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**EXPERIMENTAL AND NUMERICAL INVESTIGATION OF THE EFFECT OF
PROCESS PARAMETERS ON THE EROSION WEAR OF DIE CASTING DIES**

DISSERTATION

Presented in partial fulfillment of the requirements for the degree Doctor of
Philosophy in the Graduate School of The Ohio State University

By

Krishnan Venkatesan, MS

The Ohio State University

1995

Doctoral Examination Committee :

Dr. Rajiv Shivpuri

Dr. Jerald Brevick

Dr. June K. Lee

Approved by

Dr. Rajiv Shivpuri, Adviser

Industrial and Systems Engineering
Graduate Program

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To my Parents with Love

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VITA

September 14, 1968	Born in Madurai, India
1989	B. Tech Mechanical Engineering BHU, Varanasi, India
1991	MS Engineering Mecahnics Dept. of EM, Ohio State Univ.
1991-Present	Graduate Research Associate The Ohio State University, Columbus, Ohio

FIELDS OF STUDY

Major Field : Industrial and Systems Engineering

Studies in Manufacturing, Die Casting and Forming, Numerical Analysis

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CHAPTER I

INTRODUCTION AND PROBLEM DEFINITION

1.1 INTRODUCTION

Die casting processes are high volume production processes which produce geometrically complex parts of nonferrous metals with excellent surface finishes and low scrap rate. Production rate of 200 parts per hour and production batches of 300,000 parts are not uncommon. The U. S. die casting industry consists of about 800 production facilities which include a few plants at the OEMs and a large number of small manufacturers. Die casting is the second largest segment of the casting industry with annual production of eight billion dollars worth of castings per year.

The die casting process involves the use of permanent steel molds typically made of expensive H-13 die steel to produce castings continuously using the same die and machine. While such an arrangement provides for high production rates and efficiencies of labor, it also involves high startup costs. Dies are typically very expensive (often costing upto a million dollars) and machines capable of producing the high pressures (2,000 psi is common) and velocities (60 m/s is common) required for successful production, are required. Consequently the die casting process becomes viable from a cost/part standpoint only when a very large number of castings are required (50,000-100,000 parts are often a minimum requirement) to be produced. Because of the high cost of dies, an extension of die life will consequently have a significant impact on the economics of the die casting operation.

Die castings are generally produced by using two steel die halves called the cover and ejector die halves. Each of the die halves usually contain a portion of the die cavity. The process sequences are :

- Die Closing: (Figure 1.1.1) The die halves are closed and locked by the die casting machine. The required clamping force during the process may be in the order of hundreds of tons.
- Cavity Filling: (Figure 1.1.2) The next step involves the injection of the molten metal into the die cavity under very high pressures. Rapid filling of the die cavity is desirable as it reduces the chance for premature solidification that could shut off the flow to portions of the die cavity. Typical filling times are in the order of milliseconds with typical flow velocities of the order of 30m/sec.
- Casting Solidification: (Figure 1.1.3) The molten metal is then allowed to rapidly solidify in the die cavity. Heat management is critical in this step as proper location of cooling channels will help promote directional solidification of the casting and consequently reduce shrinkage porosity. Metal pressure intensification at the end of the injection cycle is sometimes used to feed shrinkage.
- Parts Ejection: (Figure 1.1.4) After the casting has solidified, the die halves are opened and the part is ejected.
- Lubrication: The open die halves are sprayed with water based lubricants and anti solder compounds. The die surface temperatures drop drastically during this stage.

Figure 1.1 Operating sequence for a cold chamber die casting process (Lewis, Chou, Chen, Han, & Kim, 1987).

Once the die is closed, molten metal is ladled into a shot chamber after which a plunger pushes the molten metal into the die cavity at a high velocity. This process is one of the most critical with respect to the quality of the resultant die casting. A number of speed variations are available, which are applied during filling, to reduce turbulence and wave formation that causes air entrapment and consequent porosity in the die casting. The flow of the molten metal from the shot sleeve into the periphery of the casting is accomplished by a runner system, while the entry of the molten metal into the casting is controlled by a gating system.

An improper design of the runner system and die or an incorrect choice of process parameters will cause defects in the die cast parts and a reduction in the die life. The causes of some of the commonly seen die casting defects and the common modes of die wear and failure are described in the following sections, to provide a better understanding of the design and modeling issues in die casting.

1.1.1 Defects in Die Castings

An optimum gating system design will create a preferred filling pattern within the die cavity and consequently produce sound castings. An improper design of the runner gate system or of the die cavity will however result in some of the following defects commonly seen in die castings:

- Lack of fill: The metal solidifies before the cavity fills, resulting in an incomplete casting. One of the common causes of this is due to the presence of cold metal and or cold die that promotes faster solidification before the cavity is completely filled.
- Cold shut: This is similar to the lack of fill defect seen in die castings and is produced where partially solidified streams of metal come together but do not quite fuse due to low die temperatures, and insufficient superheat and speed of shot.

- **Porosity**: Porosity is considered to be any void within the die cavity that was not intentionally created. The two common modes of porosity seen in die castings are:
 - **Gas porosity**: This is commonly caused either by gas being entrapped in the hydraulic actuator or within the die cavity during filling. This entrapment of air is commonly caused by the turbulent nature of the metal flow in the hydraulic actuator and in the die cavity as well as due to improper vent and overflow design. Other causes of gas porosity include generated porosity due to the presence of a die lubricant or the release of dissolved gases (hydrogen) during solidification.
 - **Shrinkage porosity**: This is caused by a lack of directional solidification. Typically one would like the metal shrinkage during solidification to be fed by molten metal from the gate. However due to improper design or due to a cold die, portions of the cavity will be cut off from the source of molten metal at the gate. This may be due to the gate freezing earlier than the rest of the part, or due to some sections being cut off due to the solidification of thinner sections closer to the gate. Pressure intensification during solidification is commonly practiced to pack the metal and reduce the shrinkage. However this method becomes ineffective once the gate freezes.

1.1.2 Limitations on Die Life

Besides the defects commonly seen in die castings, die wear and consequently die failure is a significant issue in die castings due to the high cost of dies and will be the main focus of this research. Dies typically cost up to a million dollars and the life of the die often becomes the significant driving factor in the economics of the die casting operation. The reason for die wear is that the die casting process inherently requires

multiple reuse of the die (typically more than 100,000 castings are produced per die campaign, with production rates of 2,500 shots per 24-hour day). Low cycle times in die casting therefore dictate that molten metal be introduced into the die cavity at high flow velocities, and that the molten metal rapidly solidify (large thermal gradients) to the part shape. Flow velocities of 40 m/s (132 ft/sec) and die temperature gradients as high as 1000 C/cm (4500 F/in) are common. While these severe conditions are mandated to achieve these high production rates, they also limit the die materials that can be used and their respective production campaign. Wear phenomena are widely observed in H-13 die steel, the most commonly used die material due to this severe mechanical and thermal loading. The major wear mechanisms leading to premature die failure are:

- Erosion or Washout: This is a result of the high velocities with which the molten metal impinges parts of the die cavity, causing steel to be washed away with the melt. Most die casting dies have complicated geometrical features such as cores, pins, ribs and corners which are especially prone to erosive wear. This reduces the ability of the die to maintain dimensional tolerances, and often requires rebuilding of regions of the die that have suffered extensive washout.
- Heat checking (thermal cracking): This is caused by the thermal fatigue due to the alternate heating and cooling of the die surface during die casting. The large thermal gradients created cause the die surface to be in compression during heating, and tension during cooling of the die. This results in thermal cracking which appears as cracks called heat checks on the die surface which reduce part surface finish, and ultimately lead to die failure.
- Soldering and Corrosion: This is caused by the chemical interaction of the casting alloy and the die material during

solidification. This results in parts of the molten metal sticking to the die surface (soldering) which obviously produces defective castings, or part of the die surface being corroded.

Some of the factors that limit die casting die life for different casting metals are shown in Table 1.1 from Malm and Tidlund (Malm & Tidlund, 1979). After extensive technical market research they concluded that die erosion and heat checking were the two most important factors that limit the life of die steels.

Table 1.1 Causes of die failure in the die casting of various metals From Malm and Tidlund(Malm & Tidlund, 1979).

Casting alloy	Factors which limit die life	Normal life, number of shots	
		Die	Core
Zinc	Erosion, Mechanical abrasion	0.5 - 5 mill.	0.5 - 5 mill.
Aluminum	Heat checking, Cracking, Erosion, Low comp. strength	100 000 - 250 000	50 000 - 150 000
Copper (Brass)	Heat checking, Low comp. strength, Erosion, Cracking	10 000 - 100 000	3 000 - 20 000

1.2 PROBLEM DESCRIPTION AND MOTIVATION

Due to the high cost of dies (some dies cost upto a million dollars) these wear phenomena significantly affect the economics of the die casting operation. These wear phenomena beside reducing die life, cause extensive losses because of machine downtime (and a consequent loss of production efficiency) and die re-work costs which often prove more significant than the costs of building a new die.

While soldering and heat checking are significant factors which affect die life, this research will focus primarily on erosive wear in die casting dies. Erosive wear which is already a quite significant factor affecting die casting die life, will become even more so with the increasing use of erosive metal matrix composites, semi-solid die casting, and with the use of exotic high strength alloys for automotive applications (Lin, 1995) which cause significantly higher erosive wear. This research will thus focus on developing a methodology to reduce die erosive wear and increase die life. The basic features of this methodology are described below and in the next section.

Because of the high cost of dies, the economics of the die casting operation will be affected significantly by an increase in die life caused by a decrease in die erosion. There are two basic ways to extend die life:

- One way is to develop new wear resistant die materials possibly ceramics or composite materials, develop new surface treatments including coatings, improve heat treatment of existing H-13 dies for higher toughness, and develop better lubricants that can protect the die material. In other words develop die material / surface treatment / lubrication combinations that will be better able to resist erosion, than the conventional die steel and alloy combination. There has been a reasonable body of work, which has attempted to reduce die erosion using this approach (Yu, Chu, & Shivpuri, 1993a; Yu, Chu, & Shivpuri, 1993b).

- A second approach, which will be the main focus of this dissertation is to develop a wear prediction system that will predict the extent and likely location of erosion at the design stage prior to die building. Such a system would be extremely useful as die erosion may be reduced by part / gate / core redesign or process parameter changes at the design stage prior to die building. If such a redesign is not possible because of other constraints, such a system should at least allow decisions to be made regarding coating of specific locations of dies (which are likely to be subjected to extreme erosion), or the building of replaceable inserts at regions that have been predicted to erode severely. Thus such a system should provide a significant tool to the die designer to decide on an acceptable die design that beside producing a sound casting, would also extend the life of the die, and reduce machine downtime due to repairs. Such a system would be much cheaper than coating / surface treatments which are often difficult to apply, have to be periodically reapplied, and in a lot of cases are ineffective.

1.3 RESEARCH OBJECTIVE AND PLAN

Thus the main focus of this research will be the development of such a wear prediction system, that will allow the die designer to make design changes prior to die building, that reduce the die erosive wear and increase die wear. Such a system, will have a significant impact on the economics of the die casting operation, because of the high costs of die replacement, and die downtime for repair.

Such a wear prediction systems should have two parts

- **Erosive Wear models:** Accurate wear models which predict the extent and location of die erosive wear as a function of process parameters such as metal velocity, die temperature, metal temperature etc. at that die location.

- **Numerical Models:** Accurate numerical models which predict accurately the values of process parameters such as velocity and temperature that appear to influence die erosion, at various locations of the die. Since die erosion mostly occurs during the filling stage, such a numerical model should predict accurately the complex filling patterns in die casting.

Thus a die designer will hypothetically use the numerical system to accurately predict the values of important process parameters at various locations in the die, and then use these determined values of process parameters in the erosive wear models to obtain the erosion rate at various die locations. From these predicted values of erosive wear rates, die design decisions can be made.

This research represents the first step in developing such a system. An analysis of the literature in the die casting area (Chapter II) reveals little or no work in the development of erosive wear models for the complex (high temperature erosive-corrosive environment) conditions present in die casting. Wear models have been developed from simulative tests in other applications and simple corrosion tests have been developed in die casting. The corrosive dip tests account for the corrosive action of molten metal but do not consider the physical effect of the impact of high velocity metal impact which is the main cause of die erosion. A large volume of these simulative erosive wear tests have been conducted to study erosion in areas unrelated to die casting (such as rain erosion of aircraft, turbine blade erosion, erosion in coal conversion plants etc.) and have resulted in some erosive wear models that may be used as a starting point for developing erosive wear models for die casting. However these models although they provide some clues, are applicable only in the narrow process parameter ranges within which these simulative experiments have been conducted. Therefore the results from neither class of tests can be directly used in the proposed predictive system.

Consequently one of the main focus areas of this research was to develop an experimental test procedure, to understand die erosive wear and to quantify the effects of important process conditions in causing erosion in die casting dies. Such an experimental erosive wear test procedure was developed to evaluate die erosive wear (and to evaluate coatings and surface treatments) under actual production conditions (Chou, Cheng, & Shivpuri, 1993b; Chou & Shivpuri, 1993a). The tests were carried out on a commercial 250 ton Buhler die casting machine and an accelerated procedure was developed to obtain meaningful wear results in a reasonable amount of time. This test procedure was then used to evaluate the effect of physical process parameters on erosive wear under actual production conditions. An experimental plan was developed to evaluate the effect of specific parameters and the results of these experiments are presented in Chapters IV and V.

To develop wear models from experimental data obtained from these erosive wear tests, the values of important process parameters at the erosive wear pins, and at different locations in the die cavity must be known during the filling and solidification stages, for different choices of process conditions. These values of flow and thermal parameters are very difficult to measure during a die casting operation. Therefore, a strategy was implemented in which the process parameters at different locations in the cavity are again determined by numerical simulations of the filling and solidification stages of the process.

Thus the numerical model is used both to come up with erosive wear models and to provide a better understanding of die erosion, as well as in a wear prediction system to predict the extent of die erosion using these developed erosive wear models. A brief flow chart of this approach is shown in Figure 1.2.

Figure 1.2 Flow chart of approach used to develop a wear prediction system.

Since erosive wear takes place primarily during the filling stage, the most important requirement of a die casting process simulation model for this study is the fluid flow modeling during mold filling. The modeling of the filling of a mold cavity in die casting is however very difficult. This is because fluid flow during this stage is highly transient, inertia dominated and often turbulent with Reynolds numbers in excess of 10,000, due to flow of high velocity metal through rapidly changing geometry in the runner-gate system or in the casting cavity. Therefore common simplifications used for modeling less taxing flow problems in sand casting and injection molding, such as the assumption of laminar and viscosity dominated flow, are no longer valid. Thus any model used for evaluation of the effect of process parameters must be capable of handling

momentum dominated flow, jetting, and the complex free surfaces commonly seen in die casting. A survey of numerical models (Chapter III) revealed that there were very few models capable of modeling the complex flow conditions in die casting die filling as a large number of these flow models utilize laminar and viscosity dominated flow assumptions. These assumptions are certainly applicable for injection molding of plastics and sand casting but are not valid for the transient, inertia dominated and turbulent flow conditions in die castings.

After this literature survey a model based on FLOW-3D (1994) a three dimensional finite difference software with the capability of handling the complex momentum driven flows common in die casting was chosen for this study. A numerical model based on FLOW-3D which has traditionally been used for space applications was then developed, to simulate the transient, inertia dominated and complex cavity filling in high pressure die castings. In order to determine the accuracy of this approach in modeling the complicated flow conditions in die casting, a comparison with experimental filling patterns obtained from water analogy results in the literature was conducted and selected results are presented in Chapter III. The results show that the model appeared to simulate the inertia dominated transient flow of die castings fairly accurately, thereby demonstrating that it is possible to obtain the values of process parameters at various locations in the die cavity, a requirement for the numerical part of the wear prediction system. Using the numerical models and the experimental wear data, conclusions regarding the nature of erosion in die casting dies were reached and preliminary wear models developed.

1.4 DISSERTATION OUTLINE

The above represents a brief synopsis of the main objectives of this research and the approaches used to meet these objectives. Having described the main objectives and approaches to be used in this research, a brief outline of the organization of this dissertation is presented below.

Chapter II describes a literature survey of the causes, mechanisms and models of erosive wear in die casting dies, and the relevant wear models obtained from simulative studies that could be potentially used in this research. A summary of the application of these models to the current study is also included.

Chapter III discusses the difficulties in obtaining a numerical model for the filling stage of the die casting process and outlines the reasons for choosing the model used in this study. Benchmarking studies using water models that were used to verify these models are also presented in this chapter.

Because of the absence of wear models that could be applied in a wear prediction system, an experimental procedure was proposed and conducted to quantify the effect of process parameters on erosive wear in die casting dies. The details of this experimental plan, procedure, execution, errors and problems are all discussed in Chapter IV.

In Chapter V results from the experimental studies and numerical simulations of the filling process in the experimental dies using the actual process conditions, are combined to obtain tentative wear models of the effect of metal velocity, angle of metal impact, die geometry and metal temperature. The applicability and significance is discussed along with surprising conclusions on the effect of metal temperature.

Chapter VI summarizes the work in this research, and suggests future work that could further complement this research, and help reduce die wear and increase the die life of die casting dies.

1.5 SUMMARY

This thesis details the work that was conducted to come up with a wear prediction system that would predict the extent and location of die erosive wear at the design stage. Such a wear prediction system should contain

accurate numerical models and erosive wear models. Thus a die designer will hypothetically use the numerical system to accurately predict the values of important process parameters at various locations in the die, and then use these determined values of process parameters in the erosive wear models to obtain the erosion rate at various die locations. Because such a wear prediction system has not been previously attempted for this complex coupled high temperature erosion-corrosion problem, such a wear prediction system is still at its early stages. Nevertheless this research should go a long way toward an understanding of the die erosion problem and represents the early stages of research to develop a powerful tool for the die designer to increase die life by design and process parameter changes alone without resorting to coating/surface treatments which increase die and maintenance costs.

CHAPTER II

WEAR OF DIE CASTING DIES: CAUSES, MECHANISMS AND THEORIES OF EROSION WEAR

2.1 INTRODUCTION

There have been very few studies that have attempted to study systematically the effect of process parameters in the erosion of die casting dies. There have on the other hand been a number of simulative tests that have developed wear models under controlled laboratory conditions, which may or may not be applicable to erosion in die casting. This chapter represents a survey of the literature in the area of die casting erosion and in the area of development of erosive wear models. From this survey, it appears that while there are no models that can be directly applied to the proposed wear prediction system, definite clues about the nature of erosion in die casting emerge. These provide a good starting point for the development of erosive wear models in die casting, and a comparison point to analyze experimental results obtained as will be discussed in later chapters.

The next section deals with a survey of the literature in the area of previous erosive wear studies in die casting. Later sections will deal with erosive wear models developed from simulative tests and other relevant applications.

2.2 PRIOR WORK IN EROSION WEAR MODELING IN DIE CASTING

Previous research on die casting erosion can be classified in two broad categories

- Empirical observation of die casting erosion
- Laboratory simulative tests that evaluate corrosion effects which are isolated from other effects. These are primarily screening tests often used to evaluate coatings and surface treatments in an accelerated corrosive environment, that is similar to the corrosive environment of die casting. They are also sometimes used for phenomenological studies of corrosion, where this corrosion phenomena is isolated from other mechanisms of wear. This provides a better understanding of the corrosive phenomena that takes place without any outside influences which could corrupt these diffusive phenomena.

A detailed description of previous studies under these broad headings is described below.

2.2.1 EROSION WEAR IN DIE CASTING: TYPES AND CAUSES

Most of the types and causes of erosive wear in die casting dies have been ascertained from empirical observation of die wear in production dies. While these studies are not systematic by their very nature, they give important clues of the types and causes of erosive wear in die casting dies. A brief synopsis of some of these studies is presented below.

2.2.1.1 Empirical observation of erosive wear in die casting dies

There have been a few publications which have outlined the likely causes of erosion in die casting dies from empirical observations. Barton (Barton,

1965) for example classified erosive wear in die casting dies as being of four significant types:

- Gate Erosion
- Washout
- Erosion caused by flow separation:
- Cavitation

Each of these distinct forms of die erosion, and some of the theories of the causes of these erosion mechanisms is discussed below.

Gate erosion: Barton (Barton, 1965) observed that a type of erosion that occurs in all dies, although it often goes unnoticed, was erosion at the gate. Simply stated, after a die has been in service for a long time, a gate is larger than it was initially. As expected this change was most noticeable with castings that have long shallow gates.

Barton (Barton, 1965) stated that gate erosion was clearly due to the passage across the surface of a high velocity metal stream, but it was highly unlikely that the die steel was rubbed away by the friction of the stream. Gate erosion was attributed to the repeated abrasion and regeneration of the amorphous oxide film which is created on the die whenever it is in contact with the molten metal. This action takes place only at the gate since it is only here that the metal velocity is sufficient to allow fluid metal to flow in contact with the die surface. In the runner and cavity, the lower rate of flow insures that a thin static boundary layer is set up adjacent to the die surface.

Washout: A second type of erosion occurs when the metal stream impinges directly on the cavity surface (e.g. a core or pin) at a short

distance from the gate (Figure 2.1). This type is highly localized along the line of the gate and is often termed as washout. Conditions here are exactly the reverse of those at the gate. Instead of a stream moving across a surface at high velocity (Figure 2.1) and low pressure, low velocity and high pressure conditions exist at the location of washout zones. Washout is the most common type of die erosion and also the most significant in relation to die performance.

Figure 2.1 Washout, caused by the impinging of a metal stream on the die or core close to a gate. Figure a) shows an erosion site opposite a shallow gate and Figure b) shows erosion opposite a pin-gate. From Barton (Barton, 1965)

The distinctive feature of an impinging jet that causes washout, is that it heats the die surface intensively, but very locally throughout the injection phase. This heating causes severe thermal stressing along the line of impingement. Because it so highly localized, the shear stress between the oxide film and the underlying metal (which have different rates of thermal expansion) are also intensified, and the film tends to break down under the pounding of the pulsating metal stream (Barton, 1965).

Barton also suggested that this process did not proceed very far because it caused a drag on the casting during ejection, which causes the core or pin in the path of the impinging metal to be cleaned or replaced.

Although both these types of erosion are mediated by the breakdown of the oxide film, their character is quite distinct. They have in common only their association with the fast moving metal stream at the gate.

Erosion caused by flow separation: Another cause of erosive wear was reported by Smith (1964) to be caused by flow separation at some point, where due to a change in direction the molten metal separates from the die surface. This was used to explain erosion where gate erosion and washout were unlikely to be the causes of the erosive wear observed. Since any normal pressure at the wall will prevent separation of the metal stream from the die surface, flow separation will occur at places of zero pressure. This flow separation is fairly common in die castings, especially when molten metal is forced through a narrow constricted gate or when flow is projected around a bend (flow here was assumed similar to a venturi section as shown in Figure 2.2). Smith surmised that erosive wear was caused immediately ahead of the constriction due to cavitation, as seen in Figure 2.2.

Barton however suggested that backfill was a more likely cause of the erosive wear due to flow separation, since constrictions and flow around a bend caused jetting flow and backfilling of the region just ahead of a constriction, which precluded cavitation as a mechanism in these locations. Figure 2.3 shows the mechanism of filling of a similar section to

that of Smith (1964) (Figure 2.3). According to Barton cavitation and backfill both happen to be associated with locations where the metal stream separates from the die surface, but that the correlation between backfilling and erosion was more direct. This does make sense, since jetting flow is widely believed to be the mechanism of cavity fill, which precludes the Smith mechanism of filling (Figure 2.3) and consequently erosive wear.

Nevertheless, it remained to be shown how anything that took place during the final phase of backfilling could initiate erosion. Barton suggested that the flow was highly turbulent during backfilling, and the metal returning along the cavity both compressed the air ahead of it and carried it into the body of the metal as dispersed and contracting bubbles. As the back pressure rises, both flow rate and strength of the vorticity diminish, and it is not easy for the remaining air to be ingested by the metal. Some residual air pockets therefore are likely to remain in contact with the metal as the final compaction pressure became effective. These will be sharply reduced in volume before being swept into the turbulent metal mass causing erosion.

This hypothesis however remains unproven, although Barton utilized the theory of imploding cavities in the design of shaped explosive charges, which penetrate metal much in the manner of die erosion, to explain his backfill theory of erosive wear. (Much of the work that seems to have been performed in erosive wear mechanisms in die casting utilize analogies in often unrelated areas because of the lack of data in die casting processes. The applicability of some of these theories to die casting is sometimes unclear, although no other option is usually available).

Figure 2.2 In a venturi section, cavitation can occur immediately downstream of a constriction, according to Smith (1965). (Barton, 1965)

Figure 2.3 Mechanism of filling ahead of a constriction according to Barton (Barton, 1965). Notice the jetting flow as opposed to the flow in Figure 2.2.

Bowden et al. (Bowden, 1965) in their study of the formation of needle thin jets within bubbles, employed two-dimensional bubbles, produced by a ring of viscous fluid compressed rapidly between two glass blocks. Under compression, the fluid moves both inward and outward resulting in the generation of a micro Munro jet (Figure 2.4).

Using the theory of exploding charges and the above theory of the formation of needle-thin jets within bubbles by Bowden et al. (Bowden, 1965), Barton developed a backfill theory which suggests that a munro jet (Figure 2.4) results whenever a cavity is collapsed at a sufficiently high rate. If the cavity is a bubble, the jet is initiated at the point of greatest curvature - that is, of least radius.

Since a die casting alloy does not wet the die, the point of greatest curvature of the air-metal interface is virtually certain to be near the zenith of the meniscus, and the micro jet is more likely to strike the surface almost square than meet it glancingly.

Barton thus suggested, that the micro Munro jet provided an adequate mechanism for concentrating the energy of a rapidly compressed air pocket and deploying it against a very localized area of the die surface (Figure 2.5). In other words it was suggested that the pocket acted like a tiny antitank charge.

If this explains how the surface can be damaged, it does not of itself explain the nature of the damage - the characteristic pitting. Reverting to the assumption that air pockets which cause the trouble are caused by the residual air not swept into the metal by the turbulent backfilling flow, Barton suggested that it was obvious that once a shallow depression in the die surface had been formed, air was more likely to be left pocketed there than on the smoother surface around. Any such depression thus tends to be progressively deepened, to produce the pitting which is observed in practice.

Figure 2.4 Micro Munro jets formed by compressing a ring of viscous liquid rapidly between two glass blocks causing the fluid to move both inward and outward, under the compressive forces. (Bowden, 1965)

Figure 2.5. Nature of Micro Munro jet formed in an air pocket according to Barton (Barton, 1965) This will cause the jet to hit the die surface squarely, rather than in a glancing manner in the actual die casting process.

It was also of interest to Barton, that Bowden also observed the formation of micro-Munro jets where droplets coalesce. Barton suggested that this point was possibly relevant to the well established fact that soldering in die casting dies is frequently associated with the employment of shallow gates which atomize the metal stream. It was surmised that the impacting of successive droplets, by generating a great number of these tiny jets, was the immediate cause of soldering (Figure 2.6).

These jets formed by the impact of metal droplets have a velocity several times that of the droplets themselves; because of the mode of their formation, they do not have any preferred orientation with respect to the die surface. Barton therefore suggested that many of these droplets strike the surface glancingly, giving a scouring action which abrades and removes the inert oxide film and so promotes mechanical adhesion of the die casting alloy to the die steel.

Figure 2.6 Formation of jets where droplets coalesce. Impacting of successive droplets, generating jets might be the cause of soldering (Barton, 1965).

Cavitation erosion: At high speeds common in die casting, cavitation bubbles are generated in the flow (Stellar, Krzytofowicz, & Reymann, 1974), (Brunton, 1970). When they collapse at the die surface, large forces are generated which produce die erosion. Davis and Murray (Davis & Murray, 1981) reported that cavitation was a common cause of erosion in the die casting of zinc alloys. Zinc die casting utilizes high gate velocities (40 - 60 m/s) to produce high quality castings. At high speeds however cavitation bubbles are generated in the flow. When clusters of cavitation bubbles collapse at the die surface, large forces are generated which produce die erosion. In zinc die casting, cavitation may occur anywhere in the feed system, cavity, gooseneck or the nozzle.

Cavitation bubbles in die casting are typically produced in places where low pressures occur, due to geometrical features. At bends, changes of cross section, junctions between runners, or protrusions such as ejector pins, there will be sharp local changes in velocity and pressure (often a result of poor runner design) and a possibility of generating cavitation bubbles. These will be carried downstream and may collapse on die surfaces, causing damage (Similar to the mechanism described by Smith (1965) shown in Figure 2.2).

Cavitation may also cause chemical or corrosive attack on the die surface (Davis & Murray, 1981) and the tendency toward this will be affected by molten alloy composition and casting temperature. Protective films of oxide, or die sprays on the die surface may be removed due to cavitation erosion. Any tendency to chemical attack, will thus be accelerated.

Davis and Murray show a number of examples of cavitation in zinc die casting dies, and try to correlate their empirical observations of die erosion with theories of cavitation erosion. For example Figure 2.7 shows the cavitation damage that occurs in a Tee junction in a multi cavity die after a large number of shots. Even at a low speed of 20 m/s the junction

generated eddies which produced local velocity and pressure variations that resulted in cavitation erosion (Davis & Murray, 1981). It is however not as significant in aluminum casting because of lower velocities.

Figure 2.7 Damage in a runner Tee junction due to cavitation. From Davis and Murray (Davis & Murray, 1981)

2.2.1.2 Simulative dip tests to evaluate corrosive wear

While the published work on the types of die erosion is primarily based on empirical observations of the wear in production dies, a review of the literature shows no studies that have attempted to quantify the effect of process parameters on erosive wear in die casting. There have however been a few different dip tests developed over the years to evaluate the effect of steel grade, heat treatment, surface treatment (coatings) and melt composition on corrosive wear and/or heat checking. These wear tests typically involve dipping of pins in molten zinc (Malm & Tidlund, 1979), or molten aluminum alloy (Yu, et al., 1993a; Yu, et al., 1993b) in order to simulate the corrosive wear or heat checking (Benedyk, Moracz, & Wallace, 1970), mechanisms seen in actual die casting dies during production. From these dip tests, some of the following conclusions were drawn:

- Melt alloying composition significantly affects the corrosive wear phenomena, with pure metals likely to be more prone to corrosion than alloys (Malm & Tidlund, 1979), (Yu, et al., 1993a; Yu, et al., 1993b), and Benedyk et al., (Benedyk, et al., 1970) (Figure 2.8). One possible explanation for this is that, the die casting metals have a limited solubility of iron, which may be diffused from the die (possibly H-13) steel. Diffusion of iron into a pure metal melt, is therefore more likely than an alloy which has a certain composition of iron already dissolved in it.
- The corrosion attacks generally decrease with decreasing melt temperature (Malm & Tidlund, 1979), Yu and Shivpuri (Yu, et al., 1993a; Yu, et al., 1993b) (Figure 2.9). This is not surprising as hardness is typically reduced and diffusion rates typically increase, at higher temperatures. This finding though intuitively obvious is contrary to the results reported in this paper, and the reasons for this divergence will be explained in a later section.

- Malm and Tidlund (Malm & Tidlund, 1979) reported that hardened and tempered samples generally show smaller loss than soft annealed samples, which is certainly in agreement with current industrial knowledge and practice. However in the corrosive wear studies conducted at Ohio State (Yu, et al., 1993a; Yu, et al., 1993b), Yu and Shivpuri report that metal hardness is relatively insignificant in resisting the diffusive attack of molten aluminum (Figure 2.10). These tests however do not take the effect of high velocity metal impact, which is the main driver for erosive wear.
- If metallic contact between the tool steel and the melt can be avoided, such as with an oxide film, the risk of severe erosion attack decreases significantly (Malm & Tidlund, 1979). This observation is similar to that of Barton (Barton, 1965) who suggested that one of the mechanisms of gate erosion and washout was the abrasion and regeneration of the oxide film which is created on the die surface. This certainly suggests that coatings would provide a mechanical barrier to reduce wear in die casting dies.

The corrosive dip tests are however merely simulative tests which consider the effect of metal diffusion only, and do not measure the effects of physical parameters like metal velocity and angle of metal impact under real production conditions, which are more critical to the erosive wear in die casting dies. Since these physical parameters are the primary drivers of erosion and washout in die casting, any test which attempts to study erosive wear must consider the effects of these process parameters. These dip tests are however excellent screen tests to evaluate coatings and surface treatments under laboratory conditions. They are also excellent for phenomenological studies of corrosion, where this corrosion phenomena is isolated from other mechanisms of wear. This provides a better understanding of the corrosive phenomena that takes place without any outside influences which could corrupt these diffusive phenomena.

Figure 2.8 Effect of alloy composition on corrosion of H-13 pins (Malm & Tidlund, 1979)

Figure 2.9 Effects of melt temperature and corrosion time on the dissolution of H13 pins (Yu, et al., 1993a; Yu, et al., 1993b).

Figure 2.10 Effect of hardness on dissolution of H-13 pins (Yu, et al., 1993a; Yu, et al., 1993b).

2.3 WEAR MODELS AND THEORIES FROM SIMULATIVE TESTS

A large number of simulative tests have been carried out over the years to study the erosive phenomena under a variety of conditions. These studies were typically application dependent and involved the study of erosive wear under conditions approximating those found in the erosive environment being studied. Depending on the cause of the erosive phenomena, these studies are classified as

- Liquid Particle Erosion studies
- Solid Particle Erosion studies

- Cavitation Erosion studies
- Erosion-Corrosion studies

As discussed in the previous section, the erosion in the die casting process may be considered to be caused by a combination of these factors; The eroding material in die casting is liquid metal (liquid particle erosion), which may have some solidified particles that contribute to the erosion (solid particle impact); Corrosion and high temperature attack of molten aluminum is another significant contributing factor to die erosion as is cavitation erosion, which is likely to occur because of the presence of cavitating gas bubbles in a high velocity molten stream.

Therefore some of the findings from these simulative tests that may be relevant to the problem of understanding die casting erosion are outlined in this section. Since die casting erosion is a likely combination of a number of these erosive effects the results from any of these studies which isolate particular erosive mechanisms (such as studies that isolate solid particle erosion effects) may not be directly applicable to die casting. They may however explain significant aspects of die casting erosion.

2.4 Studies of Erosion by Liquid Impact

2.4.1 Factors which affect Liquid Erosion

A drop of liquid impinging at high speed onto the surface of a solid can exert forces of a magnitude sufficient to permanently deform and fracture the solid. The type and extent of the impact damage depends primarily on the size, density, and velocity of the liquid drop and the strength of the solid. A diagrammatic representation of some of the mechanisms by which an impinging liquid drop can damage a solid as outlined by Brunton and Rochester (Brunton & Rochester, 1979) is shown in Figure 2.11.

Some of the damage mechanisms shown in Figure 2.11 are as follows (Brunton & Rochester, 1979)

- (1) An impinging drop shows the onset of outward radial flow across the surface. A very active component in causing erosion attack is the radial flow, the velocity of which is a function of the shape of the drop during impact, while the effectiveness is a function of surface profile of the solid at the time (Brunton & Rochester, 1979).
- (2) Surface loading showing normal and shear forces. The normal impact force appears to have a significant effect on erosion. Field et al. (Field, Camus, & Gorham, 1970) observed that, with structural metals for velocities about 600 m/s, the normal impact force caused large scale plastic flow resulting in the formation of a surface depression. They concluded that the diameter of the depression will correspond closely to the diameter of the mass of liquid first striking the solid. In the case of a jet (commonplace in die casting), this will correspond to the diameter of the head of the jet; for a spherical drop the diameter will be much smaller than the drop diameter.
- (3) Fracture in a weak brittle solid. The central area under compression may yield to produce a small depression, usually with evidence of subsurface shear flow. Circumferential fractures surround the central compressed region.
- (4) In ductile metals a smaller depression forms with erosion occurring at surface wavelets formed around the rim of the depression by the outward flow.
- (5) A hard brittle solid block protected by a soft rubbery coating. The central area of the coating is initially undamaged. Stress wave

damage occurs in the form of multiple spalling of the rear surface and the corners, and an outer ring of damage on the impact surface (Brunton & Rochester, 1979).

As can be seen there are various possible mechanisms of erosion due to liquid impact that can occur. Most of the research in liquid erosion has been conducted to address the problems of steam turbine blade erosion and rain erosion of aircraft. Excellent accounts of the early work on steam turbine blade erosion appear in reviews by Engel (Engel, 1957) and Heymann (Heymann, 1967). Rain erosion of aircraft is significant as it was found that for aircraft speeds above about 16 m/s, plastic, glass, ceramic and even metallic surfaces could be damaged by heavy rain (25 mm/h and above) (Brunton & Rochester, 1979). A review of work on rain erosion has been reported on by Engel (Engel, 1957), Fyall et al. (Fyall, King, & Strain, 1962) and Springer (Springer, 1976).

Figure 2.11 A diagrammatic representation of some of the mechanisms by which an impinging drop can damage a solid from Brunton and Rochester (Brunton & Rochester, 1979).

The experimental work in most of these studies of liquid erosion has involved simulative laboratory tests which attempt to replicate the actual erosive situation. Some of these tests are described below.

2.4.2 Experimental methods for Liquid Erosion Simulation

The simulative tests developed for liquid erosion studies can be classified as single impact tests or multiple impact tests. Both approaches are widely used depending on the application being modeled. The various variations of these techniques are described below.

Single impact methods: A number of single impact techniques have been developed under the assumption that, much can be learned about the erosion problem by examining the flow of liquid and the deformation of the solid during a single impact. Jenkins (Jenkins, 1955) studied drop impact damage by firing a specimen embedded in the nose of a projectile at a suspended or falling drop of liquid. The main difficulty with this technique relate to accelerating and decelerating the projectile without damaging the specimen, and to the degree of distortion of the stationary or falling drop by gravity and the air blast ahead of the projectile (Brunton & Rochester, 1979). Details of this method and its uses are given in papers by Jenkins and Booker (Jenkins & Booker, 1960), Fyall (Fyall, 1967), Rochester and Brunton (Rochester & Brunton, 1974) and Field et al. (Field, et al., 1970).

An alternative approach is to keep the solid specimen stationary and to fire a short jet or slug of liquid at it (Bowden & Brunton, 1958). The jet is produced by compressing a small volume of liquid using a piston, through a nozzle. It provides a simple and inexpensive method for producing liquid impact damage under controlled conditions and has been used in studies of the mechanisms of erosion (Brunton & Rochester, 1979). Various methods of jet formation, including the advantages and disadvantages of this method are described by Adler (Adler, 1979).

Multiple Impact Devices: Rotary impact devices provide the most commonly used method for achieving multiple drop impact erosion (Brunton & Rochester, 1979). One form of this device consists of a rotating disk from which specimens project radially at the rim. When the disk rotates the specimens impact against steady jets or sprays of drops directed across the path of the specimen in a direction parallel to the axis of rotation. Early devices of this kind have been described by Honneger (Honneger, 1924; Honneger, 1927), Gardner (Gardner, 1932), de Haller (de Haller, 1927) etc. More recent variations are similar, but are capable of much higher velocities in the range of 300 - 600 m/s. Field et al. (Field, Gorham, & Rickerby, 1979) used two methods for producing high-velocity liquid impacts. The first involved projecting a jet of liquid at a stationary target while in the second specimens were fired at suspended drops. An objective of this study was to place the jet method on a quantitative basis. Detailed studies of jets, using high speed photography, allowed the conditions for producing stable and reproducible jets to be obtained for a range of jet velocities upto 1000 m/s and jet diameters. The relation between jet impact and drop impact damage was achieved after experiments in which high-speed photography, pressure measuring techniques, and damage studies played important roles.

A drawback of the first method is that shooting a jet of liquid is contrary to the real case, but the method has distinct advantages in its ease of operation, low construction cost, and velocity range. The second method is nearer the practical situation, as a spherical water drop is struck, but has disadvantages as regards the size of specimen which can be projected and the deceleration (without further damage) of the specimen after impact. The suspension of spherical water particles of larger diameter is also a problem. Other multiple impact devices include rotary rain erosion equipment which is similar in principle to the wheel and jet devices described above, and linear rain erosion testing methods which include ballistic devices in which a specimen is launched from a gas gun through an atmosphere of drops (Mortensen, 1974). A review of other approaches

used to simulate physical conditions using multiple impact devices is provided by Brunton and Rochester (Brunton & Rochester, 1979).

From these studies various theories of the effect of impact variables on erosion have been advanced and these are summarized in the following section. It should however be noted that, such data only provides information on the behavior of selected materials under selected conditions, and do not describe material behavior beyond the range of the experiments in which they are obtained.

2.4.3 Impact variables and their effect on erosion

It has been known since the experiments of Honnegger (Honneger, 1927) that erosion does not proceed at a constant rate. The characteristic erosion-time curve based on weight loss measurements is shown in Figure 2.12a and as a rate curve in Figure 2.12b. There is an initial period, the incubation period or Stage I, during which there is no detectable weight loss, although there will usually be some plastic or brittle deformation of the surface. This is followed by Stage II, a period during which material is removed at maximum rate as the pits formed by material removal link up and cover the impact area (referred to as the steady rate erosion stage). After a time, the erosion rate begins to decrease, tending to a lower and approximately constant rate, Stage III. This is often referred to as the final erosion region. Experimental evidence (Springer, 1976) indicates that this pattern holds under a wide range of conditions. Heymann (Heymann, 1967b) and Brunton (Brunton, 1967) have discussed physical and geometrical surface changes that may account for this pattern, while Springer (Springer, 1976) has provided a wear model based on a simplified form of this rate curve pattern.

Figure 2.12 Typical weight loss / time curve for a ductile metal divided into three stages (a) and the corresponding rate curve of erosion (b). From Brunton and Rochester (Brunton & Rochester, 1979).

Which stage of erosion is the most critical for study of the effect of impact variables on erosion? On the one hand it has been argued that components spend most of their lives in Stage II, and that the rate of erosion during this period should therefore be used as a measure of erosion damage and on the other, that the damage at this stage is so serious that components will no longer be serviceable so that the rate of erosion during Stage II, even the duration of Stage I (this is typical for rain erosion of aircraft windows which lose their transparency and consequently their efficacy in Stage I) would be more appropriate measures of damage (Brunton & Rochester, 1979). The answer to that question obviously lies in the acceptable erosive wear for the object subjected to erosion and no universal rule can be ascertained.

Another significant question regarding the study of the effect of impact variables on erosion, is " what is the best measurement variable that can be used to quantify erosive wear". While mass loss is the usual experimental measure of erosion damage, Heymann (Heymann, 1967) suggested a better measure is volume loss per unit area of surface affected. This quantity is variously called the "erosion depth", the "mean depth of erosion", and the "mean depth of penetration." Heymann has also suggested that the volume of fluid impinged per unit area is a more appropriate measure of exposure than either time or number of impacts. This quantity has been called the "mean height of impingement". These parameters are used in order that valid comparisons can be made between erosion results obtained using different systems.

For example, damage during the incubation period poses a problem for aircraft windows which may lose their transparency long before any measurable loss of mass occurs. Under these conditions the damage is represented more appropriately by the loss of optical transmission than by the mass loss.

Accounting for these differences in stages of erosion and measures of erosion, a brief summary of the effect of process variables on erosion is presented below.

2.4.3.1 Effect of Impact Velocity

There have been many attempts to discover a general relationship between impact velocity and erosion damage which would be valid for a wide range of materials. At the moment there is no such relationship which can be safely extrapolated outside the velocity range of the tests, or which can be applied to materials other than those to which the measurements refer (Brunton & Rochester, 1979).

The erosion rate (E) during the second stage of the erosion-time curve may however be expressed either as

$$E \propto (V - V_c)^{n1} \quad 2.1$$

Where V is the velocity with which the liquid impacts the surface, V_c is the threshold velocity below which no mass loss occurs and $n1$ is about 2.5 (Brunton & Rochester, 1979), or

$$E \propto V^{n2} \quad 2.2$$

where $n2$ is about 5 (Hoff, Langbein, & Rieger, 1967). Honneger (Honneger, 1927) obtained a value of 2 for $n1$ with a constant $V_c = 125$ m/s for the impact velocity below which mass loss is zero. A similar expression was obtained by de Haller (de Haller, 1927), but here erosion was detected down to a value of 90 m/s, the lower threshold being associated with a larger jet. Baker et al., (Baker, Elliot, Jones, & Pearson, 1967) for a quenched and tempered 12 % chromium steel obtained a value of $n1$ of 2.6, and V_c is 120 m/s. Fyall et al. (Fyall, et al., 1962), used whirling arm machines and rocket sleds to simulate the high velocity conditions of rain erosion and obtained a value of $n1$ of 2.37, and a V_c of 208 m/s. For most materials the erosion rate will usually fit these equations over some part of the velocity range. In general equation 1 is believed to hold for velocities well above the threshold, while equation 2 holds for results near the threshold velocity (Brunton & Rochester, 1979).

No successful theory of the velocity relationship exists. As with failure by mechanical wear, the complexity of the erosion process appears to rule out all but the most general of explanations (Brunton & Rochester, 1979).

2.4.3.2 Effect of Impact angle

The effect of impact angle has also been widely investigated. The general conclusion is that, unlike solid particle erosion (section 2.5), the normal component of impact velocity is mainly responsible for the damage, the tangential component in most circumstances having little effect (Brunton & Rochester, 1979).

Figure 2.13 Effect of impact angle in liquid drop erosion.

Fyall et al. (Fyall, King, & Strain, 1962) found that the erosion rate during the early stages of erosion was given by the expression

$$E \propto (V \cos \theta - V_c)^n \quad 2.3a$$

where θ is the angle between the normal to the surface and the direction of the impact velocity. They observed that the angle of impact had little effect on erosion in the later stages, and they surmised that this happened because the damage was then caused by outward flow under the drops striking the rims of pits in the surface. These results were confirmed by Busch et al. (Busch, Hoff, & Langbein, 1966) for angles of incidence upto 60° .

Baker et al. (Baker, et al., 1967) on the other hand, in their test on a chrome steel proposed the equation

$$E \propto (V \cos \theta - V_c)^n \sec \theta \quad 2.3b$$

where $\sec \theta$ was introduced to account for the effect of tangential velocity components on erosion. The tangential component was also found to make a significant contribution to the erosion of low shear strength materials such as soft polymers ((Rieger, 1970)). Maximum damage occurred for θ between 15 and 25. It seems too that tangential velocities are important at high velocities; Schmitt et al. (Schmitt, Reinecke, & Waldman, 1974) found that at impact velocities in the region of 1700 m/s at shallow angles ($\theta=60^\circ$), the tangential component was as effective as the normal component in causing damage to hard polymers and ceramics.

Brunton and Rochester (Brunton & Rochester, 1979) suggest that in general, for smooth hard surfaces, the normal component of velocity determines the damage potential of the drop. For roughened surfaces or surfaces which deform substantially during an impact, the tangential

component will contribute significantly and the dependence on θ will be more complicated.

2.4.3.3 Effect of Drop size and Drop shape

Erosion studies suggest that it is not the average drop size which is important, but rather that the larger drops control the degree of damage (Brunton & Rochester, 1979). Brandenburger and de Haller (Brandenburger & de Haller, 1944) carried out systematic tests on the drop size effect. The results appeared to show a strong increase in erosion with diameter. A most common explanation of the above is that the duration of the peak impact pressure will decrease with drop size because of the proximity of the free surface of the drop to the center of impact. The cushioning effect of the wetted surface will also be more pronounced with small drops. The pressure wave from a small drop striking the liquid layer will also likely be less intense at the solid surface than the wave from a large drop striking at the same velocity. However when Heymann (Heymann, 1967b) analyzed the results in a rationalized form, only a weak dependence on drop size was found. Hancox and Brunton (Hancox & Brunton, 1966) also found no difference in the normalized erosion rate for a range of jet diameters.

The threshold velocity V_c below which no damage occurs has also been found to depend on drop size. Heymann (Heymann, 1967) has fitted the following relation between V_c and d

$$V_c d^2 = \text{constant} \quad 2.4$$

where d is the diameter of the drop.

The effect of drop shape was first studied by de Haller (de Haller, 1927). He showed that a flattened jet produced more rapid erosion when struck on its least curved surface than on that of greatest curvature. This is as the

duration of the maximum impact pressure and the area over which it acts depends on the curvature of the drop surface.

2.4.3.4 Effect of Surface shape and Roughness

It was observed by de Haller (de Haller, 1927) that the erosion rate increased when the impact surface was made concave. Vater (Vater, 1944) observed that plane surfaces were more rapidly eroded than convex ones, and that natural defects in the surface were more affected than artificial ones. The duration of the maximum impact pressure and the area over which it acts increases with the concavity of the solid surface.

Hancox and Brunton (Hancox & Brunton, 1966) examined the problem of the effect of surface roughness on erosion. They used 18/8 stainless steel, 1.3 mm diameter jets and an impact velocity of 90 m/s. Specimens were prepared with scratch finishes varying from 37 down to 1 micron. For the different finishes, the number of impacts required to produce pits and remove metal from the surface was measured. It was found that erosion was reduced to about four times as the surface roughness decreased from 12 to 1 micron, but that surface finish had no effect on erosion when the surface roughness was greater than 12 microns.

The results and discussion of these geometrical effects suggest that a slightly roughened surface will be rapidly eroded, but that as the roughness increases the surface will be gradually less susceptible to further damage. Brunton and Rochester (Brunton & Rochester, 1979) suggest that this is consistent with the typical erosion rate versus time curves which have been described previously.

2.4.3.5 Erosion of wetted surfaces

Liquid will be trapped in the bottom of pits formed on eroded surfaces, particularly in the bottom of larger pits which appear toward the end of stage II. Brunton (Brunton, 1967) stated two reasons why the presence of a

liquid film might reduce erosion damage: (i) when it is struck by the drop the liquid film deforms, so that the pressure on the solid is less than it would have been if the surface had been dry. However, if the layer is very thin, multiple reflections of the initial compression wave at the solid surface and at the underside of the drop would quickly bring the pressure up to the full value for dry impact, and : (ii) the impact pressure is reduced because the pressure wave diverges as it passes through the liquid layer. If the layer is thick compared with the diameter of the drop, attenuation will vary as the square of the layer thickness.

Some experimental evidence was presented in support of these suggestions (Brunton & Rochester, 1979). When a high velocity water jet was fired against the surface of a block which was covered with a water layer, it was observed that the pit formed in the surface of the impact (i) decreased in depth, (ii) increased in diameter, and (iii) changed from a round to a conical depression, as the water layer increased in thickness.

2.4.3.6 Effect of the mechanical properties of the target material

Many attempts have been made to relate erosion resistance to a single mechanical property of the material. Honnegar (Honneger, 1927), Gardner (Gardner, 1932), and Baker et al. (Baker, Elliot, Jones, & Pearson, 1967) among others found some correlation between erosion resistance and hardness. Heymann (Heymann, 1967b) found that erosion resistance varied, on the average, with about $8/3$ the power of hardness, using data from several different sources. Baker et al. (Baker, et al., 1967) who reported a wide ranging, yet detailed study of the erosion characteristics (measured in terms of weight loss) of several possible turbine blade materials, suggest that for a given class of alloy, such as steels, hardness provides the best correlation, but point out that there are many exceptions to this trend. They also report that no general relationship between erosion rate and mechanical properties is apparent from these weight loss studies. Fracture toughness, which might be expected to give some indication of erosion resistance, showed no correlation. Spiedel and

Kesser (Spiedel & Kesser, 1974) who also reported this result, suggested that since erosion cracking does not take place under essentially elastic conditions, as in a fracture toughness test, the lack of correlation is to be expected. They proposed that flow stress rather than modulus controls events in the impact area.

Thiruvengadam (Thiruvengadam, 1963) suggested that strain energy to fracture in a tensile test may be a good measure of the erosion resistance of a material. Thiruvengadam and Waring (Thiruvengadam & Waring, 1967) plotted cavitation damage against strain energy to fracture for a number of materials, and found that the points were scattered in a straight line, " the damage decreasing as the strain energy to fracture increased". Thomas and Brunton (Thomas & Brunton, 1970) have also reported that for impingement erosion there is a good correlation between erosion resistance and strain energy to fracture for a number of metals.

Brunton and Rochester in their extensive survey (Brunton & Rochester, 1979) however conclude that neither hardness nor strain energy to fracture give a satisfactory measure of erosion resistance for the whole range of engineering materials, or even for ductile metals. Of the two properties they suggest that, hardness appears to provide a better measure of erosion resistance than does strain energy to fracture, although both fail for some alloys.

2.4.3.7 The effect of liquid properties on erosion

Hancox and Brunton (Hancox & Brunton, 1966) showed that carbon tetrachloride erodes metal at about twice the rate of water. They suggested that this was primarily due to the greater energy density in the outflowing liquid. This would mean more damage to the surface of the solid due to the loading that occurs at the beginning of the impact and due to the shear loads that are generated when the outflowing liquid strikes against projections on the surface. Once pits had developed in the surface, they found that erosion decreased with temperature. This was attributed to an

increase in the viscosity of the liquid at lower temperatures; as viscosity increases, the velocity of the outward flowing liquid increases, and more shear damage is done to the surface of the solid.

Canavelis (Canevalis, 1968) also studied the effect of viscosity on erosion. He found that the duration of the first stage of erosion does not vary as the viscosity of the eroding liquid is changed, but that the duration of second stage increases as the viscosity is increased.

Rochester and Brunton (Rochester & Brunton, 1974) separately investigated the effects of liquid density, acoustic impedance, and viscosity on erosion of nickel using a wheel and jet test. Over all liquids, density had the greater effect on Stages II and III but did not correlate well with parameters based on Stage I. Increasing acoustic impedance reduced the incubation period and increased the rates of erosion in Stages II and III. Increasing viscosity had the opposite effect. The decrease in erosion with increasing viscosity was associated with the increase in protection provided by the thicker film of liquid adhering to the surface.

2.4.4 Mechanism of Liquid Erosion: Models and Theories

In addition to the number of relationships between erosive wear rates and process parameters a number of predictive models and theories of liquid erosion have been obtained. A summary of these theories and models is presented in this section.

A complete theory of erosion should be capable of predicting, for specified conditions of impact, the erosion curve in terms of the impact variables and the material properties. A more limited objective might be to predict certain characteristics features of the curve, such as incubation period or maximum rate of erosion, in terms of erosion data gathered from tests performed under different but not totally unrelated conditions. Any theory of erosion should, therefore, clearly specify the bounds within which it is applicable (Brunton & Rochester, 1979).

Heymann, in a series of reports (Heymann, 1967), (Heymann, 1967b), has discussed at length the various erosion theories. Heymann (Heymann, 1967b) adopted a statistical approach in which erosion rate depended on the lifetime of a layer of surface cells. Engel (Engel, 1970) followed a similar line. In her theory the specimen was divided into cells, and the lifetime of surface cells and of subsurface cells was calculated. The cells were assumed to have the same average volume, which was identified with the average volume of an eroded fragment. Experimental values for the size and shape of the fragments were used. Adler (Adler, 1979) however suggests that the conceptual model while involved, was a relatively naive approximation to the physical material removal process for brittle materials, and also not complete.

In a later theory, Heymann (Heymann, 1970) used two parameters: R_e , the rationalized erosion rate defined as the ratio of volume lost per second to volume of liquid impinging per second, and N_e , the normalized erosion resistance, defined as the measured erosion rate of a reference material divided by the measured erosion rate of the actual material under the same conditions. The product ($R_e N_e$) plotted logarithmically against the normal component of the impact velocity V gave a straight line of slope 5 for water drops and 4.4 for cylindrical water jets struck from the side (Figure 2.14).

$$N_e R_e = C_1 V^5$$

2.5

Figure 2.14 A plot of $N_e R_e$ against impact velocity for droplets and jets as plotted by Heymann (Heymann, 1970).

Thiruvengadam (Thiruvengadam, 1967a; Thiruvengadam, 1967b) developed a general theory covering erosion by bubble collapse and by drop impingement. The argument supposes that the bubble collapse energy or drop impact energy is absorbed by the material as strain energy until at a critical strain energy, which Thiruvengadam calls the "erosion strength", failure occurs. Thus the theory is based on the concept of this erosion strength S_e , which is defined as the energy absorbing capacity of the target material per unit volume, under the action of the erosive forces. Thiruvengadam thus considered the general erosion process to be controlled by two opposing phenomena: the time dependent efficiency of absorption of the impact energy by the target material, and the attenuation of the impact pressure due to changing surface topography as the target material erodes. The theory has been criticized on the grounds that it oversimplifies the collapse mode for bubbles and ignores changes in localized flow caused by changes in the surface profile (Hammit, Timm,

Hwang, & Huang, 1974), (Brunton & Rochester, 1979). Adler et al. (Adler, 1979) also suggest that since it is unlikely in practice, that an intervening liquid layer has any effect on the attenuation of the imposed impact pressures, Thiruvengadam's theory of erosion is not generally applicable to erosion by liquid drop impingement.

An analytical theory on the cause of erosion due to the impact of liquid droplets on a solid surface was provided by Springer (Springer, 1976) and by Springer and Baxi (Springer & Baxi, 1974). When a liquid droplet impinges upon a solid surface, the droplet comes to a complete stop (Figure 2.15). This sudden deceleration causes a pressure build-up at the liquid solid interface similar to the pressure rise observed in a pipe line upon the quick closing of a valve. The high pressure inside the droplet then causes the liquid to escape laterally.

Figure 2.15 Impact of a liquid drop on an elastic surface. From Springer (Springer, 1976)

The pressure acting across the liquid-solid contact area results in a force normal to the solid surface. The force generates stress waves traveling along the surface and inside the material. The changes in the stresses across these waves and the corresponding changes in the normal and shear forces are responsible for some of the material damage, according to Springer (Springer, 1976). The precise mode in which the material fails, i.e., the relationship between the stresses and the resultant damage is not fully understood. The magnitude of stresses in the material depend to some extent however, on the normal forces acting at the surface.

The jets of liquid squirting out laterally from the droplet further contribute to material damage. The jets interact with minute, existing surface flaws and may tear off bits of the surface or may widen cracks, as shown in Figure 2.16 (Springer, 1976). The outflow may also impose high shear stress along the surface thereby creating further damage. Thus Springer assumed that the damage was produced both by the effects of the high pressure at the liquid-solid interface and by the lateral outflow of the liquid.

Figure 2.16 Interaction between lateral jets and surface flaws. From Springer (Springer, 1976)

It is thus evident that the forces and stresses generated upon impact play a very important role in erosion processes. For a complete understanding of these processes, a detailed knowledge of the flow patterns and the stresses at the interface of a liquid droplet and a deformable surface was required. Due to the difficulty in calculations that would lead to a complete solution of the flow and stresses, Springer (Springer, 1976) and Springer and Baxi (Springer & Baxi, 1974), resorted to a simplified approach to represent the phenomena at the interface. Assuming that the effects of lateral outflow were negligible, that the pressure was uniform and constant over the entire contact area and that the problem was one-dimensional, Springer came up with the following relationship between the relative velocity and stress differential between the solid and liquid droplet:

$$\Delta\sigma^0 = \rho_L c_L V / (1 + \rho_L C_L / \rho_s c_s) \quad 2.6$$

where $\Delta\sigma^0$ is the stress differential between the wave fronts spreading in the solid and liquid (in other words the stress or pressure on the surface), V is the relative velocity prior to impact, ρ is the density, c is the velocity of sound in the undisturbed media and the subscripts s and L indicate solid and liquid respectively. When the liquid droplets arrive at a surface at an angle other than the normal, the above equation is modified by replacing V with the component of V normal to the surface ($V\cos\theta$). This was assuming that the normal component of velocity was responsible for erosion. Some evidence (Reinecke & Waldman, 1970) however suggests that at very high impact velocities (around 1500 m/s, which is above the range of velocities seen in die casting) the tangential component of velocity may also be responsible for damage.

The force imparted to a surface by a drop of liquid can thus be obtained by the product of the pressure determined above and the surface area of contact. These studies were extended by Springer (Springer, 1976) toward an understanding of the mechanism of erosive wear in coated materials as well.

Springer and Baxi (Springer & Baxi, 1974) and Springer then came up with a time dependent erosion model based on a cumulative fatigue damage mechanism. The model is based on the assumption that the incubation, acceleration, and maximum rate periods of the characteristic rain erosion curve can be represented by a linear relationship shown in Figure 2.17. The consequent erosion rate period (Stage II) can be assumed of the form,

$$M^* = a^* (N^* - N_1^*) \quad 2.7$$

where M^* is the dimensionless mass loss, a^* the dimensionless rate of mass loss, N^* the dimensionless number of impacts per site, and N_1^* the dimensionless number of impacts corresponding to the incubation period. A derivation based on fatigue concepts for ductile metals provides an expression for the number of impacts in the incubation period

$$N_1^* = a_1 (S/P) \quad 2.8a$$

where the relevant strength parameter S for homogeneous materials has the form

$$S = 4\sigma_u(b-1)/(1-2\nu) \text{ Nm}^{-2} \quad 2.8b$$

where σ_u is the ultimate tensile strength, b a numerical constant related to the slope of the Goodman line and ν the Poisson's ratio. The applied stress parameter P in equation 2.7 is simply the water-hammer pressure corrected for the normal component of the velocity V_0 shown in equation 2.6

$$P = \rho_l C_l V_0 / (1 + \rho_l C_l / \rho_s C_s) \text{ N m}^{-2} \quad 2.9$$

The ratio S/P thus reduces to the ratio of the ultimate tensile strength to a static radial tensile stress component. This is a brittle failure criterion. The only material constants required in the fundamental correlation are

the ultimate tensile strength and Poisson's ratio. Springer (Springer, 1976) claimed that the predictive value of these equations within the bounds of their applicability was high.

Figure 2.17 Idealized form of the general erosion curve for the incubation, acceleration and maximum rate periods shown in Figure 2.12, assumed by Springer. From Springer (Springer, 1976).

Adler (Adler, 1979) however suggests that the analytical model for specifying the mass loss degenerates to two series of curve fitting routines for an experimental data base which has a high degree of arbitrariness. He also claims that there is a discrepancy of an order of magnitude in the erosion rates and that the physical aspects of the erosion process are lacking with respect to all of the conditions considered by Springer (Springer, 1976).

Other theories of erosion include the energy based theories of Hoff et al. (Hoff, et al., 1967), and Hammitt et al. (Hammitt, Huang, King, Mitchell, &

Solomon, 1970)). Reinecke (Reinecke & Waldman, 1970) proposed a theory for the erosion of graphite based on the assumption that mass loss is controlled primarily by shear stress due to the impact and the shear strength for the graphite. Adler (Adler, 1976) developed a general mathematical form of erosion due to a process of erosion pit nucleation and growth patterns on parts after the statistical model of Heymann (Heymann, 1970). The implementation of a model of this general nature is difficult even when the properties of the target material and the impact conditions are comparatively simple as for the case of glass beads impacting glass targets (Adler, 1976).

Most of the models described above use the uniform water hammer pressure of the impacting liquid as a measure of erosive force or energy. This is however not a physically accurate description of the pressure distribution. Analytical derivations of the pressure distribution under an impinging drop have been attempted with some success. The best "working estimate" however remains the water hammer pressure. Quantitative theories of erosion described above (especially that of Springer (Springer, 1976) thus face the problem that, not only is the surface loading in drop impact not properly understood, but the stresses and the reaction of the material to these stresses are unknown.

2.5 SOLID PARTICLE EROSION

There has been a strong body of work which deals with erosive wear by the impacting action of solids. Reviews of the state of information on solid particle erosion are available in several sources; Engel, (Engel, 1976); Preece and Macmillan, (Preece & Macmillan, 1977); Hutchings, (Hutchings, 1979); Finnie and MacFadden, (Finnie & McFadden, 1978); Ruff and Wiederhorn (Ruff & Wiederhorn, 1979); Shewmon and Sunderrajan (Shewmon & Sunderrajan, 1983); and Gahr (Gahr, 1987). The state of earlier understanding has been adequately reviewed in several other papers (Finnie, (Finnie, 1960),; Finnie et al. (Finnie, Wolak, & Kabil, 1967); Bitter,

(Bitter, 1963), Neilson and Gilchrist, (Neilson & Gilchrist, 1968); and Sheldon and Finnie, (Sheldon & Finnie, 1966).

Some main processes leading to wear by single or multiple impact of particles are summarized in Figure 2.18 (Gahr, 1987). Angular particles for example, can remove material by microploughing and/or microcutting when they strike the target surface at small angles, say less than about 40° particularly. Temperature effects can be superimposed, due to high impact energies and friction forces induced by adhesion between the particles and the target or surface roughness. Various experimental techniques have been used over the years to evaluate such effects and they are summarized below.

Figure 2.18 Particles resulting in wear loss due to single or multiple impact of particles as shown by Gahr (Gahr, 1987).

2.5.1 Experimental techniques to evaluate solid particle wear

The experimental techniques that are used to evaluate solid particle wear may be classified as single particle studies and multiple particle studies.

Single Particle Erosion Methods: Erosion involves many competing processes. One way to model important parts of erosion is to consider the high velocity impact of single particles on a planar target. Such experiments have made important contributions to modeling erosion in that they successfully reproduce the high strain rate, localization of strain, and modes of metal loss found in erosion (Shewmon & Sunderrajan, 1983). They do not model the effect of the severe prior deformation found in the eroding surface, but the regularity of the initial surface and the simple geometry of the incident particle make interpretation much less ambiguous than engineering erosion experiments, where irregular particles impact randomly at an irregular surface (Shewmon & Sunderrajan, 1983).

Various experimental techniques have been used to cause single particles to impact on specimen surfaces under controlled conditions. Electrostatic acceleration methods were employed by Shelton et al. (1960) and Hutchings and Winter (Hutchings & Winter, 1975) in studies of small particle erosion effects. Explosive acceleration methods have also been reported (Hutchings & Winter, 1975), although most studies have used some type of gas projectile gun (Ruff & Wiederhorn, 1979). A difference in the characteristics of craters formed on uneroded and on previously eroded surfaces has however been found during simple impact studies (Ruff & Wiederhorn, 1979). Sheldon and Kanhere (Sheldon & Kanhere, 1972) also found a small difference in the velocity dependence of erosion between uneroded and previously eroded surfaces that is probably significant. Thus, single particle experiments alone are not sufficient and multiple particle experiments are often required.

Multiple Particle Erosion Methods: There are a number of possible designs used to obtain multiple particle erosion. In one design the specimen is moved at a controllable velocity, usually on a rotating arm fixture, through a slowly moving erosive stream. The exposure may be intermittent but can continue for a long time period. The relative particle-specimen velocity can be accurately determined since the principal velocity component is that of the specimen in the laboratory system (Ruff & Wiederhorn, 1979). The alternative design involves propelling a stream of particles at a fixed specimen. The specimen may be completely immersed in the stream, or perhaps only a portion of the specimen is exposed (Ruff & Wiederhorn, 1979). One such design allows the erosive particles to be spun from a rotating disk to strike a fixed specimen (Ruff & Wiederhorn, 1979). In this scheme some method of measuring or calculating particle velocity must be used.

Using experimental approaches similar to those described above, the effects of various process parameters on solid particle erosion was evaluated by various researchers and some of the relevant findings are outlined below.

2.5.2 Effect of Process Parameters on Solid Particle Erosion

The erosion rate is usually defined as the volume or mass loss of a material divided by the mass of the impacting particles or the exposure time. The general question of material parameters, of both eroded and eroding materials, and their effect on erosion rate is of considerable interest in this research. Indentation hardness of the eroded material is the most commonly used and yet apparently insufficient parameter. This is because other process parameters such as particle velocity, angle of attack, particle size, particle frangibility, target material properties and microstructure all appear to have a significant impact on erosion by solid particles. The additional complexities of environmental reactions and elevated temperature exposures, where surface film formation is a significant competitive process are also of great importance. The influence

of the various process parameters that have a significant impact on solid particle erosion are therefore described in the following section.

2.5.2.1 Dependence on Erosion rate on time

A systematic study of the effect of time of erosion has received rather limited attention. The data available in the literature shows three types of behavior, a constant erosion rate (Finnie, 1960), an increasing erosion rate with time (Finnie, 1960) and a decreasing erosion rate with time (Adler, 1976). These main types of erosion rate/exposure time curves have been summarized in Figure 2.19 (Gahr, 1987), (Rao & Buckley, 1983). According to type I, an incubation period is followed by an acceleration period which goes over to a steady state period (curve 1) or a deceleration period (curve 3). The type 2 (curve 2) starts with a negative erosion rate, due to a mass gain by embedding particles. Such an incubation period (with mass gain) is found mainly on soft and ductile materials. Embedding of particles also occurs particularly at greater angles of impingement (Neilson & Gilchrist, 1968), (Brown, Jun, & Edington, 1981).

An explanation for the existence of an incubation period (with no mass loss or mass gain) is provided by Shewmon (Shewmon & Sunderrajan, 1983). The erosion process involves particles harder than the target, which deforms plastically under the impact of high velocities. This deformation work hardens the surface up to a level where that of the next impact exceeds the strain required for fracture or chip formation. Thus Shewmon suggests that it should not be surprising that there exists an incubation period of many impacts before metal is lost from a fresh surface, and that this period gets shorter at higher velocities of impact, and with harder less ductile materials that can tolerate strain before fracture (Shewmon & Sunderrajan, 1983). At velocities below roughly 10 to 20 m/s, erosion thus stops or becomes insignificant. Shewmon also reported that at low impact velocity V , an incubation time is observed that increases roughly as V^{-1} .

Figure 2.19 Schematic representation of erosion rate as a function of exposure time as suggested by Gahr (Gahr, 1987).

The time dependence of erosion was also found to obey a power law relationship of the form (Sargent, Mehrotra, & Conrad, 1979), for ductile materials

$$E \propto t^c \quad 2.10$$

where c is a material constant. The time exponent c was found to be -0.1 under all conditions of velocity, particle size and angle of impingement. The decrease in erosion with increase in time was interpreted by Sargent et al. (Sargent, et al., 1979) in terms of a change of surface roughness.

2.5.2.2 Effect of Metal Velocity

In almost all the data previously reported (Finnie, 1960), (Finnie, Wolak, & Kabil, 1967), (Sheldon & Finnie, 1966), (Sheldon & Kanhere, 1972), variation of erosion as a function of particle velocity has been expressed in the following form

$$E \propto V^n \quad 2.11$$

Where E is erosion in terms of material per unit weight of particles impinged and V is the particle velocity. The exponent n is a constant between the given target and particle material and is usually in the range of 2.2 to 3.0 for ductile metals (Gahr, 1987), (Finnie & McFadden, 1978). This expression also appears to hold true for multi-particle erosion tests. For example, Sargent et al. (Sargent, et al., 1979), who performed multi-particle erosion tests by using alumina powder on Pyrex glass reported a similar expression for the maximum erosion observed at a normal angle of impact. In their tests n was found to be between 2.2 and 2.7 and was independent of the time of erosion. The velocity exponent has been reported not to depend on the type of material (Ruff & Wiederhorn, 1979) (Figure 2.20), although substantially greater velocity exponents upto 6 were reported in studies on brittle materials such as ceramics (Ruff & Wiederhorn, 1979). Gahr (Gahr, 1987) also reported that the erosion rate increases more strongly with increasing velocity on brittle materials than on ductile materials. Detailed studies have also shown some dependence of the velocity exponent on attack angle (Ruff & Wiederhorn, 1979) and a dependence on the temperature of erosion (Ruff & Wiederhorn, 1979) (Figure 2.21).

Figure 2.20 Influence of velocity on erosion for different materials as reported by Goodwin et al. (Goodwin et al., 1969) , (Ruff & Wiederhorn, 1979).

Figure 2.21 Velocity dependence of erosion on 310 stainless steel, 100 mesh SiC, 90 degree angle of attack at 975 C (Ruff & Wiederhorn, 1979).

2.5.2.3 Effect of Angle of Attack

The variation of erosion, usually measured in mass or volume loss from the specimen per unit mass of impacting abrasive, as a function of angle of attack of the particles is shown schematically in Figure 2.22 (Ruff & Wiederhorn, 1979) for brittle and ductile particles. Erosion thus depends markedly on the angle of impingement, with the dependence being quite different for ductile metals and brittle solids (Finnie, Levy, & McFadden, 1979). Many other workers have reported this general behavior. For ductile materials the peak erosion loss occurs at around 20° (Ruff & Wiederhorn, 1979). For brittle materials such as ceramics or hardened steels, erosion rates increase with increasing impact angles with the peak erosion loss occurring at around 90° (normal incidence) (Ruff & Wiederhorn, 1979), (Gahr, 1987). The dependence of erosion rate on impact angle is also influenced by the size of the impinging particles (Uetz & Fohl, 1978), (Sheldon & Finnie, 1966), the impact velocity (Uetz & Fohl, 1978), (Conrad, Shin, & Sargent, 1983), (Ruff & Wiederhorn, 1979) and the target material (Uetz & Fohl, 1978). Erosion rates decrease continuously with increasing angles of impact for materials of very high elasticity such as rubber (Gahr, 1987). A more detailed explanation of the reason for the angle of attack effects is provided in the discussion on theories of erosion.

Figure 2.22 Schematic representation of erosion rate on attack angle (Ruff & Wiederhorn, 1979).

2.5.2.4 Effect of Particle Characteristics

The particle characteristics that seem to be more important in the erosion process are as follows (Ruff & Wiederhorn, 1979):

Particle shape: The characteristic shape of the impacting particle is a factor in the erosion process. In studying erosion processes, it is important to know whether the impacting particles have sharp cutting edges or a more rounded surface. For both ductile and brittle materials, more effective erosion is generally found to be associated with angular particles (Finnie, 1960). Since angular particles either displace more material into the crater lip where it becomes vulnerable to further erosion or actually detach material from the surface (depending on particle rake angle at contact), the erosion efficiency of angular particles relative to round particles can be explained (Hutchings, 1979). Interestingly enough, many of the erosion

effects, except for the actual magnitude of material eroded, were reported quite similar for angular and spherical particles (Sheldon & Kanhere, 1972).

Particle size: The dependence of erosion on particle size has also been studied by several workers. In a detailed study, Goodwin et al. (Goodwin, Sage, & Tilly, 1969) examined the erosion of steel by quartz particles over a large size range. An increase in erosion with increasing particle size was found up to a limiting size, above which the erosion remained constant. This result thus appears to be a general one for ductile materials "For ductile materials, relative erosion is essentially independent of particle size for sizes greater than a critical value" (Ruff & Wiederhorn, 1979). Smaller sizes are less effective and a lower threshold size (~ 5 mm) for any erosion has been suggested. The effect of particle size as measured using flat test specimens may not however be applicable in all geometry's. Mills and Mason (Mills & Mason, 1977) in studying the erosion of 90 tube bends due to the conveying of abrasive sands, reported that the depth of penetration for 70 μm particles was much greater than that of 230 μm particles. For brittle materials, a strong dependence of erosion rate and strength degradation on particle size is predicted and some confirmation of these predictions has been obtained (Ruff & Wiederhorn, 1979), (Gahr, 1987). Thus erosion rates are substantially more sensitive to particle size on brittle than on ductile materials (Gahr, 1987). A possible explanation of this phenomenon is that increasing particle size, implies enhanced impact energy (Gahr, 1987). While there is contrasting information on the magnitude and applicability of the size effects, Sheldon and Finnie (Sheldon & Finnie, 1966) report that in general the erosion E , varies with the size of the impinging particle according to the following power relationship

$$E \propto D^m$$

2.12

where D is the diameter of the particle and m is a constant for a given target and particle material. As discussed above, this relationship may not be valid for all situations.

Particle hardness: For ductile solids, as long as the target surface hardness is much less than the particle hardness, little effect is expected except indirectly through particle fragmentation or other changes. However experimental data by Gulden (Gulden, 1979) suggests that particle hardness may be an important variable that must be considered when characterizing the wear of brittle materials.

Particle frangibility: For ductile materials, the effect of particle fragmentation to provide additional erosion loss has been described by Tilly (Tilly, 1973) and by Maji and Sheldon (Maji & Sheldon, 1979), while Uemois and Kleis ((Uemois & Kleis, 1975)) surmised that increased erosion potential of abrasives due to water or impurity content may derive from an altered tendency to fragment. Particle frangibility is however not as significant a factor in erosion of brittle materials, however (Ruff & Wiederhorn, 1979). Maji and Sheldon (Maji & Sheldon, 1979) for example, examined the erosion of aluminum alloy tubes and flat plates using 270 μm shot and found considerably more erosion for brittle (and consequently more frangible) shot, than for ductile shot.

Other Particle Parameters: The influence of other particle parameters such as particle concentration in the erosive stream and particle strength, have only been examined in a few instances. In the case of particle concentration, divergent findings have been reported as summarized by Uemois and Kleis (Uemois & Kleis, 1975). It would seem that relative erosion (specimen mass loss divided by impacting erosive mass) should be independent of particle concentration so long as each particle event is equally effective. At higher concentrations, where particle interference (shielding) effects occur at the specimen surface, the relative erosion should be expected to decrease, and this has been reported (Uemois & Kleis, 1975). The influence of this parameter appears to be important in

die casting but is difficult to evaluate, as is evident in the very few studies conducted in this area.

2.5.2.5 Eroded Material Characteristics: Effect of Target material properties on solid particle erosion

The properties of the eroded or target material obviously have a significant impact on erosion. A brief summary of the work in evaluating such effects is described below.

Effect of Surface Hardness, Work Hardening and Ductility: The single most important parameter most often chosen to describe ductile material erosion rate variation (Ruff & Wiederhorn, 1979) is the surface hardness. Numerous correlation studies have been attempted to determine the relation of hardness to erosion rate. For example Finnie et al. (Finnie, et al., 1967) studied the influence of material hardness on resistance to erosion by using 250 μm SiC particles at an impinging angle of 20 and some results are shown in Figure 2.23. They measured a linear increase in erosion resistance with increasing Vickers hardness of the annealed pure metals used as targets. This result is valid for small angles of incidence. However when these authors studied the effect of hardness variation for one alloy (varied through cold working of tool steel) they found little variation in erosion rates. Uetz and Fohl (Uetz & Fohl, 1978) have reported similar results on steels. Thus within one alloy system, indentation hardness appears not to be a valid parameter to describe erosion differences (Ruff & Wiederhorn, 1979). However Sheldon (Sheldon, 1977) has suggested that fully abraded surface hardness may be a more suitable quantity.

Figure 2.23 Volume removal as a function of Vickers Hardness Number for metals eroded at $\alpha = 20$ degrees and velocities of 450 ft/s. From Finnie et al. (Finnie, et al., 1967).

The wear resistance of steels could in some cases be improved by heat treatment. This positive effect diminished, however, with increasing impact angles. Uetz and Fohl (Uetz & Fohl, 1978) reported that above an impact angle of 30 degrees, heat treatment did not enhance the erosion resistance of an 0.4 % carbon steel. Figure 2.24 (Uetz & Fohl, 1978) shows that the erosion resistance of heat treated steels can decrease with increasing hardness when using an impact angle of 90. By increasing the impact angle, the resistance to blast wear can be enhanced on softer metals relative to that on harder ones. Christman and Shewmon (Christman & Shewmon, 1979) also reported that a high strength 7075-T6 aluminum alloy had much less erosion resistance than soft ductile pure aluminum, both under oblique single impacts and normal multiple impacts.

Figure 2.24 Schematic representation of resistance to blast wear at impact angles of 10 to 20 degrees (a) and 90 degrees (b) versus hardness of different materials (Gahr, 1987).

The detrimental effect of initial cold rolling on the resistance to erosion was also reported by Levy (Levy, 1979). He explained this behavior by a decrease in the capability of deformation (essentially the ductility, measured by using normal tensile tests) during the following erosion cycle. The effect of ductility can, however, be overwhelmed by too low a strength of the material. The main problem for proper correlation between data of ductility and erosion resistance however arises from the absence of ductility data obtained at such high loading rates as can occur during erosion (Hutchings, Winter, & Field, 1976).

Gahr (Gahr, 1987) explained these contrasting observations, by suggesting that, hardness, work hardening and the capability of deformation (ductility) are important physical properties of the target metal for its

resistance to blast wear. Dynamic hardness and work hardening of the target determine the amount of plastic deformation and hence the depth of the impact craters at a given impact angle and angle of incidence. The capability of deformation of the metal during impact loading affects the number of impacts which are required for the formation of wear debris. As a result, a softer metal can sometimes show greater erosion resistance than a harder one.

Strain rate Sensitivity of the Target Material: Single particle studies on metals (Winter & Hutchings, 1974), (Hutchings, 1979), indicate that the high strain rates associated with particle erosion may lead to different deformation modes in different materials. This material characteristic has not been examined thoroughly for either ductile or brittle materials (Ruff & Wiederhorn, 1979).

Toughness of the target material: Because brittle materials erode by elastic plastic processes, the dynamic toughness plays an important role in the wear of brittle materials (Ruff & Wiederhorn, 1979), (Gahr, 1987).

2.5.2.6 Effect of Microstructural elements

Gulden (Gulden, 1979) has reported on a transition from ductile to brittle erosion behavior in high carbon martensitic steel as a function of heat treatment. Ductile behavior was characterized by a maximum erosion rate occurring at impingement angles between 20 and 40 degrees, while brittle erosion was characterized by a maximum erosion rate occurring at an impingement angle of 90 degrees.

McCabe et al. (McCabe, Sargent, & Conrad, 1985) studied the erosion of two high carbon steels which were heat treated for obtaining spheroidized, pearlitic, martensitic and tempered martensite structures. At the highest velocity used the martensitic structures exhibited maximum erosion rates at an impingement angle of 90 degrees, indicative of brittle erosion. The other structures displayed their maximum erosion rates between 15 and 45

degrees which is an indication of ductile erosion, as will be discussed in section 2.5.3.1. A substantial effect of microstructure on the erosion rate was measured at an angle of impingement of 90 degrees, but only a small effect at low angles. For example no measurable effect of different degrees of spheroidizing on resistance to blast wear at an impingement angle of 45 was reported (McCabe, et al., 1985). Also at a comparable hardness, the spheroidized structure of the lower carbon steel offered a greater resistance to erosion than the higher carbon steel. It follows that the greater volume fraction of carbides is detrimental to the wear resistance on the hypereutectoid steel.

The effect of the carbide volume fraction on the erosion resistance of as-cast white cast irons was studied by Aptekar and Kosel (Aptekar & Kosel, 1985). The erosion resistance was measured by using a rotating arm and a gas-jet system and alumina and quartz erosive particles. A minimum erosion rate was measured at a 15% volume fraction of carbides independently of the angle of incidence of the alumina particles. Weight loss was lesser on the white iron exhibiting the greater carbide content in contrast to the observation of McCabe et al. (McCabe, et al., 1985), for high carbon steels. The reverse was however true when crushed particles of quartz was used. Gahr (Gahr, 1987) after an extensive survey concludes that second phases such as carbides in steels are detrimental to erosion resistance under a wide range of operating conditions. However he reported that white cast irons may be an exception to this general observation as a maximum in erosion resistance can occur on white cast irons as a function of carbide content (similar to the results of Aptekar and Kosel (Aptekar & Kosel, 1985)). The explanation offered for this was that since these carbides were more strongly embedded in the matrix they could impede erosion by agents of lower hardness than themselves.

Grain orientation and grain size effects: Although no systematic studies are available for solid particle impact, Preece et al. (Preece & Macmillan, 1977) have studied such microstructural effects on cavitation erosion.

Gahr (Gahr, 1987) suggests a more general explanation of these microstructural effects described above. At a given impact energy, dynamic hardness and work hardening determine the extent of plastic deformation, while the propensity to deformation determines the number of impacts required for detaching wear debris. The liability to deformation changes with loading conditions (e.g. loading rate). As a consequence Gahr (Gahr, 1987) suggests that, martensitic structures of steels can offer high wear resistance at an impingement angle of 30 but low wear resistance at an impingement angle of 90, while the reverse could be true in another erosion system. Thus, while the influence of microstructure on resistance to solid particle erosion is significant, the extent of this influence depends strongly on the operating conditions, such as angle of impingement, particle velocity, shape, size and the ratio of the hardness of the particles relative to the eroding material (Gahr, 1987). Additional microstructural factors such as the tendency to strain localization (adiabatic shear bands) and low cycle fatigue properties can also become more important with increasing angle of impingement, or with round impinging particles (Gahr, 1987).

2.5.2.7 Surface thermal parameters

Correlation's between erosion rates on the one hand, and thermal conductivities and specific heats on the other hand, have been noted, and erosion theories based on these correlation's have been proposed. A detailed discussion of these theories is presented in the section on erosion mechanisms (section 2.5.3).

2.5.2.8 Effect of Temperature on Erosion

Temperature effects can also influence wear mechanisms substantially, due to altered micro-structures and properties with increasing impact energy or ambient temperature. It is found in static tests that most metals cease to be of structural value when their homologous temperature ($HT = \text{temperature} / \text{melting temperature in degrees absolute}$) is about one half

(Ruff & Wiederhorn, 1979). However, because of the extremely high strain rate in erosion, one might expect to find strength levels maintained to higher temperatures than in static tests, depending on the material being eroded, the temperature range, and the environment different effects due to temperature have been reported. The most extensive collection of experimental results reported to date is that of Tilly (Tilly, 1973), who tested materials using a resistance heated specimen in an airstream. From room temperature to about 500 C, different metallic alloys showed little effect of temperature on erosion at angles of 40 and 90 degrees, while other alloys actually showed an increase in the erosion resistance. Smeltzer et al. (Smeltzer, Gulden, & Compton, 1970) also showed little effect on erosion of gas temperatures ranging from ambient to 370 degrees.

In contrast to these results, Ives and Ruff (Ives & Ruff, 1979) showed that the erosion of type 310 stainless steel, was much greater at 975 °C, than at 25 °C, and the effect was more pronounced at lower velocities where the particles did not penetrate the oxide scales. These results point out the importance of the particle depth of cut relative to the thickness of the oxide layer. Similar trends were reported by Finnie et al. (Finnie, Levy, & McFadden, 1979). Figures 2.25 and 2.26 show elevated temperature results by Finnie et al. (Finnie, et al., 1979), at particle velocities (250 µm particle size) of 30.5 and 61 m/s on 1100-0 aluminum with HT = 0.32, 0.4, 0.6 and 0.8. At the lower velocity, the curves for the lower three temperatures are quite similar. Even the curve for HT = 0.8, shows a peak at about the same angle as the other tests. This indicates that a cutting mechanism is involved at low angles as the temperature is increased. For $\alpha = 90$ degrees, the picture is reversed with no change in erosion occurring up to HT = 0.6 and then a sudden increase upto HT = 0.8. At the higher velocity (Figure 2.26), the effect of angle on erosion is decreased. The curves still show a maximum at $\alpha = 15$ degrees, but for HT = 0.8, the difference between the maximum erosion and that of 90 degrees is only about 15 percent. Finnie et al. (Finnie, et al., 1979) thus report that while there is a significant increase in erosion rate with temperature, the influence of erosion angle

on erosion rates at elevated temperatures is more complicated. Finnie et al. (Finnie, et al., 1979) also suggested that the particle size and concentration in the fluid stream were much more important at elevated temperatures than angle effects.

While a number of studies of erosion at elevated temperatures have been carried out (Ruff & Wiederhorn, 1979), it has not been possible to develop an adequate theory giving the temperature dependence of erosion rate. In most cases environmental reactions also take place, producing surface films or otherwise complicating the analysis. It is however apparent that the erosion dependence on attack angle, particle velocity, and other parameters may be significantly different at elevated temperatures (Finnie & McFadden, 1978).

Figure 2.25 Erosion of 1100-0 aluminum at a velocity of 30.5 m/s as a function of angle for several temperatures as recorded by Finnie et al. (Finnie, et al., 1979).

Figure 2.26 Erosion of 1100-0 aluminum at a velocity of 61 m/s as a function of angle for several temperatures as recorded by Finnie et al. (Finnie, et al., 1979).

2.5.3 Solid Particle Erosion Theories

There have been a number of proposed mechanisms of erosive wear by solid impact, to explain some of the erosive behavior patterns described above. The theories of erosion proposed by previous writers can be broadly classified into those which are mechanical, in which impact stresses lead to deformation or fracture of the metal and its consequent removal, and the thermal mechanisms in which melting of material is postulated, metal being removed in the form of molten droplets. Shewmon (Shewmon & Sunderrajan, 1983) summarized the extensive work conducted on developing erosion models by suggesting a model of

steady state erosion should explain the following general observations (described in the previous section), which are expressed in terms of the dimensionless erosion rate E , (grams of target loss per unit mass of the abrasive).

- 1 E varies as (velocity) ^{n} where n is usually in the range of 2.2 to 3.0.
- 2 A maximum in the erosion rate for ductile metals at an impact angle of 15 to 30°, or in the case of hardened steel a maximum at 60 to 90°.
- 3 For a given alloy, E is not changed by large increases in hardnesses, developed by heat treatment, aging or cold work.
- 4 At low impact velocity, v , an incubation time is observed that increases roughly as V^{-1} .
- 5 An excellent correlation exists between the erosion rate and thermophysical properties of the target material, i.e. the heat to raise the target to its melting point.
- 6 The erosive loss of ductile metal is intimately related to the formation and fracture of lips and/or platelets.

A summary of the various theories of erosion mechanisms which satisfy at least some of the above observations is described below.

2.5.3.1 Mechanical models of erosion

Perhaps the most significant result from previous studies was the finding that two different modes of erosion may be distinguished empirically for two different classes of target material. The ductile mode which is characteristic of most metal targets, is characterized by maximum erosion occurring at an angle of impingement of about 20 to 30 degrees which falls by a factor of 2 or 3 as normal impact is approached (Figure 2.22). The brittle erosion mode, typical of glasses and ceramics, is characterized by the erosion being a maximum at a normal (90 degrees) angle of impingement (Figure 2.22). Thus erosion mechanisms are also classified into "oblique"

and "normal" angles of particle incidence. Two classes of surface deformation during oblique impact of ductile materials have been identified; "plowing deformation", usually caused by spherical projectiles, and a type of deformation caused by angular particles and termed "cutting deformation" by Winter and Hutchings (Winter & Hutchings, 1974). The peak erosion angle of about 20 degrees (oblique impact) for ductile materials is ascribed to these ploughing and cutting deformation processes with consideration also of particle fragmentation on impact. Brittle material erosion peaks at 90 degrees (normal impact) are ascribed to the efficiency of crack formation around the impact crater.

It was concluded by Sheldon (Sheldon & Finnie, 1966) that wear generally occurs by a mixed mode of erosion, with the ductile processes becoming increasingly more important with decreasing angle of impingement. The above conclusions on ductile behavior were supported by optical and scanning microscopy studies of erosion surfaces. Ceramic materials eroded at an impingement angle of 90° exhibit a rough, chipped surface indicative of material removal by a fracture process. By contrast, erosive wear at 15° resulted in relatively smooth surfaces that were characterized by furrows, or wear scars, which indicate material removal by a plowing mechanism. This evidence for ductile erosion is consistent with single particle impact experiments, which show extensive deformation in the impact area and a significant decrease in the incidence of fracture for low angles of particle impingement. While both modes of erosion may take place in the same material, the models have usually been considered distinct and are described below.

Ductile Wear Models: One of the earliest erosion models is that of Wellinger (Wellinger, 1949), who distinguished between a tangential and a normal load component responsible for erosive wear. The erosion of metals by solid particles is often compared mechanistically to a metal cutting or grinding operation, on a small scale (Ives & Ruff, 1979). In this treatment different properties of metals are usually distinguished by a single parameter related to strength, such as hardness (Sheldon, 1977).

Perhaps the most significant success of this approach was Finnie's (Finnie, 1960) model relating solid particle impingement erosion of ductile metals to the micro-machining process. This model which is one of the earliest consideration of ductile processes of erosion, considered a micromachining mechanism model where the trajectory of the tip of a rigid abrasive grain cuts the surface of an ideally ductile material. Finnie et al. (Finnie, 1960; Finnie, et al., 1967) pictured a sharp cornered particle striking a plane surface and assumed that it would rotate only slightly while in contact with the surface. Solving simplified equations of motion by analytical methods, Finnie predicted the trajectory of the tip of the particle through the metal and hence the volume of metal removed by this "machining" action from the surface. This theoretical treatment was found to overestimate erosion rates, and a factor was introduced to account for erosion that does not take place in this idealized manner. The eroded volume V determined after these simplifying assumptions was given by the equation

$$V = (m V_o^2 / \sigma_f K d) g(\alpha) \quad 2.13$$

where m is the particle mass, V_o the impact velocity, σ_f the flow stress, K the ratio of vertical force to horizontal force on the particle, d the depth of cut, and $g(\alpha)$ is a function describing the effect of attack angle α . Using this model, Finnie was able to account correctly for the maximum in erosion rate that occurs near 20 degrees (Figure 2.27) and was also quite successful in explaining many features of solid particle erosion of ductile metals when the angle α is less than 45 degrees. However, quantitative discrepancies arose concerning the effects of flow stress, the velocity exponent and the applicability of the model itself for attack angles near $\alpha = 90^\circ$ (Ruff & Wiederhorn, 1979), where the substantial erosion that occurs is not predicted by the model. Refinements of the original analysis by Finnie and McFadden (Finnie & McFadden, 1978), in which the interaction force between the particle and the target surface is modified, have led to velocity exponents of about 2.5, which are closer to those found by experiment.

Figure 2.27 Predicted and experimental curves for erosion as a function of angle, for Finnie's cutting theory. The values are normalized to give the same maximum erosion in both cases. From Finnie et al. (Finnie, et al., 1979).

Bitter (Bitter, 1963), noting that brittle materials exhibit a maximum in erosion rate at normal incidence (Figure 2.22) and furthermore, recognizing that ductile materials work-harden and eventually fail by microfracture process, proposed that the angular dependence of erosion could be regarded as the superposition of ideally ductile (cutting mode) and brittle (deformation mode) behaviors. Bitter (Bitter, 1963) thus considered an indentation deformation process to occur near $\alpha = 90$ as well as the cutting process embodied in Finnie's model at low angles. In other words erosion was assumed to consist of two simultaneous processes, "cutting wear", and "deformation wear". For ductile metals at low angles, cutting wear predominated, while at high angles deformation wear

predominates. By considering the energies involved in these processes, Bitter was able to develop an expression to better account for erosion at all attack angles. This theory, as well as that of Nielson and Gilchrist (Nielson & Gilchrist, 1968) who simplified the approach of Bitter, requires experimentally determined parameters for complete application. This approach is however dismissed by Finnie et al. (Finnie, et al., 1979) as mere "curve fitting".

All the models described above are commonly called "cutting models" as they assume an analogy between particle impact and single point grinding or machining. Shewmon (Shewmon & Sunderrajan, 1983) suggests that these models are now primarily of historical interest. This was because SEM examination of erosion surfaces and single impact particle experiments show that the analogy is not valid even in the case of angular particles since cutting by particles that maintained a fixed rake angle near zero was very rare. Nevertheless some of the modified models described above do explain a significant extent of observed erosive phenomena.

Tilly (Tilly, 1973), (Tilly, 1979), conducted an extensive series of erosion studies on the effect of the properties of the eroding particles on erosion. From these experiments, Tilly (Tilly, 1973) showed using high speed photography, electron microscopy, and actual particle size measurements to study this effect, that some aspects of erosion of ductile metals are due to particle fracture. According to Tilly, erosion by fragmenting particles can be thought of as the effects of two stages. In the first stage, called primary erosion, particles indent the surface, removing chips and forming vulnerable hillocks or lips at the edge of impact craters. The other stage, called secondary erosion, is due to particles breaking upon impact so that fragments are projected radially to produce secondary damage to the craters and cause removal of these lips. The primary stage predominates for glancing impacts and the secondary stage for normal impacts, as shown in Figure 2.28. Measurements by Tilly also showed the extent of fragmentation to be dependent on the initial particle size and velocity, as well as on impact angle. Particles too small to break up cause primary

erosion alone, while increasing particle size causes an increase in secondary erosion. Similarly, increased velocity causes an increase in secondary damage. He was thus able to account for a reported decrease of erosion with decreasing particle size (large particles should fragment more easily and produce more damage than small ones) and introduce the concept of a maximum particle size for effective erosion.

From his two-stage erosion equations, Tilly (Tilly, 1979) concluded that for extreme conditions of small particles which do not fracture (no secondary erosion) and for large particles and high velocity (maximum fragmentation), the total erosion will tend to a velocity square dependence. In between conditions of particle velocity and size result in particle velocity exponents greater than two. These predictions have been confirmed by Sheldon (Sheldon & Kanhere, 1972) for silicon carbide grit impacting against several metals. Finnie et al. (Finnie, et al., 1979) however suggested that there was no universal approval for the generality of this model, although it worked in some cases.

Maji and Sheldon (Maji & Sheldon, 1979) reported that erosion of flat plates and tubular test specimens by spherical 270 μm steel shot showed appreciable differences in the amount of material removed, depending on the condition of the steel shot used. Ductile shot removed considerably less material than brittle or more frangible shot. In addition, lower velocity exponents were obtained for the tougher, more ductile abrasive (Maji & Sheldon, 1979). This evidence supports the view of Tilly that erosion by frangible particles is a two stage process. The tube erosion of a aluminum tube carrying brittle abrasive particles examined by Maji and Sheldon (Maji & Sheldon, 1979) also appears to be in agreement with the model of Tilly, in that a large amount of erosion is by secondary erosion of fractured abrasive particles. Abrasive particles as received appeared to fragment when passed through the tube. The fragmented particles were removed by a screening process, and the non-fragmented particles were again passed through the tube. The erosion produced by the screened particles was much less than the un-screened as received particles. The

explanation provided is that the particles used after screening were tougher than the other particles in the first pass (as received pass). These particles being tougher, would not fragment when passed through the tube a second time (screened samples), and consequently abrasive fracture and secondary erosion is lesser in the screened case than in the as-received case. This is why Maji and Sheldon (Maji & Sheldon, 1979) conclude that secondary erosion is significant during erosion and agree with the Tilly model.

Figure 2.28 Influence of impact angle for 135 μ m quartz against H46 steel at 366 m/s, showing primary and secondary stages of erosion as reported by Tilly (Tilly, 1973).

Sheldon and Kanhere (Sheldon & Kanhere, 1972) have examined the mechanism of single particle erosion of ductile materials, and developed a method to describe the deformation and machining actions observed using indentation theory and an energy balance equation. The derivation,

which applies for $\alpha = 90$ degrees, essentially is an energy balance between the kinetic energy of the particle and the work expended during indentation. Their results differ from previous calculations, giving the erosion volume as

$$V = K (d^3 V_o^3 \rho_p^{3/2} / H^{3/2}) \quad 2.14$$

where d is the particle diameter, ρ_p is the particle density and H the Vickers hardness value of the material in the "fully work-hardened" condition, which may be as much as five times greater than that of the annealed metal. This theory leads to a greater velocity dependence than expected from energy arguments.

Studies by Winter and Hutchings (Winter & Hutchings, 1974) and Hutchings (Hutchings & Winter, 1975), (Hutchings, 1979; Hutchings, Winter, & Field, 1976), (Hutchings, 1981) into the mechanisms of single rigid particle erosion on ductile metals have led to suggestions of several other significant impact processes and materials parameters. One important mechanism they report involves adiabatic shear as they found that the lips were detached along localized adiabatic shear bands, formed beneath the surface near the lips. In these studies the impact behavior observed in these model experiments were compared with that predicted by a simple model and with impact damage caused by hard irregular abrasive grains. The experiments were carried out at impact angles of 20 and 40 degrees, as maximal erosion in ductile metals occurs at these angles.

The repetitive loading imposed by the erosive environment and some observations of the fracture surfaces led these investigators to consider a correspondence between erosion resistance and fatigue in metals. The fatigue-type model used by Hutchings (Hutchings, 1979), (Hutchings, 1981) in these comparisons differs only slightly from that used by Finnie (Finnie, 1960; Finnie, et al., 1979; Finnie & McFadden, 1978; Finnie, et al., 1967) (described previously), as it does not assume a force vector of

constant direction. The advantage of the approach of Hutchings is that it predicts the variations in behavior with rake angle, unlike the model of Finnie which represents an averaging over all possible rake angles and orientations. Hutchings and Winter (Hutchings & Winter, 1975) also reported evidence that adhesive forces between an impinging particle and the target surface play an important role in forming overhanging lips at the crater edges. Unlike the fatigue model of Hutchings (Hutchings, 1981), Sundarrajan and Shewmon (Sundarrajan & Shewmon, 1983) discussed the detachment of material extruded by localized lip formation on the basis of exceeding a critical strain. Bellman and Levy (Bellman & Levy, 1981) have also proposed a platelet mechanism of erosive wear.

Microstructural Features of Oblique and Normal Impact Erosion on Ductile Materials: The detailed deformation and fracture processes that take place as a result of particle impact on metals are not yet adequately understood. Ives and Ruff (Ives & Ruff, 1979) suggested that while a much better picture of the mechanics of material removal has been gained, little effort had been reported on material response at the microstructural level. Ives and Ruff (Ives & Ruff, 1979) in their investigation therefore used SEM and TEM to study the microstructural features associated with multiple particle erosion damage to a ductile material, copper. Emphasis was placed on examining the material response at attack angles of 20 and 90 degrees. At 20 degrees, topographic features were observed to resemble those generally believed to result from a cutting process and the subsurface damage was classified into three loosely defined zones (Ives & Ruff, 1979). The first zone consisted of a layer of highly deformed grains. The second zone was characterized by the presence of deformation twins and was often separated from the third zone by a definite boundary associated with an intense shear deformation. The third zone consisted of dislocations at a concentration that decreased with increasing distance below the surface.

At 90 degree particle incidence, the deformation damage was more severe than at 20 degrees, with a high density of dislocations extended to greater depths (Ives & Ruff, 1979). Particle embedding was far more extensive at

90 degrees than at 20 degrees. The effect of the embedded layer on erosion had not been previously investigated extensively. Ives and Ruff suggested that the embedded layer could in effect be regarded as a composite layer having entirely different properties from the base metal. An erosion model was suggested in which regions containing a locally high concentrations of particles tend to be raised above the surface due to their greater indentation hardness. Once exposed, however, these regions are likely to be fractured away by impact imposed tensile and shear stress.

Ruff and Wiederhorn (Ruff & Wiederhorn, 1979) also reported that even at low (30^0) attack angles, little metal may be removed by direct cutting impacts (Finnie's theory). Rather, plowing type interactions producing extensive plastic deformation and displaced material create a surface topography from which metal may be removed by subsequent particle impacts. In view of the complex topography of eroded surfaces it will be necessary to further examine the theoretical models that presently describe the erosion process. However this result has been reported from limited studies of erosion.

Brittle material models: By contrast with ductile metals, in which material can be removed by the cutting action of a particle, as well as by other mechanisms, in ideally brittle solids removal must occur by the propagation and intersection of cracks. Thus the mechanisms of material removal for ductile and brittle behavior are completely different. Two models have been developed for the erosion of brittle materials (Ruff & Wiederhorn, 1979); one is based on the assumption that erosion occurs entirely by crack propagation and chipping (Sheldon & Finnie, 1966); the other is based on the assumption that plastic deformation contributes to the process of crack formation and surface chipping (Evans, Gulden, & Rosenblatt, 1978). Erosion rates are predicted in terms of both target (fracture toughness, hardness, flaw density, and particle properties). The models assume that particle impact is normal to the target surface, and that erosion is the result of cumulative damage of non-interacting single particle impacts.

The model proposed by Sheldon and Finnie (Sheldon & Finnie, 1966) assumes that erosion occurs as the result of Hertzian contact stresses during impact. The erosion rate W is expressed in terms of the particle size r , the particle velocity V_o and Weibull constants m and σ_o of the target surface (Ruff & Wiederhorn, 1979)

$$W = k_1 r^a v_o^b \quad 2.15a$$

where the exponents a and b are given by

$$a = 3.0(m - 0.67) / (m - 2) \text{ for round particles} \quad 2.15b$$

$$a = 3.6 (m - 0.67) / (m - 2) \text{ for angular particles} \quad 2.15c$$

$$b = 2.4 (m - 0.67) / (m - 2) \text{ for either shape} \quad 2.15d$$

Sheldon and Finnie (Sheldon & Finnie, 1966) compared their experimentally determined exponents a and b with theoretically predicted exponents and obtained satisfactory agreement for several brittle materials. In a later paper Sheldon and Kanhere (Sheldon & Kanhere, 1972) compared experimental and theoretical values of k_1 and found again reasonable agreement between theory and experiment, although the agreement was not as good as that of a and b . Although the theory by Sheldon and Finnie (Sheldon & Finnie, 1966) provides a reasonable description of erosion in brittle materials, Ruff et al. (Ruff & Wiederhorn, 1979) questioned its physical basis because it assumed Hertzian crack formation, whereas they suggested that lateral crack formation was the main cause of material removal during erosion.

Sheldon and Kanhere (Sheldon & Kanhere, 1972) also noted the importance of tangential forces in the brittle wear process. They noted that for normal impact the erosion rate of brittle materials was proportional to V_o^b where V_o is the particle velocity and b is the power exponent. If only

brittle behavior were occurring at an oblique (not normal) impact angle, then the erosion rate E might be expected to be proportional to $(V_o \sin \alpha)^b$, where α is the impact angle. By plotting $\ln E$ as a function of $\ln (\sin \alpha)$ for a constant impact velocity, they determined that the power function b was not the same as that determined from velocity studies using normal impact angles. They concluded from these results that tangential forces contribute to the wear of brittle materials.

2.5.4 Summary of Mechanical Erosion models

Shewmon and Sunderrajan (Shewmon & Sunderrajan, 1983) summarized the several kinds of mechanical models developed by expressing the erosive wear by the dimensionless erosion rate E (grams of target weight loss per unit mass of abrasive). E was then expressed by the general form (Shewmon & Sunderrajan, 1983)

$$E = (V_d \rho_b) (SR^3 N_c \rho_p) \quad 2.16$$

Where V_d is the volume undergoing heavy deformation right under the particle. It is set equal to the volume removed as a chip after N_c impacts. R and ρ_p are the incident particle radius and density respectively, S is the particle shape factor, and ρ_t is the target density.

The various models proposed fit the above equation in the following form (Shewmon & Sunderrajan, 1983)

Cutting models: As described previously, Finnie et al. (Finnie, 1960; Finnie, et al., 1979; Finnie & McFadden, 1978; Finnie, et al., 1967) and Bitter (Bitter, 1963) developed cutting models assuming an analogy between particle impact and the cutting action in single point grinding or machining. Here V_d was set equal to V_c , the crater volume, and N_c equals one.

Fatigue models: Fatigue models build on theories of low cycle fatigue. Following the Coffin-Manson equation (Shewmon & Sunderrajan, 1983)

$$N_c = (\epsilon_f / \Delta\epsilon)^2 \quad 2.17$$

where ϵ_f is the fracture strain and $\Delta\epsilon$ is the strain increment experienced by the target during each impact.

Normal impact: Hutchings et al. (Hutchings, 1979; Hutchings, 1981; Hutchings, et al., 1976) among others developed models assuming that an element of volume will be lost once it accumulates a critical strain. They do not treat the mode by which the material is lost. The model assumes that V_c is proportional to the crater volume, and that $\Delta\epsilon$ is given by the Tabor relation; $\Delta\epsilon = 0.1 WR^{-1}$, where W is the diameter of the indent. The model predicts a velocity exponent of 3, and that E will be inversely related to the target yield strength and its fracture strain.

Localization model: Shewmon and Sunderrajan (Shewmon & Sunderrajan, 1983) proposed a model that treats the shear localization leading to lip formation and fracture. Each term in equation 2.16 is calculated with equations, whose parameters can be determined independently in other experiments. An equation

$$N_c = (\epsilon_c / \Delta\epsilon) F(t) \quad 2.18$$

is derived, where $\Delta\epsilon$ is the strain increment per impact and $F(t)$ is the variation of strain with depth beneath the impact. The authors claimed that the model explains all of the observations, and agreement between calculated and observed rates was reported to be good.

The above theories are typically called mechanical theories of theories. A second class of theories attempt to link solid particle erosion to localized melting on the target surface. A brief synopsis of these theories is presented below.

2.5.5 Thermophysical or melting mechanism of erosion

Another possible erosion mechanism which may take place upon particle impact is surface melting as a result of localized energy release on impact. Smeltzer et al (Smeltzer, Gulden, & Compton, 1970) studied eroded metal specimens using replica electron microscopy and reported surface structures and metal particle shapes suggesting the occurrence of local melting. Similar observations were reported by Jennings et al. (Jennings, Head, & Manning, 1976) in erosion studies of several metals at particle velocities of 145 m/s. Christman and Shewmon (Christman & Shewmon, 1979) also reported localized melting for high particle velocities of 185 m/s. This phenomenon has not however been reported for lower particle velocities.

Proponents of thermal mechanisms of erosion tend to emphasize the correlations observed between erosion rates and thermal properties of the target materials. Ascarelli (Ascarelli, 1971) has found excellent correlation between the erosion rates of pure metals and a thermal pressure defined as the product of linear thermal expansion coefficient, the temperature rise required for melting, and the bulk modulus of the metal. Some correspondence exists between erosion rate and melting point (Smeltzer, et al., 1970), and a correlation as good as that found by Ascarelli was found with the product of density, specific heat and temperature (ρcT) rise required for melting (Hutchings, 1975) (essentially the energy required to melt). Hutchings (Hutchings, 1979) however suggests that since other correlations have been found, between erosion rates and the hardness of annealed metal (Finnie & McFadden, 1978), Young's modulus (Brauer & Kriegel, 1965), and interatomic bond energy (Vijh, 1976), consideration of thermal properties is not essential to obtaining a correlation. The

generality of this hypothesis was also questioned by Finnie et al. (Finnie, et al., 1979), because materials of widely differing thermal properties show a similar response to eroding particles.

To further prove his point Hutchings (Hutchings, 1979) estimated the likely energy transfer from impacting particle to the metal from experimental and theoretical studies. Estimating the volume to which the energy is transferred, and even assuming a 80 percent transfer of impact energy to thermal energy, the temperature rise was estimated to be only 500 K, which is insufficient for melting. Hutchings suggests that it is not enough to show (Smeltzer, et al., 1970), that there is ample energy to melt the mass of metal removed, a plausible mechanism must be proposed by which energy can be concentrated in a volume very much less than that of the indentation. Hutchings also suggests that localized shear deformation may play a role in erosion, as bands of localized shear are seen in experiment.

Hutchings (Hutchings, 1979) therefore concludes that the fundamental mechanism operating in erosion by solid particles is mechanical, and melting plays only a secondary role in material removal. It however remains to be explained, if this is indeed the case as to why conventional strength measurements and hardness of a metal give a poor indication of its erosion resistance. Hutchings suggests that a possible explanation is the high strain rates that occur during deformation, with rates as high as $5 \times 10^7 \text{ s}^{-1}$ being predicted (Hutchings, et al., 1976). Therefore Hutchings, suggests that quasi static strength measurements, cannot be used to predict the erosion behavior of metals under erosion conditions. Unfortunately as Hutchings notes, very little is known of the behavior of metals at such high rates of strain, since conventional dynamic testing, cannot be used at such strain rates.

2.6 EROSION WEAR BY CAVITATION

The loss of material by the repeated formation and collapse of cavities in a liquid at localized regions of low pressures is known as cavitation. There are a number of hypotheses to explain cavitation erosion due to the complexity of the phenomena, which depends on the physico-chemical properties of liquid and material as well as on other factors relevant to the initiation and progress of cavitation. An excellent review of these theories are available in a review paper by Preece (Preece, 1979), and Matsamura (Matsamura, 1979). Davis and Murray (Davis & Murray, 1981) also have an excellent discussion of cavitation erosion from empirical observation of zinc die casting dies. Some of the theories regarding the likely causes and possible mechanisms of cavitation damage in die casting die has already been presented in section 2.2.1.1 There are however a number of accepted opinions on the phenomena of cavitation (Stellar, et al., 1974)

- Cavitation damage occurs as the result of the collapse of cavitation bubbles when the local pressure falls below the vapor pressure, as well as due to the additional hydrodynamic effects which take place most often in unstable regions or cavitation zones. When a bubble collapses, a jet of fluid is formed which impact the surface at speeds of the order of 130 m/s (Stellar, et al., 1974). This impact produces local pressures of around 300 Mpa for a very short time duration (10^{-7} seconds). The damage caused by the collapse of bubbles near a surface is similar to that produced by the high velocity impacts of jets and droplets (Brunton, 1970). For example Figure 2.29 shows a series of high speed photographs made by Brunton (Brunton, 1970) showing the collapse of a bubble in water. Brunton showed that when the bubble collapsed near a surface defect, the forces generated were very effective in producing further damage in that area (similar to the effect of high velocity jets).

- Mechanical effects, namely collisions between the material surface and stream of liquid, and dynamic loads due to shock waves are the main and direct cause of the material damage.
- Physico-chemical and structural properties of materials as well as the properties of the liquid are responsible for the damage kinetics at a given form and intensity of cavitation.
- Damage intensity depends on the bubble shape and dimensions, impact velocity, frequency and pressure distribution.

The tendency to cavitate in a given geometry of a flow channel, is usually indicated by a cavitation number

$$K = (P_o - P_v) / (\rho / 2V_o^2) \quad 2.19$$

Where P_o and V_o are the stream pressure and velocity respectively at a point o in the metal and P_v is the vapor pressure of the molten metal (at the casting temperature in die casting) and ρ is the density of the molten metal. The density which affects the energy of the flowing metal clearly affects the cavitation phenomena. The value of K at which cavitation is initiated is referred to as K_i (Cavitation inception number) and cavitation erosion has been shown to increase rapidly when K falls below this value (Davis & Murray, 1981).

A discussion of the effect of the deleterious effect of collapse of cavitation bubbles on die wear was discussed earlier. The current study shall however ignore cavitation erosion, because of the difficulty in isolating cavitation effects in die erosion. The theories are however presented here and earlier for completeness.

Figure 2.29 The asymmetrical collapse of a disk shaped air bubble as shown by Brunton (Brunton, 1970).

2.7 Combined effects of Corrosion and Erosion

Erosion-corrosion (E-C) is a problem in a variety of dynamic environments, such as the ocean, pipelines (particularly at bends), impellers, agitators, heat exchangers, orifices, nozzles, propellers, turbines, most parts of coal conversion plants, exhaust systems, and obviously in die casting applications.

The rate of metal loss in most corroding systems stifles itself due to the formation of a protective film. However, if this film is continuously removed or damaged, the stifling of the corrosion process may fail to occur. (Vyas, 1979) Thus without several assumptions, it is expected that chemical and mechanical influences acting conjointly will produce far more damage than if acting separately (Evans, 1960). This points out some

of the problems of the corrosion dip tests used in die casting where erosive effects are ignored, and of simulative tests where corrosive effects are ignored.

There are various environments within which the erosion-corrosion phenomena can occur, and each affects wear in a different way. For example gaseous environments besides forming surface films, can affect the mechanical properties of materials by the diffusion of species from the gas phase into the bulk. Aqueous media on the other hand can affect the mechanical properties by adsorption of surface-active species on the surface of the metal, such as liquid-metal embrittlement (Vyas, 1979).

It is apparent that interactions of environment and the material produce diverse and complex effects. It is, therefore, not always possible to quantitatively separate the chemical and the mechanical effects (Vyas, 1979). The interaction may be synergistic and lead to catastrophic failure, or one component may inhibit the harmful effect of the other. There are however various simulative tests that are used to evaluate erosive-corrosive wear under controlled conditions. These tests include (Vyas, 1979):

Flow-through tests: These tests are usually carried out to simulate actual service conditions. Therefore, appropriate liquids are forced through pipes designed to duplicate flow conditions experienced in actual service. A section of the location of interest or test specimen inserted in the flow is examined for its material loss. The test is used to simulate conditions in transparent of slurries, heat exchangers parts, distillation parts etc.

Rotating sample tests: In the rotating sample test a sample, in the shape of a drum, disk, or flat plates, attached at the periphery of a corrosion resistant disk, is axially mounted on a nonconducting spindle. The assembly is rotated about its axis in a tank of given electrolytic of interest. The rotating drum test is mainly used to do mechanistic studies of the effect of mass transport of corrosion species on E-C, since the flow

characteristics of the liquid are relatively simple and easily controlled (Makridas, 1960). A metal disk is generally used to measure the effect of a range of velocities on a single sample. The velocity of the liquid across the surface of the metal varies from zero at the center of the disk to a maximum at the periphery. However the flow characteristics of liquid around the disk are rather complex and not easy to analyze (Davis, 1977).

Jet tests: Generally for high velocity tests a multivelocity jet section is used. In this test a specimen is attached just upstream from a nozzle with an appropriate orifice plate. The size of the orifice determines the velocity of the liquid flowing over the coupon specimen. By arranging several jets in parallel, liquid from a single source can be fed on several specimens at different velocities simultaneously. The main advantage of this technique is that the velocity over the sample can be measured and controlled accurately (Danek, 1966; Vyas, 1979).

Vibrating tests: These are the most widely used devices for accelerated cavitation erosion testing. For example cavitation is produced by an ultrasonic probe vibrating in a stationary liquid (Vyas, 1979).

The various testing techniques were originally designed to screen the E-C resistance of a larger number of materials to a given environmental condition, and in that capacity the tests served well. However it is difficult to compare the E-C resistance of the materials exposed to the different tests. Nevertheless general observations have been made from these tests and some relevant results are summarized below.

2.7.1 Effect of Process Parameters

2.7.1.1 Effect of Liquid Velocity

In most cases of general corrosion, the flow of the liquid increases the rate of arrival of the corrosive species to the metal and the rate of removal of the corrosion product from the metal surface, and consequently increases the rate of corrosion(Vyas, 1979). However, if the metal forms a protective oxide film the increase in velocity will increase the availability of oxygen to the surface to form a passive film and, thus, decrease the rate of material.

A schematic plot of the material loss rate as a function of the liquid velocity or the surface shear stress for copper alloys is shown in Figure 2.30 (Syrett, 1976; Vyas, 1979). The figure has been split into 5 zones labeled A-E, each zone corresponding to a different erosion-corrosion process. In zone A, the flow is laminar, while in zone B, it is turbulent, and the rate of metal loss is controlled by the transport of reactive species to the metal surface. At the breakaway velocity (i.e. at the onset of zone C), the shear stresses are so great that the film is removed in a few areas, and severe corrosion takes place at the exposed areas. In zone D, the oxide removal is more general, and therefore the contribution from galvanic currents is reduced, however the rate of metal loss is high since a larger area of the bare metal is exposed to the aggressive environment. Zone E represents the velocity range where the film is completely removed and repassivation is not possible.

Figure 2.30 Changes in Erosion-Corrosion mechanism as a function of velocity of liquid (Syrett, 1976; Vyas, 1979).

2.7.1.2 Effect of Liquid drops, jets and slurries

There is an effect of mechanical degradation by the impact event of the liquid or solid particles on the surface of the material. In a corrosive environment, the radial shear stress (while small in comparison to the impact stress) may be sufficient to crack or remove the corrosion film, and expose the fresh metal to the aggressive environment (Vyas, 1979). The most common type of E-C by jets is observed at the entrance end of condenser tubes, and it is known as impingement attack (Vyas, 1979), similar to what is seen in die casting. The E-C is caused by the mechanical effects of the high velocity turbulent water and the impacting air bubbles. In sea water applications, the passive alloys, such as stainless steels, tend to

undergo pitting corrosion, which makes them unattractive for condenser tube applications despite their good resistance to high velocity turbulent waters (Danek, 1966).

In slurries, i.e., the suspension of solid particles in a liquid, the relative importance of mechanical and chemical components varies widely with the hydrodynamic intensity, from purely mechanical damage at high velocities, to mechanically accelerated corrosion at lower velocities (Gandhi, Ricks, & Aude, 1975). Saito (Saito & Shinego, 1972) found that the rate of corrosion of brass increases markedly when sand particles are added to the solution, and the erosion rate increases with the increase in the slurry velocities from 1 to 7 m/s. Up to a velocity of 3 m/s the brass could be protected cathodically, but at higher velocities cathodic protection was not sufficient. This suggests that at low velocities of the slurry, the corrosion of the brass is the most important parameter in the metal loss, while at higher velocities, the abrasion of the brass by the sand plays a bigger role (Saito & Shinego, 1972).

Postlethwaite and co-workers (Postlethwaite, Brady, Hawrylak, & Tinker, 1978; Postlethwaite & Hawrylak, 1975) studied the E-C of steel pipes by slurries at low velocities (up to 4.5 m/s). They showed that the presence of hard abrasives such as sand and iron ore, and soft particles such as coal, increased the corrosion rate of the steel by a factor of ten. This large increase in the material loss could not be explained by the three most common effects of solid particles in flowing liquid, namely, (i) the solid particle causes local turbulence in the liquid, and thus increase the rate of diffusion of oxygen to the metal surface, (ii) increases the rate of removal of the surface corrosion film, thus exposing the bare metal to the environment, and (iii) the impacting particles roughen the surface of the metal, thus exposing a larger surface area to the corrosive environment. Therefore the authors concluded that even at such low velocities, erosive processes, such as continuous removal of the protective film from the surface, and yield dissolution of the metal, play a significant role.

The erosion of metals by slurries results from the dynamic action of the moving particles on metal surface. Depending on the flow conditions and the geometry of the system, the erosion can result from deformation of metal surface by the impact of solid particle, and eventual metal particle removal, or sliding, and hence cutting wear. In general, the rate of erosion increases with increase in particle size and slurry velocity (Gandhi, Ricks, & Aude, 1975; Vyas, 1979).

In long term tests the rate of material loss by slurries decreases as a function of time. Postlethwaite et al. (Postlethwaite, et al., 1978; Postlethwaite & Hawrylak, 1975) observed that the rate of material loss of steel pipes decreases from 4 mm/year to 2 mm/year after 300 hr of exposure to a slurry of sand in water. On changing the sand in the slurry, however, there was an increase in the rate of material loss, thus suggesting a change in the characteristics of the sand particles.

The slurry erosion in an Erosion-Corrosion environment appears to be the closest to the conditions prevalent to die casting. The literature survey however reveals, that the exact nature and extent of wear depends on the particular erosive-corrosive environment, and generalized models cannot be developed.

2.7.1.3 Effect of Solid Particles

It has generally been recognized that erosion by solid particles can accelerate the rate of material loss in a high temperature corrosive environment. For example, Nateson (Natesan, 1976) and Perkins and Coons (Perkins & Coons, 1977) have recognized the importance of erosion processes in coal conversion plants. Mahorten (Mahorten, 1974) has shown that erosion by sand and salt can have a significant effect on the deterioration of engines and turbines used in marine applications.

In a high temperature erosive environment, the temperature, the various corrosion reactions, and the resultant change in the surface of the metal

become added variables. All these variables are interdependent and comprise a complex experimental situation. Therefore the study of E-C in such environments has been restricted to an evaluation of a particular metal in a given industrial environment, and only a few investigators have attempted to obtain a better understanding of solid particle E-C.

Tilly (Tilly, 1969) and Smeltzer et al. (Smeltzer, et al., 1970) have exposed a series of super alloys, low carbon steel, stainless steel etc., to solid particle erosion up to 600 °C and found a decrease in the rate of material loss with an increase in temperature. This has been attributed to an increase in ductility of the material at the higher temperature. On the other hand, Ives (Ives, 1977) reported a tenfold increase in the rate of erosion of type 310 stainless steel at 975 °C compared to the erosion at 25 °C when exposed to a corrosive combustion gas atmosphere. Young and Ruff (Young, 1977 #114) have shown that the erosion rate of a material at the higher temperatures may increase or decrease depending on other variables, such as the angle of impact, the particle size, and the type of solid particle.

All these observations suggest that the rate of material loss is dependent on the interaction of solid particles with the growing surface oxide film on the metal at higher temperatures.

SEM observations of the damaged surface give further evidence of interaction of the corrosion film with the erosion process. Ives (Ives, 1977) and Young and Ruff (Young, 1977 #114) have observed cracked oxide films on surfaces impacted at high temperatures as against a general rumpling of the surface at lower temperatures. Goebel et al. (Goebel, Spriggs, Brakalow, & Pettit, 1977) have confirmed the existence of oxide films on metals exposed to high temperature E-C conditions by electrical conductivity measurements across the eroded surface.

Based on these observations, Goebel et al. (Goebel, et al., 1977) have proposed that the rate of erosion may be dependent on the relative size of

the particle and the thickness of the protective oxide. When the thickness of the film is small compared to the average diameter of the impacting particle, the particles break through the oxide layer and deform the substrate. The thin oxide layer has a negligible effect, and the erosion occurs by the interaction between the particle and the substrate. If the thickness of the oxide film and the average diameter of the film are comparable, the erosion process would involve the cracking and eventual spalling of the oxide film as well as some deformation of the substrate metal. Under these conditions the oxide film would have a significant effect on the erosion process.

2.7.1.4 Effect of Surface films

It is clear that the rate of material loss in E-C conditions is governed by the presence of a surface film which acts as a barrier between the aggressive environment and the base alloy (Vyas, 1979). The mechanical properties of the film, which have received relatively little attention, would be of greater importance under E-C conditions, and understanding them would lead to the design of materials which are more resistant to E-C.

Vyas (Vyas, 1979) summarized the stresses generated in films as being due to

- The volume ratio of the metal and the oxide; Pilling and Bedworth (1923) showed that if the ratio of the volume of the oxide to the volume of the metal is not unity, stresses are generated at the metal/oxide interface. Most industrial alloys have a volume ratio greater than unity, and thus gives rise to compressive stresses in the scales which, therefore are generally protective.
- Changes in the composition of the oxide, Changes in the composition of the oxide or the metal during film growth produce stresses in the film. In addition, oxidation in complex gases and the incorporation of carbon, sulfur, or hydrogen in solution in the

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or previous time-level values for all advective, pressure and other accelerations. To satisfy the continuity equation, the pressures in each cell are adjusted either implicitly or explicitly and the velocity changes induced by each pressure change are added to the velocities computed in the first step. Finally when there is a free surface or fluid interface, it is updated using Equation 1.7 to give the new fluid configuration. Repetition of these steps takes place until all full cells satisfy the continuity equation, thereby advancing a solution through any desired time step. The advantage of this algorithm is that pressure and velocity are both included in the iteration, and it thus converges faster.

In addition FLOW-3D has a number of auxiliary models which are very useful in die casting. A bubble or void region pressure model allows the modeling of the cavitation phenomena in die casting. FLOW-3D is also capable of handling two-phase gas and liquid metal flow. Coupled with the turbulence model, this two phase modeling capability should allow FLOW-3D to model entrapped gas porosity in die casting at least qualitatively.

FLOW-3D has the framework of a solidification and heat transfer model and is capable of handling the heat conduction within solid walls or obstacles such as pins. It is therefore possible, with some modification, to completely model the thermal cycling of metallic molds and pins in die casting. These modifications include modeling the ability to handle cooling lines, an improved pre-processor to model the entire die geometry and the modeling of lubricant spraying and air cooling using average heat transfer coefficients.

3.4.1 Modeling of die cavity filling and Solidification in die casting

While FLOW-3D appears to have the capability to model free surface fluid flow in die casting, it had traditionally been used to model free surface problems in space applications. So a number of boundary/initial

conditions and modeling assumptions had to be imposed to model fluid flow in die casting dies. A brief synopsis of the various boundary conditions made to model the fluid flow in die casting dies is described below.

The computational mesh is initially specified depending on the geometry of the problem being modeled. This entire computational mesh is then divided into holes (die cavity) and wall regions (die cavities) by interpolation from geometric input. This is currently done from CAD models, but was previously done by specifying quadratic equations that is used to describe the geometry. So essentially any cell in the mesh may be a hole, wall or a quadratic interpolation may be used to divide a particular cell into separate wall and hole regions. All the hole regions specify the regions that have to be filled with metal, while the wall regions represent the die or core pins.

The die cavity is treated as an initially empty region ($F=0$), which is then filled with fluid depending on the boundary conditions. This is done by specifying mixed boundary conditions on a mesh boundary that contains the geometry of the inlet (gate or biscuit). The hole region of that boundary is assumed to be filled with fluid, which flows across the hole with a specified time dependent velocity (typically assumed a constant velocity). Thus the hole region of that mesh wall acts as a source of metal (gate) allowing a mass of fluid to flow across into hole regions of the mesh (die cavity). The rest of that mesh boundary is assumed to be fixed die wall. --

Wall boundary conditions are imposed on the wall regions (die walls) that border the hole regions (die cavity) and mesh boundaries. Wall boundary conditions imply zero normal velocity. Free slip or no slip conditions can also be imposed on the wall. To represent the velocity in the boundary layer, the no-slip condition is chosen.

Pressures are imposed on the fluid free surface during filling. This pressure may be specified as a zero pressure (essentially an assumption of vacuum conditions), atmospheric pressure (essentially an assumption of sufficient venting, that results in a constant back pressure on the fluid) or a more physical assumption of a void region pressure model. This void region pressure model assumes that any void regions have an initial pressure, which increases with decreasing void region volume as specified by the universal gas equation (PV^{γ} is a constant). Thus if no venting is assumed (that is not a completely physical assumption), void region volumes will continually decrease as the cavity is filled, resulting in an increase in the back pressure on the fluid (and theoretically cavitation on the die walls) as this volume becomes extremely small. While modeling of this phenomena was attempted successfully, the physicality of this assumptions could be questioned in the absence of experimental verification. Thus the most commonly used assumption was that of a constant atmospheric pressure imposed on the fluid free surface. This is an acceptable assumption if one assumes sufficient venting at the die walls. The void region pressure model however holds great promise for modeling cavitation and gas porosity in the future.

Heat transfer takes place between die wall surfaces and the fluid during the computation. A solid heat conduction equation is used to calculate the die wall temperatures at the end of the previous time step (the temperature is specified at the start of calculations). Knowing this wall temperature, heat transfer between the molten metal to the die wall at the current time step is specified by using a heat transfer coefficient h . This is given by

$$q = h W_A (T_w - T) \quad 3.8$$

where q is the heat flux, W_A is the surface area within each cell, T_w is the wall surface temperature calculated from the previous time step, and T is the temperature of the fluid. This heat loss from the fluid during the

current time step results in a loss of enthalpy that is used to model solidification in the metal, and results in a heat gain in the wall regions. Solid heat conduction equations are then solved to obtain values of T_w which are then used for the next time step.

While the developed model certainly has the capability to obtain the values of process parameters during the filling stage of the die casting process as discussed, the accuracy of the complex filling patterns in die casting remained unproven. Benchmarking studies of the filling simulations were therefore carried out, by comparing numerical filling patterns with experimental filling patterns obtained from water modeling studies. The results of this benchmarking study are presented below along with a brief synopsis of water modeling studies.

3.5 BENCHMARKING: COMPARISONS OF NUMERICAL FILLING PATTERNS WITH FILLING PATTERNS FROM WATER MODELS

3.5.1 Water Modeling of Die Cavity Filling

One of the fundamental problems in designing the runner-gate system in order to obtain a desired fill pattern, is that visualization of the metal flow in actual die casting operations is practically impossible. Before the availability of computational fluid flow techniques to allow numerical simulation of mold filling, water analogy studies were utilized to visualize die cavity mold filling and study the effect of runner and gate geometries. Even though the water analogy method cannot be used to evaluate the effect of the molten metal temperature and solidification in die casting, the method is useful for visualizing the flow path of molten metal, estimating locations of porosity in the casting cavity, and turbulence in the gating system (Han et al., 1989).

A number of water analogy studies have been conducted over the years by among others, Wallace and co-workers (Smith & Wallace, 1963; Stuhrke & Wallace, 1964; Stuhrke & Wallace, 1965), Frommer (Frommer, 1933), Moorman and Sheptak (Moorman & Sheptak, 1962), Booth and Allsop (Booth & Allsop, 1983), for different gate, runner and cavity geometries for a range of process conditions. These water analogy studies exhibit complicated flow conditions such as jetting, splashing and spray formation, which distinguishes the flow in die cavity filling from that of sand casting or injection molding.

While the results of these water analogy studies indicate the difficulties in modeling the fluid flow in die casting processes, they also provide an excellent benchmark to test the capability of a free surface solver in simulating the mold filling in a die cavity. Die cavity filling simulations using the FLOW-3D based numerical model were therefore compared with water analogy studies to test the capability of this model in simulating the mold filling of a die cavity. The results of some of these comparisons are presented below.

3.5.2 FILLING OF FLAT PLATE CAVITIES WITH CHISEL GATES

Wallace and his coworkers (Smith & Wallace, 1963; Stuhrke & Wallace, 1963; Stuhrke & Wallace, 1965) performed a series of water analogy experiments to study the flow patterns obtained in die cavities for a variety of runner and gate geometries, shot sleeve fill percentages and gate velocities. Comparisons of flow patterns with FLOW-3D based numerical simulations for some of the cases considered is outlined below.

3.5.2.1 Effect of runner cross section

Smith and Wallace (1963) studied the effect of runner shapes and cross-sections to establish an optimum runner design that minimized turbulence and air entrapment by using transparent dies and water. A

sketch of the die, runner and gate section used by Smith and Wallace is shown in Figure 3.1. To study the effect of the ratio of runner width to thickness on the flow pattern, Smith and Wallace conducted the following tests. With an approximately constant runner area, and runner width to thickness ratios of 3:1 (0.6×0.2 in), 2:1 (0.5×0.25 in), and 1:1 (0.35×0.35) the flow patterns in the plate die were examined by Smith and Wallace.

Figure 3.2 shows the effect of these different runners and gates on the fluid flow in a plate die cavity with this constant cavity thickness, flow rate and time after entry of liquid into cavity. As can be seen in Figure 3.2, Smith and Wallace observed that the incoming stream spread to a greater extent when the gate approached a constant cross section (smaller width to thickness ratios). The numerical simulations of the filling patterns for the same gate velocities and runner, gate and cavity geometries, at approximately similar instants of time is shown in Figure 3.3. The flow patterns appear to match very well with the experimental patterns, and exhibit the qualitative trends of the experimental flow patterns. As Smith and Wallace surmised, the larger gate results in less distortion of the flow pattern because of the lower inertia of the stream. Similarly for different experiments at other thicknesses and velocities Smith and Wallace reported that, for the same flow rate, the jet or inertial effect was much less with thicker gates.

The filling sequence of the same vertical die cavity filled from the bottom through a 0.50×0.021 gate with a typical die casting velocity of 126 ft/sec is shown in Figure 3.4. The fluid stream as it enters the cavity flows through the cavity without much change in its shape or direction until it impinges upon an opposite wall. The stream which impinges on the wall opposite the gate then splits into two parts (forerunners). The inertia of the jet further causes the forerunners to follow the cavity perimeter on each side back to the gate, where they meet the incoming jet thereby causing vortex formation and resultant porosity. This typical flow pattern first suggested by Frommer (Frommer, 1933) is now widely accepted as the actual mode of

filling in die castings. This inertia or momentum dominated flow is what distinguishes die casting from slow velocity sand casting or viscosity dominated injection molding. In a metal casting this would result in a sealing of the vents and consequent entrapment of air in the casting.

A typical flow pattern obtained from numerical simulation for similar instants of time (Figure 3.5) shows a good correlation with the experimental fill patterns shown in Figure 3.4. The importance of modeling inertia is clearly seen in this numerical simulation. On the other hand viscosity dominated flow assumptions commonly used in the simulation of sand casting or injection molding would have resulted in a prediction of a continuous flow front from the gate to the opposite wall. This would have led to predictions of porosity on the wall opposite the gate unlike what is actually seen in the experiments and in the numerical simulations.

3.5.2.2 Effect of diverging gates (fanning)

Smith and Wallace (Smith & Wallace, 1963) further examined the effect of increasing the width of the gate at an angle from the runner (fanning the runner), while maintaining a constant gate area. The results obtained for gates with a 30 degree angle of fanning are shown in Figures 3.6 a and b. The results of the numerical simulation are shown in Figure 3.7. These figures show a greater distortion with the narrower gates, thereby indicating that wider gates will result in a more continuous flow pattern and reduced air entrapment. Thus when cavities in vertical dies are gated from the bottom in this manner a solid front fill of the cavity can be obtained. In addition to the lower turbulence, this type of filling facilitates the venting of the cavity from sections remote from the gate.

Narrow gates which require large velocities to prevent premature solidification cause jetting flow and resultant air entrapment and turbulence. In addition the vents can be sealed by jetting the liquid into

the cavity and causing flow around the outer die cavity surfaces. As can be seen in the numerical and experimental results shown here, reduced jetting into the die cavity and turbulence in the flowing liquid results from lower gate velocity during die fill. The necessary fill time to prevent premature solidification can then be obtained by using fanned gates of larger cross section.

Figure 3.1 Gating design for vertical injection used by Smith and Wallace (Smith & Wallace, 1963) for Water analogy studies.

(a) RUNNER : 0.35 x 0.35 INCH
GATE : 0.35 x 0.05 INCH

(b) RUNNER : 0.5 x 0.25 INCH
GATE : 0.5 x 0.05 INCH

(c) RUNNER : 0.6 x 0.2 INCH
GATE : 0.6 x 0.05 INCH

Figure 3.2 Effect of runner design on cavity flow for a flow rate of 0.026 cu ft/sec and time interval of 0.031 sec after entrance into cavity for the plate die of Figure 3.1. From Smith and Wallace (Smith & Wallace, 1963).

Figure 3.3. Numerical simulations using the FLOW-3D based model for the same geometries and gate velocities of Figure 3.2

Figure 3.4. Cavity fill sequence with a gate velocity of 126 ft/sec and a 0.5 x 0.021 gate for the plate die of Figure 3.1 (Smith & Wallace, 1963).

Figure 3.5. Fill sequence obtained using numerical simulations for the same gate geometry and gate velocity shown in Figure 3.4.

Figure 3.6. Flow patterns for gates of 30 degree half angle divergence for the plate die of Figure 3.1. From Smith and Wallace (Smith & Wallace, 1963).

Figure 3.7 Flow patterns obtained from numerical simulations for the same geometries and gate velocities of Figure 3.6.

3.5.2.3 Filling of Cavities with Cores

To produce this effect of solid front fill in other geometries not gated across the entire cross section requires a means of reducing the inertia of the incoming stream. One means of decreasing inertia suggested by Smith and Wallace was to impact the jet on a die or core surface. The effect exerted on the flow pattern in the die cavity when the entering fluid impinges on cores was thus investigated under several conditions of jetting and solid front fill, by Stuhrke and Wallace. This was done by placing square and round cores of various sizes in the plastic cavity shown in Figure 3.1.

The effect of impingement from a 0.5 in wide, 0.125 in. thick gate at a fluid velocity of 60 ft/sec on a one inch diameter core was observed by Stuhrke and Wallace and is shown in Figure 3.8. As can be seen the jet separates into two streams after impact. These streams are deflected to the side walls of the die cavity and form side runners that flow around the periphery of the die cavity. The side runners meet at the top of the cavity and combine to form a third stream. The resulting stream then flows downwards to the core forming vortices. A similar filling pattern is obtained by numerical simulations using FLOW-3D is shown in Figure 3.9.

The appearance of flow in the die cavity resulting from a jet impingement (gate width of 0.5 in) on 0.250, 0.50, 1.0 and 2.0 inch diameter cores as obtained by Stuhrke and Wallace is shown in Figure 3.10. As can be seen in each case the jet stream is separated into two side runners. Similar filling patterns are obtained for these core diameters by numerical simulation as can be seen in Figure 3.10. As seen in Figures 10 and 11, the angle between the separated streams and the initial jet is greater for the larger diameter cores. Thus the impingement of a jet stream on a core

splits this stream into two jets and causes severe turbulence and air entrapment.

The flow patterns in the die cavity resulting from a solid front flow from a gate the full width of the rectangular cavity around circular cores of 0.250 to 2.0 inch is are shown in Figure 3.12. The results of the numerical simulation for this case of solid front fill of cavities with cores of different diameters is shown in Figure 3.13. The experimental and numerical simulation show that in all cases the fluid separates leaving an air pocket behind the core even for the small gate velocity (10 ft/s) employed by Stuhrke and Wallace. Thus reducing the stream inertia by fanning the gate does not lower the likelihood of air entrapment as an air pocket is always left behind even for small gate velocities.

When a jet impinges on a core surface, the pressure on the surface is perpendicular to the surface at every point for an ideal fluid. At the center of the stream in contact with the core surface, the pressure is a maximum and the velocity is a minimum. As the distance from the center of the area of contact increases, the velocity of the fluid in the stream increases and the pressure decreases.

Eventually the pressure of the stream becomes so low that it fails to follow the surface of the round cores and separates. This creates a void of unfilled area behind the core that can lead to entrapped air. This void was observed to occur by Stuhrke and Wallace with round cores and rectangular cores during jet fill, and also with round cores during solid front fill. Thus cores placed in front of gates will result in the possibility of entrapped air behind the gate for any kind of gate or gate velocity because of the separation of fluid streams. Thus using cores to reduce metal inertia is not a very good idea as it will still create problems of air entrapment.

Figure 3.8. Flow sequence after stream impingement on one in. dia core for a gate velocity of 60 ft/sec with a 0.5 x 0.125 in gate for the plate die shown in Figure 3.1 (Stuhrke and Wallace, 1965).

Figure 3.9 Flow patterns obtained from numerical simulations for the same gate velocities and cavity filling sequence of a cavity with a round core shown in Figure 3.8.

Figure 3.10. Flow sequence after stream impingement on a 2 x 1 inch rectangular core for a gate velocity of 60 ft/sec with a 0.5 x 0.125 in gate for the plate die shown in Figure 3.1 (Stuhrke and Wallace, 1965).

Figure 3.11 Flow patterns obtained from numerical simulations for the same gate velocities and cavity filling sequence of a cavity with a rectangular core shown in Figure 3.10.

Figure 3.12. Flow sequence after stream impingement on a 1.0 in, dia core for a gate velocity of 10 ft/sec, with a 4.0×0.125 in gate (continuous fill) for the plate die shown in Figure 3.1 (Stuhrke & Wallace, 1965).

Figure 3.13 Flow patterns obtained from numerical simulations for the same gate velocities and instants of time, for the cavity with a round core of diameter 1.0 in., shown in Figure 3.12.

3.5.2.4 Horizontal or Side Injection

Another common mode of filling of a die cavity is by horizontal injection from the side of a cavity. Smith and Wallace showed a sequence of horizontal or side injection into the vertical plate die cavity for a gate velocity of 35 ft / sec is shown in Figure 3.14. The corresponding time sequence of filling from a FLOW-3D simulation is shown in Figure 3.15. As can be seen in both figures, the stream strikes the opposite wall, flows upward against the force of gravity, strikes the upper corner, flows along the top edge, and finally flows down to meet the incoming stream at the gate. A large vortex formation is thus seen in the numerical and experimental simulations at nearly the same locations, resulting in air entrapment.

Smith and Wallace also considered impinging streams as a possible means of reducing inertia leading to a continuous flow front. Figures 16 and 17 show the results of water analogy studies at low (21 ft/sec) and high velocities (52 ft/sec) respectively. Flow patterns for the same velocities obtained from numerical simulations are shown in Figures 18 and 19 and appear to show a good match. Higher flow velocities appear to provide a wider flow pattern and less trapped air. The importance of flow solvers capable of handling inertia dominated flow is also clearly seen in these side injection experiments.

Figure 3.14 Cavity fill sequence for horizontal injection with a gate velocity of 57 ft/sec and a 0.25 x 0.125 in gate for the plate die of Figure 3.1. [From Smith and Wallace (Smith & Wallace, 1963)]

Figure 3.15 Fill sequence obtained using numerical simulations for the same gate geometry and gate velocity shown in Figure 3.8, at approximately similar instants of time.

Figure 3.16 Flow pattern for impinging jets for a gate velocity of 21 ft/sec for the plate die of Figure 3.1. [From Smith and Wallace (1963)]

Figure 3.17 Flow pattern for impinging jets for a gate velocity of 52 ft/sec for the plate die of Figure 3.1. [From Smith and Wallace (1963)]

Figure 3.18 Impinging jet fill sequence obtained using numerical simulations for the same gate geometry and gate velocity (21 ft./sec) shown in Figure 3.10, at approximately similar instants of time.

Figure 3.19 Impinging jet fill sequence obtained using numerical simulations for the same gate geometry and gate velocity (52 ft/sec) shown in Figure 3.11, at approximately similar instants of time.

3.5.3 TANGENTIAL RUNNERS

Both parallel and tapered tangential runners are widely used by die casters. The trend is however toward the use of tapered runners, which have the advantage of reducing the weight of the runner. Booth and Allsop (Booth & Allsop, 1983) studied the effects of various designs of tangential runners (parallel and tapered) and cavities on the filling pattern in die cavities using transparent dies and water. The details of the flat plate cavity and runners used by Booth and Allsop is shown in Figure 3.20. The cross-sectional area of the tapered area starts at 70 mm^2 , which is the same cross sectional area as the parallel runner and diminishes to 2.3 mm^2 at the end.

The progression of the filling front across the cavity when using the parallel runner of Figure 3.20 (gate area is the same as runner area) as obtained by Booth and Allsop is shown in Figure 3.21. The numerically obtained filling front progression obtained from FLOW-3D is shown in Figure 3.22. As can be seen from the numerical and experimental results, when the fluid enters the cavity, the flow is almost parallel to the runner. The flow angle between the gate and the direction of flow is increased as soon as the runner is full and the flow is from the entire gate length. When the water reaches the overflow gates, it is deflected along the overflow side of the cavity towards the bottom edge, where it is deflected back towards the gate, thus forming a vortex in the bottom half of the cavity. As can be seen, the numerically obtained filling front progression obtained from FLOW-3D appears to be a reasonable match for the experimental results and also appears to predict the location of vortex formation. As can be seen, this is not an ideal filling pattern, as in an actual casting there will be a lot of entrapped air in the region of vortex formation. This is not an ideal filling pattern, as in an actual casting, there would be a lot of entrapped air, in that region of vortex formation which would result in gas porosity.

Figure 3.20 Gating design for tangential injection used by Booth and Allsop (Booth & Allsop, 1983), for Water analogy studies.

Figure 3.21 Filling pattern obtained by Booth and Allsop (Booth & Allsop, 1983), using a parallel runner as shown in Figure 3.20, with a 0.50 mm thick gate.

Figure 3.22 Filling pattern obtained from numerical simulations for the same geometry as the parallel runner filling sequence shown in Figure 3.21.

Figure 3.23 Filling pattern obtained by Booth and Allsop (Booth & Allsop, 1983), using a tapered runner as shown in Figure 3.20, with a 0.50 mm thick gate

Figure 3.24 Filling pattern obtained by Booth and Allsop (Booth & Allsop, 1983), using a tapered runner as shown in Figure 3.20, with a 0.25 mm thick gate.

Figure 3.25 Filling pattern obtained from numerical simulations for the same geometry as the tapered runner (gate thickness is 0.50 mm) filling sequence shown in Figure 3.23.

Figure 3.26 Filling pattern obtained from numerical simulations for the same geometry as the tapered runner (gate thickness is 0.25 mm) filling sequence shown in Figure 3.24.

The filling pattern obtained by Booth and Allsop using a tapered runner (the gate area was the same as runner area) with a 0.50 mm thick gate is shown in Figure 3.23. Numerical simulations for this tapered runner show a good correlation with the experimental results as can be seen in Figure 3.25. Comparing these filling patterns with those obtained when using the parallel runner, it can be seen that the results are very similar. In the case of the parallel runner, the ratio of runner area to remaining gate area increases as the fluid progresses along the length of runner, which results in the water leaving the gate at an increasing angle. The other difference between the two filling patterns is that more flow emerges from the gate during filling of the tapered runner, due to the increase in pressure as the flow area reduces.

3.5.3.1 Effect of tapered gate thickness

To demonstrate the effect of gate area to runner area ratio on the flow pattern within the die, Booth and Allsop used a gate of 0.25 mm thickness to give a gate area half that of the cross sectional area of the runner, which was tapered. A slightly different fill pattern was observed by Booth and Allsop as can be seen in Figure 3.24. Reducing the gate area to half the runner was found to increase the flow angle to about 60° compared with 45 obtained when the gate area equaled the runner area as can be seen in Figure 3.24. A similar pattern was observed in the numerical simulations which are shown in Figure 3.26. This fill pattern appears to reduce the amount of entrapped air in the cavity. This fill pattern appears to reduce the amount of entrapped air in the cavity.

3.5.3.2 Effect of a shortened runner and gate

It is by no means a standard practice to gate the full length of the casting, for many foundries gate only part way along the edge. Tests were carried out by Booth and Allsopp using a tangential runner one third the length of the cavity (Figure 3.27). The area of the runner was three times the gate

area. The effect of a shortened tangential runner and gate on the flow pattern, for a tapered and parallel runner are shown in Figures 3.28 and 3.29 respectively. Numerically obtained flow progressions for the short parallel and tapered runners are shown in Figures 3.30 and 3.31 respectively, and appear to show a good correlation with water analogy results (Figures 3.28 and 3.29 respectively).

The main difference in the flow pattern due to the shortening of the runner is the flow direction, which is almost perpendicular to the gate with the parallel runner. This is because the ratio of runner area to gate area increases along the runner and becomes very large near the end of the runner. The large vortex created with both the short tapered and parallel runner would cause considerable gas entrapment in an actual casting as seen in Figures 3.28-3.31.

Figure 3.27 Gating design for short tangential runner injection used by Booth and Allsop (Booth & Allsop, 1983), for Water analogy studies.

Figure 3.28 Filling pattern obtained by Booth and Allsop (Booth & Allsop, 1983), using a short parallel runner as shown in Figure 3.27, with a 0.50 mm thick gate.

Figure 3.29 Filling pattern obtained by Booth and Allsop (Booth & Allsop, 1983), using a short tapered runner as shown in Figure 3.27, with a 0.5 mm thick gate.

Figure 3.30 Filling pattern obtained from the numerical model for the same geometry as the short parallel runner filling sequence shown in Figure 3.28.

Figure 3.31 Filling pattern obtained from the numerical model for the same geometry as the short tapered runner) filling sequence shown in Figure 3.29.

Finally the effect of an obstruction to flow on the filling pattern with tangential runners, was studied by using a square cutout as a core (Figure 3.32). The flow pattern obtained by Booth and Allsop is shown in Figure 3.33, and the flow pattern from the numerical simulations is shown in Figure 3.34. As can be seen the numerical model appears to simulate this complicated filling pattern very well.

Figure 3.32 Gating design for tangential runner injection in a cavity with a square cutout used by Booth and Allsop (Booth & Allsop, 1983) for water analogy studies.

Figure 3.33 Filling pattern obtained by Booth and Allsop (Booth & Allsop, 1983) in a cavity with a square cutout using a tapered runner as shown in Figure 3.32.

Figure 3.34 Filling pattern obtained from the numerical model for the same geometry as the cavity with a square cutout, filling sequence shown in Figure 3.33.

3.6 SUMMARY

With the goal of increasing the life of the die casting die , one of the objectives of this investigation was to develop a computer program that accurately predicts the flow and solidification during the injection and solidification stages of the die casting process, and to integrate the numerical model with empirical wear models in order to predict the location and amount of die wear and failure.

Towards developing this die casting process simulation model, available numerical schemes for mold filling and solidification were evaluated in order to choose and modify software to improve the process of die design in the die casting. After this initial evaluation a model based on FLOW-3D, a three dimensional finite difference software was chosen for this study. A numerical model based on FLOW-3D which has traditionally been used for space applications was then developed, to simulate the transient, inertia dominated and complex cavity filling in high pressure die castings. In order to determine the accuracy of this approach in modeling the complicated flow conditions in die casting, a comparison with experimental filling patterns obtained from water analogy results in the literature was conducted and selected results are presented in this Chapter. The comparison of the mold filling patterns from water analogy studies with the results of the numerical simulations, shows that the numerical model appears to simulate the inertia dominated mold filling of die castings very well.

A study of the water analogy filling patterns in the literature, and that which is shown in this chapter clearly indicates that fluid inertia is very critical in the filling of die casting dies. It has been suggested that jetting flow in die casting is undesirable as it may result in air entrapment and die erosion. While the recommended gating practice is for fanned or tangential gates with lower metal inertia, jetting flow is quite common due

to other limitations caused by the complexity of the die cast part and other considerations. Thus software which is capable of modeling this complex jetting phenomena is required for modeling the mold filling during the die wear prediction process.

CHAPTER IV

EXPERIMENTAL APPROACH FOR EROSIVE WEAR MODEL DEVELOPMENT

4.1 MOTIVATION

The Literature survey in Chapter II described a number of erosive wear models developed from various simulative tests. The erosive wear models developed from these erosive wear tests give an indication of the relative importance of various process parameters on erosive wear. However these models are not directly applicable to die erosion in the die casting operation, as they have been developed using simulative tests (such as single impact or rotating arm tests), and do not consider diffusion/corrosion effects, temperature effects, material interactions and the partially solidified state of the eroding metal. Typically these tests are isothermal, and consider no effect of corrosion and material interactions between aluminum and die steel, which are believed to be a significant cause of die erosion. Corrosion dip tests are non-isothermal and definitely consider diffusion and other material interactions. Unfortunately these tests are also laboratory simulative tests, which do not consider the physical effects of high velocity molten metal, which appears to be the significant cause of die erosion.

Therefore to adequately evaluate the erosive wear of a die casting die, a proper testing procedure needed to be developed, which would consider material interactions (like the dip tests, which evaluate diffusive or corrosive wear), physical interactions (like the droplet impact studies

described), and the combination of both effects that occur in erosion of die casting dies. Ideally the test should be designed for a production scale die in an actual die casting environment.

A simple approach to this problem would be to evaluate erosion in actual production dies under different conditions. The difficulty with this approach is that conditions in production dies cannot be controlled effectively over the long time frames associated with die erosion. Effective campaigns under difficult casting conditions required for parametric evaluations may also not be possible, because of the constraint of producing near-perfect castings. Thus a systematic study over the entire range of process parameters required for effective study, that also ensures certain repeatable conditions and allows variations in only selected parameters is practically impossible. Moreover due to system complexity and variability, parametric evaluation of important factors affecting wear by experiments on production scale equipment would require a large number of expensive trials.

Thus a simple inexpensive laboratory test procedure was designed to closely reflect real production conditions, while allowing sufficient flexibility for parametric evaluations as well as accurate and quick wear measurements. Since sufficiently measurable erosive wear would typically require a large number of shots, an accelerated procedure was developed to obtain meaningful erosion results in as short a time frame as possible. A 250 ton Buhler commercial die casting machine was used in these experiments, and the experimental procedure for accelerated wear is explained below.

4.2 ACCELERATED EROSIVE WEAR TEST PROCEDURE

Industrial experience reported in the past has indicated that core pins or die inserts exposed to liquid metal attack in front of gates exhibit the highest level of erosive wear (washout) and soldering. Consequently, the

wear of core pin surfaces was chosen to represent accelerated wear of the die surface exposed to high flow velocities.

In order to evaluate different materials or surface coatings and to evaluate the effect of process parameters, "a multi-pin flat plate die" with six test pins was designed and fabricated for these experiments as shown in Figure 4.1. Only the top edge of the pin shown in Figure 4.1b is within the die cavity and is exposed to the molten metal. The rest of the pin is required for die assembly. The wear of the front 4 pins shown in Figure 4.1, were used as a surrogate measure of die erosive wear. The back pins were used as control pins to measure the amount of corrosive wear. One of the initial assumptions of this design is that the wear of all four front row pins is identical because the conditions at each of these locations are identical. This assumption was verified as will be discussed.

One of the reasons for choosing the multi-pin design was the ease of assembly, disassembly and measurement. In addition, a multi-pin design allows the testing of several pins simultaneously thus providing multiple test sites for comparative evaluations. The flat cavity (plate) design also allows for the study of the effect of filling and solidification on die wear because of its geometrical simplicity. In addition the test pins can be rotated or their shape changed, permitting the study of the effect of the angle of attack and surface geometry on wear. Each test batch contained one control pin (back pin) to check the repeatability of each campaign.

Wear being a gradual loss of material, tests could be very long term (> 100,000 shots) and costly. In order to accelerate the wear effects and to allow for quantifiable erosive wear in a reasonable number of shots, an operating procedure was developed to provide an extreme environment on the test pin so that quantifiable wear loss can occur in a reasonable number of shots. One of the operating conditions chosen to accelerate the erosive wear rate was the use of the pyramidal test pin design with sharp corners (Figure 4.1). These sharp pin corners should easily erode, solder

and exhibit heat checking along the edge within a reasonable time-frame. An additional advantage of this design, is that the wear of a straight edge is comparatively easy to measure.

A second approach to accelerate the wear was the use of a high gate velocity. This was accomplished by employing a chisel gate with a very small gate area. Therefore the plunger velocity exerted by the machine (acting on the area of the shot sleeve) is transmitted to a very high velocity at the gate because of the much smaller gate area. Gate velocities of 70 m/s were possible with this combination of gate design and machine capabilities.

Gate velocities greater than this were possible but resulted in severe operating problems. Chief among the operating problems was the dramatically increased gate soldering (the high velocities and small areas at the gate resulted in high localized temperatures) which resulted in machine shutdown after every 30 - 40 parts for soldering clean-up. This prevented a continuous test run, thereby resulting in the likelihood of suspect data and was therefore abandoned.

After designing an accelerated wear test procedure, it was important to conduct pilot tests to verify this experimental design (Shivpuri et al., 1991; Chu & Shivpuri, 1993a, 1993b). A brief synopsis of the pilot tests is described in section 4.3.

4.3 PILOT TEST VERIFICATION

Before using the multi-pin test die and test pins to evaluate the effect of process parameters on erosive wear, it was important to verify the validity of die design and test procedure for conducting the erosive wear test. The experimental design program selected for this was conducted as the "pilot stage" (Shivpuri et al., 1991; Chu & Shivpuri, 1993a, 1993b). The objectives of this pilot stage tests were to:

- Test the validity of the die design
- Check the assumption of identical conditions at pin locations
- Estimate the minimum number of shots for quantifiable amount of wear

4.3.1 Machine Operating Conditions and Performance Measurements

The following operating conditions were controlled during the experimental "pilot test" runs:

Furnace temperature:	704 °C
Fast shot velocity:	126 cm/s
Slow shot velocity:	25.4 cm/s
Accumulator pressure:	6.895 Mpa
Lubrication spray time:	4 seconds
Die hold time	8-10 seconds
Cycle time:	35 seconds
Pin material:	H-13 pin, hardness 47 Hrc (all pins identical)

4.3.2 Results of the pilot test

The weight loss and edge profile of the H-13 (without coating) pins was the primary measure for erosive wear in this experiment. The pilot test results are plotted in Figures 4.2 and 4.3. Figure 4.2 shows the variation of the weight loss of the four front row pins with the number of shots, while Figure 4.3 shows the edge profile of an eroded edge of each pin (Shivpuri et al., 1991; Chu & Shivpuri, 1993a, 1993b) after the pilot tests. Based on these results, the following conclusions were made:

- The front row pins show an approximately equal weight loss rate and similar surface wear profiles. The weight loss in pin location 2 is slightly larger than the other front row pins. This variance may originate from a few sources such as the variability in weight loss measurements or in the machining of the pins.
- The rear-row pins do not have as large a weight loss and surface wear as the front-row pins. This is because the velocity impingement on the surface of the pin in the rear row is far less than the front row.
- To verify that identical conditions are seen in each front row pin, thermocouples were placed inside each pin, just below the surface and temperature measurements were recorded for each cycle. A sample temperature reading after steady state conditions have been reached is shown in Figure 4.4 (Shivpuri et al., 1995). As can be seen the temperature at each pin location is nearly identical, showing that nearly identical conditions prevail at each pin.
- The number of shots needed for the experiments is expected to be larger than 600. From statistical analysis (Shivpuri et al., 1991; Chu & Shivpuri, 1993a, 1993b) it was determined that after 300 shots the weight changes were significant.

Thus, the main conclusion of the pilot tests were that identical conditions prevailed at each pin location, which resulted in identical wear for identical pins. Further details of the pilot test can be found elsewhere (Shivpuri et al., 1991; Chu & Shivpuri, 1993a, 1993b; Shivpuri et al., 1995). These are not reported here for brevity.

(a) Test Die

(b) Test Pin

Figure 4.1 Dimensioned drawing of the multiple pin die and pin. Units are in cm

Figure 4.2 Weight Loss vs. number of shots for four identical front row pins used in the pilot tests analysis (Shivpuri et al., 1991; Chu & Shivpuri, 1993a, 1993b).

Figure 4.3 Edge profiles of edge in front of gate of four identical front row pins used in the pilot test (Chu & Shivpuri 1993a, 1993b).

Figure 4.4 Temperature measurements at identical locations near surface of identical front row pins, after steady state conditions have been reached.

4.4 QUANTIFICATION OF EROSIVE WEAR

As briefly discussed in the pilot tests, two major performance measurements were selected for wear quantification. The first measure was the overall weight loss of the pins. The pins were measured prior to assembly of the die. After each campaign, the pins are removed. At this point there is likely to be a net increase in the weight of the pins as there is always some aluminum soldered to the pin. The soldered aluminum is removed by placing the pins in a dilute potassium hydroxide solution. The potassium hydroxide dissolves the aluminum but leaves the die steel alone. Two days in the solution usually removes almost all the aluminum. The pins were sometimes kept longer if there was excessive soldering, or if it appeared that all the aluminum had not been removed.

After the soldered aluminum was removed, the pins were dehydrated in an oven to remove all traces of moisture, prior to being measured. The pins were weighed on a calibrated digital weighing balance with an accuracy of 0.1 mg. The pins were measured thrice at each measurement stage, and the mean value was reported.

A second measure of erosive wear was the measured profile of the eroded surface. The profile was measured using a profilometer with an accuracy of 2×10^{-3} mm. The pins were clamped rigidly to a vee block, as a probe of the profilometer traversed along the surface. Since every pin edge or face had a draft, the vee blocks were placed on a sine plate with gage blocks to ensure a horizontal surface for the pin edge or face being measured. Thus the measuring probe moves horizontally across a horizontal pin surface. A slope adjustment of the profilometer is further used to remove any residual slope of the pin surface from the measurements. In some cases the edge of pins was measured, while in others the face of a pin was measured depending on the test set up as will be discussed. A sample of the uneroded pin edge surface profile measurement is shown in Figure 4.5. As can be seen there is very little variation in the wear profile of the machined edge. This wear profile is used later for comparison with the eroded pin surface. The location of the top of the pin shown in Figure 4.5 is a reference location on the pin as seen in Figure 4.7

The eroded wear profile appears to be a better measure of erosive wear, as the eroded surface profile represents most of the effect of erosive action of molten aluminum. The weight loss on the other hand is an averaged effect over the entire weight of the pin, only a small segment of which is exposed to the eroding liquid. The wear profile also provides a spatial distribution of the eroded effects, while the weight loss is a single numerical value, which has no spatial component. This allows the wear profile to be compared with spatial values of process parameters, unlike the weight loss which is a single scalar value that does not allow such comparisons.

Figure 4.5 Measured edge profile of uneroded pin edge, prior to use in the erosive wear experimental die.

4.5 EVALUATION OF THE EFFECT OF PROCESS PARAMETERS

Having verified the experimental design, an experimental plan had to be designed to evaluate the effect of process parameters on die erosive wear. An explanation of the reasoning behind coming up with an experimental plan is provided below.

4.5.1 Important parameters to be measured

As seen in the literature survey described in Chapter II, the following parameters are likely to play a significant role in erosion of die casting

- Metal velocity at impact
- Angle of metal impact
- Metal temperature
- Die hardness and heat treatment
- Die temperature
- Molten metal composition
- Time (Number of shots)

Summarizing the effects of all these parameters, the erosive wear rates can be written in the following functional form

$$E = F(t, V, \theta, T_m, H, T_d, A, G) \quad 4.1$$

where E is the generalized erosion rate, t is the time, V is the velocity, θ is the angle of impact, T_m is the metal temperature, H is the die hardness, T_d is the die temperature, A is the alloy composition and G represents the geometry effects.

Not all these parameters can be easily varied to conduct parametric evaluations. The procedure used to vary these parameters, and the difficulty in varying some of these parameters for the parametric study is described below.

4.5.1.1 Parametric variation of die temperature

The die temperature is obviously of great significance in erosive wear as hardness is likely to decrease, and corrosive diffusion rates are likely to increase with increasing die temperature. However die temperature is a parameter, which is very difficult to vary significantly, independent of other parameters. There are essentially two ways to change the die temperature

- Change the amount and time of water flowing through two water lines in either die half. However the likely change in the temperature of the pins, which are located (1.5 inch height, and 1 inch depth) far from either water line is not likely to be significant.
- Change the spray conditions, in terms of time of spray and location of spray. The spray serves as both a coolant, and as a release agent to remove the part during ejection. Spraying is essential to the smooth running of the dies, as any reduction in spraying amounts causes stuck castings which hinder smooth steady state operation. This will consequently result in less controlled conditions for the experiment.

Thus the parametric evaluation of die temperature is very difficult (both with this design and any other design) as the spray on/off and the water on/off conditions required to obtain a significant temperature variation are very difficult, maybe even impossible due to logistical production reasons. It is also not clear if turning on or off the water lines and sprays will cause a significant die temperature variation at the pin locations, to allow parametric evaluation of this variable. One approach to allow parametric evaluation of die temperature is the use of oil lines, with a temperature controller controlling the oil temperature. The design of the die and oil line should be altered to allow it to flow as close to the eroded

pin region as possible. Since core pins should ideally not intersect with water/oil lines, this condition may be difficult to meet even in such a case.

Thus die temperature was not a direct variable considered in this parametric study. An easier method of evaluating die temperature effects will be the corrosion dip tests, which are more easily capable of evaluating these effects. This is because, these tests are designed for such studies that evaluate diffusion variations due to metal and die temperature variations.

4.5.1.2 Parametric variation of impact velocity

The impact velocity acting on the pins can be varied, by varying the velocity at the gate. As will be shown in the next chapter, the jetting flow that takes place because of high gate velocities, will result in the impact velocity being the same as the gate velocity at certain locations on the pin. Thus a change in the gate velocity, produces an equivalent change in the impact velocity.

The gate velocity is varied by changing the first phase and the second phase valve open positions, in the accumulator that controls the shot tip movement (Figure 4.6). The first phase (slow shot) valve position controls the velocity in the slow injection phase (V_c in Figure 4.6) where the metal is slowly moved upto the end of the sleeve. In the second phase (plunger position P_2 to P_3), the metal is accelerated to rapidly fill the die cavity, typically in milliseconds (V_s in Figure 4.6). This is also known as the fast shot phase. The third phase is the intensification stage where pressure is applied as the metal is solidified, to compress any shrinkage pores. The first phase valve position was typically fixed at 60 % and the second phase valve position was varied to change the plunger velocity and consequently the gate velocity. To achieve very high gate velocities, the first phase was reduced in a couple of cases to 55%. This results in a decrease in the length of the slow shot stage (P_1 in Figure 4.6 has a lower value) and consequently increases the length ($P_3 - P_2$ in Figure 4.6) and time

of the fast shot stage. Thus the increase in the pressure applied in the second phase (fast shot) is applied over a greater length of plunger movement and thus can be converted into greater metal velocity. The intensification stage was maintained the same in all cases. The intensification stage may not have been very effective in these experiments because of the early freezing of the very small gate area.

A controller on the plunger rod tracks the position of the shot tip in the shot sleeve versus time, to maintain a constant gate velocity during every shot. The shot location system can maintain an accuracy of + or - 1.5 m/s gate velocity in most cases. However when there is extensive soldering of the tip, or extensive solidification of the metal in the sleeve (either because of low furnace temperature or because of extensive holding time in the sleeve because of machine problems), the plunger is incapable of maintaining high velocities. This is because metal in the sleeve now exerts a greater resistance to the applied pressure of the accumulator. When a gate velocity too far below the minimum is obtained, it is an indication of operating problems, which are typically fixed if the problem persists for more than 2 or 3 shots. Nevertheless when there are no machine problems, the velocity is maintained close to the optimal value for a large percentage of shots, and this value is reported in most cases, and exceptions are noted.

The minimum velocity used in the experiments was 27 m/s because very little erosion was seen after a 1,000 shot campaign at this velocity. Any lower velocity is consequently likely to result in negligible erosion, and therefore no tests were done at lower velocities. The highest velocity run was 63-65 m/s. Machine operation is extremely difficult at these high velocities as the die clamping is exceeded by the metal pressure, resulting in severe flashing. Also some gate soldering results after about 150 shots in each campaign because of localized heating at the gate due to the high gate velocities. Casting at 70 m/s was attempted, but abandoned due to the severe nature of the soldering and flashing problem. That prevented

smooth continuous campaigns which reduce the chances of obtaining meaningful results.

Since gate velocities in actual die casting operation rarely exceed 70 m/s, for reasons similar to those listed above, it is practical not to pursue campaigns at such high velocities. Consequently campaigns were conducted for velocities between 27 m/s and 63-65 m/s at increments of 6-7m/s. Smaller increments of velocity were not considered because the smallest increment of velocity that could be maintained without overlapping of velocities was 4.5 m/s (because within a mean nominal velocity, variations of + and -1.5 m/s were common and the minimum velocity increment was 1.5 m/s) and to obtain results in as small an experimental set as possible. Thus campaigns were conducted at 27 m/s, 35 m/s, 42 m/s, 50 m/s, 55 m/s and 63-65 m/s.

4.5.1.3 Parametric variation of angle of metal impact

In order to evaluate the effect of the angle of metal impact, two basic orientations of the test pins in the die were tested. Initially the orientation of the test pins chosen was as seen in Figure 4.1. In this design, the pin orientation is such that the molten metal from the gate first strikes the edge of the pin. This is clearly seen in Figure 4.7, which shows a schematic of this pin layout in the die cavity, and the pin geometry, indicating regions of the pin most prone to washout. As can be seen in the schematic shown in Figure 4.7b, the edge of the pin is most exposed to erosive conditions, which is likely to result in severe erosion along that pin edge. This pin arrangement will henceforth be called the "edge pin layout" and the pins in this orientation will be referred to as "edge pins".

To evaluate the effect of die geometrical features (specifically the effect of angle of attack of the molten metal) on die erosive wear a modified design and layout of pins was then chosen for use in the six pin erosive test die. A schematic of this pin layout is shown in Figure 4.8a, while the

orientation of the tested pins to the injection direction of molten aluminum is shown in Figure 4.8 b. As can be seen in Figure 4.8 unlike the edge pin layout, the face of the pin (instead of an edge) is directly in front of the molten metal from the gate. Thus the entire face is exposed to the severe erosive conditions, unlike the edge pin case, where most of the erosive conditions are seen by the edge. Such a layout is obtained by rotating the pins in the edge pin layout by 45 degrees. The front edge was however cut at different angles to the vertical (angle α in Figure 4.8b), in order to evaluate the effect of the angle of attack of the incoming molten metal jet on the erosive wear. Thus the angle at which the molten metal impinges on the pin is different for each pin, thereby providing a means to evaluate the effect of the angle of attack under similar experimental conditions. The front face angles for which experiments have been performed at present, are 89, 83, 75, 70 and 65 degrees. This pin layout and orientation will henceforth be referred to as the "angle of attack pin layout" and the pins in this orientation will be referred to as "angle pins". The width of the gate is smaller than the width of the face, which shall result in interesting flow patterns and wear results as will be discussed later.

It was proven in the pilot tests that all front row pin locations are under nearly identical wear conditions (identical pins face nearly identical conditions and have nearly identical wear rates). Numerical simulations (Chapter V) also show that the wear of each pin is likely caused by the impact of high velocity molten metal from the gate and is not significantly affected by secondary flow patterns from other pins, or by backflow.

V_1 = Initial plunger velocity just to get by the pour hole

V_c = Slow shot plunger velocity

V_s = Fast shot velocity required to achieve desired fill time of casting

P_1 = Plunger position when shot sleeve is full

P_2 = Plunger position when runner system is full

P_3 = Plunger position when the casting and overflows are full

Figure 4.6 Traditional stages of the cold chamber shot (Brevick, 1995).

Figure 4.7 Schematic of a cross section of the die geometry of the edge pin layout, indicating regions of the pin (Figure b) most prone to washout. The symmetry plane is used for numerical simulations.

Figure 4.8 Schematic of the orientation of test pins in the test die for the angle of attack layout. The location of pins with differing front edge angle α , in the die is shown in Figure a.

Therefore, it was assumed that the two pin orientations (edge and angle pin orientation) could be placed in any permutation or combination in the same campaign, because each pin location is assumed to be an independent test site. This resulted in a third layout, where edge pins and angle pins were both used in the same campaigns. The essential assumption of this pin layout was that wear conditions at each pin are influenced by flow conditions from the gate in front of each pin, and not by secondary flow from other pins. This layout will henceforth be referred to as the "mixed pin" layout. A verification of this assumption is provided in Chapter V.

Figure 4.9 Schematic of the orientation of test pins in the test die for the mixed layout. Different combination of edge and angle pins are used at pin locations a, b, c, d for different experiments.

4.5.1.4 Parametric variation of the effect of metal hardness and heat treatment

Previous studies in die casting (Chapter II) have indicated that the actual hardness may not be as critical for erosion resistance for a particular alloy system. For example Yu and Shivpuri (Yu & Shivpuri, 1993, 1994) have shown that as long as the H-13 was heat treated (quenching followed by tempering), the total hardness of the H-13 was not significant in corrosion resistance. Similar results were reported by Malm and Tidlund (Malm & Tidlund, 1979). Consequently minor variations in hardness caused by different quenching times are ignored in this experimental plan, as these minor variations are not likely to be significant over the 1,000 shot experimental campaign conducted. Large variations in heat treatments and surface treatments are however evaluated. A discussion of the results of this study in the following chapter will more closely examine the accuracy of this assumption. Three classes of pin treatments were therefore selected and are reported in this thesis. These are

- As received pins in the annealed state which are not heat treated (quenching and tempering cycles). They consequently have a very low hardness of 32 Hrc.
- Heat treated pins (heat treated per the recommended ADCI quenching and tempering cycles), which have a hardness of 47 Hrc. Since most commercial H-13 die steels follow approximately the same heat treatment and have approximately the same hardness and microstructure, results reported for these set of pins represent data for most commercial die steels used in industry.
- Surface Nitrided pins of very high surface hardness (62 Hrc). Nitriding has sometimes been considered a possible solution to erosion problems because of the high surface hardnesses obtained.

Consequently some nitrided pins were also used in the experimental plan to evaluate the effect of hardness.

The three sets of heat / surface treatments were thus chosen to represent the extremes of hardnesses of the die steel likely to be encountered in practice. Effect of small variations in hardness are not considered because a 1,000 shots may not be large enough for the effects of small variations in hardness to be significant. Constraints of time required for testing of additional hardness pins also makes the study of small hardness variations impractical.

4.5.1.5 Parametric variation of molten metal temperature

An open gas induction heated furnace, with a capacity of 400 pounds was used in these experiments to provide molten aluminum alloy. An automatic robotic ladling system with a ceramic ladle, was used to ladle metal from the metal furnace and pour the metal over into the pour hole of the shot sleeve. Pouring into the pour hole was controlled by a coated metallic funnel, which prevents dangerous molten metal spill. The molten metal temperature in the furnace is measured by a plunge thermocouple, and the temperature is controlled by a controller hooked to this plunge thermocouple.

The controller is however only an overshoot controller thereby resulting in overshoots of + and - 25 F. The residual gas heating causes the furnace temperature to shoot way above the nominal temperature when the temperature is rising, and the slow activation of the gas furnace causes the temperature to fall rapidly below the minimum when the temperature is falling. This is because the gas furnace is actuated only when the temperature falls below the desired nominal temperature and the gas furnace is shut down only when the temperature exceeds the nominal temperature. Because of the rapid heat loss from the open furnace, and the residual heating due to hot gases in the furnace that occurs even when

the gas for the furnace has been shut down, a severe overshoot occurs. However most open furnaces heated by a gas induction heating system will encounter such a problem. Large variations are avoided in industry by using a covered melting furnace and a small holding furnace which is easier to control.

However by manual control of the temperature the overshoot was reduced to + or - 15 F and in some cases to within + or - 10 F. This has been maintained for most campaigns, except where noted. This is done by moving the nominal temperature up and down to control the amount of overshoot in either direction. For example, when the nominal furnace temperature is 1250 F and the furnace temperature is 1258 F, the controller temperature setting is set to 1253 F (say). Therefore the furnace is activated when the temperature falls below 1253 F. Because of residual cooling the temperature may fall to 1242 F (say) before starting back up. By now setting the controller temperature at 1245 F (say), the furnace is shut down at this temperature, reducing the overshoot because of residual heating beyond 1250 F. A fairly consistent + or - 10 F (close to industry specs) was obtained after an initial learning curve. Any exceptions to this standard are noted in the results.

To avoid overlap of data points because of this temperature variation, furnace temperatures were varied in increments of 50 F, for different campaigns evaluating the effects of metal temperature. This ensures that the temperature at any point in each campaign, does not overlap with a temperature reading of a campaign with a different nominal temperature.

The lowest superheat used was 60 F. It is extremely difficult to cast at this superheat as cooling of metal during the ladling process causes solidification in the walls of the ladle itself, and in the pouring funnel and shot sleeve. Essentially a solidified layer forms on the ladle surface which keeps growing every cycle (as molten metal comes in contact with previously solidified metal in the ladle). This growing solidified layer

forms a barrier to metal pour from the ladle, and causes a variation in the location where metal is poured. Since the metal has to be poured into a very small funnel over the pour hole in the shot sleeve, the metal spills over the funnel every few cycles causing a shut down to correct this problem. Solidification over the funnel also causes a closing of the opening of the funnel over the pourhole, resulting in small volumes of poured metal and further problems, which result in periodic shutdown. Also the large solid fraction of the melt in the pour hole encourages sticking or soldering of the tip to the sleeve resulting in a complete machine shut down, as dies have to be disassembled when a plunger seizes.

The lowest temperature at which a campaign could be successfully run was a 60 F superheat and consequently this is the minimum superheat chosen for this study. Since the maximum superheat at which most die castings are run is around 200-250 F, 260 F was chosen as a maximum temperature for this study. Severe gate soldering problems are encountered at this temperature, although there were fewer problems during metal pour because no metal solidifies over the ladle.

Since variations of + or - 15 - 20 F were common in some campaigns temperature increments of 50 F were chosen between the minimum and maximum of 60 and 260 F superheat. This ensures that the temperature during any shot in a campaign at a particular superheat, always remains above any temperature reading at a lower superheat campaign and below any temperature reading at a higher superheat campaign. Thus the superheats chosen for this study are 60 F, 100 F, 150 F, 200 F and 260 F.

4.5.1.6 Parametric variation of geometry effects

Although different geometries in the form of circular pins or concave pin surfaces are not studied, the two pin arrangements chosen appear to represent two distinct manners of die erosion, as will be discussed in the

analysis of the results in chapter V. The angle of attack arrangement of pins appears to result in impingement or washout erosion commonly seen in die walls across from gates, while the edge pin arrangement appears to result in a "gate erosion" pattern (Chapter II). Study of other geometrical effects such as the effect of circular shapes, concave surface etc. should be an interesting follow-up to this study.

4.5.1.7 Parametric variation of alloy composition

Since alloy composition, probably has a significant contribution on die wear, particularly corrosive wear, the study of alloying effects is important in coming up with erosive wear models. Some effort in that direction was attempted in this research with three different alloying systems being used in different campaigns with identical pins. However the study is incomplete, and some of the results are confidential, and therefore the results will not be reported here.

4.5.1.8 Study of erosion variation with time

The number of shots is considered a surrogate measure of the time that the pin is exposed to erosive conditions. To study the time variation of erosion, pins were taken after each mini-campaign of 250 shots and measured for weight loss in some campaigns. The furnace capacity is around 300 shots, and so 250 shots represents a complete furnace load. Only 250 shots were cast in each mini-campaign as temperature control is extremely difficult if mini-campaigns greater than 250 shots were conducted. This is because of the large relative heat losses at the low metal levels that would result if more than 250 shots were conducted in each mini-campaign. Edge profiles were only taken once after 1,000 shots because of the time constraint of setting up profilometer measurements. Thus four data points are available for every campaign to study the effect of weight loss variation as a function of time or number of shots.

4.5.2 EXPERIMENTAL PLAN

The primary constraint in choosing an experimental plan for the experiments was the economic and time cost involved in conducting the experiments. As discussed the pilot tests indicated that 300 shots may be sufficient to evaluate erosive wear in the pins. It was however decided that campaigns of 1,000 shots would be conducted to reduce experimental and measurement errors and accentuate differences in erosion rates. Each campaign of 1,000 shots typically takes between 3 to 4 weeks to complete because of the set up, die loading/die unloading, and maintenance work involved in running a commercial die casting machine. Typically 2-3 people are required for safe machine operation. Hence, a main objective of the experimental plan was to obtain meaningful results in as few campaigns as possible.

As discussed, the following machine conditions can be varied in each campaign.

- The Furnace Temperature
- The Gate Velocity

It is desirable that experiments at as many different velocities and temperatures be conducted as possible. As discussed earlier, the minimum and maximum temperatures that were considered for logistical reasons was 60 F superheat and 260 F superheat respectively. Since temperatures can be controlled to only within + or - 15 F, increments of 50 F were used as data points in the experimental plan. Therefore the temperatures at which experiments were conducted are 60 F, 100 F, 150 F, 200 F, 260 F. As discussed previously the minimum gate velocity used was 27 m/s as negligible weight loss was obtained at that velocity and the maximum gate velocity used was 65 m/s because of machine problems beyond this velocity. Increments of 6-7 m/s were chosen for these

experiments as the velocity could be maintained at an accuracy of only + or - 1.5 m/s range. Thus the velocities used in the experimental plan are 27, 35, 42, 50, 55, and 63-65 m/s.

To have a complete factorial experimental design, all possible permutations of the velocity and temperature variable would have to be used in the experiments. Because of the limited number of experiments that could be conducted, a complete factorial design is possible only with a very small number of temperature and velocity variables. It was decided that the experimental plan should include as many data points for each variable as possible. This would however involve filling a particular row and column of the experimental matrix and leaving the rest of the experimental matrix empty. By using a single row or single column of the experimental matrix, effects of parameter interaction are not considered and the effects of a single parameter is studied while all other parameters (velocity or temperature) remain unchanged. More experiments will have to be done to complete the matrix and determine further coupling effects, but with a combination of numerical simulations to calculate different velocities and temperatures at different pin locations, this may not be necessary. Thus the effect of each variable alone is studied in greater detail, while study of parameter interactions may require more experiments.

In each campaign it is possible to use 4 pins as independent test sites. The conditions on all the pins have proven to be identical (section 4.3), and it appears that the flow around each pin is influenced only by the gate velocity and not by the flow around other pins (Chapter V). Thus it should be possible to use any combination of pin orientations in a campaign, without affecting the erosive behavior of an individual pin. Thus the erosion on a pin is independent of the orientation of other pins. In other words, it is assumed that any pin combination can be used as each pin location functions as an independent test site. Thus the campaigns to

study effect of velocity and temperature can also be used to study effect of angle of metal impact, hardness and geometry effects.

The following rules were therefore followed in coming up with an experimental plan

- The conventional heat treated (47 Hrc) edge pins were used in every campaign and served as a reference for all campaigns. All such pins are assigned a variable (b) for book keeping and are placed in one of the outside pin locations (Figure 4.9).
- A low hardness as received pin (32 Hrc), without any quenching and tempering heat treatment cycles was used for all velocity tests, to allow a comparison of the effect of hardness for the entire range of velocities tested. All such pins are assigned a variable (a) for book keeping and are placed in the other outside pin location.
- Low hardness pins and nitrided high hardness pins were used, in temperature tests to evaluate the effect of temperature and hardness on erosion. In any test only one of the low hardness or nitrided pins were used because other pins had to be used in these tests. Thus a regular hardness (pin b) and a low hardness or nitrided pin (pin a) was common in all temperature tests at the outside pin locations.
- Coated pins were used in some tests in the c & d slots in order to complete other studies on the effect of coatings. The coated pins used are indicated here in the experimental plan and some of the results are presented for completeness. A comparison study of the efficacy of coatings which include some results of these campaigns is reported elsewhere (Yu & Shivpuri, 1995).

- One campaign contained only angle of attack pins of angles 83, 76, 69, and 65 degrees to study the effect of angle of impact alone (Figure 4.8). This was an initial study to determine whether angle effects were significant enough for further study. Since this appeared to be the case from the preliminary experiments, this study was further extended across different campaigns as will be discussed below. This campaign was conducted at the reference velocity and temperature of 50 m/s and 150 F respectively.
- Angle of attack pins were also placed in six of the campaigns studied. Each of these campaigns contained one low hardness pin, one high hardness pins and two angle pins of ($\alpha = 89, 83, 76$, or 69 degrees). The angle pins were all non-heat treated (as received in the annealed state) with the same hardness as the low hardness pins. Thus the erosion of the low hardness edge pins was used as a control to quantify the erosion of different angle pins used in different campaigns as will be discussed in Chapter V. Six campaigns with two angle of attack pins, implies 12 pins. Since there are four angle pins used in the study, it was decided to use every combinations of two pins possible. Thus the six campaigns contained the following combination of pins (89, 83), (89, 76), (89, 69), (83, 76), (83, 69), (76, 69). Thus every combination of two pins is represented and exactly three data points are obtained for each pin. All these two pin combinations are placed in the central pin locations (c and d in Figure 4.9). It was hoped that such a combination of pins would reduce the error associated with comparisons across different campaigns.
- Six velocity campaigns were conducted with a constant metal superheat of 150 F at gate velocities of 27, 35, 42, 50, 55, and 65 m/s. Five temperature campaigns were conducted with a constant gate velocity of 50 m/s at metal superheats of 60 F, 100 F, 150 F, 200 F, and 260 F.

- Three different alloys were used in the campaigns to study the effect of alloy composition. However all the campaigns reported here use the same alloy (C91) provided by Alcoa, which has a near eutectic composition and has a liquidus temperature of 1100 F. So effects of alloy variation are not considered here.

The complete experimental plan is summarized in table 4.1.

Table 4.1 Summary of experimental campaign.

Campaign	Front Row Pins Used	Metal Superheat	Gate Velocity
i	HH, N, C, C	200 F	50 m/s
ii	HH, N, C, C	100 F	50 m/s
iii	83, 76, 69, 65	150 F	50 m/s
1	HH, LH, C, C	150 F	50 m/s
2	HH, N, C, C	60 F	50 m/s
3	HH, LH, 83, 69	150 F	27 m/s
4	HH, LH, 83, 76	150 F	42 m/s
5	HH, LH, 76, 69	150 F	55 m/s
6	HH, LH, 89, 76	260 F	50 m/s
7	HH, LH, 89, 69	150 F	65 m/s
8	HH, LH, 89, 83	150 F	35 m/s
a	HH, LH, S, S	150 F	50 m/s
b	HH, LH, S, S	150 F	50 m/s

* HH - High Hardness, LH - Low Hardness, N - Nitriding, C - Coated Pins, S - Soldering, 89, 83, 76, 69, 65 - Angle Pins

Campaigns i, ii, iii were preliminary campaigns to evaluate the importance of temperature and angle effects. Essentially these campaigns were conducted to see if velocity was the only significant parameter that affected die erosion. In other words "are temperature and angle effects significant enough to warrant further study?". Campaigns 1-8 were then conducted since the answer to this question was yes. In the preliminary campaigns (i-iii) weight loss and edge profiles were measured only once after a 1,000 shots, while in campaigns 1-8 (which were conducted in a sequence after campaigns i-iii), weight loss measurements were taken after each mini-campaign of approximately 250 shots (exceptions are noted) to evaluate the time variation of erosion.

4.6 DESCRIPTION OF EXPERIMENTAL CAMPAIGNS

A brief description of the experimental campaigns conducted are described here. All the problems, possible errors, and the accuracy of parameter control are detailed here. Brief discussions and analysis of weight loss and wear profile measurements taken during each campaign are also presented.

4.6.1 Campaigns i and ii

These campaigns are described together, because most of the conditions in both campaigns are identical, and they were performed in quick succession. These are temperature campaigns, with campaign i being conducted at a superheat of 200 F and campaign ii being conducted at a superheat of 100 F. In both campaigns, a nitrided H-13 pin (Hrc 62, pin a) and a high hardness H-13 pin (Hrc 47, pin b) were placed in the outer pin locations, while coated pins were placed in the central pin locations. The same combination of pins was used in campaign 2, which was also a temperature campaign. As in all velocity campaigns, a nominal gate velocity of 50 m/s was maintained within + or - 1.5 m/s. This was done by having a first phase valve position of 60 % (maintained in almost all

campaigns) and a second phase valve position at 28 %, (which is maintained in all temperature campaigns). As discussed the second phase valve position controls the gate velocity, and is thus varied to control gate velocity. Since all temperature campaigns are conducted at the same gate velocity, the second phase valve position was maintained a constant for all temperature campaigns.

There were few casting problems in campaign i where the temperature was maintained at the nominal 200 F superheat, within + or -15 - 20 F. The larger variation in temperature is because of the initial learning curve that was required to establish manual temperature control which accounted for the dynamic variations of metal level in the furnace. The high superheat however ensured that there were few casting problems in this campaign.

In campaign ii however (100 F superheat) there were a lot of casting problems, because of the low superheat. As will be discussed in the description of campaign 2 (section 4.6.4), low superheats cause solidified layers to form on the ladle, funnel and pour hole. This causes metal spillover and closing of the funnel and pourhole, and requires periodic clean up. This results in a variable amount of metal in the casting. In addition, the partially solidified metal in the sleeve exerts a greater resistance to the tip movement which periodically results in a gate velocity lower than the nominal velocity of 50 m/s. The partially solidified metal in the sleeve also resulted in shot tip soldering and scoring which eventually resulted in seizure in the middle of a campaign. A new sleeve and tip were inserted to complete the campaign. This was thus one of the more difficult casting campaigns. However the metal temperature was maintained to within + or - 10 F.

Since these were initial campaigns, weight loss measurements were not taken after every mini-campaign of 250 shots, but instead single weight loss and wear profile measurements were conducted after a 1,000 shots.

Two such campaigns were conducted for each condition to obtain weight loss measurements after 2,000 shots, but the results after 2,000 shots are not reported here because all other campaigns were conducted for only a 1,000 shots. Nevertheless the weight loss readings were qualitatively similar to the results after 1,000 shots indicating that 1,000 shots are sufficient for making comparative predictions.

The weight loss results after a 1,000 shots for campaigns 1 and 2 are shown in table 4.2. As can be seen the weight loss of the low temperature campaign was much more than the high temperature campaign for both the H-13 and Nitrided pins. This was a surprising result as conventional wisdom holds that a decrease in temperature should decrease wear because of lower diffusion rates (Malm & Tidlund, 1979; Yu & Shivpuri, 1993, 1994). This led to the further temperature campaigns (campaigns 1, 2, 6) which were conducted to evaluate temperature effects. A discussion of the entire temperature campaign, and reasons for this behavior is provided in Chapter V. Although it would appear that the weight loss is small, it must be remembered that only a small part of the pin is exposed to the erosive wear action of molten metal.

Table 4.2 Dependence of erosive wear rate of Nitrided and H-13 test pins on the temperature of molten aluminum alloy. The Weight loss of the pins is measured after 1000 shots.

Material	H-13 Weight loss (mg)	Nitriding Weight loss (mg)
Low Temperature (100 F Superheat)	51.9	65.8
High Temperature (200 F Superheat)	30.6	29.4

Figure 4.10 Dependence of edge wear profiles of High Hardness H-13 (a) and Nitrided edge pins (b) on the temperature of molten aluminum alloy from campaigns i and ii.

Three measurements of the wear profiles of the nitrided and H-13 pins were taken for both campaigns i and ii. The different measurements for each pin were all nearly identical, and so only the mean wear profile is reported here. The edge profiles after a 1,000 shots for the high hardness H-13 pins in campaigns i and ii is shown in Figure 4.10a. As can be seen, the wear profiles for both pins are fairly smooth, with erosion pits being observed at the location on the pins across from the gates (From 3-5 mm on the graph). The 100 F superheat wear profile (campaign ii) shows a greater depth of the erosion pit, while the 200 F superheat wear profile shows a much smaller erosion pit indicating that the erosive wear is lower at higher temperatures. However along the pin ends beyond the erosion pit, the 200 F superheat pins show greater wear, which indicates that these pins may suffer greater corrosive loss at the ends of the pins because of higher metal temperature. An explanation of this observation is provided in Chapter V.

The edge profile after a 1,000 shots for the nitrided pins in campaigns i and ii are shown in Figure 4.10b. As can be seen at 200 F superheat (campaign i) the wear profile is nearly uniform with very little weight loss, while at 100 F superheat (campaign ii) there is a fairly large erosion pit across from the gate. Both wear profile readings are fairly smooth without many local peaks and valleys. This again indicates that there is much greater erosion at the lower temperature.

The wear profiles on the nitrided pins indicate a much lower erosion depth than the H-13 pins at the same conditions, indicating that nitriding might reduce erosive wear. The distinction is less clear cut while observing the weight loss measurements (table 4.2). This is not surprising as the weight loss measurements average the wear effects over the entire weight of the pin, even though only a small mass of the pin is exposed to wear conditions. The wear profiles on the other hand measure local wear effects only, and any distinctions in wear are clearly seen from the wear profiles.

4.6.2 Campaign iii

This was an initial angle of attack campaign that was conducted to determine the importance of angle of attack studies. In this campaign, 83, 76, 69 and 65 degree angle pins were placed in the front row of the die as shown in Figure 4.8. The nominal conditions for the campaign were 150 F superheat (maintained within + or - 15 F) and 50 m/s gate velocity. The gate velocity was maintained within + or - 1.5 m/s with a first phase valve position of 60 % and a second phase valve position of 28. There were few problems with this campaign and a plot of the weight loss with the angle of attack is presented in Chapter V. Since angle of attack effects appeared critical after this preliminary study a series of campaigns (3-8) as discussed in the experimental plan, were designed to study angle effects.

4.6.3 Campaign 1

This was a velocity campaign that was conducted at a normal superheat of 150 F and a gate velocity that ranged from 48.5 to 51 m/s, with a nominal mean velocity of 49.8 m/s. A low hardness edge pin, a high hardness edge pin and two coated chromium carbide pins were used in the test. The uncoated edge pins were placed on either outside pin location. This campaign was relatively uneventful, except that the second mini campaign which had to be stopped after 210 shots (250-460 shots), because the number of ingots loaded were too small. Consequently 270 shots were run on the next two mini campaigns to obtain a 1,000 shot campaign. Preheating of the die was done before each campaign to reduce thermal shock and accelerate the occurrence of thermal steady state. The weight loss of each of the pins after every mini campaign is shown in table 4.3.

Table 4.3 Campaign 1 weight loss. Weight Loss for superheat = 150 F, Gate Velocity = 50 m/s.

Front Row Pin	0 shots Initial Wt. (g)	250 shots Wt. Loss (mg)	460 shots Wt. Loss (mg)	730 shots Wt. Loss (mg)	1000 shots Wt. Loss (mg)
Low Hardness H-13 edge pin (Hrc 32)	23.765	16	25	25	28
High Hardness H-13 edge pin (Hrc 47)	23.855	14	17	20	22
Coated pin Crc(1)	23.849	2	4	5	6
Coated pin Crc(2)	23.801	1	4	4	7

The low hardness pin shows the most weight loss as would be expected, while the high hardness pin shows significant if lesser weight loss than the low hardness pin for all measurements. The weight loss of the chromium carbide is much less than the uncoated pins for all measurements indicating the suitability of chromium carbide as a erosion resistant coating. The maximum weight loss in a mini campaign in both the low and high hardness cases takes place in the first 250 shots. The amount of weight lost then tapers and varies approximately linearly with the number of shots. A discussion of the functional dependence of weight loss with time is presented in Chapter V.

(a)

(b)

Figure 4.11 Edge wear profiles of edge pins in campaign 1. (a) Low hardness pin and (b) High hardness pin.

Figure 4.11a shows the edge profile measurements for the low hardness pin (pin a). As can be seen there is a large erosion pit of maximum depth of 105×10^{-2} mm. The approximate gate location is between the 3 and 5 mm locations, and the maximum erosion is seen to occur in the pin region directly in front of the gate. The three measurements taken are nearly coincidental, although they appear to have a slight shifting error. The high hardness edge pin (pin b) wear profile shown in Figure 4.11b also appears to have a qualitative wear profile similar to the low hardness pin, although the measured wear depth (67×10^{-2} mm) is not as high as the low hardness pins. This result is consistent with the expectations that an increasing hardness reduces erosion. As can be seen the two measurements recorded for pin b are nearly identical (the third measurement is identical with one of the two measurements and is not reported). The wear profiles of both pins were relatively smooth with few local peaks and valleys as can be seen. Edge profiles of the coated pins are not shown here, as they are not relevant to this study.

4.6.4 Campaign 2

This was a temperature campaign which used a nitrided pin (pin a), and a high hardness pin (pin b) at the outer pin locations, and two coated pins at the central pin locations. A velocity of 50 m/s was maintained with a valve position (60 first phase and 28 second phase) that was the same as the other temperature campaigns. The campaign was the lowest temperature campaign conducted and the one with the most casting problems. The campaign was initially run at a superheat of 50 F, but the superheat was raised to 60 F after 50 shots because of extensive casting problems. The very low superheat resulted in closing of the funnel and pourhole because of rapid solidification. Also periodic solidification build-up on the ladle due to the low superheat resulted in a constant change of the location where metal was poured resulting in spill over of molten metal. Continuous removal of the solidified build-up in the funnel, pour hole and ladle was necessary in this campaign. Nevertheless, the biscuit

size (indicating the amount of metal poured) varied from incomplete fill (negligible biscuit) to the nominal biscuit size of 1 inch. A 1-2 minute shutdown when problems became excessive was also common. This campaign thus illustrated some of the difficulties in running die casting campaigns at very low superheats. Because of excessive scoring on the plunger tip caused by highly solidified metal, seizing of the plunger tip in the shot sleeve also occurred near the end of the second mini campaign (468 shots). Dies had to be opened to remove the sleeve and the tip had to be bored out. The campaign was restarted after repair of the tip and 266 and 249 shots were run in the final mini-campaigns.

Table 4.4. Campaign 2 weight loss. Weight Loss for Superheat = 60 F, Velocity = 50 m/s.

Front Row Pin	0 shots Wt. Loss (g)	250 shots Wt. Loss (mg)	468 shots Wt. Loss (mg)	734 shots Wt. Loss (mg)	983 shots Wt. Loss (mg)
Nitrided H-13 edge pin (Hrc 62)	24.476	26	49	68	73
High hardness H- 13 edge pin (Hrc 47)	24.517	17	22	29	46
Coated (A I)	26.762	51	57	57	58
Coated (A II)	55.309	13	22	30	33

As can be seen from the table of weight loss versus number of shots for the front row pins (table 4.4), the nitrided pins have the greatest weight loss followed by the coated pin (A I) and the high hardness edge pin. The coated pin (A II) had the lowest weight loss after a 1,000 shots. However

the relative weight loss variations at preliminary measurements are different. A discussion of the time variation of erosion in this campaign is presented in Chapter V.

The edge profile of the nitrided edge pin is shown in Figure 4.12a. Two measurements with extremely sharp local peaks and valleys are seen. As can be seen there is a sharp local erosion pit slightly away from the gate (which runs from 3 to 5 mm). This may have been caused by localized flow patterns or by a slight rotation of the nitrided pin which occurred because the nitrided pin did not fit as snugly as the other pins. The edge profile measurements of the high hardness edge pins is shown in Figure 4.12b. The two measurements shown are nearly identical, and the wear pattern is relatively smooth without many local peaks and valleys unlike the nitrided pin. Again there is a sharp erosion pit approximately located in front of the gate. The maximum erosion depth is 137×10^{-2} mm is also much greater than the nitrided pins. This is again not very surprising as the nitrided pins have a much higher hardness (Hrc 62) and the results are similar to that seen in campaigns i and ii. However there does not appear to be a significant advantage in using nitrided pins from the weight loss measurements. Indeed the weight loss is greater for the nitrided pin than the high hardness H-13 pin. The depth of erosion seen in the high hardness pin is much larger than that of an identical pin used in Campaign 1 (Figure 4.11b). This shows that erosion rates increase with a drop in temperature of 100 F that is similar to the results reported in campaigns i and ii. The edge profiles of the coated pins are not reported here as they are not relevant to this study.

Figure 4.12 Edge wear profiles of edge pins in campaign 2. (a) Nitrided H-13 pin and (b) High hardness H-13 pin.

4.6.5 Campaign 3

This was a relatively smooth running velocity campaign, conducted at a low gate velocity of 27 m/s and a easily castable superheat of 150 F. A low hardness and high hardness H-13 edge pins were located in either end locations, while 83 and 69 degree angle pins were placed in the central pin locations. In this campaign the velocity was maintained at almost a constant value of 26.86 m/s with a first phase valve position of 60 and a second phase valve position of 18 except occasionally when velocities of 25.8 m/s would be recorded. The temperature was maintained at a nominal superheat of 150 F to within + or - 12 F. The campaign was relatively smooth and mini campaigns were conducted at 234, 260, 257 and 249 shots to complete a 1,000 shot campaign.

Table 4.5. Campaign 3 weight loss. Weight Loss for Superheat = 150 F, Velocity = 27 m/s.

Front Row Pin	0 shots Initial Wt. (g)	234 shots Wt. Loss (mg)	494 shots Wt. Loss (mg)	751 shots Wt. Loss (mg)	1000 shots Wt. Loss (mg)
Low Hardness H-13 edge pins (Hrc 32)	23.563	6	7	7	10
High hardness H-13 edge pin (Hrc 47)	23.424	0	3	3	3
Angle pin Hrc=32, α =83 deg.	23.445	2	4	4	5
Angle pin Hrc=32, α =69 deg.	9.296	4	4	5	7

The weight loss after each mini-campaign for the front row pins is shown in table 4.5. As can be seen the weight loss is extremely low for all pins. A

nearly linear variation in the weight loss with the number of shots is seen for all the pins as will be discussed in Chapter V. Because of the very low weight loss however, no campaigns were conducted at lower velocities.

The edge profile measurements of the low hardness pin (pin a) is shown in Figure 4.13a, while the edge profile measurements of the high hardness pin is shown in Figure 4.13b. As can be seen, there is very little erosion in either pin, although the erosion depth of the low hardness pin is slightly greater. There is also no distinct pit formation that can be recognized. This is not unexpected as the weight loss in this very low velocity campaign was also negligible. This velocity can perhaps be considered a threshold velocity below which there is little erosion after a 1,000 shots.

Wear profiles at left, middle, and right locations of the eroded face of the 83 and 69 degree angle pins are shown in Figures 4.14a and 4.14b respectively. Again, as can be seen there is no distinct erosion pit, although the depth of erosion is slightly larger at the gate location (3-5 mm). It was for these low erosion rates (both weight loss and wear profiles) that it was decided that no campaigns would be conducted at velocities below 27 m/s.

A visual observation of the pin faces shows that the angled face in front of the gate shows some erosion (the machining lines are removed and the face has a different eroded appearance) as compared to the faces away from the gate which all appear uneroded and still show machining lines. Thus the angle face is eroded, only the erosion is more uniform along the length of the face. This is unlike the edge pins where there is a large erosion pit across from the gate. Thus while the weight loss of the edge pins and the angle pins are comparable, there is no large erosion pits across from the gates in the angle pins. Thus the weight loss in the angle pins is distributed over a larger area. An explanation for this observation is provided in Chapter V.

Figure 4.13 Edge wear profiles of edge pins in campaign 3. (a) Low Hardness H-13 pin and (b) High hardness H-13 pin.

(a)

(b)

Figure 4.14 Wear profiles along left, middle and right locations of the face of the angle pins used in campaign 3. (a) 83 degree angle pin and (b) 69 degree angle pin.

4.6.6 Campaign 4

This is an intermediate velocity campaign, which was again conducted uneventfully. A nominal velocity of 42 m/s was maintained for these campaigns with a first phase valve position maintained at 60 and a second phase valve position that was changed to 25. However the actual velocity ranged from 39.8 to 43.06 m/s. The nominal superheat was 150 F and this was maintained to within + or - 12 F. A low hardness (pin a) and high hardness pin (pin b) were placed in the outer pin locations, while a 83 and 76 degree angle pins (identical in heat treatments to the low hardness pin) were placed in the central pin locations. Mini campaigns of 262, 248, 240 and 240 shots were conducted to complete a 990 shot campaign. This was probably the easiest campaign regarding casting problems, although there were some mechanical problems with the ladle system. This caused some repairs to be conducted in the middle of a campaign.

Table 4.6 Campaign 4 weight loss. Weight Loss for Superheat = 150 F, Velocity = 42 m/s.

Front Row Pin	0 shots Initial Wt. (g)	262 shots Wt. Loss (mg)	510 shots Wt. Loss (mg)	750 shots Wt. Loss (mg)	990 shots Wt. Loss (mg)
Low hardness H-13 edge pin (Hrc 32)	23.481	4	8	18	21
High hardness H-13 edge pin (Hrc 47)	23.320	2	6	8	12
Angle pin Hrc=32, α =83 deg	9.543	3	8	10	12
Angle pin Hrc=32, α =76 deg	9.319	5	13	19	22

Again there is not a large weight loss overall for all the pins in this campaign (table 4.6). No distinct weight loss vs. number of shots pattern is seen although most variation appears to be linear. The weight loss of the low hardness edge pin (pin a) and the 76 degree angle is nearly identical at these 4 measurement points, while the 69 degree angle pin and the high hardness pins (pin b) have approximately similar weight loss patterns that are lower than the other 2 pins.

The edge profile of the low hardness edge pin (pin a) is shown in Figure 4.15a. Only two measurements are shown as the other measurement is coincident with one of these two measurements. The two measurements shown here also appear identical except for a recording location shift. As can be seen there is a large erosion pit seen in this campaign, unlike an identical pin in campaign 3. A maximum depth of the erosion pit of 60×10^{-2} mm is recorded for the purpose of comparisons. Unlike the low hardness pin of campaign 1, the edge profile is not completely smooth and is somewhat jagged even close to the maximum depth of the pit. This may indicate local regions in the eroded pit which are not removed because of localized flow phenomena or simply measurement errors. Also the edge pin wear is not symmetric with more erosion at the top of the pin. This is not surprising as the gate is located unsymmetrically (only in the ejector half) and there is a draft in the pin.

The edge profile of the high hardness pin is shown in Figure 4.15b. Only one measurement is shown because all measurements are coincident. As can be seen the erosion in this pin is much less than the low hardness pin. While a maximum erosion depth of 28×10^{-2} mm is reported for later comparisons, there is no distinct erosion pit that is seen because of the low erosion rates. Instead the erosion is more spread out along the entire pin length, with a sharp local peak that may be due to some local region of high soldering, high hardness, machining discrepancies or experimental error. Again because of the low erosion rates such peaks are not unlikely and are in reality relatively small.

Figure 4.15 Edge wear profiles of edge pins in campaign 4. (a) Low Hardness H-13 edge pin and (b) High hardness H-13 edge pin.

(a)

(b)

Figure 4.16 Wear profiles along left, middle and right locations of the face of the angle pins used in campaign 4. (a) 83 degree angle pin and (b) 76 degree angle pin.

Profiles along left, middle and right locations on the face of the 83 and 76 degree angle pins are shown in Figure 4.16a and 4.16b respectively. Like in campaign 3, there appears to be no large erosion pit across from the gate in the angle pins. Visual inspections of the angle pins show nearly uniform erosion along the face of the pin with no deep erosion pits, unlike the edge pins. This is borne out by the surface profiles shown in Figures 4.15 and 4.16. Thus the surface profiles on the angle pins in this study cannot be used as a relative measure of erosive wear, unlike the weight loss. An explanation of this pattern is given in Chapter V.

4.6.7 Campaign 5

In this high velocity campaign, a low hardness edge pin (pin a) and a high hardness edge pin (pin b) were placed at either end pin locations and a 76 degree and a 69 degree angle pins were placed in the central pin locations. The first phase valve position remained unchanged at 60, while the second phase valve opening value was placed at 39 % to obtain a nominal gate velocity of 55.7 m/s. There was a fairly large variation in velocity during the campaign as such a high velocity appeared to strain the machine capability. The velocity varied from 52.2 to 56.9 m/s and 55 m/s was considered the nominal mean velocity because such a velocity was measured in a majority of shots. The nominal melt superheat was 150 F and this was maintained to within + or - 12 F in all mini campaigns.

Because of the very high velocity and applied pressure, severe flashing was a problem in this campaign. This resulted from the die inserts which are bolted into the die block, being pushed by the high molten metal pressure. While flashing is not a desirable result for production parts as the excess flash has to be physically removed, it is quite common and has little significance with regard to die erosion. Mini campaigns of 240, 245, 270, and 245 shots were conducted to complete a campaign of 1,000 shots, and the weight loss at the end of each mini campaign is shown in table 4.7.

Table 4.7 Campaign 5 weight loss. Weight Loss for Superheat = 150 F, Velocity = 55 m/s.

Front Row Pins	0 shots Initial Wt. (g)	240 shots Wt. Loss (mg)	485 shots Wt. Loss (mg)	755 shots Wt. Loss (mg)	1000 shots Wt. Loss (mg)
Low hardness H-13 edge pin (Hrc 47)	23.429	10	19	26	32
High hardness H- 13 edge pin (Hrc 47)	23.276	8	14	17	24
Angle pin Hrc=32, α =69 deg,	9.251	8	12	14	21
Angle pin Hrc=32, α =76 deg,	9.381	9	14	15	24

The low hardness edge pins showed the greatest weight loss at all measurement times, while the weight loss of the 3 other pins were close together at all measurement times as can be seen in table 4.7. The variation of weight loss with time also appears to be nearly linear in all pins, although the maximum weight loss occurs in the first 250 shots.

The edge profile measurements of the low hardness pin (pin a) are shown in Figure 4.17a. The two measurements shown are nearly identical and the wear pattern is relatively smooth with few local peaks and valleys. The maximum erosion depth of 104×10^{-2} mm is slightly lower than that of campaign 1, even though the velocity of campaign 1 is lower. A possible explanation may be that the velocity difference between campaigns 1 and 5 (50 and 55 m/s) is not high enough to cause a large

(a)

(b)

Figure 4.17 Edge wear profiles of edge pins in campaign 5. (a) Low Hardness H-13 edge pin and (b) High hardness H-13 edge pin.

Figure 4.18 Wear profiles along left, middle and right locations of the face of the angle pins used in campaign 4. (a) 69 degree angle pin and (b) 76 degree angle pin.

variation in wear profiles. The edge profile measurements of the high hardness pins are shown in Figure 4.17b. There is a slight variation in the two measurements in terms of a measurement location shift. The wear profile is also not as smooth as the low hardness pins with some severe peaks and valleys and the width of the erosion pit is also larger in the high hardness case. Similar to the low hardness edge pin however, the maximum erosion depth of 60×10^{-2} mm is slightly lower than the same pin in Campaign 1 (67×10^{-2} mm).

Wear profiles at left, middle and right location on the worn face of the 69 and 76 degree pins are shown in Figures 4.18a and 4.18b respectively. Similar to the other cases, there is not a severe erosive pit in the angle pins, although it appears a greater surface of these pins have been eroded.

4.6.8 Campaign 6

This was the highest temperature campaign that was conducted. A nominal mean superheat of 260 F was maintained throughout the campaign. At high temperatures, manual control of temperatures was slightly less effective because of the greater heat loss at higher melt temperatures. Nevertheless the temperature variation was controlled to within + or - 18 F. The valve settings were maintained at 60 % (first phase) and 28 % (second phase) which was the same for all temperature campaigns. This resulted in the velocity being maintained at 49.8 m/s within + or - 1.5 m/s, which was the nominal velocity for the temperature experiments. As usual the low hardness edge pins and the high hardness edge pins were placed on the outside pin locations, while 76 and 89 degree angle pins were placed in the middle locations.

Casting at high temperatures was relatively easy although periodic gate soldering (3 times during the 1,000 shot campaign) occurred due to the die being extremely hot. Also, flashing behind the plunger tip became common especially during the third mini-campaign. This resulted in a

seized plunger tip during the middle of the third campaign. This necessitated a tear-down of dies to remove the shot sleeve, and consequent machining to remove the plunger tip. The plunger sticking was however probably a cumulative effect of damage over the previous campaigns, and not a result of this particular campaign.

Because of these problems, no weight loss measurements were taken after the third mini campaign as the campaign was not complete. The fourth mini-campaign was extended in terms of the number of shots, but because of furnace capacity problems, only 988 shots were possible. Thus weight loss measurements were taken after 250, 497 and 988 shots, and the results are shown in table 4.8. The low hardness edge pin suffers the maximum weight loss after each mini campaign, while the 89 degree angle pin shows the minimum weight loss after each mini campaign. The weight loss of the high hardness edge pin and the 76 degree pin were intermediate to these extremes. The relative weight loss of the 76 degree pins increased with the number of shots, while the other pins showed a nearly linear increase in the weight loss over the mini campaigns although the maximum weight loss occurs in the first 250 shots. A discussion of weight loss variation with number of shots for this campaign is presented in Chapter V.

The edge profile of the low hardness pin is shown in Figure 4.19a. As can be seen the wear profile is not smooth with a number of local peaks and valleys. These could have been caused by localized or random flow, microstructure or machining effects. The maximum depth of erosion 52×10^{-2} mm is chosen as a measure of erosive wear, although that may not be completely accurate because of the jagged nature of the erosion pattern. Two nearly identical measurements of the edge profiles of the high hardness pins is shown in Figure 4.19b. As can be seen the weight loss profile is smoother than the low hardness case, while the erosion depth (25×10^{-2} mm) is smaller as anticipated. Both low and high hardness pins thus have the lowest erosive pit depth of all the temperature campaigns.

Thus despite the severe temperature conditions, the wear depths are not very large. However a greater surface area of the pin appears to have been eroded, perhaps because of the greater corrosive diffusion at higher temperatures. This can be seen in the weight loss measurements, which are comparable with the weight loss of other lower temperature campaigns. A discussion of the reasons for this observation is provided in Chapter V.

Wear profiles at left, middle and right location on the worn face of the 76 and 89 degree pins are shown in Figures 4.20a and 4.20b respectively. Similar to the other cases, there is not a severe erosive pit in the angle pins, although it appears that a greater surface area of these pins have been eroded.

Table 4.8 Campaign 6 weight loss. Weight Loss for Superheat = 260 F, Velocity = 50 m/s.

Front Row Pin	0 shots Initial Wt. (g)	247 shots Wt. Loss (mg)	497 shots Wt. Loss (mg)		988 shots Wt. Loss (mg)
Low hardness H-13 edge pin (Hrc 32)	23.373	31	35		49
High hardness H-13 edge pin (Hrc 47)	23.444	14	26		28
Angle pin Hrc=32, $\alpha=76$ deg,	9.246	10	21		32
Angle pin Hrc=32, $\alpha=89$ deg,	10.132	11	14		20

Figure 4.19 Edge wear profiles of edge pins in campaign 6. (a) Low Hardness H-13 edge pin and (b) High hardness H-13 edge pin.

6c (Superheat = 260 F, Vel= 50 m/s,
76 degree angle pin)

(a)

6d (Superheat = 260 F, Vel= 50 m/s,
89 degree angle pin)

(b)

Figure 4.20 Wear profiles along left, middle and right locations of the face of the angle pins used in campaign 6. (a) 76 degree angle pin and (b) 89 degree angle pin.

4.6.9 Campaign 7

This campaign was conducted at the maximum velocity of all the velocity campaigns. The gate velocity was maintained at between 63 and 67 m/s by reducing the first phase valve position to 52 and moving up the second phase valve open position to 65. This enables a larger gate velocity by increasing the shot sleeve distance over which the second phase velocity acts. A velocity of 65 m/s was maintained in a majority of shots. The variation of velocity is slightly larger at such high velocities because the injection system is close to its capacity and any resistance to flow cannot be easily overcome. The nominal furnace superheat at which experiments were conducted was 150 F with the temperature being maintained within + or - 10 F. The low hardness edge pin (pin a) and the high hardness edge pin (pin b) were placed at the outer pin locations with 69 degree and 89 degree angle pins at the central pin locations. Like in campaign 5 the high velocities appeared to stretch the machine capability resulting in extensive flashing, as the pressure exerted by the metal exceeded the locking force. Thus higher gate velocities were not attempted in this machine (although a few shots at 70 m/s were attempted). Campaigns of 260, 250, 260 and 230 shots were conducted to obtain a campaign of 1,000 shots. More shots were conducted in the earlier mini campaigns to insure that a 1,000 shots would be obtained, if there were any problems in the latter stages of the final mini campaign.

In this campaign, the demarcation in weight loss between the low and high hardness pins did not hold for all weight loss measurements as can be seen in table 4.9. In fact after the first 260 shots, the high hardness pins have higher weight loss than the low hardness pins. But this pattern does not hold, and after 1,000 shots the low hardness pins indeed suffer the greater weight loss. This pattern represents some of the somewhat random nature of the erosion process, and illustrates some of the difficulties in conducting a controlled experiment. In this case like in campaign 6, the 89 degree pin suffers the least weight loss, while the 69

degree angle pin suffers extensive weight loss (nearly twice that of the 89 degree pin) comparable to that of the low hardness pins. This is again somewhat contrary to conventional wisdom, which would expect that the maximum erosion occurs at normal impact ($\alpha = 89$ degrees).

Table 4.9 Campaign 7 weight loss. Data for Superheat =150 F, Velocity = 65 m/s.

Front Row Pin	0 shots Initial Wt. (g)	250 shots Wt. Loss (mg)	510 shots Wt. Loss (mg)	770 shots Wt. Loss (mg)	1000 shots Wt. Loss (mg)
Low hardness H-13 edge pin (Hrc 32)	24.068	8	20	30	42
High hardness H- 13 edge pin (Hrc 47)	23.467	16	25	33	34
Angle pin Hrc=32, $\alpha = 69$ deg,	9.111	15	26	34	42
Angle pin Hrc=32, $\alpha = 89$ deg,	9.912	2	14	18	23

(a)

(b)

Figure 4.21 Edge wear profiles of edge pins in campaign 7. (a) Low Hardness H-13 edge pin and (b) High hardness H-13 edge pin.

Figure 4.22 Wear profiles along left, middle and right locations of the face of the angle pins used in campaign 7. (a) 69 degree angle pin and (b) 89 degree angle pin.

The edge profile measurements of the low hardness pin (pin a) are shown in Figure 4.21a. The two measurements shown are nearly identical. This pin represents the deepest erosive wear seen (maximum erosion depth of 150×10^{-2} mm) of all the campaigns, and the erosion pit is relatively smooth with few projections. This is probably a reflection of the severe erosion seen in the campaign. The edge profile measurements of the high hardness pins are shown in Figure 4.21b. There is a slight variation in the two measurements in terms of a measurement location shift. The wear profile (maximum erosion depth of 89×10^{-2} mm) is also not as smooth as the low hardness pins with some severe peaks and valleys. The width of the erosion pit is also larger in the high hardness case seen. The maximum erosion depths of the low and high hardness pins is the highest for all the velocity campaigns, which is expected since this campaign is conducted at the highest gate velocity.

The wear profiles from left, middle and right locations on the face of 69 and 89 angle pins are shown in Figures 4.22a and 4.22b respectively. As can be seen, while the maximum depth of erosion pit is larger in this case (because of the high gate velocity) than in other campaigns, it is still extremely small in comparison with the depth of pits in the edge pins. Thus the use of wear profiles as a erosive wear measure for the angle pins will require more than 1,000 shots as even with a gate velocity of 65 m/s, the wear depths are not very large in the angle pins.

4.6.10 Campaign 8

In this velocity campaign a nominal velocity of 35 m/s (maintained between 34.9 to 37.1 m/s) was maintained with a superheat of 150 F (maintained to within + or - 12 F). The first and second phase valve positions were set at 60 % (maintained in almost all campaigns) and 22 % respectively to maintain this gate velocity. Because of the relatively mild conditions (intermediate velocity and superheat), this was a relatively problem free campaign. As usual, a low hardness (pin a) and high

hardness edge pin (pin b) were placed at either corner pin location, while 89 degree and 83 degree angle pins were placed at either central pin location.

Mini campaigns of 260, 250, 260 and 230 shots were conducted to finish a mini campaign of 1,000 shots. More shots were conducted in the earlier mini campaigns so that 1,000 shots could be completed in the campaign if there were any problems in later mini-campaigns. No measurements were taken after the third mini campaign (after 770 shots) because of time constraints on the machine. Because of the low weight loss in this campaign, intermediate weight loss measurements are probably not as critical.

The weight loss of all the pins after each mini campaign is shown in table 4.10. Because of the small gate velocities, the weight loss was not very high in this case, but the low hardness pins exhibited the maximum weight loss at the end of each mini-campaign. Again the 89 degree angle pin appears to have the least weight loss after every mini campaign. The weight loss rate also appears to be nearly linear in all cases, as will be discussed in Chapter V.

Two distinct measurements of the edge profile of the low hardness pins is shown in Figure 4.23a and of the high hardness pin is shown in Figure 4.23b. The wear profile of the low hardness pin is not as smooth with some oscillating variations, while the high hardness pins have a relatively smooth wear pattern. As expected, the erosion rate of the low hardness pins is much higher than the high hardness pin. The wear profile on these sets of pins is however not as severe as campaign 7, but that is to be expected as the gate velocity is only 35 m/s as opposed to 65 m/s in campaign 7.

Table 4.10 Campaign 8 weight loss. Data for Superheat = 150 F, Velocity = 35 m/s.

Front Row Pin	0 shots Initial Wt. (mg)	260 shots Wt. Loss (mg)	515 shots Wt. Loss (mg)	770 shots Wt. Loss (mg)	1000 shots Wt. Loss (mg)
Low hardness H-13 edge pin (Hrc 32)	25.8668	2	—	8	12
High hardness H-13 edge pin (Hrc 47)	23.5689	1	—	3	7
Angle pin Hrc=32, $\alpha = 89^\circ$ deg.	10.1304	2	—	3	4
Angle pin Hrc=32, $\alpha = 83^\circ$ deg.	9.6179	4	—	5	9

The profiles along locations on the left, middle and right of the face of 89 and 83 degree angle pins are shown in Figures 4.24 a and b respectively. Similar to the wear profiles of all the angle pins except Campaign 7 (where the gate velocity is the highest of all campaigns with angle pins) the depth of the erosive pit is extremely small. Thus a larger number of shots are required for the wear profiles of the angle pins to be used as a measure of erosive wear. Essentially the wear is spread over a greater area resulting in comparable weight loss with the edge pins, but extremely small erosion depths in comparison with the edge pins. Thus the weight loss is the best measure of erosive wear for the angle pins. An explanation for this observation is provided in Chapter V.

Figure 4.23 Edge wear profiles of edge pins in campaign 8. (a) Low Hardness H-13 edge pin and (b) High hardness H-13 edge pin.

8c (Superheat = 150 F, Vel= 35 m/s,
89 degree angle pin)

(a)

8d (Superheat = 150 F, Vel= 35 m/s,
83 degree angle pin)

(b)

Figure 4.24 Wear profiles along left, middle and right locations of the face of the angle pins used in campaign 7. (a) 89 degree angle pin and (b) 83 degree angle pin.

4.7 SUMMARY

An experimental erosive wear test procedure was developed, to understand die erosive wear and to quantify the effects of important process conditions in causing erosion in die casting dies. The tests were carried out on a commercial 250 ton Buhler die casting machine and an accelerated procedure was developed to obtain meaningful wear results in a reasonable amount of time. The procedure involved the use of pyramidal test pins as a surrogate measure of die wear, and avoided the drawbacks of prior studies of erosive and corrosive wear in die casting. Even though the erosive wear is accelerated, the one significant drawback of this experimental approach is the large time frame required to obtain meaningful results.

An experimental plan was developed to evaluate the effect of specific process parameters. The process parameters evaluated included impact velocity, angle of metal impact, pin geometry, pin hardness and heat treatment, number of shots and metal temperature or superheat. The reasons for choosing this experimental plan, and details of the execution of the experimental plan (including possible experimental error) are outlined in this Chapter. Details of the procedure used for various experimental campaigns conducted are similarly outlined and the raw wear measurements for every pin used in the campaigns is presented.

CHAPTER V

RESULTS AND DISCUSSION

Chapter IV described the execution of an experimental plan to determine the effects of process parameters on erosive wear, while Chapter III described the development of a numerical procedure to accurately determine the values of process parameters at various locations in the die cavity. With the aid of numerical simulations that provided values of process parameters at various die locations, the experimental results are interpreted to obtain quantitative and qualitative evaluations of the effect of specific process parameters on die erosive wear. The details of this evaluation are reported in this chapter.

5.1 NUMERICAL SIMULATIONS OF EROSIIVE WEAR DIE

As discussed in Chapter IV, a number of velocities, temperatures, pin orientations and pin angles were used in the experiments. Numerical simulations of die cavity filling and solidification of the erosive wear die, were carried out for all the process conditions and geometries considered. Important findings from these simulations as it relates to die erosive wear are summarized in this section. However only a selected few results are reported here to avoid repetitiveness.

The first layout considered is the edge pin layout (Figure 4.5). To reduce computational time and effort the geometrical region that is modeled, is given by one half of the cavity, in the schematic shown in Figure 4.5. This is achieved by imposing symmetry conditions on the right wall of the computational mesh. Only the gate in the runner system is modeled, to

further reduce computational time and effort. Thus fluid is assumed to flow across the two gates, to impact the pins and fill one half of the die cavity as shown in the simulation results (Figures 5.1-5.9).

The velocity at the gate is the nominal gate velocity used for the campaign being simulated, with the inlet gate temperature used being 50 F below the nominal, to account for temperature losses in the sleeve and ladle (Lindsay & Wallace, 1972). Heat transfer was considered between the metal and the die surface in all cases. Handbook values were used for thermal and flow properties of aluminum alloy and steel, such as thermal conductivity, density, dynamic viscosity, specific heat, latent heat and coefficient of thermal expansion (Table 5.1). During filling the heat transfer coefficient is calculated dynamically using the Prandtl fluid wall heat transfer equation which gives the heat transfer coefficient as a function of the product of some power of Reynolds and Prandtl numbers. Because of high metal velocities the heat transfer coefficient is likely to be very high. During solidification a h value of 1.31×10^7 ergs/cm² K s (Papai & Mobley, 1991) was used to account for air gap formation during solidification.

A filling pattern through a section of the cavity that passes through the gate is shown in Figure 5.1. Contoured on that plot is the cell fluid fraction. A cell fluid fractions of one indicates a full cell, while fluid fractions of zero imply an empty cell, and fluid fractions less than one indicate partially full cells depending on the value of the fluid fraction. This is for nominal conditions of 50 m/s and 150 F superheat. The plot shows some of the spraying and flashing that occur during die filling in die casting. A less clear three dimensional view of the same filling pattern at various instants of time is shown in Figure 5.2. As can be seen, the metal flows through the gate in the form of a jet and strikes the edge of the pin. The jet of metal is separated by the pin, and the split jets flow in a fine spray to the back of the cavity. After the back of the cavity is filled, the fluid flows back and fills the front of the cavity near the gate.

Table 5.1 Values of thermo-physical properties used in the numerical simulations of erosive die cavity filling.

Property	Value	Units
Heat Transfer Coefficient	1.3×10^7	erg/s/cm ² /K
Thermal conductivity of die wall	3.3×10^6	erg/s/cm/K
pc product of die wall (thermal capacity per unit volume)	3.57×10^5	erg/cm ³ /K
Latent Heat of Al	3.6×10^9	erg/gm
Specific heat of Al	9.0×10^6	erg/gm/K
Dynamic viscosity of Aluminum alloy	2.0	poise
Density of Al	2.7	gm/cm ³
Thermal conductivity of Al	9.6×10^6	erg/s/cm/K
Coeff. of linear expansion of Al	2.43×10^{-5}	cm/K

Figure 5.1 Flow pattern through a cross-section of the edge pin layout at various instants of time, showing the jetting action from the gate to the pins. Nominal process parameter values are gate velocity = 50 m/s, and inlet superheat =150 F.

Figure 5.2 Three dimensional flow pattern of the edge pin layout at various instants of time, showing the jetting action from the gate to the pins. Nominal process parameter values are gate velocity = 50 m/s, and inlet superheat =150 F.

Thus the basic assumption of the experimental design is correct; i.e. the metal flows in the form of a jet with the same cross sectional area as the gate and consequently impacts the pin with the same high velocity. This results in the severe wear conditions seen in the pin, especially the large erosion pits seen in front of the gates. Numerical models with viscous flow assumptions, would have led to predictions of continuous flow through the cavity and across the length of the pin. Such filling patterns would not have been able to explain the severe erosion seen in the pin in front of the gates. The filling patterns seen here also illustrate some of the quality problems that are encountered in die cast parts. As can be seen, the regions close to the gate and pins, will have entrapped air as they are filled by backfill from the back of the cavity. The fine mists or sprays that develop due to the flow separation and splashing against walls should also entrap air.

A cross sectional view at the pins of the same filling pattern at identical instants of time is shown in Figure 5.3. As can be seen, the metal flows from the gate in the form of a jet, and strikes the edge of the pin along a height equal to the height of the gate. There is very little flow along the top and bottom of the pins. Thus the pin merely separates the flow of the jet from the gate, resulting in an area of large impact velocities corresponding to the area of the gate. This thus explains the large erosion pit in front of the gate seen in most edge pins at large velocities. As can be seen the cavity at the back of the pin, fills from backfill that moves toward the gate from the back of the cavity. The temperature distribution in the cavity at various instants of time is also shown in the free surface plot of Figure 5.2. There is very little temperature variation during filling because of the low filling times (< 40 milliseconds) and simple cavity geometry. Thus most of the heat losses take place in the ladle and shot sleeve. Velocity and temperature profiles along the pin edge were obtained from these simulations, and the results are used for velocity and temperature comparisons later.

Figure 5.3 Flow pattern along a cross-section of the edge pin layout view shown in Figure 5.1, at various instants of time, showing the jetting action from the gate to the pins.

As discussed earlier all conditions of gate velocities and temperatures were used for the simulations. However, only some of the simulations at extreme conditions are shown here for brevity. A cross section through the cavity at the gate, for the lowest gate velocity of 27 m/s (superheat 150 F) is shown in Figure 5.4 (campaign 3). At a low velocity it is expected that momentum effects are lower, consequently resulting in a tendency of the jet to spread or the fluid to flow in a solid front without jetting. However, the filling pattern indicates that despite the reduction in velocity, the fluid momentum is high enough to cause a jet to strike the pin. However the jet is slightly more spread out, than the 50 m/s case (Figure 5.1).

Another extreme condition is shown in Figure 5.5 which shows a filling pattern through the same section shown in Figures 5.1 and 5.4. The conditions for this simulation are a gate velocity of 65 m/s (highest of all campaigns) and superheat of 150 F (campaign 7). As can be seen the jet that strikes the pin is slightly less spread out than in the other two cases of 50 m/s (Figure 5.1) and 27 m/s (Figure 5.4) shown here. However, the jet is essentially of the same area (which is approximately equal to gate cross-sectional area).

Although the manner of filling behind the pin is a little different in the three velocity cases shown here because of different momentum effects, it does not affect the flow of the jet that strikes the pin. Since this jet appears to cause the erosion on the pin, it can be concluded that the qualitative flow conditions that cause pin erosion remain the same in all cases. Also, the velocity of impact on the pin (at the center of the jet) remains the same as the gate velocity in all cases. Thus the gate velocity can be directly used to study the effect of impact velocity. This is an important conclusion that will be used in studying the effect of gate velocity on erosion.

Figure 5.4 Flow pattern through a cross-section of the edge pin layout at various instants of time, for a gate velocity of 27 m/s, and inlet superheat of 150 F.

Figure 5.5 Flow pattern through a cross-section of the edge pin layout at various instants of time, for a gate velocity of 65 m/s, and inlet superheat of 150 F.

Another extreme temperature campaign filling pattern is shown along a section through the gate (similar to the other cases) in Figure 5.6. The conditions simulated here are those for campaign 2 (gate velocity = 50 m/s, metal superheat = 60 F). As previously discussed, low metal temperatures should result in a reduction of momentum effects because of the increased metal viscosity as the metal solidifies at lower temperatures. However as can be seen, while there is a slight spreading of the jet, the filling pattern remains identical with the rest of the cases as regards flow in front of the pin. Since this flow in front of the pin appears to cause die erosion, the qualitative conditions that cause erosion at the pin remain the same.

Thus, the flow conditions in front of the edge pins, principally the jet from the gate remains similar under all conditions of temperature and velocity for which experiments were conducted. The jet cross-sectional area and temperature also does not change significantly from the gate to the pin. Since this jetting flow appears to cause the pin erosion, the gate velocity and temperature can be used to describe the conditions acting on the pin for the erosive wear models.

Filling simulations were also carried out for the angle pin layout. The filling pattern for a section through the gate identical to the section shown in the other cases, is shown in Figure 5.7. The conditions simulated here are those for campaign iii (gate velocity = 50 m/s, metal superheat = 150 F). A filling pattern through a perpendicular section along the pin is shown in Figure 5.8. As can be seen the flow conditions at the pin are different from the edge pin case. The metal jets from the gate and strikes a face of the pin. Since the area of the pin face is greater than the gate area, the jet does not separate and continue through to the back of the cavity. Instead the jet splashes against the wall of the pin and moves up and down, as well as along the sides of the pin. From there the metal flows toward the back and sides of the cavity.

Figure 5.6 Flow pattern through a cross-section of the edge pin layout at various instants of time, for a gate velocity of 50 m/s, and inlet superheat of 60 F.

Figure 5.7 Flow pattern through a cross-section of the angle pin layout at various instants of time, showing the jetting action from the gate to the pins. Nominal process parameter values are gate velocity = 50 m/s, and inlet superheat = 150 F.

Figure 5.8 Flow pattern along a cross-section of the angle pin layout shown in Figure 5.7, at various instants of time, showing the jetting action from the gate to the pins, and the flow along the face of the pins.

Figure 5.9 Flow pattern through a cross-section of the mixed pin layout at various instants of time. Nominal process parameter values are gate velocity = 27 m/s, and inlet superheat = 150 F. The pattern shows that flow conditions in front of a pin are influenced by gate geometry alone and not by secondary flow from other pins.

Thus the flow conditions are fundamentally different between the edge and angle pins. In the angle pin case there is near perpendicular impact and splashing, which leads to metal flow along the entire face of the pin, while in the edge pin case, metal impacts, an area of the edge and continues to flow through without flowing much along the length of the pin. This flow pattern obtained from the numerical simulations is more clearly seen in the schematics shown in Figures 4.5b and 4.6b. The flow pattern explains some of the findings regarding the different nature of the erosion patterns seen in the edge pin and angle pins as will be discussed in section 5.5.

Nevertheless the angle pin simulations in Figure 5.7 again show that the flow conditions in the front of any pin are primarily caused by the flow conditions from the gate in front of the pin, and not by secondary flow from other pins or by backfill. This is also seen in the flow simulation of the mixed pin case shown in Figure 5.9. In this figure, the simulation of the filling of one half of the cavity in campaign 3 is shown. Thus the assumption that all pin locations serve as independent test sites is valid, and the erosion of a specific pin is unaffected by the pin geometries and orientations of other pins used in the campaign.

The following conclusions were thus drawn from the numerical simulations

- A jet of the same cross sectional area as the gate flows from the gate and strikes each front row pin.
- The flow conditions in front of the pins, principally the jet from the gate remains similar under all conditions of temperature and velocity for which experiments were conducted. The jet cross-sectional area and temperature also does not change significantly from the gate to the pin. Since this jetting flow appears to cause the

pin erosion, the gate velocity and temperature can be used to describe the conditions acting on the pin for erosive wear models.

- Differing flow patterns occur in front of the edge and angle pins and this provides clues regarding the reason for different erosion patterns seen in front of these pins.
- The identical pin location assumption of the experimental plan is valid since the erosion appears to be caused only by flow from the gate in front of each pin and not by secondary flow from other pins or by backfill.
- Velocity and temperature distributions were obtained along the pin being eroded, and will be linked later to the erosive patterns seen.

5.2 TIME VARIATION OF EROSION

One of the objectives of this study was to evaluate whether a campaign < 1000 shots would be sufficient to provide a quantitative or qualitative evaluation of the effect of process or coating parameters. Consequently the variation of weight loss with the velocity, temperature and angle of attack were analyzed at the end of each mini-campaign to see if the same qualitative patterns hold for all measurements taken. The variation of weight loss with number of shots was also observed for each campaign, to see if weight loss followed at least the same qualitative patterns in each campaign under identical conditions. The variation of weight loss with number of shots for the campaigns for which such data is available (campaigns 1-8) is discussed below.

5.2.1 Campaign 1

The weight loss versus number of shots for campaign 1 (150 F superheat and 50 m/s) is shown in Figure 5.10. The four pins used in this campaign are a low hardness pin (pin a), a high hardness pin (pin b), and two coated chromium carbide pins (pin c and pin d). The weight loss follows nearly a linear relationship after 250 shots although the maximum weight loss takes place in the first 250 shots. The low hardness edge pin has the greatest weight loss, followed by the high hardness pin and by the two coated pins (which have nearly identical weight loss) after a 1,000 shots. This trend holds through for all measurements. The best curve fit through the measurement points was a linear fit which gives a weight loss variation (Wt. Loss) with number of shots (N) of

$$\text{Wt Loss (in mg)} = 28 (.53771 + 4.94 \times 10^{-4} N) \quad 5.1a$$

for the low hardness edge pin (pin a)

$$\text{Wt Loss (in mg)} = 22 (.5345 + 4.81 \times 10^{-4} N) \quad 5.1b$$

for the high hardness edge pin (pin b).

This is a non-dimensionalized representation of weight loss where the best curve fit equation is divided by the weight loss after a 1,000 shots. This will result in the expression within the bracket taking a value of 1, for $N \equiv 1,000$. The term outside the bracket, is thus the weight loss after a 1,000 shots. As can be seen the slope of both the low hardness and high hardness edge pin are nearly identical (around .53), which is also observed from the parallel nature of the linear curve fits. The parallel nature of the two curve fits indicates that the qualitative (order of greater resistance to wear) and quantitative (ratio of weight loss) relationships between the two pins will continue to hold after a 1,000 shots. The two chromium carbide pins have nearly identical weight losses, which are much less than both

the low hardness and high hardness edge pin. The curve fits of the two coated pins appears to diverge, suggesting that the weight loss after 1,000 shots of coated pin CrC II, will continue to be lower than that of coated pin CrC I.

Figure-5.10 Variation and curve fit of weight loss with number of shots for the pins used in campaign 1.

5.2.2 Campaign 2

The weight loss variation and best curve fit for the pins used in campaign 2, is shown in Figure 5.11. The nominal conditions for this campaign were 60 F superheat (the lowest superheat of all the temperature

campaigns) and 50 m/s gate velocity. As discussed, this was one of the campaigns with the maximum weight loss after a 1,000 shots. The four pins used in this campaign were a nitrided edge pin (pin a), a high hardness edge pin (pin b), and two coated pins (AI (pin c) and AII (pin d)). The nitrided edge pin (pin a) has a greater weight loss than the high hardness edge pin at all measurements, despite the fact that the nitrided pins are harder (62 Hrc vs 47 Hrc). This indicates that the advantages of nitriding, may not be very significant at low superheats. The weight loss follows a nearly linear relationship after 250 shots although the maximum weight loss takes place in the first 250 shots. The best curve fit through the measurement points was a linear fit which gives a weight loss variation (Wt. Loss) with number of shots (N) of

$$\text{Wt Loss (in mg)} = 79.3 (.19 + 8.14 \times 10^{-4} N) \quad 5.2a$$

for the nitrided edge pin (pin a)

$$\text{Wt Loss (in mg)} = 43.45 (.12 + 8.84 \times 10^{-4} N) \quad 5.2b$$

for the high hardness edge pin (pin b).

The equations are again written in a non-dimensionalized form so that the expression within the brackets will reduce to 1 after a 1,000 shots. The term outside the bracket, is thus the weight loss after a 1,000 shots. The two curve fits appear to diverge slightly (non-dimensionalized slopes of 8.14 and 8.84×10^{-4}), which indicates that while the same qualitative relationship (ordering of pins by weight loss or erosion resistance) will hold after a 1,000 shots, quantitative relationships (ratio of weight loss) will change slightly for measurements beyond a 1,000 shots. Weight loss variations with the number of shots for the coated pins also appear to be similar, although the values are not recorded here for brevity. This campaign also shows some variability of the relative ranking of the weight loss of the pins at each measurement. The weight loss of the

coated pin A I is the highest after 250 shots and the nitrided pins have the greatest weight loss after 750 and 1,000 shots. A reason for this behavior of the A I coated pin may be that the dense coating of this pin is mostly removed after 250 shots exposing the less dense substrate to further weight loss. The relative ranking of the other 3 pins remains the same after each mini-campaign. Thus ignoring the coated pin A I (which has a different density substrate and coating) 250, 500, 750 or 1,000 shot campaigns could have been used for relative qualitative evaluation of pin materials and coatings for this campaign.

Figure 5.11 Variation and curve fit of weight loss with number of shots for the pins used in campaign 2.

5.2.3 Campaign 3

The weight loss versus number of shots for campaign 3 (150 F superheat and 27 m/s) is shown in Figure 5.12. The four pins used in this campaign are a low hardness pin (pin a), a high hardness pin (pin b), and 83 and 69 degree angle pins (pin c and pin d). The weight loss follows nearly a linear relationship, as can be seen in Figure 5.12. The low hardness edge pin has the greatest weight loss, followed by the 69 degree angle pin, the 83 degree angle pin and by the high hardness edge pin after a 1,000 shots. This trend holds through for all measurements. The best curve fit through the measurement points was a linear fit which gives a weight loss variation (Wt. Loss) with number of shots (N) of

$$\text{Wt Loss (in mg)} = 9.35 (.49 + 4.9 \times 10^{-4} \text{ N}) \quad 5.3a$$

for the low hardness edge pin (pin a)

$$\text{Wt Loss (in mg)} = 3.6 (.0169 + 9.88 \times 10^{-4} \text{ N}) \quad 5.3b$$

for the high hardness edge pin (pin b)

$$\text{Wt Loss (in mg)} = 5.08 (.31 + 6.93 \times 10^{-4} \text{ N}) \quad 5.3c$$

for the 83 degree angle pin (pin c)

$$\text{Wt Loss (in mg)} = 6.48 (.4 + 6 \times 10^{-4} \text{ N}) \quad 5.3d$$

for the 69 degree angle pin (pin d)

As can be seen, the curve fits through all 4 pins appear to be nearly parallel, indicating that the qualitative ordering of pins and the quantitative weight loss ratios of the different pins have the same value after 250, 500, 750 and 1,000 shots and will have the same values after 1,000 shots. This campaign thus indicates that 250 shots may be sufficient to

obtain quantitative and qualitative relationships between the different pins.

The 69 degree angle pin has a greater weight loss than the 83 degree angle pin, indicating that weight loss increases with decreasing angle of impingement. This is contrary to the expectation that maximum weight loss takes place at normal impingement for liquid metal attack. As expected, the low hardness pins have a greater weight loss than the high hardness pin at all measurements. The weight loss of the angle pins while lower than the low hardness edge pins (which has the same hardness) is comparable. It must however be noted, that the weight loss in this campaign is relatively small, thereby increasing the error associated with conclusions drawn from weight loss measurements.

Figure 5.12 Variation and curve fit of weight loss with number of shots for the pins used in campaign 3.

5.2.4 Campaign 4

The weight loss of campaign 4 pins (low hardness edge pin, high hardness edge pin, 83 degree angle pin and 76 degree angle pin) is shown in Figure 5.13. The conditions of this campaign were 42 m/s gate velocity and 150 F superheat. As can be seen the weight loss of all 4 pins appear to vary linearly with the number of shots. The linear approximation of the weight loss versus number of shots relationship can be written as

$$\text{Wt Loss (in mg)} = 22.05 (-0.14 + 11.4 \times 10^{-4} N) \quad 5.4a$$

for the low hardness edge pin (pin a)

$$\text{Wt Loss (in mg)} = 11.91 (-0.11 + 11.1 \times 10^{-4} N) \quad 5.4b$$

for the high hardness edge pin (pin b)

$$\text{Wt Loss (in mg)} = 12.71 (.0568 + 9.43 \times 10^{-4} N) \quad 5.4c$$

for the 83 degree angle pin (pin c)

$$\text{Wt Loss (in mg)} = 23.5 (-.00157 + 10 \times 10^{-4} N) \quad 5.4d$$

for the 76 degree angle pin (pin d)

As can be seen in Figure 5.13, the low hardness edge pin has a greater weight loss than the high hardness edge pin at all measurements and the linear curve fits while nearly parallel do diverge (although the slopes of the non-dimensionalized curve fits are close). This indicates that while the ordering of pins for wear resistance will remain identical for all measurements, the ratio of wear resistance of the high hardness to low hardness pin will continue to increase beyond a 1,000 shots. Similarly the 76 degree angle pin has a greater weight loss than the 83 degree angle pin,

and the linear curve fits for these two pins diverge (although the slopes of the non-dimensionalized curve fits are close). This again indicates that while the ordering of pins for wear resistance will remain identical for all measurements, the ratio of wear resistance of the 83 degree angle pin to that of the 76 degree angle pin will continue to increase beyond a 1,000 shots.

Figure 5.13 Variation and curve fit of weight loss with number of shots for the pins used in campaign 4.

The 76 degree angle pin has a weight loss which is slightly greater or almost equal to the weight loss of the low hardness edge pin of identical hardness. The weight loss of the 83 degree angle pin is however lower than the low hardness edge pin of the same hardness, and consequently lower than the 69 degree pin of lower impingement angle. It however experiences weight loss that is greater than or equal to that of the high

hardness edge pin. This qualitative ordering of the weight loss of different pins remains the same for all measurements. However the quantitative ratios of weight loss at different measurements does not remain the same at all measurements. This is because the linear curve fits do not remain parallel for the four different pins and instead diverge to give two distinct sets of weight loss patterns.

5.2.5 Campaign 5

This campaign used low and high hardness edge pins and 76 and 69 degree angle pins at nominal conditions of 55 m/s and 150 F superheat. The weight loss versus number of shots for this campaign is plotted in Figure 5.14, along with the best curve fits for each of these pins. The best weight loss vs number of shots relationship for the pins is given by

$$\text{Wt Loss (in mg)} = 32.62 (.123 + 8.77 \times 10^{-4} N) \quad 5.5a$$

for the low hardness edge pin (pin a)

$$\text{Wt Loss (in mg)} = 23.31 (.145 + 8.55 \times 10^{-4} N) \quad 5.5b$$

for the high hardness edge pin (pin b)

$$\text{Wt Loss (in mg)} = 19.82 (.192 + 8.08 \times 10^{-4} N) \quad 5.5c$$

for the 69 degree angle pin (pin c)

$$\text{Wt Loss (in mg)} = 22.31 (.196 + 8.04 \times 10^{-4} N) \quad 5.5d$$

for the 76 degree angle pin (pin d)

As can be seen in Figure 5.14, the highest weight loss is experienced by the low hardness edge pins at all measurements. The high hardness edge pin has a lower weight loss than the low hardness edge pin (as expected), and

the weight loss of this pin is almost coincident with that of the 76 degree angle pin. This can be seen in the near identical curve fits of the high hardness edge pin and 76 degree angle pin. The 69 degree angle pin suffers weight loss lower than but very close to that of these two pins.

Figure 5.14 Variation and curve fit of weight loss with number of shots for the pins used in campaign 5.

Thus the relative ordering by weight loss of these pins remains the same at all measurements, although the weight loss measurements are close together. This trend is expected to hold for measurements $> 1,000$ shots because the curve fits for these pins do not appear to converge (indeed the slopes are very close together). As discussed earlier, to maintain the same weight loss ratio at all measurements, the linear curve fits should be parallel. Since the linear curve fits appear to diverge slightly, the ratio of

weight loss of the low hardness edge pin to the other pins should continue to increase.

5.2.6 Campaign 6

A low hardness edge pin, a high hardness edge pin, and 76 and 89 degree angle pins were used in this 260 F superheat, 50 m/s campaign. Large weight losses occur in this campaign as can be seen in the plot of best curve fit of weight loss with number of shots, shown in Figure 5.15. The best linear curve fit for these pins is given by

$$\text{Wt Loss (in mg)} = 48.6 (.493 + 5.07 \times 10^{-4} \text{ N}) \quad 5.6a$$

for the low hardness edge pin (pin a)

$$\text{Wt Loss (in mg)} = 29.783 (.435 + 5.65 \times 10^{-4} \text{ N}) \quad 5.6b$$

for the high hardness edge pin (pin b)

$$\text{Wt Loss (in mg)} = 33.1 (.134 + 8.65 \times 10^{-4} \text{ N}) \quad 5.6c$$

for the 76 degree angle pin (pin c)

$$\text{Wt Loss (in mg)} = 20.08 (.397 + 6.03 \times 10^{-4} \text{ N}) \quad 5.6d$$

for the 89 degree angle pin (pin d)

As can be seen the low hardness edge pin exhibits the greatest weight loss at all measurement points. The curve fit of the low hardness edge pin is nearly parallel to the high hardness edge pin as can be seen in the slopes of the curve fits. Thus the low hardness edge pin will exhibit greater weight loss and have a near identical weight loss ratio with the high hardness edge pin for all measurement points (because the slopes of the non-dimensionalized curve fits are close). However the relative ordering of

the other pins changes with each measurement. This can be clearly seen in the intersecting of the curve fits for the high hardness edge pin and 76 degree angle pin. A possible reason for this intersecting behavior may be that due to the large amounts of soldering seen in this campaign, some of the soldering may not have been removed in all the pins after the early mini-campaigns. After 1,000 shots all the pins were placed in potassium hydroxide solution for 4 days, to remove any trace of soldering. Thus the measurement after 1,000 shots represents the most accurate measurement of the relative weight loss due to erosion under the high temperature conditions of this campaign. The 89 degree angle pin has the lowest weight loss of all the pins for most measurements, although the weight loss is close to that of the 76 degree angle and high hardness edge pin after 250 shots. Again, the weight loss of angle pins appears to increase with lower angles of impingement.

Figure 5.15 Variation and curve fit of weight loss with number of shots for the pins used in campaign 6.

5.2.7 Campaign 7

A low hardness edge pin, a high hardness edge pin, a 69 degree angle pin and a 89 degree angle pin were used in this high velocity campaign (gate velocity is 65 m/s, and superheat is 150 F). The weight loss versus number of shots along with the best curve fits for these pins is shown in Figure 5.16. The best curve fits for the pins are given by

$$\text{Wt Loss (in mg)} = 41.37 (-0.077 + 10.07 \times 10^{-4} \text{ N}) \quad 5.7a$$

for the low hardness edge pin (pin a)

$$\text{Wt Loss (in mg)} = 36.14 (.311 + 6.89 \times 10^{-4} \text{ N}) \quad 5.7b$$

for the high hardness edge pin (pin b)

$$\text{Wt Loss (in mg)} = 42.2 (.161 + 8.39 \times 10^{-4} \text{ N}) \quad 5.7c$$

for the 69 degree angle pin (pin c)

$$\text{Wt Loss (in mg)} = 24.1 (-.11 + 11.1 \times 10^{-4} \text{ N}) \quad 5.7d$$

for the 89 degree angle pin (pin d)

As can be seen the 89 degree angle pin has the lowest weight loss after all measurements. The weight loss curve fit for this pin is much lower and nearly parallel with the 69 degree angle pin, indicating that this relationship will hold for all measurements. Thus, again a lower angle of impingement results in a greater weight loss due to erosive wear. The weight loss measurements for the other 3 pins do not form a clear cut pattern, as indicated by the intersecting curve fits. After 1,000 shots, the 69 degree angle pin and the low hardness edge pin have nearly identical weight loss, followed by the high hardness edge pin, which has a lower weight loss. However this pattern does not hold at earlier measurements as indicated by the intersection of the curve fits of these three pins. Like in

campaign 6, a possible explanation for this may be that all the soldering pick-up may not have been removed in earlier measurements. The measurement after 1,000 shots is however accurate and is used for further studies regarding the effect of metal velocity and angle of metal attack.

Figure 5.16 Variation and curve fit of weight loss with number of shots for the pins used in campaign 7.

5.2.8 Campaign 8

A low hardness edge pin and a high hardness edge pin were used along with a 89 degree and 83 degree pin in this campaign under nominal conditions of 36 m/s gate velocity and 150 F superheat. The weight loss versus number of shots for this set of pins is plotted in Figure 5.17. The best curve fit for the various pins is given by

$$\text{Wt Loss (in mg)} = 11.59 (-.128 + 11.28 \times 10^{-4} \text{ N}) \quad 259 \quad 5.8a$$

for the low hardness edge pin (pin a)

$$\text{Wt Loss (in mg)} = 6.04 (-0.205 + 10.25 \times 10^{-4} \text{ N}) \quad 5.8b$$

for the high hardness edge pin (pin b)

$$\text{Wt Loss (in mg)} = 3.82 (.33 + 6.7 \times 10^{-4} \text{ N}) \quad 5.8c$$

for the 89 degree angle pin (pin c)

$$\text{Wt Loss (in mg)} = 7.21 (.341 + 6.59 \times 10^{-4} \text{ N}) \quad 5.8d$$

for the 83 degree angle pin (pin d)

As expected, the low hardness edge pin has a greater weight loss than the high hardness edge pin at all measurements as indicated by diverging and almost parallel curve fits (similar non-dimensional slopes). The 83 degree angle pin also exhibits a greater weight loss than the 89 degree angle pin as is indicated by the nearly parallel curve fits (as the slopes of the non-dimensionalized curve fits are close). Thus again, the greater weight loss is experienced by the pin with the lower angle of impingement. The low hardness edge pin has a larger weight loss than the 83 degree angle pins after 1,000 shots although the trend does not hold at a low number of shots (as can be seen from the different non-dimensionalized slopes). As the weight loss at low number of shots is very low, few conclusions can be drawn of weight loss ratios, at these early measurements.

Figure 5.17 Variation and curve fit of weight loss with number of shots for the pins used in campaign 8.

5.2.9 Conclusions from time variation of weight loss studies

Some conclusions drawn from these weight loss vs number of shots studies are

- A linear curve fit provides a good correlation of the variation of weight loss with time for almost all the pins in all the campaigns. The general nature of this curve fit is given by

$$\text{Wt Loss (in mg)} = C_1 (C_2 + C_3 N) \quad 5.9$$

where C_1 (mg) is the weight loss after a 1,000 shots, C_2 is the initial normalized weight loss (no units) , and C_3 (1/shots) is the slope of the weight loss curve.

As can be seen from this curve fit, the greatest weight loss for any measurement, appears to take place after the first 250 shots (C_2 is positive), although in some cases of milder erosive conditions, C_2 is negative. After 250 shots, the weight loss is linear in almost all cases. This indicates that a large weight loss takes place initially in the first stage of erosion, before the erosion rate stabilizes to form a linear variation with number of shots. This is contrary to conclusions in erosion studies of other applications which hold that very little weight loss takes place in the initial incubation period.

- In most campaigns, the linear curve fits of the weight loss versus number of shots, for the pins used in the campaign are nearly parallel. There are however a few exceptions to this trend at large weight losses, possibly because of unremoved soldering at early measurements.
- For extremely low weight loss campaigns, there is a greater relative error associated with weight loss measurements because of the accuracy of the measuring instrument. Therefore a campaign of 250 shots or so may not be enough to obtain sufficiently accurate data under such conditions.
- For extremely high weight loss, some variations in relative weight loss from 250 to 1,000 shots were observed. This may however be a function of unremoved excess soldering in some pins that was caused by time constraints.
- To determine whether linear curve fits in different campaigns remained parallel to each other, various pin weight loss measurements from different campaigns were plotted together.

This would help determine whether relative or absolute weight loss ratios obtained at 250, 500, or 750 shots were identical to that obtained at 1,000 shots. Four such comparisons accounting for most of the pins used in the campaigns are plotted in Figures 5.18 and 5.19. Figure 5.18a shows the weight loss vs number of shots for all low hardness pins used in the velocity campaigns (1, 3, 4, 5, 7, 8) and Figure 5.18b similarly shows the weight loss vs number of shots for all high hardness pins used in the same velocity campaigns (1, 3, 4, 5, 7, 8). The relative ordering by weight loss of the pins remains mostly identical between 750 and 1,000 shots for Figure 5.18 a (low hardness velocity edge pins) and Figure 5.18b (high hardness velocity edge pins). However the weight loss curves do intersect for a few pins. Figure 5.19a, shows the weight loss vs number of shots for all angle pins used in the campaigns (3, 4, 5, 6, 7, 8) and Figure 5.19b similarly shows the weight loss vs number of shots for all pins used in the temperature campaigns (1, 2, 6). The weight loss curve fits for the angle pins (Figure 5.19 b) do not in general intersect when the weight losses are not similar, and the relative ordering of pins by weight loss in general remains identical between 500, 750 and 1,000 shots. However there appears to be large variability in the curves for the temperature pins (Figure 5.19a).

As can be seen in Figures 5.18 and 5.19, as the number of shots increases the relative ordering of pins by weight loss becomes more distinct and 500 or 750 shots may be sufficient to obtain qualitative measures of relative weight loss. However, there is an intersection of a number of these curves for all the cases considered, especially where weight loss measurements are close together. Thus it appears that no single weight loss vs number of shots relationship can be obtained that approximates the weight loss behavior of all the pins. Nevertheless mean normalized curve fits for the pins shown in Figures 5.18 a, b and 5.19 a, b were obtained and are outlined below.

The best weight loss relationship for various similar pins used in the campaigns is given by

$$\text{Wt Loss (in mg)} = C_1 (.131 + 8.94 \times 10^{-4} N) \quad 5.10a$$

for the low hardness velocity edge pins (1a, 3a, 4a, 5a, 7a, 8a)

$$\text{Wt Loss (in mg)} = C_1 (.120 + 9.26 \times 10^{-4} N) \quad 5.10b$$

for the high hardness velocity edge pins (1b, 3b, 4b, 5b, 7b, 8b)

$$\text{Wt Loss (in mg)} = C_1 (.39 + 6.2 \times 10^{-4} N) \quad 5.10c$$

for the high hardness temperature pins (1b, 2b, 6b)

$$\text{Wt Loss (in mg)} = C_1 (.20 + 8.27 \times 10^{-4} N) \quad 5.10d$$

for the angle pins (3-8 c, d)

where C_1 is the weight loss after 1,000 shots.

A plot of the normalized weight loss for each of these pin conditions and the mean normalized weight loss vs number of shots is plotted in Figure 5.20. As can be seen from the figures and from equations 5.10 a and b, the weight loss versus number of shots for low and high hardness velocity pins are very close together. The angle pins also have a mean normalized weight loss that is close to these pins. However, the temperature campaigns appear to have a different weight loss vs number of shots relationship as compared to the other pins, even though the best curve fits for these pins was linear.

Figure 5.18 Weight loss versus number of shots and best curve fit for velocity campaign edge pins. (a) low hardness pins and (b) high hardness pins

(a)

(b)

Figure 5.19 Weight loss versus number of shots and best curve fit for (a) temperature edge pins and (b) angle pins.

Figure 5.20 Best curve fits of normalized weight loss for various pin combinations used in Campaigns 1-8.

5.3 EFFECT OF METAL VELOCITY

As discussed in Chapter II, liquid velocity appears to play a significant part in erosion by liquid drops and jets. The relationship between liquid velocity and erosion can in general be represented as

$$E \propto (V - V_1)^{n1} \quad 5.11a$$

$$E \propto (V)^{n2} \quad 5.11b$$

where V is the impact velocity, V_1 is a threshold velocity below which no erosion occurs, n_1 and n_2 are experimentally determined exponents, and E is a measure of erosion rate. Various researchers have found the value of n_1 to be between 2 and 3 for a variety of materials and n_2 to be about 5 as discussed in Chapter II. Die casting erosion however does not take place under the controlled erosive conditions (no corrosive or temperature effects) that lead to these predictions. To evaluate the effect of impact velocity on die casting wear, variations of edge profiles and weight loss with velocity were analyzed, and the results are described in this section.

As described in section 5.1, the impact velocity at the pins is the same as the gate velocity because of jetting. Therefore the gate velocity can be used to evaluate the effect of impact velocity on die erosive wear. As also described earlier, there were two measures of erosive wear chosen for this study; (a) weight loss of pins and (b) wear profile of pins. Variation of impact velocity with the erosive wear evaluated using either measure is described here.

5.3.1 Effect of impact velocity on weight loss

Low hardness and high hardness edge pins were used in 6 velocity campaigns and weight loss data is available at 250, 500, 750 and 1,000 shots for all these campaigns. The weight loss versus impact velocity for these pins after 250 shots, 500 shots, and 750 shots is shown in Figures 5.21, 5.22 and 5.23 respectively. The weight loss of these pins after a 1,000 shots is plotted in Figure 5.24. The results after a 1,000 shots represent the most accurate weight loss readings. This is because the percentage effect of random variables and experimental error in the weight loss is the lowest after 1,000 shots. Also as discussed, measurement errors due to unremoved soldering occurring at earlier measurements are reduced after a 1,000 shots. So most of the conclusions drawn from the weight loss readings are after a 1,000 shots.

Figure 5.21 Weight loss after 250 shots vs impact velocity for edge pins.

Figure 5.22 Weight loss after 500 shots vs impact velocity for edge pins.

Figure 5.23 Weight loss after 750 shots vs impact velocity for edge pins.

Figure 5.24 Weight loss after 1,000 shots vs impact velocity for edge pins.

Nevertheless, as can be seen in these plots, the variation of weight loss with velocity is almost linear for both the low hardness and high hardness pin cases, at all measurements. As expected the low hardness pins have a greater weight loss than the high hardness pins. The curve fits of the low and high hardness pins are parallel to each other in the 1,000 shot case indicating that the effect of hardness does not change with impact velocity. Approximately similar patterns are seen after 750 shots, although there appears to be a large variation from the linear curve fits after 250 and 500 shots as seen in Figures 5.21 - 5.22. This is again because measurement and experimental errors may account for a greater percentage of weight loss measurements after 250 and 500 shots as compared with that after 750 and 1,000 shots.

The best curve fit for the low hardness edge pin after a 1,000 shots can thus be represented by

$$C_1 = 0.88 (-18.14+V) \quad 5.12a$$

and for the high hardness edge pins by

$$C_1 = 0.84 (-25.23+V) \quad 5.12b$$

where C_1 is the Wt Loss after 1,000 shots (in mg) given in equation 5.9. The first term in the bracket may represent the threshold velocity and the term outside the bracket represents the slopes of the two lines, which indicates that the effect of hardness does not change significantly with velocity. Since there also does not appear to be a very large variation in weight loss between low and high hardness pins, one of the experimental assumptions that small variations in hardness can be ignored for the purposes of this experiment is valid.

Expressions at lower number of shots are not reported here, because measurements after 1,000 shots represent the most accurate weight loss

readings in the campaign. The variation of weight loss with velocity for the angle pins is considered in section 5.5.

5.3.2 Variation of wear profiles with velocity

As discussed a second measure of die erosive wear is the wear profile of the pins which gives a more spatial distribution of erosive wear as compared with the weight loss. The variation of the edge profile of the low hardness pins with velocity is seen in Figure 5.25. As can be seen, the wear profile is deeper at larger velocities, although the distinction is less clear at lower velocities. Similarly the edge profiles of the high hardness pins for various gate velocities are plotted in Figure 5.26. Again, the wear profile depths are greater at higher velocities, and similar to the low hardness case, the effects are less clear cut at lower velocities. These plots however clearly indicate that an increase in velocity increases the extent of the localized erosive wear seen.

To quantify the effect of wear profiles, a single scalar value had to be chosen to represent each wear profile. The measure chosen was the maximum depth of the erosion pit of the wear profile. There is not a distinct pit seen at low velocities because of low wear, and in some case the erosion pit is not smooth. Nevertheless the measured maximum depth of erosion represents a reasonable scalar measure of the erosive wear profile. Since the erosive wear profile is mostly created by erosion effects, the wear depths provide the best measure of erosive wear quantification. The wear depths of upto 1.5 mm seen in some cases after just 1,000 shots illustrate the dimensional tolerance problems that will be encountered in production under severe conditions. The values used for the maximum erosion depth for each pin were detailed in Chapter IV.

Figure 5.25 Edge profiles of the low hardness edge pins used in the velocity campaigns.

Figure 5.26 Edge profiles of the high hardness edge pins used in the velocity campaigns.

Thus the maximum wear depth vs velocity is plotted for both the low hardness and the high hardness edge pins in Figure 5.27. As can be seen a linear curve fit appears to provide the best curve fit for the variation of wear depth with velocity for both low and high hardness pins. The curve fits for the low and high hardness pins while nearly parallel, appear to diverge at high velocities, indicating that the effect of pin hardness is more significant at higher velocities. However a greater error in relative weight loss at lower velocities may account for this variation. Nevertheless the edge profile results show that wear depths vary linearly with impact velocity.

The best curve fit of the wear depth vs impact velocity for the low hardness edge pin after a 1,000 shots can be represented by

$$C_1 = 3.69 (-24.17+V) \quad 5.13a$$

and for the high hardness edge pins by

$$C_1 = 2.58 (-26.72+V) \quad 5.13b$$

where C_1 is the wear depth in 10^{-2} mm, and V (m/s) is the gate velocity. Like in the weight loss curve fits, the first term represents hypothetically, the threshold velocity below which no erosion is seen. Also C_1 can be substituted in equation 5.9 to obtain a variation of maximum wear depth with the number of shots. Although equation 5.9 was obtained from weight loss variations with time, wear depth can be substituted for weight loss in the non dimensionalized form of this equation. This assumption will be valid as long as the weight loss is primarily caused by edge erosive wear. As can also be seen, the slope of the low and high hardness pins is different, as indicated by the non parallel nature of the curve fits. This indicates that the effect of hardness on erosion may change with velocity. However the slopes are reasonably close.

Figure 5.27 Maximum wear depth of the edge profile after 1,000 shots vs impact velocity for edge pins.

5.3.3 Comparison of spatial variation of wear and velocity

Velocity was used in the above studies for a single value of the experimental wear (either weight loss or maximum wear depth). Spatial distributions of wear along the edge of the pin were therefore compared with spatial distributions of normal velocity along the pin edge obtained from numerical simulations, to further quantify the effect of metal velocity. One such comparison is shown in Figure 5.28b. In this plot the mean wear at a pin edge location for the four pins used in the pilot test (Figure 5.28a), were non-dimensionalized by dividing by the maximum wear depth. The normal velocity distribution along the pin edge obtained by numerical simulation was non-dimensionalized, by dividing the normal component of velocity at the location by the maximum normal velocity along the pin edge. Thus both the normalized weight loss ratio and the normalized wear profile (wear ratio) have a maximum value of 1. As can be seen in Figure 5.28b, there is a one to one correspondence

between the velocity profile and wear profile, with both curves being nearly coincident. This again indicates that metal velocity is a primary mechanism of erosive wear and that there exists a linear relationship between impact velocity and erosive wear.

To further illustrate this comparison of spatial wear distribution and spatial velocity distribution, wear profiles of low hardness pins used in various campaigns (1a, 4a, 5a, 7a, 8a) were again non-dimensionalized by dividing by the maximum wear depth for each campaign (Figure 5.29a). Pin 3a (velocity =27 m/s) is not included, because there is negligible wear and no definite wear profile on that pin. As can be seen in Figure 5.29a, the non-dimensional wear profiles while not identical are qualitatively similar. The mean normalized wear profile of the pins shown in Figure 5.29a and the normalized velocity profile are plotted in Figure 5.29b. As can be seen, the two curves are nearly identical, especially close to the top, indicating a linear relationship between normal velocity and wear.

Normalized wear profiles for the higher wear, high hardness pins (ib, iib, 1b, 2b, 5b, 7b) are shown in Figure 5.30a. A similar comparison between the mean normalized wear profile of these high hardness pins and the normalized velocity is shown in Figure 5.30b. As can again be seen the two curves are nearly identical especially close to the top of the pin, indicating a linear relationship between velocity and wear.

Thus the comparisons of non-dimensionalized wear profiles and normal impact velocity profiles indicates a linear relationship between wear depth and impact velocity. This is similar to the experimental variation of impact velocity with wear depth which also indicates a linear relationship between wear depth and velocity. These graphs thus show that prediction of wear profiles is possible. More over since, the ratio of maximum velocity to maximum wear depth is the same as given by equation 5.13 (which gives in effect the variation of the maximum impact velocity versus maximum depth), this plot indicates that the wear profile predicted by the numerical simulation is almost identical to the experimental wear

profile. Such a comparison between, the predicted wear from numerical simulations and the measured wear is shown in Figure 5.31. As can be seen the predicted and measured wear are nearly identical. This comparison was for a low hardness pin in campaign 1 (velocity = 50 m/s, and superheat = 150 F). Negative values of wear predicted for low velocities by equation 5.13 are not physically possible and are therefore replaced by zero predicted wear for this comparison.

Thus verification of the numerical prediction of wear profiles, which was one of the objectives of this research has been attained, even if only for a simple geometry.

The numerical simulations also show that there is less than a 2 F variation in the surface temperature along the edge of the pin during filling. This is despite the severe local heating that may be expected to occur due to the impact of the jet on a small area. A simple explanation of this behavior is that the jetting conditions that may cause localized heating, take place during the milliseconds of filling as compared with the 5 seconds of solidification and the 30 seconds of die open time. Thus the numerical simulations also show that the erosion pits are a result of die material removal by physical action of high velocity metal and not by increased diffusion due to localized heating of the die material. Thus physical impact of high velocity is a primary mechanism of die erosion in die casting dies.

Figure 5.28 Comparison of the normalized mean edge wear obtained from the pilot tests (a) with the normalized normal velocity along the pin edge obtained from the numerical simulations.

Figure 5.29 Comparison of the normalized mean edge wear obtained from the low hardness edge pins of the campaigns with significant edge wear (a), with the normalized normal velocity along the pin edge obtained from the numerical simulations.

Figure 5.30 Comparison of the normalized mean edge wear obtained from the high hardness edge pins of the campaigns with significant edge wear (a), with the normalized normal velocity along the pin edge obtained from the numerical simulations.

Figure 5.31 A sample comparison of predicted and measured wear profiles.

Thus the following conclusions were drawn from the velocity simulations.

- Velocity and hardness studies of the effect of velocity on weight loss and edge profiles indicate that there is a linear increase in weight loss or erosion depths with velocity for both the low and high hardness pins. Low hardness pins have a lower weight loss than the high hardness pins in all cases, although the difference is not very large (despite the large difference in hardness). Thus the assumption that 1,000 shots may be insufficient to study small variations in hardness is valid.

- Study of the variation of weight loss after 250, 500 and 750 shots indicate that while the same linear curve fits are predicted, the variation from this curve fit is quite large after 250 and 500 shots. The 750 shot curve fit however has only a small variation as compared to the 1,000 shot case. This indicates that 750 shots may be a minimum number of shots that are required for quantitative wear model development for this experimental set up.
- The ratio of weight loss between the low and high hardness pins appears to be a constant with velocity, while the ratio of edge wear between the low and high hardness pins increases slightly with velocity indicating that the relationship between hardness and edge wear may be dependent on velocity, at high velocities.

This is in contrast to the laboratory simulative studies of liquid drop erosion, where quadratic and cubic relationships between wear and velocity are obtained. Metal loss by diffusion, and temperature effects may account for this erosive behavior in die casting. This result also illustrates the danger in using laboratory simulative tests in applications different from the wear conditions simulated in the experiment.

- Relationships between the weight loss and impact velocity (equation 5.12) and between the maximum wear depth and impact velocity (equation 5.13) were obtained from these curve fits. The general nature of the relationship obtained is given by

$$C_1 = C_2 (-V_0 + V) \quad 5.14$$

where C_1 is the wear measure (weight loss or wear depth), C_2 is the slope of the wear measure vs velocity curve, V_0 is the threshold velocity below which no wear occurs after 1,000 shots and V is the impact velocity (gate velocity in this experimental set up). Coupling this expression with the time variation of erosion

expression (equation 5.9), the value of the wear measure after N shots for a gate velocity V can be obtained. It must however be emphasized that the values of the constant determined are unique to the alloying, spraying and geometry conditions of this experiment. The actual values of these constants will vary with production practice, die geometry, alloying composition etc., and the values of the above constants will have to be determined for each set of unique operating conditions.

However in most production facilities a common set of production practice conditions may exist along with a common set of alloys. Once a set of these constants are determined, these values may be applied across different geometries and process conditions for that production facility.

- Wear profiles in different experiments were normalized by dividing by the maximum wear depth of each pin. It is seen that for all pins with significant erosion, the non-dimensionalized wear patterns are qualitatively similar, with large erosion pits of equivalent width seen in front of the gate.
- Comparisons of normalized edge profiles with normalized velocity profiles showed a nearly one to one match between the velocity profile and the edge profile. Thus the numerical simulations also predict the linear relationship between velocity and wear depth outlined in equation 5.14. This indicates that the numerical system can be used to predict wear at least qualitatively under production conditions.

5.4 EFFECT OF PIN GEOMETRY

Like the edge pins, it was hoped that wear profiles of the angle pins could be used as a measure of die erosive wear. However, as discussed in Chapter IV, there are no distinct erosion pits seen in the angle pins and

the wear profile is nearly uniform in most cases. This is in contrast to the severe erosion pits seen in the edge pins in front of the gates. An explanation for this wear pattern can be provided by the flow patterns around both pins obtained from the numerical simulations. As seen in Figure 5.7 and 5.8, in the angle pins the metal from the gate strikes the pin and does not continue to flow through in the same direction, as the width of the face is greater than the width of the jet. Instead, the jet splashes against the pin face and then moves along toward the top and bottom of the pin as shown in Figure 5.8. Thus after splashing against the pin in front of the gate, metal flows along the entire face of the pin. This is similar to the "impingement erosion" (Barton, 1965) of die walls case seen in Chapter II.

On the other hand in the edge pin case, the metal hits the edge of the pin, in the form of a jet. The metal jet separates because of the pin geometry, and it continues to flow through to the back of the cavity. Thus the jet impacts the pin at near normal impact, and the pin causes flow separation of the jet. There is some flow along the top and bottom of the pin edge which also causes some erosion along the length of the pin. Although not exactly identical, this simulation seems somewhat analogous to "gate erosion" described in chapter II, due to the flow of high velocity metal parallel to the die surface. However there is normal impact in this case unlike gate erosion.

Thus there appears to be two distinct kinds of flow patterns in either pin. How does this account for the different erosion patterns seen in both pins? In both cases a similar area of the pin is impacted by a near normal jet of identical velocity. So, why is there a dramatically different erosion pattern between the two cases? A possible explanation is the two stage micro-machining, micro-ploughing or solid particle erosion models developed by various researchers who have studied solid particle erosion (Tilly, 1973; Maji & Sheldon, 1979).

According to these two stage models, erosion takes place in a two stage process. In the first stage impact of particles causes a damage of the surface of the pin. Flow of high velocity metal parallel to the surface will remove these damaged surfaces (such as lips and platelets) causing erosion. Thus by an adaptation of this theory to the case of edge pin erosion, normal impact of the pin by a jet causes a damaged surface on the pin. As will be discussed in section 5.5 and 5.6, damage is significantly enhanced by the presence of solid particles entrapped in the melt. Tangential high velocity metal continually removes this damaged surface to cause pin erosion. In the angle pin case on the other hand, the normal impact of high velocity metal, while it causes the same initial damage on the pin surface, does not result in severe erosion because there is no high velocity tangential flow to remove the damaged metal on the surface. Thus the presence of large velocity tangential flow appears to be a requirement for severe localized erosion.

Even though the localized wear on the angle pins is not as severe, a greater area of the angle pin face surface is exposed to flow of metal along the face. This accounts for a larger area of the surface being eroded and a comparable weight loss, without comparable localized erosion. This may explain why cores and die geometry regions where fluid jets are split suffer large localized erosion loss as compared to die wall regions with smaller jet impact conditions but where jet splashing occurs.

So the essential condition in the edge pin campaign is that there exists high velocity near normal metal impact, and the flow of high velocity metal tangential to the surface of the pin at that location. These conditions will encourage the removal of deformed particles created by the normal impact, with the high velocity tangential flow. This observation may explain why cores or die regions where flow separation occurs, exhibit greater weight loss than die walls where "washout" (a normal impact of high velocity metal, without any tangential flow of high velocity metal) conditions prevail.

On the other hand in the angle pins, while near normal impact of high velocity metal occurs, splashing on the die surface results in lower velocity metal moving tangential to the pin surface. Thus any deformation or damage that occurs at the jet impact region may not be easily removed by the lower velocity metal that flows tangential to the surface. This is a possible reason why "washout" conditions do not result in severe localized erosion.

However, while localized erosion may not be as significant in the angle pins, the flow patterns results in metal flowing over the larger area of the entire pin face. Thus the entire pin face is eroded because of these flow conditions, even though jet impact regions are less severely damaged as compared with the edge pin case. Thus more uniform erosion over a greater area results in equivalent weight losses between the angle and edge pins, although erosion pit depths are not comparable.

Thus two distinct forms of die erosion have been identified; (a) "Core" erosion, where near normal impact and high tangential velocities both prevail and (b) "washout" erosion, where there is normal impact but not high velocity tangential flow. The core form of erosion appears to account for the severe localized erosion seen, where flow separation occurs in a die cavity.

5.5 EFFECT OF ANGLE OF METAL IMPACT

In campaign iii (Chapter IV), 83, 76, 69 and 65 degree angle pins were used as the four front row pins in a campaign. The weight loss after a 1,000 shots for these pins is shown in Figure 5.32. As can be seen the maximum weight loss does not occur at 90 degrees as would be expected if liquid metal were to attack the core pins. Instead the maximum erosion appears to take place at an impact angle of 72 degrees. As discussed in Chapter II, research on liquid erosion has shown that the maximum erosion for liquid impact takes place at 90 degree impingement (Brunton & Rochester, 1979), as seen in Figure 2.13. On the other hand studies of solid particle

impact have shown that for ductile material erosion, the maximum erosion takes place at low angles of metal impact, typically between 20 and 30 degrees (Ives & Ruff, 1979) as seen in Figure 2.22. Thus a possible explanation of the experimental variation of the angle of metal impact is that, the metal entering the cavity and impacting the pins is not completely liquid, but is instead partially solidified or contains solid particles mixed in the liquid stream. The erosion caused by these solid particles results in increasing erosion, with a decreasing angle of metal impact. It is widely accepted that at the time of injection 15-20% of the metal entering the cavity is solidified even at 150 F superheat in the furnace (Lindsay & Wallace, 1972), because of heat losses in the ladle and sleeve. A detailed explanation of the causes of solid particle formation in the melt is given in section 5.6.

Figure 5.32 Dependence of erosive wear rate of test pins on attack angles of molten aluminum alloy. The Weight loss of the pins (campaign iii) is measured after 1000 shots.

To further illustrate the effect of angle of metal impact , 89, 83, 76 and 69 degree angle pins were utilized in campaigns 3-8. As discussed 2 of these 4 pins were used in each of these campaigns, at the central pin locations. As also discussed in Chapter IV, the 4 angle pins were used in a manner that would result in all possible combinations of pins being used in the campaigns. Some of the conclusions drawn from these campaigns are discussed below.

- In every campaign, the lower angle pin had a greater weight loss than the higher angle pin except in campaign 5 where the 76 degree angle pin had a slightly greater weight loss than the 69 degree angle pin. This is similar to the results of campaign iii discussed above in that a reduction of impact angle causes an increase in erosion. The difference for campaign 6 where the 76 degree pin has a slightly greater weight loss than the 69 degree angle pin may be because of the plateau at 72 degrees seen in campaign iii (Figure 3.31), or it may be due to experimental variations.
- To evaluate the effect of angle of metal impact across different campaigns the weight loss of the angle pins were normalized by dividing the weight loss after 1,000 shots of the angle pin by the weight loss of the low hardness edge pin (which has an identical heat treatment). The normalized weight loss obtained for different angles is plotted versus attack angle in Figure 5.33 As can be seen the pattern is very similar to that seen in campaign iii, with a reduction in attack angle resulting in an increase in erosive wear.
- A curve fit through the mean of the normalized weight loss is also shown. Again the maximum weight loss appears to take place around 70 degrees, although no definite conclusions can be drawn, because there were no campaigns conducted below 69 degrees. Thus this value could be a local maximum, and no conclusions can be made regarding a global maximum until more angles were

considered. As can also be seen, there is some variability in the normalized weight loss values for each angle, but that is to be expected, because of the different conditions in each campaign.

- A graph of the weight loss of the different angle pins versus velocity is shown in Figure 5.34. As can be seen, for a given velocity, the lower angle pin has the greater weight loss. Also for a given angle, the weight loss varies linearly with number of shots, although there are very few data points. However, there is a large variability in data indicating that the coupled angle velocity relation is complex. More experiments are required to obtain such a coupled relationship. Nevertheless a preliminary conclusion from this data is that velocity effects appear to be more critical than angle effects for erosion.
- Thus the angle of attack studies suggest that a decrease in impact angle increases the extent of erosive wear possibly because of solid particles present in the melt. The maximum erosion appears to take place around 72 degrees, although more experiments at different angles will have to be conducted to determine this accurately. The coupled relationship between velocity and angle of metal impact appears to be complex one and more experiments are required to evaluate this effect.

5.6 EFFECT OF METAL TEMPERATURE

Studies were also conducted to determine the effect of molten metal temperature on erosive wear of wear pins under production conditions. In Chapter IV, a brief discussion of the variation of erosion with metal temperature for a few campaigns was discussed. The variation of erosion with metal temperature for all the temperature campaigns conducted is described here.

Figure 5.33 Weight loss ratios of angle pins versus impingement angles. The weight loss ratios are calculated by dividing the weight loss of the angle pin by the weight loss of the low hardness edge pin in the same campaign.

Figure 5.34 Variation of weight loss of different angle pins with velocity.

As previously discussed, temperature campaigns were conducted at superheats of 60 F, 100 F, 150 F, 200 F, and 260 F. Low hardness, high hardness and nitrided (hardness 62 Hrc) pins were used in these campaigns. The edge profiles of the high hardness pins at these superheats is plotted in Figure 5.35. As can be seen, a decrease in temperature causes an increase in erosion, with the largest erosion of all campaigns occurring at 60 F superheat. In contrast there appears to be little or no severe localized erosion for a 260 F superheat. Similar patterns for the low hardness and nitrided pins are shown in Figures 5.36 a and b. However there are fewer data points for these pins.

Figure 5.35 Edge profile of high hardness edge pins used in the temperature campaigns.

Figure 5.36 Edge profile variations of different nitrided pins (a) and low hardness edge pins with temperature.

This is a surprising result as previous studies of the effect of metal temperature using dip tests (Yu & Shivpuri, 1994; Yu 1994; Malm & Tidlund, 1979) have shown that an increase in temperature causes an increase in the extent of corrosive wear (Figures 2.7-2.9). A possible explanation for this difference between a production scale test and the dip tests is that these dip tests do not consider the effect of physical impact of high velocity molten metal (metal velocity is ignored in these tests). This does not however explain, why a decreasing temperature causes an increase in die erosion. A possible explanation for this behavior is given below.

The high velocity molten metal as it is held in the furnace, ladle and shot sleeve loses heat, and the metal as it is injected into the cavity has a percentage of pre-solidified metal particles, as previously described. These solid particles are created by heat losses, not only on the metal at the top of the furnace that is exposed to ambient air, on the metal in the ladle region exposed to the ambient air and on the walls of the funnel, but also on the region of the metal exposed to the walls of the sleeve and ambient air in the sleeve. The distribution of these solid particles in the melt is not uniform, as typically the metal in contact with the sleeve wall, ladle wall or ambient air gets solidified at a different rate than in the interior of the metal in the sleeve or ladle. Beside the solid particles created in the melt, ladle and funnel a skin is formed on the surface of the metal exposed to the walls of the shot sleeve. The formation of a skin in the metal near the walls of the sleeve is likely to happen even if the average metal temperature is high, as there is a very large temperature difference between the metal and the sleeve wall or ambient air. This results in a large heat flux that results in the solidified skin that is injected into the cavity. The size of this skin increases with holding time and reduced superheat. The formation of this skin is visually observable in these campaigns, even if it was not quantified. This illustrates one of the problems with the die casting process. Even as the molten metal is injected into the cavity in milliseconds to prevent premature

solidification, a lot of heat is lost in the few seconds the metal is in the ladle and sleeve before injection.

The percentage of these pre-solidified particles and skin increases with a decreasing superheat. Thus a possible explanation of the erosion behavior with temperature, is that a large extent of erosion damage is likely caused by the impact of the solidified particles in the high velocity metal flow that impacts a die wall. As the metal superheat goes down, the solid fraction in the melt increases as a larger solidified skin forms in the metal in the sleeve and ladle. When the superheat is extremely high, there is likely to be very little solid particle formation, as the heat flux across sleeve walls, ladle walls and ambient air may not be large enough to cause solidification skins. This would result in a low percentage of solid particles in the melt and consequently result in the low erosive wear seen at high superheats. This theory would clearly explain the erosion temperature relationship seen. The formation of solid particles prior to injection is commonly observed in commercial die casting operations, due to the time involved in transferring the metal from furnace to injection into the cavity. This study thus shows that the heat losses in molten metal transport and hold not only result in casting problems like premature solidification, but also result in severe die erosion.

To quantify the effect of increase in wear with decrease in superheat, the maximum depth of erosion was plotted against metal superheat in Figure 5.37. As can be seen there is a linear decrease in erosion depth with a increase in temperature.

The variation of maximum wear depth with melt superheat is given by

$$C_1 = 152.5 - 0.513 T \quad 5.15$$

where C_1 (10^{-2} mm) is the maximum wear depth after 1,000 shots and T is the metal superheat (F). The equation indicates that there will be no erosive wear profile at $T = 152.05 / 0.513$ F. However, this does not

preclude the greater diffusion losses that take place at such a high superheat. A more appropriate relationship may be between the percentage of solid particles in the melt entering the cavity versus wear depth. However the numerical model cannot be used, as a filling time of milliseconds results in very little melt solidification during filling itself. The heat losses during the ladle pour and sleeve holding times cause the excessive solid particles in the melt that appears to cause die erosion. However this is a more complex function to determine as there are a large number of possible heat loss mechanisms, which result in varying sizes and distributions of solid particles. Ladle pour and die hold time also vary quite extensively from shot to shot.

Figure 5.37 Maximum wear depth vs superheat of the high hardness pins used in the temperature campaigns.

Similar to the velocity case, C_1 can be substituted in equation 5.9 to obtain a variation of maximum wear depth with the number of shots for a given superheat. Although equation 5.9 was obtained from weight loss variations with time, wear depth can be substituted for weight loss in the non dimensionalized form of this equation. This assumption will be valid as long as the weight loss is primarily caused by edge erosive wear.

Coupling between velocity and temperature variation is not reported here because very few coupling experiments were conducted. Experimental evaluations to study the effect of superheat at different velocities will help complete this coupling.

Thus erosion by the impact of partially solidified melt appears to play a significant role in die erosion. The corrosive-diffusion effects also play an important role in die erosion and soldering as suggested in the corrosion dip tests. The damage caused by physical impact of high velocity solidified particles may however be more critical in causing severe die erosion. The effect of corrosion in die erosion may not however be insignificant. A plot of the weight loss vs metal superheat for these campaigns is presented in Figure 5.38. As can be seen, there is no longer a linear decrease in erosion rate as the metal superheat increases. Instead, there is a decrease in erosion rate with increase in superheat upto 150 F superheat, after which the weight loss tapers off and does not continue to decrease at the same rate as before. In fact there is an increase in erosion after 200 F superheat. This may be because severe heating of the pin at high superheats increases the diffusion of H-13, while there are no local erosion pits because of the absence of significant solid particles. Since diffusion losses can take place over the entire pin surface area, the extra weight loss that this diffusion loss creates, makes up for the reduced weight loss due to the lack of localized erosion.

Thus while increased temperature reduces severe localized erosion, diffusion rates cause a more uniform erosion over the entire pin area. Thus increasing temperature to reduce erosion is not a problem free solution. While severe localized erosion may be reduced by this approach,

corrosion while not as significant in casting will be enhanced. This will also increase soldering problems in die casting.

Figure 5.38 Variation of weight loss for the temperature edge pins with metal superheat.

In conclusion, these temperature studies show that the significant factor which affects erosion is the percentage of solids in the melt. It was observed that there was a linear decrease in erosion depth as a function of temperature. As the temperature decreases, the solid fraction and consequently the amount of solid particles in the melt increases. The impact of more solid particles on the pins, at the same velocity thus causes greater wear at the lower temperature. Although the die is likely to be cooler at a lower temperature thereby reducing metal diffusion, the

increased wear due to the impact of more solid particles, appears to be more significant to the overall wear.

These results thus show that the most significant driver of die erosion is the impact of high velocity solid particles on the die. However at higher melt temperatures increased diffusion between the melt and die walls takes place as the die temperature also increases. This will result in increased corrosion losses at higher temperatures, which however do not result in severe pits. Soldering also increases at such temperatures. Thus reduced erosion at higher temperatures must be balanced with greater diffusion erosion losses

5.7 SUMMARY

Numerical simulations of the erosive wear die cavity were carried out for all combinations of process conditions used in the experiments. These numerical simulations show that all pin locations are identical, with flow conditions in front of the pin being governed only by the gate geometry in front of the pin and not by secondary flow from the other pins. Thus any combination pin geometry can be used in these experiments. The flow patterns also show a high velocity jet of the area equal to the gate area striking the pin edge (edge pin) or pin face (angle pin) at the same velocity as the gate. Thus the gate velocity can also be considered as the impact velocity. It is seen that this fill pattern may account for the severe erosion pits seen in the edge pins. Different filling patterns were obtained for the angle pins. These differing fill patterns may account for the differing erosion patterns seen in either pin geometry.

A study of variation of erosion with the number of shots indicates that a linear relationship between the weight loss and number of shots holds for most of the pins. A best curve fit linear relationship was obtained for the low hardness edge pins and high hardness edge pins in the velocity campaigns, and for the angle pins and high temperature edge pins. There is a degree of variability in these plots. That is to be expected considering

the difference in the process conditions and pin geometries and hardnesses. These curve fits also indicate that there is a initial period of rapid erosion, before the erosion rate becomes severe.

Velocity and hardness studies of the effect of velocity on weight loss and edge profiles indicate that there is a linear increase in weight loss or erosion depths with velocity for both the low and high hardness pins. The ratio of weight loss between the low and high hardness pins appears to be a constant with velocity, while the ratio of edge wear between the low and high hardness pins increases slightly with velocity. This is in contrast to the laboratory simulative studies of liquid drop erosion, where quadratic and cubic relationships between wear and velocity are obtained. Metal loss by diffusion, and temperature effects may account for this erosive behavior in die casting. Comparisons of normalized edge profiles with velocity profiles showed a nearly one to one match between the velocity profile and the edge profile indicating that the numerical system can be used to predict wear qualitatively under production conditions.

The study of the effect of pin geometry on erosion was carried out with the aid of numerical simulations that predicted differing flow conditions for either pin condition used in the campaign. Two distinct forms of die erosion have been identified from this study; (a) "Core" erosion, where near normal impact and high tangential velocities both prevail and (b) "washout" erosion, where there is normal impact but not high velocity tangential flow. The core form of erosion appears to account for the severe localized erosion seen, where flow separation occurs in a die cavity.

The effect of angle was evaluated and it was observed that the maximum weight loss does not take place at normal impingement but it takes place at 72 degrees. This is contrary to the expectation that similar to liquid metal impact, the maximum erosion loss takes place at 90 degree angle impingement. A possible explanation for this observation is that the erosion of die casting dies is closer to slurry erosion, where a mixture of solid particles impact (maximum erosion occurs between 20 and 30

degrees for solid impact alone) and liquid jet impact causes the maximum erosion to take place around 72 degrees. Studies of erosion variation with temperature appear to confirm this theory. The angle of attack studies show that the relation between angle and velocity is quite complex, and requires more experimentation.

Studies on the effect of metal temperature show that the erosive wear increases linearly with decreasing temperature. This is possibly because the impact of larger numbers of larger sized solid particles at lower temperatures, causes an increase in the erosive wear of the pin. This indicates that the primary driver of erosion in die casting is the impact of partially solidified metal or solid particles at high velocities, with diffusion effects not being as critical. However, the studies also show that corrosive weight loss and soldering played an important role in die erosion at high temperatures.

Preliminary wear relationships were obtained to evaluate the effect of impact velocity, superheat and number of shots on die erosion. More experiments will however have to be conducted to determine coupled relationships of velocity, temperature and angle with die erosive wear. This study however provides a qualitative estimate of such relationships.

CHAPTER VI

CONCLUSIONS AND FUTURE WORK

6.1 CONCLUSIONS

The aim of this research was to develop a wear prediction system that could predict the extent and likely location of erosion in die casting dies. Such a system would be extremely useful as die erosion may be reduced by part / gate / core redesign or process parameter changes at the design stage prior to die building. This would provide a significant tool to the die designer to decide on an acceptable die design that beside producing a sound casting, would also extend the life of the die, and reduce machine downtime due to repairs.

A preliminary wear prediction system was developed which consisted of two parts

- **Erosive Wear models:** Wear models which predict the extent and location of die erosive wear as a function of process parameters such as metal velocity, die temperature, metal temperature etc. at that die location.
- **Numerical Models:** Numerical models which predict accurately the values of process parameters such as velocity and temperature that appear to influence die erosion, at various locations of the die. Since die erosion mostly occurs during the filling stage, such a

numerical model should predict accurately the complex filling patterns in die casting.

An analysis of the literature in the die casting area revealed little or no work in the development of erosive wear models for the complex (high temperature erosive-corrosive environment) conditions present in die casting. Wear models have been developed from simulative tests in other applications and simple corrosion tests have been developed in die casting. However the results from neither class of tests can be directly used in the proposed predictive system.

Consequently one of the main focus areas of this research was to develop an experimental test procedure, to understand die erosive wear and to quantify the effects of important process conditions in causing erosion in die casting dies. Such an experimental erosive wear test procedure was developed to evaluate die erosive wear (and to evaluate coatings and surface treatments) under actual production conditions. The tests were carried out on a commercial 250 ton Buhler die casting machine and an accelerated procedure was developed to obtain meaningful wear results in a reasonable amount of time. The procedure involved the use of pyramidal test pins as a surrogate measure of die wear, and avoided the drawbacks of prior studies of erosive and corrosive wear in die casting. Even though the erosive wear is accelerated, the one significant drawback of this experimental approach is the large time frame required to obtain meaningful results.

To develop wear models from experimental data obtained from these erosive wear tests, the values of important process parameters at the erosive wear pins, and at different locations in the die cavity must be known during the filling and solidification stages, for different choices of process conditions. These values of flow and thermal parameters are very difficult to measure during a die casting operation. Therefore, a strategy was implemented in which the process parameters at different locations in the cavity were determined by numerical simulations of the filling and

solidification stages of the process. Thus the numerical model is used both to come up with erosive wear models and to provide a better understanding of die erosion, as well as in a wear prediction system to predict the extent of die erosion using these developed erosive wear models.

After a literature survey a model based on FLOW-3D, a three dimensional finite difference software with the capability of handling the complex momentum driven flows common in die casting was chosen for this study. A numerical model based on FLOW-3D which has traditionally been used for space applications was then developed, to simulate the transient, inertia dominated and complex cavity filling in high pressure die castings. In order to determine the accuracy of this approach in modeling the complicated flow conditions in die casting, a comparison with experimental filling patterns obtained from water analogy results in the literature was conducted and selected results are presented. The results show that the model appeared to simulate the inertia dominated transient flow of die castings fairly accurately, thereby demonstrating that it is possible to obtain the values of process parameters at various locations in the die cavity, a requirement for the numerical part of the wear prediction system.

Thus, using the numerical models and the experimental wear apparatus, conclusions regarding the nature of erosion in die casting dies could be reached and preliminary wear models developed. An experimental plan was therefore developed to evaluate the effect of specific parameters and the results of these experiments form the main focus of this dissertation. The process parameters evaluated included impact velocity, angle of metal impact, pin geometry, pin hardness and heat treatment, number of shots and metal temperature or superheat. Numerical simulations of the filling and solidification stages were carried out to obtain the values of process parameters at various locations in the die. These process parameters at pin locations were then compared with experimentally obtained wear

profiles and pin weight loss to evaluate the effect of process parameters and to numerically predict the wear profiles.

The conclusions drawn from these studies of the effect of process parameters on die erosive wear are summarized below:

- Numerical simulations of the erosive wear die cavity were carried out for all combinations of process conditions used in the experiments. These numerical simulations show that all pin locations are identical, with flow conditions in front of the pin being governed only by the gate geometry in front of the pin and not by secondary flow from the other pins. Thus any combination pin geometry can be used in these experiments. The flow patterns also show a high velocity jet of the area equal to the gate area striking the pin edge (edge pin) or pin face (angle pin) at the same velocity as the gate. Thus the gate velocity can also be considered as the impact velocity. It is seen that this fill pattern may account for the severe erosion pits seen in the edge pins. Different filling patterns were obtained for the angle pins. These differing fill patterns may account for the differing erosion patterns seen in either pin geometry.
- A study of variation of erosion with the number of shots indicates that a linear relationship between the weight loss and number of shots holds for most of the pins. A best curve fit linear relationship was obtained for the low hardness edge pins and high hardness edge pins in the velocity campaigns, and for the angle pins and high temperature edge pins. There is a degree of variability in these plots. That is to be expected considering the difference in the process conditions and pin geometries and hardnesses. These curve fits also indicate that there is a initial period of rapid erosion, before the erosion rate becomes severe.

- Velocity and hardness studies of the effect of velocity on weight loss and edge profiles indicate that there is a linear increase in weight loss or erosion depths with velocity for both the low and high hardness pins. The ratio of weight loss between the low and high hardness pins appears to be a constant with velocity, while the ratio of edge wear between the low and high hardness pins increases slightly with velocity. This is in contrast to the laboratory simulative studies of liquid drop erosion, where quadratic and cubic relationships between wear and velocity are obtained. Metal loss by diffusion, and temperature effects may account for this erosive behavior in die casting.
- Comparisons of normalized edge profiles with velocity profiles showed a nearly one to one match between the velocity profile and the edge profile indicating that the numerical system can be used to predict wear qualitatively under production conditions.
- The study of the effect of pin geometry on erosion was carried out with the aid of numerical simulations that predicted differing flow conditions for either pin condition used in the campaign. Two distinct forms of die erosion have been identified from this study; (a) "Core" erosion, where near normal impact and high tangential velocities both prevail and (b) "washout" erosion, where there is normal impact but not high velocity tangential flow. The core form of erosion appears to account for the severe localized erosion seen, where flow separation occurs in a die cavity.
- The effect of angle was evaluated and it was observed that the maximum weight loss does not take place at normal impingement but it takes place at 72 degrees. This is contrary to the expectation that similar to liquid metal impact, the maximum erosion loss takes place at 90 degree angle impingement. A possible explanation for this observation is that the erosion of die casting dies is closer to slurry erosion, where a mixture of solid particles impact

(maximum erosion occurs between 20 and 30 degrees for solid impact alone) and liquid jet impact causes the maximum erosion to take place around 72 degrees. Studies of erosion variation with temperature appear to confirm this theory. The angle of attack studies show that the relation between angle and velocity is quite complex, and requires more experimentation.

- Studies on the effect of metal temperature show that the erosive wear increases linearly with decreasing temperature. This is possibly because the impact of larger numbers of larger sized solid particles at lower temperatures, causes an increase in the erosive wear of the pin. This indicates that the primary driver of erosion in die casting is the impact of partially solidified metal or solid particles at high velocities, with diffusion effects not being as critical. However, the studies also show that corrosive weight loss and soldering played an important role in die erosion at high temperatures.
- Preliminary wear relationships were obtained to evaluate the effect of impact velocity, superheat and number of shots on die erosion. More experiments will however have to be conducted to determine coupled relationships of velocity, temperature and angle with die erosive wear. This study however provides a qualitative estimate of such relationships.

6.2 FUTURE WORK

Some possible avenues of research that could further the knowledge of die erosion and develop more quantitative wear models for wear predictions are outlined below.

- Conduct further experiments to evaluate the relationship between angle of metal impact and metal velocity as it relates to die erosive

wear and develop coupled wear models of velocity and angle of metal impact.

- Conduct further experiments to evaluate the relationship between angle of metal impact and superheat as it relates to die erosive wear and develop coupled wear models of superheat and angle of metal impact. This would further allow evaluation of the effect of solid particles on die erosion.
- Study the effect of different angles than those considered in this study, to evaluate the effect of angle less than 65 degrees on die erosive wear.
- Conduct further experiments to evaluate the coupled relationship between velocity and temperature. This would allow the prediction of wear given velocity and temperature, which would result in significant improvement over the models presented in this research.
- Conduct experiments for more than 1,000 shots to evaluate if time relationships developed here continue to hold. This would also allow more significant localized wear in angle pins, and therefore allow comparisons of predicted wear from numerical simulations and measured wear.
- Use preliminary wear models to estimate erosion for the different geometries and process conditions available in industrial dies. This would allow the validation or refinement of these wear models over a wider range of process and geometrical conditions, and demonstrate the importance of this approach to reduce wear.
- Use a similar test procedure to develop similar models for soldering and heat checking wear in die casting dies.

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