4B5 Punching System

The Utah smelter and the Inspiration smelter employ 4B5 Kennecott mechanical punchers to clear a blocked tuyere by forcing a steel rod through it. The 4B5 puncher is attached to each tuyere, with its punch rod inserted half-way through the tuyere pipe during injection. Therefore to carry out the dynamic pressure measurements at the tuyeres of the two smelters it was necessary to remove the 4B5 punchers in order to attach the tuyerescopes to the back of the tuyeres to be monitored. In the course of these measurements it was suspected that the punching system plays a role in the accretion growth at the tuyere tip.

During most of the observation period at the Utah and Inspiration smelters, the tuyeres under study (with the tuyerescope attached to them) remained open. Accretion formation was observed mainly during the first period of the slagmaking stage, wherein a fast build up of accretions
occurred. Later in the cycle, only occasionally some accretion growth was observed during short intervals, after which the solidified material dislodged by itself from the tuyere tip, perhaps due to remelting effects. Nevertheless the rest of the tuyeres in the converter (with the 4B5 puncher at their back) were intermittently blocked, as evidenced by the drop in total gas flow rate into the converter.

The fact that the tuyeres being monitored showed almost no accretion build-up for considerable lengths of time, while for the other tuyeres blockage did take place, indicates that the 4B5 punching system affects in some way the mechanism of accretion growth by influencing the fluid-dynamic conditions inside the tuyere pipe.

To test this hypothesis some laboratory tests were carried out. Using a pitot static tube, the velocity profile across a 600 mm (51 mm I.D.) pipe positioned in a tuyere body from the Utah smelter was measured. Working under gas flow rate conditions close to normal operation three different situations were studied, namely:

a) no punch bar inserted through the back of the tuyere
b) a 25-mm punch bar inserted 600 mm through the back of the tuyere body and touching the bottom of the tuyere pipe
c) same as in b) but now with the punch rod touching the top of the tuyere tip.

The velocity profiles measured under these three conditions can be observed in Figure 5.22. When no punch bar is inserted the velocity profile is non-symmetrical, which is a consequence of the off-axis injection of air through the tuyere body and the short length of the tuyere pipe which makes it difficult to obtain fully developed flow conditions. Nevertheless, although non-symmetrical, the velocity profile is relatively flat as expected for the flow of air under turbulent conditions (the Reynolds number throughout the measurements was about $6 \times 10^5$). When the bar was inserted touching the bottom of the tuyere pipe the velocity profile was observed to be relatively symmetrical and parabolic, with a maximum velocity close to the tuyere centerline and decreasing rapidly toward the wall of the pipe. Finally, when the inserted punch rod was touching the upper part of the tuyere pipe wall the velocity profile was observed to be significantly non-symmetrical, with the lowest velocities near the bottom of the pipe wall and gradually increasing to reach a maximum value close to the top of the tuyere pipe wall. Thus it is clear that the punching system affects the flow regime at the tuyere pipe and therefore possibly also the process of accretion growth.
Figure 5.22 Velocity Profile Inside a Tuyere Pipe Showing the Effect of the Punching System.
5.2.2.5 Tuyere Pipes. Metallographic Work

Two tuyere pipes from a converter out of service for relining were obtained; a short (25 cm) pipe and a longer (50 cm) pipe supposedly coming from the centre and the end of the tuyere line respectively. These tuyeres were examined and compared with high pressure tuyeres from the trials at ASARCO Tacoma\textsuperscript{34}. The end tuyere (Figure 5.23 (a)) exhibits what appears to be a neck 15 mm from the tip of the tuyere presumably due to the tensile strains caused by the punch bar. There is evidence of damage by the punch bar because the tip of the tuyere has been bent back at right angles to its axis on one side. The outside of this tuyere tapers down somewhat presumably due to the oxidation and spalling. The central tuyere, Figure 5.23 (b) in contrast shows no evidence of necking or tapering of the outside. The tuyere seems to be wearing away on the inside presumably due to the punching action and thermal and chemical attack. This tuyere obviously was shorter than the end tuyere; therefore its tip was farther removed from the bath, making it colder and less prone to necking under the influence of the punch bar.

Metallographs of the central tuyere at 5 mm, 18 mm, and 25 mm from the tuyere tip are presented in Figure 5.24 (a) to (c) respectively; Figure 5.24 (d) shows a metallograph from the back of the tuyere pipe (virgin pipe).
Figure 5.23 Longitudinal Sections of Tuyere Pipes from a Converter at Utah Smelter
(a) End Tuyere, (b) Central Tuyere
Figure 5.24 Metallographs of the Central Tuyere from a Converter at Utah Smelter
(a) 5 mm from the Tuyere Tip
(b) 18 mm from the Tuyere Tip
(c) 25 mm from the Tuyere Tip
(d) Back of the Tuyere, Virgin Pipe
Neither of the tuyeres exhibit significant internal oxidation of the steel pipe in contrast to the high pressure tuyere which exhibits internal oxidation running back 120 mm from the tip. This could also indicate that the tip of the high pressure pipe is significantly closer to the bath and is running much hotter.

5.2.3 Bath Surface Movement and Splashing

As described previously, to study the bath surface movement inside the converter, two set of observations at the Inspiration Smelter were carried out.

During the first set of trials movie pictures were obtained with the camera placed on the sampling platform of the Hoboken and the Inspiration converters, in such a way that it was possible to observe events taking place mainly in the non-spout region of the bath. The results obtained from this preliminary trial indicated that it is possible to observe with some detail the bath surface movement of a syphon-type converter while blowing. The movie pictures indicate that in the single-phase zone the bath surface moves mainly from the region above the tuyere line toward the opposite wall. Some axial movement of the bath was also observed, as evidenced by solids floating on the bath surface. It is not clear whether this axial movement takes place over the entire converter length or not.
Superimposed on this bath movement, there is evidence of a strong splashing taking place from the spout region toward the mouth of the converter. Unfortunately it was not possible to film the core of the spout region, in which the gas bursts through the bath surface.

During the second set of trials it was attempted to carry out observations of both the two-phase spout and the remainder of the bath surface. A major objective of these trials was to link bath movements and events at the tuyere tip to define optimum conditions to minimize build-up in the converter. Thus, an attempt was made to study the influence of several injection variables on splashing and build-up at the mouth and off-take regions of the converter and relate them to injection behaviour.

Unfortunately, as was mentioned, two attempts to implement an appropriate view port failed, after which no further trials were pursued. Therefore, it was not possible to make direct observations of bath surface movement at the spout region nor to link these observations with some injection parameters. Nevertheless it was possible to observe the amount of liquid material ejected from the charging mouth of the converter and to relate it to tuyere submergence. These observations, although rather crude, indicated that for the Inspiration converter a
tuyere submergence of less than 70 cm tends to generate severe splashing. On the other hand no splashing could be observed for tuyere submergences deeper than 90-100 cm.
6.1. Scope of the Heat Transfer Model

In Chapter I several operating problems which affect operating practice, such as tuyere blockage due to accretion growth at the tuyere tip, refractory wear at the tuyere line, and tuyere erosion, were described. Many of these problems are related to the injection dynamics in the tuyere region as well as to heat losses from the converter while the reactor is out of the stack.

Because accretion formation involves solidification of bath at the tuyere tip, the temperature of the refractories near the tuyere line, particularly just as the converter is rolled into the stack, can influence tuyere blockage and also punching frequency. If, while the converter is out of the stack, sufficient heat is lost through the mouth to cool the inside wall below the solidus temperature of the bath, some material will freeze against the refractories when the converter is rolled back into the stack to commence blowing.
To quantify the influence of out-of-stack time and other converter variables on the temperature distribution in the refractory wall, especially at the tuyere line, a mathematical model has been formulated. Factors such as diameter of the converter, size and position of the converter mouth and the use of a mouth cover have been studied with the model, in order to relate converting practice to operating problems.

6.2 The Mathematical Model

The model calculates the radiative heat exchange between a given surface element and the rest of the elements in the mantle and the end walls of the converter as well as the mouth of the reactor. The convective and radiative heat losses from the external surface of the vessel and conduction through the refractory wall also are included in the calculations. The model is flexible in that it allows variation in the size and position of the converter mouth. Therefore it is possible to simulate heat-transfer conditions for both Peirce-Smith (central mouth) and Hoboken (end-wall mouth) converters. Figure 6.1 schematically depicts both types of reactors.
Figure 6.1  (a) Schematic of the Peirce-Smith Converter  
(b) Schematic of the Hoboken Converter
To develop the heat transfer mathematical model, the following assumptions were made:

a) The converter is empty during out-of-stack periods.
b) The convective heat transfer inside the internal volume of the converter is negligible.
c) The heat exchange by conduction between the different elements into which the converter wall is divided is negligible.
d) The refractory is considered as a gray body.
e) The gas inside the converter is a non-absorbing and non-transmitting medium.
f) The mouth of the converter is considered as a black body at constant temperature.
g) The converter radius is large enough such that each element into which the converter internal surface is subdivided can be considered as a plane surface.
h) The size of the elements into which the converter surface is subdivided is small enough such that the shape factor angles can be considered as constants for each element.
i) The thermal conductivity of the refractory is a linear function of temperature.
6.2.1 Radiation Heat Transfer in Copper Converters

The radiation heat transfer taking place amongst the different surface elements in the copper converter can be treated conveniently in terms of the radiosity $J_i$, which is defined as the rate at which radiation leaves a given surface per unit area.

$$J_i = \lambda_i G_i + \epsilon_i E_{b,i} \quad (6.1)$$

The net rate of heat transfer from a surface $i'$ by radiation is the difference between the radiosity and the irradiation.

$$q_i = A_i (J_i - G_i) \quad (6.2)$$

In the model the refractory and the gas inside the converter are considered as a gray surface with constant emissivity and as a non-absorbing and non-transmitting medium, respectively. For gray bodies,

$$\lambda_i = 1 - \epsilon_i \quad (6.3)$$

then:

$$q_i = \frac{A_i \epsilon_i}{1-\epsilon_i} (E_{b,i} - J_i) \quad (6.4)$$
The incident radiation (irradiation) consists of the portions of radiation which impinge on $A_i$ from all the elements, and can be expressed compactly in the form

$$A_i \cdot G_i = \sum_{j=1}^{N} A_i \cdot J_{j} \cdot F_{i,j}$$  \hspace{1cm} (6.5)

Substituting Equation (6.5) for $G_i$ in Equation (6.2) yields:

$$q_i = A_i \left[ J_i - \sum_{j=1}^{N} J_j \cdot F_{i,j} \right]$$  \hspace{1cm} (6.6)

To calculate the radiation heat transfer from any one of the $N$ elements into which the converter is subdivided it is necessary to solve $N$ equations in $N$ unknowns. These equations are obtained by evaluating the emittances of the surfaces and the radiation shape factors between them and writing Equations (6.4) and (6.6) for each element. Then, in a general form:

$$\frac{q_i}{A_i} = \frac{e_i}{1 - e_i} \cdot \left[ E_{b,i} - J_{i} \right] = J_i - \sum_{j=1}^{N} J_j \cdot F_{i,j}$$  \hspace{1cm} (6.7)

with $1 < i < N$

Equation (6.7) can be recast in the more convenient form:

$$a_{i,1} J_1 + a_{i,2} J_2 + \ldots + a_{i,N} J_N = C_i$$  \hspace{1cm} (6.8)
Where the coefficients \( a_{i,j} \) and \( C_i \) are:

\[
a_{i,i} = 1 - F_{i,i} + \frac{\epsilon_i}{1 - \epsilon_i} \tag{6.9}
\]

\[
a_{i,j} = -F_{i,j} \quad (i \neq j) \tag{6.10}
\]

and

\[
C_i = \frac{E_{b,i} \cdot \epsilon_i}{1 - \epsilon_i} \tag{6.11}
\]

In the model the mouth of the converter is considered as a black surface at constant temperature. For any black surface at temperature \( T \) in the converter enclosure, the radiosity must equal \( \sigma T^4 \) and is no longer an unknown.

To simplify the calculation procedure, the following matrix representation is defined

\[
A = \begin{bmatrix}
a_{11} & a_{12} & \cdots & \cdots & a_{1N} \\
a_{21} & a_{22} & & & \\
& & \ddots & & \\
& & & \ddots & \\
a_{N1} & a_{N2} & \cdots & \cdots & a_{NN}
\end{bmatrix}
\]
The set of equations to be solved can be written then as:

\[
[A] [J] = [C]
\]  \hspace{1cm} (6.12)

If \([A]^{-1}\) represents the inverse of matrix \([A]\), the solution for the radiosities is given by:

\[
[J] = [A]^{-1} [C]
\]  \hspace{1cm} (6.13)

Once the radiosities are known, the rate of heat flow can be obtained from Equation (6.4) for each element.

To take into account the fact that the mouth of the converter is being considered as a black surface, the elements \(a_{ii}\) and \(C_i\) in the converter mouth are arbitrarily defined as:

\[
a_{ii} = \left[1 + \frac{\varepsilon_i}{1 - \varepsilon_i}\right] \times 10^9 \quad C_i = \left[\frac{E_i}{1 - \varepsilon_i}\right] \times 10^9
\]
In order to calculate the rate of heat flow from each element in the model, it is necessary to evaluate the fraction of the total diffuse radiation leaving one surface which is intercepted by another surface and vice versa. The fraction of diffusely distributed radiation leaving a surface $A_E$ that reaches surface $A_R$ is called the radiation shape factor $F_{E,R}$. The technique employed to calculate the radiation shape factor in the model is given in Appendix III.

6.2.2 Transient Conduction in the Elements of the Model

Once the rate of heat flow from each element of the model has been calculated, it is possible to evaluate the variation in temperature that takes place within each element. An implicit one-dimensional finite difference method was considered suitable to carry out the calculations. The simplest implicit formula for node 'm' in any of the elements into which the converter has been subdivided, in the absence of heat generation, is\(^97\):

$$\frac{c^0}{V_m} \left[ T_m(j+1)-T_m(j) \right] = \sum_{n=1}^{N} C_{n,m} \left[ T_n(j+1)-T_m(j+1) \right]$$

(6.14)

where 'n' denotes the nodes bordering node 'm'; $T_m(j)$ and $T_m(j+1)$ are the temperatures of node 'm' at times 'j' and 'j+1', respectively, and $C_{n,m}$ is the thermal conductance between nodes 'n' and 'm', defined as:
\[ C_{n,m} = \text{Conductivity} \times \frac{\text{Area Transverse to Conduction Path}}{\text{Length of Conduction Path}} \]

In general:

\[ C_{n,m} = K_n \times S_{n,m} \quad (6.15) \]

The thermal conductance expressions for the different types of nodes in the one-dimensional calculations are presented in Appendix IV. Rearrangement of Equation (6.14) yields:

\[ T_m(j) = T_m(j+1) \left[ 1 + \sum_{n=1}^{N} \frac{C_{n,m} t}{c \rho V_m} \right] - \sum_{n=1}^{N} \frac{C_{n,m} t}{c \rho V_m} T_n(j+1) \quad (6.16) \]

Therefore, the method requires the simultaneous solution of a set of equations at every time step.

For a general interior node, Equation (6.16), after some simplifications, can be expressed in a matrix compatible notation as:

\[ B_i = T_{i-1}(j+1) A_{i,i-1} + T_i(j+1) A_{i,i} + T_{i+1}(j+1) A_{i,i+1} \quad (6.17) \]

where:

\[ B_i = \frac{c \rho V_i}{K_i t} T_i(j) \]

\[ A_{i,i} = \frac{c \rho V_i}{K_i t} + S_{i,i-1} + S_{i,i+1} \]
\[ A_{i,i-1} = -S_{i,i-1} \quad \text{and} \quad A_{i,i+1} = -S_{i,i+1} \]

For node \( i=1 \), i.e. the node at the internal surface of one element, it is necessary to take into account the rate of radiative heat flow at each surface node. In this case the heat balance, in matrix compatible notation, is expressed as:

\[
B_1 = T_1(j+1) A_{1,1} + T_2 A_{1,2} \quad (6.18)
\]

where:

\[
B_1 = \frac{c \rho V_1}{K_1 t} T_1(j) - \frac{q}{A} \frac{A_1}{K_1}
\]

\[
A_{1,1} = \frac{c \rho V_1}{K_1 t} + S_{1,2}
\]

\[
A_{1,2} = -S_{1,2}
\]

The radiative heat flow, \( q/A \), is calculated according to the method outlined in Section 6.2.1.

For node \( i=N \), i.e. the node at the external surface of one element, it is necessary to take into account the heat losses by convection and radiation from the surface to the surroundings. In this case the heat balance in matrix compatible notation is expressed as:
\[ B_N = T_{n-1}(j+1) A_{N,N-1} + T_N(j+1) A_{N,N} \] (6.19)

where:
\[ B_N = \frac{c \rho V_N}{K_N} T_N(j) - \frac{A_N}{K_N} \left[ h_N(T_N(j) - T_\infty) + \sigma(T_N^4(j) - T_\infty^4) \right] \]

\[ A_{N,N} = \frac{c \rho V_N}{K_N} + S_{N,N-1} \]

\[ A_{N,N-1} = S_{N,N-1} \]

The convective heat transfer coefficient from the external surface, \( h_N \), is calculated by the method outlined in Appendix V.

Equations (6.17), (6.18) and (6.19) represent a set of equations for the 'future' temperature of each nodal point and the future temperatures at neighboring points. There will be as many equations as there are unknown future temperatures, and once this set of equations is solved the resulting future temperatures become the initial temperatures for the next time increment. The coefficients are always positive and the implicit technique will therefore be stable with any time increment step. In essence, the implicit technique reduces to the solution of a set of simultaneous algebraic equations at each time increment; a matrix inversion method can be used for solution by means of a digital computer.
To solve the set of equations, it is necessary to know the temperature profile under steady state conditions, that is when the converter is charged and air is being injected into the melt, as an initial condition. Also it is necessary to take into account the variation of the refractory thermal conductivity with temperature. Appendix VI outlines the procedure employed to calculate both the steady state temperature profile and the variation of thermal conductivity with temperature.

6.2.3 Computer Program

The mathematical heat-transfer model described in the previous sections was solved by using the program reproduced in Appendix VII. A flow chart illustrating the sequence of operations for the computer solution of the model is shown in Figure 6.2. Two double-precision subroutines developed by the UBC Computing Centre were used; the subroutine DSLIMP to obtain, and improve iteratively, the solution of Equation (6.12) and the subroutine SLE to solve the system of linear Equations (6.17), (6.18) and (6.19).

As was mentioned, in the case of the one-dimensional, finite-difference method, the coefficients are always positive and the implicit technique will therefore be stable with any
START

EVALUATE PARAMETERS AND CONSTANTS

COMPUTE INITIAL TEMPERATURE PROFILE IN THE REFRACTORY

COMPUTE RADIATION SHAPE FACTOR FOR THE ELEMENTS IN THE CONVERTER MODEL

\[ t = t + \Delta t \]

**NO**

CALCULATE RADIOSITIES
SOLVE EQ. (6.12) WITH SUBROUTINE DSLIMP-UBC

EVALUATE HEAT FLOW FROM EACH CONVERTER ELEMENT

EVALUATE TEMPERATURE PROFILES
SOLVE Eqs. (6.17), (6.18), (6.19) WITH SUBROUTINE SLE-UBC

PRINT OUT OF STACK TIME
HEAT FLOW RATES
REFRACTORY TEMPERATURES

**STOP**

**YES**

\[ t = t_f \]

**STOP**

Figure 6.2 Flow Chart of the Computer Program
time increment step. However, it has been pointed out\textsuperscript{98} that there is an optimum combination of the size increment step $\Delta x$, and the time increment step $\Delta t$. Thomas et al.\textsuperscript{99} indicated that this optimum occurs at smaller time steps with finer meshes and that the dimensionless parameter

$$\frac{K \Delta t}{\rho c \Delta x^2}$$

appears to remain constant with a value of roughly 0.1 at the optimum of three meshes under study. Therefore, with the values for density and specific heat of the refractory given in Table 6.1 and assuming an average refractory temperature of 800 °C, the optimum combination of size and time increment steps would be:

$$\frac{\Delta t}{\Delta x} = 18.65$$

That is, if a time step of 100 s is chosen, the size increment step has to be about 2.3 cm.

6.2.4 Range of Variables Studied in the Model

The following variables dealing with geometric and operating characteristics of the copper converting process were studied:
i) Diameter of the Converter

ii) Size of the Converter Mouth

iii) Position of the Converter Mouth

iv) Temperature of the Converter Mouth

v) Use of a Mouth Cover

By changing the position of the mouth it was possible to calculate the thermal conditions prevailing in both, central mouth, or Peirce-Smith converters, and end wall mouth, or Hoboken or Inspiration converters. The values of the variables employed in the solution of the heat-transfer model were selected to reproduce the conditions prevailing in industrial converters. Table 6.1 shows the levels of the variables as well as some constant values employed in the solution of the model.

6.3 Accuracy of the Heat-Transfer Model

As was mentioned, for purposes of modelling, the inside wall of the converter was subdivided into a number of surface elements. The accuracy of the model was calculated based on the value of the radiation shape factor of the elements. Since for an enclosure, such as that formed by the internal surface of the converter and its mouth, the radiant energy leaving the surface 'i' must impinge on the N surfaces forming the enclosure:

\[ \sum F_{i,j} = 1.0 \] (6.20)
TABLE 6.1

PARAMETERS IN THE HEAT TRANSFER MODEL

<table>
<thead>
<tr>
<th>Description</th>
<th>Values</th>
</tr>
</thead>
<tbody>
<tr>
<td>Converter Diameter, m</td>
<td>4.0</td>
</tr>
<tr>
<td></td>
<td>4.6</td>
</tr>
<tr>
<td></td>
<td>5.2</td>
</tr>
<tr>
<td>Converter Length, m</td>
<td>9.1</td>
</tr>
<tr>
<td>Converter Mouth Temperature, °C</td>
<td>30</td>
</tr>
<tr>
<td></td>
<td>400</td>
</tr>
<tr>
<td></td>
<td>600</td>
</tr>
<tr>
<td>Area of the Mouth, m²</td>
<td>4.50</td>
</tr>
<tr>
<td></td>
<td>6.24</td>
</tr>
<tr>
<td>Emissivity of Refractory Surface</td>
<td>0.4</td>
</tr>
<tr>
<td></td>
<td>0.6</td>
</tr>
<tr>
<td></td>
<td>0.8</td>
</tr>
<tr>
<td>Refractory Thermal Conductivity, W/m K</td>
<td>1.16 @ 199 C</td>
</tr>
<tr>
<td></td>
<td>1.47 @ 649 C</td>
</tr>
<tr>
<td></td>
<td>1.73 @ 1316 C</td>
</tr>
<tr>
<td>Refractory Density 101, kg/m³</td>
<td>2930</td>
</tr>
<tr>
<td>Refractory Specific Heat, J/kg K</td>
<td>960</td>
</tr>
<tr>
<td>External Surface Temperature, °C</td>
<td>190</td>
</tr>
<tr>
<td>Heat Transfer Coefficient, W/m² K</td>
<td>7.2</td>
</tr>
</tbody>
</table>
The error associated with the model calculation can therefore be defined as:

\[ e = 1 - \frac{1}{N} \sum_{i}^{N} \sum_{j}^{N} F_{i,j} \]  

(6.21)

Figure 6.3 shows the average error, and the computing cost associated with the number of surface elements into which the converter model is divided. As a compromise between the accuracy of the model and its computing cost, the converter was therefore divided into about 200 elements for all the computer runs performed during the calculations. Under these circumstances the estimated error in the calculations was always less than 4%.

6.4 Influence of the Refractory Emissivity

The emissivity of most bodies is a function of the wavelength of the radiation and of some properties of the emitting surface such as type of material, roughness and temperature. The value of emissivity ranges from zero to unity and may be regarded as a 'correction' factor representing the deviation of the actual emissive power of a surface from that of a blackbody at the same temperature. The reported emissivities of some materials show considerable variation even over a narrow range of temperatures.\textsuperscript{123,124}
Figure 6.3 Average Error and Computing Cost Associated with the Number of Surface Elements into Which the Converter Model is Subdivided
Caution therefore should be exercised in using emissivity values and it is necessary to study the sensitivity of the model to such an important parameter as the emissivity of the refractory surface. For purposes of this analysis three values of the refractory emissivity were used (0.4, 0.6 and 0.8) to calculate the temperature of the refractory at the central tuyere of a converter as a function of out-of-stack times. The results are presented in Figure 6.4, for the case of a Peirce-Smith converter 4 m in diameter and 9.1 m in length. As can be observed, the temperature at the central tuyere is almost negligibly affected by the value of the emissivity employed in the calculations. This result is not surprising if one looks at the geometry of the converter which is strikingly similar to the laboratory approximation of a blackbody, i.e. a cavity whose interior walls are maintained at a uniform temperature. The irradiation in such a cavity is equal to the emissive power of a blackbody at the same temperature (indeed this is the definition of a blackbody in practical terms). A small hole in the wall (and the converter mouth is a small hole as compared with the other dimensions of the converter) of this cavity will not disturb this condition appreciably, and the radiation escaping from the converter therefore will have blackbody characteristics dependent only on its temperature.
Figure 6.4 Temperature of a Central Tuyere in a Peirce-Smith Converter as a Function of Out-of-Stack Time for Different Values of Refractory Emissivity.
6.5 Model Predictions

The model was utilized to study the effect of changes in different variables and the implications for the copper converting operation.

6.5.1 Effect of the Converter Mouth Position

The temperature profiles calculated along the tuyere line for different out-of-stack times are relatively flat. Figure 6.5 shows the temperature of the refractory surface at the tuyere line as a function of the distance from the centre of a Peirce-Smith converter for different out-of-stack times. As can be observed, for all times being considered, the temperature difference between a tuyere facing the mouth of the converter and one close to the end wall of the reactor is less than 30 °C. Similar results are obtained for end-wall mouth vessels, Hoboken or Inspiration converters, as indicated in Figure 6.6, which shows the temperature at the tuyeres as a function of the distance from the end wall. In this case the difference in temperature between an end-wall tuyere and a tuyere facing the converter mouth is about 60 °C. Figure 6.7 compares the drop in temperature as a function of out-of-stack times for tuyeres close to the end wall of the converter and tuyeres facing the mouth of the reactor for both central-mouth and end-wall mouth converters. If one considers that the melting
Figure 6.5 Temperature Profiles along the Tuyere Line in a Peirce-Smith Converter for Different Out-of-Stack Times.

- \( T_m = 30^\circ C \)
- \( h = 7.2 \times 10^{-4} \text{ W/cm}^2 \text{ K} \)
- \( D_c = 400 \text{ cm} \)
- \( L_c = 910 \text{ cm} \)
- \( A_m = 4.5 \text{ m}^2 \)
Figure 6.6 Temperature Profiles Along the Tuyere Line in an End Wall Mouth Converter for Different Out-of-Stack Times.

- $D_c = 400$ cm
- $L_c = 910$ cm
- $A_m = 4.5$ m$^2$
- $T_m = 30^\circ$C
- $h = 7.2 \times 10^{-4}$ W/cm$^2$K
Figure 6.7 Temperature Drop as a Function of Out-of-Stack Time for End and Central Tuyeres for both Central Mouth and End Wall Mouth Converters.
point of copper mattes is about 1050 to 1100 °C, for the case of central-mouth or Peirce-Smith converters, out-of-stack times of more than about 10 minutes would generate massive accretion formation along the entire length of the tuyere line when blowing is resumed. On the other hand for end-wall mouth converters, out-of-stack times of 10 minutes would tend to produce accretion formation mainly in the region facing the mouth of the reactor.

The position of the mouth has almost no effect with respect to heat losses from the converter. Figure 6.8 shows the rate of heat losses and the total heat losses through the converter mouth as a function of out-of-stack times for both central-mouth and end-wall mouth converters. As can be seen, converters with the same geometric and operating characteristics, other than the position of the converter mouth, behave almost identically from the point of view of heat losses from the converter mouth while the reactor is out of the stack.

6.5.2 Thermally Active Zone in the Refractory

The temperature changes inside the refractory wall at different out-of-stack times can be observed in Figure 6.9. Changes in temperature are restricted to a penetration of about 6 to 8 cm, even for out-of-stack times as long as one hour. This means that for practical purposes the refractory wall of the copper converter may be considered as composed by
Figure 6.8 Rate of Heat Losses and Total Heat Lost as a Function of Out-of-Stack Times for Central Mouth and End Wall Mouth Converters.

\[ D_c = 400 \text{ cm} \]
\[ L_c = 910 \text{ cm} \]
\[ A_m = 4.50 \text{ m}^2 \]
\[ T_m = 30^\circ \text{C} \]
\[ h = 7.2 \times 10^{-4} \text{ W/m}^2\text{K} \]
Figure 6.9 Temperature Profiles Inside the Refractory Wall at Different Out-of-Stack Times for an Element Facing the Mouth of a Peirce-Smith Converter.

- \( D_c = 400 \text{ cm} \)
- \( L_c = 910 \text{ cm} \)
- \( A_m = 4.5 \text{ m}^2 \)
- \( T_m = 30 ^\circ \text{C} \)
- \( h = 7.2 \times 10^{-4} \text{ W/cm}^2 \text{ K} \)
two regions. One, the zone 6 to 8 cm thick mentioned above, is subjected to a thermal cycle of temperature decrease during the out-of-stack periods followed by a rise in temperature as blowing is resumed. The other zone, beyond this internal unsteady state zone, is characterized by a refractory temperature that does not change with changes during the converting cycle. Once the temperature profile inside the refractory wall has developed, this external region of the refractory maintains this temperature profile during the rest of the converter campaign.

6.5.3 Effect of the Converter Diameter

Changes in the diameter of the copper converter do not affect significantly the thermal response of the reactor. Figure 6.10 depicts the surface temperature of a tuyere facing the mouth of a Peirce-Smith converter as a function of the out-of-stack time. In the calculations three different converter diameters—400 cm, 460 cm and 520 cm—have been considered. Perhaps the most interesting effect of an increase in the converter diameter is the consequent increase of the time for accretion formation upon resumption of blowing. For the 400-cm diameter case the temperature at the central tuyere of the converter drops to 1050 °C after about 12 minutes of the converter being out of the stack. For a converter with a diameter of 460 cm the temperature at the tuyere line of the
Figure 6.10 Surface Temperature as a Function of Out-of-Stack Time for Three Different Diameters of a Peirce-Smith Converter.
reactor is above the temperature of accretion formation for out-of-stack times of up to 17 minutes. For the largest diameter considered in the calculations, blowing could be resumed after about 20 minutes without extensive accretion formation at the tuyere line.

6.5.4 Effect of the Area of the Converter Mouth

An increase in the area of the mouth of the converter does not affect the shape of the temperature profiles along the tuyere line as shown in Figure 6.11 for the case of a Peirce-Smith converter. In the calculations two converter mouth areas have been considered to calculate thermal profiles for out-of-stack times of 300 s and 1800 s. Although it does not significantly change the shape of the temperature profiles, an increase in the area of the mouth obviously increases the total heat losses from the reactor as well as the drop in temperature at the refractory surface. Figure 6.12 indicates that a mouth area of 4.5 m² decreases the temperature at a central tuyere in the converter down to 1050 °C in about 12 minutes. The same reduction in temperature for a mouth area of 6.24 m² takes place in a matter of 8 minutes. Obviously, large converter mouth areas will tend to generate formation of accretion at the tip of the tuyere faster than smaller mouth areas.
Figure 6.11 Temperature Profiles along the Tuyere Line in a Peirce-Smith Converter for Two Converter Mouth Areas.
Figure 6.12 Predicted Tuyere Line Temperature and Total Heat Losses in a Peirce-Smith Converter Plotted Against Out-of-Stack Time for Two Mouth Sizes.

$D_c = 400 \text{ cm}$
$L_c = 910 \text{ cm}$
$T_m = 30^\circ \text{C}$
$h = 7.2 \times 10^{-4} \text{ W/cm}^2 \text{ K}$

$A_m = 4.5 \text{ m}^2$
$A_m = 6.24 \text{ m}^2$
6.5.5 Influence of the Coverage of the Mouth

The most important factor affecting the heat exchange in the converter is whether the mouth of the converter is covered or not while it is out of the stack. Figure 6.13 shows the influence of placing a cover over the mouth of the converter while it is out of the stack. Both the tuyere line temperature drop and total heat losses are dramatically reduced by covering the converter mouth. After half an hour out of the stack with the mouth covered, the tuyere line remains well above 1050 °C as compared with the case of an uncovered mouth where the temperature drops to 1050 °C in about 10 min. The effect of covering the mouth is even more impressive from the standpoint of total heat losses from the converter, which with the mouth covered, are decreased by a factor of four, as can be observed in Figure 6.13.

The coverage of the mouth not only reduces the drop in temperature of the refractory surface and the total heat losses from the reactor. Possibly its most important effect is the reduction in thermal gradients through the refractory at the inside wall as shown in Figures 6.14 and 6.15 for the Peirce-Smith and Hoboken converters respectively. In both cases, with the converter mouth uncovered, the temperature gradient at the inside wall swings from negative to positive by 40-50 °C/cm during an out-of-stack period of about 10 min.
Figure 6.13 Tuyere Line Temperature and Total Heat Losses Plotted Against Out-of-Stack Time for a Peirce-Smith Converter with the Mouth Covered and Uncovered.
Figure 6.14 Temperature Gradient Through the Refractory at the Inside Wall in a Central Mouth Converter Plotted Against Out-of-Stack Time.
Figure 6.15 Temperature Gradient Through the Refractory at the Inside Wall in an End-Wall-Mouth Converter Plotted Against Out-of-Stack Time.
On the other hand, coverage of the mouth halves the change in temperature gradient which remains negative. The large change in thermal gradient generates stresses in the refractory, as mentioned earlier, which could contribute to tuyere line refractory erosion. It may be noted that in the Hoboken converter, the through-thickness temperature gradient at the tuyere line remote to the mouth, Figure 6.15, is considerably less than that in the Peirce-Smith converter, Figure 6.14. If thermal cycling is a significant factor in tuyere line erosion, refractory wear would be greater in the mouth region of a Hoboken converter than near the opposite end when the mouth is uncovered while out of stack. In the Peirce-Smith converter the wear would be more uniform along the tuyere line.

In the calculations carried out to evaluate tuyere line temperature and total converter heat losses with the mouth covered while out of the stack, Figure 6.13, the panel covering the mouth of the converter was assumed to be initially at room temperature. In the model the temperature of the panel subsequently increases as the refractories in the mouth cover are heated by the radiation from the inside wall of the reactor. Figure 6.16 shows the total heat losses from the reactor while out of the stack for three different initial temperatures of the panel covering the mouth of the converter, namely room temperature, 400°C and 600°C.
Figure 6.16 Total Heat Losses from a Covered-Mouth Converter as a Function of Out-of-Stack Time for Three Different Temperatures of the Mouth Cover.
As can be observed in Figure 6.16, an increase in the temperature of the mouth to 600 °C reduces the total heat losses by a factor of two as compared to the case of the mouth cover at room temperature as previously assumed.

The coverage of the mouth of the converter is also beneficial from the standpoint of savings in energy of the burners employed to keep the converter at working temperature while out of the stack. Figure 6.17 compares the fuel-oil consumption rate for the cases of converters with and without mouth covers. Again three temperatures are considered for the refractories in the panel covering the mouth. The results are expressed in kg/hr of fuel-oil necessary to compensate for the total heat losses from the converter as a function of the exit gas temperature. When the mouth of the converter is not covered while out of the stack, the fuel-oil consumption is about 140 kg/hr if the exit gas temperature is assumed to be 1000 °C. This consumption of fuel-oil can be reduced by a factor of three to four when the converter mouth is covered.
Figure 6.17 Fuel-Oil Rate Consumption as a Function of Exit Gas Temperature for Uncovered and Covered-Mouth Converters.
CHAPTER VII

DISCUSSION

7.1 Injection Dynamics at the Tuyere Tip

The pressure traces obtained from the tuyeres of the laboratory model and the operating copper converters were carefully studied because it was believed that hitherto unknown differences in injection behaviour could be discerned. It was also considered important to learn to 'read' the pressure traces in a diagnostic sense, so that in future, the pressure measurements could be used as a quick and easy check on injection behaviour which could influence process performance.

In the pursuit of these goals, three aspects of the pressure traces were examined and compared:

i) the shape of the pressure pulses

ii) the frequency of pulses, and

iii) the duration of the constant-pressure intervals separating pulses.

Looking first at pulse shape, the measurements of Hoefele and Brimacombe showed that a sharp rise in pressure at the tuyere tip occurs when liquid washes against the tuyere, while
a decline in pressure corresponds to the growth of a bubble at the tuyere tip. As was mentioned in Section 2.3 the theoretical basis for the link between tuyere pressure and bubble formation has been set out elsewhere \(^{81,82}\). In summary, a classical bubbling regime is characterized by a pressure trace in which a sudden pressure rise is followed by a slower decline as shown schematically in Figure 7.1. No intervals of constant low pressure are observed in this classical bubbling regime.

The second aspect of the pressure traces to be considered, i.e. the frequency of the pressure pulses, is important because it affects the size of the bubbles. Bubble volume and equivalent radius must be considered for the copper converter because the horizontal tuyeres are separated by only about 200 mm (Table 4.3). Thus, as Hoefele and Brimacombe \(^{31}\) have suggested earlier, interaction among bubbles growing at adjacent tuyeres is possible.

The third characteristic of the pressure traces, the periods of constant, low pressure between pulses, is likely caused by such interaction among adjacent tuyeres. These periods of low pressure are not caused by jetting because the tuyere pressure and air velocity are too low.
Figure 7.1 Idealized Tuyere Pressure Traces for Three Regimes of Gas Discharge from Horizontal, Closely Spaced Tuyeres.
The interaction among adjacent tuyeres was confirmed by the high-speed observations carried out in the water model. As can be observed in Figure 5.9 the coalescence of bubbles discharging from several adjacent tuyeres creates a horizontal unstable gas envelope covering several tuyeres simultaneously; at the same instant other tuyeres are working more independently. Thus for part of the time a given tuyere feeds this unstable envelope with little resistance from surrounding liquid, and the tuyere pressure remains low until the envelope locally collapses. At this point the pressure sharply rises then drops as a gas bubble begins to form again. The bubble continues growing until it either detaches from the tuyere tip, in which case a new bubble starts to grow, or until it coalesces with an adjacent bubble whereupon the pressure drops to the previous low level. Thus, with such tuyere interaction the duration of the bubble growth period is reduced and the pressure pulse becomes more symmetrical than is found with classical bubble growth from non-interacting tuyeres. An idealized pressure trace resulting from tuyere interaction is shown in Figure 7.1. The interaction between tuyeres can also be ascertained by studying pressure traces coming from adjacent tuyeres, as indicated by Figure 5.7. The rapid change from interactive to non-interactive injection conditions is also an indication of the instability of the gas-envelope covering several tuyeres simultaneously making interaction between them possible.
In addition, the question of bubble size relative to tuyere submergence has to be considered since gas channeling to the surface of the bath is a possibility. If the tuyere submergence is shallow, relative to the bubble size, bubbles may break through to the surface of the bath before disengaging from the tuyere tip so that for most of the time there is little resistance to gas flow and the tuyere pressure is primarily low. Periodically, this gas channel to the surface collapses and causes a pressure pulse; but due to the proximity of the tuyere to the bath surface, the channel is reopened quickly so that the pulse is symmetrical and of short duration, as depicted in Figure 7.1.

Comparison of Figure 2.5(d), from the slag fuming furnace measurements carried out by Richards and Brimacombe, and Figure 7.1 reveals that in the full slag fuming furnace (deep tuyere submergence, viscous bath), the air discharges in the form of bubbles from the individual tuyeres. This is confirmed by the single-nozzle bubble model of Davidson and coworkers which predicts a frequency of 6 s\(^{-1}\) for the slag-fuming injection conditions. This is in excellent agreement with the measured value of 5 to 6 bubbles per second. Thus although closely spaced, each tuyere operates independently like the single tuyere employed by Hoefele and Brimacombe to inject gases into mercury which gave rise to the
bubbling behaviour shown in Figure 2.3. The lack of interaction among adjacent tuyeres is perhaps surprising since with a bubbling frequency of $5 \text{s}^{-1}$ at a gas flow rate of $3.6 \text{ to } 4.4 \text{ Nm}^3/\text{min per tuyere}$ the radius of an equivalent spherical bubble at ambient temperature is $150 \text{ mm}$ which is larger than the half-spacing between tuyeres of $100 \text{ mm}$. The apparent lack of coalescence may be due to the bubbles being elongated vertically, but more importantly may be related to the high viscosity of the slag which is about $0.5 \text{ N s m}^{-2}$ (5 poise). The high viscosity retards drainage of the liquid film separating adjacent tuyeres. An injection diagnosis for the full slag fuming furnace is summarized in Figure 7.2.

Turning to the copper converter, comparison of Figures 5.16(b) and 7.1 suggests that bubbles growing from adjacent tuyeres submerged $500 \text{ mm}$ are coalescing to form an unstable gas envelope as depicted schematically in Figure 7.3. For the time interval of one second shown in Figure 5.16(b), tuyere discharge into the unstable gas envelope (constant low pressure part of the trace) occupies about $0.8 \text{ second}$. There are several reasons to expect this injection behaviour. Firstly, if classical bubbling occurred at each tuyere with a frequency of $4 \text{s}^{-1}$ while blowing at $11.8 \text{ Nm}^3$ per minute, the radius of an equivalent spherical bubble at ambient temperature would be $230 \text{ mm}$ which is more than double the tuyere half-spacing
Figure 7.2 Summary of Injection Behaviour in the Full Slag Fuming Furnace, as Diagnosed from Dynamic Pressure Measurements in a Tuyere.
Figure 7.3 Summary of Injection Behaviour in the Copper Converter with 254 Charges and 500 mm Tuyere Submergence, as Diagnosed from Dynamic Pressure Measurements in a Tuyere.
of 100 mm. This bubble size is considerably larger than that calculated for the slag fuming furnace. Secondly, the viscosity of the bath in the converter is only $10^{-2} \text{ N s m}^{-2}$, fifty-fold less than that of the fuming furnace slag, so that coalescence of bubbles at adjacent tuyeres is more favorable. Thirdly, the converter had already processed 254 charges with the result that the refractory at the tuyere line had eroded to form a typical V-shaped notch. This tends to act as a horizontal cavity providing a measure of stability for the gas envelope. Such a stabilizing notch is not found in the slag fuming furnace because, being water-jacketed, refractories are not required as a lining. A summary of the injection behaviour diagnosed for the 'middle-aged' copper converter with 500 mm tuyere submergence is given in Figure 7.3.

Further evidence of the influence of the state of the tuyere pipe and the refractory at the tuyere line on injection dynamics may be gained by comparing the pulse frequency of about $4 \text{ s}^{-1}$ measured in the middle-aged copper converter with the values of $14 \text{ s}^{-1}$ and $10$ to $12 \text{ s}^{-1}$ measured in the tuyeres of freshly relined copper and nickel converters, respectively. Pressure traces from freshly relined nickel and copper converters are strikingly similar as can be compared from Figures 2.4 and 7.4, respectively. In both cases the diameter of an equivalent spherical bubble forming at the tuyere tip
Figure 7.4 Summary of Injection Behaviour in a Copper Converter with New Refractory Lining, as Diagnosed from Dynamic Pressure Measurements in a Tuyere
is over 200 mm, again double that of the tuyere half-spacing so that coalescence of bubbles at adjacent tuyeres is likely. But the overall duration of low-pressure intervals, indicative of multiple tuyere interaction, is only about 50 to 60 pct of the time studied as compared to 80 pct in the middle-aged copper converter. This is because in the new converters the tuyere pipes protrude 150 mm or more beyond the refractory lining, so that there is no notch to provide stability to the horizontal envelope. A summary of the injection behaviour in a freshly relined converter is presented in Figure 7.4.

It may be that gas bubbles discharging from the protruding tips of adjacent tuyeres tend to coalesce less readily so that the horizontal gas envelope is not well established and tuyeres operate more independently. Thus the intervals of low pressure are reduced and the pulse frequency is high as bubbles subjected to the influence of both buoyancy and bath motion discharge from the tuyere. After the tuyeres burn back to the wall, the refractory itself may play a role in enhancing tuyere interaction through surface tension effects. If in the early life of the lining, the refractory is not wetted by the bath, bubbles discharging from a given tuyere can easily spread laterally along the wall to link up with adjacent bubbles and form the horizontal envelope. This has the effect of reducing the resistance to gas flow for longer time
periods which lengthens the intervals of low pressure and decreases the pulse frequency measured in the tuyere. Later on as the notch at the tuyere line deepens due to refractory erosion, the horizontal envelope is stabilized further. Thus surface tension effects also may strongly influence injection behaviour in the copper converter.

The V-shaped notch which forms in the refractory along the tuyere line after several charges also may have the effect of reducing the bath circulation velocity over the tuyere tip. If this reduction in velocity takes place a reduction in bubbling frequency should be expected as Ashman et al. have shown from the results of a mathematical model developed to describe bubble formation at the tuyeres of a copper converter. The reduction in bath circulation velocity induced by the notch was simulated in the laboratory model. Two deflectors were placed along the tuyere line in the model, the effect of which was to locally reduce the bath circulation velocity over the tuyere tip and also to confine the discharging gas. As can be observed from figures 5.1 to 5.3 for a low gas flow rate, the effect of the deflector is to reduce the frequency of pressure pulses by 6 to 7 pulses per seconds. At higher air flow rates this reduction is less pronounced.
Finally, examining Figures 2.5 (b), 5.8 (b), and 5.16 (a) obtained with shallow tuyere submergences in the fuming furnace, the laboratory water model and the copper converter, respectively, the relatively constant pressure trace interrupted periodically by pressure bursts is indicative of gas channeling, as described earlier. In this case the gas channeling likely prevents either bubble formation or multiple tuyere interaction. A summary of these results is shown in Figure 7.5. Gas channeling is likely to be detrimental to productivity at least from the standpoint of decreased oxygen utilization because the residence time of the air in the bath and the bath/gas contact area both may be reduced. Indeed the Tacoma Smelter, where a shallow tuyere submergence of about 300 mm is standard practice, has reported one of the lowest oxygen efficiencies, 63 pct, in the worldwide compilation of converter practice by Johnson et al. However other factors also must have an influence because a good correlation between tuyere submergence and oxygen efficiency could not be found from the compilation.
Figure 7.5 Gas Channelling to the Surface of the Bath in the Copper Converter with 300 mm Tuyere Submergence, as Diagnosed from Dynamic Pressure Measurements in a Tuyere.
7.2 Bath Movement and Slopping

The laboratory work indicated that a shallow tuyere submergence has the disadvantage of leading to bath slopping at lower levels of buoyancy power (gas flow rates) as well as the disadvantage of causing gas channeling described earlier. The extrapolation of the model results to operational conditions also indicated that most converters are being operated at the maximum air flow rate close to the point of bath slopping.

Bath slopping correlates well with buoyancy power per unit mass of bath and not with the kinetic power, which numerically is more important, or with the total power per unit mass being incorporated into the system. Abramovich\textsuperscript{130} has pointed out that the kinetic energy is rapidly dissipated in the immediate vicinity of the tuyere and thus does not contribute significantly to the stirring energy of the bath. As a consequence during submerged injection the stirring power per unit mass of bath can be expressed as:

\[ \epsilon_s = \epsilon_b + a\epsilon_k \]  

(7.1)

in which the factor \( a \) varies from 0.04 as proposed by Abramovich\textsuperscript{130}, to 0.06 as reported by Lehrer\textsuperscript{131} and to 0.15 according to Haida and Brimacombe\textsuperscript{104}.  

It has been pointed out\(^{104}\) that Equation (7.1) is useful as a scale-up criterion, because it incorporates the important energy terms that are responsible for fluid flow and mixing. The results of the present work provide supporting evidence to this idea, because a good correlation is obtained between buoyancy power and tuyere submergence, without taking into account the kinetic power contribution. This, in addition, suggests that the factor \(a\) in Equation (7.1) is relatively unimportant for horizontal submerged injection. Further evidence of the usefulness of Equation (7.1) as a scaling criterion is found when the extrapolation of the critical slopping line obtained in the laboratory agrees well with data from industrial converter operations.

At this point however it is necessary to emphasize the fact that to extrapolate the critical slopping line to industrial operations the buoyancy power in the converter was calculated under standard conditions. This means that the work done by the thermal expansion of the air when contacted with the molten matte is being neglected. Also for the evaluation of the buoyancy power per unit mass of bath (assumed to be blister copper) a 40 pct. filling of the converter was considered. Therefore, it is clear that more work has to be performed to further clarify the effect of parameters such as bath temperature, tuyere diameter, tuyere angle and other
variables on bath slopping in the laboratory. Once these effects have been elucidated it will be possible to delineate a comprehensive blowing practice for an industrial operation.

It is also necessary to consider the definition of the modified Froude number, which since the pioneering work by Themelis et al\textsuperscript{26} has been widely used to describe the injection of gas into a liquid. The modified Froude number, described as the ratio of gas kinetic energy to gas potential energy due to buoyancy force has a clear meaning when it is used to describe the trajectory of side blown gas injection where the former and the latter energies rule the horizontal and the vertical motion of the gas, respectively.

The buoyancy energy can be expressed as:

\[ E_b = P_a V_o \ln \left( \frac{P_a + \rho_1 g h}{P_a} \right) \]  \hspace{1cm} (7.2)

For the case when the volume change of the gas is negligible small, i.e. $P_a \gg \rho_1 g h$, the buoyancy energy is given by:

\[ E_b = (\rho_1 - \rho_g) V_o g h \]  \hspace{1cm} (7.3)

On the other hand, the kinetic energy is:

\[ E_k = \frac{1}{2} V_o \rho g u_o^2 \]  \hspace{1cm} (7.4)
And the modified Froude number is:

\[ \text{Fr} = \frac{E_k}{E_b} \]  

(7.5)

By introducing Equations (7.3) and (7.4) into Equation (7.5):

\[ \text{Fr} = \frac{\rho g}{2(\rho_1 - \rho g)} \frac{u^2}{g h} \]  

(7.6)

The modified Froude number defined by Equation (7.6) is similar to the modified Froude number defined in Equation (4.1) with the difference that the nozzle diameter has been replaced by the tuyere submergence as the characteristic length. Both definitions become equivalent as far as the dimensional similarity of \( d_o \) and \( h \) is maintained.

On the other hand, the liquid disturbances that can generate splashing depend on the ratio between the inertial force of the jet disturbing the surface and the force of gravity tending to flatten it out. Therefore it seems more appropriate to study this phenomenon in terms of the Froude number of the system instead of using the modified Froude number as before.
7.3 Accretion Formation and Tuyere Blockage

The observations of accretion growth as well as the X-ray diffraction analysis of the accretion samples indicate that the mechanism of accretion formation is basically the solidification of bath around the tuyere tip, rather than, say, the generation by chemical reaction of a solid phase like magnetite. That the accretion commences to grow mainly in the lower region of the tuyere can be explained simply by buoyancy effects. Liquid, which in the vicinity of the tuyere is driven upward by the ascending bubbles, presses in on the discharging gas at the bottom of the tuyere and is solidified by the cold gas. At the same time, owing to the low value of the modified Froude number, the upper area of the tuyere is surrounded predominantly by rising gas which also is directed upward by the growing accretion. Whether the accretion grows inside the tuyere or commences to build up just at the tip and grow outward is not clear. However, the latter is most likely based on observations through the converter mouth of the tuyere line after it had been rolled up out of the bath. An example of a pipe-shaped accretion, taken from a converter from Noranda, is shown in Figure 2.7.
The bright, hotter areas in the upper part of the accretion, seen in Figures 5.18 (e) and (f), are consistent with a solidification mechanism. Being the last to freeze, they are thinner and able to conduct more heat to the cooling inside surface of the accretion from the bath than the thicker region at the base of the accretion. The striations observed in the accretions may have similar origins as the 'onion' layered structure observed by Davis and Magny\textsuperscript{107} in their cast-iron accretions. It seems most likely that this structure arises from the solidification of successive layers of material as the bath periodically washes against the accretion, and is not due to droplets of liquid being propelled into the tuyere as suggested by Davis and Magny.

The role of tuyere punching in the formation of accretions is very important. Factors such as the punching system being employed, the punching frequency, and the shape of the puncher head could profoundly influence the dynamics of fluid flow at the tuyere tip.

From the results in Figure 5.23 it is clear that the punching system employed affects the flow regime at the tuyere pipe and therefore possibly also the process of accretion growth.
The punching frequency may also affect the rate of accretion growth. During punching, depending on its shape, the puncher head virtually may fill the tuyere cross-section blocking the flow of air and allowing bath to wash against the tuyere tip. Then as the punch rod is withdrawn it may suck liquid a short distance into the tuyere providing an anchor for subsequent accretion growth. Presently in every smelter the punching of tuyeres is a non-selective operation, that is all the tuyeres are punched whenever a drop in total gas flow rate into the converter is observed. Under these circumstances some of the tuyeres indeed are blocked while the rest are open. Then, the indiscriminate punching practice implies that in some cases the punch rod is being inserted into open, unblocked tuyeres, generating unnecessarily in this way the washing/sucking process above mentioned which will contribute to the acceleration of the accretion formation process. A selective tuyere punching operation then seems to offer an advantageous alternative. This selective punching can be easily developed taking advantage of the differences in the pressure signals from the tuyeres when they are blocked, Figure 5.17 (b), as compared to the case when the tuyeres show no accretion formation.
The shape of the puncher head also could profoundly affect the dynamics of fluid flow at the tuyere tip at the instant of punching and thereafter as the punch rod is withdrawn, via the blocking/sucking mechanism explained above. Since the puncher head used in all the converters under study were round with a diameter close to the inside diameter of the tuyere, punching practice could have contributed to the initial stages of accretion formation. Alternative designs of puncher heads such as a chisel shape, therefore, may offer certain advantages\textsuperscript{132}.

The growth of accretions in the slag fuming furnace\textsuperscript{85} takes about 200 seconds for complete coverage, similar to the coverage time in the copper converter. However it must be emphasized that this time is not meaningful for normal slag fuming operations because the coal to the tuyere had been turned off in order to visualize the tuyere tip. With coal feeding, tuyere blockage is relatively infrequent, possibly because the pulverized coal abrades the accretion or the coal partially combusts generating sufficient heat to prevent local bath freezing. The possibility that solids injection could prevent tuyere blockage at least for part of the converting cycle warrants further investigation. It may be that siliceous flux currently added to the bath surface with a flux gun could be injected through the tuyeres and thereby prevent tuyere blockage in the slagmaking stage of converting. Analysis of
the accretion samples from the fuming furnace indicated that, as in the case of the copper converter, the accretion and bath had similar compositions. Thus, the accretion also forms by a freezing mechanism.

At the oxygen levels normally employed in copper converting, the enrichment of the blast does not prevent accretion growth. Nevertheless, oxygen-enrichment affects the accretion formation process. In converters in which oxygen-enriched air is used the accretions are porous and can be easily removed by punching. In contrast, in converters without oxygen enriched air, the accretions are more dense and stable. This may indicate that enriched air produces a more intensive oxidation at the region close to the tip of the tuyeres. Therefore a greater heat generation may be expected in this zone, with higher bath temperatures and thinner and more porous accretions, as compared with the case of no-oxygen enrichment of the blast. This suggests that the formation of accretion around the tuyeres of the copper converter may be controlled by adjusting the oxygen level in the blast, as is currently practiced in the steel industry. Indeed control of oxygen enrichment of the blast, coupled with injection at higher pressures\(^{33,34}\) offers an alternative to conventional low-pressure injection practice so that conditions can be adjusted, depending on matte grade and other variables of the process, to build up accretions around the tuyeres to protect the adjacent refractory and thereby prolong lining life.
7.4 Heat Losses from the Converter

The excessive wear observed at the tuyere line of the copper converter and the region facing the charging mouth, is related to the charging-blowing practice of the operation under consideration. At most smelters it is not uncommon for a converter to be out of the stack for periods of about sixty minutes or longer; typical turnaround times are also quite prolonged. Under these circumstances large heat losses by radiation through the mouth of the converter occur, as the heat transfer model developed in the present work clearly shows. These heat losses through the mouth of the converter cause the inside wall to cool rapidly which leads to freezing at the tuyere line and tuyere blockage when blowing is resumed. The rapid change in temperature, localized within 60 to 80 mm of the inside wall, may generate severe thermal stresses which contribute to the refractory wear at the tuyere line.

In order to solve, or at least reduce, the problem of excessive refractory wear at the tuyere region, three major aspects of the converting operation should be considered. Firstly, it is obvious that out-of-stack times as well as turnaround times should be kept at minimum levels. In this way the thermal cycling at the inside surface of the refractory would be less frequent with the consequent reduction of thermal stresses and refractory wear.
To reach this goal the complete operation at the converter aisle has to be optimized in order to maximize crane utilization and reduce the dead times in a converter. When it is unavoidable to keep a converter out of the stack burners, big enough to compensate the converter heat losses, should be used even for out of stack times as short as ten minutes.

Secondly, it is extremely advisable to implement the coverage of the mouth as a routine practice when the converter is out of the stack, during turnaround times, and even during the start-up period of a recently relined converter. If this practice is implemented the thermal gradients at the inside wall can be reduced by a factor of two and therefore the wear of the refractory at the tuyere line also can be reduced. Moreover, the coverage of the mouth makes unnecessary the use of burners inside the converter even for long out-of-stack times. It is entirely possible that the savings in fuel oil alone would pay for any modification made to the converter in order to implement the practice of mouth coverage.

Finally, the reaming practice in use at some smelters has to be considered from the point of view of thermal stress in the refractory wall while the converter is out of the stack. In some smelter the tuyeres are reamed ('dressed') between converter cycles in order to keep the tuyere pipes clean and to obtain high air flow rates during blowing. This
practice, although it may be helpful to meet the design blast air setpoint, could be extremely detrimental to the operation, at least from the standpoint of refractory life. In one of the smelters visited during the present work massive damage of the refractory during reaming was observed. Pieces of, or complete bricks were observed to break off as a consequence of the reaming bar being inserted through the tuyeres. Without doubt reaming is more deleterious when applied to a converter after a long out-of-stack period due to the thermal stresses induced at the inside surface of the refractory wall. If the reaming practice is to be pursued it should be performed immediately after blowing or after a burner has generated an even thermal field in the refractory surrounding the tuyere pipe.
8.1 Summary

The injection dynamics and related accretion build up as well as bath motion and heat losses in the copper converter have been investigated. The studies have involved physical and mathematical models coupled with plant trials at four copper smelters to examine gas discharge dynamics, bath slopping and heat transfer within the converter.

The laboratory work, carried out in a $1/4$th scale model of a Pierce-Smith converter has shown that there is significant interaction amongst adjacent tuyeres such that an unstable envelope of gas exists at the tuyere line, covering several tuyeres simultaneously. Each tuyere feeds into the envelope which breaks down periodically to release gas bubbles. The frequency of the discontinuous discharge of air into the bath increases with gas flow rate and is affected by the bath circulation velocity close to the tuyere line. Bath slopping measurements have indicated that slopping is dependent on tuyere submergence and the buoyancy power input to the bath from the rising gas bubbles. Shallow tuyere submergence gives rise to slopping at lower levels of buoyancy power per unit
mass of bath and therefore is undesirable. Slopping conditions can be delineated on a 'slopping-behaviour' diagram and it is useful to examine industrial converter operations in this light.

The industrial trials investigated the injection dynamics and related accretion build-up at the tuyeres of operating Peirce-Smith, Hoboken and Inspiration converters. A tuyerescope attached to the back of a tuyere has permitted the direct observation of accretion formation and the sampling of accretions during blowing. Dynamic pressure fluctuations in the tuyere due to injection behaviour have been measured with a piezoelectric transducer. The trials indicated that, under normal conditions, the Peirce-Smith, the Hoboken and the Inspiration converters operate in the bubbling regime. Careful examination of the shape and frequency of the pressure pulses and the duration of periods of low pressure in the tuyere, sensed by the piezoelectric transducer, revealed that in non-ferrous submerged injection processes the interaction between tuyeres depends on the bath viscosity, state of the refractory at the tuyere line, and tuyere submergence. In high viscosity baths, such as that in the slag fuming furnace, individual tuyeres act independently and the gas discharges in the classical bubbling regime. In the copper converter with relatively deep tuyere submergences there are long periods in which the
tuyere discharges into a horizontal unstable envelope formed by the coalescence of bubbles at adjacent tuyeres. This behaviour is enhanced by the relatively low viscosity of the bath, and for the case of middle-aged converters, the V-shaped notch at the tuyere line resulting from refractory erosion that stabilizes the horizontal gas envelope. If the tuyere submergence is shallow, such as 300 mm in the copper converter, bubbles may break through the bath surface before detaching from the tuyere so that the gas forms a vertical channel. This may lead to poor oxygen utilization in the copper converter. The piezoelectric measurements also indicated that the pressure traces from individual tuyeres provide a method to ascertain whether the tuyere is blocked or not. This could be used to develop a selective punching operation, and eliminate the indiscriminate punching practice employed nowadays in every smelter which accelerates the accretion formation process. The tuyere pressure measurements also revealed a decrease of the pulse frequency with increasing number of charges processed by the converter. This is related to the converter relining practice and again to the V-shaped notch which forms at the tuyere line after several charges.
Analysis of the accretion samples revealed that the accretions in the copper converter form by a solidification mechanism. The accretions were observed to grow upward from the bottom of the horizontal tuyere and to cover the tip within 60 to 200 seconds. Protruding parts of the accretion sometimes disappeared then reappeared suddenly as the bath washed over the accretion then receded. Striations visible on the accretions may be caused by successive layers of bath freezing onto the accretion due to the periodic washing of the tuyere tip. The role of tuyere punching in the early stages of accretion formation could not be determined but could be important if the puncher head blocks the air flow during punching or sucks liquid back into the tuyere during withdrawal. The type of puncher used in the converter affects fluid flow conditions inside the tuyere pipe and this could have an influence on accretion formation at the tuyere tip. It is possible that the formation of accretion around the tuyeres of the copper converter may be controlled by adjusting the oxygen level in the blast, as is currently practiced in the steel industry, in order to protect the adjacent refractory and thereby prolong lining life. The fact that accretion build-up is not a problem with coal feeding in the slag fuming furnace may indicate that tuyere blockage in the copper converter could be prevented, at least for part of the converting cycle, by injecting powdered siliceous flux through the tuyeres.
The mathematical heat-transfer model has indicated that when the converter is out of the stack, heat losses through the mouth of the converter cause the inside wall to cool rapidly which may lead to freezing at the tuyere line and tuyere blockage when blowing is resumed. The temperature gradient into the refractory at the inside wall can change by up to 50 °C/cm within the first 10 minutes of the converter being out of the stack. The rapid temperature change is localized to within 60-80 mm of the inside wall and may contribute to the refractory wear at the tuyere line. Covering the converter mouth during out-of-stack periods markedly reduces the change in through-thickness temperature gradient at the inside wall. Moreover, the coverage of the mouth makes unnecessary the use of burners inside the converter even for long out-of-stack times.

8.2 Suggestions for Further Work

It is hoped that the present thesis has shed light on many aspects of the copper converter practice. From the results obtained throughout the work some ideas have emerged that merit further research and development.

In the laboratory model it is necessary to conduct a more complete study on the effect of several injection
variables on bath motion. A possible correlation between the oscillatory disturbances of the bath surface and the Froude number of the system should be investigated. This would help to develop a definite criterion to control slopping and splashing in the copper converter.

It is also necessary to carry out a set of in-plant trial in which the minimum air pressure required for the punchless operation of tuyeres in a copper converter can be determined for different levels of oxygen enrichment of the blast and different tuyere diameters. For a given tuyere diameter, the trial would likely proceed as follows. Normal air at say 60 psig would be injected initially to ensure that the tuyere does not block. This pressure would be lowered steadily until accretions begin to form. The pressure would be raised until no accretions were formed. Next the air would be enriched in oxygen and again the minimum pressure at which accretions are prevented would be defined. The air enrichment would be increased and the procedure repeated, and so on. The plant trials should also help to verify the heat transfer mathematical model before a further modelling work is done.
The heat transfer mathematical model should be further developed in two main areas. Firstly, it is necessary to obtain a more quantitative information about thermal stresses being generated at the tuyere line of the copper converter. Secondly, the model should be able to predict the shape and size of the accretions which will form at the tuyere line once the converter is turned back to the stack and blowing is resumed.
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APPENDIX I

THE PLATE ORIFICE

A thin-plate square-edged orifice was used to measure the air flow rate into the system. The orifice was designated two inches, corresponding to the nominal I.D. of the pipe in which it was installed. Corner taps were used, as suggested by Spink for pipe diameters less than 51 mm. For other tap locations there is the possibility that the low-pressure tap will be downstream from the vena contracta, in a highly turbulent region where the standard discharge coefficients would not apply.

The most important orifice dimensions are discussed in detail in the A.S.M.E. report on fluid meters. In the following paragraphs the recommendations of this report are considered and related to the design of the orifice used. The actual dimensions of the device are listed in Table I.1.

Two different diameter ratios, defined as the ratio between the orifice diameter and the inside diameter of the pipe, were used. A diameter ratio of 0.6 was employed when measuring relatively high gas flow rates, whereas a diameter ratio of 0.4 was used for lower rates.
<table>
<thead>
<tr>
<th>Characteristic Dimension</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Inside Diameter of Pipe, $D_p$, mm</td>
<td>50.4</td>
</tr>
<tr>
<td>Orifice Diameter ($\beta = 0.4$) $D_o$, mm</td>
<td>20.2</td>
</tr>
<tr>
<td>Orifice Diameter ($\beta = 0.6$) $D_o$, mm</td>
<td>30.2</td>
</tr>
<tr>
<td>Length of Hole in Plate, $L_o$, mm</td>
<td>1.50</td>
</tr>
<tr>
<td>$L_o/D_p$</td>
<td>0.030</td>
</tr>
<tr>
<td>$L_o/D_o$ ($\beta = 0.4$)</td>
<td>0.074</td>
</tr>
<tr>
<td>$L_o/(D_p-D_o)$</td>
<td>0.050</td>
</tr>
<tr>
<td>Width of Slit, $D_s$, mm</td>
<td>1.02</td>
</tr>
<tr>
<td>$D_s/D_p$</td>
<td>0.020</td>
</tr>
<tr>
<td>Recommended Ratio, $D_s/D_p$</td>
<td>0.02</td>
</tr>
<tr>
<td>Exit Length, $L_1$, mm</td>
<td>1346.2</td>
</tr>
<tr>
<td>$L_1/D_p$</td>
<td>26.71</td>
</tr>
<tr>
<td>Recommended Minimum $L_1/D_p$</td>
<td>25</td>
</tr>
<tr>
<td>Entrance Length, $L_2$, mm</td>
<td>914.4</td>
</tr>
<tr>
<td>$L_2/D_p$</td>
<td>18.14</td>
</tr>
<tr>
<td>Recommended Minimum $L_2/D_p$</td>
<td>8</td>
</tr>
<tr>
<td>Plate Thickness, $L_p$, mm</td>
<td>3.18</td>
</tr>
<tr>
<td>Thickness Ratio, $L_p/D_p$</td>
<td>0.063</td>
</tr>
</tbody>
</table>
The upstream orifice edge was made as sharp as possible in order to render its effect on the discharge coefficient negligibly small. The plate thickness selected was 3 mm. The location of the pressure tap holes was fixed by specifying corner taps. For the case of corner taps, it is the width of the slit between the orifice plate and the end of the pipe that is important, rather than the tap hole diameter. The A.S.M.E. report recommends that the slit be made the same size as flow nozzles, namely less than or equal to 0.02 $D_p$. This later specification was used. The roughness of the pipe wall, according to the report, is not an important factor if the pipe wall is smooth, as was the case for the copper pipes employed in the present work.

Since the standard orifice coefficient, which includes the effect of jet contraction, would increase if the edge-width ratio ($L_o/D_p$) was too large, it is necessary to specify an upper limit to it. According to the A.S.M.E. report, the edge-width ratio has no appreciable effect on the discharge coefficient if $L_o$ does not exceed any of the following values:

\[
\frac{L_o}{D_p} < \frac{1}{30} \quad \frac{L_o}{D_o} < \frac{1}{8} \quad \frac{L_o}{D_p-D_o} < \frac{1}{8}
\]
The actual size of $L_o$ used in the plate is listed in Table I.1. This is seen to be smaller than the corresponding recommended maxima. However, the plate thickness was greater than $L_o$, and so following the recommendations of the A.S.M.E. report the downstream face of the plate was bevelled at 45°.

The theoretical rate of flow of a compressible fluid, according to the A.S.M.E. report is:

$$\dot{m}_{\text{ideal}} = A_o \left[ \frac{2 \gamma p_1 \rho_1 (r^2/\gamma - r^{(\gamma+1)/\gamma})^{\frac{1}{2}}}{(\gamma-1)(1 - r^2/\gamma \beta^4)} \right]$$  \hspace{1cm} (I.1)

where:

- $r$: static pressure ratio ($p_o/p_1$)
- $\gamma$: ratio of specific heats ($c_p/c_v$)
- $A_o$: cross sectional area of the orifice
- $\rho_1$: density of the fluid upstream the orifice
- $p_1$: absolute static pressure upstream the orifice
- $\beta$: diameter ratio ($D_o/D_p$)

The actual rate of flow is:

$$\dot{m} = C_D \cdot \dot{m}_{\text{ideal}}$$  \hspace{1cm} (I.2)
To calculate $C_D$, the discharge coefficient, the equation presented by Murdock\textsuperscript{127} was used:

$$C_D = C_o + \Delta C \left[ \frac{10^4}{R_d} \right]^a$$  \hspace{1cm} (I.3)

where $C_o$ and $\Delta C$ represent the discharge coefficient when the throat Reynolds number $R_d = \infty$ and the increase in the discharge coefficient for the arbitrary Reynolds number change from $10^4$ to infinity, respectively. The exponent 'a' in Equation (I.3) takes the value of one for flange and pipe taps. Values for $C_o$ and $\Delta C$ are listed in Table I.2 for orifices with flange taps, inside diameter of 50.8 mm, and diameter ratios of 0.4 and 0.6. Tables I.3 and I.4 show the results for the calibration of the plate orifice used in the present work, for diameter ratios of 0.4 and 0.6, respectively.

The relationship of gas flow rate and pressure drop at the orifice plate can be expressed as an exponential curve. If the air flow rate is expressed in Nl/s and the pressure drop in mm H$_2$O the relationship for the case of a diameter ratio of 0.4 is:

$$Q = 2.45 (\Delta P)^{0.485}$$  \hspace{1cm} (I.4)

with a correlation coefficient of 99.97 %.
TABLE I-2

VALUES FOR USE IN EQUATION I-2 TO

OBTAIN DISCHARGE COEFFICIENTS FOR 2 INCH PLATE ORIFICES

<table>
<thead>
<tr>
<th>$\beta$</th>
<th>$C_0$</th>
<th>$\Delta C$</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.30</td>
<td>0.59784</td>
<td>0.01440</td>
</tr>
<tr>
<td>0.32</td>
<td>0.59852</td>
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### TABLE I.3

**CALIBRATION OF THE PLATE ORIFICE**

**WITH A DIAMETER RATIO OF 0.4**

<table>
<thead>
<tr>
<th>$\Delta P$ (mm H$_2$O)</th>
<th>$P_1$ (Pa x 10$^{-5}$)</th>
<th>$P_o$ (Pa x 10$^{-5}$)</th>
<th>$C_D$</th>
<th>Air Flow Rate (Nm/s)</th>
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</thead>
<tbody>
<tr>
<td>23.5</td>
<td>1.016</td>
<td>1.014</td>
<td>0.61747</td>
<td>3.75</td>
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<td>0.61517</td>
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<td>1.015</td>
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<td>0.60905</td>
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<td>1.017</td>
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<td>10.73</td>
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<td>1.018</td>
<td>0.60667</td>
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<tr>
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<td>0.60611</td>
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<td>1.052</td>
<td>1.019</td>
<td>0.60588</td>
<td>13.43</td>
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</table>
TABLE I.4

CALIBRATION OF THE PLATE ORIFICE

WITH A DIAMETER RATIO OF 0.6

<table>
<thead>
<tr>
<th>P (mm H₂O)</th>
<th>P₁ $\times 10^{-5}$</th>
<th>P₀ $\times 10^{-5}$</th>
<th>C₀</th>
<th>Air Flow Rate Nl/s</th>
</tr>
</thead>
<tbody>
<tr>
<td>31.3</td>
<td>1.022</td>
<td>1.019</td>
<td>0.61965</td>
<td>11.21</td>
</tr>
<tr>
<td>57.1</td>
<td>1.029</td>
<td>1.023</td>
<td>0.61734</td>
<td>13.98</td>
</tr>
<tr>
<td>81.6</td>
<td>1.035</td>
<td>1.027</td>
<td>0.61566</td>
<td>17.04</td>
</tr>
<tr>
<td>134.6</td>
<td>1.050</td>
<td>1.036</td>
<td>0.61403</td>
<td>21.63</td>
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<tr>
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<td>1.039</td>
<td>0.61376</td>
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<td>1.062</td>
<td>1.043</td>
<td>0.61312</td>
<td>25.49</td>
</tr>
<tr>
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<td>1.059</td>
<td>0.61241</td>
<td>29.55</td>
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<td>1.062</td>
<td>0.61231</td>
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<td>49.75</td>
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<tr>
<td>788.8</td>
<td>1.239</td>
<td>1.162</td>
<td>0.61060</td>
<td>50.12</td>
</tr>
</tbody>
</table>
For a diameter ratio of 0.6 the relationship is:

\[ Q = 2.10 (\Delta P)^{0.475} \]  \hspace{1cm} (I.5)

with a correlation coefficient of 99.89 %.

Figures I.1 and I.2 show the experimental results of the calibrations for both diameter ratios as well as for the curves representing Equations (I.4) and (I.5).
Figure I.1 Air Flow Rate versus Pressure Differential for the Plate Orifice with a Diameter Ratio of 0.4
Figure 1.2 Air Flow Rate versus Pressure Differential for the Plate Orifice with a Diameter Ratio of 0.6
APPENDIX II

PRESSURE TRACES FOR DIFFERENT CHARGES
OF THE COPPER CONVERTER

Pressure oscillations for different charges of the copper converter are shown in Figure II.1. The measurements were performed at the tuyeres of two converters from the Utah smelter. In all the photographs the pressure pulses as coming from the piezoelectric transducer are shown in Channel 2 of the oscilloscope (bottom part of the screen). Channel 1 (upper part of the screen) shows the same signal as in Channel 2 after filtering all the ambient electrical noise with a frequency greater than 30 Hz. In all the photographs the vertical scale is set at a value of 4.8 kPa/div.
Figure II.1 Pressure Traces for Different Charges of a Peirce-Smith Copper Converter.
Vertical Scale: 4.8 kPa/div
(a) Charge 1,  (b) Charge 3
(c) Charge 6,  (d) Charge 12
(e) Old Converter.
APPENDIX III

RADIATION SHAPE FACTORS IN THE
HEAT TRANSFER MODEL

In order to calculate the rate of heat exchange from each element in the model it is necessary to evaluate the fraction of the total diffuse radiation leaving one surface which is intercepted by another surface and vice versa. The fraction of diffusely distributed radiation leaving a given surface $A_e$ that reaches surface $A_r$ is called the radiation shape factor $F_{e,r}$, which evaluated on the basis of the area of the emissive surface is:

$$ F_{e,r} = \frac{1}{A_e} \int_{A_e} \int_{A_r} \frac{\cos\alpha_e \cdot dA_e \cdot \cos\alpha_r \cdot dA_r}{\pi D^2} $$

The different terms in Equation (III.1) are defined according to the geometric arrangement notation sketched in Figure III.1.

In the model the converter radius is assumed large enough such that each element into which the converter internal surface is subdivided can be considered as a plane surface. Also it is assumed that the areas of the elements in the model are small enough so that both angles in Equation (III.1) can be considered as constant for each element. By virtue of
Figure III.1 Geometric Arrangement to Calculate Radiation Shape Factors in the Heat Transfer Model.
these two assumptions Equation (III.1) can be expressed as:

\[ F_{e,r} = \frac{\cos^e \cos^r}{\pi D^2} A_r \]  \hspace{1cm} (III.2)

Also \( \cos^e \) and \( \cos^r \) can be expressed as:

\[ \cos^e = \frac{D^2 + Q_e^2 - S_e^2}{2 D Q_e} \]  \hspace{1cm} (III.3)

and:

\[ \cos^r = \frac{D^2 + Q_r^2 - S_r^2}{2 D Q_r} \]  \hspace{1cm} (III.4)

Substituting Equations (III.3) and (III.4) for \( \cos^e \) and \( \cos^r \) in Equation (III.2) yields:

\[ F_{e,r} = \frac{(D^2 + Q_e^2 - S_e^2)(D^2 + Q_r^2 - S_r^2)}{4 \pi Q_e Q_r D^4} A_r \]  \hspace{1cm} (III.5)

It is possible to express \( D, Q_e, Q_r, S_e \) and \( S_r \) in Equation (III.5) as:

\[ D = \left[ (X_e-X_r)^2 + (Y_e-Y_r)^2 + (Z_e-Z_r)^2 \right]^{\frac{1}{2}} \]  \hspace{1cm} (III.6)

\[ Q_e = \left[ (X_e-X_{oe})^2 + (Y_e-Y_{oe})^2 + (Z_e-Z_{oe})^2 \right]^{\frac{1}{2}} \]  \hspace{1cm} (III.7)

\[ Q_r = \left[ (X_r-X_{or})^2 + (Y_r-Y_{or})^2 + (Z_r-Z_{or})^2 \right]^{\frac{1}{2}} \]  \hspace{1cm} (III.8)
\[ S_e = \left[ (X_r - X_{oe})^2 + (Y_r - Y_{oe})^2 + (Z_r - Z_{oe})^2 \right]^{\frac{1}{2}} \]  \quad (III.9)

\[ S_r = \left[ (X_e - X_{or})^2 + (Y_e - Y_{or})^2 + (Z_e - Z_{oe})^2 \right]^{\frac{1}{2}} \]  \quad (III.10)

Equations (III.5) to (III.10) provide a method to numerically calculate the radiation shape factors in the heat-transfer model.
APPENDIX IV

THERMAL CONDUCTANCES FOR THE DIFFERENT NODES IN THE MODEL

The thermal conductances defined by Equation (6.15) depend on the specific geometry and position of the node under consideration. This appendix indicates the expressions used for the interior and boundary nodes in the mantle and the end walls of the converter.

Mantle of the Converter

i) For an interior node as shown in Figure IV.1(a), the thermal conductances can be expressed as:

\[
C_{i,i+1} = K_i \frac{\gamma z}{\Delta r} \left[ r_i + \frac{\Delta r}{2} \right] = K_i \cdot S_{i,i+1}
\]

\[
C_{i,i-1} = K_i \frac{\gamma z}{\Delta r} \left[ r_i - \frac{\Delta r}{2} \right] = K_i \cdot S_{i,i-1}
\]

and the volume of the node is:

\[
V_i = \gamma \cdot z \cdot r_i \cdot \Delta r
\]
Figure IV.1 Geometric Configurations to Evaluate Thermal Conductances for the Different Nodes in the Model.
ii) For a node at the internal surface of the converter, Figure IV.1(b), the thermal conductance is expressed as:

\[ C_{1,2} = K_1 \frac{\gamma z}{\Delta r} \left[ r_1 + \frac{\Delta r}{2} \right] = K_1 \cdot S_{1,2} \]

and the volume of the node is:

\[ V_1 = \frac{\gamma \cdot z}{8} \cdot (\Delta r^2 + 4 \cdot r_1 \cdot \Delta r) \]

iii) For a node at the external surface of the converter, Figure IV.1(c), the thermal conductance is expressed as:

\[ C_{N,N-1} = K_N \frac{\gamma z}{\Delta r} \left[ r_N - \frac{\Delta r}{2} \right] = K_N \cdot S_{N,N-1} \]

and the volume of the node is:

\[ V_N = \frac{\gamma \cdot z}{8} \cdot (4 \cdot r_N \cdot \Delta r - \Delta r^2) \]

**End Wall Nodes**

In this case there is a plane wall to be considered, as shown in Figure IV.1(d).
i) Therefore for an interior node

\[ C_{i,i+1} = K_i \frac{R^2 \tan(\gamma/2)}{\Delta r} = K_i S_{i,i+1} \]

\[ V_i = R^2 \tan(\gamma/2) \Delta r \]

ii) For a node at the internal surface of the converter end wall

\[ C_{1,2} = K_1 \frac{R^2 \tan(\gamma/2)}{\Delta r} = K_1 S_{1,2} \]

\[ V_1 = R^2 \tan(\gamma/2) \frac{\Delta r}{2} \]

iii) For a node at the external surface of the converter end wall

\[ C_{N,N-1} = K_N \frac{R^2 \tan(\gamma/2)}{\Delta r} = K_N S_{N,N-1} \]

\[ V_N = R^2 \tan(\gamma/2) \frac{\Delta r}{2} \]
During its operation, the external surface of the converter is losing heat to the surroundings by convection and radiation. If the thermal conductivity of the refractory is a linear function of temperature, the heat flow by conduction through the cylindrical shell of the converter, Figure V.1, can be expressed as:

\[ q_{\text{cond}} = -2 \pi r L k_o (1 + k_1 T) \frac{dT}{dr} \] (V.1)

Rearranging and integrating, the heat flow per unit length of converter is obtained,

\[ Q_{\text{cond}} = \frac{q_{\text{cond}}}{L} = 2 \pi k_o \frac{1}{\ln(R_e/R_i)} \left[ T_i - T_e + \frac{k_1}{2} (T_i^2 - T_e^2) \right] \] (V.2)

The heat flow by conduction has to be equal to the heat losses by convection and radiation

\[ Q_{\text{cond}} = 2 \pi R_e \left[ h(T_e - T_\infty) + \sigma (T_e^4 - T_\infty^4) \right] \] (V.3)
Figure V.1 Thermal Profile in the Cylindrical Shell of the Copper Converter.
To evaluate the heat-transfer coefficient the following equation has been proposed\textsuperscript{101} for horizontal cylinders under natural convection

\[ \text{Nu} = 0.13 \left( \frac{\text{Gr Pr}}{Pr} \right)^{1/3} \]  \hspace{1cm} (V.4)

in the turbulent range \(10^9 < \text{Gr Pr} < 10^{12}\). To evaluate the properties of the ambient air a film temperature, \(0.5(T_e + T_m)\) was used. It is necessary to emphasize that \(T_e\) in Equations (V.2) and (V.3) is unknown. Table V.1\textsuperscript{133} shows values of some properties of air at atmospheric pressure as a function of film temperature.

Table V.2 shows the calculated values of the heat-transfer coefficients as a function of \(T_e\), for converter diameters of 400, 460 and 520 cm. The thickness of the refractory was taken as 40 cm, and the ambient temperature 30 °C.

Once the heat-transfer coefficient is known for a given temperature of the external surface of the converter the heat losses by convection can be evaluated. Table V.3 shows the values of the heat flow by conduction through the wall as well as the heat losses by convection and radiation, as a function of \(T_e\), for different converter diameters. The heat-balance, Equation (V.3) is satisfied in all cases with an external surface temperature of about 190 °C and a heat-transfer coefficient of about 7.15 W m\(^{-2}\) K.
TABLE V.1

PROPERTIES OF DRY AIR AT ATMOSPHERIC PRESSURE

<table>
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<tr>
<th>Temperature °C</th>
<th>Density kg m⁻³</th>
<th>Viscosity N s m⁻² x 10⁻⁵</th>
<th>8 K⁻¹ x 10²</th>
<th>Conductivity W m⁻¹K⁻¹ x 10²</th>
<th>Pr</th>
</tr>
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<tr>
<td>82</td>
<td>0.995</td>
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<td>2.817</td>
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<td>0.696</td>
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<td>3.079</td>
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</tr>
<tr>
<td>92</td>
<td>0.968</td>
<td>2.139</td>
<td>2.740</td>
<td>3.115</td>
<td>0.694</td>
</tr>
<tr>
<td>97</td>
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<td>2.703</td>
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<td>0.693</td>
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<td>2.564</td>
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<td>0.690</td>
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<td>2.532</td>
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<td>2.286</td>
<td>2.500</td>
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</table>
TABLE V.2

HEAT TRANSFER COEFFICIENT FOR
DIFFERENT CONVERTER DIAMETERS

<table>
<thead>
<tr>
<th>D = 400 cm</th>
<th></th>
<th></th>
<th></th>
</tr>
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<tr>
<td>T, C</td>
<td>Gr x 10^{-11}</td>
<td>Nu x 10^{-3}</td>
<td>h W m^{-2}K^{-1}</td>
</tr>
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<td>179</td>
<td>7.807</td>
<td>1.058</td>
<td>7.06</td>
</tr>
<tr>
<td>184</td>
<td>7.834</td>
<td>1.059</td>
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</tr>
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<td>189</td>
<td>7.844</td>
<td>1.059</td>
<td>7.15</td>
</tr>
<tr>
<td>194</td>
<td>7.849</td>
<td>1.061</td>
<td>7.20</td>
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</table>

<table>
<thead>
<tr>
<th>D = 460 cm</th>
<th></th>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>T, C</td>
<td>Gr x 10^{-12}</td>
<td>Nu x 10^{-3}</td>
<td>h W m^{-2}K^{-1}</td>
</tr>
<tr>
<td>179</td>
<td>1.112</td>
<td>1.191</td>
<td>7.07</td>
</tr>
<tr>
<td>184</td>
<td>1.115</td>
<td>1.192</td>
<td>7.11</td>
</tr>
<tr>
<td>189</td>
<td>1.117</td>
<td>1.192</td>
<td>7.15</td>
</tr>
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<td>194</td>
<td>1.118</td>
<td>1.192</td>
<td>7.19</td>
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<th>D = 520 cm</th>
<th></th>
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<tr>
<td>T, C</td>
<td>Gr x 10^{-12}</td>
<td>Nu x 10^{-3}</td>
<td>h W m^{-2}K^{-1}</td>
</tr>
<tr>
<td>179</td>
<td>1.525</td>
<td>1.323</td>
<td>7.07</td>
</tr>
<tr>
<td>184</td>
<td>1.530</td>
<td>1.324</td>
<td>7.11</td>
</tr>
<tr>
<td>189</td>
<td>1.532</td>
<td>1.324</td>
<td>7.15</td>
</tr>
<tr>
<td>194</td>
<td>1.533</td>
<td>1.325</td>
<td>7.19</td>
</tr>
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TABLE V.3

HEAT LOSSES BY CONVECTION AND RADIATION
AS COMPARED WITH CONDUCTIVE HEAT FLOW INSIDE THE WALL

<table>
<thead>
<tr>
<th>D = 400 cm</th>
<th>Q_{\text{cond}} W/m x 10^{-4}</th>
<th>Q_{\text{conv}} W/m x 10^{-4}</th>
<th>Q_{\text{rad}} W/m x 10^{-4}</th>
<th>Q_{\text{conv}} + Q_{\text{rad}} W/m x 10^{-4}</th>
</tr>
</thead>
<tbody>
<tr>
<td>T, C</td>
<td>W/m x 10^{-4}</td>
<td>W/m x 10^{-4}</td>
<td>W/m x 10^{-4}</td>
<td>W/m x 10^{-4}</td>
</tr>
<tr>
<td>179</td>
<td>5.06</td>
<td>1.59</td>
<td>2.85</td>
<td>4.44</td>
</tr>
<tr>
<td>184</td>
<td>5.04</td>
<td>1.65</td>
<td>3.01</td>
<td>4.66</td>
</tr>
<tr>
<td>189</td>
<td>5.02</td>
<td>1.71</td>
<td>3.18</td>
<td>4.89</td>
</tr>
<tr>
<td>194</td>
<td>5.00</td>
<td>1.78</td>
<td>3.35</td>
<td>5.13</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>D = 460 cm</th>
<th>Q_{\text{cond}} W/m x 10^{-4}</th>
<th>Q_{\text{conv}} W/m x 10^{-4}</th>
<th>Q_{\text{rad}} W/m x 10^{-4}</th>
<th>Q_{\text{conv}} + Q_{\text{rad}} W/m x 10^{-4}</th>
</tr>
</thead>
<tbody>
<tr>
<td>T, C</td>
<td>W/m x 10^{-4}</td>
<td>W/m x 10^{-4}</td>
<td>W/m x 10^{-4}</td>
<td>W/m x 10^{-4}</td>
</tr>
<tr>
<td>179</td>
<td>5.75</td>
<td>1.79</td>
<td>3.21</td>
<td>5.00</td>
</tr>
<tr>
<td>184</td>
<td>5.73</td>
<td>1.85</td>
<td>3.39</td>
<td>5.24</td>
</tr>
<tr>
<td>189</td>
<td>5.71</td>
<td>1.93</td>
<td>3.57</td>
<td>5.50</td>
</tr>
<tr>
<td>194</td>
<td>5.68</td>
<td>2.00</td>
<td>3.77</td>
<td>5.77</td>
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</tbody>
</table>

<table>
<thead>
<tr>
<th>D = 520 cm</th>
<th>Q_{\text{cond}} W/m x 10^{-4}</th>
<th>Q_{\text{conv}} W/m x 10^{-4}</th>
<th>Q_{\text{rad}} W/m x 10^{-4}</th>
<th>Q_{\text{conv}} + Q_{\text{rad}} W/m x 10^{-4}</th>
</tr>
</thead>
<tbody>
<tr>
<td>T, C</td>
<td>W/m x 10^{-4}</td>
<td>W/m x 10^{-4}</td>
<td>W/m x 10^{-4}</td>
<td>W/m x 10^{-4}</td>
</tr>
<tr>
<td>179</td>
<td>6.44</td>
<td>1.98</td>
<td>3.56</td>
<td>5.54</td>
</tr>
<tr>
<td>184</td>
<td>6.42</td>
<td>2.07</td>
<td>3.76</td>
<td>5.83</td>
</tr>
<tr>
<td>189</td>
<td>6.39</td>
<td>2.14</td>
<td>3.97</td>
<td>6.11</td>
</tr>
<tr>
<td>194</td>
<td>6.37</td>
<td>2.22</td>
<td>4.18</td>
<td>6.40</td>
</tr>
</tbody>
</table>
APPENDIX VI

TEMPERATURE PROFILES IN THE CONVERTER WALL

WITH VARIABLE THERMAL CONDUCTIVITY

From data presented by Szekely and Themelis\textsuperscript{100}, the thermal conductivity of a chrome-magnesite refractory brick as a function of temperature can be expressed as:

\[ k = 1.08(1 + 4.5 \times 10^{-4}T) = k_o(1+k_1T) \quad (VI.1) \]

where \( k \) and \( T \) are expressed in \( \text{W m}^{-1} \text{C}^{-1} \) and \( ^\circ \text{C} \), respectively, for the range 200 to 1300 \( ^\circ \text{C} \).

For the cylindrical wall of the converter (extending from \( R_i \) to \( R_e \)), under steady-state conditions and in the absence of heat generation the energy balance equation is:

\[ \frac{d}{dr} \left[ k_o(1+k_1T)r \frac{dT}{dr} \right] = 0 \quad (VI.2) \]

Integration of Equation (VI.2) twice with the boundary conditions:

\[ T(r=R_i) = T_i \]
\[ T(r=R_e) = T_e \]
gives the temperature profile for the cylindrical wall

\[
\frac{T - T_i + \frac{1}{2} k_1(T_i^2 - T_e^2)}{T_i - T_e + \frac{1}{2} k_1(T_i^2 - T_e^2)} = \frac{\ln(r/R_i)}{\ln(R_i/R_e)} \tag{VI.3}
\]

Similarly for the end walls of the converter the steady state energy equation is:

\[
\frac{d}{dz} \left[ k_o (1+k_1 T) \frac{dT}{dz} \right] = 0 \tag{VI.4}
\]

Again integrating Equation (VI.4) twice with the boundary conditions:

\[
T(z=z_i) = T_i
\]

and

\[
T(z=z_e) = T_e
\]

gives the temperature profile for the end walls

\[
\frac{T - T_i + \frac{1}{2} k_1(T_i^2 - T_e^2)}{T_i - T_e + \frac{1}{2} k_1(T_e^2 - T_i^2)} = \frac{z - z_i}{z_i - z_e} \tag{VI.5}
\]
APPENDIX VII

HEAT TRANSFER MODEL PROGRAM
This program calculates heat exchange between all the elements of the converter assuming constant heat transfer coefficient and taking into account the end walls.

IMPLICIT REAL*(A-H,O-Z)
DIMENSION TI(240,26),FER(240),Q(240)
DIMENSION DA(240,240),DT(240,240),DX(240),DB(240),IPERM(480)
DIMENSION DA1(26,26),DT1(26,26),DX1(26),DB1(26),IPERM1(52)
DIMENSION F(240,240),DRZ(240)
COMMON/ZZ1/XE,YE,ZE,XR,YR,ZR,OE,OR,D,SE,SR
COMMON/ZZ2/GAMMA,THETE,THETR,AR,Q,O,TQ,QL,QT
COMMON/ZZ3/TR,TM,TI,TF,F,DA,DB,DT,FER,TIME,DTIME,TE,DH
COMMON/ZZ4/R,CL,CL,W1,W2,Z,E,NSL,NSEC,ISM,LSM,NSM,NCS,N,M
COMMON/ZZ5/TC0ND.DIF.PENT,PI,SIGMA,HCAP,DENST,HTRAN
COMMON/ZZ6/Y,DR1,DX1,DA1,C1,C2,C3

R = CONVERTER RADIUS (CM)
DR = REFRACTORY THICKNESS (CM)
CL = CONVERTER LENGTH (CM)
W1 = MOUTH WIDTH (CM)
W2 = MOUTH LENGTH (CM)
Z = WIDTH OF ONE SLICE (CM)
E = REFRACTORY EMISSIVITY
DTIME = DELTA TIME (S)
TIME = OUT OF STACK TIME (S)
DH = SIZE INCREMENT (CM)
TM = MOUTH TEMPERATURE (C)
TR = INITIAL REFRACTORY TEMPERATURE (C)
TE = EXTERNAL SURFACE TEMPERATURE (C)
TI(I) = INITIAL TEMPERATURE OF ZONE I (C)
TF(I) = FINAL TEMPERATURE OF ZONE I (C)
Q(I) = HEAT LOSSES FROM ZONE I (W/CM2)
QL = HEAT LOSSES THROUGH THE MOUTH (W/CM2)
HTRAN = HEAT TRANSFER COEFFICIENT (W/CM2 K)
M = SLICES IN THE REFRACTORY
NSL = NUMBER OF SLICES (NSL = CL/Z)
NSEC = NUMBER OF SECTIONS
NSM = NUMBER OF SLICES IN THE MOUTH
ISM = INITIAL SLICE IN MOUTH
LSM = LAST SLICE IN MOUTH
NSC = NUMBER OF CENTER SLICE
TC0ND = THERMAL CONDUCTIVITY (W/CM K)
Y = PENETRATION IN THE REFRACTORY (CM)
SIGMA = STEFAN-BOLTZMANN CONSTANT (W/CM2 K4)
HCAP = REFRACTORY SPECIFIC HEAT (W/GR C)
DENST = REFRACTORY DENSITY (GR/CM3)

READ(5,5),R,DR,CL,Z,E,TIME,DTIME,DH,NSEC,ISM,LSM
FORMAT(8F7.2,3I3)
NSL = CL/Z
NDIMAT = 240
N = NSEC*(NSL+2)
NRHS = 1
ITMAX = 7
NDIMA = 26
NDIMT = 26
NDIMBX = 26
NSOL = 1
M = (DR/DH) + 1.D0
TR = 1200.D0
TM = 600.D0
TE = 190.D0
C1 = 1.D0
C2 = 4.5892D-4
C3 = 1.0919D-2
HCAP = 0.96D0
DENST = 2.9D0
HTRAN = 7.20D-4
SIGMA = 5.672D-12
NCS = (NSL + 1 + (NSEC * 2)) / 2
W2 = (LSM - ISM + 1) * Z
NSM = W2 / Z
PI = 3.141592654D0
GAMMA = (2.DO * PI) / (DFLOAT(NSEC))
W1 = 4.DO * R * DTAN(GAMMA / 2.DO)
WRITE(6, 10) R, DR, CL, W1, W2, Z, E, TIME, DTIME, DH, TM, TR, TE, NSL, NCS,
   NSM, ISM, LSM, NSEC, DENST, HCAP, HTRAN, M
10 FORMAT(' ', T10, 'CONVERTER RADIUS=', F7.2, '/,
   T10, 'REFRACTORY THICKNESS=', F5.2, '/,
   T10, 'CONVERTER LENGTH=', F8.2, '/,
   T10, 'MOUTH WIDTH=', F7.2, '/,
   T10, 'SLICE SIZE=', F5.1, '/,
   T10, 'REFRACTORY EMISSIVITY=', F4.2, '/,
   T10, 'OUT OF STACK TIME=', F8.2, '/,
   T10, 'TIME STEP=', F8.2, '/,
   T10, 'SIZE INCREMENT=', F4.2, '/,
   T10, 'MOUTH TEMPERATURE=', F8.2, '/,
   T10, 'INITIAL REFRACTORY TEMPERATURE=', F8.2, '/,
   T10, 'EXTERNAL SURFACE TEMPERATURE=', F8.2, '/,
   T10, 'NUMBER OF SLICES=', I3, '/,
   T10, 'CENTRAL SLICE=', I3, '/,
   T10, 'SLICES IN THE MOUTH=', I3, '/,
   T10, 'INITIAL SLICE IN MOUTH=', I3, '/,
   T10, 'LAST SLICE IN MOUTH=', I3, '/,
   T10, 'NUMBER OF SECTIONS=', I3, '/,
   T10, 'REFRACTORY DENSITY=', F6.3, '/,
   T10, 'REFRACTORY SPECIFIC HEAT=', F6.3, '/,
   T10, 'HEAT TRANSFER COEFFICIENT=', F12.10, '/,
   T10, 'SECTIONS IN THE REFRACTORY=', I3, '/,
   T10, 'CENTRAL SLICE=', I3, '/,
   T10, 'SLICES IN THE MOUTH=', I3, '/,
   T10, 'INITIAL SLICE IN MOUTH=', I3, '/,
   T10, 'LAST SLICE IN MOUTH=', I3, '/,
   T10, 'NUMBER OF SECTIONS=', I3, '/,
   T10, 'REFRACTORY DENSITY=', F6.3, '/,
   T10, 'REFRACTORY SPECIFIC HEAT=', F6.3, '/,
   T10, 'HEAT TRANSFER COEFFICIENT=', F12.10, '/,
   T10, 'SECTIONS IN THE REFRACTORY=', I3, '/,
   T10, 'CENTRAL SLICE=', I3, '/,
   T10, 'SLICES IN THE MOUTH=', I3, '/,
   T10, 'INITIAL SLICE IN MOUTH=', I3, '/,
   T10, 'LAST SLICE IN MOUTH=', I3, '/,
   T10, 'NUMBER OF SECTIONS=', I3, '/,
   T10, 'REFRACTORY DENSITY=', F6.3, '/,
   T10, 'REFRACTORY SPECIFIC HEAT=', F6.3, '/,
   T10, 'HEAT TRANSFER COEFFICIENT=', F12.10, '/,
   T10, 'SECTIONS IN THE REFRACTORY=', I3, '/,
   T10, 'CENTRAL SLICE=', I3, '/,
   T10, 'SLICES IN THE MOUTH=', I3, '/,
   T10, 'INITIAL SLICE IN MOUTH=', I3, '/,
   T10, 'LAST SLICE IN MOUTH=', I3, '/,
   T10, 'NUMBER OF SECTIONS=', I3, '/,
   T10, 'REFRACTORY DENSITY=', F6.3, '/,
   T10, 'REFRACTORY SPECIFIC HEAT=', F6.3, '/,
   T10, 'HEAT TRANSFER COEFFICIENT=', F12.10, '/,
   T10, 'SECTIONS IN THE REFRACTORY=', I3, '/,
   T10, 'CENTRAL SLICE=', I3, '/,
   T10, 'SLICES IN THE MOUTH=', I3, '/,
   T10, 'INITIAL SLICE IN MOUTH=', I3, '/,
   T10, 'LAST SLICE IN MOUTH=', I3, '/,
   T10, 'NUMBER OF SECTIONS=', I3, '/,
   T10, 'REFRACTORY DENSITY=', F6.3, '/,
   T10, 'REFRACTORY SPECIFIC HEAT=', F6.3, '/,
   T10, 'HEAT TRANSFER COEFFICIENT=', F12.10, '/,
   T10, 'SECTIONS IN THE REFRACTORY=', I3, '/,
   T10, 'CENTRAL SLICE=', I3, '/,
   T10, 'SLICES IN THE MOUTH=', I3, '/,
   T10, 'INITIAL SLICE IN MOUTH=', I3, '/,
   T10, 'LAST SLICE IN MOUTH=', I3, '/,
   T10, 'NUMBER OF SECTIONS=', I3, '/,
   T10, 'REFRACTORY DENSITY=', F6.3, '/,
   T10, 'REFRACTORY SPECIFIC HEAT=', F6.3, '/,
   T10, 'HEAT TRANSFER COEFFICIENT=', F12.10, '/,
   T10, 'SECTIONS IN THE REFRACTORY=', I3, '/,
   T10, 'CENTRAL SLICE=', I3, '/,
   T10, 'SLICES IN THE MOUTH=', I3, '/,
   T10, 'INITIAL SLICE IN MOUTH=', I3, '/,
   T10, 'LAST SLICE IN MOUTH=', I3, '/,
   T10, 'NUMBER OF SECTIONS=', I3, '/,
   T10, 'REFRACTORY DENSITY=', F6.3, '/,
   T10, 'REFRACTORY SPECIFIC HEAT=', F6.3, '/,
   T10, 'HEAT TRANSFER COEFFICIENT=', F12.10, '/,
   T10, 'SECTIONS IN THE REFRACTORY=', I3, '/,
   T10, 'CENTRAL SLICE=', I3, '/,
   T10, 'SLICES IN THE MOUTH=', I3, '/,
   T10, 'INITIAL SLICE IN MOUTH=', I3, '/,
   T10, 'LAST SLICE IN MOUTH=', I3, '/,
   T10, 'NUMBER OF SECTIONS=', I3, '/,
   T10, 'REFRACTORY DENSITY=', F6.3, '/,
   T10, 'REFRACTORY SPECIFIC HEAT=', F6.3, '/,
   T10, 'HEAT TRANSFER COEFFICIENT=', F12.10, '/,
   T10, 'SECTIONS IN THE REFRACTORY=', I3, '/,
   T10, 'CENTRAL SLICE=', I3, '/,
   T10, 'SLICES IN THE MOUTH=', I3, '/,
   T10, 'INITIAL SLICE IN MOUTH=', I3, '/,
   T10, 'LAST SLICE IN MOUTH=', I3, '/,
   T10, 'NUMBER OF SECTIONS=', I3, '/,
   T10, 'REFRACTORY DENSITY=', F6.3, '/,
   T10, 'REFRACTORY SPECIFIC HEAT=', F6.3, '/,
   T10, 'HEAT TRANSFER COEFFICIENT=', F12.10, '/,
   T10, 'SECTIONS IN THE REFRACTORY=', I3,
IF(I .GE. ISM .AND. I .LE. LSM) GO TO 20
IF(I .GE. ISM+NSL*(NSEC-1) .AND. I .LE. LSM+NSL*(NSEC-1)) GO TO 20
TI(I,L) = DX(L)
GO TO 13
13 CONTINUE
14 CONTINUE
WRITE(6,19)K,QT,QL,QTL
N1 = NSEC+((NSEC/2)-1)*NSL+1
N2 = N1+NSL-1
PRINT 11
DO 17 I = N1, N2
WRITE(6,16)I, TI(I,1), TI(I,2), TI(I,3), TI(I,4), TI(I,5),
   TI(I,6), TI(I,7), TI(I,8), TI(I,9), TI(I,10), TI(I,11)
16 FORMAT(T2,I3,T6,F5.0,T12,F5.0,T18,F5.0,T24,F5.0,T30,F5.0,
   T36,F5.0,T42,F5.0,T48,F5.0,T54,F5.0,T60,F5.0)
17 CONTINUE
19 FORMAT(/,T5,'TIME=',I5,T19,'0I  = ' ,E12.3,T40, 'QL=' ,E12.3,
   T60,'OTL=',E12.3)
30 CONTINUE
STOP
END
SUBROUTINE SFER(NE, NR)
IMPLICIT REAL*8(A-H,0-Z)
DIMENSION TI(240,26), FER(240), Q(240)
DIMENSION DA(240,240), DT(240,240), DX(240), DB(240), IPERM(480)
DIMENSION DA(26,26), DT(26,26), DX(26), DB(26), IPERM(52)
DIMENSION F(240,240), DRZ(240)
COMMON/Z1/ XE, YE, ZE, XR, YR, ZR, QE, OR, D, SE, SR
COMMON/Z2/ GAMMA, THETE, THETR, AR, Q, QT, QL, QTL
COMMON/Z3/ TR, TM, TI, TF, DA, DB, DX, FER, TIME, DTIME, TE, DH
COMMON/Z4/ R, DR, CL, W1, W2, Z, E, NSL, NSEC, ISM, LSM, NSM, NCS, N, M
COMMON/Z5/ TCOND, DIF, PENT, PI, SIGMA, HCAP, DENST, HTRAN
DO 117 NE = 1, N
DO 116 NR = 1, N
IF(NE.GT.NSEC) GO TO 103
THETE = (GAMMA/2.DO) + (NE-1)*GAMMA
XE = R*DCOS(THETE)/DSQRT(2.DO)
YE = R*DSIN(THETE)/DSQRT(2.DO)
ZE = CL/2.DO
XOE = XE
YOE = YE
IF(NR.LE.NSEC) GO TO 101
IF(NR.GT.NSEC*(NSL+1)) GO TO 102
ZOE = ((NCS-NR)+((NR-NSEC-1)/NSL)*NSL)*Z
GO TO 105
101 ZOE = CL/2.DO
GO TO 105
102 ZOE = -CL/2.DO
GO TO 105
103 IF(NE.GT.NSEC*(NSL+1)) GO TO 104
THETE = ((NE-NSEC-1)/NSL)*GAMMA+(GAMMA/2.DO)
XE = R*DCOS(THETE)
YE = R*DSIN(THETE)
ZOE = ((NCS-NE)+((NE-NSEC-1)/NSL)*NSL)*Z
XOE = O.DO
YOE = O.DO
ZOE = ZE
273

GO TO 105

104 \( \text{THETE} = (\text{GAMMA}/2.0) + (\text{NE}-1-\text{NSEC}*(\text{NSL}+1)) \times \text{GAMMA} \)

\( \text{XE} = R \times \text{DCOS} (\text{THETE}) / \text{DSQRT}(2.0) \)

\( \text{YE} = R \times \text{DSIN} (\text{THETE}) / \text{DSQRT}(2.0) \)

\( \text{ZE} = -\text{CL}/2.0 \)

\( \text{XOE} = \text{XE} \)

\( \text{YOE} = \text{YE} \)

IF (NR.GT.NSEC*(NSL+1)) GO TO 102

IF (NR.LE.NSEC) GO TO 101

\( \text{ZOE} = ((\text{NCS}-\text{NR}) + ((\text{NR}-\text{NSEC}-1)/\text{NSL}) \times \text{NSL}) \times \text{Z} \)

105 IF (NR.GT.NSEC) GO TO 108

\( \text{THETR} = (\text{GAMMA}/2.0) + (\text{NR}-1) \times \text{GAMMA} \)

\( \text{AR} = R \times R \times \text{DTAN}(\text{GAMMA}/2.0) \)

\( \text{XR} = R \times \text{DCOS} (\text{THETR}) / \text{DSQRT}(2.0) \)

\( \text{YR} = R \times \text{DSIN} (\text{THETR}) / \text{DSQRT}(2.0) \)

\( \text{ZR} = \text{CL}/2.0 \)

\( \text{XOR} = \text{XR} \)

\( \text{YOR} = \text{YR} \)

IF (NE.LE.NSEC) GO TO 106

IF (NE.GT.NSEC*(NSL+1)) GO TO 107

\( \text{ZOR} = ((\text{NCS}-\text{NE}) + ((\text{NE}-\text{NSEC}-1)/\text{NSL}) \times \text{NSL}) \times \text{Z} \)

GO TO 110

106 \( \text{ZOR} = \text{CL}/2.0 \)

GO TO 110

107 \( \text{ZOR} = -\text{CL}/2.0 \)

GO TO 110

108 IF (NR.GT.NSEC*(NSL+1)) GO TO 109

\( \text{THETR} = ((\text{NR}-\text{NSEC}-1)/\text{NSL}) \times \text{GAMMA} + (\text{GAMMA}/2.0) \)

\( \text{AR} = 2.0 \times Z \times R \times \text{DTAN}(\text{GAMMA}/2.0) \)

\( \text{XR} = R \times \text{DCOS} (\text{THETR}) \)

\( \text{YR} = R \times \text{DSIN} (\text{THETR}) / \text{DSQRT}(2.0) \)

\( \text{ZR} = ((\text{NCS}-\text{NR}) + ((\text{NR}-\text{NSEC}-1)/\text{NSL}) \times \text{NSL}) \times \text{Z} \)

\( \text{XOR} = 0.0 \)

\( \text{YOR} = 0.0 \)

\( \text{ZOR} = \text{ZR} \)

GO TO 110

109 \( \text{THETR} = (\text{GAMMA}/2.0) + (\text{NR}-1-\text{NSEC}*(\text{NSL}+1)) \times \text{GAMMA} \)

\( \text{AR} = R \times R \times \text{DTAN}(\text{GAMMA}/2.0) \)

\( \text{XR} = R \times \text{DCOS} (\text{THETR}) / \text{DSQRT}(2.0) \)

\( \text{YR} = R \times \text{DSIN} (\text{THETR}) / \text{DSQRT}(2.0) \)

\( \text{ZR} = -\text{CL}/2.0 \)

\( \text{XOR} = \text{XR} \)

\( \text{YOR} = \text{YR} \)

IF (NE.LE.NSEC) GO TO 106

IF (NE.GT.NSEC*(NSL+1)) GO TO 107

\( \text{ZOR} = ((\text{NCS}-\text{NE}) + ((\text{NE}-\text{NSEC}-1)/\text{NSL}) \times \text{NSL}) \times \text{Z} \)

110 IF (NE.LE.NSEC. AND. NR.LE.NSEC) GO TO 111

IF (NE.GT.N-SEC. AND. NR.GT.N-SEC) GO TO 111

IF (NE.EQ.NR) GO TO 111

\( \text{QE} = \text{DSQRT}((\text{XE}-\text{XOE})**2+(\text{YE}-\text{YOE})**2+(\text{ZE}-\text{ZOE})**2) \)

\( \text{QR} = \text{DSQRT}((\text{XR}-\text{XOR})**2+(\text{YR}-\text{YOR})**2+(\text{ZR}-\text{ZOR})**2) \)

\( \text{D} = \text{DSQRT}((\text{XE}-\text{XR})**2+(\text{YE}-\text{YR})**2+(\text{ZE}-\text{ZR})**2) \)

\( \text{SE} = \text{DSQRT}((\text{XR}-\text{XOE})**2+(\text{YR}-\text{YOE})**2+(\text{ZR}-\text{ZOE})**2) \)

\( \text{SR} = \text{DSQRT}((\text{XE}-\text{XOR})**2+(\text{YE}-\text{YOR})**2+(\text{ZE}-\text{ZOR})**2) \)

IF (QE.EQ.0.OR.QR.EQ.0) GO TO 111

\( \text{F1} = \text{D}**2+\text{QE}**2-\text{SE}**2 \)

\( \text{F2} = \text{D}**2+\text{QR}**2-\text{SR}**2 \)

\( \text{F3} = 4.0 \times \text{PI} \times \text{QE} \times \text{QR} \times \text{D}**4 \)

\( \text{F4} = \text{F1}+\text{F2}/\text{F3} \)

\( \text{F} = (\text{NE}, \text{NR}) \times \text{F4} \times \text{AR} \)
GO TO 112
111 F(NE,NR)=O.DO
112 FER(NE)=FER(NE)+F(NE,NR)
116 CONTINUE
117 CONTINUE
DO 125 NE=1,N
DO 124 NR=1,N
IF(NE.GE.ISM.AND.NE.LE.LSM)GO TO 114
IF(NE.GE.ISM+NSL*(NSEC-1).AND.NE.LE.LSM+NSL*(NSEC-1))GO TO 114
DA(NE,NR)=-(1.DO-E)/E)*F(NE,NR)
GO TO 124
113 DA(NE,NR)=1.DO+FER(NE)*(1.DO-E)/E
GO TO 124
114 IF(NE.EQ.NR)GO TO 115
DA(NE,NR)=0.DO
GO TO 124
115 DA(NE,NR)=1.DO
124 CONTINUE
125 CONTINUE
RETURN
END

C SUBROUTINE TEMPI(I,J)
C
C IMPLICIT REAL*(A-H,O-Z)
DIMENSION TI(240,26),FER(240),O(240)
DIMENSION DA(240,240),D1(240,240),DX(240),DB(240),IPERM(480)
DIMENSION DA1(26,26),DI1(26,26),DX1(26),DB1(26),IPERM1(52)
DIMENSION F(240,240),DRZ(240)
COMMON/ZZ3/TR,TM,TI,TF,F,DA,DB,DX,FER,TIME,DI,TE
COMMON/ZZ4/R,DR,CL,W1,W2,Z,E,NSL,NSEC,ISM,LSM,NSM,NCS,N,M
COMMON/ZZ6/Y,DI1,DX1,DA1,C1,C2,C3
DO 209 I=1,N
DO 208 J=1,M
F1=DH*(DFLOAT(J-1))
IF(I.LE.NSEC.OR.I.GT.N-NSEC)GO TO 201
Y=R+F1
IF(I.GE.ISM.AND.I.LE.LSM)GO TO 207
IF(I.GE.ISM+NSL*(NSEC-1).AND.I.LE.LSM+NSL*(NSEC-1))GO TO 207
F2=DLDG(Y/R)/DLDG(R/(R+DR))
F3=C1*(TR-TE)+0.5D0*C2*((TR**2)-(TE**2))
F4=F2+F3
F5=C1*TR+0.5DO*C2*(TR**2)
F6=-(F4+F5)
F7=DSQRT((C1**2)-(2  D0*F6*C2))
F8=F7-C1
TI(I,J)=F8/C2
GO TO 208
201 IF(I.GT.N-NSEC)GO TO 202
Y=CL/2.DO+F1
F9=(CL/2.DO-Y)/DR
GO TO 203
202 Y=-CL/2.DO-F1
F9=(Y+CL/2.DO)/DR
203 F10=C1*(TR-TE)+0.5D0*C2*((TR**2)-(TE**2))
F11=F9+F10
F12=C1*TR+0.5DO*C2*(TR**2)
F13=-(F11+F12)
F14=DSQRT((C1**2)-(2  DO*C13*C2))
275

F15=F14-C1
TI(I,J)=F15/C2
GO TO 208
207 TI(I,J)=TM
208 CONTINUE
RETURN
209 CONTINUE
END

C...*******************************************************************************
SUBROUTINE HEAT(I)
C...*******************************************************************************
IMPLICIT REAL*8(A-H,O-Z)
DIMENSION TI(240,26),FER(240),Q(240)
DIMENSION DA(240,240),DT(240,240),DX(240),DB(240),IPERM(480)
DIMENSION DA1(26,26),DT1(26,26),DX1(26),DB1(26),IPERM1(52)
DIMENSION F(240,240),DRZ(240)
COMMON/ZZ2/GAMMA,THETE,THETR,AR,Q,QT,QL,QTL
COMMON/ZZ3/TR,TM,TI,TF,F,DA,DB,DX,FER,TIME,DTIME,TE,DH
COMMON/ZZ4/R,DR,CL,W1,W2,Z,E,NSL,NSEC,ISM,LSM,NSM,NCS,N,M
Q1=0.DO
QT=0.DO
QL=0.DO
DO 300 I=1,N
Q(I)=0.DO
300 CONTINUE
DO 302 I=1,N
DO 301 J=1,N
Q(I)=Q(I)+F(I,J)*(DX(I)-DX(J))/FER(I)
301 CONTINUE
302 CONTINUE
DO 304 I=1,N
IF(I.GT.NSEC.AND.I.LE.N-NSEC)GO TO 303
A=R*R*DTAN(GAMMA/2.DO)
QT=QT+Q(I)*A
GO TO 304
303 A=GAMMA*R*Z
QT=QT+Q(I)*A
304 CONTINUE
DO 305 I=ISM,LSM
A=GAMMA*R*Z
Q1=Q1+Q(I)*A
305 CONTINUE
QL=2.DO*Q1
QTL=QTL+QL*DTIME
RETURN
END
C...*******************************************************************************
SUBROUTINE MATRA(I,J)
C...*******************************************************************************
IMPLICIT REAL*8(A-H,O-Z)
DIMENSION TI(240,26),FER(240),Q(240)
DIMENSION DA(240,240),DT(240,240),DX(240),DB(240),IPERM(480)
DIMENSION DA1(26,26),DT1(26,26),DX1(26),DB1(26),IPERM1(52)
DIMENSION F(240,240),DRZ(240)
COMMON/ZZ2/GAMMA,THETE,THETR,AR,Q,QT,QL,QTL
COMMON/ZZ3/TR,TM,TI,TF,F,DA,DB,DX,FER,TIME,DTIME,TE,DH
COMMON/ZZ4/R,DR,CL,W1,W2,Z,E,NSL,NSEC,ISM,LSM,NSM,NCS,N,M
COMMON/ZZ5/COND,DIF,PENT,PI,SIGMA,HCAP,DENST,HTRAN
COMMON/ZZ6/Y,DA1,C1,C2,C3
DO 470 J=1,M
TCOND = C3*(C1+C2*TI(I,J))
HEAT = HCAP*DENST/(TCOND*DTIME)
IF(I.GT.NSEC.AND.I.LE.N-NSEC)GO TO 403
A = R*R*DTAN(GAMMA/2.DO)
V = 0.5DO*DH*A
IF(J.GT.1)GO TO 401
DA1(J,J) = HEAT*V*A/DH
DA1(J,J+1) = -A/DH
DB1(J) = HEAT*V*TI(I,J)-Q(I)*A/TCOND
GO TO 470
401. IF(I.EQ.M)GO TO 402
DA1(J,J-1) = -A/DH
DA1(J,J) = 2.DO*(HEAT*V*A/DH)
DA1(J,J+1) = -A/DH
DB1(J) = 2.DO*HEAT*V*TI(I,J)
GO TO 470
402 DA1(J,J-1) = -1/DH
DA1(J,J) = HEAT*V*A/DH
F1 = SIGMA*(((TI(I,J)+273.DO)**4-(TM+273.DO)**4)
F2 = HTRAN*(TI(I,J)-TM)
DB1(J) = HEAT*V*TI(I,J)-(A/TCOND)*(F1+F2)
GO TO 470
403 Y = R+DH*(DFLOAT(J-1))
IF(J.GT.1)GO TO 404
SP = (GAMMA*Z/DH)*(Y-0.5DO*DH)
V = GAMMA*Z*(DH**2+4.DO*Y*DH)/8.DO
DA1(J,J) = HEAT*V*SP
DA1(J,J+1) = -SP
DB1(J) = HEAT*V*TI(I,J)-Q(I)*GAMMA*Y*Z/TCOND
GO TO 470
404 IF(I.EQ.M)GO TO 405
SN = (GAMMA*Z/DH)*(Y-0.5DO*DH)
SP = (GAMMA*Z/DH)*(Y+0.5DO*DH)
V = GAMMA*Z*Y*DH
DA1(J,J-1) = -SN
DA1(J,J) = HEAT*V+SN+SP
DA1(J,J+1) = -SP
DB1(J) = HEAT*V*TI(I,J)
GO TO 470
405 SN = (GAMMA*Z/DH)*(Y-0.5DO*DH)
V = GAMMA*Z*(4.DO*Y*DH-DH**2)
DA1(J,J-1) = -SN
DA1(J,J) = HEAT*V+SN
F1 = SIGMA*(((TI(I,J)+273.DO)**4-(TM+273.DO)**4)
F2 = HTRAN*(TI(I,J)-TM)
DB1(J) = HEAT*V*TI(I,J)-(GAMMA*Z*Y/TCOND)*(F1+F2)
470 CONTINUE
RETURN
END