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says
about...

GATING AND RISERING OF CAST IRON

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Sequential Filling of Mold Cavities

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ABSTRACT

A technique has been proposed whereby the sequential filling of mold cavities can be accomplished making use of a runner dam, or weir. Through this procedure, individual mold cavities can be poured at a rate more suitable to that necessary to obtain optimum casting results. This technique makes it possible to fill the mold cavities of multiple mold cavity molds one at a time, or in a given predicted and calculated sequence, in full accordance with the filling rates calculated using most of the well-known calculation methods. The technique also permits the orderly filling of pattern impressions where multiple inserts are used, and where it is desired to isolate the filling of each impression. In those cases where the riser ingate is positioned above the normal parting line to effect sequential filling, riser efficiency is substantially improved.

INTRODUCTION

Proper, effective and efficient design of gating systems has long been the goal of the foundryman, and a number of systems have been proposed to accomplish this task. These systems may vary significantly, but they are usually based upon sound principles derived from the application of hydraulic flow to molten metals. It has been suggested¹ that the gating system be designed to accomplish the following objectives:

- 1) fill the mold rapidly, without laps or requiring excessively high pouring temperatures;
- 2) reduce or prevent agitation or turbulence and the formation of dross in the mold;
- 3) prevent slag, scum, dross and eroded sand from entering the casting by way of the gating system;
- 4) prevent aspiration of air or mold gases into the metal stream;
- 5) avoid erosion of molds and cores;
- 6) aid in obtaining suitable thermal gradients to attain directional solidification and minimize distortion in the casting;
- 7) obtain a maximum casting yield and minimum grinding costs; and,
- 8) provide for ease of pouring, using available ladle and crane equipment.

Gating systems purporting to meet these objectives may be pressurized or unpressurized, the differences being related primarily to the location of the flow control orifice (the choke) within the system. Choke location at, or near, the base of the sprue renders the gating system unpressurized and full control of the metal flow is achieved when the sprue is filled.

This type of gating system permits a low velocity of metal flow to be realized in the runners and gates, and separation of slag and/or dross in the runners before the metal enters the mold cavity. In this type of system a variety of runner and gate locations have been advocated. With the runner located in the drag and the gates in the cope, the runner is filled prior to the entry of any metal into the mold cavity. Furthermore, this geometry insures that the runner will always remain full regardless of the details of pouring, e. g., interrupted pour, inadequate pouring rate to keep the system full, etc. On the other

hand, pressurized gating systems have the choke at, or near, the casting gate. In these systems full control of metal flow is not achieved until the entire gating system is filled. Entry of metal into the mold cavity is controlled by the respective gates.

THE GATING SYSTEM

The selection of a suitable gating system, whether it be pressurized or unpressurized, is the result of a number of considerations which may be unique to a given casting, e. g. casting geometry, yield requirements, space available in the mold, need for risers, etc. The principles behind both pressurized and unpressurized gating systems consider similar factors in the calculation of component geometry:

- 1) casting weight;
- 2) critical casting section thickness;
- 3) pouring rate;
- 4) metal fluidity;
- 5) type of sprue;
- 6) type of runners;
- 7) type and location of ingates; and,
- 8) sprue height or cope height.

Examples presented for the application of most gating systems first consider the production of a single casting with single or multiple ingates. Where multiple ingates are involved, procedures have been developed so that the flow rate of metal through each ingate is the same. This entails the introduction of procedures such as tapering or stepping down the runner, adjustment of the gate area, etc. When multiple castings are to be produced from a single gate system, the usual procedure is to design the gating system so that the flow rate of metal through each ingate is the same or, in other words, all of the mold cavities fill at the same time and rate. However, calculation of the gating system where multiple mold cavities are involved usually results in mold cavity filling times which are substantially longer than those determined for a single mold cavity. In fact, the foundryman often finds it impossible to deliver metal through the gating system rapidly enough to fill a multiple mold cavity system at a rate adequate to match that based on the mold-filling rate of a single mold cavity. The result often experienced is that the multiple mold cavity arrangement yields defects related to slow pouring rates, e. g. cold shuts, misruns, entrapping slag and dross, pinholes, chilled edges in gray and ductile irons, etc.

To illustrate this, one procedure^{2,3} to calculate a gating system bases the pouring time for gray cast iron on the following formula (the results of which have frequently been presented graphically):

$$t = K(0.95 + T/0.853) \cdot W^{1/2}$$

where

- t = pouring time in sec
 W = casting weight in lb
 K = fluidity factor, and
 T = critical section thickness in inches.

Consider a 7.5 lb gray iron casting having a critical section thickness of 0.5 in. (and a fluidity factor of 1.0). This results in a pouring time of 4.2 sec (1.8 lb per sec). As a first approximation this may be considered the time to fill the mold cavity.

Now, consider the production of four of these same castings from a common gating system (the usual procedure to accomplish this is to multiply the casting weight by four). The resulting pouring time would then be 8.4 sec (3.6 lb per sec), a mold cavity filling time which has doubled. If eight castings were to be made from a common gating system, this procedure results in a pouring time of 11.9 sec (5.0 lb per sec). To maintain the same mold cavity filling time as that of a single mold cavity

Fig. 1. Example of sequential filling of a small gray cast iron shaft using runner dams (A, B and C) to control the metal flow into pairs of mold cavities (1 and 2, 3 and 4, 5 and 6, 7 and 8).

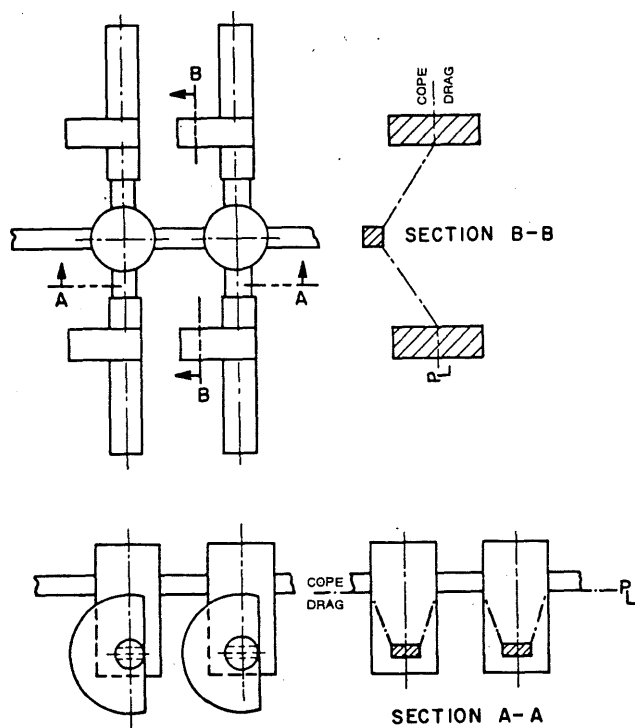
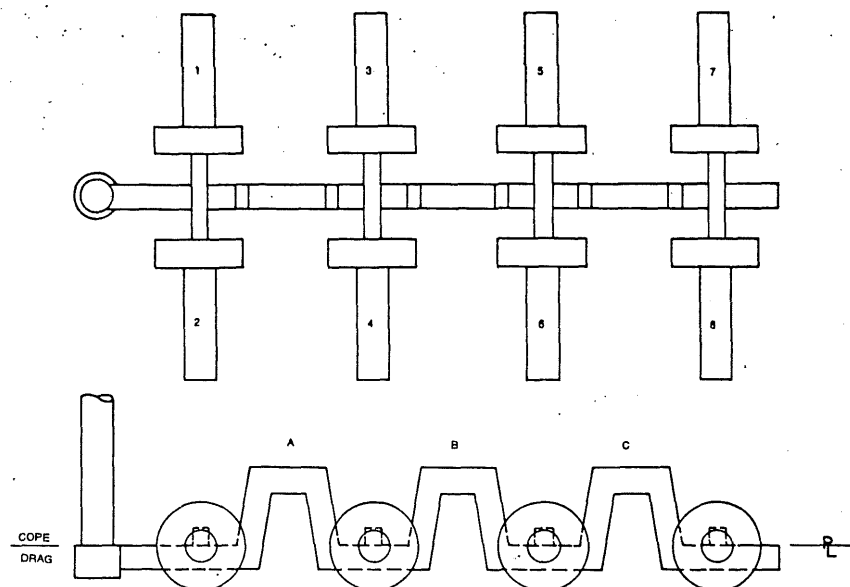


Fig. 2. Sequential filling of mold cavities using an elevated runner to distribute metal into the riser of adjacent pairs of mold cavities.

a fixed sprue location and a fixed "master runner." Various inserts can be mounted in this pattern plate and a variety of runner systems may result depending upon the number of inserts, and the number of individual mold cavities.

Figure 3 presents a simplified combination of insert patterns, one of which is located primarily in the drag and the other split between the cope and the drag. It is preferable to pour these individual castings in a manner such that the filling of one mold does not adversely affect the filling of the other mold cavity. The common master runner involved in this case would normally result in filling the two mold cavities somewhat individually until one mold cavity was filled to the level of the gates. The

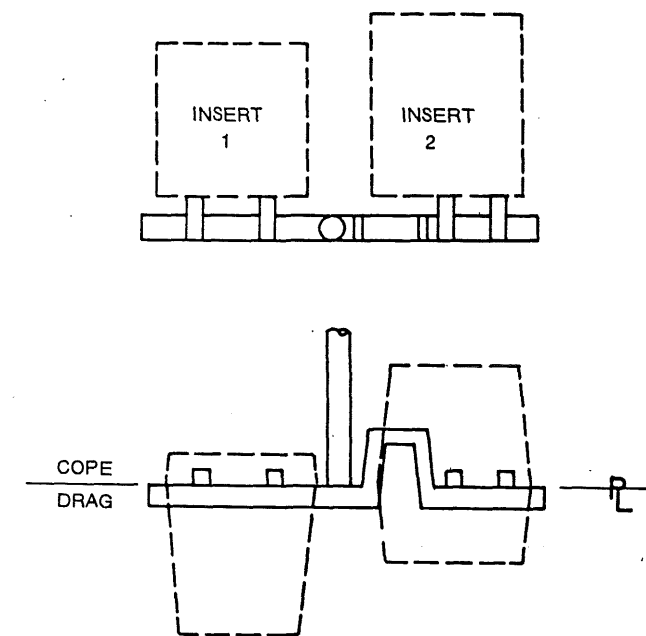


Fig. 3. Example illustrating the application of the sequential filling procedure to a situation where pattern inserts are employed.

filling of that mold cavity would then cease until the other mold cavity reached that level. Further filling of the mold cavities (the cope portion) will occur at a rate dependent upon the mass of those casting sections and the ability of the gating system to provide metal to the castings. However, inclusion of a runner dam, or weir, in the master runner causes one mold cavity to be filled completely before filling of the other mold cavity is initiated.

In a similar manner, a variety of other mold cavity combinations can be filled in an improved manner. Multiple insert mold cavities often involve castings which would best be treated individually, e. g. one casting which should be poured at a rapid

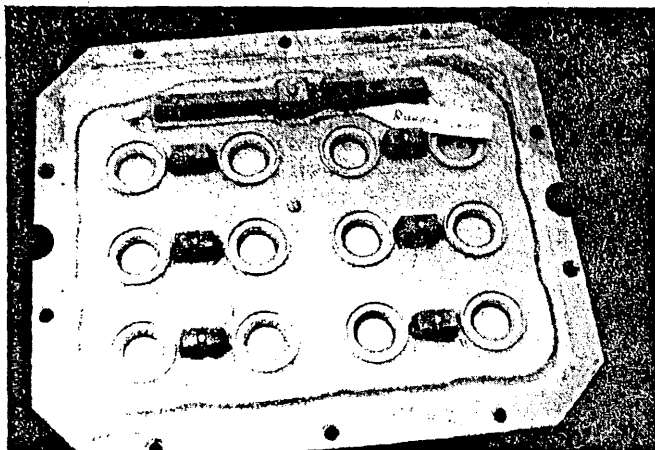


Fig. 4. Cope view of a production pattern showing the print for the core that forms a runner dam to divide the gating system into two sections.

pouring rate and another which should be poured more slowly; or, one casting which is chunky and the other which is heavily cored. Modification of the gating system using the principle of sequential filling of mold cavities enables solution of these problems.

CONCLUSIONS

Application of the sequential filling procedure requires that an appropriate gating system be used in conjunction with this technique. In the examples set forth, the system was unpressurized. A pressurized gating system (where flow control is established at the gates) results in a back-up of metal in the runner, thereby causing metal to flow over the runner dam, or weir, prior to the complete filling of the mold cavity (or cavities) designated.

In summary, a technique has been proposed whereby the sequential filling of mold cavities can be accomplished making use of a runner dam, or weir. Through this procedure, individual mold cavities can be poured at a rate more suitable to that necessary to obtain optimum casting results.

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APPENDIX

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In the Belleville Foundry of Sperry New Holland, the sequential method of filling mold cavities has been applied with con-

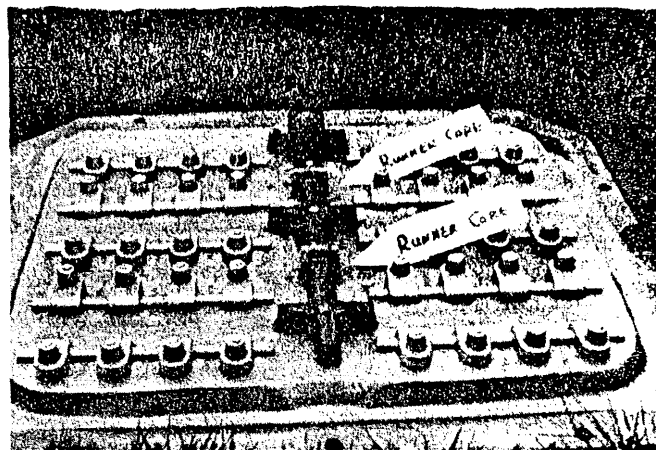


Fig. 5. Drag view of a lift pivot pattern showing the coreprints for two runner dam cores.

siderable success to a number of jobs. An oil sand core has been designed for the application and is now produced in a three impression dump box. The core is two inches in overall height, with one inch in the drag coreprint leaving one inch of the core to serve as a runner dam in the cope.

An example of the application of this technique to a ductile iron casting is shown in Fig. 4. Originally the gating system was similar to that shown but without the runner dam. Severe problems were experienced with cold shuts and misruns to the extent that scrap levels of 41, 55 and 54% were realized. When first experimenting with the system, each section of the mold was not treated as a complete gating system. As a result, the casting ingates were acting as the choke and misruns were still being experienced. Adjustments to the ingate size resulted in successful casting production. Since applying the sequential filling techniques, the scrap rates for misrun and cold shuts are negligible.

A second example is shown in Fig. 5. In this case, this gray iron lift pivot had experienced a substantial amount of cold shuts in the 3/64 inch wall between cored holes. Use of the sequential filling technique resulted in being able to fill these castings in series: first eight castings, then sixteen castings, and then the final sixteen castings. Scrap due to misrun or cold shut has virtually been eliminated.

Presently the sequential filling method of gating is being used on five different jobs, and there is sufficient confidence in its success that a number of new jobs are being rigged using this technique. More impressions can often be placed on a plate since each section can be treated as a separate mold, thus increasing the yield.

The system is not a cure-all, but may be applied to those jobs where problems are being experienced due to the filling process of the mold. However, when applied properly, the results have been encouraging.

Factors Influencing Dross Formation in Ductile Iron Castings

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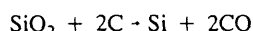
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LITERATURE

Experiments have been conducted to determine the critical effects of cast iron melt temperature on dross formation. The surface of a continuously cooling magnesium-treated ductile iron melt has been examined and it was noted that a film of dross appeared and thickened as the temperature decreased.¹ The process is reversible, and the investigators suggest that the magnesium vapor formed over the melt is responsible for protecting it from oxidation. This study¹ showed that dross in castings is related to the temperature at which the castings were poured. A casting poured at over 2600F contained a shallow surface dross defect whereas one poured at under 2450F contained large subsurface chunks. This latter type of inclusion is magnesium-rich and crystalline, containing mostly MgO and Mg₂SiO₄ (forsterite), similar in composition to the dross formed initially in the ladle and clearly detrimental to casting integrity.²

Dross, like slags formed during gray and white iron heats, is favored at lower temperatures where most relevant oxidation reactions have higher negative free energies. The temperature at which SiO₂ is reduced is of critical importance in determining its availability for reaction in forming dross.^{2,3} The SiO₂/CO inversion reaction is:



The equilibrium temperature for this reaction is influenced by melt composition, atmosphere, cooling rate, and other variables.² Below this temperature, silica is stable and the reaction shown above proceeds to the left. In an air atmosphere, a range of inversion temperatures has been measured between 2460 and 2610F.⁴ The silicon needed for reaction to form the higher compounds in the dross (mostly forsterite, Mg₂SiO₄; and to a lesser extent enstatite, MgSiO₃; and some amorphous silicates) is provided to a large

degree by late metal treatment. However, the temperature dependence of silica stability still strongly affects the temperature dependence of dross formation in ductile iron.⁵ Considerations of temperature and reaction kinetics are also important in determining the composition and the form (crusty, viscous, or fluid) of common gray iron slags containing mostly SiO₂, MnO, and FeO.³

The compositions of phases present in ductile iron drosses influence the tendency to agglomerate in crystalline chunks or as finer, more evenly dispersed inclusions.^{4,6} The following constituents have been identified in drosses taken from the ladle or in castings: MgO, SiO₂, Mg₂SiO₄ (forsterite), Fe₂SiO₄ (fayalite), MgSiO₃ (enstatite), MgS, oxides of iron, calcium, and aluminum, Fe-Si metallic phases, Mg₂Si (mainly unreacted nodularizer), and numerous vitreous, glassy phases not identifiable by x-ray diffraction techniques.^{1,4,5,7-9} The first three of those listed constitute the majority of dross detected.^{1,8}

Several classifications for dross particles in castings have been suggested.⁷ The first is large inclusions, including those particles readily visible to the naked eye at or near the cope surface. This form may contain dross formed after metal treatment which was not removed before the metal flow reached the mold cavity. Teapot ladling has been shown to be effective in preventing ladle dross from entering the stream.⁶ Fluxing with fluoride compounds such as NaF₂ causes crystalline dross to become more fluid and more likely to stick to the sidewalls or rise to the top of the melt and form as a surface film.^{6,8} Fluid slag or dross tends to remain in larger cohesive pieces with good gating conditions.²

The second classification proposed⁷ is the stringer form of dross, which is associated with exploded graphite. This type of dross inclusion usually exhibits deteriorated nodularity in its vicinity. Stringer inclusions tend to rise toward the cope surface and be located a short distance beneath it. The third form is called fine dross. Because of its smaller size, it does not rise as readily toward the cope, hence it is more evenly distributed vertically in the casting. Fine sulfides and MgO are the usual constituents of this type of dross.⁵ In general, dross particle size is proportional to chemical complexity.

Table Lit. 1 lists the effect of some processing variables on the quantity and composition of dross collected from smaller experimental heats.⁸ When silicon is present in the melt as compared to silicon added as postinoculant or nodularizer, it produces less dross. This implies a tradeoff between less late silicon to avoid dross and enough to provide sufficient postinoculation to minimize carbides and refine the eutectic cell size.⁹ The amount of forsterite present is related to the availability of silica to react with MgO.

Table Lit. 1 Effect of Processing Variables on the Quantity and Chemistry of Dross⁸

Variable	Quantity of Dross	%Forsterite	%MgO
Time	increases	increases	decreases
Holding temperature	decreases	decreases	maximum occurs
Nodularization temperature	maximum occurs	minimum occurs	decreases
PO ₂ in atmosphere	no effect	increases	decreases
Sulfur in the melt	increases	decreases	decreases
Magnesium added to the melt	increases	increases	no effect
Cerium added to the melt	increases	decreases	increases
Silicon in the melt	decreases	increases	increases
Silicon added in the nodularizer	increases	increases	no effect
Silicon added in the post-inoculant	increases	increases	increases

Table 1. Mold Material Variables and Casting Codes from Heat III

Code	Type of Mold
3 ST	STANDARD green sand (AFS #38): 5% western bentonite, 3% seacoal, 3.5% water.
3 FG	Same Composition as above except base sand is FINE GRAINED: AFS #70.
3 HS	Green sand with HIGH SEACOAL: 5% western bentonite, 6% seacoal, 3.5% water.
3 HM	Green sand with HIGH MOISTURE: 5% western bentonite, no seacoal, 6% water. Rammed same day of heat.
3 DR	DRIED green sand: 9% western bentonite, 3% seacoal, 6% water. Dried overnight in oven at 300 °F.
3 CR	CHEM-REZ no-bake
3 SS	SODIUM SILICATE with acetate ester hardener.
3 LC	LINO-CURE no-bake.

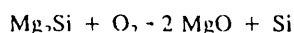
The complexity of the dross problem is related to silica availability at several stages of processing. Silica can nucleate the reaction between itself and MgO to form forsterite; this is believed to contribute to higher Mg fading rates in silica crucibles compared to magnesia crucibles.⁷

Forsterite is produced by direct reaction between MgO and SiO₂ according to the reaction



which occurs preferentially compared to other possible combinations such as dissolved Si and O reacting with MgO.⁵ Studies of powder compacts fired to molten metal temperatures^{2,8} have shown that the relative rates of diffusing Mg +² and Si +⁴ ions through the reacting phases dictates a predominance of forsterite as a reaction product as opposed to enstatite (MgSiO₃), which can form as a minor reaction product. Above the silica inversion temperature, as expected, the reaction to forsterite is reduced by the lower availability of silica.

The relative contribution of magnesium introduced in the processing of ductile iron compared to other compositional parameters has not been shown. A greater severity of dross defects are reported to result from higher levels of retained Mg.⁷⁻¹⁰ Increasing magnesium levels above 0.10% exert little additional effect.⁸ Magnesia available for reaction with SiO₂ is produced by the reaction.⁸



Another source of MgO is the oxidation of dissolved Mg in the molten iron. This oxidation is related to the magnesium loss rates that reach a maximum at about 2500F and may be related to the SiO₂/CO inversion.⁵ Higher levels of magnesium contribute to the formation of crystalline forsterite and fine dross produced by oxidation and desulfurization during pouring and filling of the mold.⁸ It is indicated that the silicon and aluminum in the treatment alloys produce more dross than magnesium.⁹ The type of nodularization treatment can influence the temperature loss in the ladle and the tendency to form dross.¹¹ High treatment weights are recommended to minimize turbulence and to conserve temperature. Longer holding and pouring times promote reaction between the metal and atmosphere (oxidation)¹ and more time for cooling below the inversion temperature for the particular system.²

Although control of atmosphere is not practical in commercial ductile iron production, the effects on dross behavior are significant. Table A indicates the beneficial effects of a lower oxygen partial pressure, such as occurs in closed treatment systems, in lowering the inversion temperature, and slowing the reaction of forsterite.^{2,8} A lower PO₂ reduces the magnesium loss rate and may slow down the major dross-forming reactions.⁷ Local oxygen depletions within the body of the melt are reported to account for the smaller inclusion size of fine dross distributed throughout the body of the casting. Fine sulfides are preferentially formed as a result of oxygen and sulfur competition for available magnesium atoms.⁴ Table 1 shows that increasing levels of sulfur results in more total dross while reducing the presence of forsterite and MgO.⁸

Trace amounts of aluminum and calcium from additions contribute to dross in the form of oxides in the glassy phases.⁸ It is indicated from powder compact studies that, as the amount of impurity oxides Al₂O₃, CaO, and FeO increase, the percentage of forsterite as a reaction product decreases.^{2,5,8}

The rare earth element, cerium, reduces the later formation of MgS and MgO as well as limiting the oxidation of silicon and resulting reaction to forsterite. Higher cerium produces an initial increase in the total ladle dross in the form of uncombined MgO, but a decrease in casting defects if the metal is properly skimmed prior to pouring.⁸ The use of CeMgFeSi may contribute to inadequate nodularity of a sooty fracture appearance called carbon dross.⁷ Flake and exploded (Type V) graphite is common near dross defects, while poor nodularity and heavy concentrations of graphite (carbon flotation) near the cope frequently occur in high carbon equivalent compositions. The use of NiMg as a treatment alloy is reported to reduce dross defects compared to silicon-containing MgFeSi or CeMgFeSi.⁷ While carbon flotation and dross both appear as a result of some of the same processing conditions, such as a pouring temperature, slower solidification rate, and higher carbon equivalent, the two have different causes.¹² Raw materials and melting practice often contribute to the formation of dross. Thin and rusty scrap or overblowing during cupola melting may result in a more oxidized heat⁹ which causes more dross as well as increased losses of silicon, magnesium, and carbon.

The preceding considerations have shown that dross problems may occur in the production of ductile iron and that the nature of the dross changes continually from the furnace tap until the metal has solidified in the mold. Much of the dross can be removed in the ladle and during pouring to produce cleaner castings. The geometry of the casting may also influence dross defects. Castings with large, flat surfaces and cylindrical, vertically poured castings are particularly susceptible.^{1,9,10}

The design of a gating system provides opportunities to trap dross in the metal stream and reduce further dross formation. It is reported that as long as temperature and metal conditions are not too highly conducive to dross formation, a pressurized system is generally considered superior to an unpressurized (or reverse choke) system in reducing the number of large inclusions.^{10,13} A gating system with a sprue-to-runner-to-ingate-area ratio of 4:8:3, called a "positive pressure" system, has been designed to reduce the velocity of the metal in the runner, thereby reducing turbulence of the metal stream ahead of the ingate and trapping inclusions.¹⁰ As fast a pour as possible is always recommended to keep the

runner bar full, thereby reducing additional turbulence, aspiration, and the formation of reoxidation products.^{9,10} Studies of cast steel macroinclusions, though different in source than ductile iron dross, have shown that inclusions tend to collect at locations in the gating system or in the casting where the molten metal is reduced in velocity or changes direction.¹³

Even finely dispersed inclusions can be removed in a properly designed gating system which reduces velocity and flow rate in key areas. This may occur in part by flotation, the rate of which is proportional to the square of particle radius.¹⁴ Another procedure is to control the metal flow rate using several ingates, multiple downgates (sprues), and by choking the gating system.¹¹ Whirl gates, also called centrifugal traps or spinner traps, may be effective in trapping inclusions by creating a vortex in the metal passage. Whirl gates may produce as well as remove dross. It is reported that a 3-to-2 ratio of inlet to outlet area increased the efficiency of these gates.¹¹ A study was conducted of a number of parameters that relate to fluid flow characteristics and cleaning efficiency, such as trap geometry, angular displacement and relative tangency of the inlet and outlet.¹⁵ A good swirl effect was obtained when gating into a side riser and the riser entrance was turned vertically and placed perpendicular to the neck in the drag portion of the casting.¹⁶ Dam or weir gates have been shown to enhance the swirl effect of the riser and retain dross.^{7,10} Bottom gating into the casting is generally advised to reduce turbulence and further oxidation once the metal enters the mold cavity.¹⁰

Metal-mold and metal-refractory interactions are only discussed to a limited extent in the literature. The reaction with the mold is reported to be very slight in low or normal moisture green sand.^{2,6} However, the possibility exists of picking up loose or eroded sand from the mold walls. Quartz has been found in cast steel inclusions and traced to molding sand.¹³ It is indicated that casting dross is much higher in SiO_2 than ladle dross removed prior to pouring, although this may be partially the result of the greater reoxidation of silicon compared to magnesium.¹ The study of macro-inclusions in cast steel shows that the use of a graphite mold reduces the percentage of SiO_2 in the inclusions.¹³ Good molding practice and reduced metal turbulence help to eliminate sand pickup. Metal-refractory interactions are important in all ferrous melts, including cast steel and ductile iron. Accretions of MgO and SiO_2 in the crucible are an important potential source of dross. Fluxes can be used to prevent ladle buildup and to fluidize dross entering the mold.⁶

Objectives of This Investigation

The investigation of the occurrence of dross in ductile iron involves the effects of the following process variables:

- pouring temperature
- design of the gating system and ingate location in cope and drag and in thin and thick sections
- presence of a core in the mold cavity
- a wide variety of molding materials including regular green sand, graphite mold, several types of no-bake sands, sodium silicate bonded sands, dry sand, green sand with high moisture content, and separately with high seacoal content
- effect of fine and coarse grained silica sands and the presence of a wash on the casting
- metal composition including various levels of sulfur in the treated molten iron, different retained magnesium levels, and various carbon equivalents and carbon-silicon ratios

MATERIALS AND PROCEDURE

Overview of Variables Studied

Heats I and II were cast to the same ductile iron aim composition at two different pouring temperatures: a high pouring temperature of 2700–2750F for Heat I, and a low pouring temperature of 2400–2450F for Heat II. The primary purpose of both heats was to investigate the several different gating systems and ingate locations on the dross produced in the standard ductile iron casting. All molds were a standard green sand composition.

Heat III had a standard gating system for all castings. All metal was poured into molds at 2400–2450F from a ductile iron that was slightly lower in carbon equivalent (although still high) than for Heats I and II. The primary variable was mold materials: green, dry, no-bake, and sodium silicated bonded sand made with a coarse and fine silica sand. Heat IIIA was a small heat poured into a graphite mold similar in design to the standard test mold. The graphite mold was poured to determine the dross in the treated iron since it was thought that metal-mold dross reactions would not occur in a graphite mold.

Heats IV, V and VI were used to evaluate the influence of pouring temperature, type of mold, and compositional variations. All three heats poured pairs of molds, one green sand and one no-bake from different compositions of ductile iron at 2400, 2500, and 2600F. Heat IV studied 0.015, .035, and .08% levels of sulfur in the molten iron before treatment. Heat V studied three different magnesium contents in the iron after treatment of 0.03, .05, and 0.08%. Heat VI varied the carbon equivalent of the ductile iron poured with aim compositions of 4.0, 4.3, and 4.5% CE.

Some difficulty was experienced with high dross contents in the no-bake molds, so Heats VII and VIII were undertaken to study the reasons for this difficulty and to correct it. These two heats were poured from iron of the same chemical composition poured at about the same pouring temperature of 2450F into the molds of standard gating systems. These heats included various types of molds set with no-bake binders, green sand molds of different compositions, and molds with and without mold washes. Molds that were set with sodium silicate and a dry sand mold were included in Heat VII.

The Materials and Procedure section is divided into a description of the development of the standard casting and gating system, the molding and molding materials, and the charge materials, and melting and pouring procedures.

Casting and Gating Design

The first step in the investigation was to design a test casting capable of distinguishing the relative susceptibility of various processing conditions in producing dross. A trial heat with an unrisered casting design was poured initially. However, considerable open shrinkage occurred in the cope surface where most of the dross would be expected to accumulate. Therefore a somewhat scaled-down version of the same general shape was designed with an adequate riser, and a hardwood pattern was constructed with several degrees of draft. Figure 1 shows the final dimensioned design; this casting proved suitable for studying all variables. The dashed lines in Fig. 1 show the size and location of a core that was located in some of the castings. The various faces of the casting have been labelled at the top of Fig. 1 for identification. This pattern was constructed so that the casting could be poured with any of three gating systems and with the 1/4-in. thick section at either the top or bottom of the 2 1/2-in. high block.

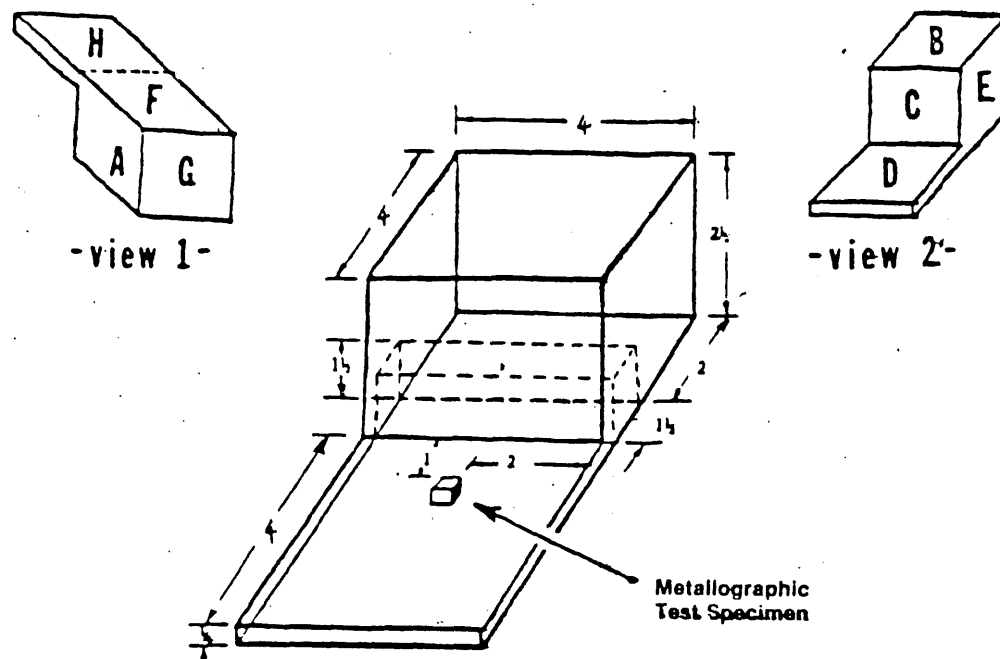


Fig. 1. Test casting sketches showing identification of faces observed for dross effects (top), and casting dimensions (bottom, not to scale).

The purpose of Heats I and II was to investigate dross incidence in the casting as affected by three different gating systems:

- 20% choke, pressurized system
- 4:8:3 positive pressure system
- 1:4:4 low velocity non-pressurized system

The dimensions of each gating system are listed below and in Figs. 2 and 3.

Sprue Dimensions:

Gating system	Diameter (in.)	Area at Base (in. ²)
20% Choke	1	.78
4:8:3	1	.78
1:4:4	1.21 tapered to .75 at base	.44

Runner Dimensions:

Gating System	Location	Average Width (in.)	Height (in.)	Area (in. ²)
20% Choke	cope	1	1 1/16	.81
4:8:3	cope	1 5/8	1	1.63
1:4:4	drag	1 1/4	1	1.75

In the 20% choke system, a straight-sided sprue is used; the cross-sectional area of the runner is small and the ingate is choked 20% from the runner. In the 4:8:3 and 1:4:4 systems, the numbers indicate the ratio of sprue base-to-runner-to-ingate cross-sectional areas. The 4:8:3 system also includes a straight-sided sprue and ingate choke, but has a relatively large runner area. The 1:4:4 system is choked at the sprue base as the numbers indicate and utilizes a big end up tapered sprue.

The pattern was designed so that each of the gating systems could be utilized with four different ingate locations. The position of the ingates either at the top or the bottom, and into either the 4 x 4 x 2 1/2-in. block or 4 x 4 x 1/4-in. plate portion of the casting, is shown in Figs. 2a, 2b, 3a, and 3b. Ingate dimensions are listed in these figures for each of the three gating systems. Note that the gates overlap the casting where necessary to achieve the required ingate areas. This arrangement permits evaluation of four ingate locations as well as three different gating systems.

Figures 2 and 3 illustrate the 4 1/4-in. dia by 6-in. high cylindrical riser with a hemispherical bottom cap. The riser neck is about 1 in. long and is attached to the block portion of the casting with the smallest riser neck cross-section of 1 1/2-in. high by 2-in. wide.

Heats I and II utilized these twelve gating conditions. In addition, a core was employed on castings gated through the thin section both from the top and bottom for the 20% choke and 1:4:4 gating systems. Heat I was poured at temperatures of 2700–2750F and Heat II poured at 2400–2450F. The temperature was measured in the ladle with a Pt – Pt + 10% Rd immersion thermocouple.

Based on the results with these different gating systems, all molds that were poured from Heats III through VIII, except for the graphite mold, had a 4:8:3 gating system with the ingate located at the bottom of the heavy section of the casting. This arrangement was selected because the 4:8:3 ratio gating system was the best of the three types investigated and this ingate location produced the most dross for this system. The details of this gating arrangement are shown in Fig. 4.

Molds and Molding Materials

All of the molds used in Heats I and II were green sand molds.

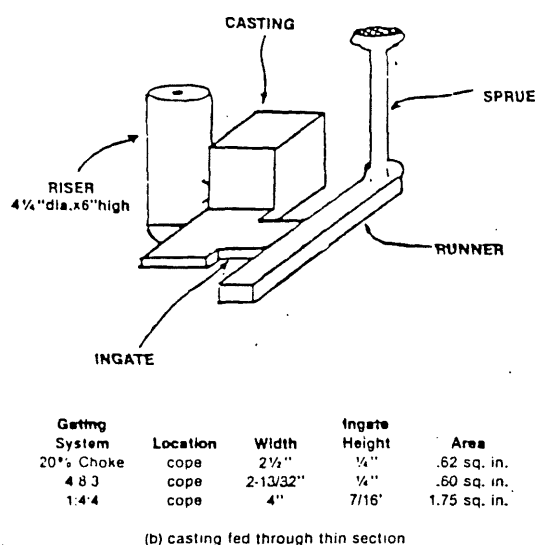
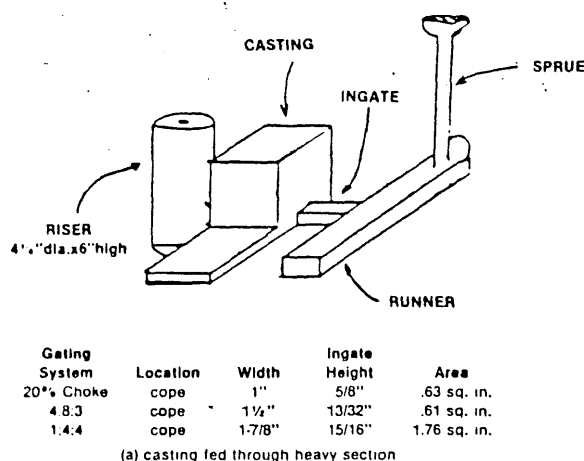


Fig. 2. Gating and risering arrangement for bottom-gated castings.

The base sand was subangular silica bank sand with an AFS fineness no. 38. Blended into the facing sand was 3.5% moisture, 5% western bentonite binder, and 3% seacoal. Recycled green sand of similar composition was used for backing sand. The sand was well rammed around the pattern in a 14 x 18-in. tight steel flask. The cope portion of the flask was 10 in. high and the drag portion was 5 in. high.

The cores were produced from the same base sand and the Lino-cure sand binder system. Lino-cure consists of an oil-modified alkyd resin binder "A", an organometallic drier "B", and a polymeric MDI-type isocyanate co-reactant "C".

Heat III tested molds produced from various molding materials with the same standard gating system shown in Fig. 4. The pouring temperatures for Heat III were 2400–2450F. Identical molds were produced both with and without a core. Table I lists the compositions of the various molding sands used in Heat III. The Chem-Rez sand binder system is a phenol-formaldehyde resin with a sulfonic acid catalyst. The specific resin used in this experiment was Chem-Rez 485, which is a low nitrogen, low odor, and low moisture type. The sodium silicate molds were made using Carsil, which is fused soda ash and silica sand dissolved in water. The binder was set by adding an acetate ester catalyst called Carset.

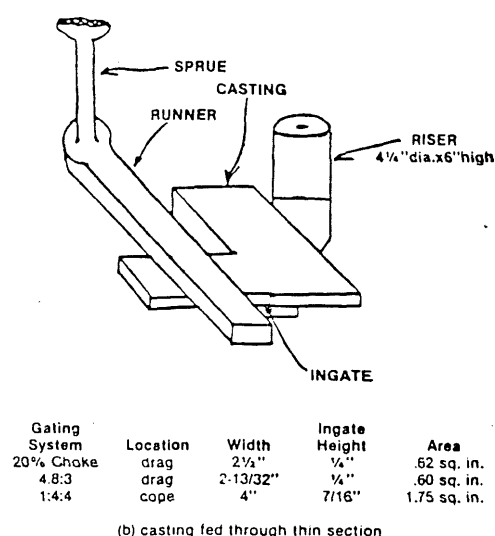
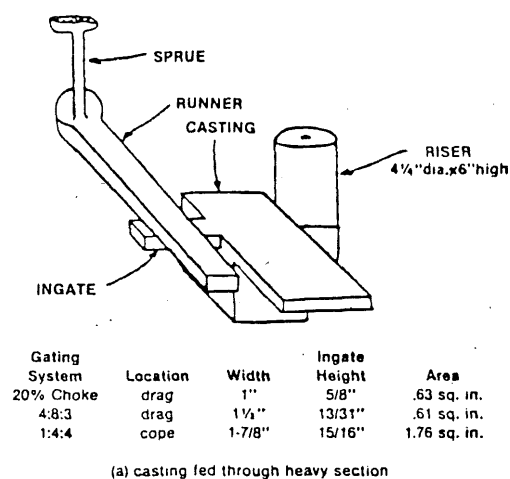


Fig. 3. Gating and risering arrangement for top-gated castings.

A small heat designated Heat IIIA was conducted to test a specially designed graphite mold. Castings were made by pouring into the riser through a large ingate and into a cavity close in dimensions to the casting in Fig. 1. The only differences are that the plate section of the casting is 1/2-in. rather than 1/4-in. thick so that the iron will run in this molding media and the vertical edges are rounded. The purpose of using the graphite mold was to eliminate any mold-metal reaction that could cause dross formation. The mold, prior to pouring, was preheated to prevent a severe chill effect and to avoid blow-holes. The mold was poured twice: once at 2400F to measure conditions at a low pouring temperature, and once at 2700F to assess the effect at high pouring temperatures.

Every other mold poured in Heats IV, V, and VI had a green sand mold composition. Four molds in Heat VII and two molds in Heat VIII are also green sand molds. The base sand used was subangular silica beach sand with AFS fineness no. 38. Blended into the facing sand for the green sand molds in Heats IV, V, and VI was 3.5% moisture, 5% western bentonite binder, and 3.5% seacoal for all green sand molds except those for Heats VII and VIII. This sand and ramming procedure was identical to that employed in Heats I, II, and III except that 3.5% seacoal was used rather than only 3% in the earlier heats. One half of the molds (or every other mold) in Heats IV, V, and VI were made of a no-bake

Table 2. Mold Material Variables Compositions and
Casting Code from Heat VII

Code	Type of Mold
7 SC2	Green sand with 2% seacoal; 6% western bentonite, 3.5% water
7 SC3	STANDARD GREEN SAND: 5% western bentonite, 3% seacoal, 3.5% water
7 SC4	Green sand with 4.5% seacoal; 6% western bentonite, 3.5% water
7 SC6	Green sand with 6% seacoal; 5.5% western bentonite, 4% water
7 DS	Dried green sand: 9% western bentonite, 3% seacoal, 6% water Dried overnight in oven at 300°F
7 GP	Graphite mold
7 SS	Sodium silicate with acetate ester hardener
7 P	Pepset mold
7 PW	Pepset mold with graphite wash (Velvalite GA-3)
7 200	Chem-Rez 200 no-bake furan binder
7 200W	Chem-Rez 200 no-bake furan binder, with graphite wash (Velvalite Ga-3)
7 212	Chem-Rez 212 no-bake furan binder
7 212W	Chem-Rez 212 no-bake furan binder, with graphite wash (Velvalite Ga-3)
7 270	Chem-Rez 270 no-bake furan binder
7 270W	Chem-Rez 270 no-bake furan binder, with graphite wash (Velvalite GA-3)
7 487W	Chem-Rez 487 no-bake binder, with graphite wash (Velvalite GA-3)

Table 3. Mold Material Variables Compositions and
Casting Modes from Heat VIII

Code	Type of Mold
8 GS	Standard green sand: 3% seacoal, 3.5% water, 5% western bentonite, AFS #48 sand
8 200	Wedron 5010 sand, 1.5% Chem-Rez 200*, 2.5% C-2000**
8 200W	Wedron 5010 sand, 1.5% Chem-Rez 200, 2.5% C-2000, with Velvalite ZA-3 (zircon wash)
8 212	Wedron 5010 sand, 1.5% Chem-Rez 212, 20% C-2006
8 212W	Wedron 5010 sand, 1.5% Chem-Rez 212, 20% C-2006, with Velvalite ZA-3 (zircon wash)
8 270	Wedron 5010 sand, 1.5% Chem-Rez 270, 20% C-2006
8 270W	Wedron 5010 sand, 1.5% Chem-Rez 270, 20% C-2006, with Velvalite ZA-3 (zircon wash)

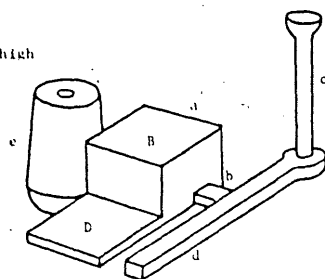
*Based on sand

**Based on binder

Table 4. Chemical Analysis of Raw Materials

	C	Si	Mn	S	P	Mg	Ce	Ca	Al
Pig Iron (Sorel)	4.35	.20	.009	.016	.020				
Steel Scrap	.01	.04	.26	.016	.01				
Ferromanganese	1.42	.81	81.40						
Foundry-Grade Ferrosilicon		78.11						.73	
Magnesium Ferrosilicon:									
Heats I & II		45.70				5.30	.39		
Heats III-VIII		47.40				5.65	.98	.57	.82

- a casting
b ingate
c sprue
d runner
e riser
1/2" diameter x 6" high



Gating System

4:8:3

Location	Ingate		
	Width	Height	Area
Cupe	1 1/2"	13/32"	.61 sq.in.

Fig. 4. Gating and risering arrangement for bottom-gated 4:8:3 casting, fed through heavy section.

sand. The specific resin used was Chem-Rez 485. This binder system is a low nitrogen phenol-formaldehyde liquid resin containing low moisture with a sulfonic acid catalyst.

One of the purposes of Heat VII was to investigate the effect of the seacoal content in green sand molds on dross incidence. The seacoal contents of the green sands in this heat varied from 2% to 3% for the standard composition to 4.5% and 6%. Two standard green sand molds (3.0% seacoal content) were tested in Heat VIII. These sands were compounded so that the same green compression strength of 11.1 to 11.4 psi was obtained. Attaining these same green compression strengths required increasing the western bentonite to 6% at the lowest seacoal level of 2% and to 6% and 5.5% bentonite, respectively, for the 4.5 and 6% seacoal percentages. All castings in this heat were poured in the temperature range of 2430–2480F.

Table 2 lists the compositions of the wide variety of molding sand tested in Heat VII. The specific resins used for furan type no-bake sand molds were Chem-Rez 200, 212, and 270. Chem-Rez 200 and 212 no-bake furan binder is a high furan, low nitrogen resin which self-cures at room temperature in the presence of an acid catalyst (phosphoric acid, toluene sulfonic acid, or benzene sulfonic acid solution). Chem-Rez 270 has zero nitrogen, phenol or formaldehyde and water contents and is high in furfuryl alcohol. Chem-Rez 487, a binder similar to 485 that contains no furfuryl alcohol, was also used for molds in this heat. Pep Set is a no-bake phenolic-urethane three-part binder system. Some of the difficulty experienced with no-bake binders continued to occur in Heat VII, requiring Heat VIII.

Another purpose of Heat VII was to investigate the influence of a graphite wash in the no-bake mold on the dross formation. The specific wash used in this experiment was Valvalite GA-3. It is a 100% graphite-based coating, black in color, and supplied in a paste form. It disperses rapidly in alcohol and stays in suspension well when mixed to the recommended proportions for spraying. A sodium silicate mold was also poured in Heat VII. This binder was produced as described in Heat III. The dry sand mold investigated in Heat VII was also similar to that poured in Heat III. A casting in Heat VII was also poured in the graphite mold described in Heat III, using a similar procedure.

Because the surface finish of the castings from the no-bake molds in Heat VII was still inferior, another smaller heat, number VIII, was poured into four kinds of molds, including two of standard green sand compositions. Three kinds of no-bake molds with

Table 5. Relative Percentages of Major Charge Materials

HEAT NUMBER	% SOREL	% DUCTILE IRON	
		REMELT	STEEL SCRAP
I	64.3	27.3	8.3
II	46.1	43.2	11.7
III	61.0	28.1	10.9
IV	87.5	-	12.5
V	85.6	11.1	3.3
VI	75.2	11.1	13.7
VII	85.0	12.2	2.8
VIII	85.0	12.2	2.8

and without a zircon wash were tested. Table 3 lists the compositions of the four types of molding sand used in this heat. These molds were made using the standard pattern employed in this study. The special sand used for these no-bake molds was Wedron 5010 sand, which is fine AFS no. 69. All mixes were prepared in a Hobart batch mixer. The acid was added first and mixed two minutes. The binder was added and mixed two minutes. Two sets of molds were prepared from each basic mix. One set was coated with Valvalite ZA-3 at 90° baume. It is a 100% zircon-based coating, off-white in color, and supplied in a paste or slurry form. The molds in heat VIII were poured over a 2450–2460F temperature range.

Charge Materials, Alloys, Melting and Pouring

A total of three 800-lb heats (nos. I, II, and III) plus one smaller Heat IIIA to test the graphite mold, four 900-lb heats (nos. IV, V, VI, and VII), and a 450-lb Heat VIII were cast in this investigation. The basic charge for all three heats consisted of low-impurity pig iron, ductile iron scrap, and scrap steel. While the addition of sufficient remelt eliminated the need to add silicon to the furnace, crushed graphite and 80% high carbon ferromanganese were added for the carbon and manganese contents. Most of the graphite was added to the bottom of the charge sandwiched between the steel scrap prior to melting, while the ferromanganese was added just before tapping in order to minimize losses.

Tables 4 and 5 list the compositions and relative percentages of the basic charge materials and the analyses of the alloys used. Each heat was melted in a magnesia-lined high-frequency induction furnace. After the initial charge was melted, the ferromanganese added, and the slag removed from the surface, the furnace temperature was monitored with an immersion thermocouple. The castings were poured from a preheated clay graphite ladle into which the metal was tapped progressively. Nodularization was accomplished in the ladle by plunging a graphite bell containing finely divided 6% magnesium alloy (see Table 4 for composition) below the melt surface. After the magnesium addition, 0.6% Si contained in 75% foundry grade ferrosilicon was added as a postinoculant. Some additions of silicon metal were also added to the ladle on the heats where different silicon contents were required. Two molds were poured from each ladle. Fine graphite and in several instances 50% ferrosilicon was added to the furnace as needed between pours to compensate for oxidation losses and maintain the levels of carbon and silicon close to those of the first tap.

During the course of Heat II, the fill time of the molds for each of the three gating systems were measured by stopwatch and recorded. The metal velocity in the gating system was later calculated from this information.

Heat IV was intended to investigate the influence of sulfur content, pouring temperature, and molding material. Three sulfur levels (.08, .035, and .015%), three pouring temperatures (2400, 2500, and 2600F), and two types of molding materials (green and

Table 6. Chemical Analysis of Heat

HEAT	LADLE NUMBER	C	Si	Mn	S	P	Mg		
	AIM	3.80	2.60	.40	.02 max.	.02 max.	.040		
I	11	3.70	2.29	.40	.014	.02	.039		
	12						.057		
	13			2.39			.050		
	14	3.72					.047		
	15			.41	.013		.046		
	16	3.75	2.78				.057		
	17						.061		
	18	3.66	2.48	.40	.011		.065		
	Average	3.71	2.49	.40	.013	.02	.053		
II	AIM	3.80	2.70	.40	.02 max.	.02 max.	.050		
	21	3.63	2.64	.39	.015	.02	.050		
	22						.073		
	23		2.85				.070		
	24	3.50					.060		
	25			.41	.015		.063		
	26	3.54	2.80				.062		
	27						.072		
	28	3.48	2.92	.41	.013		.071		
	Average	3.53	2.80	.40	.014	.02	.065		
III	AIM	3.70	2.60	.40	.02	.02	.050		
	31 (ST)	3.70	2.61	.42	.015	.027	.071		
	32 (FG)						.062		
	33 (HS)		2.62				.055		
	34 (HM)	3.75					.057		
	35 (DR)			.43	.011		.053		
	36 (CR)	3.69	2.86				.053		
	37 (SS)						.066		
	38 (LC)	3.77	2.70	.42	.011		.052		
	Average	3.73	2.70	.42	.012	.027	.059		

HEAT	LADLE NO.	C	Si	Mn	S	P	Mg	AVERAGE	
IV	(AIM for high S)	3.60	2.50	.35	.08	.02	.09	%S	%Mg
	411	3.37	1.98	.04	.020	.03	.074		
	431				.036		.065	.028	.070
	(AIM for med. S)				.035		.056		
	412		2.26		.017		.058		
	422	3.31			.022		.062		
	432				.022		.054	.020	.058
	(AIM for low S)				.020		.045		
	423	3.29	2.62	.16	.006		.053		
	433				.007		.052	.0065	.0525
	Average	3.33	2.28	.045		.03			
V	(AIM for high Mg)	3.60	2.50	.35	.02	.02	.08	Average	
	511	3.70	2.47	.32	.006	.03	.073	%Mg	
	521	3.67	2.55		.007		.084		
	531	3.70	2.95		.006		.067	.075	
	(AIM for med Mg)						.05		
	512	3.67	2.81		.007		.054		
	522	3.66	2.99	.40			.051		
	532	3.67	2.76	.32	.006		.051	.052	
	(AIM for low Mg)						.030		
	513	3.66	2.93	.40			.042		
	523		3.45				.03	.036	
	Average	3.67	2.86	.35	.0064	.03			

HEAT	LADLE NO.	C	Si	Mn	S	P	Mg	C.E.	Avg.
VI	(AIM for high C.E.)	3.85	2.15	.35	.025	.03	.045	4.5	C.E.
	611	3.68	2.81	.49	.02	.03	.030	4.62	
	621		2.65				.035	4.56	
	631		2.76				.047	4.60	4.59
	(AIM for med C.E.)	3.55	2.45					4.3	
	612	3.48	2.94	.44	.019		.031	4.46	
	632		3.02				.050	4.49	4.47
	(AIM for low C.E.)	3.25	2.45					4.0	
	613	3.11	2.70		.018		.036	4.01	
	623		2.95				.047	4.09	
	633		2.91				.054	4.08	4.06

HEAT	LADLE NO.	C	Si	Mn	S	P	Mg		
VII	(AIM)	3.60	2.50	.35	.025	.03	.045		
	71(4GS2.3)	3.73	2.54	.45	.025		.039		
	72(4GS4.6)		2.62				.043		
	73(4DS.4SS)		2.60				.038		
	74(4GP.4487W)	3.64	2.94		.024		.043		
	75(4P.W)		2.67				.037		
	76(4200.W)		2.73				.040		
	77(4212.W)	3.74	2.90	.49	.025		.048		
	78(4270.W)		2.91				.043		
	Average	3.70	2.74	.46	.025	.03	.041		
VIII	(AIM)	3.60	2.50	.35	.025	.03	.045		
	81(5200.W)	3.70	2.35	.47	.015	.031	.030		
	82(5212.W)	3.68	2.40	.37	.024	.024	.053		
	83(5270.W)	3.66	2.41	.42	.021	.029	.051		
	84(5GS)	3.65	2.46	.40	.018	.024	.054		
	Average	3.67	2.41	.42	.020	.027	.047		

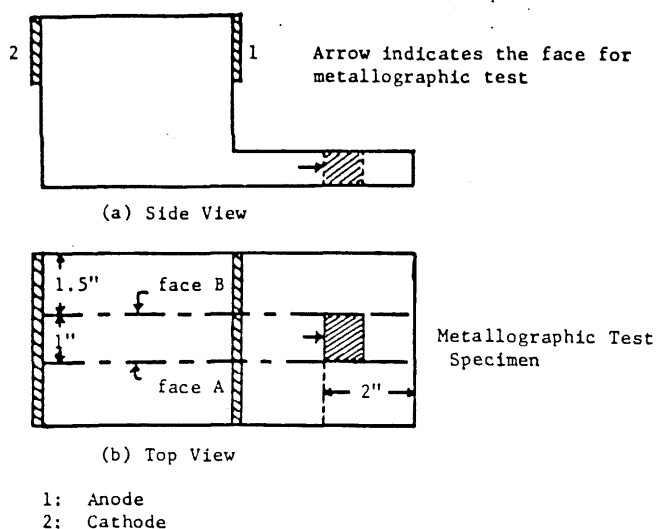


Fig. 5. The positions of electrodes for the magnetic particle inspection, cuts for the cut section inspection and metallographic test.

no-bake sands) were tested in this heat. The sulfur content was controlled by adding ferrous sulfide to the ladle with the postinoculant to obtain the two higher sulfur contents. The amount of magnesium ferrosilicon utilized as the nodularizer was increased to accommodate the greater requirement for magnesium.

Heat V studied the influence of magnesium content as well as the three pouring temperatures and two types of molding materials. The three residual aim magnesium contents were controlled by varying the amount of magnesium ferrosilicon as nodularizer. To obtain 0.03%Mg, .067~0.086%Mg as MgFeSi was plunged, for 0.05%Mg, 0.111~0.143%Mg was added, and for 0.08%Mg 0.18~0.23%Mg was added.

The purpose of Heat VI was to investigate the influence of carbon equivalent, along with the three pouring temperatures and two types of molding materials as in the previous heats. Three levels of carbon equivalent were selected: 4.0, 4.3, and 4.5%. The aim analysis was 3.25%C, 2.45%Si for the 4.0%CE, 3.55%C, 2.45%Si for the 4.3%CE, and 3.85%C, 2.15%Si for the 4.5%CE. To control the carbon content, fine graphite was added to the furnace. Commercially pure silicon was also used to control the silicon content without greatly affecting the number of nucleation sites for graphite nodules in the casting. This silicon was added to the ladle together with the postinoculant.

In Heats IV and V, it was considered to be possible that more dross was entering the first mold poured from each ladle compared to the second mold from each ladle. To investigate this possibility, the order of filling the molds from each ladle was reversed on Heat VI compared to Heats IV and V. On Heat VI the green sand mold was filled first followed by the no-bake mold. This reversed the order of filling used in the two previous heats. Ferrous sulfide was added in the furnace in Heat VI to increase the sulfur content to 0.025% before treatment and to provide a little more dross in the test casting. The sulfur content before treatment in Heat V was below 0.02%.

Heats VII and VIII were conducted to investigate mold effects as previously discussed. These heats were made with an aim analysis of 3.5%C, 2.5%Si, and some added iron sulfide to attain a sulfur level of .025% before treatment.

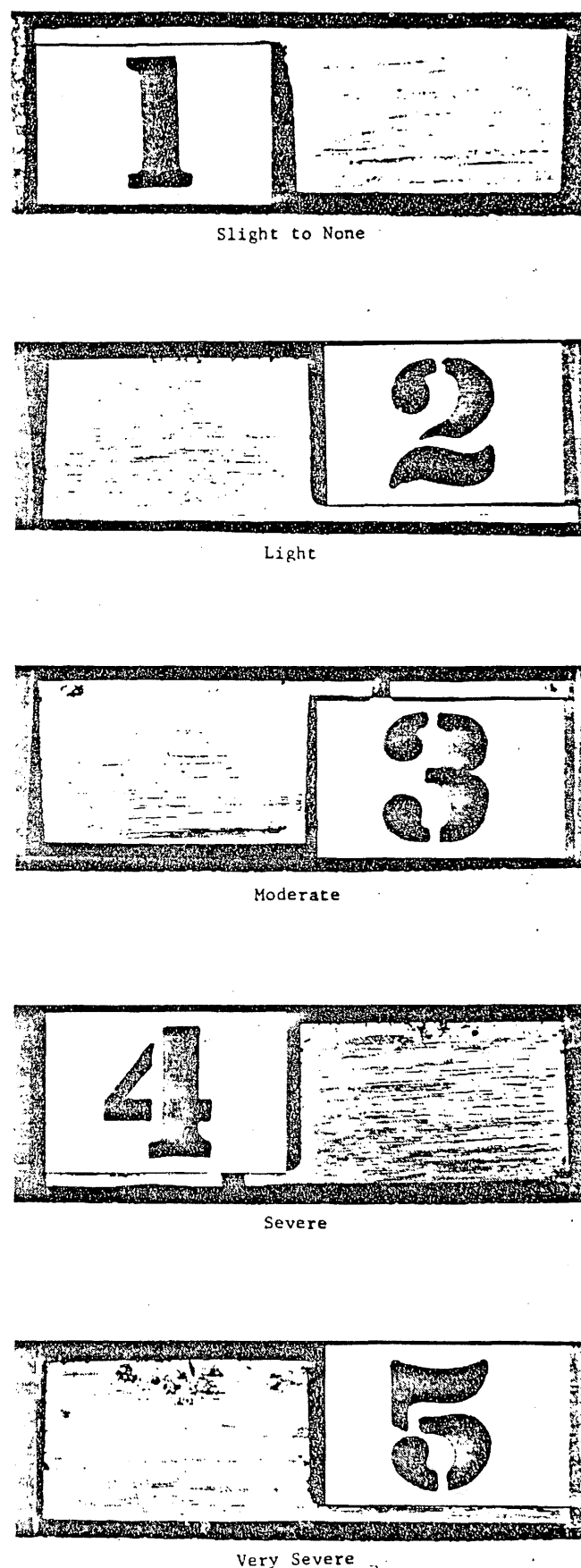


Fig. 6. Cut section visible dross rating scale—1 through 5, least to most severe.

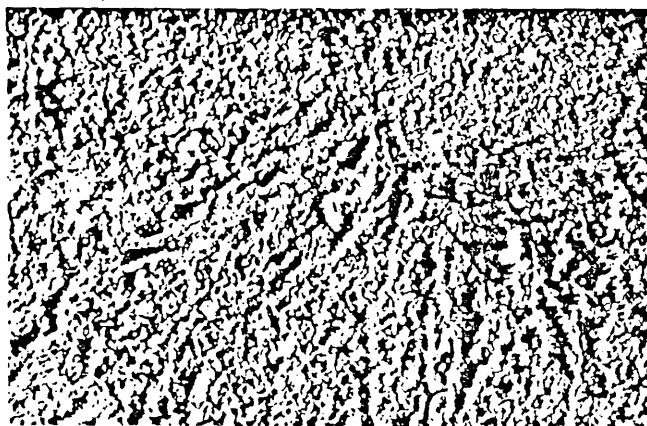


Fig. 7. Magnetic particle pattern on B-face of casting bottom-gated through heavy section, pressurized system, from Heat III. 1X.

Cleaning and Evaluation of the Castings

The castings were allowed to remain in the mold overnight and had completely cooled when shaken out. After shakeout, the castings were cut off at the riser neck and at the ingate and the test portions of the castings were sandblasted clean. The surfaces were first inspected visually for dross-related defects. Then the horizontal cope surfaces of each casting were examined by magnetic particle inspection. Faces B and D of Fig. 1 were tested on bottom-gated castings, and faces F and H tested for the top-gated castings in Heats I and II. Since all heats from III to VIII were cast as shown in Fig. 4, faces B and D only were examined. Magnetic particle examination consisted of passing a current of 250 to 270 amperes through the long direction of the casting and sprinkling Magnaflux No. 1 Gray Powder or Magnaflux No. 8A Red Powder lightly and evenly over the surface. The current was supplied by a DC motor-generated arc welding unit. The location of the probes on the surface B is indicated in Fig. 5. Since practically no dross was located in the thin section, surface D was not examined.

After magnetic particle examination, the subsurface dross was observed directly by sectioning the casting with one or two vertical cuts in the long direction. The single cut used in Heats I to III was centrally located; the two cuts in Heats IV to VIII were positioned as shown in Fig. 5. The cut sections were cleaned on a belt sander using 80-grit material and examined macroscopically for visible inclusions. Fig. 6 shows several of the cut sections.

Examination of the cope surfaces of the test casting provided the means for evaluating the severity of drossing. Visual surface appearance was often misleading in determining dross severity, so it was not used. The magnetic particle inspection and visual examination of the cut surfaces provided better means of assessing dross severity. A scale of 1 through 5 was employed for each method. On this scale, 1 is the lower end indicating either uniform distribution of the iron powder over the surface or the very light dross shown in Fig. 6. As the intensity of the magnetic particle indications increased, the rating scale was increased to 5. Figure 7 demonstrates a 5 severity for magnetic particle examination. The increasing severity of dross on the cut sections for the different ratings is illustrated in Fig. 6.

After macroexamination, smaller metallographic specimens were taken from selected castings from all three heats to examine particles of "dirt" trapped within the matrix.

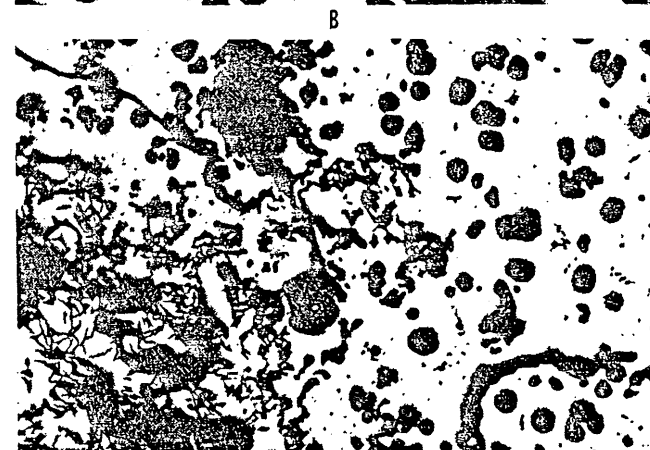
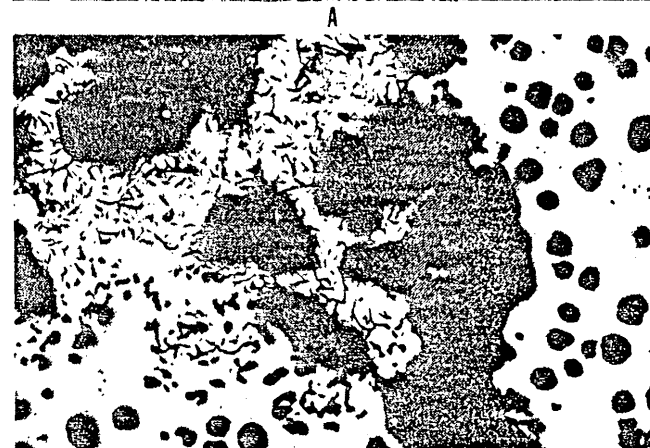


Fig. 8. Chunk dross defects shown in (a) and (b). Stringer dross defects shown in (c) and (d). 50X.

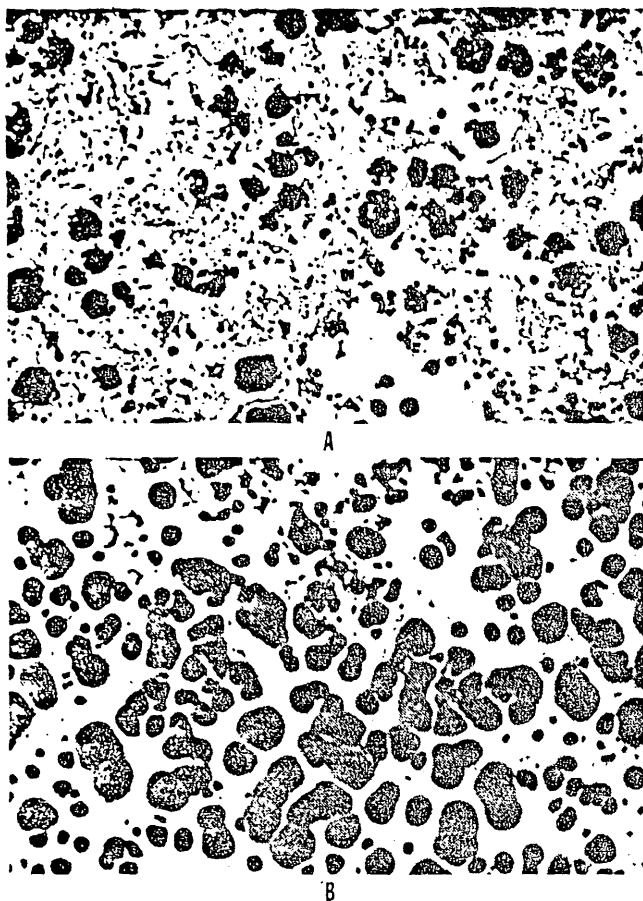


Fig. 9. Region of graphite flotation near cope surface.

Chemical and Metallographic Examination

A disc specimen 1½-in. in dia was cast in a graphite chill block for each ladle poured. Most of these specimens were analyzed by the American Cast Iron Pipe Company. Carbon and sulfur analyses were made using combustion techniques, and the other elements were determined spectrographically.

The metallographic test specimen and the location of the vertical face that was examined are shown in Fig. 1. The samples were ground and polished in the usual manner and examined before and after etching with nital. The metallographic examination determined the nodularity per the ASTM classification, the number of graphite nodules per square millimeter and the percentage of intercellular pearlite present in the microstructure. All graphite structures were entirely Type I and II nodules.

RESULTS AND DISCUSSION

Chemical Analyses and Microstructures

The individual ladle and average chemical analyses for each heat are listed in Table 6. Some variations from the aim compositions were obtained. The manganese levels were very close to the desired 0.40% except on Heat IV that went through an oxidizing period during melting that reduced the manganese to lower values. The phosphorus contents of all heats was about 0.02%. Heat I was slightly lower in carbon and silicon than the aim, but still hypereutectic at 4.54%CE. Heat II was nearly 0.3% lower in carbon and 0.1% higher in silicon than the aim, but hypereutectic

at 4.46%CE. Heat III was close to the aim composition with a carbon equivalent of 4.63%. Heat IV was the only heat that was significantly lower in carbon, silicon, and manganese because of its oxidizing period during melting. This heat was nearly 0.3% low in carbon and 0.2% low in silicon on the average; the carbon equivalent was reduced to a hypoeutectic value of 4.09%. Heat V was slightly high in carbon and somewhat high in silicon with a carbon equivalent of 4.62%. Heat VII was 0.1% high in carbon and 0.24% high in silicon with a carbon equivalent of 4.61%. Heat VIII was very close to aim in both carbon and silicon and had a carbon equivalent of 4.50%. Heat VI had intentional variations in carbon equivalent to show the influence on dross formation. The high carbon equivalent was 4.59%, medium was 4.47%, and low was 4.06%. Most of the heats were deliberately produced to a fairly high carbon equivalent in the 4.4 to 4.6% range so that the opportunity for dross formation would be high and the effects of the other variables increased.

The desired magnesium level for all heats except Heats IV and V was 0.04–0.05%. This somewhat higher than normal level was desired to assure a completely nodular structure and to provide a magnesium content on the high side so that significant dross formation would be obtained. The residual magnesium contents on Heats IV and V varied because those heats had deliberate variations in the sulfur content before treatment and in the magnesium content after treatment. The average magnesium contents on all except Heats IV and V are 0.041–0.059% with some individual values as low as 0.030 and as high as 0.073%. Heat IV had deliberate variations in sulfur before treatment of 0.08, .035, and .02%; the high sulfur irons had 0.074 and .065%Mg; the medium sulfur 0.054–0.062%Mg and the lowest sulfur 0.052–0.053%Mg. The average residual sulfur contents were 0.03, 0.02, and .0065% for the 0.08, .035, and .02% original sulfur contents, respectively. Heat V was designed to have three levels of retained magnesium, 0.08 with an actual average of 0.075%Mg; .05 with an actual average of 0.052%Mg; and .03 with an actual average of 0.036%Mg. The retained sulfur contents varied from 0.006–0.025% on all except Heat IV with its deliberately high contents. The magnesium recovery increased with lower treating temperatures, averaging about 35, 40, and 45% for the 2600, 2500, and 2400F pouring temperatures, respectively.

The higher amounts of magnesium alloy used to attain the higher residual magnesium and increased dross generally assured Type I and Type II nodules. Degenerate nodules did occur near dross inclusions and in the area of carbon flotation, as illustrated in Figs. 8 and 9, respectively. Some exploded on Type V graphite occurred in the carbon flotation area in Heat III and with the low and medium carbon equivalent irons in Heat IV. Some difficulty was experienced with the magnesium treatment in the first ladle on Heat VIII producing some exploded graphite. In general, however, the nodularity was greater than 85% and mostly 100%. The nodule count and percent of intercellular pearlite in the matrix are listed in Table 7. The various castings are described in Tables 1, 2, and 3. The nodule counts increase with higher carbon equivalent and the amount of pearlite decreased with higher silicon content in the various irons.

Results From Heat I and Heat II—Gating Effects

Heat I was produced using the 16 gating arrangements sketched in Figs. 2 and 3 and listed in Table 8. After removal of gates and risers, visual examination of the casting surfaces showed a few dross discontinuities. In one instance, a fairly large dross chunk was located at the cope surface of a casting produced with the

20% choke pressurized gating system. However, in all other cases where a discoloration or some surface indication of dross did appear, the castings were sectioned but no dross projected beneath the surface. The surface-only defects are a result of a film of dross and slag forming on the melt surface, without further thickening and breakup into chunks that occur below the surface. The nature of the dross layer is highly temperature dependent. At the high pouring temperatures of the first heat (over 2700F) very few larger size dross inclusions can form.

The results of Heat I indicated that the castings were clean and insufficient dross was present to allow for a systematic evaluation of gating system and ingate location. It is apparent that pouring temperatures over 2700F minimize dross irrespective of gating conditions.

Heat II was poured in the range of 2400–2450F with gating variables exactly as in Heat I. The seventh of the eight ladles was cooled to so low a temperature that molds B1X and D1X (see Table 8 for description) did not fill properly. Therefore, these two

castings were disregarded and the remaining fourteen were studied.

The castings produced in this second heat had considerably larger dross defects than in Heat I. This was readily apparent from a visual examination of the casting surfaces. The heavier dross defects were evident visually for a portion of the B-face of a bottom-gated casting produced with the 20% choke pressurized system (see Fig. 1 for identification of casting surfaces). Magnetic particle inspection of the cope surfaces was followed by cutting the castings and rating dross severity on a scale from 1 to 5. Ratings for both the magnetic particle and cut section methods are tabulated in Table 9 and plotted in Fig. 10. The magnetic particle evaluation compares reasonably well to the ratings based on the cross-sectional cut.

Heat II results are averaged in Table 10 to permit an evaluation of each variable concerned with gating. The first major conclusion is that gating into the heavy section of the test casting (average cut section rating 3.7) produced more cross than thin section gating (rating 2.2). This result was expected since metal introduced

Table 7. Metallographic Results

HEAT	SAMPLE	NODULE COUNT (per sq. mm)	PERCENT INTERCELLULAR PEARLITE	HEAT	SAMPLE	NODULE COUNT (per sq. mm)	PERCENT INTERCELLULAR PEARLITE
I	A1	200	25	V	4322	148	50
	B1	110	20		4331	216	20
	C1	240	10		4332	228	45
	D1	170	15		5111	188	40
	A2	160	25		5112	152	40
	B2	140	15		5121	228	20
	C2	180	15		5122	200	25
	D2	150	20		5131	240	20
	A3	220	10		5132	264	20
	B3	150	5		5211	168	25
	C3	210	15		5212	128	25
	D3	140	10		5221	248	20
	B1-X	180	20		5222	276	20
	D1-X	170	30		5231	276	20
II	B3-X	190	25	VI	5232	176	20
	D3-X	170	30		5311	136	40
	A1	270	20		5312	132	40
	B1	270	less than 5		5322	196	40
	C1	380	5		6111	220	45
	D1	190	15		6112	236	45
	A2	240	less than 5		6121	256	40
	B2	200	less than 5		6122	268	40
	C2	330	5		6131	212	40
	D2	300	less than 5		6132	248	55
	A3	270	5		6211	216	40
	B3	240	less than 5		6212	216	50
	C3	290	less than 5		6231	224	40
	D3	150	less than 5		6232	212	25
III Cored	B3-X	230	15		6311	188	25
	D3-X	260	10		6312	192	55
	ST	240	40		6321	184	35
	FG	250	35		6322	184	35
	HS	240	35		6331	244	40
	HM	290	45		6332	180	30
	DR	210	40	VII	7SC2	256	50
	CR	130	55		7SC3	204	55
	SS	140	40		7SC4	216	55
Non-Cored	ST	210	15		7SC6	200	55
	FG	160	20		7DS	172	40
	HS	260	5		7SS	232	50
	HM	250	20		7P	200	40
	DR	180	10		7PW	220	35
	CR	160	20		7200	144	40
	SS	150	20		7200W	180	40
IV	4111	260	60		7212	172	35
	4112	356	55		7212W	184	25
	4121	176	40		7270	224	30
	4122	344	40		7270W	224	40
	4221	220	30		7487W	128	25
	4222	236	55	VIII	8200	88	40
	4231	136	5		8200W	52	40
	4232	236	50		8212	208	50
	4311	232	40		8212W	176	55
	4312	312	50		8270	244	40
	4321	168	25		8270W	248	40
					8GS	192	50
					8GS	192	55

Table 8. Description of Casting Codes from Heat I and Heat II

CODE	GATING SYSTEM	CASTING IN COPE/Drag	INGATE TO THIN/HEAVY SECTION	CORE PRESENT IN HEAVY SECTION
A1	20% Choke (Pressurized)	Cope	Heavy	No
B1	20% Choke (Pressurized)	Cope	Thin	No
C1	20% Choke (Pressurized)	Drag	Heavy	No
D1	20% Choke (Pressurized)	Drag	Thin	No
A2	4:8:3 (I.N.C.O.)	Cope	Heavy	No
B2	4:8:3 (I.N.C.O.)	Cope	Thin	No
C2	4:8:3 (I.N.C.O.)	Drag	Heavy	No
D2	4:8:3 (I.N.C.O.)	Drag	Thin	No
A3	1:4:4 (Non-Pressurized)	Cope	Heavy	No
B3	1:4:4 (Non-Pressurized)	Cope	Thin	No
C3	1:4:4 (Non-Pressurized)	Drag	Heavy	No
D3	1:4:4 (Non-Pressurized)	Drag	Thin	No
B1X	20% Choke Pressurized	Cope	Thin	Yes
D1X	20% Choke Pressurized	Drag	Thin	Yes
B3X	1:4:4 (Non-Pressurized)	Cope	Thin	Yes
D3X	1:4:4 (Non-Pressurized)	Drag	Thin	Yes

Table 9. Magnetic Particle Inspection Ratings and Cut Section
Dross Ratings from Heat II**Key**

- 1 slight
2 light
3 moderate
4 severe
5 very severe

Gating System	Casting Location	Section Gated	Core	Magnaflux Rating Number *	Cut Section Rating Number **
20% Choke	Cope	Heavy	No	5	5
20% Choke	Cope	Thin	No	3	3½
20% Choke	Drag	Heavy	No	3	1½
20% Choke	Drag	Thin	No	3	3½
4:8:3	Cope	Heavy	No	2½	3½
4:8:3	Cope	Thin	No	2	2
4:8:3	Drag	Heavy	No	2	3
4:8:3	Drag	Thin	No	1	1
1:4:4	Cope	Heavy	No	3½	4
1:4:4	Cope	Thin	No	2½	2
1:4:4	Drag	Heavy	No	4	5
1:4:4	Drag	Thin	No	2	1½
1:4:4	Cope	Thin	Yes	3½	1½
1:4:4	Drag	Thin	Yes	3	4

* "Magnaflux" Number 1 gray powder was used in the magnetic particle inspection. The horizontal surfaces facing the cope were checked in this inspection. The rating assigned each casting was based on an overall consideration of the pattern which developed upon application of electric current.

** Each casting was cut longitudinally through the center of both the thin and heavy sections into two equal pieces. A visual determination was then made of the severity of the dross in the cast vertical sections.

through the thin section experiences less turbulent flow into the mold cavity, thus reducing the oxidation products that form as a result of turbulence and metal-air contact.

This data also indicates that bottom-gating causes more dross inclusions than top-gating. This result is somewhat surprising since bottom-gating introduces the metal less turbulently. Table 11 reveals that this effect is not pronounced in most cases and reversed

in one case. The data indicates that this factor is not as significant as heavy versus thin section gating. In larger castings, where top-gating would mean metal falling over larger distances, bottom-gating could prove to be superior in producing clean castings.

Evaluation of the results obtained with the three gating methods indicated that the 4:8:3 system is superior to both the 20% choke pressurized and 1:4:4 non-pressurized system. The tapered sprue

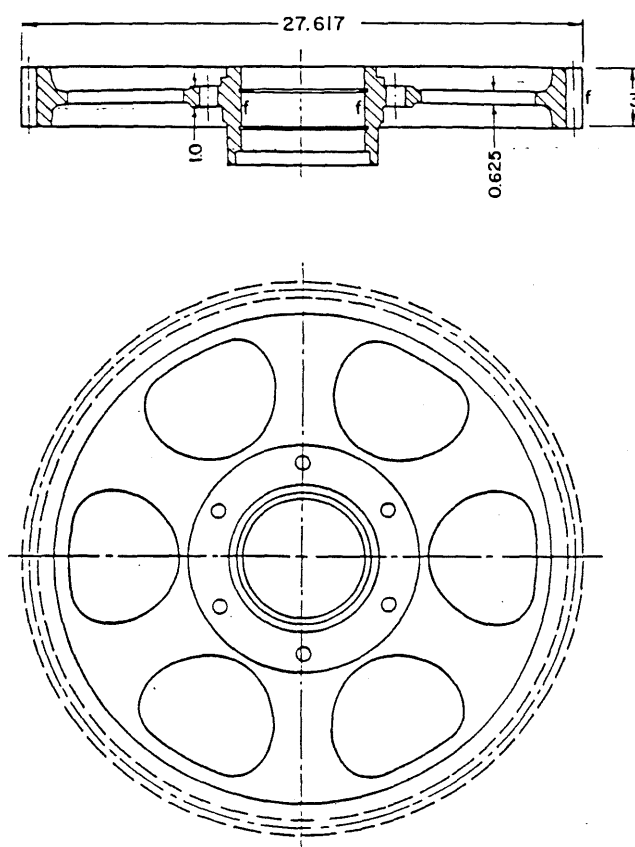


Fig. 3. Drawing of gear.

prepared from the solid geometric data base, Fig. 1. These capabilities are expected to eventually result from the UW CAST METALS PROGRAM. The final consequence of this approach to computer aided casting design and process engineering is expected to be the most functional and cost efficient cast component or product.

TWO DIMENSIONAL ANALYSIS

While casting process engineering and computer aided design becomes feasible from a 3D geometric data base, the expenditure of time and money for a 3D analysis may not be necessary for all aspects of casting processing. For castings of appropriate shape, a 2D analysis can accomplish some of the same objectives. Actually the conventional machine drawing serves as the 2D geometric data base from which engineering analyses are presently made. This is suggested in the middle of the CMP Information Flow Plan, Fig. 1. Casting shapes exhibiting symmetry with respect to an axis can most easily benefit from 2D analysis. Castings of revolution such as gears, wheels, pulleys or their sectors and extrusions such as plates, bars, rods, levers and arms can have the needed axial symmetry.

For 2D analysis, computer programs employing the Apple II or II Plus computer with disk drive, graphics digitizing tablet and pen, printer and video TV monitor have been specifically developed for calculating casting volume, weight and freezing order; the former called WEIGHT and the latter called ORDER (2).

The functioning of the program is best illustrated by means of

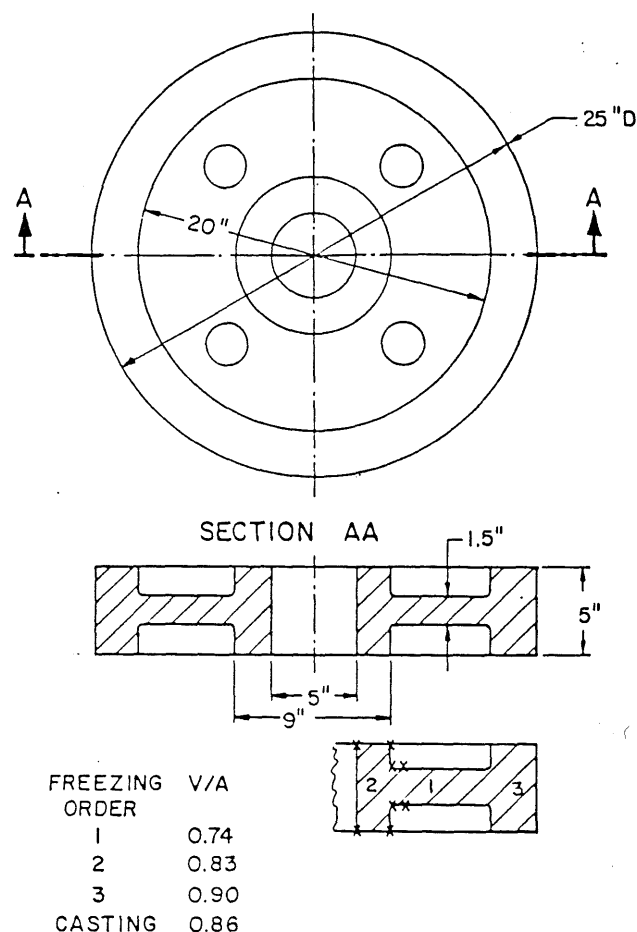


Fig. 4. Gear blank drawing.

a videotape. However, for the present reader's convenience a brief description of the tape presentation will be offered using the drawing of a gear shown in Fig. 3. The blueprint of the casting is placed on the digitizing tablet. Two points on the axis of rotation are entered first from the blueprint using the digitizing tablet and pen. The dimensional scale is then established by entering two points of known distance on the blueprint so that all subsequent dimensions are scaled accordingly.

Then, the outline of the cross section of revolution is entered point by point from the blueprint on the tablet. The cross section is revolved around the symmetry axis to calculate the total volume. The program permits lightening holes to be deducted from the gross volume as extrusions (bosses can be added) to obtain the correct volume. Casting weight is calculated from the volume by entering metal density. For the casting shown in Fig. 3, volume is 678.4 cu. in.; giving 192 lbs. as the finished casting weight at a steel density of 0.283 lbs./cu. in. Casting allowances and tolerance can be added to the component drawing and this will increase the volume and weight by known amounts. For the example, if a tolerance of +0.060-0.00 in. and a 0.25 in. machine finish allowance are added, the maximum casting weight to be expected then becomes 279 lbs. Since the tolerance is a +0.060 maximum, the casting will most likely weigh somewhat less than 279 lbs.

The ORDER sequence in the videotape is needed for risering the casting for soundness. The freezing order calculation is based on Chvorinov's rule which states that solidification time of a casting or portion of a casting is a function of its section

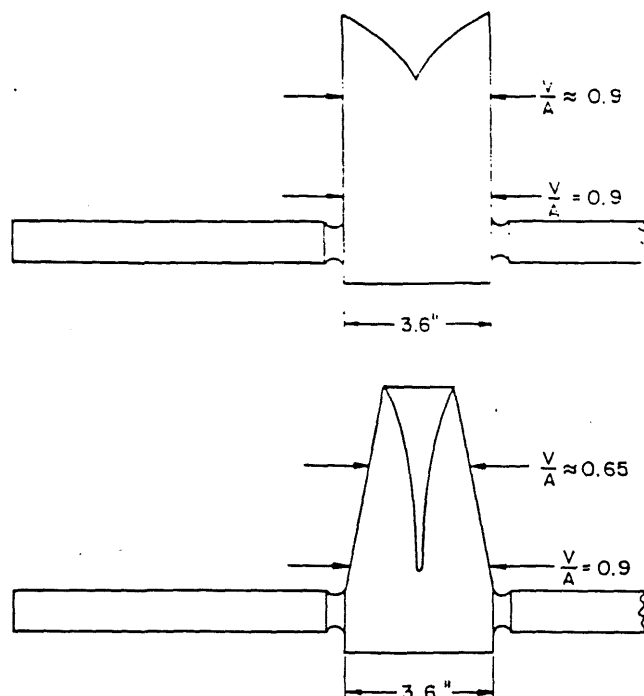


Fig. 5. Piping cavity in conventional cylindrical risers versus hollow tapered risers.

PIPING IN CYLINDRICAL vs. TAPERED RISER

modulus. Section modulus is defined as the casting volume divided by its surface area transferring heat out of the casting; V/A . In a 2D cross section, section modulus is estimated by cross-sectional area divided by perimeter.

This is the assumption on which the 2D solidification order analysis presented on the videotape is based. The order of solidification for the casting in Fig. 3 is web first, hub T-junction second and flange T-junction last as shown on the videotape.

Figure 4 provides another gear blank casting example of section modulus — freezing order determination by this technique. The modulus of the entire casting was found to be 0.86 in. The hub T-junction modulus was determined to be 0.83 in., the web 0.74 in. and the flange T-junction 0.90 in. Freezing order and modulus is shown as the video screen as in Fig. 4 and may be recorded by a printer. Both T-junctions freeze later than the web and therefore require risering. Web section may be thick and so that only the flange T-junction needs to be fed. With section moduli known the Riser system can then be designed.

TWO DIMENSIONAL MODELING FOR RISERING

Either the 2D or the 3D geometric data base is necessary to proceed with foundry process engineering including risering and gating. Because the 2D data base can be used for many castings, both the 2D and 3D techniques are being developed as a part of CMP, Fig. 1 center. Since 3D geometric modeling is not yet convenient, only the 2D geometric risering approach will be discussed here using steel castings for examples.

Conventional risering of steel castings suggests that V/A of a cylindrical riser should be 1.2 to 1.4 times V/A of the casting, Fig. 5, without feeding aids. The feed metal transferred to the casting leaves a cavity in the riser (and sprue) which most frequently appears as in Fig. 5, upper (4). Directional solidification to a point within the riser and just outside the casting along with feed metal to keep that point full is necessary.

However, tapered risers can accomplish this just as well as cylindrical risers. The tapered riser shown in Fig. 5, lower, has V/A riser $> 1.2 V/A$ casting at its largest section near the base but narrows in diameter toward its top. Several examples of open and blind tapered side risers which fed a one inch steel plate are shown in Fig. 6. The tapered riser is intended to leave a hollow shell that results from the most efficient use of feed metal.

The thickness frozen at different levels in a tapered riser may be 2D modeled. The thermal wall or thickness frozen that keeps the pipe open must become thicker as time increases during the feeding period as can be seen in the risers of Fig. 6. Thickness frozen follows the general equation $TF = c\sqrt{t} + b$ where TF = thickness frozen, t = time and c and b are constants.

Thickness frozen may be calculated in several ways or empirically determined from sectioned tapered type risers such as those shown in Fig. 6. Geometric analysis can be approached via the section modulus, V/A and Chvorinov's rule, $\log t = K \log (V/A)$. The equation for steel adapted from Fig. 19 of C. W. Briggs, AFS Transactions 1962, is $\log t = 1.8739 \log V/A + 1.13487$, where t = minutes and V/A = inches. However, this equation is not necessary for modeling the risers.

The thickness frozen within a shape can be viewed as having the exterior surface area for heat transfer and a volume which depends on the solidified thickness. This concept of visualizing solidification fronts was suggested by R. M. Kotschi. The V/A of thickness frozen can be calculated from $V/A = D(1-a^2)/4$, Fig. 7. Thickness frozen, $TF = (D-aD)/2$, Fig. 7, where a is a fraction of D as 0 to 1.0. From these equations, graphs of thickness frozen vs. section modulus of the thickness frozen for diameters from 1.0 to 6.0 in. can be constructed as in Fig. 8. If the relationship between solidification times and V/A is recalled, Fig. 8 indicates that at $V/A = 0.5$, a 2.0 in. D riser might be solidified to the center, a 3.0 in. D riser could have a thickness frozen of about 0.32 in., and a 6.0 in. D riser would have a thickness frozen of about 0.28 in.

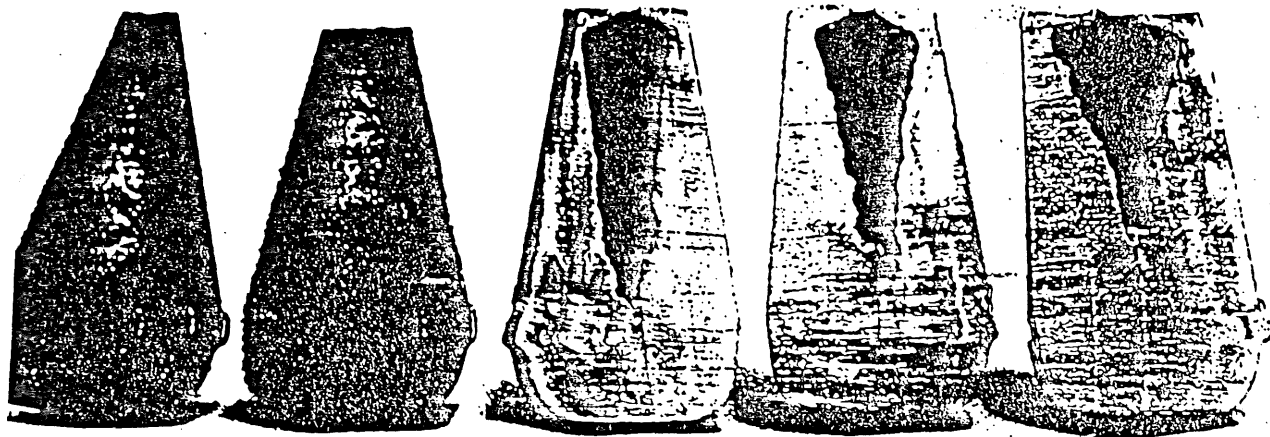


Fig. 6. Tapered blind and open side risers used to feed 1 in. carbon steel plates.

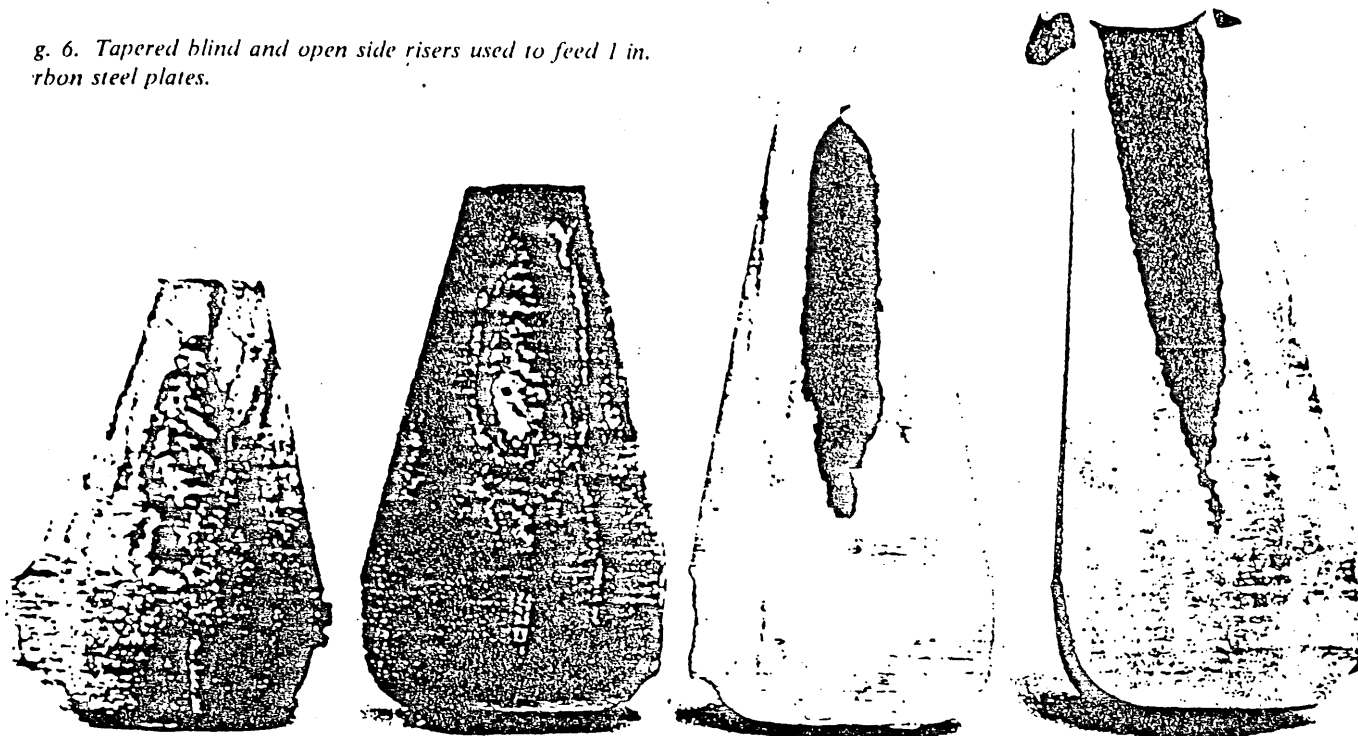
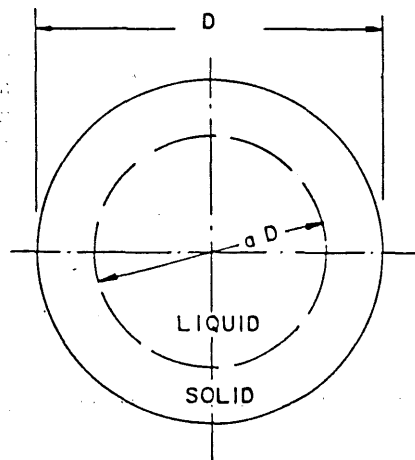


Fig. 7. Equations for calculating the section modulus, V/A , of the thickness frozen, TF , in a riser.



$$\text{EQN.1 THICKNESS FROZEN, } TF = \frac{D - aD}{2}$$

$$\text{SECTION MODULUS OF } TF = \frac{V}{A} \times \left(\frac{\pi D^2}{4} - \frac{\pi a^2 D^2}{4} \right) \div \pi D$$

$$\text{EQN.2 } \frac{V}{A} = \frac{D(1 - a^2)}{4}$$

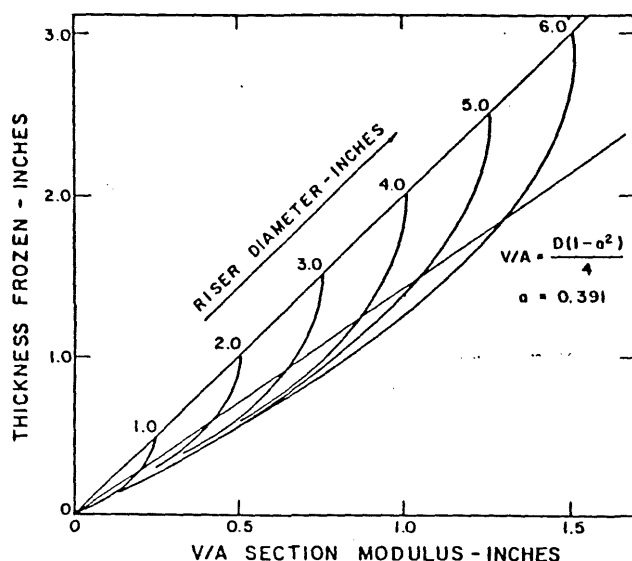


Fig. 8. Graphical relationship of thickness frozen, TF , to section modulus of the thickness frozen for riser diameters of 1.0 to 6.0 in.

Tapered Riser Design

The extent to which tapered risers actually behave, as suggested by Fig. 8, can be measured from the thickness frozen at a given diameter of a number of sectioned risers such as the ones shown in Fig. 6. The line on Fig. 8 labeled $a = 0.391$ is an example of this behavior in many ductile iron risers. The taper design can be varied as suggested in AFS preprint 82-20, Ref. 5 and as is evident in Fig. 6 and Fig. 9. The top diameter, maximum bottom diameter and height are obvious design features of importance as discussed in Ref. 5. The bottom diameter, D_R is designed from equation 3. $D_R = 2W + D_p$, where W is the casting's effective section thickness at the riser's location and D_p is the diameter of the pipe calculated by the geometric risering technique for cylindrical risers, Ref. 5 and 6. The effective section thickness $W = 2 \times V/A$ where V/A is the section modulus at the casting

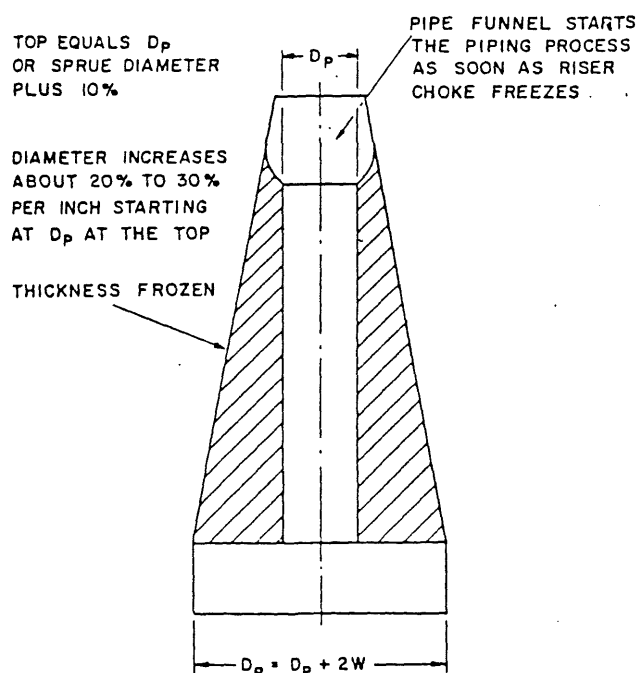


Fig. 9. Example of design system for tall tapered risers.

where the riser is to be attached. Equation 3 then becomes $D_R = f \times V/A + D_p$.

The geometric technique of risering has been amply publicized in the literature, Ref. 5-7, and will therefore not be discussed again. However, calculation of D_p requires feed metal constants for steel. Feed metal volume required is expressed by equation 4 as F_m , cu. in. = $K(V_c + V_R)$ where V_c = casting volume, cu. in.; V_R = riser volume, cu. in. and K is a constant dependent on section thickness. From experiments on 1.0 in. carbon steel plate castings, $K = 2.5$ to 3.0%. If the conservative assumption is made that yield is 50% or greater, true when tapered risers are used, then equation 4 becomes $FM = K \times 2 V_c$.

The value of K is being studied for geometric and metal factors. The value of D_p is then calculated from $FM = \pi(D_p^2 \times H_p/4)$ where H_p is the riser height chosen on the basis of mold rigging or cope height. The feeding section of the riser can then be completely dimensioned. A typical design is shown in Fig. 9 and an example of a tapered open top riser from a commercial steel casting is shown in Fig. 10. Gating design is an important feature of this risering system. The gating is designed with a choke to cause it to freeze off quickly so that piping begins early after pouring.⁵

Optimizing riser shape and efficiency is an objective of CMP research. The same feed metal volume might be supplied by tapered risers of varying height and constant D_R at the base, Fig. 11. The shortest hollow riser will provide the highest yield. A stepped riser cylinder may be more efficient than a tapered riser. Fig. 12 shows stepped risers of cast carbon steel feeding 1.0 in. plates tapered to increase feeding distance. Efficiency calls for feed metal volume sufficient to feed both casting and riser leaving a hollow shell of minimum weight. Hollow shell tapered or stepped risers provide riser yield in the 55-70% range when yield is defined as casting weight divided by casting weight plus riser weight. The development of improved yield risering is another of the objectives of the CMP.

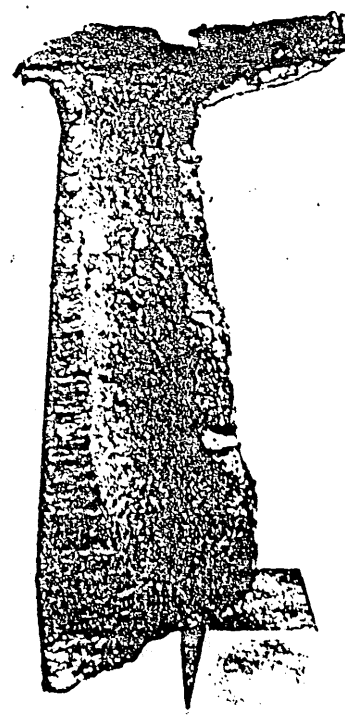


Fig. 10. Tapered open top riser from a commercial steel casting

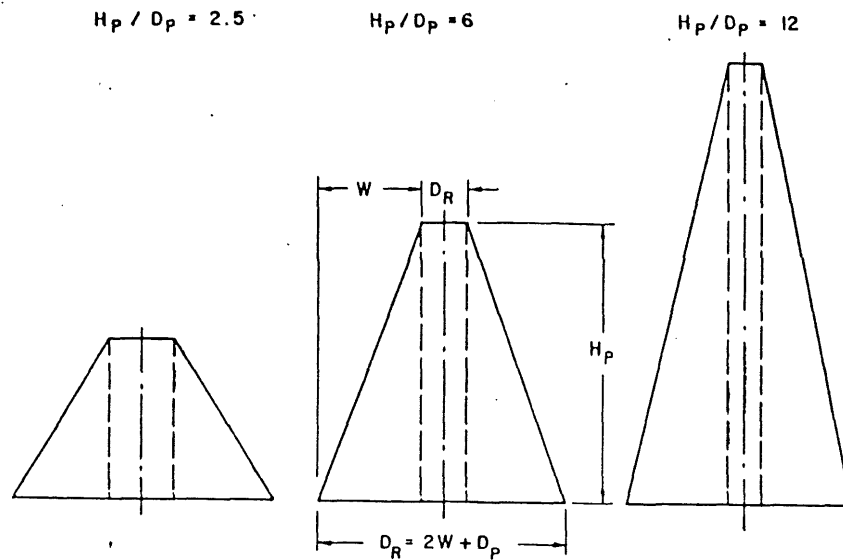


Fig. 11. Equal feed metal volumes are designed into each of the three shapes shown.

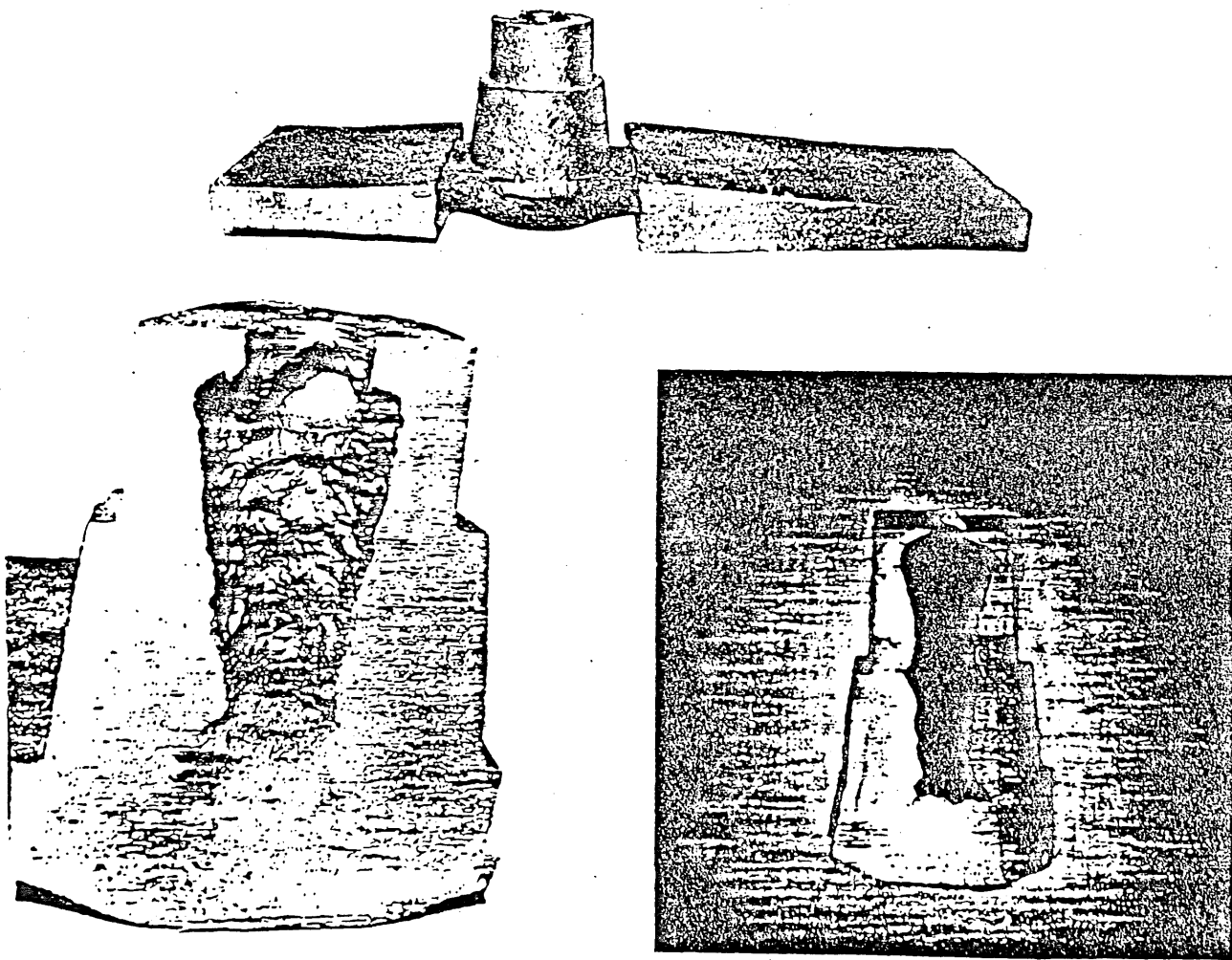


Fig. 12. Photograph of castings and stepped cylinders riser feeding 1.0 in. thick plates tapered 10% in thickness and 5 x 9 in. and 5 x 5 in. in width and length.

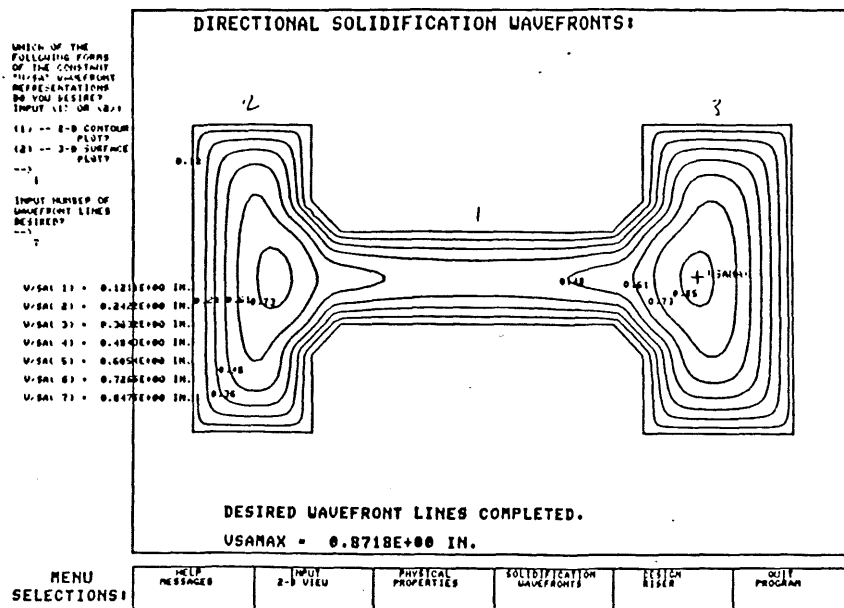


Fig. 13. Solidification wave fronts and their section modulus for gear blank in Fig. 4.

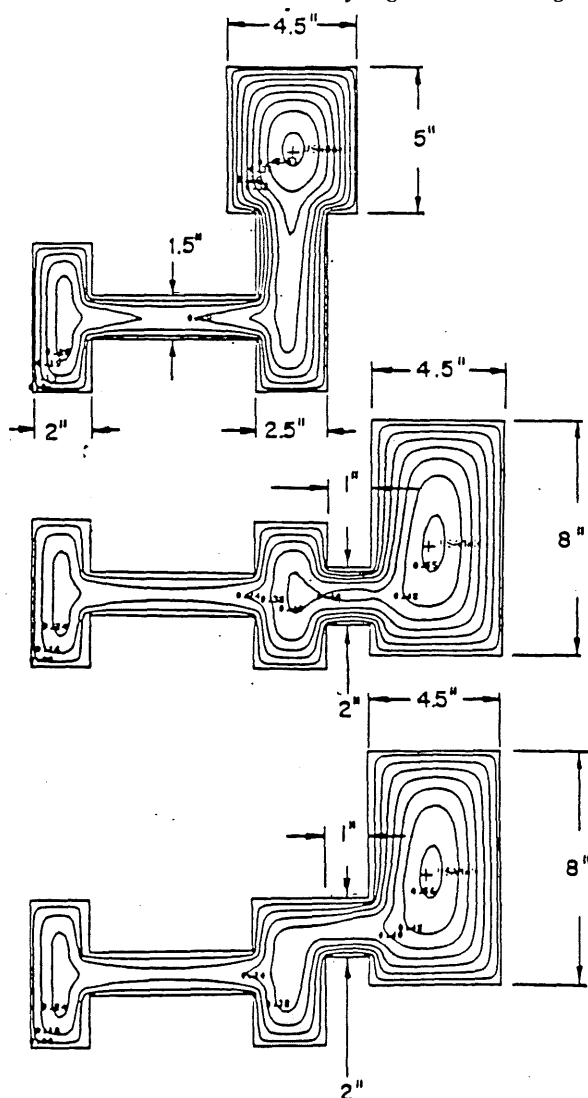


Fig. 14. Effect of risers and riser contacts on solidification wave fronts in gear blank casting of Fig. 12.

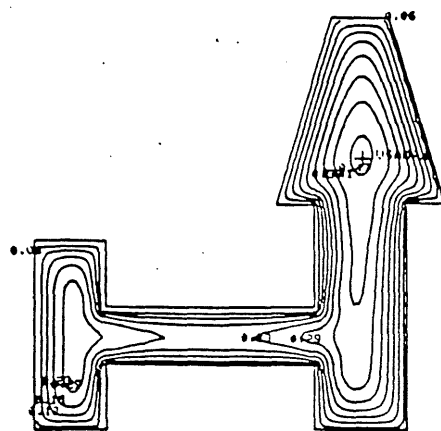
Geometric modeling and analysis can also be applied to riser contacts, bases of side risers and taper to promote feeding in various sections as has been described in articles in the bibliography of Ref. 6. Obviously, the entire system requires directional solidification from faster freezing casting regions toward slower regions, through riser contacts and finally into the riser. Meanwhile feed metal is transferred from the riser leaving behind a time dependent solidified thickness that reflects the solidification process in both the casting and the riser. The thickness solidified in the casting can also be modeled geometrically by 2D and 3D analysis of solidification fronts similar to the analysis suggested for risers.

MODELING OF SOLIDIFICATION FRONTS

Classical heat transfer calculations by finite element or difference techniques have been used to simulate the progress of solidification wave fronts with time from the casting surface to the end of solidification. Even for simple 2D shapes such as infinite cylinders, plates, bars, etc. large amounts of programming time and expense are required. Even larger amounts are necessary for 3D analysis and of course the solid geometric data base must be available to prepare the finite element grid. In addition the geometry must be described by the 3D data base in a way that permits the calculation methods to proceed, Fig. 1. To the extent that they are practical, 2D analyses should be developed as an alternative.

Solidification wave fronts may be geometrically modeled using the Chvorinov principle. The riser analyses of Fig. 6 and 7 provide an example. Computer software for calculating solidification fronts in casting cross sections has been developed through CMP research. The program results will be described in the case of the simple gear blank shown in Fig. 4.

The WEIGHT ORDER program gives the overall section modulus as 0.86; and freezing order as (1) the web as 0.74; (2) the hub junction as 0.83; and (3) the flange junction as 0.9. The



DESIRED WAVEFRONT LINES COMPLETED.
USAMAX = 0.4805E+00 IN.

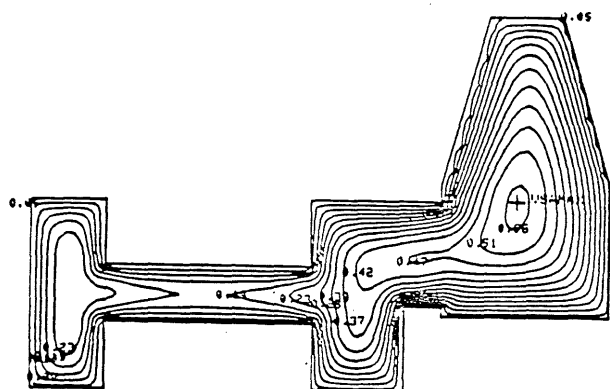


Fig. 15. Effect of tapered riser shape on the solidification wave fronts shown in Fig. 14.

results of solidification front analysis are shown in Fig. 13. The program indicates a maximum section modulus of 0.872 in. in the flange T-junction and a freezing order the same as in Fig. 4. The small difference between section modulus in the two different methods of analysis result from a slightly different scale input. Actually the difference in calculating $D_R = 4 \times V/A + D_p$ is only 4×0.872 vs. 4×0.9 or 3.48 vs. 3.60 in. with D_p remaining the same.

The software, called SOLIDIFICATION ANALYSIS [8] in Fig. 1, produces 2D solidification wave fronts in the gear blank cross section as shown in Fig. 13. Seven wave fronts and the last point to freeze are shown. Section modulus of each front and the maximum for the last point to freeze is tabulated on Fig. 13. Freezing order by the SOLIDIFICATION ANALYSIS program is the same as with the ORDER program and the section modulus values are nearly identical. The program permits analysis of regions of interest with more closely spaced wave fronts.

Adding risers and riser contacts to the casting results in interesting changes in solidification wave fronts depending on the location of the riser attachments. Only the flange riser location will be analyzed. Three points of attachment may be considered as shown in Fig. 14. The top riser would obviously be preferred from a yield viewpoint though it presents molding problems. The riser contact which produces an x-junction, Fig. 14 middle, leads to solidification delayed longer than if there were no riser attached and shrinkage would remain in the casting.

The riser contact which produces an L-junction, Fig. 14 lower, has favorable directional solidification although a slightly thicker contact would provide a margin of safety. Changing to the tapered riser shape alters the solidification fronts in the top and L-junction riser locations as shown in Fig. 15. Note that the final point of solidification (or maximum section modulus) moves closer to the riser contact in Fig. 15 vs.

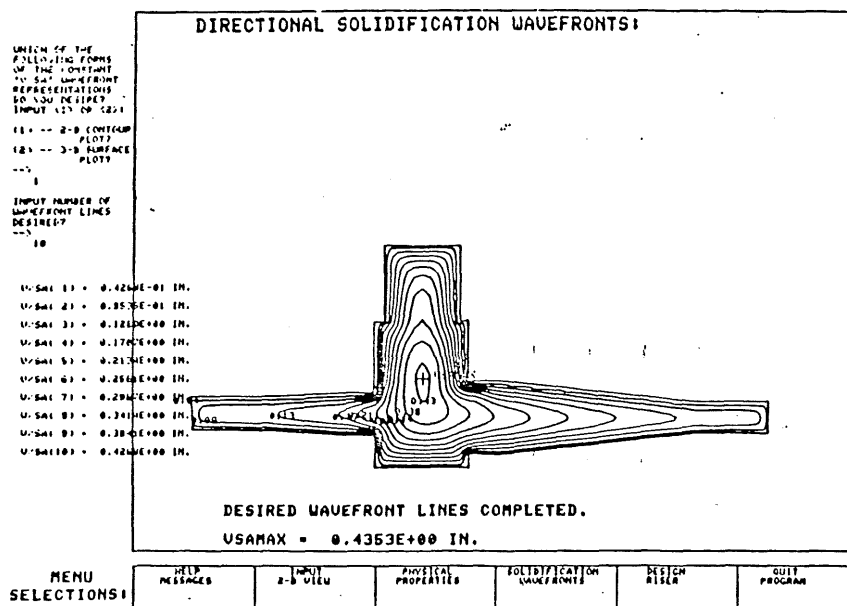


Fig. 16. Solidification wave fronts in cross section of tapered 1.0 in. plate and stepped riser as shown in Fig. 11.

Fig. 14. The tapered riser results in directional solidification in the riser from the top toward the riser contact. Liquid metal leaving the riser reveals the solidified thickness at its time of departure. Fig. 6, 10 and 12.

Geometric modeling of risers and riser contacts can lead to the most efficient use of feed metal and maximum yield. Obviously the casting exerts an influence on solidification of the contact and the riser because it alters the geometric configuration at the location where the casting must be fed. To use the principle of directional solidification at maximum yield efficiency apparently requires the kind of solidification front analysis just described. For example, tapered or stepped risers can cause directional solidification to be completed just outside the riser/casting contact. A solidification front analysis of the tapered plate—stepped riser combinations in Fig. 12 is shown in Fig. 16. Note the way the solidification fronts in Fig. 16 follow the step in the riser as revealed by the cross section of the riser in Fig. 12. Full utilization of these principles will require a 3D solid geometric analysis in some geometries while many more can be satisfied by the 2D method.

CMP SOFTWARE

As might be expected, the CMP software programs are being steadily modified to improve their usefulness. The WEIGHT ORDER program is presently being field tested by sponsors for foundry use. Modification of the program toward significantly increasing its capability for dealing with more complex shapes is under way. When the programs are satisfactory in practice, it is the intention of the sponsors and investigators to make them available through the AFS. The ORDER portion of the program will be followed by a RISER program that is currently being written.⁹

The SOLIDIFICATION ANALYSIS program is still in the early stages of development. Research with this program will continue to explore both the 2D and 3D capabilities of this type of solidification front analyses as applied to risering, effect of chills, insulation, feeding aids, etc. as well as the many geometric questions such as padding, cooling fins, core effects, riser contacts, riser configuration and riser design. Gating and the effects of gating on feeding from riser is another research area.

Presently, the SOLIDIFICATION ANALYSIS program uses a main frame computer. However, it may soon be possible to adapt the program to a minicomputer and this attempt is under study. Correlation of solidification front analysis with actual thermal analysis of solidifying castings is a desirable and necessary step to determine the extent to which the methods are basically sound.

The equations and language of the programs can be modified in many ways to ensure basic soundness in principle. The intent of this paper is not to examine questions of that nature but rather present useful engineering tools appearing on the computer horizon through the Cast Metals Program in the Computer Aided Engineering Center of the University of Wisconsin College of Engineering at Madison.

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Extending the Modulus Approach to Feeding to Account for Corner Effects

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ABSTRACT

The Chvorinov-based modulus method has been used successfully in riser design now for a number of years. The questions of correction for casting shape and end effects, which represent areas of uncertainty, have generally been handled by artificially adjusting the magnitude of the "mold constant." As a result of a recent study of the edge effect on casting solidification a new approach to such problems has emerged.

It has been shown in the present work that for a wedge-shaped mold intrusion or extrusion in contact with a molten metal, the difference between its absorption of heat per unit time and depth, and that heat absorption rate occurring over a corresponding planar mold, does not vary with time. That is, although both heat transfer rates vary with time, the extent of their difference is invariable with respect to time. Based on this theoretical finding, an analysis of the extent of the corner effect on the freezing time of bar castings with polygonal cross-section has been undertaken, and a Chvorinov-type formula derived. This shorthand formula uncovers a previously inconspicuous size effect on correlation of the modulus and the square-root-freezing-time. Further quantitative study of the edge effect on the freezing of a polygonal cross-section reveals an important geometric parameter, termed the "shape index," which is defined as $\text{volume}/(\text{surface area})^2$ and used in correlating the edge-induced freezing time to the edge-free freezing time.

It is also found that several thermophysical properties of the cast metal, in particular the thermal conductivity, affect the solidification process significantly. Four cast metals (Al, Sn, Pb and 0.25% carbon steel) have been selected to represent different groups of properties, in a series of numerical analyses designed to study the property influence. The properties of silica sand molds are used to simulate the chilling process. The results reveal how the "shape index" affects the proportionality of the modulus and the square-root-freezing-time and further how cast metal properties can bias the influence.

INTRODUCTION

One of the most significant applications of the knowledge of the heat transport in sand casting systems to the rationalization of the founding art has been the dimensioning of risers. A great number of researchers have focused on the subject of risering techniques. The original endeavor in rationalizing the art of risering castings was due to Farquhar and Chvorinov. Farquhar¹ empirically set forth criteria for risering castings. Chvorinov² procured the risering principles

via the mathematical deduction of Schwarz's analysis³ and set up a rule that relates the freezing time of a slab casting to its volume to surface area ratio, which is commonly known as the "modulus."

Modified forms of Chvorinov's one-dimensional rule that account for simple surface curvature effects have been established.^{4,5} It is notable that the Chvorinov's rule has since been the rationale of all risering principles, even with non-slab shaped castings. The first risering equation taking into consideration the amount of contraction on solidification was due to Caine.⁶ Caine's empirical equation was based on Chvorinov's concept of relative freezing time of riser and casting. Adams and Taylor⁷ subsequently derived an analytical equation for risering simply-shaped castings and compared the results with that given by Caine's equation. Good agreement between the two formulae for steel castings poured in sand molds was obtained. The concept of using a correction factor in the basic risering equation to account for the effect of thermal additives applied to the riser and casting such as insulators, chills and riser toppings was also discussed by Adams and Taylor.⁷

In their extensive studies of solidification rates and risering criteria, Pellini and his co-workers⁸⁻¹¹ have established several practical rules for producing sound castings. The role played by temperature gradient within the casting in determining the feeding capacity was examined.⁸ To generalize the often empirical estimation of the shape effect on feeding Bishop, Myskowski and Pellini¹¹ introduced a shape factor, $(\text{casting length} + \text{casting width})/\text{casting thickness}$, to represent the ranginess of shapes for estimating the required size of cylindrical risers. Riser of sections with appendages was also studied.¹¹ Merchant¹² reorganized the data obtained by Pellini and company⁸⁻¹¹ and proposed a general equation for risers which is similar to Caine's equation.⁶ Parallel with the advancement of knowledge of risering, the technique of applying exothermic additives to risers in order to improve both yield and soundness had been advanced.¹³ The part played by mold dilatation effects on risering requirements has also been noticed.^{14,15}

The use of moduli in calculating the size, shape and location of risers provides an economical approach to the design of optimum risers. A systematic procedure for the modulus method has been provided by Wlodawer.¹⁶ Heine and his co-workers^{17,18} have independently promoted the concept of feeding path for predicting the requirement of risers, as a modification of the modulus method. The modulus approach has thus long been adopted as a practical approach to risering design by modern foundrymen,^{19,20} who have also recognized the economical aspect of using insulating materials for feeder heads in commercial foundry production. As a whole, the empirical science of risering is well established, particularly so for steel sand castings. However, the only available literature on the quantification of the role played by the corner effect in determining the chilling power of sand molds seems to be the works by Ruddle and Skinner,²¹ and by Brandt, Bishop and Pellini.²² This notable lack of information is due to the unavailability of a general solution for the heat diffusion within non-rectangular corners.

A generalized analysis of the transient edge effect on

heat conduction in wedges has recently been undertaken.²³ The analysis reveals the fact that, for a wedge subjected to constant surface temperature, the *difference* between its loss or absorption of heat per unit time and depth, and that heat loss or absorption rate occurring over a corresponding *planar* surface does not vary with time. Based on the theoretical finding, this paper is intended to analyze the extent of the corner effect on the freezing time of shaped bar castings with polygonal cross-sections, for which the end effect on the freezing of the mid cross-section is insignificant. The analysis leads to a Chvorinov-type formula. This shorthand formula uncovers an otherwise inconspicuous size effect on correlation of the modulus and the square-root-freezing-time. Further quantitative study of the edge effect on the freezing of a polygonal cross-section reveals an important geometric parameter, which has been termed the "shape index." This is defined as volume/ (surface area)² and is involved in correlating the edge induced freezing time to the edge-free freezing time. It is also found that several thermophysical properties of a cast metal, in particular the heat conductivity, interact with the shape effect and contribute significantly to its solidification time. Four casting materials (Al, Sn, Pb and a carbon steel) are selected to represent different groups of properties, in a series of numerical analyses designed to study the property influence. The properties of silica sand molds are used in these computations to simulate the chilling process. The results elucidate how the "shape index" affects the proportionality of the modulus and the square-root-freezing-time and how cast metal properties can bias the influence. This new approach should prove to be of considerable utility in basic riser design problems.

SOLIDIFICATION TIMES OF SIMPLY SHAPED CASTINGS

In an ideal solidification process where a semi-infinite mold and a semi-infinite molten metal are brought into perfect contact, Schwarz³ has shown, having assumed constant properties for mold, solidified metal and liquid metal, that the mold-metal interface temperature is constant at all times during solidification. Chvorinov² further demonstrated the practicality of Schwarz's analytical solution in steel sand castings of finite thickness. An averaged temperature of the liquidus and the solidus was used as the freezing temperature required in Schwarz's formula for the mold-metal interface temperature. The balancing of loss and gain of heat in the casting and the mold has led to the well-known Chvorinov's rule, which states that the modulus of a casting, defined as volume/surface area, is linearly proportional to the square root of its solidification time for a specified set of mold and casting conditions. A straightforward application of the rule to the design of risers is to determine the minimum riser size required so that it (the riser) does not solidify before the casting which is to be fed. As a first approximation, the Chvorinov's rule for a large slab sand casting can be put into the following simple form:^{5,24}

$$\frac{V}{A} = \frac{2}{\sqrt{\pi}} \left[\frac{T_i - T_\infty}{\rho_c L} \right] \sqrt{K_m \rho_m C_m} \sqrt{t_f} \quad (1)$$

The symbols used are listed in the table of nomenclature. The assumptions implied in the above formula are: (1) all

thermophysical properties are independent of temperature; (2) the thermal conductivity of the cast material is infinitely large so that the mold-metal interface temperature is constant, either at the melting temperature of pure metals or an averaged temperature of the solidus and the liquidus for alloys,² throughout the solidification period; (3) the convection flow effects due to pouring and density differences are neglected and the superheat may be incorporated in the latent heat term; and (4) the sand mold is sufficiently large so that no temperature variation occurs at the outer surface of the mold. Corresponding formulae for cylinder or sphere castings poured in large sand molds have the general form,^{5,24}

$$\frac{V}{A} = \frac{T_i - T_\infty}{\rho_c L} \left[\frac{2}{\sqrt{\pi}} \sqrt{K_m \rho_m C_m} \sqrt{t_f} + \frac{n K_m t_f}{2 r} \right] \quad (2)$$

where r is the casting radius and $n = 1$ for cylinder, and 2 for sphere. It ought to be noted that, for cylinders, the above formula is valid only for small time intervals.^{5,25}

TWO-DIMENSIONAL CORNER EFFECT ON SOLIDIFICATION TIMES

A recent theoretical study of the transient edge effect on heat conduction in wedges²³ has uncovered a significant edge effect on the amount of heat transfer across the surface of a wedge which was instantaneously brought into contact with an isothermal heat source. The nature of sand casting systems in maintaining the mold-metal interface temperature near the solidification temperature whilst the casting solidifies allows one to separate the heat transfer phenomenon occurring within a two-dimensional dry sand mold and casting system into two isolated ideal heat transfer sub-systems. With the same assumptions made earlier for deriving the Chvorinov's rule, the casting is to dissipate its latent heat into an isothermal heat sink, at the casting boundary, at the solidification temperature. The heat absorbed by the sand mold can be studied, separately, by consideration of the mold at its initial temperature being suddenly brought into contact with an isothermal heat source at the solidification temperature. By equating the heat loss and the heat gain in these two isolated sub-systems, a two-dimensional Chvorinov-type rule can be formulated.

Figure 1 shows two fictitious, unbounded wedge-shaped molds. The "unboundness" idealization is easily achieved in practice. Ruddle and Skinner,²¹ in their study of the heat extraction at rectangular corners in sand molds filled with

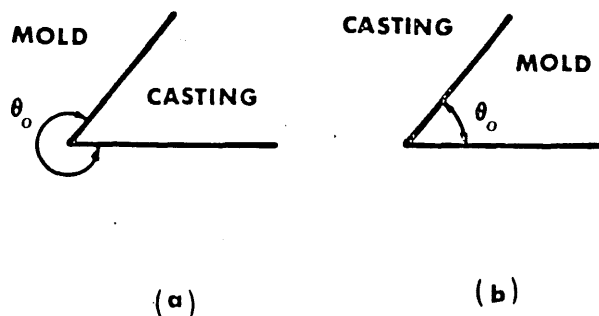


Fig. 1. (a) Semi-infinite mold intrusion; (b) semi-infinite mold extrusion.

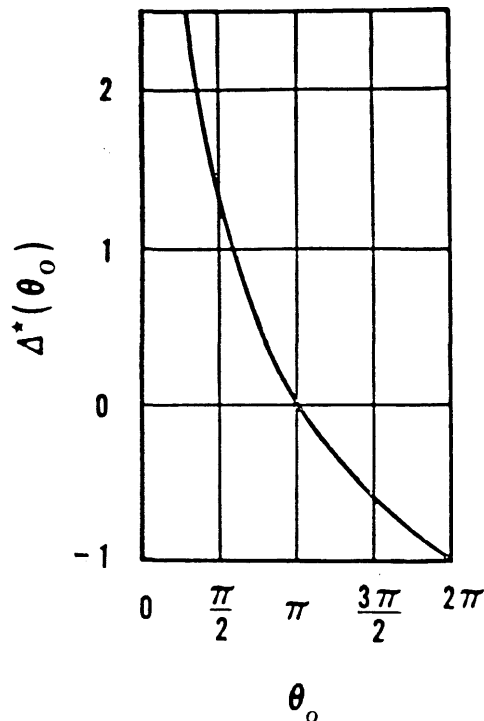


Fig. 2. Plot of $\Delta^*(\theta_0)$ vs. θ_0 ; after Hurwitz and Roe.²⁷

molten aluminum-30% copper alloy, observed that the edge influence on the isotherms measured diffused only 3-5 cm away from the edge in 15 minutes after pouring. A more general, theoretical analysis of the localization of the edge effect has also been realized.²⁶ For a wedge-shaped mold subjected to an isothermal heat source, it has been shown²³ that the *difference* between its absorption of heat per unit time and depth, and that heat absorption rate occurring over a corresponding *planar* mold, does not vary with time. This *difference* in heat absorption rate is a function of the wedge angle (θ_0) only and equals to $-K_m(T_i - T_\infty)\Delta^*(\theta_0)$, where²³

$$\Delta^*(\theta_0) = 2 \int_{-\infty}^{\infty} \coth \eta \left[\frac{1}{\pi \sinh \eta} - \frac{1}{\theta_0 \sinh \frac{\pi \eta}{\theta_0}} \right] d\eta \quad (3)$$

The above has been termed the *wedge invariant* and can be plotted²⁷ against θ_0 , as shown in Figure 2. The numerical values of $\Delta^*(\theta_0)$ are dimensionless.

The fact that the edge influence on heat transport is localized to the vicinity of the edge permits one to extend the result for a single wedge to the analysis of finite geometries, such as a long bar casting with a polygonal cross-section for which the end effect on the freezing time is negligible. Figure 3 shows such a cross-section, surrounded by a sufficiently large sand mold. Provided that no interactions occur among the neighboring edges during the solidification period, the total *difference* in heat absorption rate within the mold, as compared with that in a *planar* mold, is also invariant with respect to time and is equal to the algebraic sum of the contributing wedge invariants, multiplied by the thermal conductivity of the mold and the temperature difference. Symbolically,

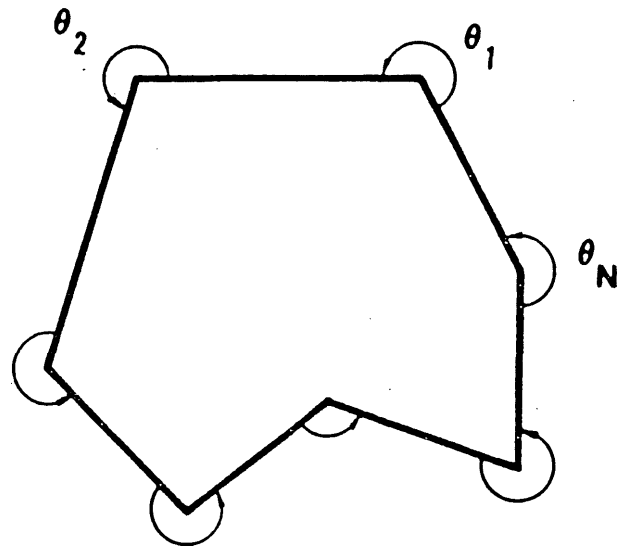


Fig. 3. Cross-section of a bar cast in a sufficiently large sand mold.

$$Q_{2d}(t) - Q_{1d}(t) = K_m (T_i - T_\infty) \left[-\sum_{n=1}^N \Delta^*(\theta_n) \right] \quad (4)$$

where Q_{2d} represents the total heat flow per unit time and depth, across the mold-metal interface, and Q_{1d} the reference heat flow rate across a corresponding planar mold of the same interface area.

Given the reference, one-dimensional heat flow rate,²⁵ one can integrate equation (4) with respect to time and obtain the total heat transported across the interface as a function of time. At the final freezing time t_f , this accumulated heat would be equivalent to the latent heat of the casting ($\rho_c V L$), and therefore leads to a modified Chvorinov's rule, namely:

$$\frac{V}{A} = \frac{T_i - T_\infty}{\rho_c L} \left\{ \frac{2}{\sqrt{\pi}} \sqrt{K_m \rho_m C_m} \sqrt{t_f} + \frac{K_m}{A} \left[-\sum_{n=1}^N \Delta^*(\theta_n) \right] t_f \right\} \quad (5)$$

Depending on the net geometric effect on diverging or converging the heat flow into the mold, the above equation states that the freezing time of the casting may be less or greater than that of a large slab having the same modulus.

CORNER AND SHAPE INDICES

The linear relationship existing between the modulus and the square-root-freezing-time, as exhibited in equation (1), does not hold for the two-dimensional formula (5) because the coefficient of the freezing time (t_f) is size-dependent. The implicit interplay of the corner and the size effects in formula (5) can be disentangled by introducing a heat transport parameter and a shape parameter. On the right hand side of (5), the first term corresponds to the heat that would have been transported had the corners been replaced by planar mold walls. It then follows that, on the right hand side of (5), the ratio of the second term to the first term would represent the extent of the corner induced diverging or converging heat flow. This ratio is called the corner index, denoted by ξ , and

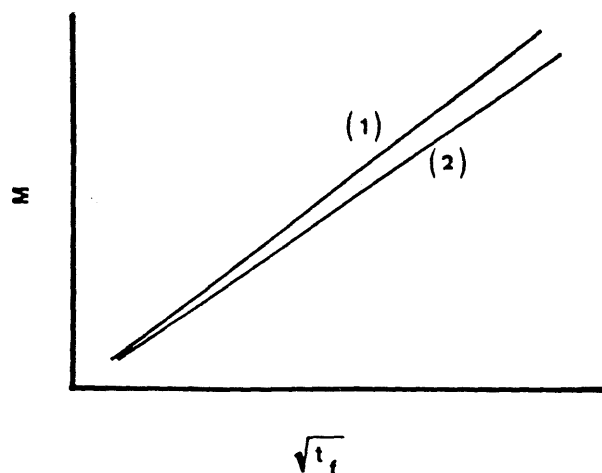


Fig. 4. A graphical representation of V/A vs. $\sqrt{t_f}$ for (1) a square bar, and (2) a large plate cast in sufficiently large molds.

$$\xi = \frac{\sqrt{\pi K_m / \rho_m C_m}}{2A} \left[\sum_{n=1}^N \Delta^*(\theta_n) \right] \sqrt{t_f} \quad (6)$$

The above expression is useful in determining the error incurred when one calculates the modulus without considering the corner effect. Another point of significance of the corner index is that it manifests the system variables comprising the corner effect.

Solving for the square-root-freezing-time in the quadratic equation (5) and substituting the result into equation (6) yields

$$\xi = -\frac{1}{2} + \frac{1}{2} \left\{ 1 + \frac{\pi \rho_c L}{\rho_m C_m (T_i - T_\infty)} \left[\sum_{n=1}^N \Delta^*(\theta_n) \right] \frac{V}{A^2} \right\}^{1/2} \quad (7)$$

The above expression indicates that the corner index ξ depends, geometrically, only on the product of the sum of the wedge invariants and a shape parameter, V/A^2 , which has been named the shape index.²⁶ It then follows that the corner index for linearly similar polygonal cross-sections is an invariant.

The concept of the corner index is helpful in quantifying the error incurred when one calculates the modulus without consideration of geometric effects. By denoting the solidification time resulting from the Chvorinov rule in equation (1) as t_f and that resulting from equation (5) as t_f' , one can derive a relation among t_f' , t_f and ξ by equating (1) and (5) which yields

$$\sqrt{\frac{t_f}{t_f'}} = 1 + \xi \equiv \frac{M}{M'} \quad (8)$$

where M/M' is used to represent the corner effect on the modulus of the casting. It is convenient to regard M as the virtual modulus obtained taking no account of the corner effect and M' as the effective modulus which is no longer just a volume to apparent surface area ratio. The effective modulus is a useful indicator for estimating the freezing time of a bar casting, on a V/A vs. $\sqrt{t_f}$ graph, from the straight line generated by equation (1).

Table 1. Latent Heat of Selected Cast Materials

Metal or Alloy	Latent Heat (J/g)	Density (g/cm ³)	Volume Latent Heat (J/cm ³)
Al	401.8	2.4	956
Sn	60.7	6.8	414
Pb	24.7	10.5	259
0.25%-C Steel	272.1	7.1	1940

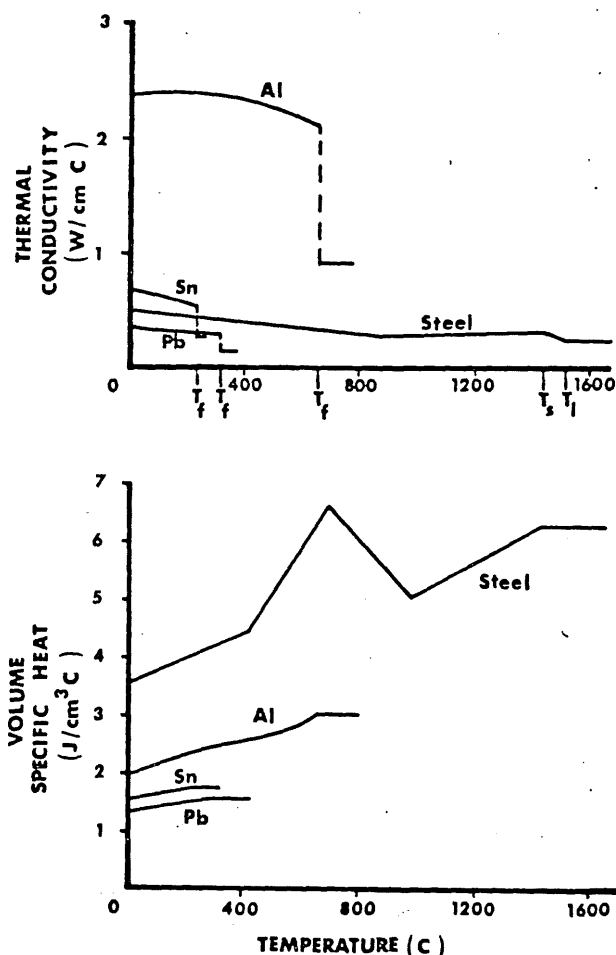


Fig. 5. Thermal conductivity and volume specific heat, as functions of temperature, for selected cast materials (T_f = freezing temperature, T_l = liquidus, T_s = solidus). After Refs. 19 and 30.

The above analysis states that the linear relationship between the modulus and the square-root-freezing-time still holds among a set of linearly similar polygonal cross-sections. Given a specified shape index, equation (5) traces a straight line on a V/A vs. $\sqrt{t_f}$ plane which is equivalent to a rotation of the line generated by equation (1) around the origin. Figure 4 illustrates a graphical representation of V/A vs. $\sqrt{t_f}$ for a square-sectioned bar and a large plate cast in sufficiently large molds.

ANALYTICAL STUDY OF THE PROPERTY INFLUENCE

The above theoretical analysis provides a solid basis for the

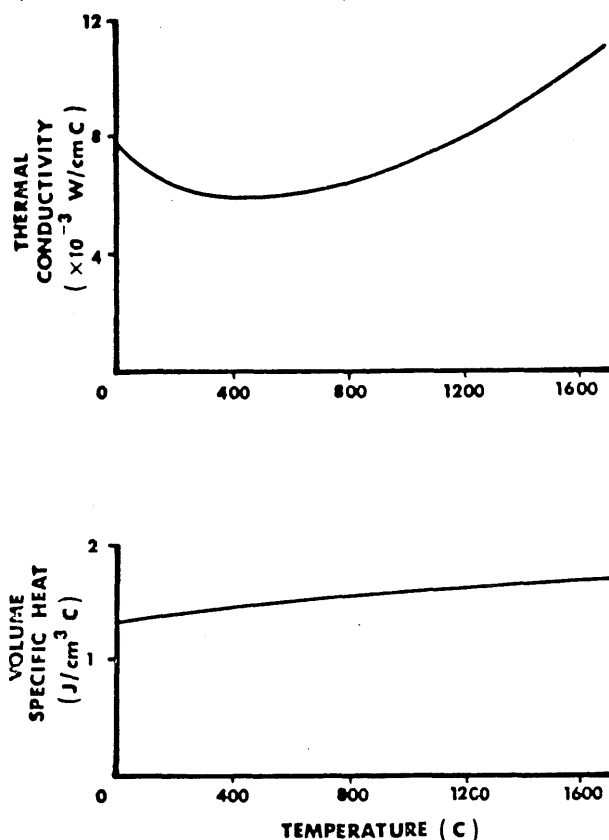


Fig. 6. Thermal conductivity and volume specific heat, as functions of temperature, for dried silica sand molds,³¹ density = 1.52 g/cm³.

quantification of the two-dimensional corner effect on freezing times. In practice, however, one particular assumption made earlier when deriving the shorthand rule needs to be refined. It was assumed that the thermal conductivity of the cast material was so great that little temperature gradient occurs across the casting during the solidification process. It follows intuitively that a poor heat conductivity would have a two fold effect on the freezing time: a larger temperature gradient which requires the mold to absorb a certain amount of sensible heat in the solidified layer and a lower mold-metal interface temperature which diminishes the chilling power of the mold. Other influential properties of the cast material on the solidification process may be the amount of volume latent heat and the solidification temperature.²⁸ Unfortunately, a closed form solution for the solidification process in the form of a shaped mold and casting system is far from one's reach. An analytical study of the influence exerted by material properties upon the corner effect has to rely on discretized, numerical approaches.

Four cast materials have been selected to represent different groups of properties in a series of numerical experiments. Table 1 and Figure 5 list the properties^{29,30} that have been used in the analysis. A set of recently acquired property data of dried silica sand molds,³¹ as shown in Figure 6, has been used to simulate the chilling process. A reliable explicit finite difference enthalpy model,³² has been used to simulate the heat transport within the mold and casting systems. The advantages of the enthalpy ap-

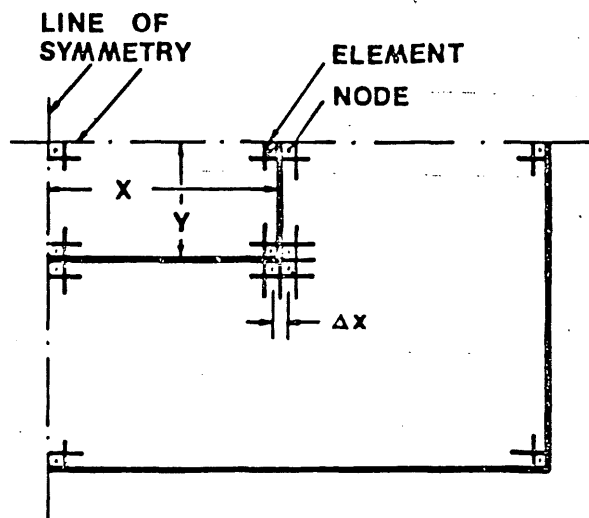


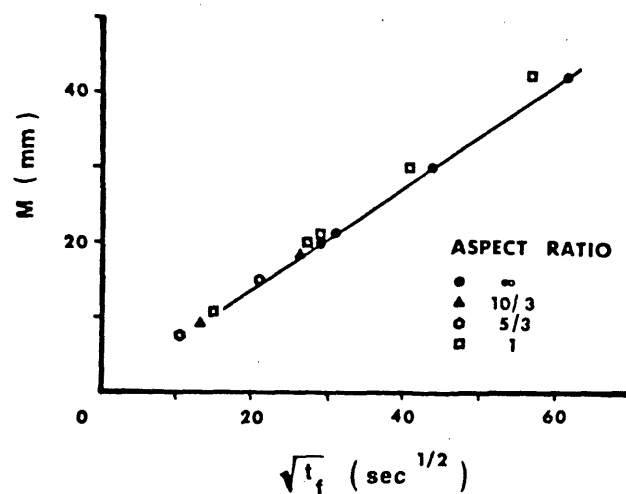
Fig. 7. Discretized mold and casting cross-section (aspect ratio = X/Y).

proach can be viewed in two major respects: the convenience in incorporating in the computation the temperature dependence of material properties, and the reliability in handling the loss of latent heat. The computations have been performed on rectangular cross-sections. Figure 7 illustrates the discretization of a rectangular cross-section surrounded by a sufficiently large mold. Equally spaced square elements and nodes are used to represent the continuous space. The temperature-time curves for all nodal points can be obtained by solving the discretized heat equations in the castings and the mold regions. The continuity of temperature and heat flux at the mold-metal interface are enforced upon the interface nodes at every discretized time step. The resulting freezing time is taken as the time step at which the temperature of the node nearest to the center first drops below the melting point or the solidus. Typical nodal distance Δx ranges from 2 to 10 mm. The solidus temperature of the low carbon steel was estimated, via Scheil's equation,³² as the temperature corresponding to a solid fraction of 90%. Zero superheat was used in these computations to avoid the complication caused by convection flows. The initial mold temperature was taken as the room temperature.

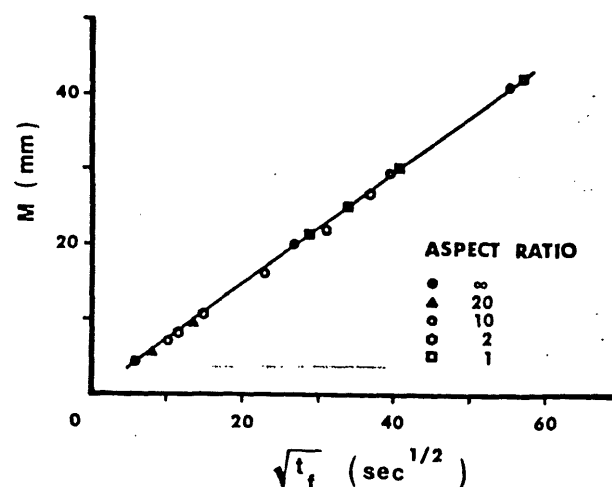
Figure 8 shows the analytical results. For rectangular cross-sections, the shape index varies only with the aspect ratio. In Figure 8, the aspect ratio is plotted as a parameter, and it will be noted that an infinite aspect ratio corresponds to infinitely large plates. Reference lines are also drawn through the points corresponding to these idealized systems.

DISCUSSION

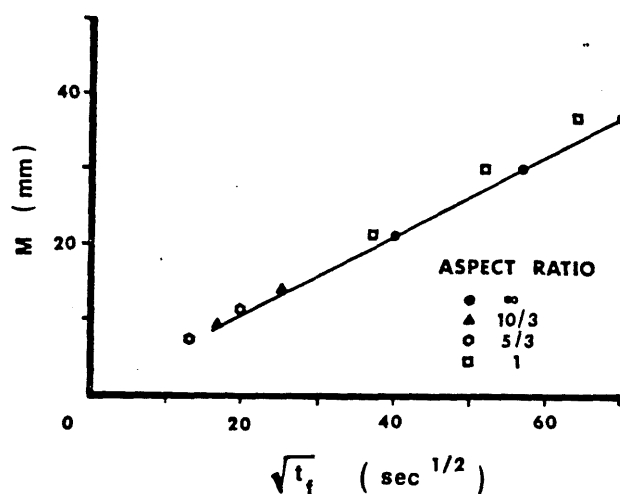
The validity of the theoretical assertions made herein on the geometric influence of corners upon casting solidification can be examined via the numerical experiments, which take into account the temperature dependence of properties of cast and mold materials. The roles played by thermal conductivity, solidification temperature and volume latent heat of a cast metal in pacing the solidification process can also be identified. Although differences in chilling power exist among various molding sands,^{2,5,31} the interplay of



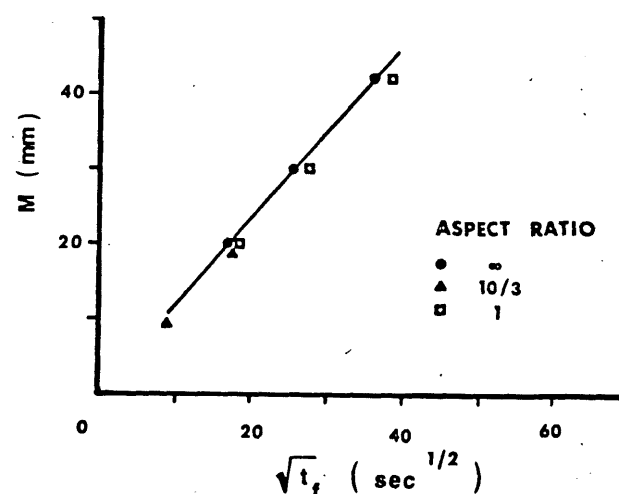
(a)



(c)



(b)



(d)

Fig. 8. Computed freezing times for various aspect ratios for (a) aluminum, (b) tin, (c) 0.25% carbon steel, and (d) lead. (Note and aspect ratio of ∞ represents an infinite plate, of one a square-sectioned bar.)

the property and geometric effects as revealed in Fig. 8 by adopting only dried silica sand molds will not be substantially affected.

The analytical results presented in Fig. 8 prompt the statement that the thermal conductivity of a cast metal is the most influential property in biasing the idealized edge effect with respect to the freezing time, as described by formula (5). On comparison of Fig. 8 with Fig. 4, several other factors emerge. For example, it appears that highly heat-conductive materials such as aluminum and tin exhibit the same corner effect as seen in an idealized cast material having an infinitely large thermal conductivity. The similarity in appearance of the numerically computed and analytical sets of results demonstrates the practicality of the theoretical approach.

Increasing the solidification temperature of a casting would normally enhance the chilling power of the sand

mold and, in effect, shorten the freezing time, while increasing the latent heat would do the reverse. However, as revealed in equation (6), a decrease in freezing time would result in a smaller corner index and therefore a less significant corner effect. Figures 8(a) and (b) reveal that the solidification temperature has been the dominant factor in determining these freezing times. It should be noted that all observations are based on the assumption of a perfect contact at the mold-metal interface. Generally speaking, the relatively low thermal conductivity of sand molds tends to veil the role played by the interface air gap due to density changes during the chilling process. In practice, however, the contribution of air gap in diminishing the heat flow may become significant in systems with low solidification temperature because of the fourth power relationship in the thermal radiation heat transfer across the air gap.

The geometric effect as illustrated in Figs. 8(a) and (b) is significantly biased in systems of poor heat conductive

metals, such as low carbon steel and lead. These metals exhibit large temperature gradients across the hot and cold regions in the casting, particularly at the corners, which would lower the mold-metal interface temperature and require the mold to absorb a certain quantity of sensible heat in the solidified layer during the solidification process. These effects jointly reduce the chilling power of the mold and prolong the freezing process. The counteraction of the enhanced heat transport in the mold due to its geometry with the impaired chilling power ascribed to the poor heat conductive metal determines the final corner effect on the freezing time. Both low carbon steel and lead systems exhibit a biased corner effect. The property effect overcomes the geometric contribution and results in an apparently reversed corner effect. In Fig. 8(c), the balance of these two contributing factors has produced a narrow distribution that appears to conceal the corner effect. This observation provides an analytical reasoning for the success in applying the modulus technique to feeding shaped steel castings without consideration of corner effects.¹⁶

Despite the influence exerted by properties of cast metals upon the corner effect on freezing times, the linearity with respect to the shape index, a remark that followed equation (8), holds for all cases. It is worth noting that this linearity also applies to rangy cross-sections for which the presumption of independent edge influences may be oversimplifying. In Fig. 8, all points representing a specific aspect ratio and therefore a specific shape index lie approximately on a straight line. The counteraction of the casting geometry with the contributing properties of the casting in determining the freezing time of a square-sectioned bar, in terms of the analytical equation (7) and the computed results, is summarized in Fig. 9.

CONCLUSIONS

This paper has presented a new, theoretical approach to understanding the corner effect on the freezing time of bar castings having polygonal cross-sections and poured in sufficiently large sand molds. It has also been demonstrated that several thermophysical properties of cast materials, in particular the thermal conductivity, play important parts in composing the corner effect. A rule of thumb that can be drawn from the analysis is that the linearity of the virtual modulus (V/A) with the square-root-freezing-time holds for all cross-sections having the same shape index, i. e., for linearly similar cross-sections. The interplay of the thermal conductivity of a cast metal with the casting geometry in producing reversed corner effects is examined. The quantification of how the shape index affects the solidification system is found to be *material dependent*. The new approach should prove to be fundamental for further studies of corner effects on casting solidification.

ACKNOWLEDGMENTS

The authors would like to acknowledge the financial support provided by the National Science Foundation under the Grant No. DAR 78-24301 (Program Manager, Dr. W. M. Spurgeon), and the valuable discussions shared with Dr. P. N. Hansen at the Technical University of Denmark.

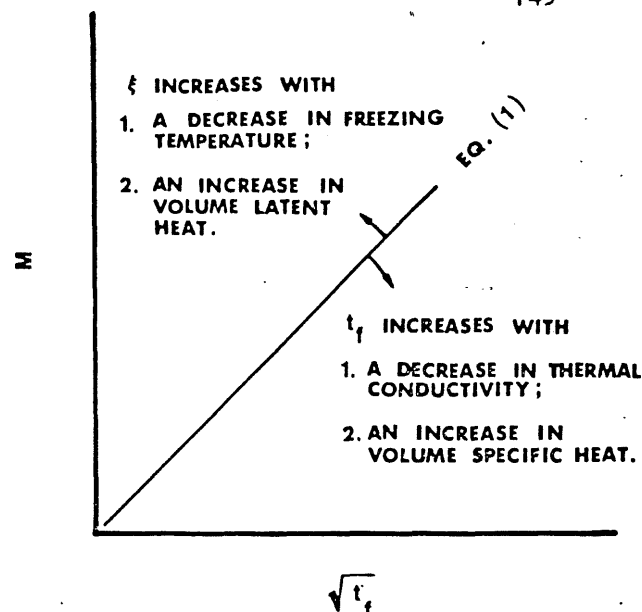


Fig. 9. Counteraction of the casting geometry with the contributing properties of the casting in determining the freezing time of a square-sectioned bar.

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NOMENCLATURE

A	Surface area;
C_m	Specific heat of the mold;
K_m	Thermal conductivity of the mold;
L	Latent heat per unit mass;
M	Modulus (V/A);
M'	Effective modulus;

$Q_{1d}(t)$	Heat flow per unit time and depth, over a planar mold;
$Q_{2d}(t)$	Heat flow per unit time and depth, over a wedge-shaped mold;
t	Time;
t_f	Freezing time;
T_i	Interface temperature;
T_∞	Initial mold temperature;
V	Volume;
$\Delta^*(\theta_0)$	Wedge invariant;
θ_0	Wedge angle;
ξ	Corner index;
ρ_c	Density of the cast metal;
ρ_m	Density of the mold

EQUATIONS

$$\frac{V}{A} = \frac{2}{\sqrt{\pi}} \left(\frac{T_i - T_\infty}{\rho_c L} \right) \sqrt{K_m \rho_m C_m} \sqrt{t_f} \quad (1)$$

$$\frac{V}{A} = \frac{T_i - T_\infty}{\rho_c L} \left(\frac{2}{\sqrt{\pi}} \sqrt{K_m \rho_m C_m} \sqrt{t_f} + \frac{n K_m t_f}{2 r} \right) \quad (2)$$

$$\Delta^*(\theta_0) = 2 \int_{-\infty}^{\infty} \coth \eta \left[\frac{1}{\pi \sinh \eta} - \frac{1}{\theta_0 \sinh \frac{\pi \eta}{\theta_0}} \right] d\eta \quad (3)$$

$$Q_{2d}(t) - Q_{1d}(t) = K_m (T_i - T_\infty) \left[-\sum_{n=1}^N \Delta^*(\theta_n) \right] \quad (4)$$

$$\frac{V}{A} = \frac{T_i - T_\infty}{\rho_c L} \left\{ \frac{2}{\sqrt{\pi}} \sqrt{K_m \rho_m C_m} \sqrt{t_f} + \frac{K_m}{A} \left[-\sum_{n=1}^N \Delta^*(\theta_n) \right] t_f \right\} \quad (5)$$

$$\xi = \frac{\sqrt{\pi K_m / \rho_m C_m}}{2A} \left[-\sum_{n=1}^N \Delta^*(\theta_n) \right] \sqrt{t_f} \quad (6)$$

$$\xi = -\frac{1}{2} + \frac{1}{2} \left\{ 1 + \frac{\pi \rho_c L}{\rho_m C_m (T_i - T_\infty)} \left[-\sum_{n=1}^N \Delta^*(\theta_n) \right] \frac{V}{A^2} \right\}^{1/2} \quad (7)$$

$$\sqrt{\frac{t_f}{t_f'}} = 1 + \xi \equiv \frac{M}{M'} \quad (8)$$

Comparing the Functioning of Risers to Their Behavior Predicted by Computer Programs

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The design and functioning of risers are compared with the results from two different types of computer programs. The intention of this paper is to focus on how the similarities and differences in the risers designed by the programs appear in the results obtained with risered castings made with Al-Si eutectic and A356 Al alloys, ductile iron, and gray iron.

The computer programs are based on extended versions of the geometric technique for risering which has been described earlier,^{1,2} and which is extensively used in foundries. Embodying the technique in computer programs was presented in references 3 and 4. The microcomputer program, called RISER II (available through AFS/CMI) is based on gross casting section modulus and local section modulus values that identify the freezing order and feeding paths that must be risered in the casting. The mainframe computer program, called the *wavefront solidification program*, is based on

the section modulus of solidifying fronts as they progress directionally toward heat centers in the casting. The equations for developing the wave fronts and the programming principles are described in references 3-7. The wave front section modulus values are used to locate feeding paths and distances that require risering at various locations in the castings. In both programs, the maximum diameter of a riser is defined by $D_R = 4 SM + D_p$, where D_p is the diameter of the pipe feeding cavity and SM is the section modulus of the casting at the location of the riser contact. The riser design dimensions are shown in the Appendix.

The similarities, differences, and practical results of these programs are best presented by examples using specific castings. Two different plate castings will be used for this purpose as shown in Figure 1. The drag pattern half is shown in Figure 2. The thick/thin plate combination of Figure 1 is used to obtain a high percentage of low section modulus-fast freezing casting portion; in this case 33% of the total volume. To proceed in the wave front program the shape of the cross-section is input via a digitizing tablet and its properties determined (Fig. 3). Casting and VSA values are needed for the riser design. The two plates have been combined as one for convenience (Fig. 3).

The solidified wave fronts in the combined plate are also shown in Figure 3. Any number of section modulus waves may be selected. Riser design may now continue.

RISERING BY THE WAVE FRONT PROGRAM

The risering system based on the wave front program considers the total section modulus range of the casting, VSA, and divides it into equal fractions according to the number of VSA fractions chosen by the program operator. The principle is illustrated in

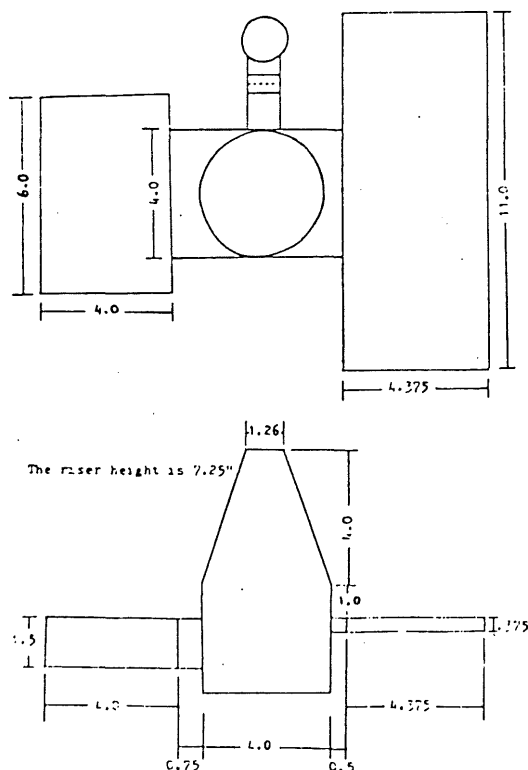


Fig 1. Drawing of 1.5-in. and 0.375-in. plates attached to center riser.

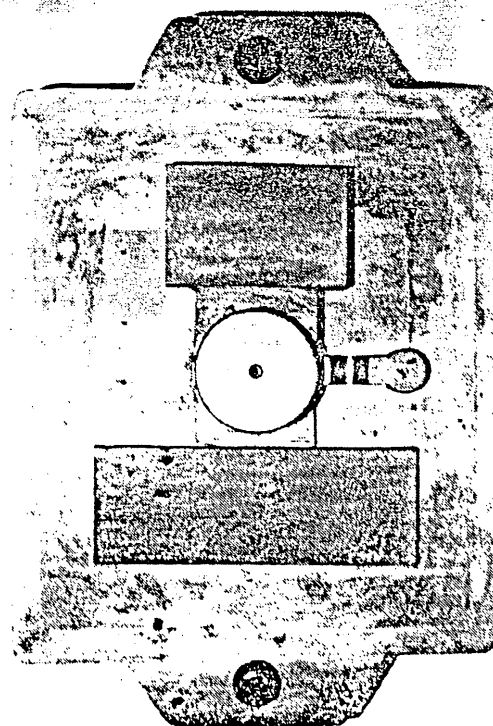


Fig. 2. Photograph of drag half of pattern of Figure 1.

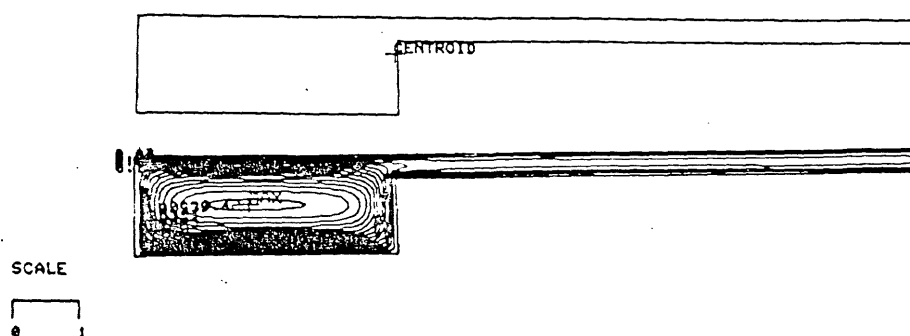
MAIN MENU LEVEL

PHYSICAL PROPERTIES:

X-SECT AREA = 8.9491 SQ. IN.
 VOLUME = 53.695 CU. IN.
 SURFACE AREA = 179.78 SQ. IN.
 WEIGHT = 4.8325 LB.
 GROSS U/SA = 0.29867 IN.
 X-CENTROID = -1.2073 IN.
 Y-CENTROID = -2.7678 IN.

SOLID OF EXTRUSION:

LENGTH OF EXTRUSION = 6.0000 IN.



STATUS: CASTING NOT MOLD RAW NO SUPERHEAT

Fig. 3. Properties of volume, surface area, weight, gross section modulus, and centroids printed from graphics screen (upper). Wavefronts are shown in the lower figure reproduced from a separate graphics screen.

Table 1. Volume Percentage Feed Metal

Volume Percentage Feed Metal					
Material-Section	Mold	% Feed Metal = vol. feed metal vol. casting plus risers	Casting Min. Soundness Riser Top D, in.	** Reference	
1. Carbon steel- 1.0 inch 2.0 inches	green sand	3.7 5.4	S S	2.0 2.0	1
2. 316 stainless 1.0 inch	green sand	3.8	U	2.0	1
3. ductile iron 1.0 to 1.5 in.	green sand	2.65	S	1.0	
4. A356 aluminum 1.0 inch	no-bake	2.14	U	1.5	1
1. A356 Aluminum 0.375 to 1.5 in.	green sand	2.9	S	1.75	2
2. 12% Si-Aluminum 0.375 to 1.5 in.	green sand	3.9% 1350°F pour 4.65% 1500°F pour	S	1.75	2
3. Ductile iron 0.375 to 1.5 in.	green sand	4.75%	S	1.25	2
4. Gray iron 0.375 to 1.5 in.	green sand	1.7-1.9%	S		2

*S = sound; U = unsound; ** tapered risers.

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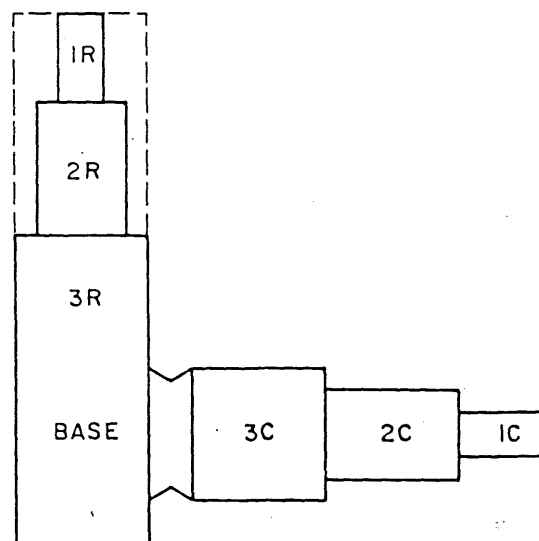
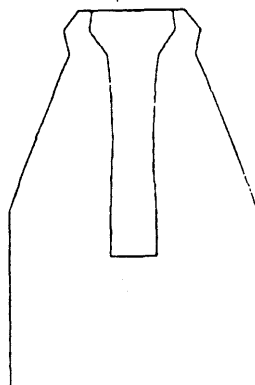


Fig. 4. Step-block casting fed by a step block side riser, or alternately a cylinder.

CURVTR
FEED
GRADNT
GRAPH
NEU
PROPTY
REVISE
RISER
USA
CLEAR
EXIT
HELP
INPUT
RECALL
ROTATE
S-HEAT
STORE
TRANS
ZOOM

SORTING VSA VALUES
 TOP OR SIDE RISER?
 S,T -->S
 PRESSURE SECTION?
 IN. -->0.5
 ASPECT RATIO?
 -->1.5
 FEED METAL %?
 % -->3.5
 # OF SECTIONS?
 # -->8
 CURRENT USAMAX = 0.33169 IN.
 CHANGE CURRENT USAMAX?
 Y,N -->Y
 NEW VALUE?
 -->0.75

RISERING



SCALE



ASPECT
DRAW
PRESSR
RESULT
SECTNS
SHRINK
TYPE
USAMAX
CLEAR
EXIT
HELP
INPUT
RECALL
ROTATE
S-HEAT
STORE
TRANS
ZOOM

Fig. 5. Risering menu level, information which must be provided and riser drawing for the castings of Figures 1-3.

Figure 4 with the example of a step-block casting. The smallest section modulus part of the casting, 1C, is fed by the smallest riser, 1R. The intermediate casting section modulus, 2C, is fed by the intermediate riser size, 2R; and so forth. Since this is a side riser, a base, B, must be added. With this geometry the yield will be less than 50% unless each casting step is lengthened to its maximum feeding distance. If the riser had no steps, it would be cylindrical rider of the maximum diameter shown. Each step is a disk that matches the corresponding VSA values in the casting.

The computer program provides a choice of input of the number of sections or disks that are stacked to form the riser. It also permits choice of the ratio of disk diameter to height, which is called the *aspect ratio*. The program also calls for a choice of top or side riser, the addition of a pressure section to the riser to provide pressure head, the percentage feed metal requirement, and a choice of the section modulus at the riser location or the average VSA computed by the program (Fig. 3). Typical feed metal requirements determined from the research are listed in Table 1.

The riser menu level is called up and the data entered as listed in Figure 5. The conditions chosen were as follows:

- 1) Top or Side Riser: S, T - S
- 2) Pressure Section Inches: 0.50
- 3) Aspect Ratio: 1.5
- 4) Feed Metal %: 3.5
- 5) Number of Sections: 8
- 6) Current VSA Maximum
- 7) Change Current VSA Max: Y, N - Y
- 8) New Value: 0.75

The conditions entered above and a drawing of the riser can then be displayed as in Figure 5, and the dimensional results as in Figure 6. Note the funnel shape at the top of the riser (Fig. 5) and its dimensions (Fig. 6). The top shape reflects the high early feed metal demand of the low VSA thin plate added to that of the heavy plate. Correlation of the riser design with casting behavior will be discussed later.

If a cylindrical riser is desired, an entry of one disk section instead of the eight disk sections entered for Figure 5 will then produce the output shown in Figure 7. However, a 6% reduction in yield results when the change from a tapered to a cylindrical riser is made in this case. An increase of about 4-11% yield is typical of the change from cylindrical to tapered risers.⁹

Using the dimensions of the riser from the program (Figs. 5 and 6) a drawing of the casting and tapered riser assembly as in Figure 1 may be entered into the program to determine that solidification is occurring directionally from the castings into the riser. When the riser is attached to the casting in the drawing, the riser diameter is halved. This is because in the 2-D representation of a riser $T/2 = D/4$ or $T = 0.5D$. The 2-D drawing sees all sections as plates and none as cylinders. Figure 8 shows the solidification wave fronts when no superheat is involved and Figure 9 shows them for 1.5% superheat. The latter is a program feature described in reference 4. In addition a surface coefficient of 0.02 was introduced at the top surface of the riser to simulate the air gap formed when the metal draws away because of piping action from feeding the casting. Figures 8 and 9 show good directional solidification with the final location in the riser outside the casting (mark +).

RISERING

RISER TYPE - SIDE FEED		PRESSURE SECT - 0.50		PAGE 1	
SUBRISER #	SUBRISER DIA	SUBRISER HEIGHT	TOTAL HEIGHT	PIPE DIA	
1	1.58	0.30	0.30	1.28	
2	2.05	0.38	0.68	1.23	
3	1.89	0.35	1.03	0.75	
4	2.19	0.41	1.44	0.67	
5	2.53	0.47	1.92	0.63	
6	2.88	0.54	2.46	0.61	
7	3.28	0.61	3.07	0.62	
8	3.72	0.70	3.77	0.69	
9	3.72	2.02	5.79	0.00	
CAST WEIGHT	RISER WEIGHT	YIELD	FEED %	ASPECT RATIO	DENS
4.83	5.87	45.14	3.50	1.50	0.05

Fig. 6. Dimensional results and yield for riser drawing of Figure 5.

RISERING

RISER TYPE - SIDE FEED		PRESSURE SECT - 0.50		PAGE 1	
SUBRISER #	SUBRISER DIA	SUBRISER HEIGHT	TOTAL HEIGHT	PIPE DIA	
1	3.06	8.06	2.06	0.91	
2	3.06	8.03	4.08	0.00	
CAST WEIGHT	RISER WEIGHT	YIELD	FEED %	ASPECT RATIO	DENS
4.97	7.55	39.69	3.50	0.75	0.00

SCALE

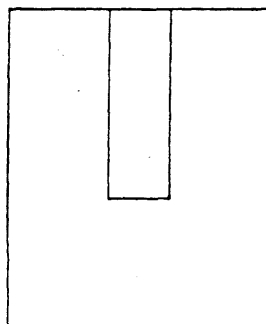
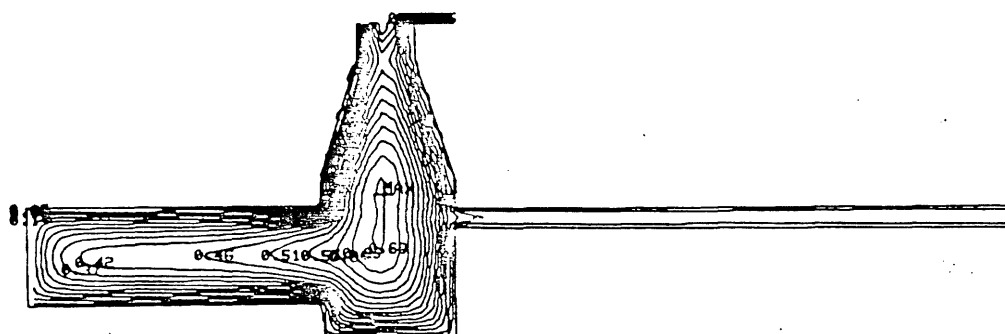


Fig. 7. Cylindrical riser designed to replace the tapered riser of Figure 5.

ASPECT
DRAW
PRESSR
RESULT
SECTNS
SHRINK
TYPE
USAMAX
CLEAR
EXIT
HELP
INPUT
RECALL
ROTATE
S-HEAT
STORE
TRANS
ZOOM
ASPECT
DRAW
PRESSR
RESULT
SECTNS
SHRINK
TYPE
USAMAX
CLEAR
EXIT
HELP
INPUT
RECALL
ROTATE
S-HEAT
STORE
TRANS
ZOOM

RAW DATA
COMPUTING VALUES.
FILL= 8679
OF CONTOURS?
--15

SECTION MODULUS



SCALE



STATUS: CASTING NOT MOLD NORMALIZED NO SUPERHEAT

USA (IN.)
0.4618E-01
0.9236E-01
0.1385E+00
0.1847E+00
0.2309E+00
0.2771E+00
0.3233E+00
0.3694E+00
0.4156E+00
0.4618E+00
0.5080E+00
0.5542E+00
0.6003E+00
0.6465E+00
0.6927E+00
0.7019E+002

APEN.
CAST
NORM
RANGE
SAND
WAVE3D
WAVES
CLEAR
EXIT
HELP
INPUT
RECALL
ROTATE
S-HEAT
STORE
TPANS
ZOOM

Fig. 8. Riser with thick and thin plate castings attached as in Figure 1 and showing directional solidification toward riser. No superheat. Note cooling fin effect of thin plate shown by wave fronts.

The wave fronts in Figures 8 and 9 indicate that the thin plate is functioning as a cooling fin, thus shifting the final solidification location in the riser toward the heavy plate. This actually occurred in the casting experiments. If the thin plate is replaced with a plate 1.5-in. thick then the thermal condition on both sides of the riser is balanced. The wavefronts are then nearly symmetrical around the riser axis, as seen in Figure 10. Also, after designing the riser for thick/thick assembly it is evident that its shape at the top changes significantly (Fig. 11) from that of the thick/thin riser assembly (Fig. 5). Cavities in risers made with the thick/thin and thick/thick plate casting combinations bear out the wave front predictions (Fig. 12). An Al-Si eutectic alloy was used to make these castings. The thin plate is seen to cause the pipe cavity to develop by feeding toward the heavy casting (Fig. 12). In contrast, feeding in the thick/thick plate combination produces a vertical pipe cavity in the riser (Fig. 12). The thick castings were found to be metallographically sound.

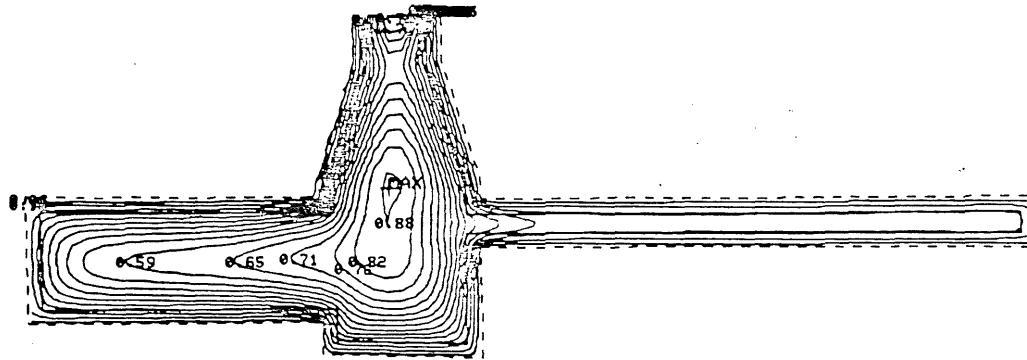
Risers designed with the wave front program do not display the riser contact graphically or dimensionally at this time. However, the riser base is included in side risers (Figs. 5-7 and 11). The contact used would follow the principles presented in references 1 and 2.

RISERING BY THE SECTION MODULUS MICROCOMPUTER PROGRAM

The program principles involved are described in reference 3. The program is called RISER II and has been made available to AFS. Again, a drawing of the shape is needed to place on the microcomputer digitizing table so the dimensions and configuration can be input. The volume, surface area, and gross section modulus are determined by vector geometry (as in the wave front program). Then the section modulus of all sections of the casting and their freezing order must be determined. Figure 13 shows printouts of the total casting volume, section modulus, and also the section modulus of the two different sections of the casting. The overall section modulus is much lower than the section modulus of the thick section because of the very low section modulus thin section. The larger section modulus must be used to design the riser, not the average or overall modulus; $D_R = 4SM + D_p$. Much too small a riser would result if the average SM were used. Using the section modulus and freezing order SM data, the riser(s) can then be designed.

SUPERHEAT
PERCENT SUPERHEAT?
% -->1.5
ORIGINAL AREA = 17.645 IN2
SUPERHEATED AREA = 26.571 IN2
COMPUTING VALUES.
FILL= 11689
OF CONTOURS?
-->15

SECTION MODULUS



SCALE

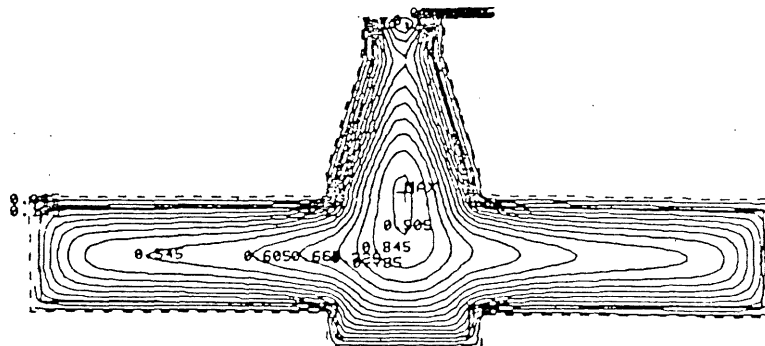


STATUS: CASTING NOT MOLD RAW NO SUPERHEAT

Fig. 9. Same as Figure 8 but with 1.5% superheat.

RAW DATA
SUPERHEAT
PERCENT SUPERHEAT?
% -->1.5
ORIGINAL AREA = 21.435 IN2
SUPERHEATED AREA = 26.516 IN2
COMPUTING VALUES.
FILL= 16255
OF CONTOURS?
-->15

SECTION MODULUS



SCALE



STATUS: CASTING NOT MOLD NORMALIZED NO SUPERHEAT

Fig. 10. Riser with two thick plate castings attached. Note balanced directional solidification compared with Figures 8 and 9. Superheat of 1.5%.

USA (IN.)

0.5879E-01
0.1176E+00
0.1764E+00
0.2352E+00
0.2939E+00
0.3527E+00
0.4115E+00
0.4703E+00
0.5291E+00
0.5879E+00
0.6467E+00
0.7055E+00
0.7642E+00
0.8230E+00
0.8818E+00
0.8936E+00

AREA
WAVE
CAST
NORM
RANGE
SAND
WAVE3D
WAVES
CLEAR
EXIT
HELP
INPUT
RECALL
ROTATE
S-HEAT
STORE
TRANS
ZOOM

USA (IN.)

0.6018E-01
0.1204E+00
0.1806E+00
0.2407E+00
0.3009E+00
0.3611E+00
0.4213E+00
0.4815E+00
0.5417E+00
0.6018E+00
0.6620E+00
0.7222E+00
0.7824E+00
0.8426E+00
0.9028E+00
0.9148E+00

AREA
WAVE
CAST
NORM
RANGE
SAND
WAVE3D
WAVES
CLEAR
EXIT
HELP
INPUT
RECALL
ROTATE
S-HEAT
STORE
TRANS
ZOOM

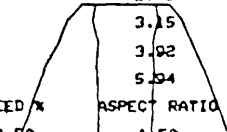
RISERING

RISER TYPE - SIDE FEED

PRESSURE SECT - 0.50

PAGE 1

SUBRISER #	SUBRISER DIA	SUBRISER HEIGHT	TOTAL HEIGHT	PIPE DIA
1	1.32	0.25	0.25	0.04
2	1.75	0.33	0.58	0.09
3	2.00	0.37	0.95	0.86
4	2.59	0.45	1.40	0.87
5	2.73	0.51	1.91	0.83
6	3.10	0.58	2.49	0.82
7	3.52	0.66	3.15	0.86
8	4.09	0.77	3.92	1.05
9	4.09	2.02	5.94	0.00



CAST WEIGHT

RISER WEIGHT

YIELD

FEED %

ASPECT RATIO

DENS

6.53

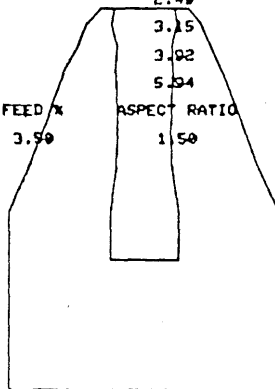
7.09

47.96

3.59

1.50

0.09



SCALE



ASPECT
DRAW
PRESSR
RESULT
SECTMS
SHRINK
TYPE
USAMAX
CLEAR
EXIT
HELP
INPUT
RECALL
ROTATE
S-HEAT
STORE
TRANS
ZOOM

Fig. 11. Riser design for the thick/thick plate casting assembly. Compare with Figures 5 and 6 and note change in funnel shape at the top.

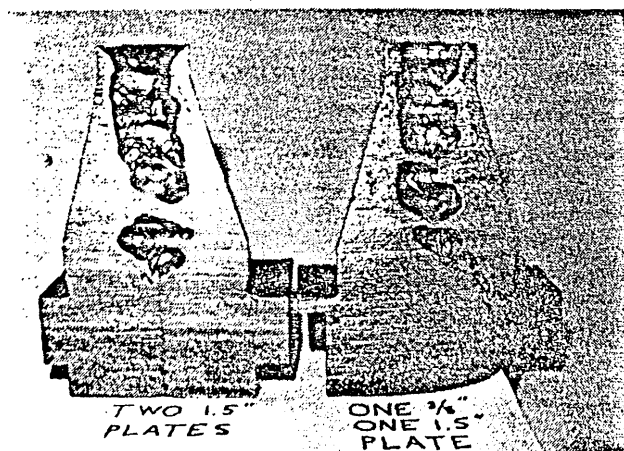
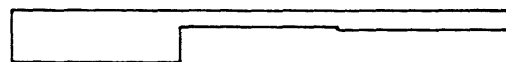


Fig. 12. Sectioned Al-Si eutectic alloy risers. Left, riser used to feed two 1.5-in. plates for comparison with Figure 10. Right, riser used to feed one 1.5-in. thick plate and one 0.375-in. thick plate for comparison with Figure 9.

SM = 0.301

VOL = 54.44



(3) New Subsection

(4) New Shape

Choose

SM = 0.298

VOL = 4.45



SM = 0.598 0.747 0.173

(3) New Subsection

(4) New Shape

Choose

Fig. 13. Printout of microcomputer screen showing the shape input, its volume and overall section modulus and the modulus of the two different sections of the casting and consequently their freezing order.

SM = 0.6						SM = 0.75					
RSM	HP	DP	HP	DR	YIELD	RSM	HP	DP	HP	DR	YIELD
0.26	0.33	0.81	4.84	51.39 *		0.28	0.33	0.81	5.44	42.08 *	
0.41	0.93	1.61	4.13	56.11 *		0.46	0.93	1.61	4.73	46.38 *	
0.52	1.71	2.42	3.81	57.39 *		0.59	1.71	2.42	4.41	47.56 *	
0.59	2.64	3.22	3.62	57.59 *		0.68	2.64	3.22	4.22	47.72 *	
0.63	3.69	4.03	3.49	57.33 *		0.74	3.69	4.03	4.09	47.44 *	
0.65	4.85	4.84	3.40	56.83 *		0.77	4.85	4.84	4.00	46.93 *	
0.67	6.11	5.64	3.32	56.21 *		0.79	6.11	5.64	3.92	46.31 *	
0.67	7.47	6.45	3.28	55.53 *		0.80	7.47	6.45	3.86	45.63	
0.67	8.91	7.26	3.21	54.81 *		0.80	8.91	7.26	3.81	44.92	
0.66	10.43	8.06	3.17	54.08 *		0.80	10.43	8.06	3.77	44.19	
0.66	12.04	8.87	3.14	53.34 *		0.80	12.04	8.87	3.74	43.47	
0.65	13.71	9.67	3.11	52.61		0.79	13.71	9.67	3.71	42.75	
0.64	15.46	10.48	3.08	51.88		0.79	15.46	10.48	3.68	42.05	
0.64	17.28	11.29	3.05	51.17		0.78	17.28	11.29	3.65	41.36	
0.63	19.17	12.09	3.03	50.47		0.77	19.17	12.09	3.63	40.69	
0.62	21.12	12.90	3.01	49.79		0.77	21.12	12.90	3.61	40.03	
0.62	23.13	13.70	2.99	49.12		0.76	23.13	13.70	3.59	39.39	
0.61	25.20	14.51	2.98	48.47		0.75	25.20	14.51	3.58	38.77	

(* - marks where max RSM: 1.3*SM being fed)

Press return to continue

Designing Riser's for Path 1		Designing Riser's for Path 1	
Number of riser's	= 1, 2 (1)	Number of riser's	= 1, 1 (0.666)
Height of feeding section	= 3.22 in.	Height of feeding section	= 3.22 in.
Height of pressure section	= 0.5 in.	Height of pressure section	= 0.5 in.
Top diameter	= 1.22, 0.86 in.	Top diameter	= 1.22 in.
Base diameter	= 3.62, 3.26 in.	Base diameter	= 4.22 in.
Total riser height	= 5.52 in.	Total riser height	= 5.970 in.
Height of contact	= 1.2 in.	Height of contact	= 1.5 in.
Width of contact	= 3 in.	Width of contact	= 3.75 in.
Length of contact	= 0.5 in.	Length of contact	= 0.5 in.
Riser volume	= 39.76, 31.24 cubic in.	Riser volume	= 59.15 cubic in.
Path volume	= 54 cubic in.	Path volume	= 54 cubic in.
Riser yield	= 57.59, 46.36%	Riser yield	= 47.72%
Enter-yes-for menu		Enter-yes-for menu	

Fig. 14. Alternate choices of tapered riser diameter, Hp/Dp, Hp and yield computed by riser program, (upper). Yield usually maximizes near Hp/Dp = 2.5. Riser dimensions, (lower) are shown after a choice of Hp is entered.

For the successful operation of this program, there must be a priori knowledge of:

- 1) The number of feeding paths in the casting: The SM values calculated are an indication of relative solidification times involved in all parts of a particular casting that determine the number of feeding paths.
- 2) The percentage feed metal required for the material chosen. The feed metal requirement is used in determining the size of the riser pipe.
- 3) The expected riser yield, as a percentage. A safe initial estimate is 50%.
- 4) The height of the pressure section. This height depends upon the location of the highest point to be fed in the casting.
- 5) The maximum feeding distance. The feeding distance over which a riser can feed a casting effectively must be known, or determined by experiment in order to ascertain the number of risers required for a given feeding path. This distance is a capability characteristic of the metal and the section thickness.

With the above information operation of the program can proceed. The program asks for the information needed to produce the output shown in Figure 14, top columns. A specific Hp, feeding section height, must be chosen and input from the list in order for the program to continue.

SM = 0.6						SM = 0.75					
RSM	HP	DP	HP	DR	YIELD	RSM	HP	DP	HP	DR	YIELD
0.87	0.33	0.81	4.84	48.55 *		0.98	0.33	0.81	5.44	39.19 *	
0.82	0.93	1.61	4.13	50.77 *		0.93	0.93	1.61	4.73	41.35 *	
0.79	1.71	2.42	3.81	50.09 *		0.91	1.71	2.42	4.41	40.61 *	
0.78	2.64	3.22	3.62	48.69 *		0.90	2.64	3.22	4.22	39.21 *	
0.77	3.69	4.03	3.49	47.10 *		0.89	3.69	4.03	4.09	37.71 *	
0.76	4.85	4.84	3.40	45.49 *		0.88	4.85	4.84	4.00	36.19 *	
0.75	6.11	5.64	3.32	43.94 *		0.88	6.11	5.64	3.92	34.73 *	
0.75	7.47	6.45	3.28	42.45 *		0.87	7.47	6.45	3.86	33.36	
0.74	8.91	7.26	3.21	41.05 *		0.87	8.91	7.26	3.81	32.08	
0.74	10.43	8.06	3.17	39.73 *		0.87	10.43	8.06	3.77	30.88	
0.73	12.04	8.87	3.14	38.49 *		0.86	12.04	8.87	3.74	29.77	
0.73	13.71	9.67	3.11	37.32		0.86	13.71	9.67	3.71	28.73	
0.73	15.46	10.48	3.08	36.22		0.86	15.46	10.48	3.68	27.76	
0.72	17.28	11.29	3.05	35.19		0.86	17.28	11.29	3.65	26.85	
0.72	19.17	12.09	3.03	34.21		0.86	19.17	12.09	3.63	26.00	
0.72	21.12	12.90	3.01	33.29		0.85	21.12	12.90	3.61	25.21	
0.71	23.13	13.70	2.99	32.42		0.85	23.13	13.70	3.59	24.46	
0.71	25.20	14.51	2.98	31.59		0.85	25.20	14.51	3.58	23.75	

(* - marks where max RSM: 1.3*SM being fed)

Press return to continue

Designing Riser/s for Path 1		Designing Riser's for Path 1	
Number of riser's	= 1, 1 (0.666)	Number of riser's	= 1, 1 (0.666)
Height of feeding section	= 3.22 in.	Height of feeding section	= 3.22 in.
Height of pressure section	= 0.5 in.	Height of pressure section	= 0.5 in.
Top and bottom dia.	= 3.62, 3.623 in.	Top and bottom dia.	= 4.22, 4.223 in.
Total riser height	= 5.52 in.	Total riser height	= 5.970 in.
Height of contact	= 1.2 in.	Height of contact	= 1.5 in.
Width of contact	= 3 in.	Width of contact	= 3.75 in.
Length of contact	= 0.5 in.	Length of contact	= 0.5 in.
Riser volume	= 56.89 cubic in.	Riser volume	= 83.60 cubic in.
Path volume	= 54 cubic in.	Path volume	= 54 cubic in.
Riser yield	= 48.70%	Riser yield	= 39.21%
Enter-yes-for menu		Enter-yes-for menu	

Fig. 15. Same as Figure 14 but for cylindrical risers.

The output provides several alternative riser diameters and Hp heights. Based on mold requirements, a specific diameter/height combination is chosen. In general, best results are obtained when the ratio of Hp/Dp is 2 to 4:1, where Hp is the height of the feeding section, and Dp is the diameter of the pipe. A possible range is marked * in the output column. However, yield is maximized when Hp/Dp $\approx$ 2.5. After selecting and entering Hp, the final design is output and listed as in Figure 14 (lower). Figure 14 shows results for tapered risers at two different SM values, 0.6 and 0.75. A $\sim$ 10% loss in yield occurs when the 0.75 SM is used to design the riser. This points to the tremendous importance of choosing the correct SM value for riser design. All dimensions including the riser contact are given. The contact dimensions are for a full geometric contact. They may be reduced by necks or notches according to foundry experience.¹

The microcomputer risering program also provides a design choice for either tapered or cylindrical risers. If the risers are designed to be cylindrical, the output then becomes as shown in Figure 15. Again there is a $\sim$ 10% yield loss if a 0.75-in. SM is used instead of 0.60 in. The cylindrical risers also have about 6% lower yield than the tapered risers.

Comparison of the riser dimensions in Figure 14 with those in Figure 6 indicate that the dimensions are nearly identical from both the microcomputer and wave front programs. The assembly of plates and risers can be checked for section modulus and freezing order to determine that solidification proceeds directionally with both programs. This is done on the assembly in exactly the same way as the plates in Figure 13.

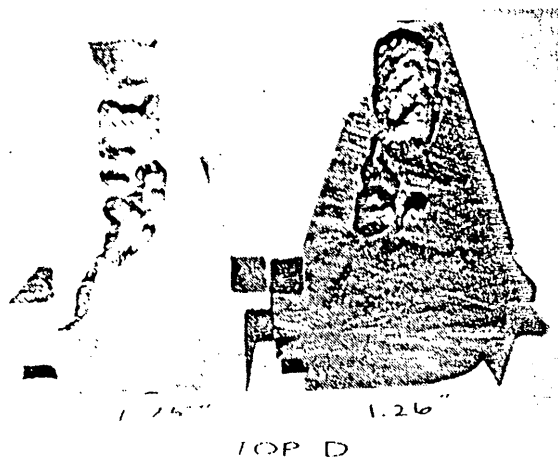


Fig. 16. Effect of top diameters of 1.26 and 1.75 in. on riser piping behavior of the thick/thin plate assembly. The 1.75-in. top dia. was obtained by capping the 1.26-in. dia. tapered riser.

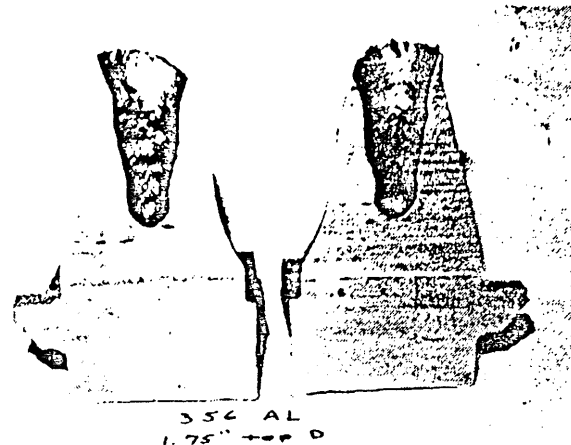


Fig. 17. Failure to pipe in A356 alloy riser with top diameter of 1.26 in.

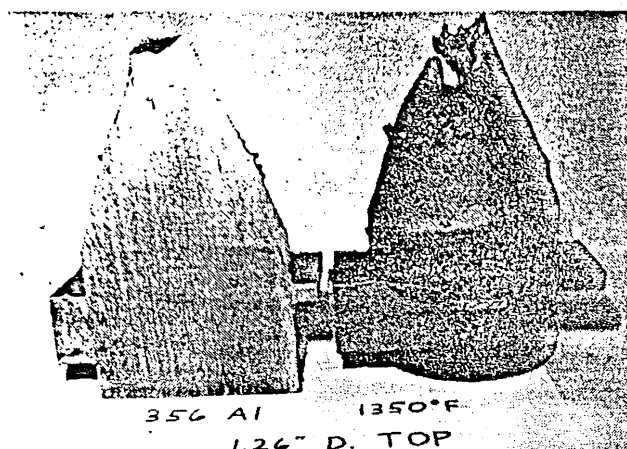


Fig. 18. Effective piping in A356 alloy riser with top diameter of 1.75 in. Compare with Figure 17. Poured at 1450F.

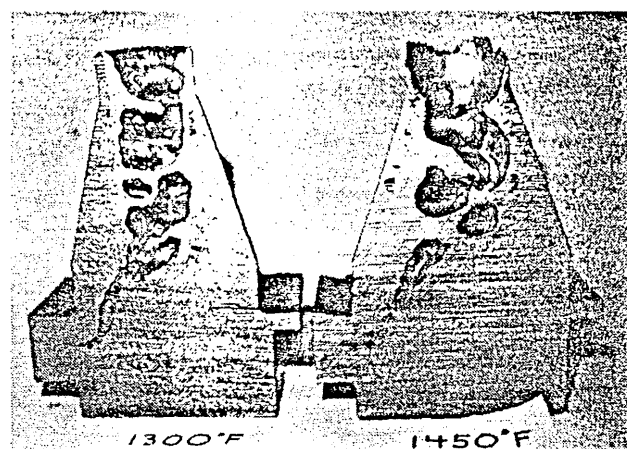


Fig. 19. Eutectic aluminum alloy risers showing increased piping and funnel top with increased pouring temperature.

CASTING EXPERIMENTS

Castings were produced in green sand to check the validity of the riser sizes and the piping behavior predicted by the computed results. In addition, certain other variables involved in risering were investigated. The alloys used were Al-Si eutectic alloy, A356 aluminum alloy, ductile iron and gray iron. The test pattern is shown in Figure 1.

Top Diameter of Tapered Risers

The top diameter of tapered risers must not be so small that it freezes off before piping begins. For cast steel, earlier work has shown that the top diameter should not be less than 1.75 in. for castings of 1.0–2.0 section thickness.⁸

Top diameters of 1.26 in. and 1.75 in. were compared with Al-Si eutectic castings (Fig. 16). A top diameter of 1.26 in. resulted in freezing over and failure to pipe adequately. Even increasing pouring temperature to 1550F did not produce consistent piping at the top. When the 1.26-in. dia. tapered riser was fitted with a 1.75-in. dia. cap, it piped successfully at all superheat temperatures of 1300–1500F (Fig. 16). Alloy 356 produced the same results

(Figs. 17 and 18). Note the smooth wall pipe of the non-eutectic alloy. The top diameter limit seems to be the same for Al and cast steel: between about 1.5 and 2.0 in.

Pouring Temperature

Providing the top diameter is sufficient; the major effect of pouring temperature seems to be on piping behavior. This is especially true of the tapered top where higher pouring temperature exaggerates the funnel effect at the top (Fig. 19). Also, more pipe volume occurs higher in the riser and a greater percentage of feed metal is required for the Al-Si alloy (Fig. 20). Note also the very smooth surface of the top funnel cavity. These effects are noted with all alloys cast thus far.

Gating Choke

Feed metal for the solidifying casting actually comes from the sprue and runners until they can no longer transport liquid because they have frozen. A choke in the gating system can be used to shut off flow from the sprue.² Early piping and maximum delivery

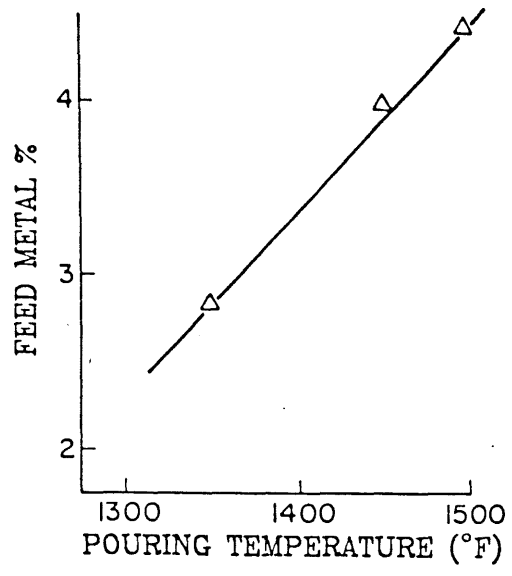


Fig. 20. Effect of pouring temperature on feed metal percentage required by eutectic Al-Si alloy.

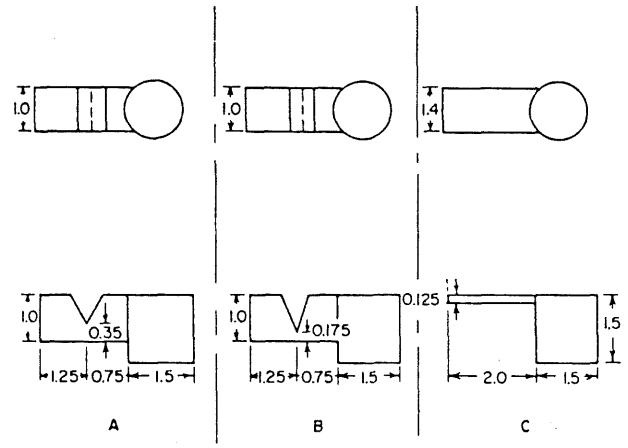


Fig. 21. Choke arrangements studied. Choke C was used as the most effective for piping.

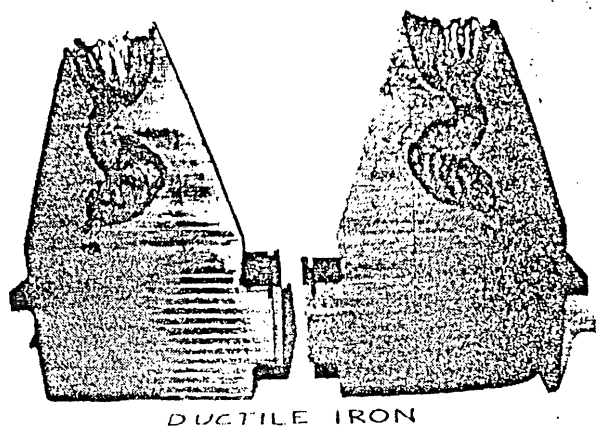


Fig. 22. Riser cross-section from thick/thin plate ductile iron casting assembly; 1.26-in. dia. top.

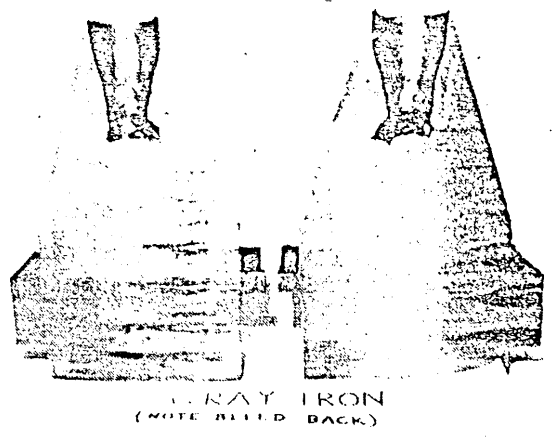


Fig. 23. Riser cross-section from thick/thin plate gray iron casting. Note bleed back, 1.26-in. dia. top.

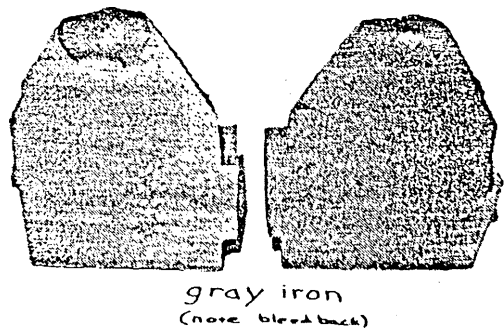


Fig. 24. Riser cross-section from thick/thin plate gray iron casting designed to produce a shorter riser, larger D_p , and shorter H_p .

of feed metal from the risers will then occur. The gating chokes were briefly considered in this research (Fig. 21). The thinner choke resulted in about 1% more feed metal being delivered by the riser.

Casting Alloy

Al-Si eutectic and A356 aluminum alloy casting results are depicted in Figures 16–20. A section risered from a ductile iron casting of the thick/thin plate type is shown in Figure 22. Piping occurred even with the 1.26-in. dia. top and a substantial part of the top of the tapered riser is missing. Other reports have shown that tapered risers with top diameters as little as 1.0 in. will function.^{2,8} Gray iron showed the same behavior at the top but the pipe cavity revealed bleeding back (Fig. 23). Feed metal percentage was 1.7. The gray iron riser appears unnecessarily large because of the bleeding back phenomenon. To check this, the riser was redesigned using the microcomputer program so that the taper was much greater; this leads to a shorter riser feeding section as shown in Figure 24. Bleed back again occurred, D_p was larger, H_p smaller, H_p/D_p smaller, the feed metal percentage was 1.9% and yield increased 5%.

Feed Metal Percentage and Piping

The volume percentage metal necessary to feed both the casting and the riser was determined by measuring the volume of the pipe in the riser. Feed metal percentage equals (pipe volume ÷ casting plus riser volume) × 100. The results of these experiments are listed in Table 1. The effect of pouring temperature on feed metal percentage for the Al-Si eutectic alloy was shown in Figure 20. The other alloys were poured at representative temperatures such as 1450F for A356 and 2500F for the cast irons.

The computer program calculation and output include the pipe dimensions D_p and H_p . In tapered risers the aluminum castings reproduce the calculated D_p and H_p values very well (Figs. 13 and 20). In ductile and gray cast iron graphitization during solidification can lead to bleed back, which displaces the pipe cavity. Cast steel has been previously shown to pipe as predicted.^{3,8} Apparently, the assumptions of a D_p and H_p which can be calculated and programmed based on section modulus are valid.

Yield

Depending on the choice of tapered or cylindrical shape and section modulus, the riser dimensions calculated and chosen, yield for this particular test pattern may vary from about 39.2–57.6%. The choice of a tapered riser instead of a cylinder increases yield about 4–8% for this pattern. The upper limit of yield with this pattern is limited by its dimensions. The thick plate provides only 4 x 6-in. feeding distance. The maximum feeding distance for a 1.5-in. plate is at least 7.5 in. in steel. Feeding two 7.5 x 7.5-in. plates from a central riser would permit yields approaching 70%. Published research has shown that yields of 75–80% are possible from a central riser if the maximum feeding distance capability of the metal is built into the casting feeding path distance.^{10,11} Hence, 39–55% yield shown in this work is lower because it is inherent in the plate design shown. Nevertheless, yield improvement available from the use of tapered risers, about 4–11%, is available in most cases.

Energy Conservation

Yield improvement leads to lower energy consumption and increased productivity. In ferrous castings, energy savings amount

to about 15 KWH/Ton of casting mold weight poured per 1% yield improvement.⁹

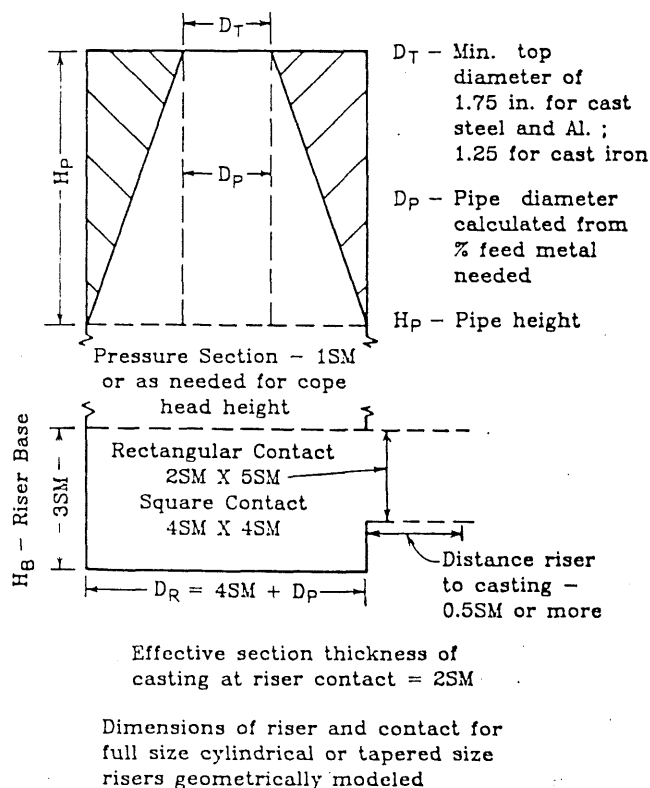
SUMMARY

The relationships between risers designed by two different types of computer programs and the piping and feed volume percentage requirements for Al-Si eutectic and A356 Al alloys has been established. Piping behavior in cross-sectioned risers has validated the predictions of the solidification wave front program as well as the riser dimensions calculated. The same riser dimensions are calculated by the microcomputer section modulus computer program although its output does not display whether the directional solidification will occur. The wave front program can prove directional solidification will occur. Both programs provide valid means of calculating riser dimensions for castings.

ACKNOWLEDGMENTS

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The M-C Method for Riser Design for Non-Ferrous and Ferrous Metals

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ABSTRACT

A new method for riser design for non-ferrous as well as ferrous metals has been developed. This method was developed as an extension to the Q-Number Method, which was developed by McAdam for steel riser design. The Q-Number Method gives results similar to that of Caine, but is easier to use and covers a wider range of casting conditions.

One of the steps in the development of the Q-Method was the relationship between the freezing ratio and the Q-Number, which is:

$$V_f/V_z = 200/Q + 0.15$$

The relationship can then be modified into an expression for the volume of the riser, which is:

$$V_r = 200 M_c^3 + bV_z$$

This expression indicates that the riser volume can be expressed as the sum of two terms, a modulus term (M_c^3), which represents the solidification time factor for the critical section of the casting section being fed by the riser, and a second term which represents the feed metal requirements (V_z) of the casting section, which includes the critical section plus any appendages being fed. The coefficient "a," which is the solidification adjustment factor, is a function of the type of riser, such as side riser on top riser, the solidification conditions, such as the use of insulating sleeves, and various other factors. The coefficient "b," which is the feed metal adjustment factor, is a function of various factors, such as the solidification shrinkage characteristics of the alloy and the feed metal capacity of the riser.

The paper shows the development of these two coefficients for various riser types and for several non-ferrous alloys. The model is applied to a relatively simple problem to demonstrate how the method can be applied.

INTRODUCTION

The Q-Number system was developed by J. McAdam¹ for steel riser design. This method produces results similar to that of Caine's Method,^{2,3} but it is much easier to use and includes many additional factors such as feeding distance, chilling, cores, and junctions. The purpose of this study is to modify the Q-Method so it can be used for other metals, especially non-ferrous metals. The modified version of the Q-Method will be referred to as the M-C Method, which is a modulus (M) method that is corrected (C) for solidification shrinkage.

The Q-Number system^{1,4} was developed because the Modulus System (based upon feed metal requirements) and the Shape Factor System (based upon solidification requirements) were adequate for the low yield and high yield ranges respectively, but neither method was satisfactory for all steel castings. This problem was also observed in the design results obtained by geometric programming^{5,6} and a short iterative approach was presented for obtaining a solution, but iterative solutions are not accepted by most foundrymen.

The Q-Number uses the modulus and volume of the section being fed by the riser. The nomenclature for the variables in the equations developed is in Table 1. The Q-Number procedure is easy to apply and can be represented by the following three steps:

Step 1: Determine M_c and V_z ,

where M_c = modulus of section to which the feeder is attached

V_z = volume of section plus appendages fed by feeder

Step 2: Calculate M_f ,

$$M_f = 0.181 \sqrt[3]{200 M_c^3 + 0.15 V_z} \quad (1)$$

where M_f = modulus of the feeder

Step 3: Calculate D

$$D = 6.0 M_f \text{ when } H = D \quad (2)$$

$$D = 5.35 M_f \text{ when } H = 1.5D \quad (3)$$

$$D = 5.0 M_f \text{ when } H = 2D \quad (4)$$

where D = diameter of cylindrical side feeder

H = height of cylindrical side feeder

McAdam¹ then considers modifications to the formula for chilling, cores, and etc. Most of the modifications are adjustments to M_f and the last step remains the same.

MODEL DEVELOPMENT

The Q-Number is not directly used in the solution procedure, but it is necessary for the understanding of the M-C method and must be developed. The Q-Number is defined as:

$$Q = V_f/M_c^3 \quad (5)$$

$$V_f/V_z = 200/Q + 0.15 \quad (6)$$

Figure 1 shows a comparison of curves expressing the volume ratio, V_f/V_z , as a function of the Freezing Ratio (Caine's Method) and as a function of the Q-Number. The Freezing Ratio can also be expressed as the ratio of the feeder modulus to the casting modulus, M_f/M_c . If the updated coefficients for Caine's Formula are used, the curves for the Q-Number and Freezing Ratio with Volume Ratio are nearly identical, as indicated by Figure 2.

If one substitutes Equation 5 into Equation 6 and rearranges the terms, one obtains:

$$V_r = 200 M_c^3 + .15V_z \quad (7)$$

This expression indicates that the volume of the riser is the sum of two terms, a modulus term which represents the solidification time factor and a volume term which represents the feed metal requirements of the casting. This expression can be expressed in a generalized form of:

$$V_r = aM_c^3 + bV_z \quad (8)$$

Where a = solidification adjustment factor (saf)
 b = feed metal adjustment factor ($fmaf$)

The saf is approximately 200 for most cylindrical side riser shapes. The saf values for various cylindrical riser types and H/D ratios are presented in Table 2. The saf values will be affected by riser sleeves, hot topping, and other parameters, but these effects have not been developed at this time.

The $fmaf$ will be primarily dependent upon the solidification shrinkage characteristics of the alloy and the feed metal capacity of the

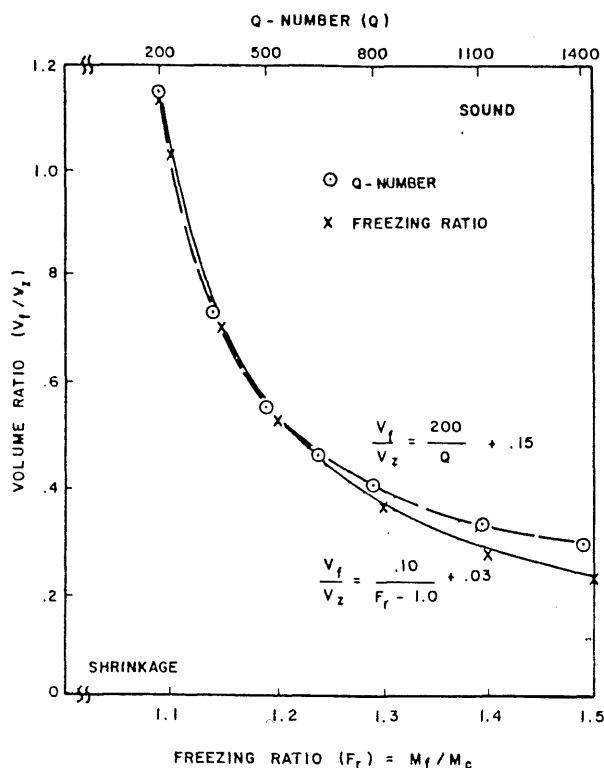


Fig. 1. Volume ratio as a function of the freezing ratio (F_r) and as a function of the Q-Number (Q).

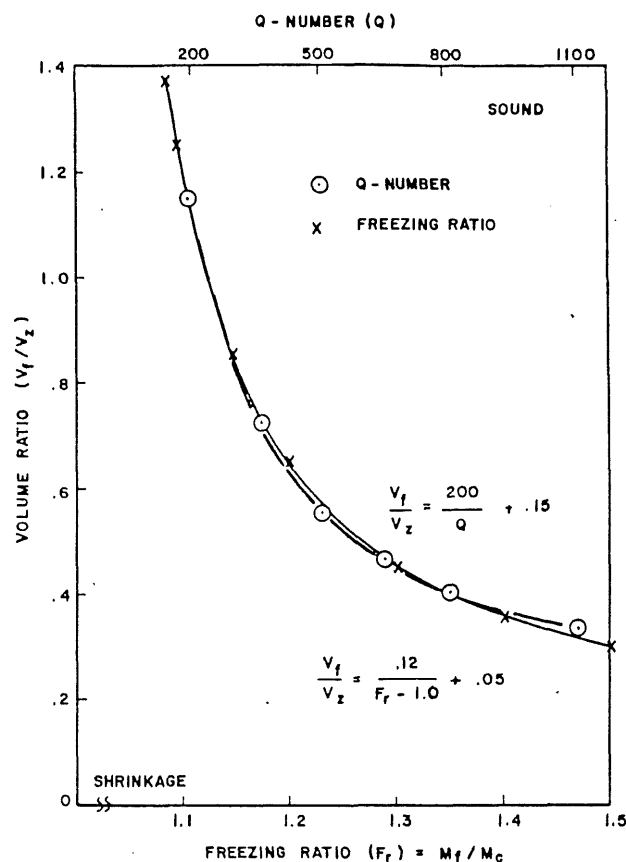


Fig. 2. Volume ratio as a function of the freezing ratio (F_r) using the updated version of Caine's formula and as a function of the Q-Number (Q).

riser. The $fmaf$ can be approximated by the ratio of the mean solidification shrinkage to the feed metal capacity of riser shape. For example, the feed metal capacity for a cylindrical riser has been shown by Umble⁷ to be approximately 1/6th of the riser volume for sand risers. The values of solidification shrinkage for various alloys was reported by Merchant⁸ and are included in Table 2. The values of the feed metal adjustment factors ($fmaf$) for various alloys are presented in Table 3. These $fmaf$ values would also change with insulating aids such as riser sleeves, because the feed metal capacity would change. The calculated $fmaf$ value for steel fortunately was the same as the value in the original expression represented by Equation 7.

MODEL APPLICATION

To illustrate the application of the model to a foundry situation, consider the casting of a 3-in. $\times$ 5-in. $\times$ 5-in. brass bar in a sand mold with a cylindrical side riser. The values of V_z , SA_c , and M_c would be 75 in³, 110 in², and 0.68 in. If a side riser is used, the value for "a" is 200 and the $fmaf$ value, or "b", would be 0.135 for brass. Inserting these values into Equation 8, a riser volume of 73.5 in³ would be required. The value of the cylindrical riser, V_r , can be expressed in terms of the riser height and diameter by:

$$V_r = \pi D^2 H/4 \quad (9)$$

The H/D ratio must be selected before the values of H and D can be determined. The non-ferrous risers have H/D ratios greater than

unity. If a value of H/D = 2 is selected, then the values for D and H would be 3.6-in. and 7.2-in. respectively. The modulus of the riser should be checked to verify that it is greater than the modulus of the casting. An expression for the modulus of a side riser is:

$$M_r = DH/(2D + 4H) \quad (10)$$

The value of M_r is 0.72 in., which is still greater than the 0.68 in. for M_c . The feeding distance should also be calculated to ascertain that the riser will properly feed the casting.

DISCUSSION

The proposed M-C Method involves the calculations of the calculation of the riser volume based upon the sum of two components, one representing the effect of solidification time and the other representing the effect of feed metal requirements. The solidification time adjustment factor is determined by the type of riser, such as a side on top riser, and is affected by insulating sleeves, chills, and other items which alter the solidification time. The feed metal adjustment factors is determined primarily by the alloy and the feed metal capacity, but other factors such as superheat and mold hardness could also be considered. Although the H/D ratio is permitted to vary, it must be restricted so the modulus of the riser exceeds that of the casting and also so pipe shrinkage would not extend into the casting.

Table 1. Table of Nomenclature

Symbol	Term
a	solidification adjustment factor (saf)
b	feed metal adjustment factor (fmaf)
M_c	modulus of section of casting under consideration (V_c/SA_c)
V_c	volume of casting section for modulus calculation
SA_c	surface area of casting section for modulus calculation
V_z	volume of casting section plus appendages fed by riser
M_f	modulus of riser
D	diameter of cylindrical riser
H	height of cylindrical riser
Q	Q-number
V_f	volume of riser
F_r	freezing ratio (M_f/M_c)

The riser design results presented are restricted to alloys which have a narrow freezing range and a low centerline feeding resistance. Alloys which freeze in a mushy manner, have a wide freezing range and high centerline feeding resistance will have dispersed porosity regardless of the riser design.

SUMMARY

A simple, direct expression for determining the riser volume has been obtained from the Q-Method and has become the basis of the M-C Method. This expression, in its generalized form, from Equation 8 is:

$$V_r = aM_c^3 + bV_z \quad (11)$$

Values for "a" and "b," the solidification adjustment factor and feed metal adjustment factor, respectively, are presented in Table 2 and 3. This system allows for a variation in the height-to-diameter ratio and can be applied to alloys that have a narrow freezing range and low centerline feeding resistance. Experimental testing needs to be performed to verify the validity of the M-C Method and to quantify the effect of solidification aids, chills, and superheat upon the two adjustment factors.

APPENDIX A

The Q-Number System

The Q-Number is a dimensionless, modulus-based shape indicator, which is calculated by

$$Q = V_r/M_c^3 \quad (A-1)$$

Table 2. Solidification Adjustment Factors (saf) for Cylindrical Risers

Riser Type	H/D Ratio	saf Value (calculated)	saf Value (recommended)
Side	1.0	169	200
Side	.5	201	200
Side	2.0	196	200
Top	.5	84.5	100
Top	1.0	98	100

Table 3. Feed Metal Adjustment Factors (fmaf) for Various Alloys

Alloy	Solidification Shrinkage	Mean Solidification Shrinkage	Feed Metal Capacity	fmaf value
[1]	[2]	[3] = [2]/2	[4]	[5] = [3]/[4]
Steel	.05	.025	1/6	.15
Brass (60-40)	.045 [8]	.0225	1/6	.135
Aluminum	.057 [8]	.0285	1/6	.171
Bronze (88-10-2)	.065 [8]	.0325	1/6	.195
Al - 4.5 Cu	.065 [8]	.0325	1/6	.195
Al - 8.5 Mg	.062 [8]	.031	1/6	.186
Copper	.04 [8]	.02	1/6	.12

where

V_z = volume of casting section plus appendages

M_c = modulus of casting section under consideration

The modulus of the casting is based upon the total volume of the casting section considered and the total cooling surface area. The cooling surface area may be adjusted for chills and cores. The volume ratio, V_r/V_z , can be expressed by

$$V_r/V_z = 200/Q + .15 \quad (A-2)$$

where

V_r = volume of riser (feeder)

Thus, by knowing V_z and M_c , one can determine Q and then V_r , the volume of the feeder. The optimal ratio for the side riser is $H = D$ and for the top riser is $H = D/2$, so one can determine the appropriate values of H and D by

$$D = (4 V_r / (\pi (H/D)))^{.333} \quad (A-3)$$

where

H/D = height: diameter ratio for the riser shape selected

In the original presentation, the author used for Q-Number to calculate the riser modulus, M_r , and then calculated the riser dimensions from the riser modulus. The expression for M_r is

$$M_r = .19 \sqrt[3]{200 M_c^3 + .15 V_r} \quad (A-4)$$

The values of .19 and 200 both include safety factors, so the diameter calculated is at least 5% greater than the optimal and may be even 10 to 12% greater. These factors may result in riser volumes of 15 to 40% above optimal. If one wants to apply a safety factor, it should be on the volume rather than the modulus. Also, one can calculate the riser volume easier and it will have one less adjustment.

Adjustments for chills and cores can be made to the cooling surface area in the casting modulus; that is

$$M_c = V_r/SA \quad (A-5)$$

where

SA = cooling surface area

For chills the surface area would be increased and for small cores the area would be decreased. The modulus can also be decreased by a modulus alteration factor for insulating materials.

Thus, the purpose of the modified method is to calculate the volume of the riser more directly and use one less correction factor. The modulus of the riser is not as critical as the volume of the riser, and the Q-Method uses the volume to calculate the modulus and then the modulus to calculate the diameter. On the other hand, the M-C Method calculates the riser volume and then calculates the riser diameter; it does not use the riser modulus directly except as a check to prevent a poor H/D selection.

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Riser Conversion From White Iron to Ductile Iron Casting

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INTRODUCTION

Malleable iron foundries producing both malleable and ductile iron castings may be asked to convert existing pattern equipment from malleable to ductile iron production. Modifying the risering and gating is the most common change required. After altering risering and gating for ductile iron, reconversion to malleable iron sometimes occurs. This report describes the conversion from malleable to ductile iron risering and gating which is used successfully by one producer.

THE GEOMETRIC RISERING TECHNIQUE

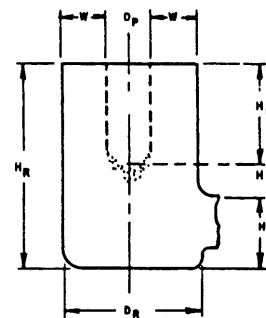
Design of the risers initially used on patterns for malleable castings is based on the geometric risering technique. Conversion involves starting with this type of riser, especially if it is a cast pattern plate. The geometric technique for both malleable and ductile iron risering has been extensively reported in the *AFS Transactions*, most recently in reference 1 and its bibliography. However, a brief review of the technique is in order since this is the starting point of the conversion. The review is condensed from reference 1.

In the geometric risering system feed metal required by the casting is held to be mainly the result of dilation of the green sand mold cavity and amounts to about 2.0 to 8.0% by volume depending on casting section thickness and sand mold density. Hence, feed metal is required even though an expansion in the late stages of freezing of ductile iron occurs. This is claimed as making riserless castings possible under certain conditions. The geometric technique assumes that mold cavity dilation will occur, that feed metal is required and that it will amount to 2 to 8% by volume. The technique will be illustrated first for a single side riser, Fig. 1. The riser, Fig. 1, is divided vertically into three sections: the feeding section of the height H_P , the pressure section of height H_M , and the base of height H_B . The feeding section is intended to provide the feed metal from a pipe cavity having a volume equal to a percentage of the casting volume, usually 2 to 8%. For white iron the pipe cavity was initially chosen to have a height/diameter ratio H_P/D_P of 2.5 to 1.0 because foundry experience produced good results. The volume of feed metal V_F then is

$$V_F = \frac{(D_P)^2 \pi H_P}{2} = \frac{\pi (D_P) \times 2.5}{4} \quad (1)$$

since $H_P = 2.5 D_P$.

Also, $V_F = V_C \times \%$ feed metal required, where V_C is the casting



$$D_R = 2W + D_P; D_P = \text{DIA. OF PIPE}$$

W = EFFECTIVE THICKNESS OF CASTING SECTION TO BE FED:
OR 0.5 D OF BAR SECTIONS.

$$H_R = H_P + H_M + H_B; H_P = \text{HEIGHT OF PIPE SECTION}$$

H_M = HEIGHT OF PRESSURE SECTION

H_B = HEIGHT OF BOTTOM SECTION

V_F = VOLUME OF PIPE AND VOLUME OF FEED METAL REQUIRED FROM RISER.

$$V_F = \frac{(D_P)^2}{2} \times H_P$$

Fig. 1. Side riser cross section showing the feeding section $D_R \times H_P$; the pressure section $D_R \times H_M$; and the riser base $D_R \times H_B$ and riser connection.

volume. Or D_P , the diameter of the pipe from (1) is:

$$D_P = \sqrt[3]{\frac{V_C \times F}{1.9635}}; \text{ where } F \text{ is the percent feed metal required.}$$

Feed metal weight required rather than volume is more conveniently used since casting weight is usually known.

The diameter of the riser, D_R , at the feeding section is obtained as follows:

$$D_R = D_P + 2W \quad (2)$$

where D_P is the pipe diameter and W is the effective section thickness of the feeding path of the casting. Proper choice of W , the effective section thickness of the casting, is often the most difficult part of obtaining D_R from (2). The significance of W is illustrated in the case of a geometrically designed side riser for a white iron casting which ran out at the parting line a short time after pouring was completed, Fig. 2. The solidifying wall, W , that holds in the interior liquid, had already begun to form when the runout occurred, Fig. 1. On the diameter of a cylindrical riser, $2W$ is needed to permit D_P to remain liquid during the time period when feeding of the casting occurs. Obviously, the riser connection must also be open during this time period.

To determine W , the effective section thickness of the feeding path to which the riser is connected must be known. In the simplest case, W = plate thickness, T , where the side riser connection is made into a plate casting; or, $W = 0.5D$, where D is the bar diameter where the side riser connection is made into the side or end of a cylindrical bar, or $0.5E$, where E is the edge dimension of a square bar; or, $W = 2 \times (V/SA)$ where (V/SA) is the effective section modulus of the feeding path. Again, V/SA

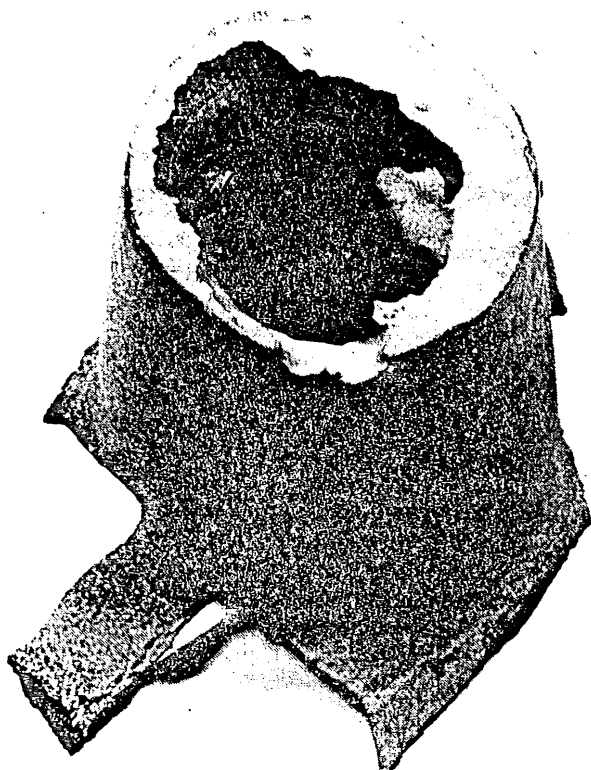


Fig. 2. Geometrically designed side riser from white iron casting which ran out at the parting line a short time after pouring was completed. With reference to Fig. 1, note the solidified wall, w , which delays freezing of the interior, D_P , which had begun to pipe; $D_R = 2w + D_P$.

section modulus = $T/2$ for infinite plates or $W = T$; and V/SA for infinite bars = $D/4$ or $W = 2D/4 = 0.5D$ as stated above; and for square bars $V/SA = E/4$ or $W = 2 \times E/4 = 0.5E$.

Thus, W is always expressed as the effective section thickness of a plate equivalent to the section modulus of the feeding path, or $2V/SA$. The diameter of the feeding section of the riser then is obtained from equation 2 and its height from $H_P = 2.5D_P$ as long as the H_P/D_P ratio is assumed to be 2.5. The calculation may be simplified by graphical presentation as in Fig. 3.

The value of D_P and H_P necessary to provide 6% feed metal volume based on the weight of the casting, assuming a density of 0.25 lb/in.^3 is shown for H_P/D_P ratios of 1.0 to 4.0 in Fig. 3. The 2.5 ratio is commonly used for white iron. The information in Fig. 3 is also presented in Table 1.

Designing the riser feeding section for a 10 lb malleable plate casting of $8 \times 5 \times 1.0 \text{ in.}$ is done very simply using Table 1 or Fig. 3. When $H_P/D_P = 2.5$ and casting weight is 10 lb, Table 1 gives D_P as 1.07 inches, $H_P = 2.67$ inches, and $W = 1.0$ inches. $D_R = 2W + D_P = 2 \times 1 + 1.07 = 3.07$ inches. The feeding section of the riser then is a cylinder with $D_R = 3.07$ and $H_P = 2.67$ inches. The height of the riser is increased as needed for pressure head and since the casting is a plate in the drag only a small increase of 0.5

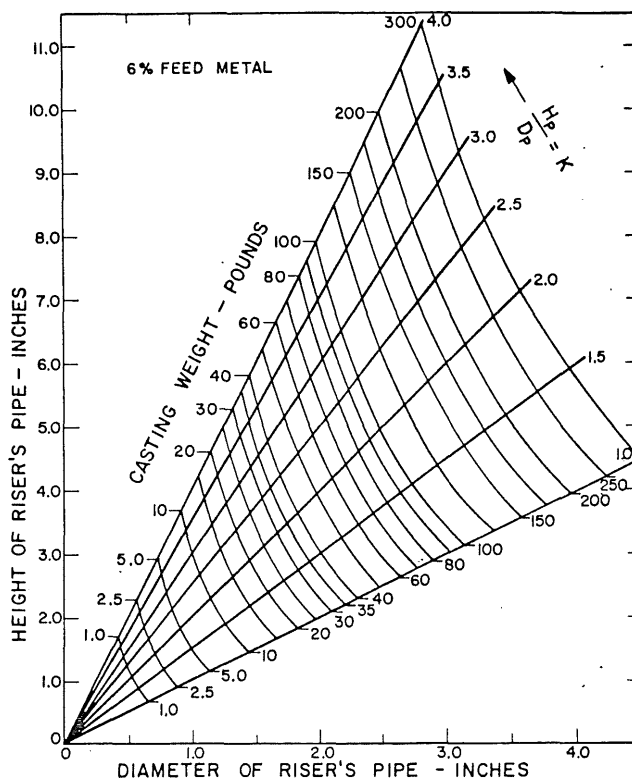


Fig. 3. Dimensions of riser volume providing feed metal, D_P , diameter, and H_P , height of pipe for various ratios of H_P and D_P as related to casting weight assuming 6% feed metal is required and iron has a density of $0.25 \text{ lb per cubic inch}$.

to 1.0 inches above the parting is needed. The feeding section, H_P , and the pressure section designed in this way for malleable iron castings is the first change considered for ductile iron. Actually, the bottom half (or less) of the geometrically designed feeding section is used for the converted riser.

CONVERSION OF RISER FEEDING SECTION FROM WHITE TO DUCTILE IRON

One of the most important changes that must be accomplished when altering malleable iron risers for ductile iron is to obtain the proper design of riser for piping. If the top diameter of the riser is not of the correct size, the riser will not pipe; and shrinkage will result at the ingate of the casting or in the casting.

The simplest way to obtain a proper diameter from the malleable riser is to mount a pin on top of the present riser with the diameter of the pin slightly larger than D_P for ductile iron, Fig. 4a. Usually, a pin of 1 inch to 1-1/4 inch with 5° draft will give good piping of the riser for castings in the 5 lb to 40 lb range. The length of the pin should be between 1-1/2 inches and 3 inches. If longer than 3 inches, the pin may freeze too rapidly; and, if shorter than 1-1/2 inches, it may not pipe properly. Pins which are effective can be ascertained by sectioning, as shown in Fig. 4b. If the diameter and height of the pin are incorrect, secondary shrinkage will occur below the pipe and in the casting, Fig. 4b, left. A proper size pipe will pipe effectively down into the riser body as in Fig. 4b, right. The riser, although effective for feeding, will decrease the mold yield.

To increase the yield of the mold and still have proper piping,

Table 1. Riser Pipe Dimensions for Feeding Section (0.25 lb/in³ density; 6% feed metal)

Casting weight, Pounds	$H_p + D_p = 1$		$H_p + D_p = 1.5$		$H_p + D_p = 2.0$		$H_p + D_p = 2.5$		$H_p + D_p = 3.0$		$H_p + D_p = 4.0$	
	D_p	H_p	D_p	H_p	D_p	H_p	D_p	H_p	D_p	H_p	D_p	H_p
1.0	0.674	0.67	0.588	0.882	0.535	1.07	0.496	1.24	0.467	1.40	0.424	1.696
2.5	0.91	0.91	0.8	1.2	0.73	1.45	0.674	1.68	0.634	1.90	0.58	2.3
5	1.15	1.15	1.01	1.51	0.91	1.83	0.849	2.12	0.8	2.4	0.73	2.9
10	1.45	1.45	1.26	1.90	1.15	2.30	1.07	2.67	1.01	3.03	0.914	3.66
15	1.66	1.66	1.45	2.18	1.32	2.64	1.22	3.06	1.15	3.45	1.05	4.19
20	1.83	1.83	1.59	2.39	1.45	2.90	1.35	3.37	1.27	3.80	1.15	4.6
25	1.97	1.97	1.72	2.58	1.56	3.13	1.45	3.63	1.37	4.1	1.24	4.96
30	2.09	2.09	1.83	2.74	1.66	3.32	1.54	3.86	1.45	4.35	1.32	5.27
35	2.20	2.20	1.93	2.89	1.75	3.50	1.62	4.06	1.53	4.58	1.39	5.55
40	2.30	2.30	2.01	3.02	1.83	3.66	1.70	4.24	1.60	4.8	1.45	5.8
50	2.48	2.48	2.17	3.25	1.97	3.94	1.83	4.57	1.72	5.16	1.56	6.25
60	2.64	2.64	2.3	3.46	2.09	4.19	1.94	4.86	1.83	5.48	1.66	6.64
70	2.78	2.78	2.42	3.64	2.20	4.40	2.05	5.11	1.92	5.77	1.75	6.99
80	2.90	2.90	2.54	3.80	2.30	4.6	2.14	5.35	2.01	6.03	1.83	7.3
90	3.02	3.02	2.64	3.96	2.4	4.8	2.22	5.56	2.09	6.27	1.9	7.6
100	3.13	3.13	2.73	4.1	2.48	4.96	2.30	5.76	2.17	6.5	1.97	7.88
125	3.37	3.37	2.94	4.41	2.67	5.35	2.48	6.2	2.34	7.0	2.12	8.49
150	3.58	3.58	3.13	4.69	2.84	5.68	2.64	6.59	2.48	7.44	2.25	9.0
175	3.77	3.77	3.29	4.94	2.99	5.98	2.78	6.94	2.61	7.84	2.37	9.49
200	3.94	3.94	3.44	5.16	3.13	6.26	2.90	7.26	2.73	8.19	2.48	9.93
250	4.24	4.24	3.71	5.56	3.37	6.74	3.13	7.82	2.94	8.83	2.67	10.7
300	4.51	4.51	3.94	5.9	3.58	7.16	3.32	8.3	3.13	9.38	2.84	11.36

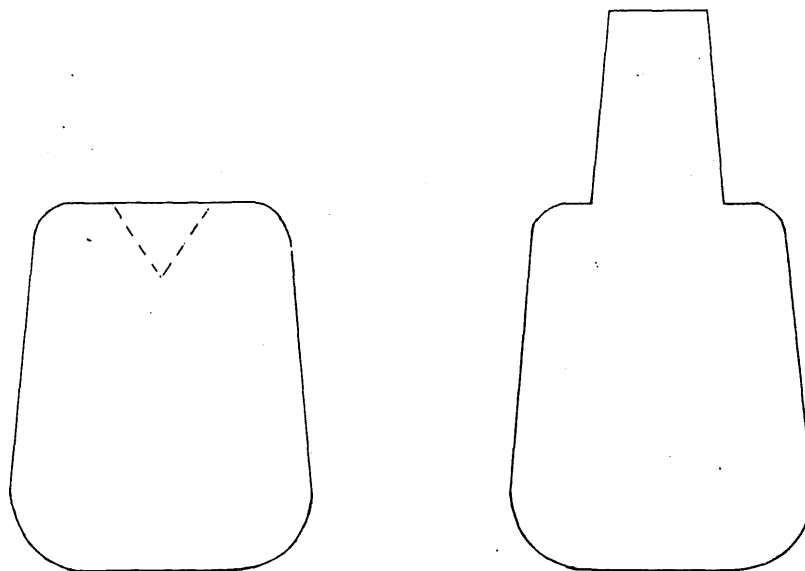


Fig. 4A. Cross section of present malleable iron riser with pin mounted on top to give effective increase in diameter when cast in ductile iron.

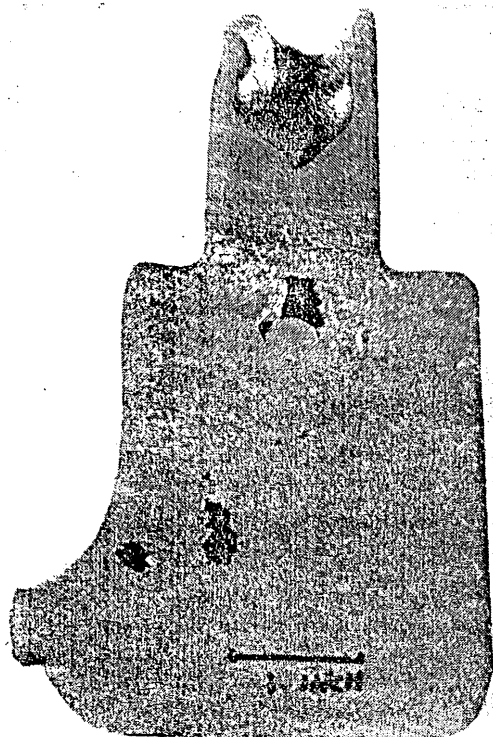


Fig. 4B. Inadequately sized pin showing poor piping action and secondary shrinkage in the riser. Right: adequately sized pin showing good piping and no secondary shrinkage.

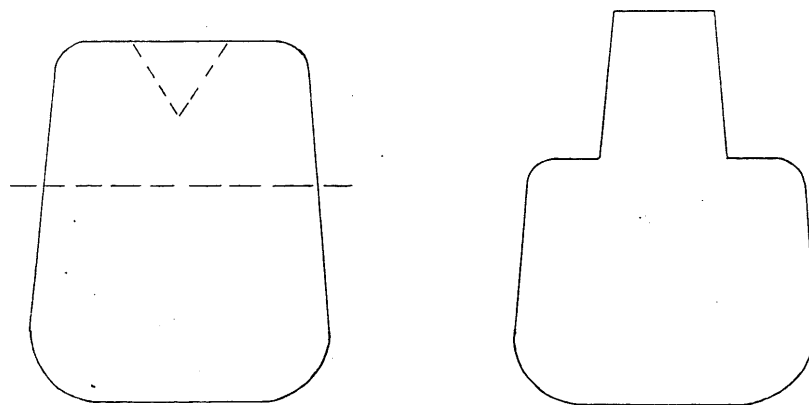


Fig. 5. Malleable riser reduced 50% in cope height and then fitted with cap and pin to maintain proper pipe size.

the risers in question should then have their cope height reduced by 50% or more. This can easily be done on a milling machine. Onto this half riser, a cap is mounted, Fig. 5. The pin on top of the cap can be a pin as previously discussed; or, if upon sectioning not enough feed metal is present, the taper of the pin on the cap may be increased or its diameter increased.

REDUCTION IN RISER CONTACT SIZE

Once the riser has a proper design for piping and feeding, the next change which must be made is the reduction in size of the

riser contact for improved spruing. Again, the riser base and contact designed initially for malleable iron comes from the geometric technique. Geometrically, the depth of the base, H_B in Fig. 1 and 6, need not be more than $1.5W$ with diameter, D_R , where W is the effective section thickness of the casting feeding path connected to the riser; or, $H_B = (1.5 \times 2 \times V)/SA$ based on the section modulus of the feeding path. Connecting the riser base to the casting involves two geometric types: rectangular and square or cylindrical in cross-section. The rectangular connection shown in Fig. 6 has cross section dimensions of $W \times 2.5W$ to $W \times D_R$, while the square connection would require $2W$

x 2W dimensions on the basis of geometric design criteria. These dimensions have been found to work well with white iron, steel and nonferrous metals, but can be reduced for ductile iron because of its unique solidification behavior. Values of 20 to 35% of W can be substituted instead in the rectangular and square connections, preferably by notching the design dimensions of the connection, as suggested in Fig. 6. Tapers of 15° from the riser to the top of the notch improve effectiveness of the notch. The same connection dimensions can be used at the base of top risers.

To convert to ductile iron, the cross section of the malleable iron riser contact is most easily altered by reducing the height of the connection. To facilitate proper spruing and still have a proper feeding path, 2/3 of the overall ingate height can usually be removed. The contact may be reduced in the drag half of the plate, cope half, or both.

CHANGES IN GATING

Gating directly into the risers is the usual procedure. Malleable iron pouring rates of 6-7 lb per second should be increased to 9-10 lb per second for ductile iron to reduce the amount of cold shut or misruns that may result when pouring a thin sectioned, large surface area type casting. To increase the pouring rate, the drag runners may be opened up or knife gates may be added to the runner system to increase the gate area and the pouring rate. These knife gates, approximately 1 inch x 1/16 inch, will not shrink at the ingate in ductile iron and usually yield a slag-free casting. Knife gates will also help remove heat from the riser connection due to excessive flow through. The reduction in heat at riser connections will help reduce shrinkage at ingates on ductile iron castings where this is a problem. With both ingate locations, at the risers and into the casting, it is important that they freeze off quickly in order to start the piping of the converted riser very early after pouring the casting is complete. In cases where existing sprue, runner, and ingates cannot be modified, completely new gating must be designed for the pattern. Either the system provided in reference 2 or the procedures taught by AFS may be used.

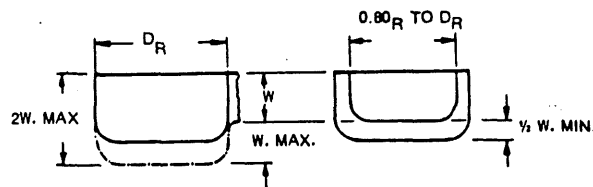
CHANGE IN YIELDS

One of the benefits of altering malleable iron riser designs when changing to ductile iron is the increase in yield which can be obtained.

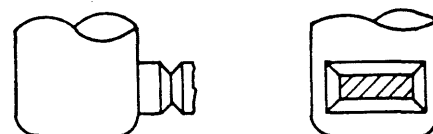
The average pattern yield on altered patterns as described is running 55%. This is an 18% increase in yield over the same jobs run in malleable iron. Yields as high as 71% can be obtained. Frequently, the number of risers on a pattern may be reduced, resulting in a large increase in yield. Theoretically, yield may approach 80% for reasons described in reference 1. However, casting design limitations and pattern layout usually lead to a yield in the 55-70% range. Gating exclusive of risering frequently amounts to 10 to 20% of mold yield and is a limiting factor.

SUMMARY

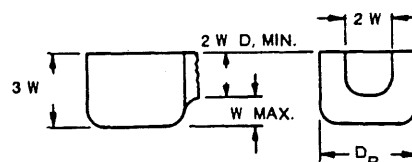
A simple method of converting risering on patterns used for malleable castings to risering and gating for ductile iron has been described. The system depends on using part of the lower



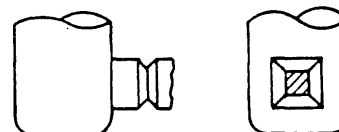
RISER BASE DIMENSIONS FOR RECTANGULAR CONNECTION TO PLATE TYPE CASTING



RECTANGULAR NOTCHED CONNECTION FOR DUCTILE IRON 0.25 W TO 0.5 W BY 2 W



RISER BASE DIMENSIONS FOR CYLINDRICAL OR SQUARE CONNECTION TO BOSS OR ROUND OR SQUARE BAR END WHERE 2 W D; D BAR DIAMETER.



SQUARE NOTCHED CONNECTION FOR DUCTILE IRON 0.5 W TO 0.25 W BY 0.5 W TO 0.25 W

Fig. 6. Riser and connection design suggested for ductile iron.

half of a geometrically designed riser and adding a cap or pin to replace the upper half. Riser connection areas are reduced for improved spruing. Gating is modified to insure early piping in the cap or pin. If piping does not occur, the cap or pin is too small and shrinkage will appear in the riser connection or casting. The converted risers and gating follow the principles described in the tapered riser design for ductile iron, as provided in reference 2.

ACKNOWLEDGEMENTS

The authors gratefully acknowledge the assistance of the Victaulic Company of America in promoting this work.

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1. R. W. Heine, "Rising Principles Applied to Ductile Iron Castings Made in Green Sand," *Trans. AFS*, 1979.
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Risening Principles Applied to Ductile Iron Castings Made in Green Sand

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Introduction

Risening ductile iron castings made in green sand molds challenges the technological knowledge and ingenuity of foundrymen. Although it has been cast in ever-increasing tonnages since its development, design systems for risering ductile iron castings seem to be technologically uncertain. The literature presents conflicts claiming that conventional risering design methods produce satisfactory results, that they do not produce satisfactory results, that no risers are needed, or only single risers are needed with a pressurized solidification system. Success stories are advanced for each claim.

A noted foundry subject lecturer suggests three approaches to risering of ductile iron castings.

- 1) Conventional risering techniques; as, for example, steel risering.
- 2) Riserless, multiple ingate feeding; particularly for castings not made in green sand; pressurized by rapid freezing of ingates.
- 3) Single top riser feeding with multiple ingating.

Since substantial increases in yield may be obtained from riserless or single top riser castings over conventionally risered castings, the appeal of the alternative methods is great. They

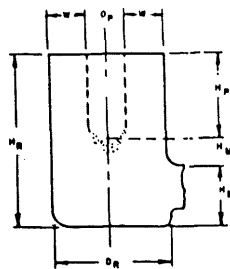
therefore are tried repeatedly and fail in more cases than they succeed. However, the apparent successes call for more and more trials, yet selection of risering method and the degree of success achieved remains uncertain. The intention of this paper is to reexamine the use of these risering techniques with specific application to small and medium weight green sand ductile iron castings and the conditions under which they are likely to produce successful results.

Conventional Risering

"Conventional risering" implies that risers will be designed in cross section, height and connection so that directional solidification will proceed along feeding paths in the casting through the connection and into the riser proper. Feed metal will move from the riser through the riser connection and through feeding paths in the casting to the farthest distance which can be fed from the risers. Conventional risering assumes that the freezing order of all sections of the casting can be determined so that one or more late freezing feeding paths can be identified. Risers may then be attached to these feeding paths to provide a volume of metal sufficient to produce a sound casting. An additional assumption of conventional risering is that the feeding distance from risers along various configurations of feeding paths is known. When freezing order, feeding paths and feeding distances are known, the number of risers and their location is established. The design of the risers and their connection to the casting may then proceed along so-called conventional lines. There are several conventional techniques but for convenience only one, known as the "Geometric Risering Technique," will be considered.

Feed Metal from Geometric Risers

The geometric risering technique has been extensively reported in AFS literature.¹⁻⁶ The feed metal required by the casting is held to be mainly the result of dilation of the green sand mold cavity and amounts to about 2.0 to 8.0% by volume depending on casting section thickness,^{1,4,6} and sand mold density. Hence feed metal is required even though several investigators have shown that little or no solidification contraction occurs with



$D_R = 2W + D_P$; $D_P = \text{DIA OF PIPE}$
 $W = \text{EFFECTIVE THICKNESS OF CASTING SECTION TO BE FED.}$
 OR $0.5 D$ OF BAR SECTIONS
 $H_R = H_P + H_M + H_B$; $H_P = \text{HEIGHT OF PIPE SECTION}$
 $H_M = \text{HEIGHT OF PRESSURE SECTION}$
 $H_B = \text{HEIGHT OF BOTTOM SECTION}$
 $V_F = \text{VOLUME OF PIPE AND VOLUME OF FEED METAL REQUIRED FROM RISER}$
 $V_F = \frac{\pi D_P^2}{2} \times H_P$

Fig. 1. Side riser cross section showing the feeding section $D_R \times H_P$; the pressure section $D_R \times H_M$; and the riser base $D_R \times H_B$ and riser connection.

ductile iron of sufficiently high carbon equivalent. There may even be an expansion in the late stages of freezing of ductile iron and this is claimed as making riserless castings possible under certain conditions. The geometric technique assumes that mold cavity dilation will occur, that feed metal is required and that it will amount to 2 to 8% by volume. The technique will be illustrated first for a single side riser, Fig. 1. The riser, Fig. 1, is



Fig. 2. Geometrically designed side-riser from white iron casting which ran out at the parting line a short time after pouring was completed. With reference to Fig. 1, note the solidified wall, W , which delays freezing of the interior, D_P , which had begun to pipe; $D_R = 2W + D_P$.

divided vertically into three sections: the feeding section of height H_P , the pressure section of height H_M and the base of height H_B . The feeding section is intended to provide the feed metal from a pipe cavity having a volume equal to a percentage of the casting volume, usually 2 to 8%. The pipe cavity was initially chosen to have a height/diameter ratio H_P/D_P of 2.5 to 1.0 because foundry experience produced good results. The volume of feed metal V_F then is

$$V_F = \frac{(D_P)^2}{2} \pi H_P = \frac{\pi (D_P)^3 \times 2.5}{4} \quad (1)$$

since $H_P = 2.5 D_P$.

Also, $V_F = V_C \times \% \text{ feed metal required}$, where V_C is the casting volume.

Or D_P , the diameter of the pipe from (1) is:

$$D_P = \sqrt[3]{\frac{V_C \times F}{1.9635}}; \text{ where } F \text{ is the percent feed metal required.}$$

Feed metal weight required rather than volume is more conveniently used since casting weight is usually known.

The diameter of the riser, D_R , at the feeding section is obtained as follows:

$$D_R = D_P + 2W \quad (2)$$

where D_P is the pipe diameter and W is the effective section thickness of the feeding path of the casting. Proper choice of W , the effective section thickness of the casting, is often the most difficult part of obtaining D_R from (2). The significance of W is illustrated in the case of a geometrically designed side riser for a white iron casting which ran out at the parting line a short time after pouring was completed, Fig. 2. The solidifying wall, W , that holds in the interior liquid, had already begun to form when the runout occurred, Fig. 1. On the diameter of a cylindrical riser, $2W$ is needed to permit D_P to remain liquid during the time period when feeding of the casting occurs. Obviously, the riser connection must also be open during this time period.

To determine W , the effective section thickness of the feeding path to which the riser is connected must be known. In the simplest case,

$W = \text{plate thickness, } T$, where the side riser connection is made into a plate casting,

or, $W = 0.5D$, where D is the bar diameter where the side riser connection is made into the side or end of a cylindrical bar, or $0.5E$, where E is the edge dimension of a square bar,

or, $W = 2 \times (V/SA)$ where (V/SA) is the effective section modulus of the feeding path. Again V/SA section modulus $= T/2$ for infinite plates or $W = T$; and V/SA for infinite bars $= D/4$ or $W = 2D/4 = 0.5D$ as stated above; and for square bars $V/SA = E/4$ or $W = 2 \times E/4 = 0.5E$.

Thus, W is always expressed as the effective section thickness of a plate equivalent to the section modulus of the feeding path, or $2V/SA$. The diameter of the feeding section of the riser then is obtained from equation (2) and its height from $H_P = 2.5 D_P$ as long as the H_P/D_P ratio is assumed to be 2.5. The calculation may be simplified by graphical presentation as in Fig. 3. The value of D_P and H_P necessary to provide 6% feed metal volume based on the weight of the casting, assuming a density of 0.25 lb/in.³ is shown for H_P/D_P ratios of 1.0 to 4.0. For ductile iron, risers with ratios of H_P/D_P in the range of 3.0 to 3.5 have been observed to perform better than those of 2.5 ratio. The latter

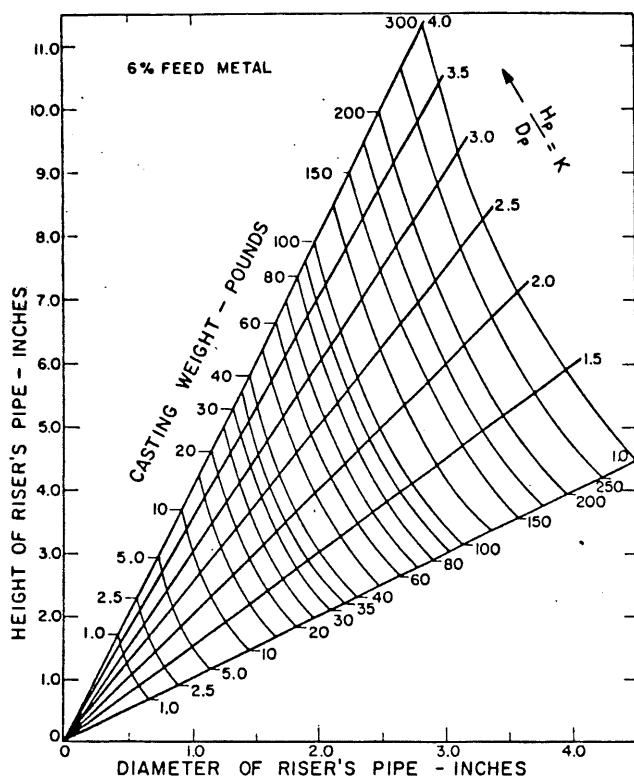


Fig. 3. Dimensions of riser volume providing feed metal, D_P = diameter, and H_P = height of pipe for various ratios of H_P/D_P as related to casting weight assuming 6% feed metal is required and iron has a density of 0.25 lb/in³.

have been found useful for white iron and steel and some nonferrous alloys.

Designing the feeding section of a riser for a 100 lb ductile iron plate casting of 20 x 20 x 1.0 in. section thickness can be done very simply with the aid of Fig. 3. When $H_P/D_P = 2.5$, $D_P = 2.3$ and $H_P = 5.75$ in., or when $H_P/D_P = 3.5$, $D_P = 2.07$ and $H_P = 7.1$ in., from Fig. 3. In each case, the combination of H_P and D_P produces a feed metal volume which is 6% of the casting volume. Then the diameter of the feeding section is:

$$D_R = 2.3 + 2 \times 1.0 = 4.3 \text{ in.}; \text{ or } D_R = 2.07 + 2 \times 1.0 \text{ or } 4.07 \text{ in.}$$

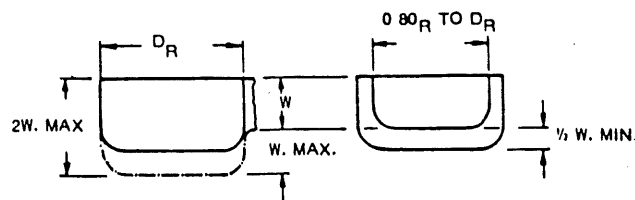
respectively for the 2.5 and 3.5 H_P/D_P ratios. These dimensions assume that $W = 1.0$ in. for the plate and that the entire casting is to be fed from one riser, a side or top riser. If two risers were required, each would need to provide feed metal for only half the casting, so Fig. 3 would be entered at the 50 lb casting weight in this example.

Pressure Section of Geometric Risers

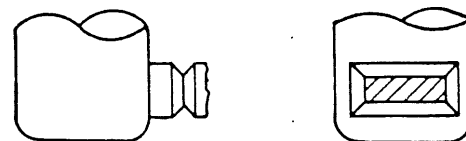
As feeding of the casting progresses, the metal level in the piping section falls. Retaining pressure on the liquid requires sufficient head to move the liquid to the castings regions which freeze last. A pressure section is added to the feeding section, H_M of Fig. 1, as needed to move liquid to the highest level to be fed in the cope. If the feeding path is in the drag, H_M generally need not be more than 1.0 in. for either side or top risers. Sprue and riser height determine the maximum pressure head, but the H_P/D_P ratio chosen establishes the riser height during the major feeding stage of solidification, with H_M added as needed.

Riser Base Design

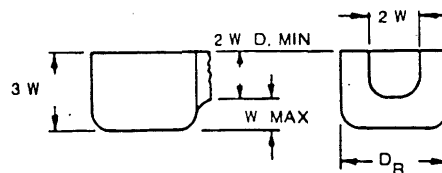
Feed metal from risers must pass through the riser base and connection to proceed along the feeding path. A side riser base and connection that is sufficiently delayed in the freezing process to permit maximum feed metal transport is the aim of



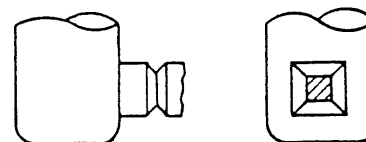
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Fig. 4. Riser base and connection design suggested for ductile iron.

the design method. Geometrically, the depth of the base, H_B in Fig. 1 and 4, need not be more than 1.5W with diameter, D_R , where W is the effective section thickness of the casting feeding path connected to the riser; or, $H_B = (1.5 \times 2 \times V) / SA$ based on the section modulus of the feeding path. Connecting the riser base to the casting involves two geometric types: rectangular and square or cylindrical in cross section. The rectangular connection shown in Fig. 4 has cross section dimensions of W x 2.5W to W x D_R , while the square connection would require 2W x 2W dimensions on the basis of geometric design criteria.⁵ These dimensions have been found to work well with white iron, steel and nonferrous metals, but can be reduced for ductile iron because of its unique solidification behavior. Values of 20 to 35% of W can be substituted instead in the rectangular and square connections, preferably by notching the design dimensions of the connection as suggested in Fig. 4. Tapers of 15° from the riser to the top of the notch improve effectiveness of the notch. The same connection dimensions can be used at the base of top risers.

Designing the riser connection is the most uncertain part of the geometric risering technique as applied to ductile iron. The full dimensions of W x 2.5W or 2W x 2W for rectangular and

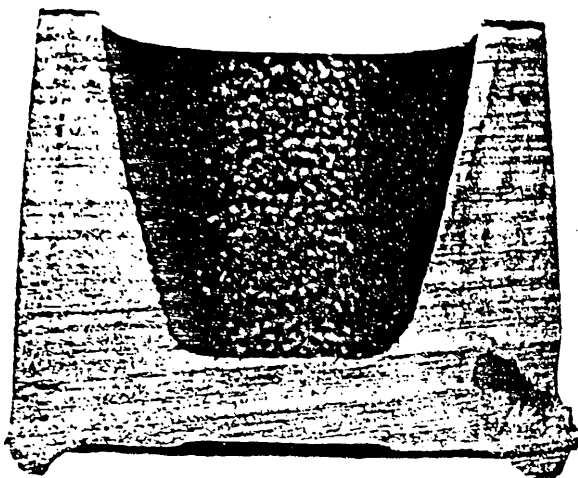


Fig. 5. Cavity on the casting side of riser connection-casting section junction as a result of too thin a rectangular connection. A slightly thicker connection prevents the cavity from forming.

square cross section connections respectively can cause objectionable riser removal problems because of the ductility and strength of the iron. Further, because of the solidification mode of ductile iron, the full dimensions are often not needed. They may be greatly reduced in thickness and notched for ease of riser removal, as stated above and shown in Fig. 4. However, when connections are reduced too much, a shrinkage cavity will occur at the casting-riser connection junction, usually on the casting side, Fig. 5. When such a cavity develops, the connection must be made thicker, in this case increased to 30% of the casting section being fed. This problem cannot be corrected by using a larger riser, but is usually achieved by a larger connection, moving the riser back from the casting when a larger connection is used, changing to a square type connection, using a taller riser, or running the iron through the casting into the riser, i.e., use of

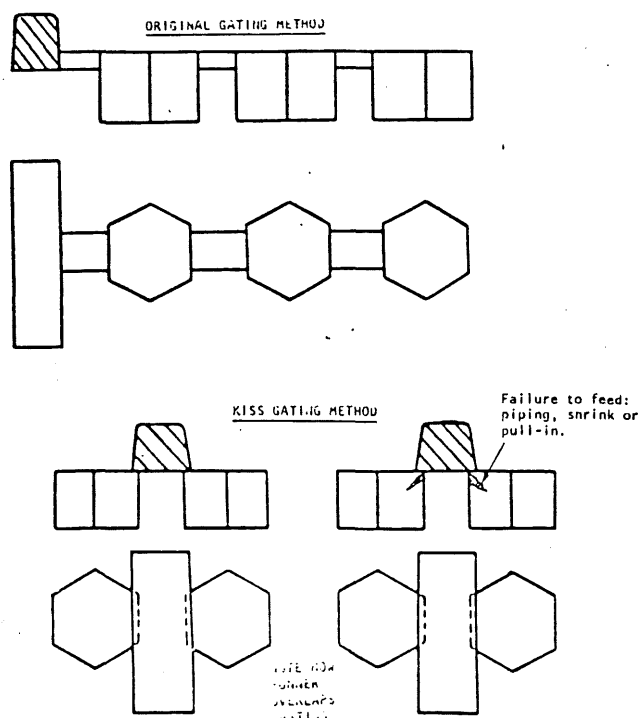


Fig. 6. Kiss gating of hex nut castings, from AFS Manual, "Basic Principles of Gating and Riser Design," see reference 11. Note location of unsatisfactory piping.

a cold riser rather than a hot riser. Selecting which of these to use depends on the casting configuration, gating, pattern rigging, molding method, temperature-freezing time gradients and other foundry variables.

When conventional risering is applied to ductile iron castings, a number of special problems are encountered which may require modification of principles. The unique solidification mode of ductile iron is the cause of the changes required. These considerations will be presented after the other risering techniques have been described.

Riserless Multiple Ingate Feeding (Sprue Runner Feeding)

Volumetric solidification contraction measurements which suggest that ductile iron castings do not need feed metal from risers result in attempts to develop riserless gating systems. Early formation of a solid skin encasing the liquid and later expansion of the interior against that skin is said to make risering unnecessary.¹⁰ Further, it is said that to take advantage of the expansion stage, freezing-off of ingates is required so that the casting cavity functions as a closed container. The gating system lets the metal enter the cavity through wide but thin ingates which freeze as the solid skin forms. Multiple ingates are required to obtain thin gate dimensions. This approach has been advocated to make riserless casting possible and is said to be applicable to rigidly bonded sand molds such as silicate, nobake, ceramic and V-process molds.¹⁰ Even in such molds, thermal dilation of the mold cavity requires transport of metal from the sprue and runner to keep the cavity full and pressurized while the metal is cooling in the liquid state until the solid skin forms. But with green sand, cavity dilation may amount to 6% or more by volume,^{10,11,12} thus making this feeding technique difficult or impossible to apply. The sprue, runner and ingates must be designed so that transport of feed metal into the casting will occur as needed by dilation of the mold cavity. Since the gating system actually provides the feed metal, it is more correct to think of the system when applied to green sand castings as risering by "sprue runner feeding" rather than as being "riserless."

Sprue runner risering requires the system to provide a volumetric percentage of feed metal as in the case of conventional risering. The feed metal comes from the sprue, the runners, or generally both. Enlargements in the runners may also be used. Since gating and risering are combined into one system, some limiting conditions exist which frequently determine whether the system can succeed. These are the following:

- 1) Castings must freeze in a short time or in less than about 1.5-2.0 minutes. Sections must be under 0.50 in. thickness to apply the system.
- 2) A pouring time of 8-10 sec or less is preferred.
- 3) Six percent of volumetric feed metal is assumed to be necessary.
- 4) The number of ingates on a casting is generally determined by a maximum of 3 to 4 in. feeding distance or distance between ingates and a maximum ingate thickness of about 0.125 in. and width of 0.5-1.0 in.
- 5) Feeding and piping must occur from the sprue, sprue cup, or runner system, not at casting ingates. Feeding is mainly to the drag.

The simplest type of sprue runner feeding is the kiss gating system¹¹ as is illustrated for casting a hex nut in Fig. 6. The dimensions of the runner cross section must be at least $2W \times 2W$, or $2W + (D_p)^2$ for feeding. If the casting has a W of 0.375 in. and weighs 1.0 lb, then D_p is 0.467 for 6% feed metal and $H_p D_p = 3.0$, from equation (2):

$$D_p = \sqrt[3]{\frac{4 \times 0.06 \times 1.0}{3 \times \pi \times 0.25}}; D_p = 0.467 \text{ and } H_p D_p = 1.401$$

The runner cross section would be $(2 \times 0.375) + 0.467 = 1.117$ in.² and H_P lies along the runner axis. The sprue could be 1.125 in. in diameter and provide feed metal to the runner and thus to the castings. As long as the sprue feeds effectively, a number of castings may be attached along the runner without increasing its cross section. If the casting section is limited to 0.50 in.², the runner cross section need not be more than 1.50 in.² for a casting that can be fed by this method.

Pressure on the feed metal is obtained from the sprue. However, if a 4:8:3 gating system design is used, the sprue or sprue base is required to have a choke which may freeze off and prevent sprue feeding from occurring. This is one of the problems that interferes with some applications of sprue runner risering. The other problem is the location of the piping cavity when feed metal is transported. There is a pronounced tendency for piping to occur adjacent to the gate connection, as shown in Fig. 6. Failure of piping to occur according to design is a major cause of unsoundness in ductile iron castings. Pressurizing the liquid in the later stages of freezing, whether from pressure head or graphitization, requires that a solid skin container be developed by both the casting and the riser systems. When piping at the surface begins at the riser or gate connection, it usually proceeds into the casting to produce a pipe cavity in the casting. Considerable design effort must be expended to ensure that piping occurs as designed. If either the runner cross section, the sprue, runner chokes or the kiss gate connection is too small, the skin will pull into the casting proper and appear as shrinkage, as shown in Fig. 5 and 6.

In some cases, the runner need not have a full cross section along its entire length. Instead, runner enlargements may be placed at the casting and the runner cross section substantially reduced between enlargements, as shown in Fig. 7. In Fig. 7, the casting has $W = 0.375$ in. as revealed by the piping action. Another example of using runner enlargements to feed a 4 lb casting is shown in Fig. 8 for a vertically parted mold. In this example, W from the casting is 0.375 in., so $2W \times 2W$ would be a 0.75×0.75 in. runner plus D_P . For half the 4 lb casting, since there are two feeders, D_P of the pipe would be about 0.50 in. and H_P about 2.0 in. at $H_P/D_P = 4$, Fig. 3. This is shown to occur in the enlargement. The runner has been reduced between the enlargements and also choked to cause flow through both ingates. Metal removed from the runner may be added to the enlargement as needed.

Since the sprue runner feeding system is used for feeding small castings under $W = 0.50$ in. section, the gate connections may be quite thin; connections of 0.125×1.0 in. or as little as 0.06×0.625 in., for example. However, as soon as they become too small, a shrink or suck-in will occur in the casting at the connection or internally at a hot spot. This can be overcome by tapering the connection to enlarge it toward the feeder so heat input will keep it open long enough for feeding to occur. Taper will also help it break cleanly at the casting during spruing.

Maximum feeding from runner to runner enlargements can be obtained when the sprue makes a maximum feed metal contribution. Sprue diameter thus should be about 10% larger in cross section than the runner cross section. An abrupt choke at the base of the sprue can serve both to control pouring time and to promote movement of the feed metal into the runner feeders, but only until the choke freezes off. A vertical choke in the runner, rather than a horizontal one at the parting, will permit maximum feeding from the sprue. A square cross section choke also promotes feeding. Within the casting, feeding to vertical heights above the ingate is uncertain and limited.

The design rules for sprue runner feeding of light section castings may be simply summarized:

- 1) The runner cross section is $(2W \times 2W) + D_P$ for kiss



Fig. 7. Runner enlargements to feed castings from a sprue runner feeding system. Piping and feeding occurred from the enlargements after the sprue ceased to keep them full.

- gating; usually 1.0 to 1.25 in.² for most castings; or,
- 2) Runner cross section is reduced and vertical enlargements are used with cross section $(2W \times 2W) + D_P \times H_P$.
- 3) Gate connections are thin and wide, 0.060 to 0.125×0.625 to 1.0 in., respectively.
- 4) Flaring of runner or runner enlargement to the connection notch at the casting is needed to keep the connection open.
- 5) A flow rate choke at the sprue base or in the runner must be vertical rather than horizontal; or it may be square in cross section.
- 6) Feeding distance appears to be limited to about 3 to 4 in. in light sections so that single castings must fulfill this limit or else multiple ingates must be used on a single casting and spaced at this distance. With the assumptions listed earlier, piping and feeding usually occur as expected.
- 7) The casting must be positioned mainly below the ingate, as feeding into the cope is limited.

Whether the use of thin ingates results in a pressurized condition later when the casting freezes or simply results in movement of feed metal into the casting at an earlier time when it is needed to prevent porosity is not easily proven from this feeding design method. However, piping demonstrates that feed metal must be transferred into even light section castings. Probably this is due to rapid and early dilation of the green sand mold cavity.

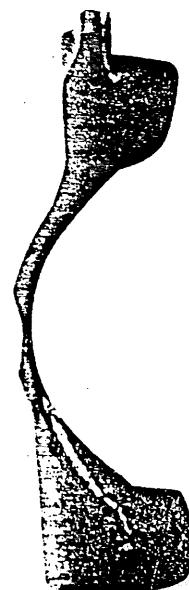


Fig. 8. Runner enlargements used to feed castings in a vertically parted mold. A single casting was fed from two ingates and two runner enlargements with a choke between them to promote flow through both ingates.

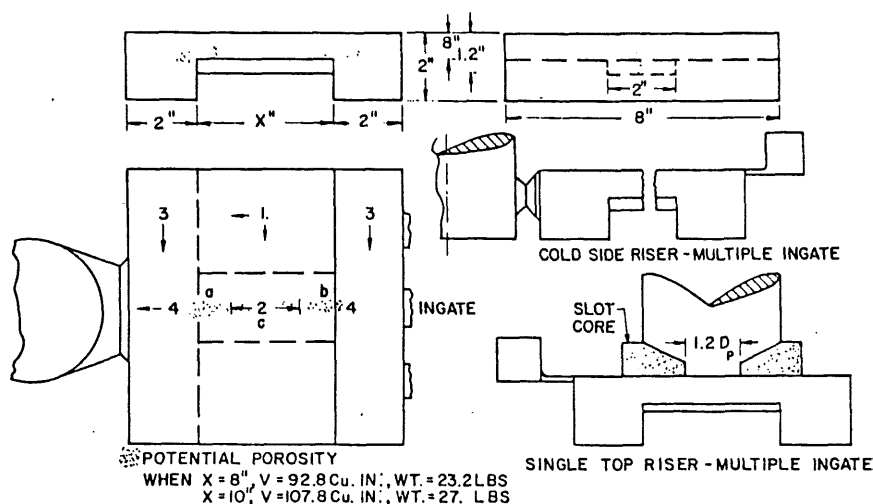


Fig. 9. Base plate casting fed from side riser or top riser with multiple ingates.

Top Riser Feeding

Top risers promote vertically pressurized feed metal flow which is most favorable for preventing porosity in ductile iron. Many castings may be fed from a single top riser when more than one side riser might be required. This is especially true when heavier sections at the outer periphery are connected to the interior by adequate sections. Use of a single top riser seems to reduce mold dilation at heavy side riser connections that permit the casting to open up internally. This problem is illustrated in Fig. 9 for a chunky casting. Introducing metal through a single conventionally designed side riser often results in shrinkage not far inside the riser connection, as indicated in Fig. 9. In addition, there may be shrinkage on the opposite side which is interpreted as a feeding distance problem and usually leads to the addition of a second side riser opposite the shrinkage location, Fig. 9. The second riser then often fails to completely clear up the porosity at the locations where it occurred initially. Sometimes the porosity can be eliminated by converting the side risers from hot to cold risers by gating directly into the casting with three or more ingates located between the risers. Many times this indicates that the casting could be fed from a single top riser (or side riser) with three or more ingates spaced along the side of the casting, Fig. 9.

The simplest design approach which may be tried first is to calculate the feeding section of the riser by the geometric method and attach it as a top riser directly to the section to be fed, as in Fig. 9. Because the D_R of the riser is large, the top riser connection becomes a serious riser removal problem. A necked-down connection or a slotted wafer core may be used, as in Fig. 9. The connection dimension through a wafer core is usually about D_P to $1.3 D_P$ for effective feeding. The same riser size may also function successfully as a cold side riser with a wafer core connection, if it can be located not too far from the site chosen for a single top riser. Pressure and bottom sections must be added to the feeding section when a side riser is used.

Multiple ingates spaced 3 or more in. apart are frequently a necessary part of the single top riser technique. The ingates must freeze well ahead of the casting at the point of entry so that there is no tendency for piping to begin at the ingate. Feed metal flow caused by the pressure head from the top riser then is not cancelled by the pressure head from the sprue. Thus, there is a positive riser pressure head on all casting walls soon after the mold cavity has filled. As an example, if a 1.0 in.² total ingate is calculated as needed to control pouring time, it is evident that four 0.25 x 1.0 in. ingates will freeze much sooner than one 1.0 x

1.0 in. ingate.¹² Early freezing ingates, top risers and cold side risers are means of achieving the condition of a solidified skin encasing the casting interior which is connected to a feed metal source under pressure head. This appears to be the closest approximation to high yield or riserless castings which can be achieved in green sand or any other dilating mold cavity.

Vertical pressure feeding requires that the freezing order in the casting proceed toward the top riser to obtain directional solidification and feeding. Feeding paths connecting the top riser must be identified. Also, feeding distance capability along vertical and horizontal paths must not be exceeded. Analysis of the casting configuration in regard to feeding path identification and feeding distance requirements is necessary if the top riser is to function predictably.

Ductile Iron Feeding Distance and Feeding Paths

Transport of feed metal into the casting occurs most favorably when temperature gradients and directional solidification result in a completely open liquid feeding path. Ductile iron presents feed metal transport difficulties because of its unusual solidification mode. First, it is very difficult to establish temperature and solidification gradients. Thermal analysis shows that liquid metal exists at the surface nearly as long as it exists in the center of a section.⁹ Gradients along a feeding path toward a riser are also very difficult to establish. Metal flow volume through a section apparently does not help to develop temperature gradients along a feeding path, but merely delays the start of solidification.⁹ Hence, favorable temperature gradients and solidification times across casting sections and along feeding paths are very difficult to develop in complex casting configurations. Further, the congealing nature of ductile iron solidification causes resistance to the movement of the liquid feed metal especially along horizontal feeding paths. Better movement would be expected along vertical feeding paths. Considering all these factors inhibiting metal transport, variability of feeding distance from risers would be expected. Short or long risers must be used, depending on the solidification time and casting geometry. The literature on the feeding distance of ductile iron seems to support this type of behavior.¹¹⁻¹⁹

Feeding Distance

Early studies report the feeding distance information listed in Table 1 and in Fig. 10 for feeding plates horizontally from side risers in green sand molds.¹¹⁻¹⁵ A more recent study agrees with

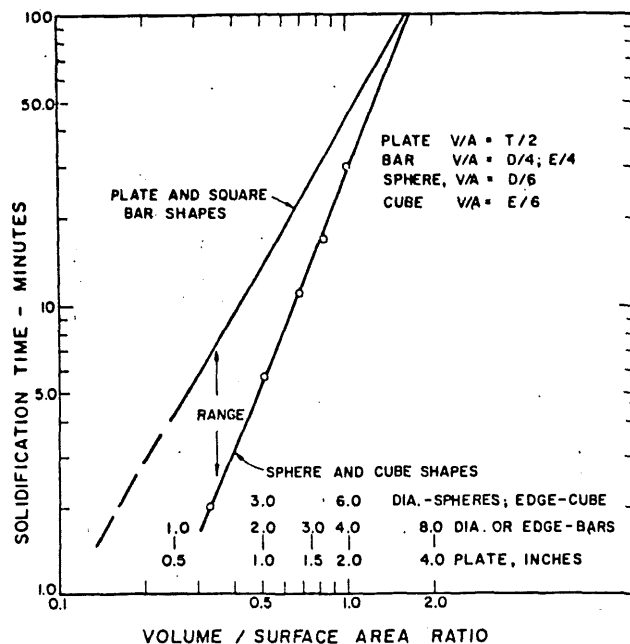


Fig. 11. Solidification time of ductile iron shapes as obtained from the literature for bar and plate shapes and from reference 27 for spheres.

fillets influence the results greatly.^{3,21-26} Freezing order and surface area/volume (SA/V) ratios (inverse of section modulus) of a variety of T-junction designs have been reported^{3,21} and other configurations have also been examined.²¹⁻²⁵ The extent to which Fig. 12 may be used to estimate feeding distance from the section modulus V/A of complex geometry feeding paths is presently not known. The method has proven effective in white

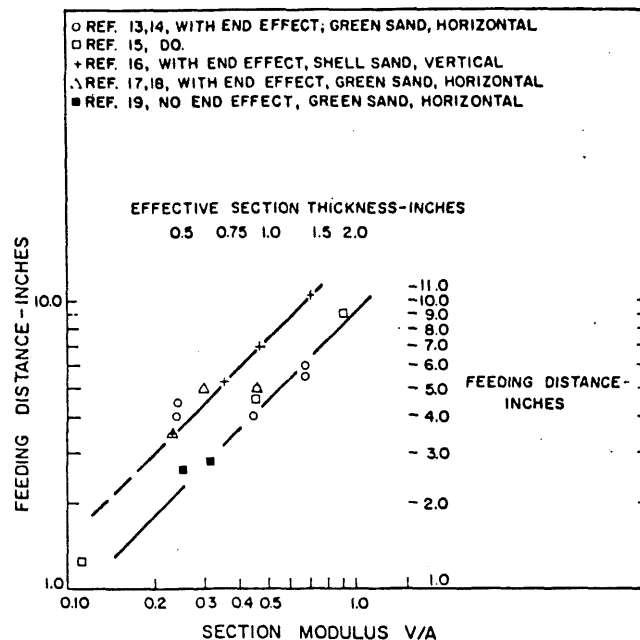


Fig. 12. Feeding distance data of Fig. 10 plotted versus section modulus.

iron castings²⁵ and needs to be investigated for ductile iron castings. Limited foundry experience suggests it to be a useful technique. To illustrate, consider a filleted T-junction of 1.0 in. plates which is found to have a V/A of 0.65. From Fig. 11, freezing time of the plates, $V/A = 0.5$, would be estimated to be about 14.0 min and that of the T-junction about 20 min. Since the T-junction freezes later it must be used as the feeding path.

Table 2. Riser Pipe Dimensions for Feeding Section (0.25 lb/in³ density; 6% feed metal)

Casting weight, Pounds	$H_p \pm D_p = 1$		$H_p \pm D_p = 1.5$		$H_p \pm D_p = 2.0$		$H_p \pm D_p = 2.5$		$H_p \pm D_p = 3.0$		$H_p \pm D_p = 4.0$	
	D_p	H_p	D_p	H_p	D_p	H_p	D_p	H_p	D_p	H_p	D_p	H_p
1.0	0.674	0.67	0.588	0.882	0.535	1.07	0.496	1.24	0.467	1.40	0.424	1.696
2.5	0.91	0.91	0.8	1.2	0.73	1.45	0.674	1.68	0.634	1.90	0.58	2.3
5	1.15	1.15	1.01	1.51	0.91	1.83	0.849	2.12	0.8	2.4	0.73	2.9
10	1.45	1.45	1.26	1.90	1.15	2.30	1.07	2.67	1.01	3.03	0.914	3.66
15	1.66	1.66	1.45	2.18	1.32	2.64	1.22	3.06	1.15	3.45	1.05	4.19
20	1.83	1.83	1.59	2.39	1.45	2.90	1.35	3.37	1.27	3.80	1.15	4.6
25	1.97	1.97	1.72	2.58	1.56	3.13	1.45	3.63	1.37	4.1	1.24	4.96
30	2.09	2.09	1.83	2.74	1.66	3.32	1.54	3.86	1.45	4.35	1.32	5.27
35	2.20	2.20	1.93	2.89	1.75	3.50	1.62	4.06	1.53	4.58	1.39	5.55
40	2.30	2.30	2.01	3.02	1.83	3.66	1.70	4.24	1.60	4.8	1.45	5.8
50	2.48	2.48	2.17	3.25	1.97	3.94	1.83	4.57	1.72	5.16	1.56	6.25
60	2.64	2.64	2.3	3.46	2.09	4.19	1.94	4.86	1.83	5.48	1.66	6.64
70	2.78	2.78	2.42	3.64	2.20	4.40	2.05	5.11	1.92	5.77	1.75	6.99
80	2.90	2.90	2.54	3.80	2.30	4.6	2.14	5.35	2.01	6.03	1.83	7.3
90	3.02	3.02	2.64	3.96	2.4	4.8	2.22	5.56	2.09	6.27	1.9	7.6
100	3.13	3.13	2.73	4.1	2.48	4.96	2.30	5.76	2.17	6.5	1.97	7.88
125	3.37	3.37	2.94	4.41	2.67	5.35	2.48	6.2	2.34	7.0	2.12	8.49
150	3.58	3.58	3.13	4.69	2.84	5.68	2.64	6.59	2.48	7.44	2.25	9.0
175	3.77	3.77	3.29	4.94	2.99	5.98	2.78	6.94	2.61	7.84	2.37	9.49
200	3.94	3.94	3.44	5.16	3.13	6.26	2.90	7.26	2.73	8.19	2.48	9.93
250	4.24	4.24	3.71	5.56	3.37	6.74	3.13	7.82	2.94	8.83	2.67	10.7
300	4.51	4.51	3.94	5.9	3.58	7.16	3.32	8.3	3.13	9.38	2.84	11.36

the earlier results.^{17,18} An end effect of 1.0 in. or more is present in the feeding distances listed in the upper part of Table 1. Feeding toward a shrinkage location where there is no end effect reduces feeding distance, as shown in the lower part of Table 1. Thus, the data suggests that feeding from a side riser in most cases amounts to only about 3.0 to 4.0 in. on sections up to 1.0 in. thickness.

Vertical feeding distance in shell molds was found to be greater than horizontal feeding in green sand. The difference is believed to be due to the pressure effect of vertical feeding rather than from the differences between a shell and a green sand mold. A similar effect is seen in green sand molds when taller side risers are used to replace shorter risers.

Sometimes, foundry experience provides specific cases of feeding distances considerably in excess of those indicated in Table 1 and Fig. 10. The effect of the feeding paths in adjacent sections and relative solidification times in the casting configuration seems to result in increased feeding distance. Increased feeding distance and improved yield might be designed if these factors could be identified and measured.

Feeding Paths

Feeding paths are identified primarily by their delayed freezing time relative to adjoining sections of a casting. The section modulus, V/A , (or alternately the surface area to volume ratio, SA/V) of adjoining sections of castings may be used to identify the relative freezing order in castings. Regions with a high section modulus freeze slowly while those with a low section modulus freeze rapidly. Freezing time of ductile iron plotted against V/A is shown in a Chvorinov log/log graph, Fig. 11. The band of freezing times shown in Fig. 11 represents the range obtained from a survey of solidification time data reported in the literature. Riser freezing time must not be earlier than that of the feeding path to which it is attached. For example, a riser attached to a 1.0 in. plate has approximately the same freezing time as a 2.0 in. diameter cylindrical riser so the riser should have a diameter sufficiently larger to provide the necessary feed metal (this is the basis of $D_R = 2W + D_P$). Obtaining the feeding path section modulus V/A thus measures the effective section thickness, W , of the feeding system through which a riser must function ($W/2 = V/A$). Since V/A determines the relative freezing time, the order of freezing in various castings can be estimated from Fig. 11. However, in complex configurations such as T, L and X junctions, bosses, ribs, etc., the value of V/A may be difficult to determine. Methods for determining V/A (or A/V) have been presented^{3,21-23} and a specific application resulting in design modification to promote feeding along feeding paths was demonstrated.²⁴ These references should be studied for the techniques. One other example will be provided later.

The shape has an important effect on solidification time at a particular V/A ratio as shown in Fig. 11. For example, a 1.0 in. infinite plate having a V/A of 0.5 has a freezing time of about 14.0 min, whereas a 3.0 in. sphere or cube with the same V/A has a freezing time of only about 6.0 min. The difference is caused by the shape effect resulting in linear heat flow from the surface of the plate and highly divergent heat flow from the sphere or cube. As the diameter of the sphere increases, its surface curvature decreases, heat flow becomes less divergent and the difference in freezing time of sphere and plate decreases. The shape effect must be carefully considered when side or top risers are positioned on chunky castings. Addition of the riser can convert a chunky casting shape to the equivalent of a bar and thereby substantially increase its freezing time.

Table 1 and Fig. 10 show that there is a certain limit to the length of a feeding path. This feeding distance can be expressed in relation to its section modulus and has been done for plates in

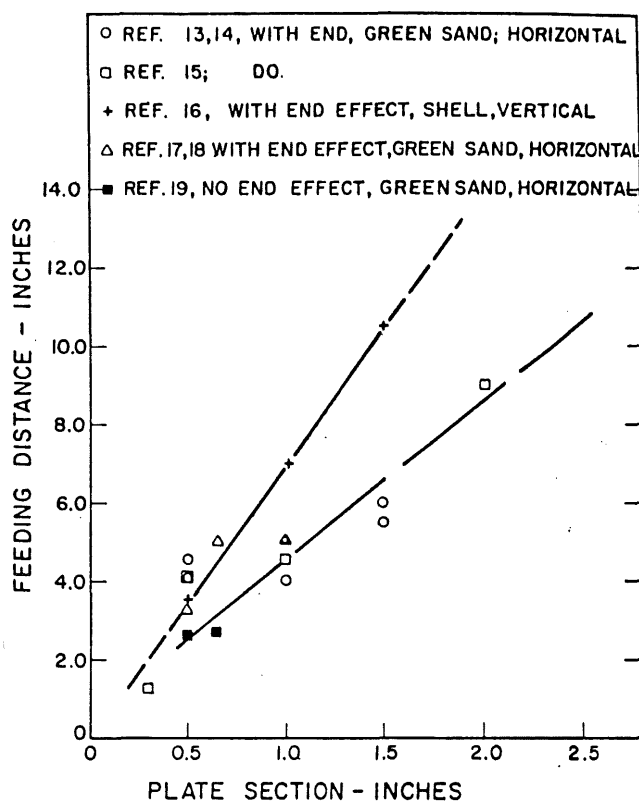


Fig. 10. Feeding distance for plate and bar shapes of ductile iron as reported in references 13-19.

Fig. 12. Feeding distance variations for both horizontal and vertical feeding paths in plates cast in green sand and shell molds are included in the scatter band of Fig. 12. Differences in the casting shape used by the investigators¹³⁻¹⁹ also account for a part of the scatter in Fig. 12. Obviously, a shorter horizontal feeding distance is more similar to vertical feeding, so that a minimum distance of 4.0 to 5.0 in., including end effect (or 2.5 to 3.5 without end effect), may be reached at a section thickness of 0.50 in., Fig. 12.

When equal plate sections are joined as in a T-section, the joint may have a section modulus 30% higher than the plates. Feeding along a junction should progress farther than feeding toward or away from a junction. This was found to be true,²⁵ but

Table 1. Feeding Distance of Ductile Iron

Plate Thickness	Feeding Distance Including End-Effect-Inches			
	Ref. 13,14 Green Sand*	Ref. 15 Green Sand*	Ref. 17, 18 Green Sand*	Ref. 16 Shell Sand**
0.25	-	1.25	-	-
0.50	4.0-4.5 in.	4.0	3.5	3.5
0.625	-	-	5.0	-
0.75	-	-	-	5.25
1.0	4.0-5.0	4.5	5.0	6.5
1.5	5.5-6.0	-	-	9.0
2.0	-	9.0	-	-

*Feeding path horizontal

**Feeding path vertical, v. 6.5T or 6.0T to 7.0T.

Plate Thickness	Feeding Distance Without End Effect	
	Ref. 13*	
0.5	2.625 inches	
0.625	2.75	

The distance which can be fed along the junction is estimated from Fig. 12 as about 8 to 9 in. vertically or 5 to 6.0 in. horizontally, whereas these same distances would be 6 to 7 or 4 to 5 in. respectively in the plates entering the junction.

Steps in Riser

Three types of riser systems have been presented. Castings which can be fed from the sprue and runner are generally limited to those molded mainly in the drag, with cross sections less than 0.5 in. thickness and with less than about 3.0 in. feeding distance between ingates. Other castings require conventional side and top risering.

Knowing the weight (or volume) of the casting and with a drawing, or better yet a model to visualize its dimensions and configurations, the riser design process involves some or all of the following:

- 1) Analyze the casting geometry to determine the freezing order of all regions so that feeding paths may be identified. The section modulus for all regions must be obtained in order to determine that the required feeding distance along connected feeding paths can be obtained.
- 2) Determine the number of risers needed to satisfy all feeding paths. Can one riser be used to connect all feeding paths? If feeding distances are exceeded along a path, more than one riser may be required on that path. Or, if two high value section modulus paths (slow freezing) are separated by a low value section modulus path (fast freezing), then a riser must be used for each high value section modulus path. Are side or top risers to be used? If top risers can be used, the single top riser method is applicable if all feeding paths can be tied to one path with the highest value section modulus.
- 3) Calculate the dimensions of the feeding section of the riser or risers by the geometric technique or estimate it from Fig. 3. When more than one riser is used, the weight of the casting to be fed is proportioned according to the region to be fed by each riser. The effective section thickness, W , is determined by the section modulus of the feeding path to which the riser is attached: $W/2 = V/A$. The choice of a taller or shorter riser is made at this time. Obviously, a top riser permits or dictates shorter risers and low H_P/D_P . When Fig. 3 is used to predict D_P , a 6% feed metal requirement is assumed. However, 2% may be sufficient or 8% may be needed depending on molding, rigging, flasks, etc. Tables 2 and 3 provide data for 2, 4, 6, and 8% requirements.
- 4) Design the pressure section, base and riser connection for side or top risers as the case may be. The riser connection must be very carefully studied to anticipate problems. Riser into a vertical wall creates a T-junction which may require an excessively large connection. Then, the riser must be far enough from the casting so that a sharp notch can be designed into the connection as described earlier. Or, a wafer core may be molded into place. In either case, the riser-casting distances must be

Table 3. Riser Pipe Dimensions for Feeding Section (0.25/in³ density; $H_P/D_P = 2.5$)

Casting Weight lbs.	2% Feed Metal		4% Feed Metal		8% Feed Metal	
	D_P in.	H_P in.	D_P in.	H_P in.	D_P in.	H_P in.
1	0.344	1.366	0.369	0.922	0.546	1.37
2.5	0.467	1.17	0.50	1.25	0.74	1.85
5	0.588	1.47	0.63	1.58	0.93	2.34
10	0.74	1.85	0.79	1.99	1.18	2.94
15	0.849	2.12	0.91	2.27	1.35	3.37
20	0.93	2.34	1.0	2.5	1.48	3.71
25	1.01	2.52	1.68	2.7	1.60	4.0
30	1.07	2.67	1.15	2.86	1.70	4.24
35	1.13	2.81	1.21	3.02	1.79	4.47
40	1.18	2.94	1.26	3.15	1.87	4.67
50	1.27	3.17	1.36	3.4	2.01	5.03
60	1.35	3.37	1.44	3.61	2.14	5.35
70	1.42	3.55	1.52	3.8	2.25	5.63
80	1.48	3.71	1.59	3.97	2.35	5.88
90	1.54	3.86	1.65	4.13	2.45	6.12
100	1.60	4.0	1.71	4.28	2.54	6.34
125	1.72	4.3	1.84	4.61	2.73	6.83
150	1.83	4.57	1.96	4.9	2.90	7.26
175	1.92	4.81	2.06	5.16	3.06	7.63
200	2.01	5.03	2.16	5.39	3.18	7.99
250	2.16	5.42	2.32	5.81	3.44	8.60
300	2.30	5.76	2.47	6.17	3.66	9.14

*See Appendix 1A for 6% feed metal D_P and H_P .

sufficient for heat to flow away from the junction so it does not freeze later than the riser. Riser connections leading directly into plate sections, whether horizontally or vertically, are preferred; avoid forming T-junction connections whenever possible.

- 5) Evaluate the riser system design to determine how gating should be attached. If top risers are used, knife gates leading directly into the casting are applied, as described earlier. With side risers, gating through the riser to promote solidification time gradients is successful unless the region in front of the connection has its solidification time extended so much by flow-through that the riser freezes off early above the base. Then, knife gating into the casting and use of a cold riser may be successful. One advantage of knife gating directly into the casting is the ability to trap dross in the runners. However, this does not prevent dross formation within the casting mold cavity.
- 6) Review the section modulus of all regions of the casting to affirm that they increase toward the main feeding path for each riser. An increase of at least 10% in V/A as feeding paths join is needed to obtain full feeding distance. Vertical feeding must be used whenever possible. If necessary, padding or design changes must be used to assure that risers can reach feeding paths for the full feeding distance.^{3,21}
- 7) Methods for ensuring piping of the risers must be applied. These include a notch at the top of the riser, a flow-off, flow-through, hot-topping, or whatever means are successful. When a riser shows no visible pipe, it may still contain one internally¹ or it may mean the sprue has served to keep the riser full long enough to freeze the top.
- 8) Casting samples of the system need to be completely searched for shrinkage. Isolated shrinkage spots denote high V/A regions not reached by feeding paths. The system then needs to be reexamined for means of connecting the shrinkage to feeding paths of adequate V/A and within feeding distance limits, whether horizontal or vertical. Surface shrinkage, pull down or draw indicates that feeding is inadequate.

- 9) Consider the problems of the risering and gating system relative to castings per riser, molding, coring, horizontal versus vertical parting, etc., and revise accordingly.
- 10) Yield of the riser system must be calculated and compared with maximum yield (described later). Riser yield may be used as a measure of the efficiency of the system.²² Because of the important effect of other process variables such as permanent versus removable flasks, fit of jackets, mold aggregate, mold density, pouring temperature, carbon equivalent and others it is imperative to cast samples using normal production equipment and techniques. This will provide a final check on the design of the risering and gating system.

An Example of Designing a Riser System

The geometric technique for riser system design will be illustrated with an analytical approach that considers several alternative solutions to a specific case. Choice of the best solution might require sample lots made each way. Consider the 27.0 lb base plate casting of Fig. 9. Dimensions are shown on the drawing. Assume X to be 10.0 in. but the effects of other dimensions on X will be considered later.

- 1) The section modulus of all regions of the casting are analyzed on Fig. 13. These may be calculated or graphically determined.^{3,18-21} Freezing order is from 1 to 4 as indicated on Fig. 9 and 13 with a range of freezing times estimated from Fig. 11 as from 8 to 18 min for the values of V/A that appear on Fig. 13. Feeding paths are identified by the freezing order as 1-3-4 and 1-2-4 from the region freezing first toward the region freezing last. Without feeding, shrinkage would most likely occur at points a, b, and c, Fig. 9.
- 2) The use of one side riser at position 4 would be desirable and a full contact would produce a highest V/A of 0.66 at the riser connection at 4, in the lower right corner of Fig. 13. Feeding might then occur along 4 to 2 and 4 to 3, Fig. 9, but unfed shrinkage might occur. The distance of 10.0 in. at a $V/A = 0.60$ does not appear feasible from Fig. 12

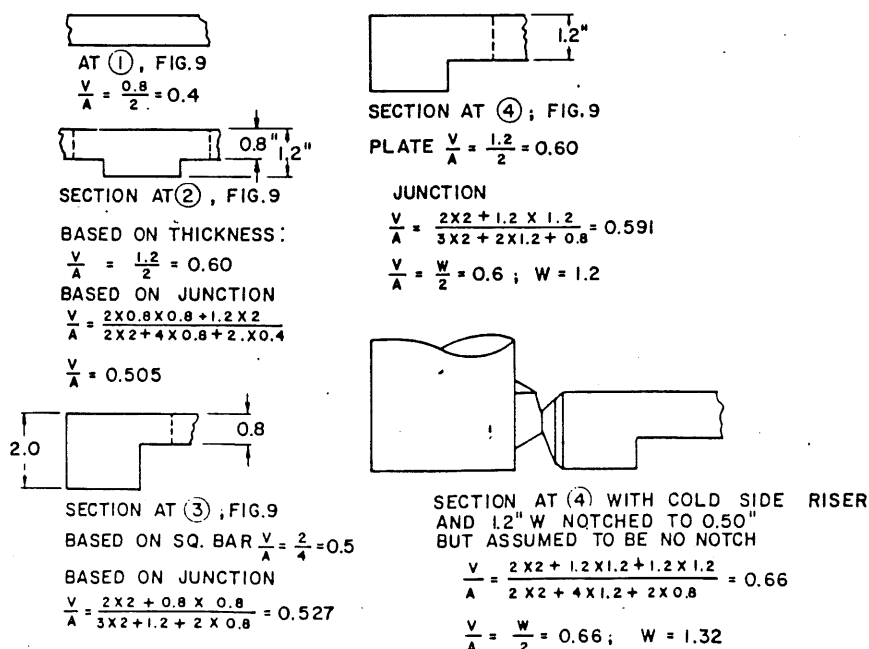


Fig. 13. Section modulus analysis of freezing in order from 1 to 4 of casting shown in Fig. 9.

unless all conditions were ideal (very unlikely). However, addition of the riser connection at 4 makes 4 the origin for feed metal from the riser and reduces the distance to be fed to 8.0 in., still a doubtful distance as seen in Fig. 13. Two side risers would reduce the feeding distance to 3.0 in. from each side from 4 and this appears feasible from Fig. 13. To use only one side riser would require padding along the path from 4 to 2. Increasing the width and/or thickness of the rib at section 4 to 2 might permit one side riser to be used. However, the amount of padding required would still leave a marginal feeding distance and present the disadvantage of using one more riser. Thus, it appears that two side risers are needed. The top risering possibility will be considered later.

- 3) Dimensions of the feeding sections of the risers are taken from Fig. 13. D_P is estimated as 1.45 in. for one riser or 1.10 in. for two risers for a 27.0 lb casting, when $H_P/D_P = 3.0$. In both cases, effective section thickness is $2 \times (V/A)$ or $2 \times 0.66 = 1.32$ in. The diameters then are $D_R = (2 \times 1.32) + 1.45 = 4.09$ in. for one side riser or $D_R = (2 \times 1.32) + 1.10 = 3.74$ in. for two side risers. The values of H_P are 4.35 in. or 3.3 in. for one or two risers respectively. The single side riser feeding section might be used as a top riser if the feeding paths are open.
- 4) The casting is made in the drag. The riser base and connections for the side risers follow the geometric techniques. The base is 1.98 in. (1.5W) below the parting, thickness of the contact is 1.32 in. (W) notched to about 0.50 in., width is 2.64 to 3.3 in. (2 to 2.5W) notched to 2.0 to 2.5 in. and the distance from the riser to the casting is 1.0 in. (1.5W), Fig. 13.

If a top riser is used, a cylindrical or square connection would be made through a wafer core with an opening of 1.0 to 1.3 D_P . Distance between the casting and the feeding section is a maximum of W and tapers toward the wafer core thickness to about 0.125 in. at the slot opening, as shown in Fig. 13.

- 5) With two side risers, gating would most likely proceed through the risers with dross being trapped in the runners to the risers. The use of a cold side riser would involve gating with three ingates from a cope runner into the side opposite the riser, as in Fig. 9. Sufficient sprue to runner to ingate feeding might occur to offset the need for the second side riser. Actual design and dimensioning of the gating system is not within the scope of this paper.
- 6) A review of section moduli of the design alternatives described above indicate that the freezing order proceeds toward the principal feeding path. Whether the casting is sound depends on the prevailing feeding distance in the marginal cases.
- 7) A sharp V-notch at the top of the riser or a flow-off or both will assist piping.² Pouring temperature and rate have a major effect on piping. The V-notch may decrease the available feed metal and is another reason for using taller risers (larger H_P/D_P) on ductile iron castings.
- 8) When sample lots are studied for soundness, the location and type of shrinkage frequently indicates the nature of the problem. Center line shrinkage usually denotes that feeding distances have been exceeded or excessive swelling has occurred.⁶ Localized shrinkage usually occurs at late freezing regions (highest V/A) which have not received feed metal because feeding paths are closed too early, feeding distance is exceeded or mold dilation is occurring while these regions are in a fragile mixed liquid solid state.
- 9) The casting of Fig. 13 could be fed in a vertically parted mold with a single top riser replacing the side riser in the horizontally parted mold. The wafer core effect could be molded into the sand in this case. Feeding distance would be maximized by the vertical path for feeding.

10) Riser yield will be calculated for two cases.

a) One side riser

$$\text{Riser yield} = \frac{\text{Volume or weight of casting} \times 100}{\text{Volume or weight of riser (includes base) + casting}}$$

$$= \frac{107.8 \times 100}{\frac{4.09^2}{2} \times \pi \times (4.35 + 1.98) + 3.3 \times 1.32 \times 1.0 + 107.8}$$

$$= 55\% \text{ riser yield}$$

The single top riser excludes the base, so yield is 56.5%.

b) Two side risers

$$\text{Riser yield} = \frac{107.8 \times 100}{2 \times \left[\frac{(3.74)^2}{2} \times \pi \times (3.3 + 1.98) + 2.64 \times 1.32 \times 1.0 \right] + 107.8}$$

$$= 46.7\% \text{ riser yield}$$

Yield could be improved by using one side riser between two castings, using the single top riser and by vertical parting. Sprue and runners are not included in the above calculation. However, it is readily evident why yields in typical jobbing ductile iron foundries are in the 38 to 55% range depending on the production mix of casting size and shape. Maximum riser yield for this casting should approach about 78% if full advantage could be taken of feeding distance as pointed out next.

Riser Yield

A guide to the percentage of yield which might be expected under the most favorable conditions may be obtained from the feeding distance information of Fig. 12. Consider the case of a single top riser feeding a circular plate at its center for the maximum feeding distance of Fig. 12. Half circles were fed in this way by early investigators.¹³⁻¹⁸ A riser yield percentage maximized in this way approaches a calculated value of 80% exclusive of sprue and runners, as indicated in Table 4. An 80% riser yield is rarely reached because the geometric configuration of castings usually prevents achievement of maximum feeding distances or requires more than one riser and because other process variables may reduce feeding distance. Sprue and runners further reduce yields. The 80% riser yield level has also been reported for white iron castings³⁶ with similar limitations. Even if the shortest feeding distance on Fig. 12 is used, riser yield is over 70%.

Overall yields of 35-55% are quite common in jobbing

Table 4. Hypothetical Yield Based on Maximum Feeding Distance of Circular Plates from Center Riser. Riser Yield Based on Feeding Section Design of Top Riser, Fig. 3

Section Thickness inches T	Feeding Distance inches FD	Circular Plate volume-weight Cu. in. - lbs.	Top Riser* weight lbs.	Maximum yield
0.5	3.75	22.1 - 5.53	1.6	77.6
0.75	5.75	77.9 - 19.5	5.2	79.
1.0	7.6	181.5 - 45.4	12.2	79.
1.5	11.1	570.2 - 142.6	49.	79.

* $D_P = 1.45$ in., $D_R = 4.09$ in. from Fig. 3; assumes 6 feed metal required.

* $H_P = 2.5 D_P$.

*Riser weight = 2.25 lbs. Cu. in. riser connection.

*Yield based on feeding section design of top riser, Fig. 3.

foundries producing small to medium green sand castings when virtually complete casting soundness is the criterion. This yield range is common to white iron, steel, and ductile iron despite their vastly different solidification modes. This supports the contention that attained yields are largely the result of mold dilation and design. This is also the reason the geometric risering technique has been successfully applied to nonferrous castings. As maximum yields are approached, the solidification mechanism may then become important.

Summary

Three techniques for risering small and medium section ductile iron castings made in green sand have been reviewed: conventional risering by the geometric technique; sprue-runner feeding of light section castings molded mainly in the drag; and single top risering (a variant of the geometric technique). Solidification time and feeding distance information related to section size and configuration as reported in the literature have been reviewed. An example of the way the geometric technique is applied to ductile iron castings was provided. It is of course based on the available information which is clearly uncertain about feeding distance possibilities, especially when horizontal and vertical feeding are compared. Despite these reservations, the techniques have been successfully applied to many ductile iron castings. However, feeding distance along T, L, and X-junctions in horizontal and vertical positions, the influence of joining and adjacent sections and the shape factor effect should be investigated in order to make riser system design completely predictable. This summary, then, presents riser design systems which can be generally applied but in which there is a degree of uncertainty of success because of a lack of certain factual information about the relationships of feeding behavior of ductile iron and geometric configurations of castings.

Successfully producing sound castings is the aim of the riser design systems described. But conditions other than the design systems can determine whether sound castings will be produced. For example, all of the mold variables that permit substantial fluctuation of the casting dimensions can contribute to unsoundness even though the feeding system is well-designed. Accurate and reproducible casting dimensions, weight and volume are influenced by process conditions such as permanent flasks versus removable flasks, clamping of permanent flasks, conditions of jackets, weights, shifting, mold and core stability, mold aggregates, etc. Close attention to these factors will produce castings of accurate dimensions, weight and volume. Lack of attention to them will produce castings that vary widely and erratically. In the latter case, porosity occurs often and the riser design is modified to suit the worst combination of conditions. Metallurgical variables such as low carbon equivalent and others can also cause frequent erratic feeding behavior and unsoundness. This paper focuses on a system for designing risers that derives from the geometry of the casting and does not deal with these other considerations.

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Design Method for Tapered Riser Feeding of Ductile Iron Castings in Green Sand

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INTRODUCTION

Principles of design for piping risers for ductile iron and other metals have been described in reference 1 and the Bibliography. However, a unique problem arises with riser designs for ductile iron. Very frequently the riser will fail to pipe and instead will shrink into the casting at the riser connection, Fig. 1, for example. Various techniques for causing the riser to pipe have been advocated in the past. This report deals with a geometric riser design system aimed at tall tapered risers that pipe and promote feed metal efficiency and improve yield.

Piping of ductile iron risers is inhibited by the congealing nature of its solidification mechanism and the resulting difficulty of establishing temperature gradients. This is especially true as the size (diameter) of the riser increases. Piping is also prevented by events resulting from the typical gating system, Fig. 2. Pressure from the sprue delays piping for a time period dependent on sprue diameter, runner cross-section, ingate and riser contacts, and flow through heating of the sand, in addition to casting configuration and section modulus. Unless piping begins soon after the mold is full, piping capability diminishes as time increases after

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pouring. Early feeding is accomplished from the sprue, which keeps the riser full. Not until the sprue itself or the connections from the runner to the riser freeze off (or become sufficiently mushy) will metal begin to move from the riser into the casting so piping can begin. Failure to pipe early can prevent the riser from feeding the casting even though it is designed adequately from a thermal and volume viewpoint.¹ Overcoming these problems is the objective of the design system for tapered riser feeding of small and medium green sand castings.

GATING

Early freezing of ingates from runners to tall risers is desired. This minimizes pressure feeding from the sprue so riser piping can begin early. Sprue pressure feeding is also minimized by making the riser as tall as possible, Fig. 3. Freezing of the ingates prevents the sprue from keeping pressure on the top of the riser, Fig. 3. Earlier freezing for a given ingate choke area can be achieved by using two ingates of approximately half the calculated cross-section for a single ingate, Fig. 3. Since freezing time of the ingates, runners and sprue is so important in promoting early riser piping, the gating design for this system will be discussed after the risering method has been presented.

RISER DESIGN

The pipe is assumed to be cylindrical, Fig. 3, as in the case of the previous piping riser system described.¹ The feed metal required is first assumed to be 2% of the casting volume.

Then, $V_f = C_w \times 4 \times 0.02 C_w = 0.08 C_w$, density = 0.25 lb per cubic inch

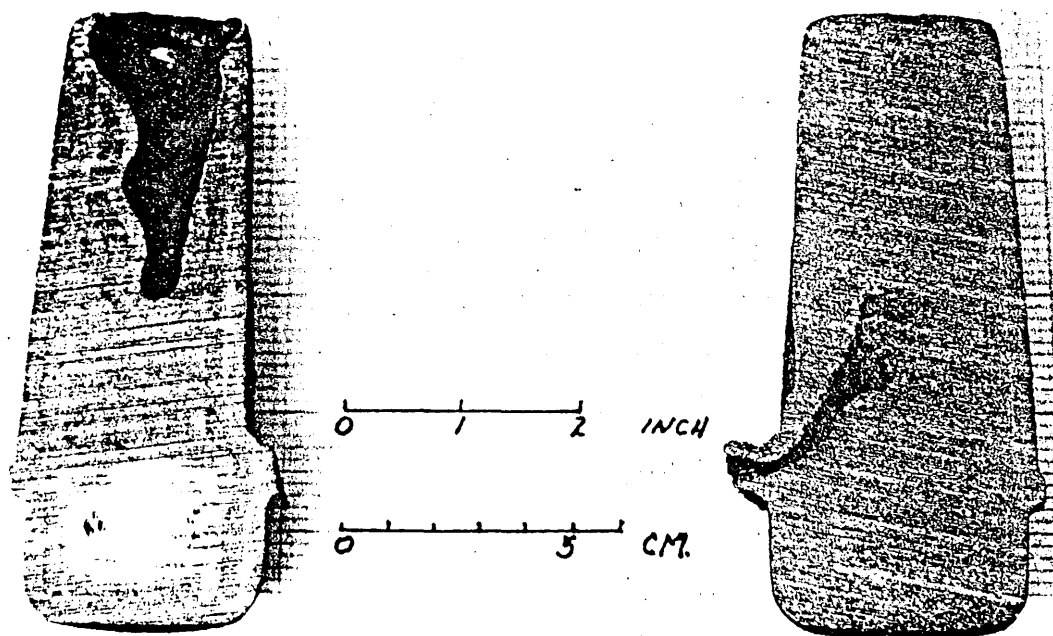


Fig. 1. Example of erratic piping in ductile iron riser. Funnel pipe produces sound casting. Absence of funnel pipe produces porosity at the riser connection and a void in the casting.

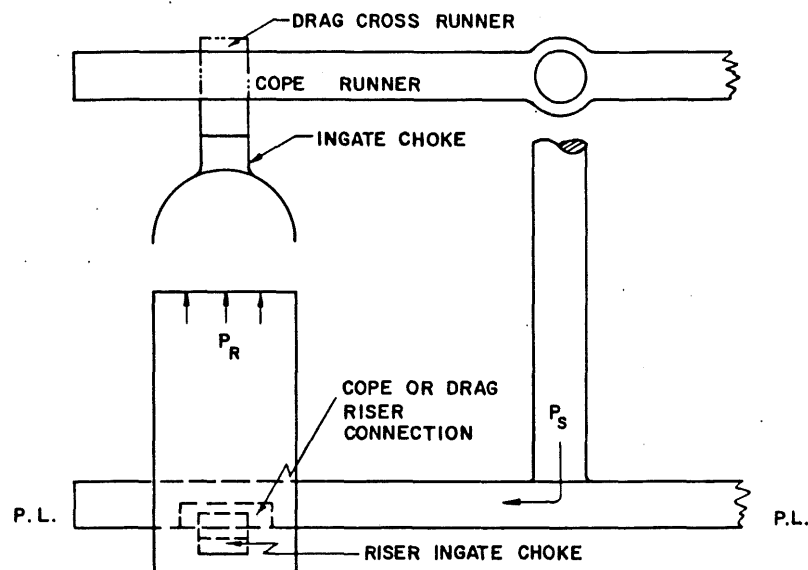


Fig. 2. Typical sprue, runner, ingate, riser geometry which delays initiation of pipe.

V_f = volume feed metal
 C_w = casting weight

Pipe volume then is $V_p = V_f = \frac{\pi D_p^2}{4} \times H_p$, D_p = diameter pipe
 H_p = height pipe

Pipe dimensions for 2% and 6% feed metal and different combinations of D_p and H_p are given in Tables 1 and 2.

For different pipe heights, $H_p = K D_p$ where K may vary from 2.5 upward. Obviously, with a 9.0 inch cope height, the pipe should be less than 8.0 inches high. Typically recommended H_p/D_p 's are in the range of 4-8. The values of D_p and H_p are listed in Tables 1 and 2 for casting weights from 1 to 200 pounds and $K = H_p/D_p$ ratios of 2.5 to 12 to 1. Choosing the H_p/D_p ratio is dependent on limiting cope height as well as casting height and weight. If the cope dictates a maximum $H_p = 6.0$ inches, then the casting weight will establish another limit. Consider for example, a 30 lb casting fed from one riser with H_p limited to 6.0 in. Table 1 suggests that ratios of 9 and 10 would not be usable because they would require an H_p greater than the 6.0 inch limit. Small castings will produce shorter risers and it becomes very important that riser chokes freeze quickly so feeding starts early. The pipe then develops at the riser top and is kept open by the thickness of skin frozen as feeding proceeds. Figure 4 provides examples of riser designs that have been piped by the design system being presented.

Riser Top

The diameter at the top of the riser must be properly chosen. Obviously, it should not be smaller than D_p . If it is too small, it will freeze off quickly. If it is smaller than the sprue, it may freeze off before the sprue and piping will not begin early. An example of the design is shown in Fig. 5 in cross section. The pipe funnel starts very early

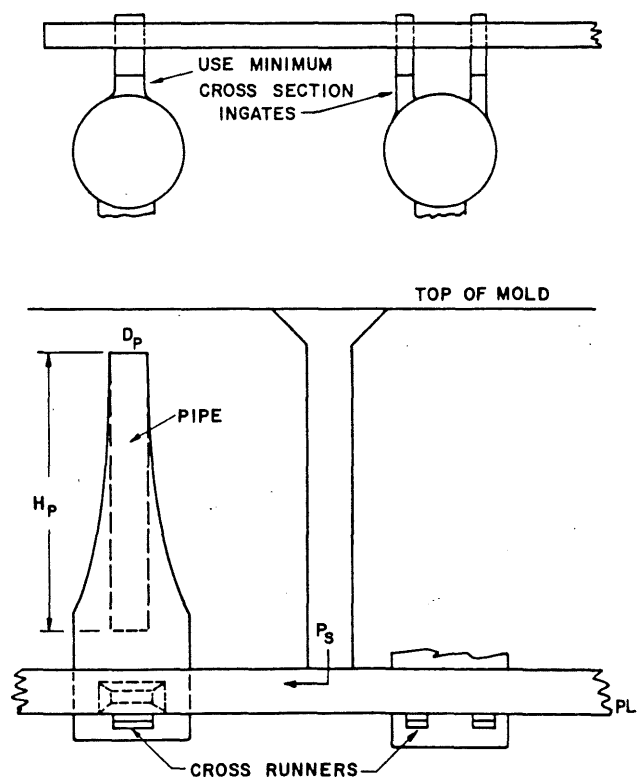


Fig. 3. Design of tall tapered riser and ingates to cause piping to begin soon after pouring.

from the initially rapid dilation of the green sand mold cavity. Keeping the pipe open as the metal level drops requires the diameter to increase gradually so that the thickness frozen does not choke off the pipe. This also preserves thermal and solidification gradients in the riser below the level of the remaining liquid in the pipe as its level drops. This is accomplished by the design of the taper of the riser.

Casting
Weight
(lb)

	$H_p/D_p = 2.5/1$		$H_p/D_p = 4/1$		$H_p/D_p = 5$		$H_p/D_p = 6$		$H_p/D_p = 7$	
	D_p	H_p	D_p	H_p	D_p	H_p	D_p	H_p	D_p	H_p
1	0.34	0.85	0.294	1.18	0.273	1.37	1.259	1.54	0.244	1.71
2	0.43	1.07	0.371	1.48	0.344	1.72	0.334	2.0	0.308	2.15
4	0.538	1.35	0.467	1.87	0.4335	2.168	0.408	2.45	0.388	2.71
6	0.62	1.55	0.535	2.14	0.496	2.48	0.467	2.8	0.444	3.11
8	0.68	1.70	0.588	2.35	0.546	2.73	0.514	3.08	0.488	3.42
10	0.735	1.84	0.634	2.54	0.588	2.94	0.554	3.32	0.526	3.68
15	0.84	2.09	0.726	2.9	0.673	3.368	0.634	3.8	0.602	4.21
20	0.93	2.31	0.8	3.2	0.741	3.707	0.698	4.19	0.663	4.64
25	0.995	2.49	0.86	3.44	0.799	3.99	0.751	4.51	0.714	5.0
30	1.05	2.64	0.914	3.66	0.849	4.243	0.799	4.79	0.756	5.31
40	1.21	3.02	1.01	4.02	0.934	4.67	0.879	5.27	0.835	5.84
50	1.31	3.27	1.08	4.33	1.01	5.03	0.947	5.68	0.899	6.3
60	1.34	3.37	1.15	4.61	1.069	5.346	1.01	6.04	0.956	6.69
70	1.40	3.50	1.21	4.85	1.126	5.628	1.06	6.36	1.01	7.04
80	1.46	3.65	1.27	5.07	1.177	5.884	1.107	6.64	1.052	7.36
90	1.52	3.80	1.32	5.27	1.224	6.119	1.152	6.91	1.094	7.66
100	1.57	3.92	1.37	5.46	1.267	6.33	1.193	7.16	1.133	7.93
120	1.68	4.20	1.45	5.8	1.347	6.735	1.268	7.61	1.204	8.43
140	1.76	4.40	1.53	6.11	1.418	7.09	1.335	8.01	1.268	8.887
160	1.80	4.50	1.6	6.39	1.483	7.4	1.396	8.37	1.325	9.28
180	1.92	4.80	1.66	6.65	1.542	7.71	1.451	8.71	1.378	9.65
200	1.99	4.98	1.72	6.88	1.597	7.986	1.503	9.02	1.428	9.99

Casting
Weight
(lb)

	$H_p/D_p = 8$		$H_p/D_p = 9$		$H_p/D_p = 10$		$H_p/D_p = 11$		$H_p/D_p = 12$	
	D_p	H_p	D_p	H_p	D_p	H_p	D_p	H_p	D_p	H_p
1	0.233	1.87	0.224	2.02	0.217	2.17	0.21	2.31	0.2	2.45
2	0.294	2.35	0.283	2.55	0.273	2.73	0.265	2.91	0.257	3.08
4	0.371	2.97	0.356	3.21	0.344	3.44	0.333	3.67	0.324	3.89
6	0.424	3.39	0.408	3.67	0.394	3.94	0.382	4.20	0.371	4.45
8	0.467	3.74	0.449	4.04	0.434	4.34	0.42	4.62	0.408	4.9
10	0.503	4.02	0.484	4.35	0.464	4.64	0.45	4.98	0.439	5.27
15	0.576	4.61	0.554	4.98	0.535	5.35	0.518	5.7	0.503	6.04
20	0.634	5.07	0.609	5.48	0.588	5.88	0.57	6.27	0.554	6.64
25	0.683	5.46	0.657	5.91	0.634	6.34	0.614	6.75	0.596	7.16
30	0.726	5.80	0.698	6.28	0.673	6.73	0.65	7.18	0.634	7.6
40	0.799	6.39	0.768	6.91	0.741	7.41	0.72	7.9	0.697	8.37
50	0.860	6.88	0.827	7.44	0.799	7.99	0.77	8.5	0.751	9.02
60	0.914	7.31	0.879	7.91	0.849	8.49	0.82	9.05	0.8	9.6
70	0.962	7.70	0.925	8.33	0.893	8.93	0.866	9.52	0.841	10.1
80	1.01	8.04	0.967	8.71	0.934	9.34	0.905	9.95	0.879	10.55
90	1.046	8.37	1.006	9.05	0.971	9.71	0.94	10.4	0.914	10.97
100	1.084	8.67	1.042	9.38	1.006	10.1	0.97	10.7	0.947	11.4
120	1.152	9.21	1.107	9.97	1.069	10.69				
140	1.212	9.70	1.166	10.49	1.126	11.26				
160	1.268	10.14	1.219	10.97	1.177	11.77				
180	1.318	10.55	1.268	11.41	1.224	12.24				
200	1.365	10.92	1.313	11.82	1.268	12.68				

Casting weight, Pounds	$H_p \div D_p = 1$		$H_p \div D_p = 1.5$		$H_p \div D_p = 2.0$		$H_p \div D_p = 2.5$		$H_p \div D_p = 3.0$		$H_p \div D_p = 4.0$	
	D_p	H_p	D_p	H_p	D_p	H_p	D_p	H_p	D_p	H_p	D_p	H_p
1.0	0.674	0.67	0.588	0.882	0.535	1.07	0.496	1.24	0.467	1.40	0.424	1.69
2.5	0.91	0.91	0.8	1.2	0.73	1.45	0.674	1.68	0.634	1.90	0.58	2.32
5	1.15	1.15	1.01	1.51	0.91	1.83	0.849	2.12	0.8	2.4	0.73	2.92
10	1.45	1.45	1.26	1.90	1.15	2.30	1.07	2.67	1.01	3.03	0.914	3.66
15	1.66	1.66	1.45	2.18	1.32	2.64	1.22	3.06	1.15	3.45	1.05	4.20
20	1.83	1.83	1.59	2.39	1.45	2.90	1.35	3.37	1.27	3.80	1.15	4.60
25	1.97	1.97	1.72	2.58	1.56	3.13	1.45	3.63	1.37	4.1	1.24	4.96
30	2.09	2.09	1.83	2.74	1.66	3.32	1.54	3.86	1.45	4.35	1.32	5.28
35	2.20	2.20	1.93	2.89	1.75	3.50	1.62	4.06	1.53	4.58	1.39	5.56
40	2.30	2.30	2.01	3.02	1.83	3.66	1.70	4.24	1.60	4.8	1.45	5.80
50	2.48	2.48	2.17	3.25	1.97	3.94	1.83	4.57	1.72	5.16	1.56	6.24
60	2.64	2.64	2.3	3.46	2.09	4.19	1.94	4.86	1.83	5.48	1.66	6.64
70	2.78	2.78	2.42	3.64	2.20	4.40	2.05	5.11	1.92	5.77	1.75	6.99
80	2.90	2.90	2.54	3.80	2.30	4.6	2.14	5.35	2.01	6.03	1.83	7.32
90	3.02	3.02	2.64	3.96	2.4	4.8	2.22	5.56	2.09	6.27	1.9	7.60
100	3.13	3.13	2.73	4.1	2.48	4.96	2.30	5.76	2.17	6.5	1.97	7.89
125	3.37	3.37	2.94	4.41	2.67	5.35	2.48	6.2	2.34	7.0	2.12	8.49
150	3.58	3.58	3.13	4.69	2.84	5.68	2.64	6.59	2.48	7.44	2.25	9.0
175	3.77	3.77	3.29	4.94	2.99	5.98	2.78	6.94	2.61	7.84	2.37	9.49
200	3.94	3.94	3.44	5.16	3.13	6.26	2.90	7.26	2.73	8.19	2.48	9.93
250	4.24	4.24	3.71	5.56	3.37	6.74	3.13	7.82	2.94	8.83	2.67	10.68
300	4.51	4.51	3.94	5.9	3.58	7.16	3.32	8.3	3.13	9.38	2.84	11.36

Table 2. Riser Pipe Dimensions for Feeding Section
(0.25 lb per cu in density, $H_p \div D_p = 2.5$)

Casting Weight lbs.	2% Feed Metal		4% Feed Metal		8% Feed Metal	
	D_p in.	H_p in.	D_p in.	H_p in.	D_p in.	H_p in.
1	0.344	1.366	0.369	0.922	0.546	1.37
2.5	0.467	1.17	0.50	1.25	0.74	1.85
5	0.588	1.47	0.63	1.58	0.93	2.34
10	0.74	1.85	0.79	1.99	1.18	2.94
15	0.849	2.12	0.91	2.27	1.35	3.37
20	0.93	2.34	1.0	2.5	1.48	3.71
25	1.01	2.52	1.68	2.7	1.60	4.0
30	1.07	2.67	1.15	2.86	1.70	4.24
35	1.13	2.81	1.21	3.02	1.79	4.47
40	1.18	2.94	1.26	3.15	1.87	4.67
50	1.27	3.17	1.36	3.4	2.01	5.03
60	1.35	3.37	1.44	3.61	2.14	5.35
70	1.42	3.55	1.52	3.8	2.25	5.63
80	1.48	3.71	1.59	3.97	2.35	5.88
90	1.54	3.86	1.65	4.13	2.45	6.12
100	1.60	4.0	1.71	4.28	2.54	6.34
125	1.72	4.3	1.84	4.61	2.73	6.83
150	1.83	4.57	1.96	4.9	2.90	7.26
175	1.92	4.81	2.06	5.16	3.06	7.63
200	2.01	5.03	2.16	5.39	3.18	7.99
250	2.16	5.42	2.32	5.81	3.44	8.60
300	2.30	5.76	2.47	6.17	3.66	9.14

Casting Weight lb	$H_p/D_p = 5/1$		$H_p/D_p = 6/1$		$H_p/D_p = 7/1$		$H_p/D_p = 8/1$		$H_p/D_p = 9/1$		$H_p/D_p = 10/1$	
	D_p	H_p	D_p	H_p	D_p	H_p	D_p	H_p	D_p	H_p	D_p	H_p
1	.394	1.97	.371	2.23	.352	2.46	.337	2.69	.324	2.91	.313	3.13
2	.496	2.48	.467	2.80	.444	3.11	.424	3.39	.408	3.67	.393	3.93
4	.625	3.126	.588	3.53	.559	3.91	.534	4.28	.514	4.63	.496	4.96
6	.716	3.579	.674	4.04	.639	4.48	.612	4.90	.588	5.29	.568	5.68
8	.788	3.939	.741	4.45	.704	4.93	.674	5.39	.648	5.83	.625	6.25
10	.849	4.243	.799	4.79	.759	5.31	.726	5.80	.698	6.28	.674	6.74
15	.971	4.857	.914	5.48	.868	6.08	.830	6.64	.799	7.19	.771	7.71
20	1.069	5.346	1.01	6.04	.956	6.69	.914	7.31	.879	7.91	.848	8.48
25	1.152	5.759	1.08	6.50	1.03	7.21	.985	7.88	.947	8.52	.914	9.14
30	1.224	6.12	1.15	6.91	1.09	7.66	1.05	8.37	1.01	9.05	.971	9.71
40	1.347	6.736	1.27	7.61	1.20	8.43	1.15	9.21	1.11	9.97	1.069	10.69
50	1.451	7.256	1.37	8.19	1.30	9.08	1.24	9.92	1.19	10.74	1.15	11.50
60	1.542	7.71	1.45	8.71	1.38	9.65	1.32	10.55	1.27	11.41	1.22	12.2
70	1.623	8.117	1.53	9.16	1.45	10.16	1.39	11.10	1.33	12.01	1.29	12.90
80	1.697	8.487	1.60	9.59	1.52	10.62	1.45	11.61	1.40	12.56	1.35	13.50
90	1.765	8.826	1.66	9.97	1.58	11.04	1.51	12.07	1.45	13.06	1.40	14.0
100	1.828	9.14	1.72	10.32	1.63	11.44	1.56	12.51	1.50	14.37	1.45	14.5
120	1.943	9.714	1.83	10.97	1.74	12.16	1.66	13.3	1.60	14.37	1.54	15.4
140	2.045	10.227	1.93	11.55	1.83	12.79	1.75	13.99	1.68	15.13	1.62	16.2
160	2.138	10.692	2.01	12.08	1.91	13.38	1.83	14.63	1.76	15.82	1.70	17.0
180	2.224	11.12	2.09	12.56	1.99	13.92	1.90	15.21	1.83	16.45	1.77	17.7
200	2.304	11.518	2.17	13.01	2.06	14.42	1.97	15.76	1.89	17.04	1.83	18.3

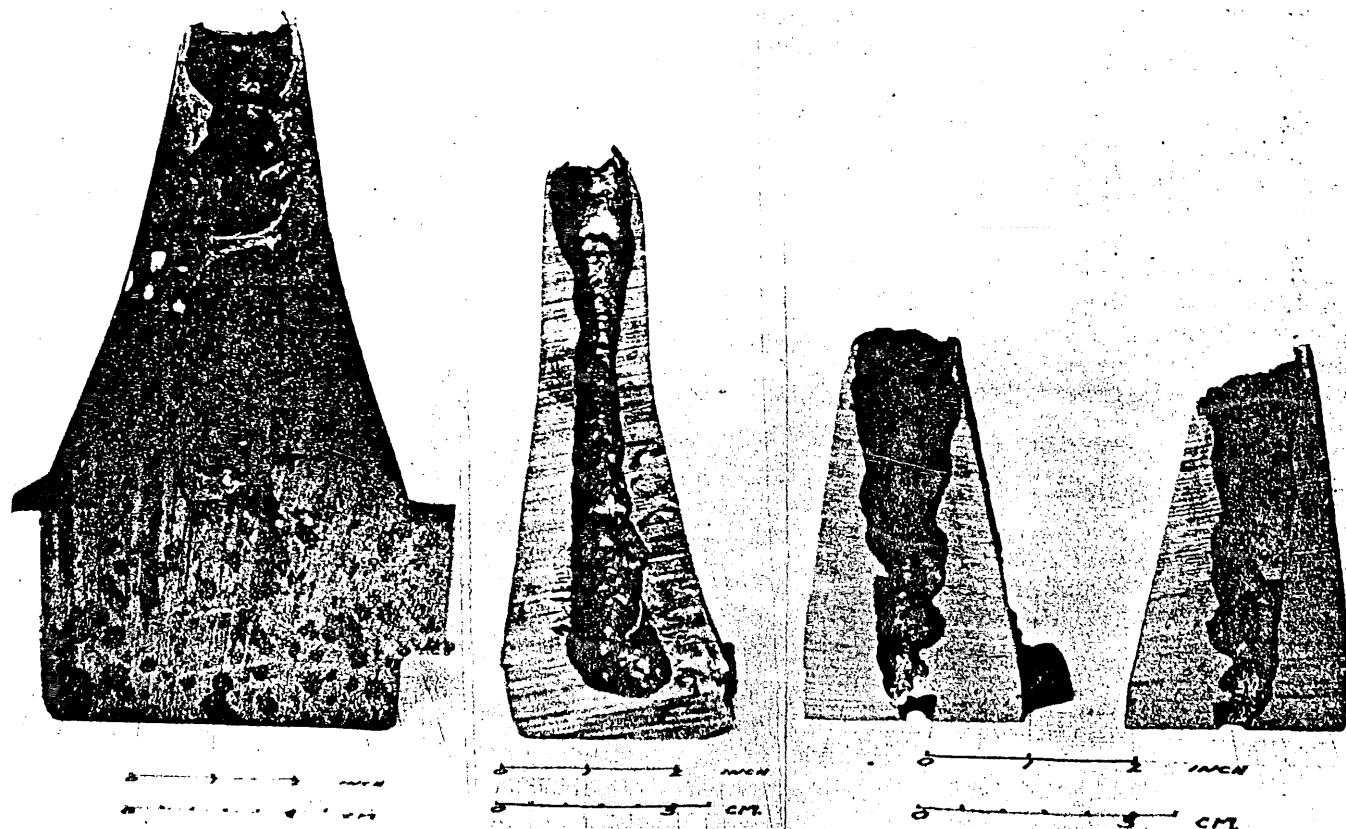


Fig. 4. Examples of tapered risers designed to develop a controlled pipe.

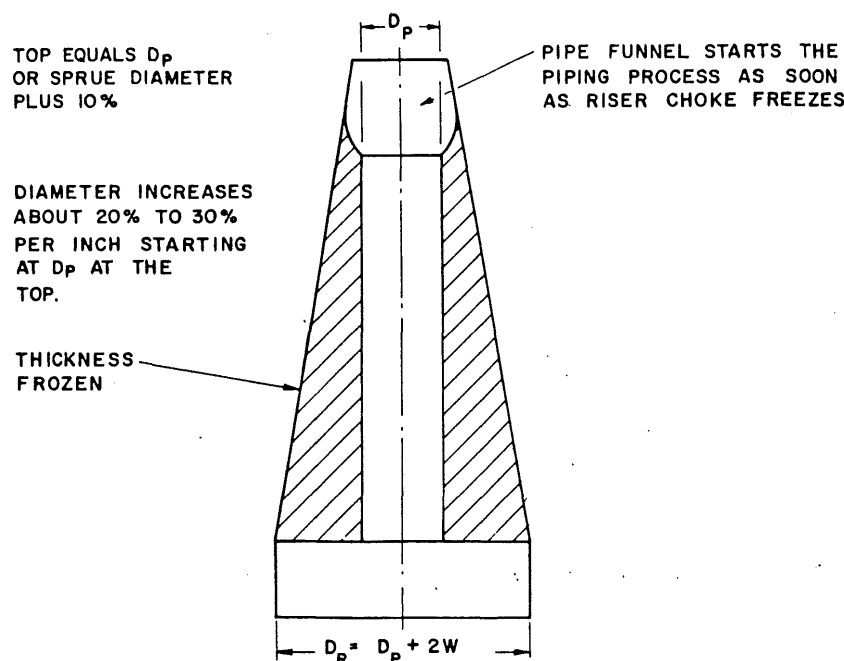


Fig. 5. Examples of design system for tapered tall risers.

Taper

The objective of designing the tapered riser is to retain the pipe cavity as the liquid metal level in it drops and the thickness frozen increases. The limits of thickness frozen at the top and bottom of the riser's feeding section are the simplest to consider first. At the top of the pipe, thickness frozen is nearly zero if piping begins immediately, Figs. 3-5. The riser diameter at the top may be at least as much as D_p but not so small as to freeze before the sprue, say 10% larger than the sprue diameter. At the bottom of the pipe, the thickness frozen must be nearly equal to the effective section thickness, W , of the casting section fed. Thus, the riser diameter at the bottom of the pipe is $D_R = 2W + D_p$, just as it was defined for the geometric risering design technique described in Ref. 1. Taper, then, is simply to connect the diameter at the riser top to D_R at the bottom of the pipe. This provides the simplest design method for obtaining a tapered riser which might have the necessary thermal wall and pipe volume that will feed the casting.

As an example, consider a 50 lb slab casting of dimen-

sions $2 \times 10 \times 10$ inch = 200 cu. in. volume, $W = 2.0$ inch. Assume it will be fed from one side riser and require 2% feed metal, D_R . Table 1 lists the possibilities (see data at bottom of page):

Tapered risers showing the extremes of H_p and D_R from the above possibilities are shown in Fig. 6. Obviously, the short riser will have higher yield. However, it may not be tall enough to feed parts of the casting in the cope. More importantly, both pressure head and solidification gradients for promoting piping may not prevail. Piping may begin too late or near the riser contact as shown earlier, Fig. 1. The taller risers are less efficient in yield but more effective in promoting feeding by piping. They are also effective in preventing piping at the riser contact and into the casting. In order to get the pipe started soon after pouring, feeding from the sprue must be cut short by freezing off the ingates from the runner to the riser. The top will then begin to pipe. To continue the piping process, an adequate thermal wall thickness frozen must be provided by the taper on the riser. The maximum thermal wall or thickness frozen is

H_p/D_p	H_p , in.	D_p , in.	D_R $2W + D_p$, in.	Vol_R , cu. in.	Riser Yield, %
2.5	3.27 in.	1.35 in.	5.31 in.	28.14 cu. in.	88.6%
4.0	4.33	1.08	5.08		
5.0	5.03	1.01	5.01		
6.0	5.68	.947	4.947	38.74	83.8
7.0	6.30	.899	4.899		
8.0	6.88	.860	4.86		
9.0	7.44	.827	4.83		
10.0	7.99	.799	4.80		
11.0	8.50	.77	4.77		
12.0	9.02	.751	4.75	53.6	78.9

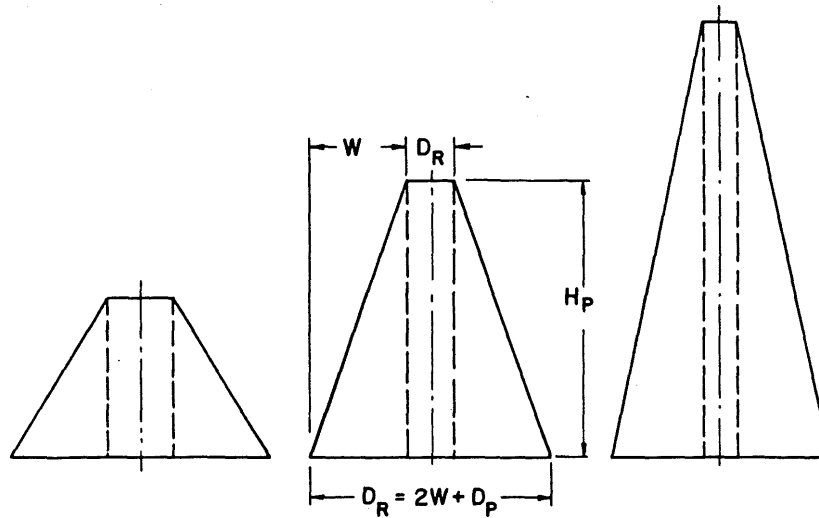


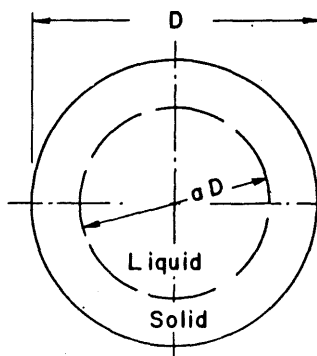
Fig. 6. Comparison of tapered risers with $H_p \div D_p$ ratio varying from 2.5 to 12. Frozen thickness to maintain the pipe is discussed in the text.

needed near the bottom of the pipe where feeding and solidification times are the longest. There it is equal to the effective section thickness W of the casting location being fed. Thus, the diameter of the bottom of the feeding section of the riser is $D_R = 2W + D_p$, as described before.¹ Choosing the correct taper from the bottom upward among the possibilities shown by the example in Fig. 6 is based on the thermal wall provided by the thickness frozen.

Thickness Frozen

The thermal wall or thickness frozen that keeps the pipe open must become thicker at time increases during the feeding period, Fig. 3-5. Thickness frozen follows the general equation $TF = c\sqrt{t} + b$, where TF = thickness fro-

zen, t = time, and c and b are constants. Thickness frozen may be calculated in several ways or empirically determined from sectioned tapered type risers such as those shown in Fig. 4. Geometric analysis can be approached via the section modulus, V/A , and Chvorinov's rule, $\log t \sim \log(V/A)^2$. The V/A of thickness frozen can be calculated from $V/A = D(1-a^2)/4$, Fig. 7. Thickness frozen, $TF = (D - aD) \div 2$, Fig. 7. From these equations, graphs of thickness frozen vs. section modulus for various riser diameters from 1.0 to 6.0 inches can be constructed, as in Fig. 8. If the relationship between solidification times and V/A is recalled, Fig. 8 indicates that at $V/A = 0.5$, a 2.0 in. D riser might be solidified to the center, a 3.0 in. D riser could have a thickness frozen of about 0.32 in., and a 6.0 in. D riser would have a thickness frozen of about 0.28 inches.



Eqn. 1- Thickness Frozen, $TF = \frac{D-aD}{2}$
 Section Modulus of $TF = \frac{V}{A} = \left(\frac{\pi D^2}{4} - \frac{\pi a^2 D^2}{4} \right) \div \pi D$

Eqn. 2- $\frac{V}{A} = \frac{D(1-a^2)}{4}$

Fig. 7. Calculation of section modulus and thickness frozen relationships.

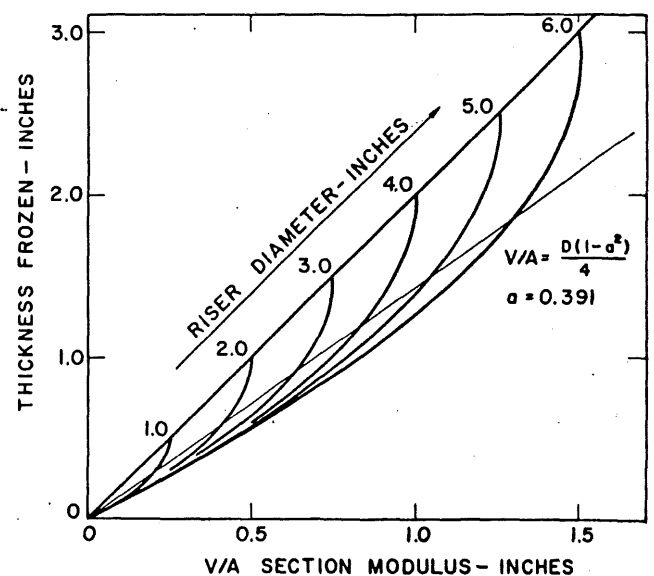


Fig. 8. Graphical relationship of thickness frozen, section modulus, and riser diameters.

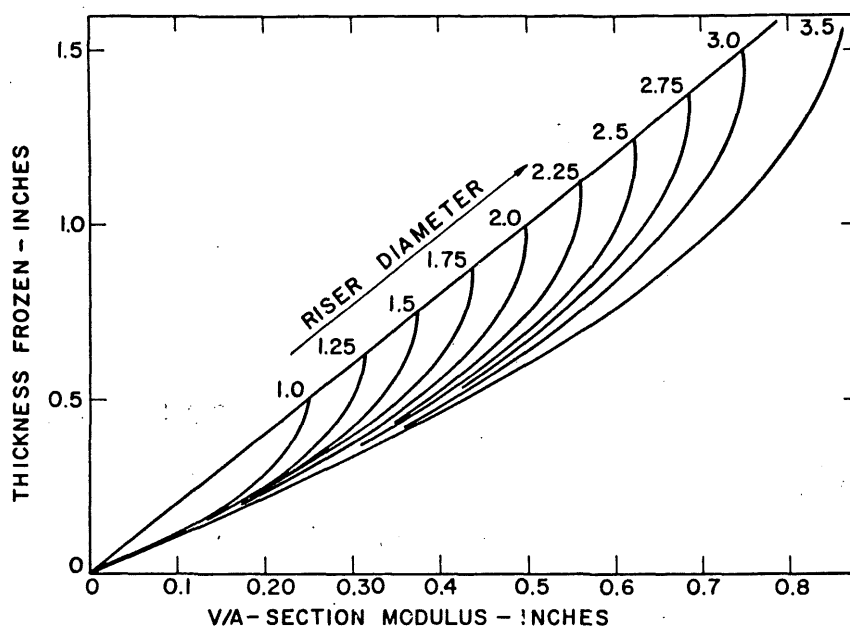


Fig. 8 (continued)

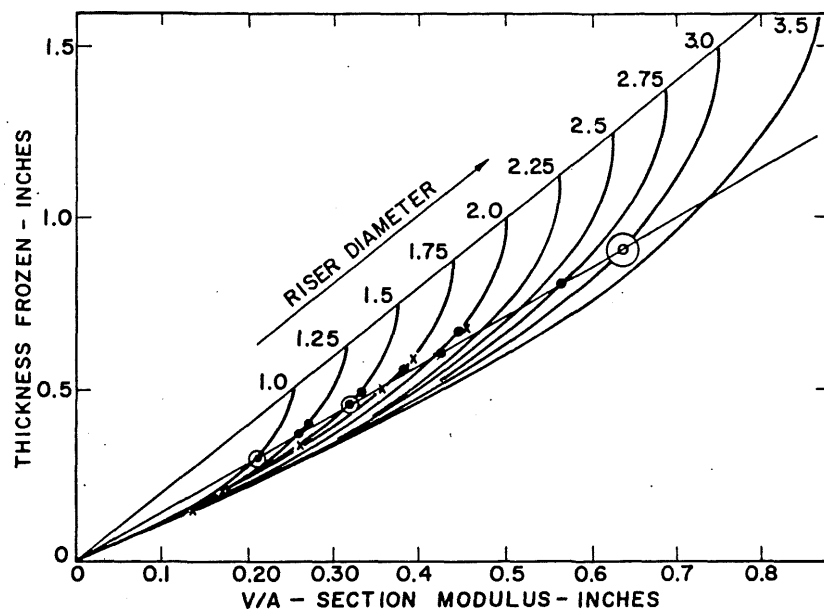


Fig. 9. Comparison of thickness frozen and riser diameter of tapered risers actually produced in the foundry with the graphical relationship of thickness frozen, section modulus, and riser diameter from 1.0 to 3.0 inches.

The extent to which actual tapered risers behave as suggested by Fig. 8 can be measured from a number of sectioned risers such as the one shown in Fig. 4. Figure 9 shows a plot of such data for sections of tapered risers in the 1.0 to 3.0 inch D_R range. The data plots near a TF vs. V/A where $a = 0.391$ in $V/A = D(1 - a^2) \div 4$, as shown in Fig. 9. This same approximation extends to the 6.0 in. D riser. Figures 8 and 9 can be used to estimate whether the pipe can be expected to develop nearly as designed. However, it must be recognized that taper from top to bottom, Fig. 6, greatly influences the thickness frozen. A rapid increase in the tapered diameter means that adjacent dia-

eters will change their respective section modulus so that the equations do not hold. They are based on a single cylinder diameter. This effect can be illustrated in the case of the example of Fig. 6. At $D_R = 3.0$ inches, equations 1 and 2 give TF = 0.914 inches and $V/A = 0.635$, respectively, the point encircled on Fig. 9. However, in the case of the three risers shown, the pipe requires that TF is 1.13, 1.03, and 0.85 inches for the tallest, the intermediate and shortest risers, respectively. Reference to Fig. 9 indicates that 1.13 inches at $D_R = 3.0$ suggests that the riser is freezing off to the center. Thus, relative to the empirical data, the tallest riser might freeze across the pipe. The shortest

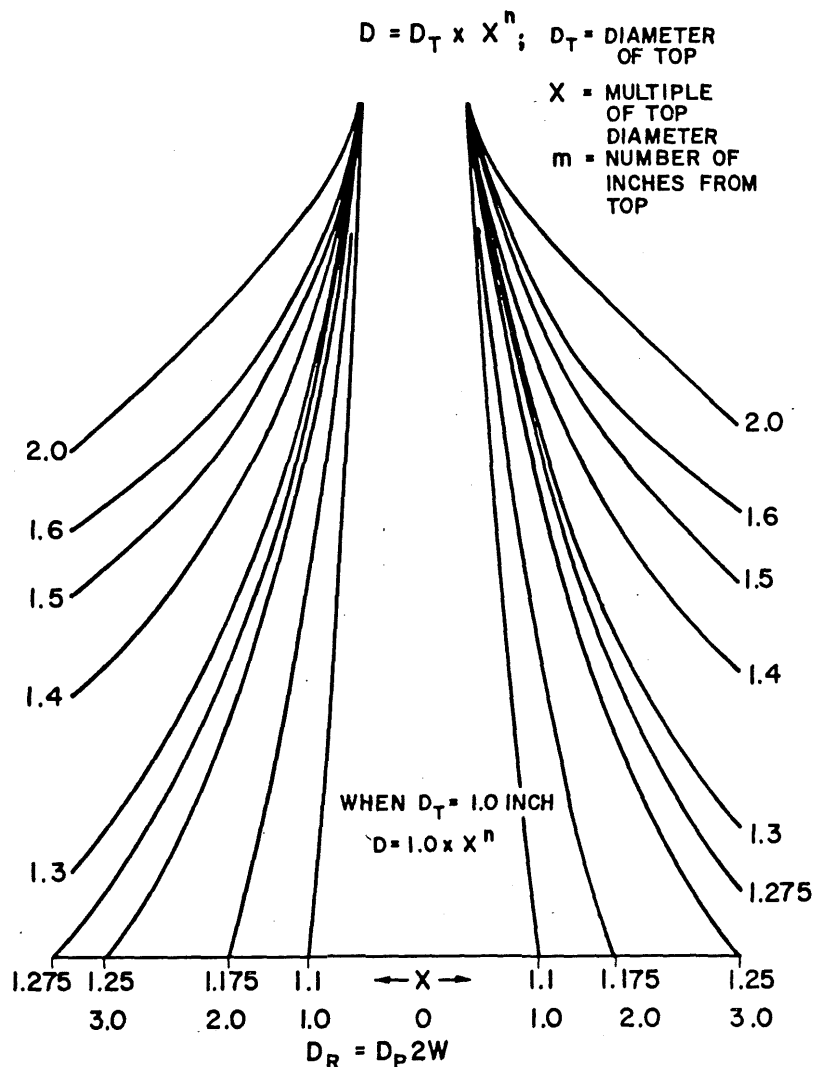


Fig. 10. Concave curvature forms for risers increasing by $D = D_T \times X^n$.

might not maintain a pipe with adequate head for pressure feeding and to prevent piping at the riser contact into the casting as indicated by the position of 0.85 inches on $D_R = 3.0$ in Fig. 9. The taper for maintaining the TF to preserve the piping action can thus be estimated from Figs. 8 and 9 and equations 1 and 2. Experience to this time suggests that taper where D_R increases by 1.1 to 1.3 D_R per inch of riser height top to bottom is successful. However, these are empirical relationships for the case histories studied thus far. It needs to be verified for the conditions peculiar to the foundry operation. For example, the effect of metallurgical and mold variables on the thickness frozen has not been studied. Sectioning of risers to measure D_p and TF provides the data for any necessary changes.

Curved Taper

A concave curved taper should be more efficient in yield than the straight taper discussed earlier and illustrated in Fig. 5 and 6. This is predicted from the relationship $TF = c\sqrt{t} + b$ cited earlier. If the diameter is increased by a fixed percentage per inch of height from the top downward, a curved taper will result. The shape is described by $D = D_T \times X^n$, where D_T is the diameter at the top selected as stated

before, X is the multiplier indicating the percentage increase per inch of height down from the top, and n is the location down from the top in inches. For example, if the $D_T = 1.0$ inches; and $X = 1.2$, an increase of 20% per inch; and $n = 1, 2, 3, 4$, etc. then the riser diameter will change as follows:

$D_T = 1$
 $D = 1.2$ at 1.0 inch below top
 $D = 1.44$ at 2.0 inch below top
 $D = 1.728$ at 3.0 inch below top
 etc.

Concave curvature of the riser exterior for values of X from 1.1 to 2.0 are shown in Fig. 10. Taper of this kind can be applied most conveniently by matching the calculated D_R and top diameter to the curvatures in Fig. 10, providing that D_p is adequately considered in the top diameter.

Converting Existing Risers

A simple way to convert risers that are behaving erratically because they are too short or inadequate is to add a post onto the existing riser. The post may start with the existing D_R and be reduced to a minimum at the top. The top diameter and total height is chosen as described earlier.

However, the ingate to the riser must be choked to freeze quickly so that piping will begin and continue.

FEED METAL REQUIREMENT

Feed metal requirement may vary from 2% to 6% or more in green sand depending on process variables. Dilation of the mold cavity is a major reason feeding is required. Since dilation and other process variables influence feed metal requirements, the successful application of tapered risers for prevention of internal porosity means that feed metal requirement be known for the particular foundry. It can be determined by measuring feed metal volume in risers that have piped satisfactorily.¹ Ordinarily, it is in the range of 2% to 6% for ductile iron made in high density green sand molds, Tables 1 and 2. Castings with sections under about 0.75 in. will usually be fed satisfactorily with 2.0% feed metal. Over 1.0 in. section thickness, feed metal of 6.0% may be required, especially in molds not of adequate density.

RISER BASE AND CONNECTION

Design of the riser base and riser connection to the casting has been described.¹ Reference should be made to that paper for the recommended design approach. However, the riser contact is a big factor in the incidence of shrink on the casting side of the contact. Too small a contact will still permit shrink to occur in the casting just inside the contact area.

Excessive metal flow through the contact can also lead to shrink at the contact on the casting side. When this occurs and the riser is piping well, it can be prevented by decreasing flow-through. As much as 70% of the metal can be introduced into the casting directly by knife gates from the runner, the balance being allowed to flow through the riser and fill the casting. This prevents a hot spot from developing at the riser contact that freezes more slowly than the riser.

FEEDING PATH AND FEEDING DISTANCE

Consideration of these questions are presented in reference 1.

GATING

Fast freezing of ingates to risers has been emphasized as the means of causing piping to begin soon after pouring is completed. Ingates to risers then become the choke. The total ingate area is calculated first, using the commonly accepted formula:

$$A = \frac{w}{t} \times \frac{1}{K \times d \times \sqrt{2gH}} \quad (1)$$

where: A = area of choke, sq. in.
 w = weight poured; lb or kg
 t = pouring time in seconds
 d = metal density; pounds per cubic in. or kg. per cubic cm, 0.25 cu. in. is used.
 g = gravity constant
 H = effective height of sprue or vertical distance from choke area to top of cope
 K = efficiency coefficient of the choke area

Many simplifying assumptions are usually made when the formula is used. For example, the value of the pouring time t, which is used to select the pouring rate, w/t, is thought to have an optimum value which can be calculated for molten iron by the method summarized in ref. 2. Or, the molding machine may require a standard molding pouring time, say t = 10 sec. In either case, the pouring time must be known to proceed with the calculation. The efficiency coefficient, K, is arbitrarily chosen between about 0.4 and 0.8 for most systems, whereas it might be calculated for a specific design depending on the kind and number of channels, changes in direction, friction losses, etc., ref. 2. Further, a constant value of H is chosen even though it changes during the period of pouring. Also, it is assumed the gating system is full of fluid. In spite of these and other limitations, the simplified equation is very useful as a starting point. When objectives other than control of flow rate are introduced into the gating system design, an even more empirical design method may make it unsuitable to refine the formula further. In this system, improved piping and feeding from ductile iron is the chief aim of the gating system. Design of gating systems will be discussed from that viewpoint only.

Design for Flow Rate Control into Riser

Consider choke area A in formula 1 for molten iron of d = 0.25 lb per cubic in. and K = 0.46. Then,

$$A = \frac{w}{t} \times \frac{1}{0.46} \times \frac{1}{\sqrt{H}} \times \frac{1}{0.25\sqrt{2 \times 64.4 \times 12}}$$

$$A = \frac{0.313}{\sqrt{H}} \times \frac{w}{t}; \text{sq. inches}$$

The relationship of pouring rate, $\frac{w}{t}$, to A for H = 6, 9, and 12 inches is shown graphically, Fig. 11. Note that the location of the choke is not stated by the formula. Neither are the linear dimensions of A described. Its cross section could be square, rectangular, or circular. Equivalent sides of a rectangle having an area equal to a square cross section are expressed by $A = na^2$, where n is the ratio of the long

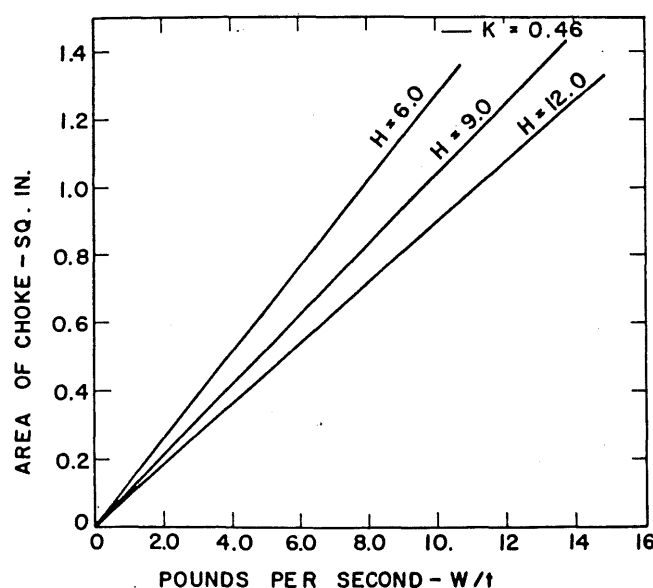


Fig. 11. Relationship of ingate area and sprue height on pounds iron poured per second when K = 0.46.

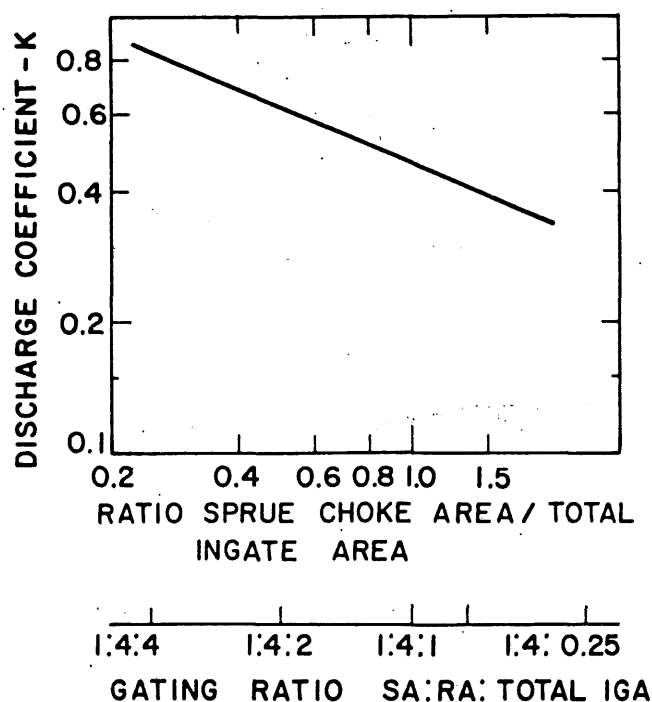


Fig. 12. Effect of gating ratio on K.

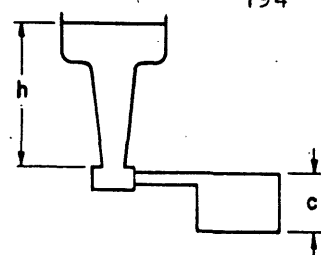
side to the short side and a is the short edge of the rectangle. Not until n is greater than about 4 will the section modulus, V/A , of the rectangular cross section be smaller than that of a square cross section of the same area. Hence, for fast freezing the choke area may have a width more than 4 times its thickness. Obviously, the choke area for an ingate can be divided into several ingates to promote fast freezing. As more total surface area is added to the ingate, K becomes larger, and the total choke area may need to be increased if pouring time becomes excessive.

Sprue and Runners

When more than one riser is used, the total ingate area is divided among the risers. The sprue is made larger in cross-section than the total ingate area at the risers by 20-30%. The ratio of sprue area to ingate area may follow the 4:3 ratio suggested in gating ratios of 4:8:3. The requirement for runner area is simply that it not have a choke effect over the ingates and sprue. It may be enlarged to trap slag if desired. A ratio of 1.5 to 4 times the ingate area is feasible. For trapping slag, runners are placed in the cope and riser ingates connected in the drag into the riser base, Fig. 3. However, the 1:4:4 gating ratio recommended by AFS in which the choke is at the sprue has also been used for trapping dross and slag. In this case, it is still necessary that the riser connection to the runner freeze rapidly. Gating ratio may effect K , as shown in Fig. 12. Methods for calculating K such as recommended by the AFS publication, reference 2, are also useful.

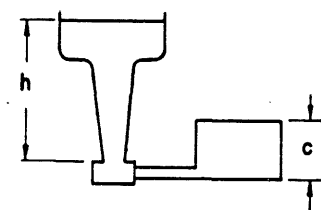
Effective sprue height should be used in the choke area calculation. The calculation is performed as described in Fig. 13 from ref. 2. The effect of K on pouring rate at a given effective sprue height is shown in Fig. 14. When emphasis is placed on fast freezing chokes in this design technique, K decreases and larger total choke areas may be

a. TOP GATING
EFFECTIVE
HEAD, $H = h$



b. BOTTOM GATING

EFFECTIVE
HEAD, $H = h - \frac{c}{2}$



c. PARTING LINE GATING

$H = \text{EFFECTIVE HEAD} = \frac{2hc - p^2}{2c}$

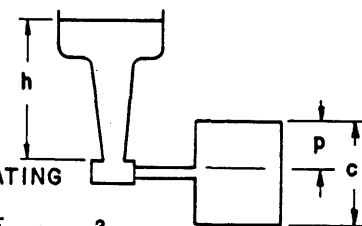


Fig. 13. Effective sprue height calculation from reference 2.

needed for the same effective sprue height and pouring time. The major concern in the gating design is that piping starts soon after pouring. Regardless of the gating calculation method used, the purpose is to optimize the contribution of gates and risers to feeding the casting and to maximize yield.

YIELD

Theoretical riser yield for ductile iron approaches 80%.¹ Gating usually requires 10 to 20%. Overall yield with tapered risers appears to be in the range of 55 to 65%.

REVIEW OF DESIGN PROCEDURE

The steps in the design procedure may be summarized as follows:

- 1) List the constraints to the design process; cope height, maximum tapered riser height, number of risers, runners, ingates needed, effective section thickness of feeding paths to which risers are attached, feeding distance from each riser, casting weight, mold weight, pouring time, etc. If the casting is small, riser will not be tall, obviously.
- 2) Choose D_p and H_p from Table 1 for the weight of casting to be fed based on the longest H_p within the height constraints of the mold.
- 3) Set the bottom diameter of the feeding section as $D_R = 2W + D_p$.

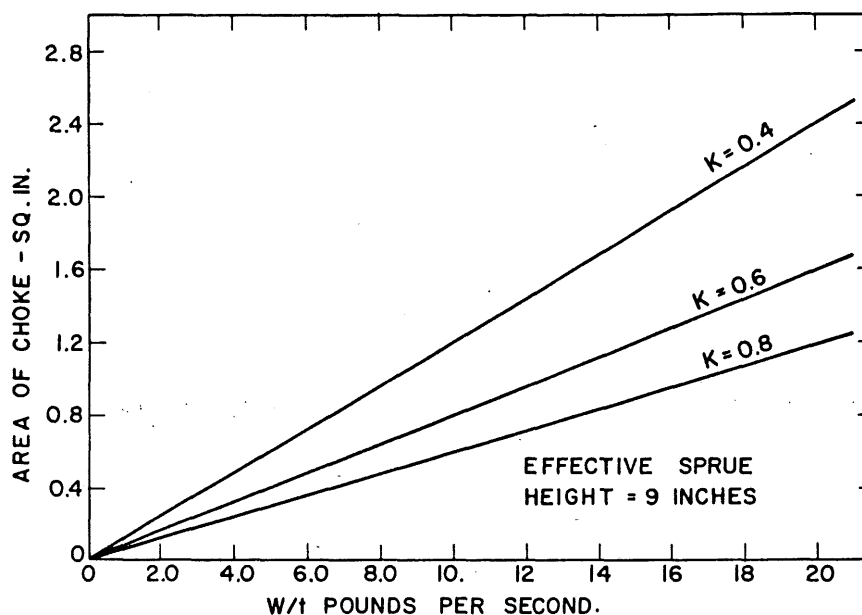


Fig. 14. Effect of change in K on pouring rate-choke area relationship at constant effective sprue height of 9.0 inches.

- 4) Choose the top diameter based on D_p and sprue D .
- 5) Taper the riser considering the rules provided earlier.
- 6) Design the base.
- 7) Design the gating system for early freezing off of chokes into risers; keep pouring time short; divide choke area if needed. This is especially important with smaller castings.
- 8) Pour test castings, check pipe configuration and modify taper as needed, or review riser ingate choke, as it may be too large.

The thickness frozen estimate may be compared with what is actually obtained by plotting the results from sec-

tioned risers on diagrams such as in Figs. 8 and 9. Pipe volume estimate may also be compared with results by measuring with water from a burette. The tapered riser should prevent erratic piping, such as shown in Fig. 1, and porosity on the casting side of the riser contact. All the other contributors toward internal porosity may still lead to problems that have not been reviewed.

REFERENCES

1. R. W. Heine "Rising Principles Applied to Ductile Iron Castings Made in Green Sand," *AFS Trans.*, 1979, and references.
2. American Foundrymen's Society, *Basic Principles of Gating and Rising*, Des Plaines, IL, 1973, Figs. 3-25.

Experiences with an Applied Riser Technique for the Production of Ductile Iron Castings

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ABSTRACT

Developing the ability to produce ductile iron castings free from contraction related defects, is for many foundries, a costly process of trial and error using traditional methods. These methods very often rely on experience gained from alloys other than ductile iron. Consequently, the methods do not acknowledge the unique volume change pattern experienced by a cooling and solidifying ductile iron alloy. In spite of a good deal of effort from foundries, many ductile iron castings therefore contain contraction related defects. This increases operating costs, if the affected castings are scrapped. More significantly, field performance may be reduced if the defective castings are inadvertently shipped to the casting user.

Using the limited scientific knowledge available at the present time regarding volume change patterns in cooling and solidifying ductile iron alloys, an applied risering technique has been developed. The technique addresses the problem of contraction related defects in a logical and systematic way.

An account is given of how this technique was applied in the Canadian foundry of Ancast Industries Ltd. and of the results obtained with the technique. In addition to obtaining significant improvements in pattern and true casting yields when using the applied risering technique, the process of gaining customer approval of sample castings has been improved.

This paper contains general background information of the applied risering technique, a typical calculation of applied risering system design and a summary of results obtained with a range of castings.

INTRODUCTION

During the 35 years which have elapsed since the announcement of the commercial ductile iron process, many millions of tons of ductile iron castings have been produced and used satisfactorily. Current forecasts indicate that ductile iron castings production will continue to grow modestly at an annual rate of 4% per year taking the worldwide annual castings production rate to 10 million metric tons by 1985.¹

As the requirement for ductile iron castings has increased, so it seems, has the sophistication of casting users in terms of what constitutes a casting of acceptable quality. What was a highly acceptable casting ten years ago, probably will not satisfy the customer today.

Rejection of castings occurs for a multitude of reasons but arguably, one of the most frequently occurring reasons for rejection of ductile iron castings, is contraction related or shrinkage defects.

Obviously, contraction related defects are not new phenomena, as any producer of ferrous castings will testify. However, the mechanism by which these defects appear in ductile iron castings is fundamentally different to the mechanism of shrinkage formation in steel or malleable iron, for example.

Research² has discovered that ductile iron solidifies in an extremely wide range, endogenous manner whereas narrow range, exogenous solidification is typical for steel and malleable iron. Further, liquid ductile iron actually increases in volume during cooling. Various attempts have been made to measure the magnitude of this expansion.^{3,4,5,6} While the absolute values of measured expansion may be of only passing interest to the practical foundryman, the magnitude of this expansion force is sufficient to plastically deform various molding media resulting in swollen castings, which in turn contain shrinkage defects Fig. 1.

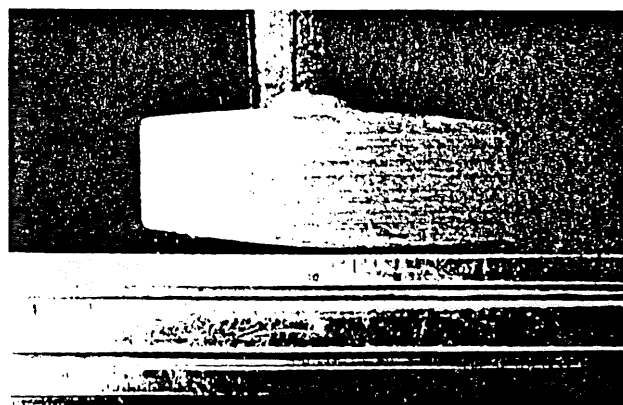


Fig. 1. Cross section of casting with swollen surfaces.

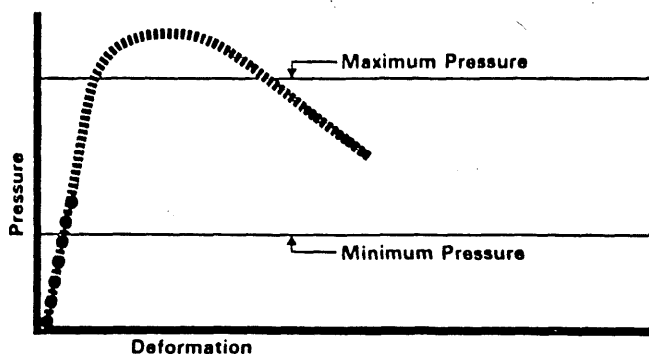


Fig. 2. Schematic of expansion pressure containable by green sand mold.

In view of the net increase in volume experienced by a cooling and solidifying ductile iron casting, in theory at least, it should be possible to produce a casting free of contraction related defects without having to compensate for contraction during the cooling and solidification cycle. Many foundries produce large ductile iron castings free from contraction related defects without the need for risers. To produce large shrinkage free, riserless ductile iron castings, a very rigid mold is necessary, capable of resisting the considerable expansion pressure of the cooling liquid ductile iron. A very favorable casting yield results from these circumstances.

Many high volume foundries are unable to produce molds of sufficient rigidity to allow them to adopt a riserless production technique. Economics dictate the use of clay/water bonded green sand which, no matter how prepared, cannot withstand the expansion pressure, except in the case of very thin castings.

The foundry producing ductile iron castings in green sand molds has then possibly two alternatives for producing shrinkage free castings.

- 1) Use a conventional risering technique which invariably utilizes relatively large risers. This results in very low



Fig. 3. Photomicrograph of secondary shrinkage.

casting yield. Castings which contain isolated heavy sections are difficult to produce free of shrinkage defects even with the aid of expensive chills and concurrent potential problems of blow, fusion and locally changed microstructure.

- 2) Devise a method of controlling the level of expansion pressure in the cooling liquid ductile iron such that the liquid iron is always under positive pressure in relation to atmospheric pressure, but never such high pressure that the green sand mold will deform plastically Fig. 2.

VOLUME CHANGES IN DUCTILE IRON

Research and direct practical observations in foundries suggest the existence of three distinct phases in the cooling and solidification of a ductile iron alloy.

- 1) An initial period of liquid contraction.
- 2) A secondary period during which the cooling liquid expands. This may create a positive pressure in the mold of several times atmospheric pressure. The expansion is intimately interrelated with graphite precipitation.
- 3) The last period starts when expansion ceases and contraction returns. There is strong circumstantial evidence to suggest this period is a period of contraction of the last liquid to solidify. It is further postulated that this last period is responsible for the so-called secondary shrinkage phenomenon Fig. 3.

The above suggestions were summarized by Karsay⁸ in the form of a general volume change pattern for cooling and solidifying ductile iron alloys Fig. 4.

It was further proposed that the general volume change pattern could be influenced by: a) cooling rate of the ductile iron alloy;⁵ b) the metallurgical history of the alloy.

Generally these influences materialize as a greater or lesser tendency to form shrinkage defects. This apparently erratic behavior indicates that, in ductile irons, specific volume at any given temperature is not a material constant, but depends on a) and b).

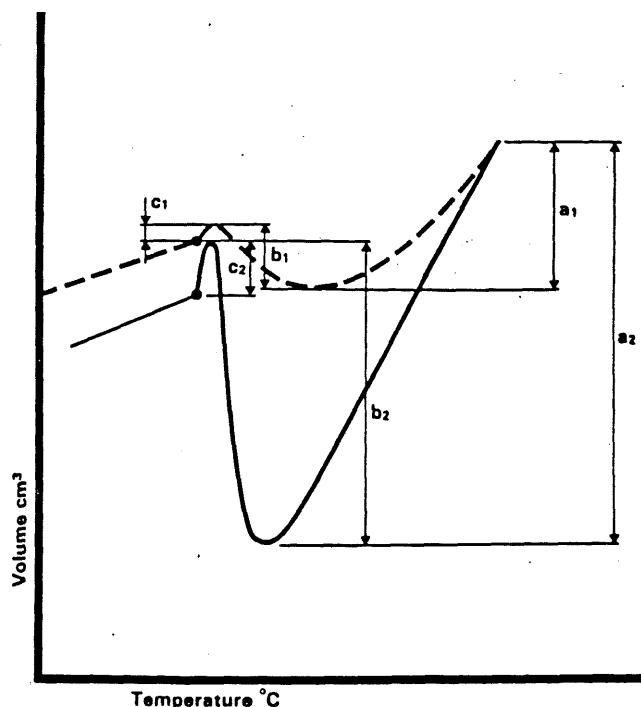


Fig. 4. Proposed volume change pattern for cooling/solidifying ductile iron.

APPLIED RISERING TECHNIQUE — PRESSURE CONTROL RISERING (PCR)

The developed technique is shown schematically in Fig. 5.

Initially the blind riser compensates for liquid contraction in the casting cavity. Thus a void is created inside the riser.

As the liquid in the casting cavity starts to expand, during the second period of the cooling cycle, pressure increase in the liquid in the casting cavity is initially avoided since the pressure energy drives liquid from the casting cavity back into the riser which is ideally refilled.

Once the riser is refilled it is obviously not possible to transfer any more liquid from the casting cavity and so the liquid in the casting/riser complex experiences a moderate pressure increase which continues to the end of the second stage of cooling.

Providing the positive pressure in the liquid iron at the end of the second period of cooling is greater than the pressure decrease resulting from volume contraction of the last liquid to solidify during the third period of cooling, a secondary shrinkage defect will not form.

ANCAST INDUSTRIES LIMITED

Located in Winnipeg, Manitoba, this company produces gray and ductile iron castings for the agricultural, automotive, railroad and general engineering industries. Casting production

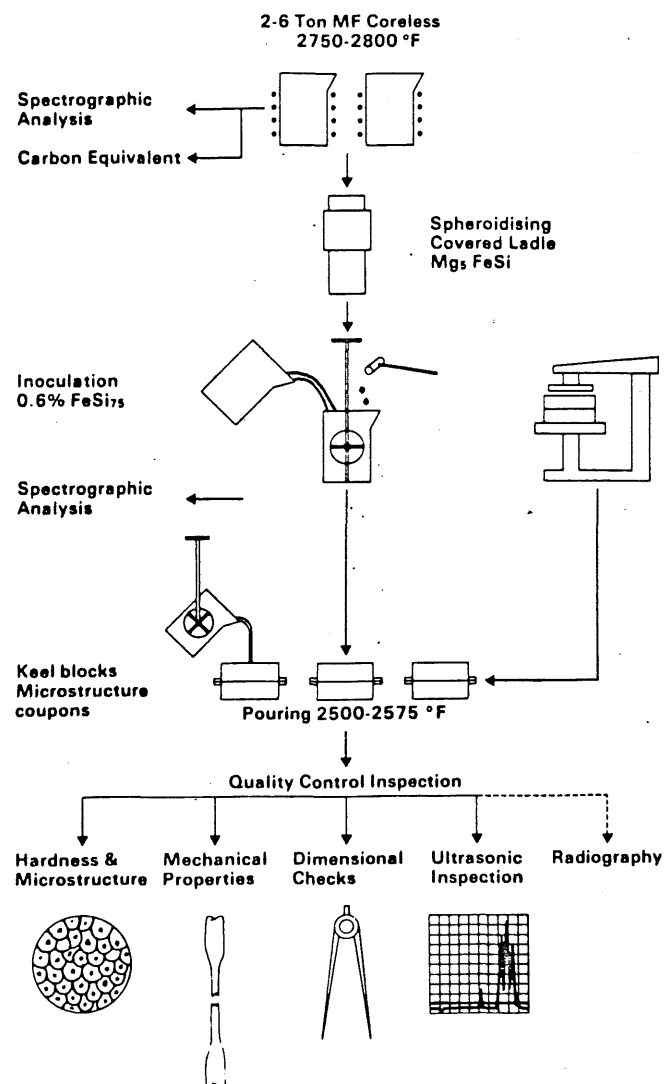


Fig. 6. Schematic of foundry production route.

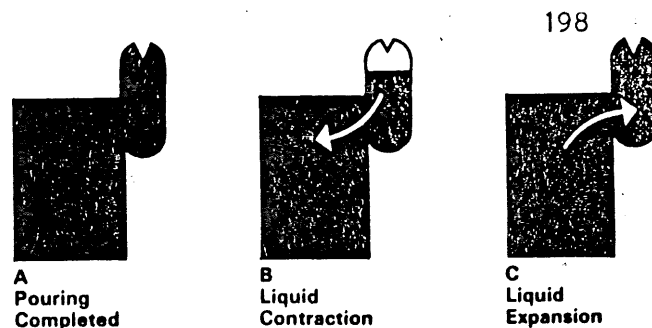


Fig. 5. Schematic of pressure control risering principle.

started on the present site in 1912 and the company had a major modernization in 1973. A schematic of the foundry production route is shown in Fig. 6.

The majority of castings produced by the company are used in safety-related applications. For example, brake parts, high pressure valve bodies and wheel hubs have particularly exacting quality control and inspection requirements. The specifications governing the production of these high integrity castings very often require batch sampling for radiography, 100% ultrasonic inspection or both. With such exacting inspection requirements there is little opportunity to disguise casting defects, whether potentially harmful or not. With regard to shrinkage defects in particular, foundry management decided the only sure way was to attempt to produce castings free from internal defects. The pressure control type of the applied risering techniques was adopted for this purpose.

CASE HISTORY — PART NO. 770

Very often, a foundry will face a dilemma when planning

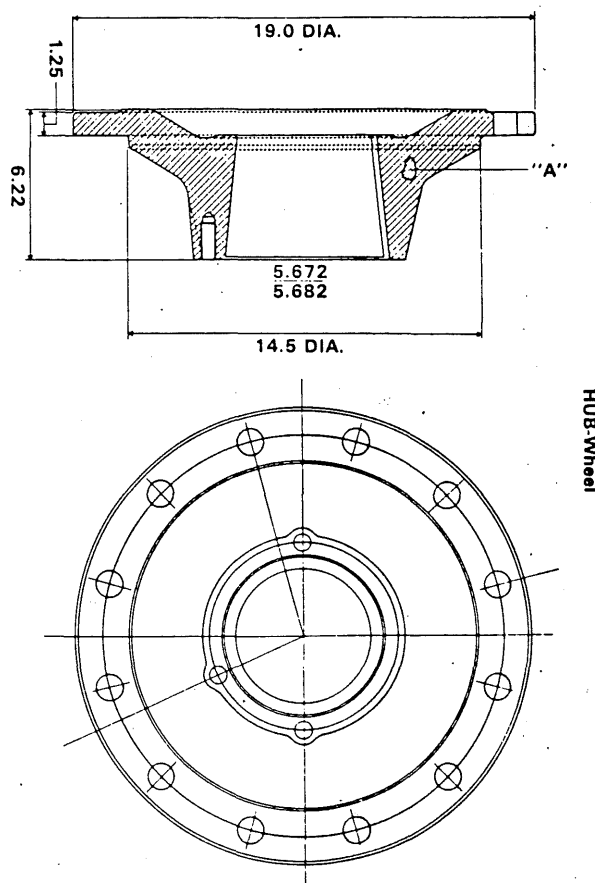
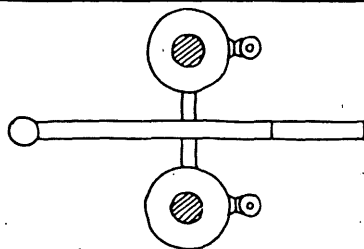


Fig. 7. Drawing of part no. 770.

GATE/RISER SYSTEM

Part No: 770 Company: ABC
 Estimated Weight: 150 lbs

1. Layout:

2. Modulus = $\frac{V}{CSA}$

$M_1 = \frac{2\frac{1}{2} \times 1\frac{1}{4}}{6\frac{1}{4}} = 0.50 \text{ inch}$
 $M_2 = \frac{2\frac{1}{2} \times 1\frac{1}{4}}{5} = 0.87 \text{ inch}$
 $M_3 = \frac{1\frac{1}{4} \times 2}{3\frac{1}{4}} = 0.77 \text{ inch}$
 $M_4 = 0.87 \text{ inch}^*$

3. Transfer modulus (M_t) (Table)

$$M_t = 0.50 \text{ inch}^*$$

4. Riser modulus (M_r), $M_r = M_t$

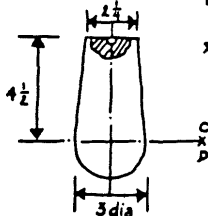
$$M_r = 0.50 \text{ inch}$$

5. Blind Riser Type Type 1 $D_r = 5.68 \times M_r$

(pg. 78)

Type 2 $D_r = 4.91 \times M_r$ $2.46 = 3.0 \text{ inch}$

Type 3 $D_r = 4.53 \times M_r$ $=$



* Note: Max. $M_t = M_r = 0.5 \text{ inch}$
 Assumes good metallurgical quality of the liquid iron.

6. Riser Contact modulus (M_n)

$$M_n = 0.67 (M_r) = 0.67 (0.5) = 0.335 \text{ inch}$$

6b. Contact Shape:

Square,	Side Length = 4 (M_n)	= 1.3 inch
Round,	Diameter = 4 (M_n)	= 1.3 inch
Rectangular	Short Side = 3 (M_n)	= 1.0 inch
	Long Side = 6 (M_n)	= 2.0 inch

7. Check Feed Volume:

Estimated Casting weight = 150 lbs

Estimated Casting volume = $\frac{150}{0.25} = 600 \text{ ins}^3$

Required feed volume = 3 % of $\frac{600}{100} = 18 \text{ ins}^3$

Available feed volume: 'X' dimension = 32 inch

Available feed volume = 19 ins³

Number of risers required / casting = 1

8. Total choke cross sectional area (p. 24), $A_c = 0.65 \text{ ins}^2$

Number of gates, n , = 1

Gate dimensions (4/1):

$n (4a^2) = A_c$ $4a^2 = 0.65$ $a = 0.4 \text{ inch}$

$4a = 1.6 \text{ inch}$

Gate modulus, $M_g = \frac{4a^2}{10a} = \frac{0.65}{4.0} = 0.16$

$M_g < M_t$ $\frac{0.16}{0.5}$

$\frac{M_g}{M_t} = \frac{1}{3.1}$

9. Runner Bar:

Cross sectional area, $A_R = 2 \text{ to } 4 (A_c) = 3.2 (0.65)$

Height = 2 × width, $2a^2 = A_R = 2 \text{ to } 4 (A_c)$ $a = \sqrt{3(0.65)}$

$a = 1.4 \text{ inch}$

$2a = 2.8 \text{ inch}$

10. $A_s \geq A_c \sqrt{\frac{H}{h}}$ or $A_c \leq A_s \sqrt{\frac{h}{H}}$

$A_s \geq 2(0.65) \sqrt{\frac{10}{3}}$ $D_s^2 = \frac{4.2(0.65) \sqrt{\frac{10}{3}}}{\pi}$ Sprue diam. = 1.74 ins²
 or, total choke $\leq 0.43 \text{ ins}^2$

11. Pattern Yield:

Volume of casting	2x600	1200
riser & contact		64
sprue & basin		62
runner		50
gate		1
sprue well		10
Total volume poured =		<u>1387</u>

Pattern Yield = $\frac{\text{Casting Volume}}{\text{Total volume poured}} = \frac{1200}{1387} = 86.5 \%$

* Standard Sprue dia. = 1.0 inch

$A_c \leq \frac{\pi (1)^2}{4} \sqrt{\frac{1}{10}}$ $A_c \leq 0.43 \text{ ins}^2$

using 1 gate (choke) / casting, $2 (4a^2) \leq 0.43 \text{ ins}^2$

$a \leq 0.23 \text{ inch}$

$4a \leq 0.92 \text{ inch}$

production. This dilemma involves a compromise between obtaining maximum casting yield for each mold produced and minimizing defects which may occur due to overcrowding on a pattern plate. This is particularly significant where thick castings are produced in green sand molds.

A typical example of a heavy hub casting (150 lb) produced to ASTM A536-80, grade 65-45-12 is shown in Fig. 7. Originally, success rate was unsatisfactorily low. The major reason for rejection was a shrinkage deflection at location A.

Since using the applied risering technique, with respect to shrinkage defects, success rate is close to 100%.

After completing the initial important contemplation, (consideration of layout, riser placement, etc.), the detailed design calculations were made.

The detailed design calculations are summarized in Fig. 8 and the individual steps were as follows.

- 1) Determine the significant modulus (M_s) of the casting. This involves breaking down the complex casting shape into simple shapes and determining the modulus of each simple shape.

Modulus is defined as the ratio:

$$M_s = \frac{\text{Volume}}{\text{Effective Cooling Surface Area}}$$

Modulus is considered to be the only parameter which allows an accurate prediction of cooling and solidification rate. Under the given conditions M_s is the largest segment modulus.

- 2) From M_s , determine liquid transfer modulus, M_r . M_r is defined as the modulus of a segment through which liquid transfer will take place from an adjacent larger segment, for

Fig. 8. Design calculations for risering system — part no. 770.

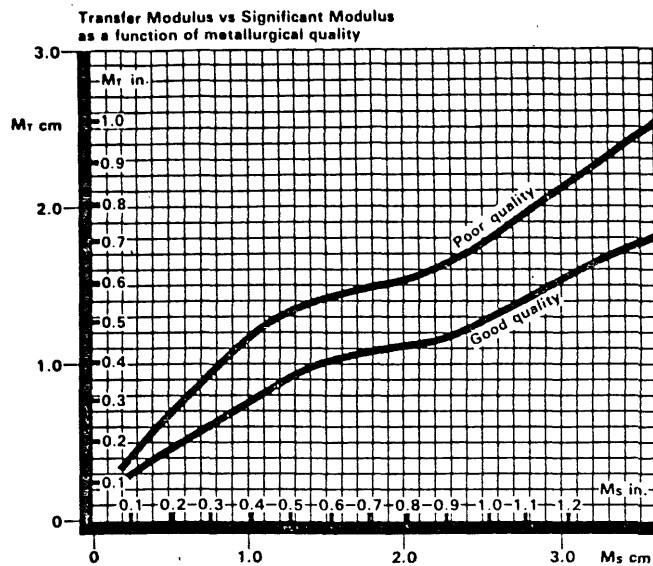


Fig. 9. Relationship of M_s to M_r .

as long a time as necessary. The necessary time is that required for the significant (heavier) segment (modulus = M_s) to dissipate its expansion pressure by transferring expanding liquid through an adjacent (smaller) segment. Obviously the adjacent segment must remain permeable to liquid iron for the necessary length of time. In other words,

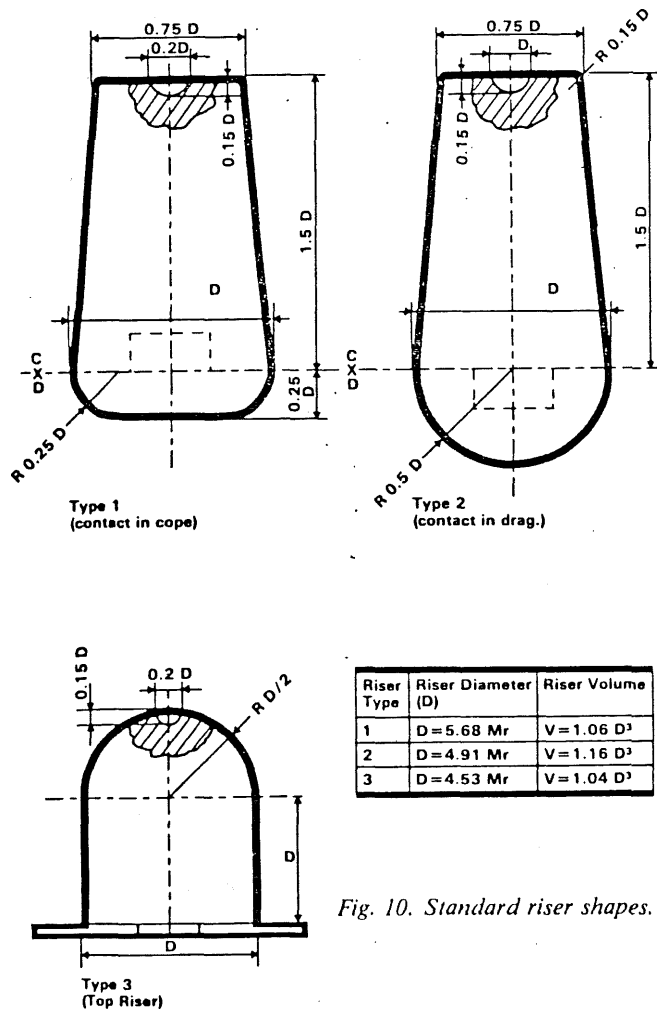


Fig. 10. Standard riser shapes.

the adjacent segment must have a certain minimum modulus, M_r . A proposed relationship between significant modulus, M_s and liquid transfer modulus, M_l is shown in Fig. 9.

This relationship has its basis in the laws of Chvorinov and Newton, subsequently modified in the light of practical experience with ductile iron.

- 3) As long as the significant segment of the casting needs to transfer liquid (to maintain containable pressure in the mold cavity), the riser must be able to receive that liquid. That is to say the riser and riser contact must be permeable to liquid iron. Therefore, the riser and riser contact also must have a certain minimum modulus.

This is ensured by constructing a riser with modulus

$$M_R = M_l$$

By using the series of standard riser shapes shown in Fig. 10, riser dimensions may be calculated very quickly.

- 4) Construction of the riser neck, or contact, needs to be such that the contact also has a modulus M_N where

$$M_N = M_r$$

However, providing the riser contact is short, a reduction in riser contact modulus is permissible on the grounds that the sand mold immediately surrounding the riser contact becomes superheated. Hence, the acting riser contact modulus is larger than what would be calculated from its dimensions. In other words, providing riser contact length is short, smaller riser contacts may be used.

Consequently, $M_N = 0.67 (M_r)$ was used for design purposes.

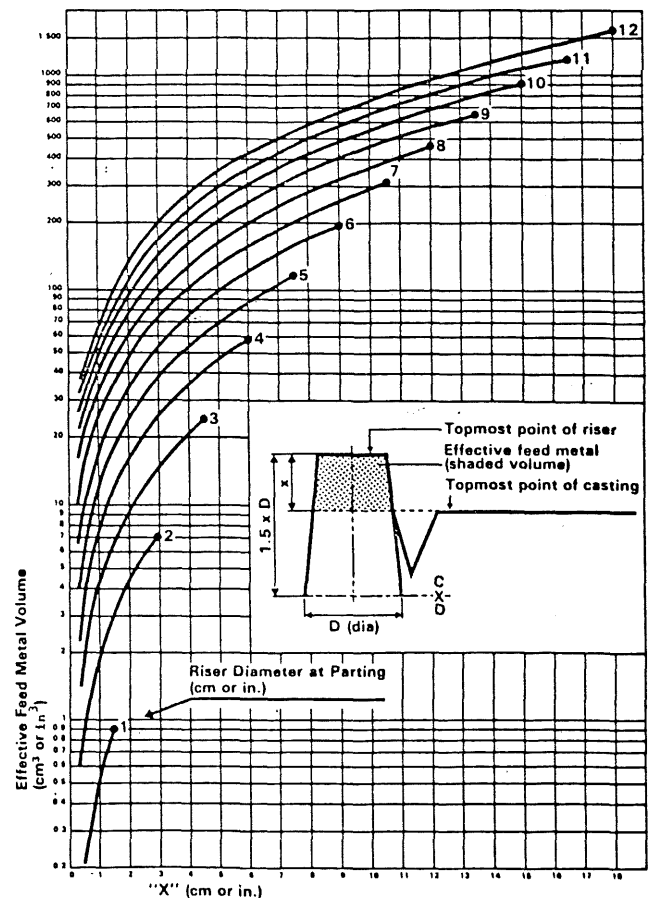


Fig. 11. Effective riser volume.

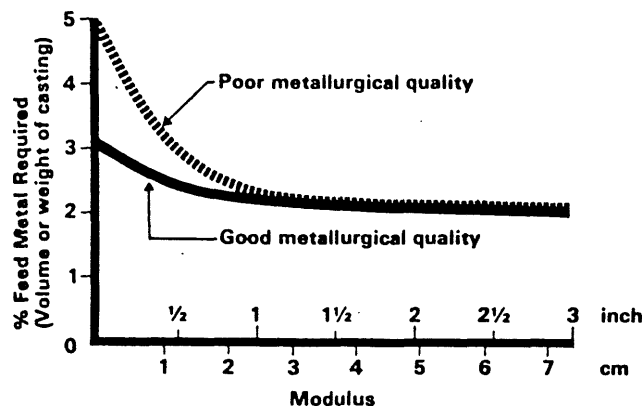


Fig. 12. Relationship of modulus to shrinkage.

- 5) Having constructed a suitable riser, it was necessary to determine whether or not the riser contained sufficient effective volume to compensate for the initial liquid contraction occurring in the casting during the first stage of the cooling cycle. Figure 11 illustrates the problem. Only the iron contained in the riser above the top most point of the casting is effective in feeding the liquid contraction of the casting.

An estimation of expected liquid contraction was obtained using Fig. 12 which relates casting modulus to contraction. Knowing the weight (volume) of the casting it was a simple matter to predict liquid contraction of the casting. The final step was to compare predicted casting shrinkage volume with effective riser volume. This design step is detailed in paragraph 10 of Fig. 7.

In the example chosen, the designed riser size is adequate to compensate for liquid contraction of the casting. When the designed riser does not contain sufficient effective volume to feed the casting, effective riser volume may be increased by: a) increasing riser height; b) using two risers; c) using a top riser.

With cases a) and c), a sufficiently thick layer of sand must remain between the top of the riser and mold cope surface. Two inches of sand is considered a safe minimum. With less than two inches of sand, the mold may rupture and control over liquid iron expansion will be lost, resulting in shrinkage defects appearing in the castings.

Results With Part No. 770

The as-cast appearance of the design is shown in Fig. 13. A section through the casting, riser contact and riser is shown in Fig. 14. Casting and riser are free of contraction related defects. Six castings were sectioned from the first production run and on subsequent runs, occasional destructive tests are performed to check internal soundness.

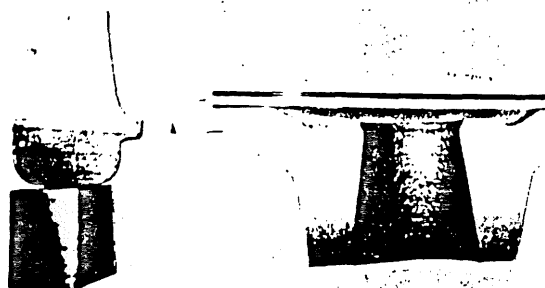


Fig. 14. Section of part no. 770 and riser.

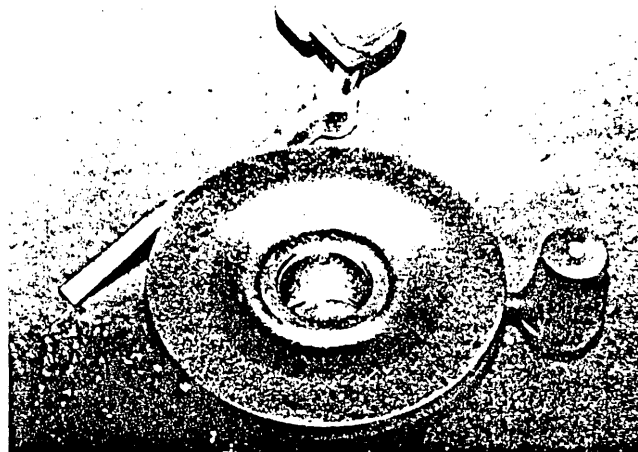


Fig. 13. As cast appearance of part no. 770.

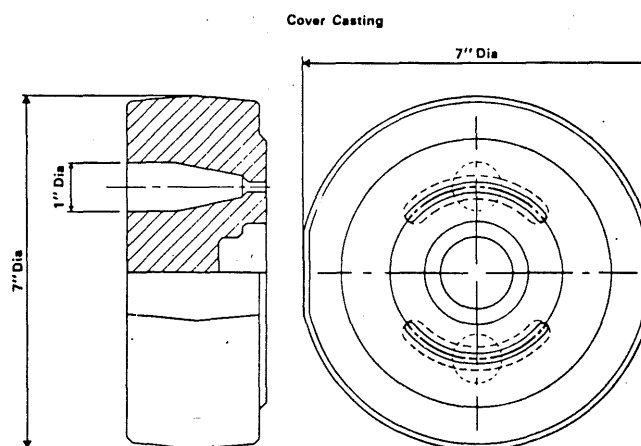


Fig. 15. Drawing of part no. 211.

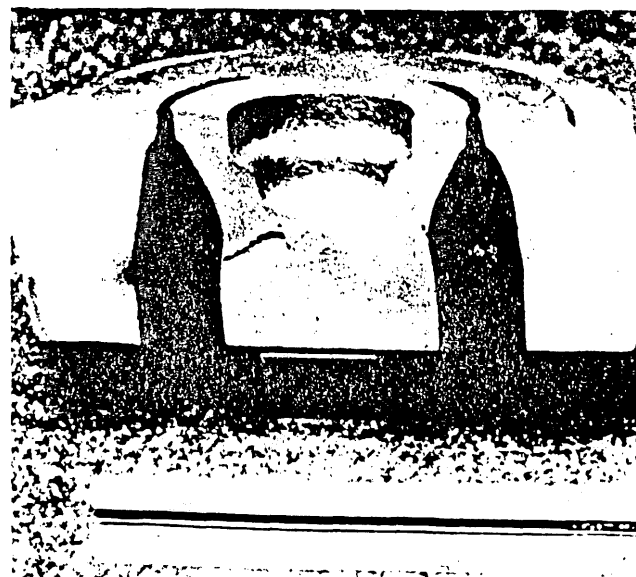
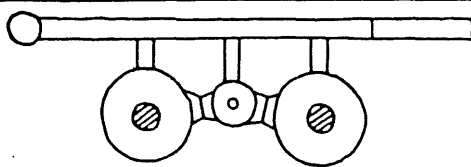


Fig. 16. Location of shrinkage defect in part no. 211.

GATE/RISER SYSTEM

Part No: 211 Company: ACMEEstimated Weight: 27 lbs

1. Layout:



2. Modulus = $\frac{V}{CSA}$

Core cooling - negligible

$$V = \frac{\bar{A}(\tau)^2}{4} \cdot 2.875 = 110.6 \text{ ins}^3$$

$$M = 0.8 \text{ ins}$$

$$CSA = \frac{2\bar{A}(\tau)^2}{4} + \bar{A}(\tau) 2.875 = 140.2 \text{ ins}^2$$

$$M_s = 0.8 \text{ ins}$$

3. Transfer modulus (M_t) (Table)

$$M_t = 0.6 \text{ ins}$$

4. Riser modulus (M_r), $M_r = M_t$

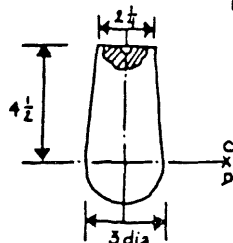
$$M_r = 0.6 \text{ ins}$$

5. Blind Riser Type Type 1 $D_r = 5.68 \times M_r$
(pg. 78) Type 2 $D_r = 4.91 \times M_r$

$$2.95 = 3.0 \text{ ins}$$

Type 3 $D_r = 4.53 \times M_r$

=

6. Riser Contact modulus (M_n)

$$M_n = 0.67 (M_t) = 0.67 (0.6) = 0.4 \text{ ins}$$

6b. Contact Shape:

Square, Side Length = $4 (M_n) = 1.6 \text{ ins}$

Round, Diameter = $4 (M_n) = 1.6 \text{ ins}$

Rectangular, Short Side = $3 (M_n) = 1.2 \text{ ins}$

Long Side = $6 (M_n) = 2.4 \text{ ins}$

7. Check Feed Volume:

Estimated Casting weight = 27 lbs

Estimated Casting volume = $\frac{27}{0.25} = 108 \text{ ins}^3$

Required feed volume = $3\% \text{ of } (108) 2 = 6.5 \text{ ins}^3$

Available feed volume: 'X' dimension = 2.94 ins

Available feed volume = 20.7 ins

Number of risers required/casting = < 1

8. Total choke cross sectional area (p. 24), $A_c = 0.5 \text{ ins}^2$

Number of gates, $n = 3$

Gate dimensions (4/1):

$$n (4a^2) = A_c \quad 3 (4a^2) = 0.5 \quad a = 0.20$$

$$4a = 0.80$$

Gate modulus, $M_g = \frac{4a^2}{10a} = \frac{0.16}{2.0} = 0.08 \text{ ins}$

$$M_g < M_t \quad \frac{0.08}{0.6} \quad \frac{M_g}{M_t} = \frac{1}{7.5}$$

9. Runner Bar:

Cross sectional area, $A_R = 2 \text{ to } 4 (A_c) = 3 (0.5)$

$$\text{Height} = 2 \times \text{width}, 2a^2 = A_R = 2 \text{ to } 4 (A_c) \quad a = \sqrt{\frac{1.5}{2}}$$

$$a = 0.9$$

$$2a = 1.8$$

10. $A_s \geq A_c \sqrt{\frac{H}{h}}$ or $A_c \leq A_s \sqrt{\frac{h}{H}}$

$$A_s \geq 0.5 \sqrt{\frac{L}{3}} \quad D_s^2 \geq \frac{4}{\pi} (0.5 \sqrt{\frac{L}{3}})^2 \quad \text{Sprue diam.} = 0.99 \text{ ins}$$

or, total choke =

11. Pattern Yield:

Volume of casting	2 x 110.6	221
riser & contact		39
sprue & basin		20
runner		16
gate		1
sprue well		2
Total volume poured =		299 ins ³

$$\text{Pattern Yield} = \frac{\text{Casting Volume}}{\text{Total volume poured}} = \frac{221}{299} = 74\%$$

One casting/mould,

$$\text{yield} = \frac{111}{188} = 59\%$$

Fig. 17. Design calculations for part no. 211.

With two castings/mold, pattern yield is 86% with a true yield in excess of 80%. Original true yield was significantly lower than required.

$$\% \text{ True yield} = \frac{\text{Total weight of castings shipped} \times 100}{\text{Total weight of iron poured into molds}}$$

CASE HISTORY. PART NO. 211

The second example, Fig. 15, is a motor cover weighing 27 lb.

Previous experience in producing this casting was unsatisfactory. Many castings were scrapped for internal shrinkage defects shown in Fig. 6. Also apparent are swollen casting surfaces which is a typical sign of improper risering system design with ductile iron.

The previous step-by-step approach was used to design a new risering system for this casting. The design details are shown in Fig. 17.



Fig. 18. Sections of part no. 211 produced: A) By Applied Riser B) By Previous Practice.

Results With Part No. 211

The results of applying the new design of risering system are summarized in Fig. 18. Sound castings with flat surfaces were produced by proper application of pressure control risering. An unexpected problem arose during trials with this casting.

The blind riser design requires a hot spot to be created in the top of the riser. This is to prevent formation of a complete solid skin around the riser, to ensure the liquid iron in the riser remains at atmospheric pressure through the mold hot spot. Under this condition the riser will feed the casting for as long as the riser liquid iron level remains higher than the top of the casting. Under conditions where no riser hot spot is formed, a complete skin may form around the riser/mold interface. As the riser supplies liquid to the casting, a void is formed in the riser. If this void is surrounded by a pressure tight solid skin, pressure inside the void will fall below atmospheric pressure and the riser will stop feeding.

Returning to the trial castings of part no. 211, the riser pattern quite clearly incorporates a dimple hot spot Fig. 19 and yet the initial castings produced contained obvious primary (liquid shrinkage) defects. Figure 20 shows the cope surface depression typical of the defect experienced.

Inspecting the molding practice revealed that the sand peak was breaking off the mold during the pattern withdrawal and remaining in the pattern. Subsequent molds did not contain a sand peak.

The riser dimple was modified to the type shown in Fig. 21 and no further problems of this type were experienced. Figure

21 also shows very well, the blind riser in process of refilling by liquid transfer from the casting back into the riser.

Before the application of pressure control risering, true yield with this casting was approximately 30%. Since redesign of the risering system, 50% yield has been attained with one casting/mold. With two castings/mold, pattern yield will be close to 70%.

GENERAL STATEMENT OF RESULTS

During a period of eight months, pressure control risering systems were designed for 20 different types of castings. Results with the two examples described above are typical of the experiences throughout the eight month trial period. In some cases, substantial yield improvements have been obtained. In the remaining cases yield improvement was at least 10%.

Several new jobs have been tried during the eight month period and while it is not possible to calculate yield improvements for these, there have been significant reductions in lead times, from casting drawing to approved samples, compared to previous experiences.

The Importance of Pouring Temperature

For the initial trials with the pressure control risering system, no special precautions were taken regarding pouring temperature beyond ensuring pouring temperature was adequate to minimize cope side casting defects.

As the trials progressed it became very obvious that once pouring temperature dropped below 2475F (1357C), the incidence of castings with shrinkage defects increased significantly. Although the increase in defects was not analyzed statistically,



Fig. 19. Riser for part no. 211.

Fig. 20. Example of primary liquid shrinkage due to incorrect riser function.

the foundry describes the effect as changing from producing few castings with shrinkage defects to producing few castings without shrinkage defects once the pouring temperature became lower than approximately 2475F.

As a probable explanation for the influence of pouring temperature on the occurrence of shrinkage defects, the following is offered:

The success of the pressure control rising system depends essentially on creating a sufficiently large shrinkage void in the blind riser during the initial period of liquid contraction. After all, it is this void which by liquid transfer, will allow the pressure of the expanding cooling liquid in the casting cavity to be maintained at a level which is containable by the weak green sand mold. This directly influences the occurrence of contraction related defects in the castings.

Resulting from these experiences, a minimum iron pouring temperature of 2500F (1371C) is maintained and the iron temperature in pouring ladles is measured regularly before and after pouring. The maximum iron pouring temperature is more complex to define. In practice it is determined by the initial iron temperature required to pour a given number of molds while still pouring the last mold with iron temperature not less than 2500F. It has not been necessary to exceed 2575F (1413C) as an initial iron pouring temperature.

The Influence of Metallurgical Quality

Figure 8, relating significant modulus (M_s) to transfer modulus (M_T) shows it is possible to select from a range of M_T values for any value of M_s .

Fig. 21. Modified riser design showing riser in process of refilling.



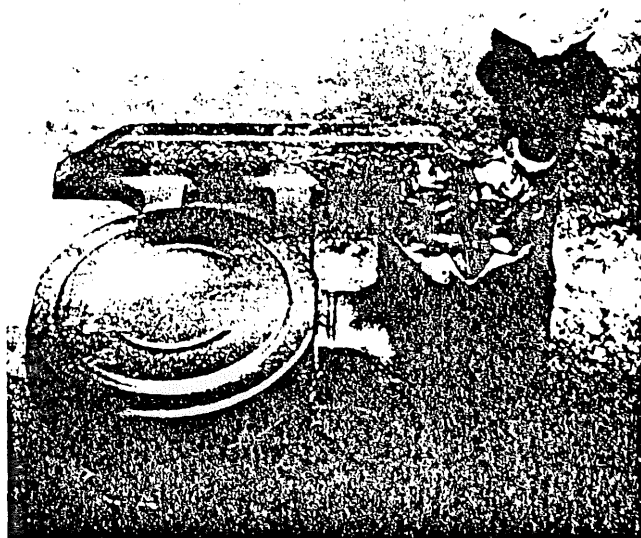
The differences in possible values for transfer modulus for a given value of significant modulus, are ascribed to the different metallurgical qualities of liquid iron.

Metallurgical quality of the liquid iron can be influenced by: melt charge composition, method of melting, degree of superheat experienced by the liquid iron, dwell time in the furnace and degree of inoculation, among others.⁸

At this time, metallurgical quality assessment for the purpose of riser design, is restricted to making an educated guess. We simply do not yet have reliable means to evaluate the influence of the individual factors mentioned above, and others, on metallurgical quality.

The significance of the possible range of M_T values is that the higher in the range the value chosen, the larger will be the riser required. Hence, casting yield suffers.

In the practice reported, mid-range values of M_T were used. In other words, the metallurgical quality of the liquid iron was considered average. This decision was made on the basis of using nodule count as a measure of metallurgical quality in a sample of a given modulus. Nodule count is determined routinely on a standard AFS test coupon.



CONCLUSIONS

The applied risering design system used has given significant improvements in true casting yield.

A standardized, logical approach to risering system design can result in the production of consistently defect-free castings in green sand molds. The design is not unduly complex and together with adequate production controls, reduces errors and improves lead times for sample casting approval.

Thorough contemplation of pattern layout proved to be perhaps the most important single step in the design process. Fewer impressions per pattern with a properly designed system always gave superior true casting yield compared to patterns containing maximum number of impressions with a makeshift system.

Relatively wide limits may be applied to the liquid ductile iron production route but iron pouring temperature must be rigidly controlled. A design pouring temperature range of 2500-2575F (1371-1413C) proved compatible with all the risering designs attempted. Rigid temperature control may be difficult in some foundries.

Applied risering system design required riser contact sizes slightly larger than those to which the foundry had become

accustomed with previous practice. Consequently, riser removal was slightly more difficult. In one case this led to an unauthorized reduction in riser contact size, accompanied by the re-appearance of shrinkage defects in the castings. The compromise is obvious.

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Cylindrical Top Riser Design Relationships for Evaluating Insulating Materials

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ABSTRACT

A cylindrical top riser model was analyzed with geometric programming to determine the effect of insulating materials and solidification shrinkage upon the riser dimensions. The riser and casting were both assumed to follow Chvorinov's Rule for solidification and the feed metal supplied by the riser to the casting was assumed to be one-sixth of the riser volume. The solidification constants (B) and solidification exponents (n) in Chvorinov's Rule, $t = B(V/SA)^n$, were assumed to be equal for the riser and the casting. The volume of the casting and riser

were adjusted for the volumetric contraction during solidification such that the final values would be:

$$\begin{aligned} V_{R(f)} &= V_{R(i)} - \beta V_{c(i)} = V_{R(i)} - 1/6 V_{R(i)} = 5/6 V_{R(i)} \\ V_{c(f)} &= V_{c(i)} + \beta V_{c(i)} = V_{c(i)} (1 + \beta) \end{aligned}$$

The volume of the cylindrical riser was

$$V_R = \pi D^2 H / 4$$

and the effective surface area of the riser was

$$SA_R = a\pi DH + b\pi D^2 / 4$$

where a and b were the apparent surface alteration factors for the side and top of the riser.

The geometric programming model was then formulated with the objective function to minimize the riser volume and require the riser to solidify after the casting and to supply the necessary feed metal. The geometric programming problem was first formulated in the primal form and converted into the dual form for the dual solution. The dual variables were then converted into the primal variables H and D . Fortunately, an exact solution for H and D could be found in terms of the apparent surface alteration factors, a and b , of the riser, the volume and surface area of the casting, and the volumetric contraction of the metal.

INTRODUCTION

The geometric programming method of riser design is based upon the mathematical techniques of geometric programming developed by Zener,¹ Beightler,² Phillips³ and others which are based upon the arithmetic-geometric mean inequality. This technique is not the geometric method of riser design used by Heine, which is based upon a specific geometric shape for the pipe in the riser cavity. The technique of geometric programming had previously been applied to riser design,⁵⁻⁷ but it has not included the effect of the riser connection area upon the casting surface area. Although the effect of the riser connection area is small upon plate shaped castings, it can be considerable on chunky castings.

Geometric Programming Model Development

The model developed was based upon Chvorinov's Rule, which has been the basis of models by Poirier and Gandhi,⁸ Ruddle,⁹ and Shenoy and Panchanathan.¹⁰ These models, however, result in the solution of cubic equations, which are presented in the form of nomograms for specific H/D ratios and are somewhat complex.

On the other hand, geometric programming can often lead to simpler expressions for the riser parameters H and D, although the process for obtaining these expressions is rather complex. The model does not need to be resolved once the design relationships are obtained.

The model developed was designed to determine the minimum riser volume and meet the solidification constraint

$$t_R \geq t_C \quad (1)$$

where

$$t = \text{solidification time} \\ R = \text{riser} \quad C = \text{casting.}$$

From Chvorinov's Rule,

$$t = B(V/SA)^n \quad (2)$$

Equation 1 can be rewritten as

$$B_R(V_R/SA_R)^{n_R} \geq B_C(V_C/SA_C)^{n_C} \quad (3)$$

where

$$B_R = \text{solidification constant for the riser} \\ V_R = \text{volume of riser} \\ SA_R = \text{surface area of the riser} \\ n_R = \text{solidification exponent for the riser} \\ B_C = \text{solidification constant for the casting} \\ V_C = \text{volume of the casting} \\ SA_C = \text{surface area of the casting, and} \\ n_C = \text{solidification exponent for the casting.}$$

There is a transfer of metal from the riser to the casting as the casting directionally solidifies to the riser. Under conditions of complete feeding, the amount of metal transferred to the casting would be βV_C , where β is the volumetric contraction during freezing. The volumetric contraction, as defined by Ruddle,¹¹ comprises the liquid shrinkage, the freezing shrinkage, a minor amount of the solid shrinkage, and a component due to mold wall movement. Also, the connecting area of the riser must be subtracted from the initial surface area of the casting. Thus, equation 3 becomes:

$$B_R((V_R - \beta V_C)/SA_R)^{n_R} \geq B_C((V_C + \beta V_C)/(SA_C - A_C))^{n_C} \quad (4)$$

where

$$\beta = \text{volumetric contraction, and} \\ A_C = \text{connecting area.}$$

Some simplifying assumptions, which are usually made in most riser models, can greatly reduce the complexity of the model. The simplifying assumptions made were:

- 1) $n_R = n_C$; the solidification exponents of the riser and casting are equal.
- 2) $B_R = B_C$; the solidification constants of the riser and casting are equal.
- 3) The feed metal from the riser leaves a parabolic pipe equal to one sixth of the riser volume. This analysis has been applied by Johns¹² and Umble.¹³
- 4) The area of the casting surface where the riser is connected is at least equal to the connecting area.

If assumptions (1) and (2) are applied, equation 4 becomes

$$(V_R - \beta V_C)/SA_R \geq V_C(1 + \beta)/(SA_C - A_C) \quad (5)$$

For a cylindrical top riser, one has:

$$V_R = \pi D^2 H / 4 \quad (6)$$

$$SA_R = a\pi DH + b\pi D^2 / 4 \quad (7)$$

$$A_C = \pi D^2 / 4 \quad (8)$$

where

$$H = \text{height of the cylindrical top riser,} \\ D = \text{diameter of the cylindrical top riser,} \\ a = \text{apparent surface alteration factor for the riser side, and} \\ b = \text{apparent surface alteration factor for the riser top.}$$

Using assumption (3) and equations 6 through 8, equation 5 can be rewritten as:

$$(\pi D^2 H / 4 - 1/6 \pi D^2 H / 4) / (a\pi DH + b\pi D^2 / 4) \geq V_C(1 + \beta) / (SA_C - \pi D^2 / 4) \quad (9)$$

Reducing the expression and rewriting the equation in the standard geometric programming form, equation 9 becomes:

$$(\pi / (4SA_C)) D^2 + (24aV_C(1 + \beta) / (5SA_C)) D^{-1} + (6bV_C(1 + \beta) / (5SA_C)) H^{-1} \leq 1 \quad (10)$$

The geometric programming primal representation for this riser design model is:

$$\text{Minimize: } V_R = \pi D^2 H / 4 \quad (11)$$

$$\text{Subject to: } (\pi / (4SA_C)) D^2 + (24aV_C(1 + \beta) / (5SA_C)) D^{-1} + (6bV_C(1 + \beta) / (5SA_C)) H^{-1} \leq 1 \quad (12)$$

The standard dual form of this geometric programming model is:

$$W_{01} = 1 \quad (14)$$

$$2W_{01} + 2W_{11} - W_{12} = 0 \quad (15)$$

$$W_{01} - W_{13} = 0, \quad (16)$$

where W_{01} , W_{11} , W_{12} and W_{13} are the dual variables.

By combining the relationships between the primal and dual, an additional equation was developed and the solution for the dual was obtained via the procedure used previously.¹⁴ The results obtained from the solution gave exact solutions for D and H. The expression for H was complex, but could be simplified if expressed in terms of D. The expressions obtained were:

$$D = 6(6/5) (a) V_C(1 + \beta) / SA_C \quad (17)$$

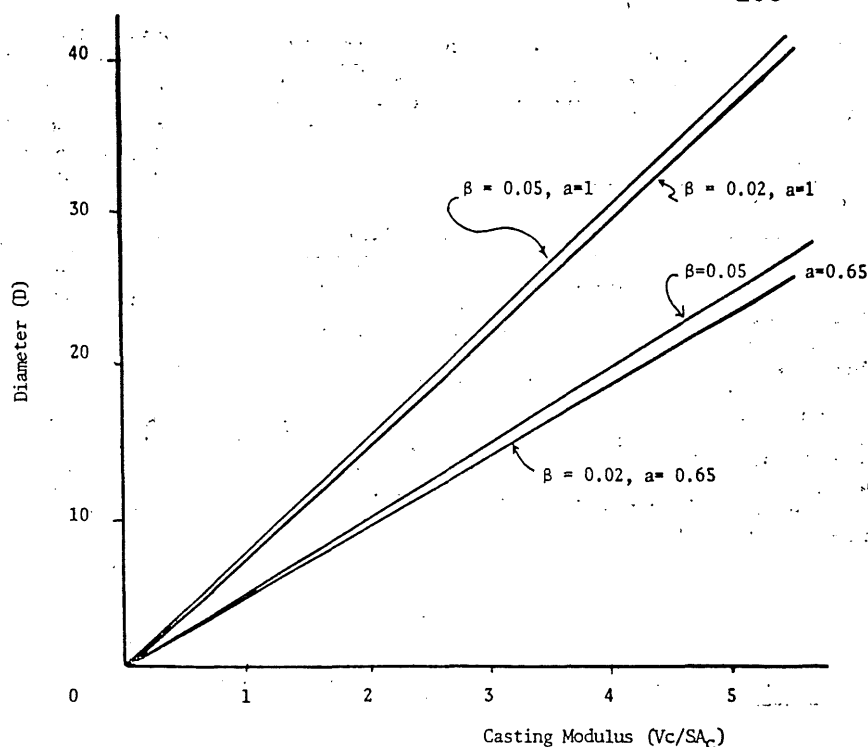
$$H = 3(6/5) (b) V_C(1 + \beta) / (SA_C - 3\pi D^2 / 4) \quad (18)$$

These expressions are much easier for the foundryman to use than the previous methods and nomograms are not required. The expressions clearly indicate the effect of insulating materials upon the riser diameter and height.

Interpretation of the Results

These results are quite consistent with those developed by Wlodawer¹⁵ and those previously developed by geometric

Fig. 1. The effect of solidification shrinkage and insulating walls upon the cylindrical top riser diameter.



programming.⁵⁻⁷ For example, the diameter is often expressed as six times the casting modulus. Thus, if

$$d = 6(V_c/SA_c) (6/5) a (1 + \beta) \quad (19)$$

the first part of the equation is the standard modulus expression. The $(1 + \beta)$ term represents the adjustment for the volumetric contraction, the a term represents the adjustment for the apparent surface alteration factor of the side wall material when sleeves are used, and the $6/5$ represents the 1.2 factor which is commonly used as a "factor of safety."¹¹⁻¹³ It is important to note that this "factor of safety" is not really a factor of safety, but represents the adjustment in the solidification time of the riser for the feed metal supplied to the casting by the riser. The 1.2 results from the assumption that the parabolic pipe shrinkage was one-sixth of the riser volume.

Figure 1 shows the effects of the volumetric contraction and the apparent surface alteration factor of the side wall material upon the riser diameter. Thus, Fig. 1 is a plot of equation 19 for two values of β and a can easily be made for any alloy.

The interpretation for the riser height is somewhat more complex, but is still consistent with the previous works.^{5-7,15} If equation 18 is rewritten as:

$$H = 3(V_c/SA_c - 3D^2/4) (6/5) (b) (1 + \beta) \quad (20)$$

the first part of the equation is similar to the standard top riser relation that $H = D/2$ or three times the casting modulus. The surface area of the casting is adjusted by three times the area of the riser connection. The reason for the factor of 3 in adjusting the surface area is that all of the adjustment for the riser connection area is made on the riser height and none on the riser diameter. Since the riser volume is dimensionally related to the riser height by a power of 3, the surface area adjustment must also be made in the same ratio. The $(1 + \beta)$ term represents the adjustment for the volumetric contraction, the b term represents the adjustment for the apparent surface alteration factor of the top material (such as hot topping or insulation), and the $6/5$ or 1.2 represents the adjustment in the solidification time of the

riser for the feed metal supplied to the casting by the riser.

The H/D ratio was also evaluated to be:

$$H/D = (1/2) (b/a) (SA_c/(SA_c - 3\pi D^2/4)) \quad (21)$$

This expression shows the effect of the insulating materials upon the H/D ratio. This expression indicates why chunky castings where the $3\pi D^2/4$ correction term is a large fraction of SA_c , require larger H/D ratios and why insulating aids are not used on plate castings where the $3\pi D^2/4$ approaches a H/D lower limit of 0.5.

SUMMARY AND CONCLUSIONS

A geometric programming model of the cylindrical top riser was formulated and solved. The simplified general relationships developed for the diameter and height of the top riser were:

$$\begin{aligned} D &= 6(V_c/SA_c) (1.2) (a) (1 + \beta) \\ H &= 3(V_c/(SA_c - 3D^2/4)) (1.2) (b) (1 + \beta) \end{aligned}$$

The effect of insulating materials (such as insulating sleeves, exothermic compounds, and topping materials) upon the dimensions of a top riser can be easily obtained from these design relationships. The various terms in the design relationships were analyzed in terms of adjustment factors to the casting modulus relationship.

Some of the conclusions are:

- the riser diameter is directly proportional to the apparent surface alteration factor for the riser side;
- the riser diameter is unaffected by the connecting area between the riser and the casting, whereas the height of the riser is affected by the connecting area;
- the riser height is in direct proportion to the apparent surface alteration factor for the riser top, whereas the riser diameter is unaffected by this factor;
- the H/D ratio for the riser is independent of the casting volume as it approaches $(b/a)/2$ as a lower limit.

With this information obtained from this study, foundrymen will be able to evaluate the effectiveness of various insulating materials and foundry suppliers can better serve their customers.

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Tellurium Core Wash and Its Use in Solving Internal Shrinkage Defects

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ABSTRACT

Tellurium, when used in small concentrations in a core wash, is an effective method of providing localized solidification control. The carbide forming potential of Te can be selectively placed on a mold or cored surface creating a rapidly formed skin of iron. This change in solidification rates can be used to prevent shrinkage in localized areas.

There are many variables that affect the Te wash's ability to produce consistent results. The concentration of the Te in the wash is discussed providing information determining the amount of primary carbide resulting from any given percentage of Te suspended in a core wash. Application thickness is evaluated determining the criticality of the applied coat of Te wash. The difference in the resulting chill depth as a function of the number of applications is also discussed. In addition evaluating concentration and application thickness, the effect of the location of the Te wash is discussed. Comparisons between horizontal and vertical applications are made with explanations of the differences in the resulting reaction. Also included in this evaluation is a discussion of the impact iron chemistry, predominantly the sulfur content, has on the formation of the carbide reaction, and how layering the Te wash with a protective ceramic changes the reaction between the Te and the iron.

INTRODUCTION

It is often necessary to change a part's original design to accommodate the casting process. However, changing the design is not always an option available to the foundryman. When a casting's design results in shrinkage scrap and cannot be changed, alternative methods to stop the shrink must be found. Conventional means of solving shrinkage defects (risers, mechanical chills, etc.) should always be tried first. If they fail, an alternative solution is available. This solution uses a special core wash containing small concentrations of Te.

The Te core wash works by promoting primary carbides in the iron where it is applied. Small amounts of Te are transferred from the wash into the iron and promote localized carbides. This reaction starts the solidification process faster, making a large section size react like a smaller section size. The chilling effect, induced by the Te, is dependent on many variables. This report describes variables that change the ability of a Te core wash to control localized solidification and promote the desired carbide. Laboratory experiments were designed to answer several fundamental questions and provide guidelines for the foundryman using a Te core wash: 1) What effect does variable Te concentration have on the formation of chill?; 2) How important is the application thickness to the effectiveness of the core wash?; 3) What change does the Te wash have on the solidification characteristics of the iron?; 4) How do the iron filling characteristics change the Te's reaction?; 5) What effect does the application of a protective coat of core

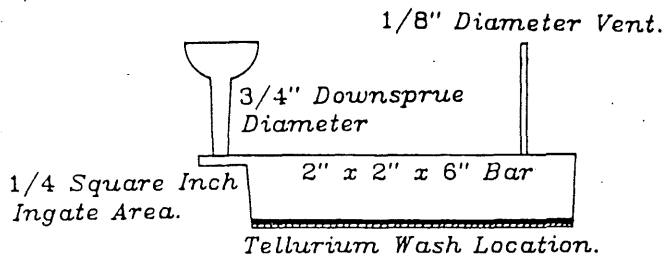


Fig. 1. Mold design for application and concentration experiments.

wash have on the reactivity of the Te?; and 6) What variables in the iron chemistry change the effectiveness of the Te core wash?

Common Experimental Procedures

Tellurium Core Wash Specifications

The Te wash used in all experiments is a combination of silica, kaolinite, talc, iron oxide and Te combined with a mixture of suspension ingredients. The Te is measured on a Wt.% basis. For these experiments, the concentration of Te was varied between 0.0 and 10.0% Te. The core wash was mixed to $50^{\circ} \pm 2^{\circ}$ Baume for all applications unless otherwise noted.

Iron and Temperature Control

The iron used was an alloyed gray iron containing various concentrations of Ni, Cu, Cr, and Mo. The C content ranged from 3.20–3.40%. The Si content ranged from 2.15–2.30%, yielding a CE ranging from 3.92–4.17%. The Mn levels average 0.58% and the S content was between 0.06–0.07%.

The iron was taken from an 80-ton induction holding furnace and transferred into a 6000-lb. teapot ladle. During this transfer, the iron was inoculated at 5.0 lb./ton with an Sr bearing FeSi. The iron was then transferred to a two-man pouring ladle or taken with an iron sampling cup for use. The pouring temperature for all tests ranged between 2600–2650F.

Microstructural Analysis

The Te wash will produce two distinct zones at the iron/core interface. The zone closest to the core wash is primary carbide or chill. The next zone, just past the primary chill, is a degenerate mesh type graphite. These chill depth measurements for all experiments do not include any of the mesh graphite zone. All samples were etched with 5% nital and measurements were taken at 100X.

Core Process

Unless otherwise specified, the core system used is a cold box phenolic urethane system with 1.25% total binder and 0.5% Macor addition.

Experiment 1. Te Concentration

Design and Procedure

A test block measuring 2 x 2 x 6 in. was constructed. The gating and venting can be seen in Figure 1. The Te wash was applied to the drag surface with the coverage rate of $\frac{1}{4}$ cubic centimeter of wash per square inch of core surface. The Te concentration was varied between 0.5 and 10.0%. The molds were dried, assembled,

CHILL DEPTH VS. PERCENT TELLURIUM

Linear Regression Fit = Solid Line
95% confidence limits = Dashed Line

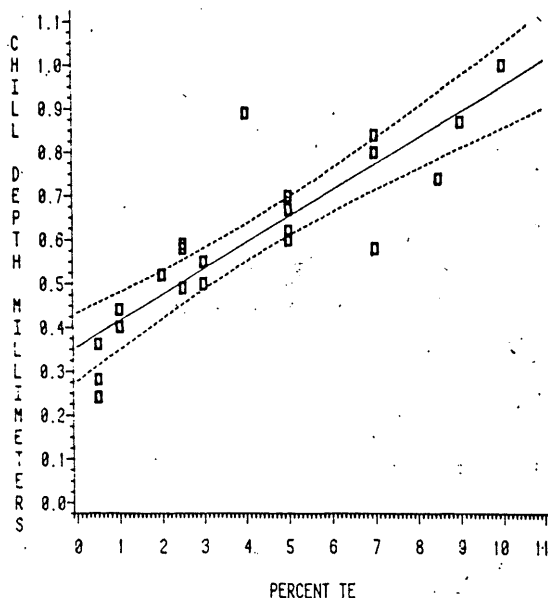


Fig. 2. Chill depth vs. Te concentration in the core wash (exposed to iron).

and sealed with a general purpose core paste. The castings were poured, cooled to room temperature, and sectioned for examination.

Chill Depth at Variable Te Concentration

The depth of chill increases as the concentration of Te increases. The graph in Figure 2 illustrates this relationship. A linear regression curve fit shows the 95% confidence limits along with the mean value for any given percentage of Te. The equation fits the following general format:

$$Y = b_0 + b_1 * X$$

Experiment 2. Application Thickness

Experimental Design and Procedure

A test block identical to Experiment 1 (Fig. 1) was used. The drag was divided into four equal areas. The Te core wash was applied to the drag at the same coverage rate ($\frac{1}{2}\text{cm}^3/\text{in.}$). On one fourth of the drag (nearest the gate), one application was used. On the second fourth of the drag, two applications were used. On the third fourth of the drag, three applications were used. On the fourth area (farthest from the gate), four applications were used.

Applied Coats vs. Chill Depth

Application thickness is a critical factor in the effectiveness of the wash. As the thickness of the wash increases, the amount of chill depth increases. Increasing the number of applied coats of wash increases the chill depth approximately 30% for each additional layer of Te wash. This relationship can be seen in Figure 3. This experiment also showed that lower concentrations of chill depth provide a more consistent layer of chill than did higher concentra-

CHILL DEPTH AND APPLICATION THICKNESS TELLURIUM EXPOSED TO IRON CONTACT

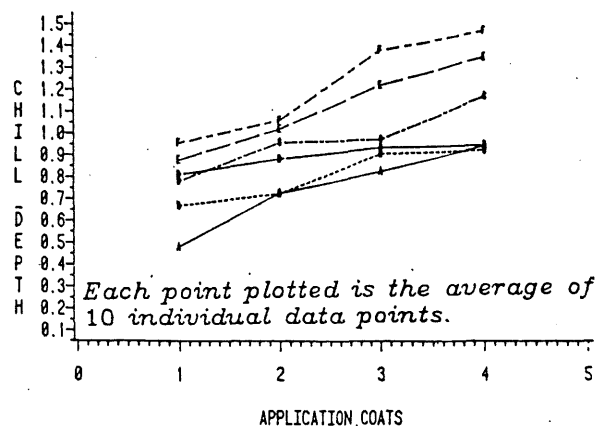


Fig. 3. Chill depth vs. application thickness for varying percents of Te.

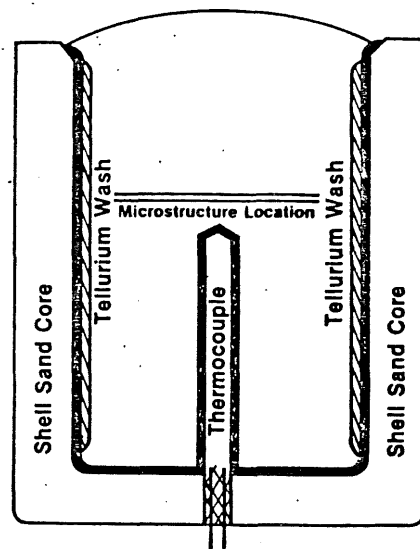


Fig. 4. Mold fabrication for solidification tests using various Te concentrations.

tions. If a consistent depth is critical, it is advantageous to use multiple coats of a lower percent Te wash instead of one coat of a higher percent Te wash.

Experiment 3. Effect on Solidification

Introduction and Scope

The reaction of the Te based core wash goes beyond the layer of chill that is induced at the core/iron interface. If the simple presence of the layer of chill prevented the shrinkage in massive areas, then concentrations as low as 1.0% Te could be used to stop the shrink.

SOLIDIFICATION CURVES

COOLING CUPS WITH TELLURIUM EXPOSED

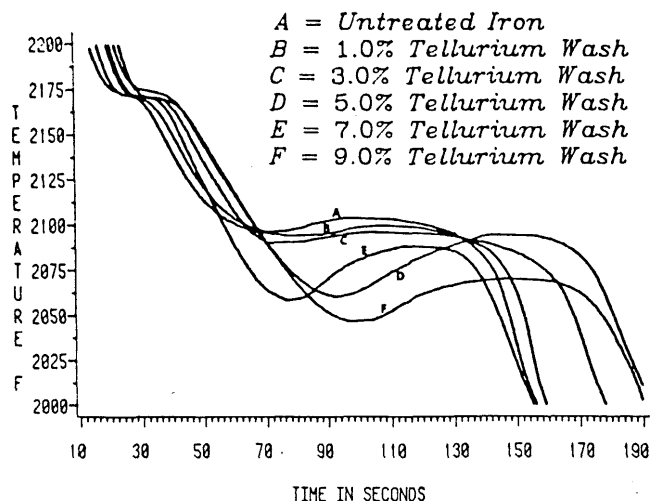


Fig. 5. Plot of critical cooling curve temperatures for Te concentrations.

Table 1. Tellurium Wash Concentration vs. Liquidus, Solidus, Undercool and Percent Tellurium Transferred

Coating weight	Color	Ref.
0.2 - 0.3 g/m ²	Transparent	[10]
0.2 - 0.3 "	Pale yellow - golden yellow	
0.2 - 0.3 "	Light green - dark green	
0.033 g/m ²	Transparent Yellow	[9]
0.11 g/m ²	Transparent	ASTM B449 72

Unfortunately, low concentrations of Te does not usually stop shrinkage. It is theorized that the reaction between the Te wash and the iron happens as a gas/metal transfer. In washes with large Te concentrations, greater percentages of Te are transferred into the iron and primary carbides are formed. At slightly smaller concentrations, just beyond the primary chill layer, a mesh type graphite is formed. Both of these reactions are seen at the Te wash/iron interface. This experiment to determine if small levels of Te transferred into the iron change the solidification pattern.

Design and Procedure

Standard Type K thermocouple cooling curve cups were used (see Fig. 4 for design details). Varying percentages of the Te wash were applied to the wall of the cooling cup. The samples were poured with the same base iron and the data were gathered with a computerized data acquisition system.

Solidification vs. Te Percent

A comparison of the cooling curves reveals a difference in the

CRITICAL SOLIDIFICATION TEMPERATURES

COOLING CUPS WITH TE WASH EXPOSED

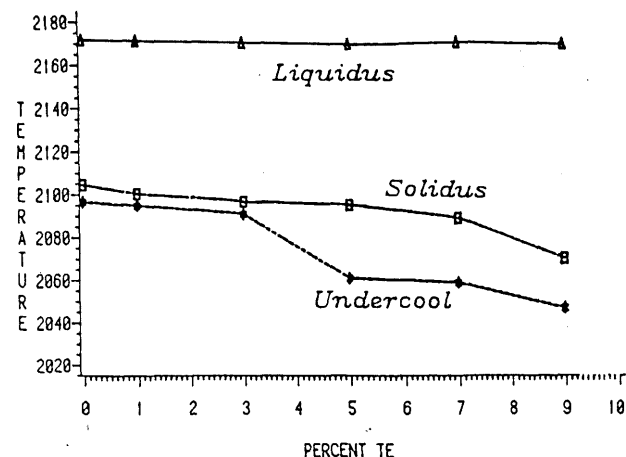


Fig. 6. Cooling curve plots for various Te concentrations.

solidification of the iron dependent on the Te concentration. The solidus and the undercool temperatures were lowered as the percentage of Te increased. The liquidus temperature remained unchanged. Table 1 lists the percent of Te in the wash, the concentration of Te transferred into the metal and the critical cooling curve temperatures. See Figure 5 for the graphic representation of the Te concentration and iron solidification relationship. A comparison of the actual curves (Fig. 6), illustrates that an increase in the Te content increases the undercool of the iron.

Microstructural Analysis

The graphitic structure of the samples are indicative of expected microstructures as the undercool and solidus temperatures are suppressed. Examples of the structures are shown for normal iron (Fig. 7a), 3% Te wash (Fig. 8b), and 9% Te wash (Fig. 7c).

It is apparent that small amounts of Te (when transferred evenly into the iron) will change the iron's solidification. This effect will change the way a section size reacts. The amount of Te transferred into the sample's matrix depends on the violence of the reaction, the time the iron remains liquid, and the amount of Te in the wash.

By remaining liquid for an extended time, none of the Te in the wash created a chilled boundary at the core/iron interface. All of the Te available either volatilized as a gas or was transferred evenly into the iron and acted as an alloy. The amount of the change in the solidification ranged from little (tendencies to type D graphite) to total type D and E graphite.

Experiment 4. Protective Ceramic Wash

Design and Procedure

To reduce the tendency of total Te transfer into the iron matrix (as seen in Experiment 3), a protective coat of core wash was applied over the Te wash. The core wash used was silica based ceramic type wash. Similar Te concentrations and the same metal handling procedures were used as in Experiment 3.

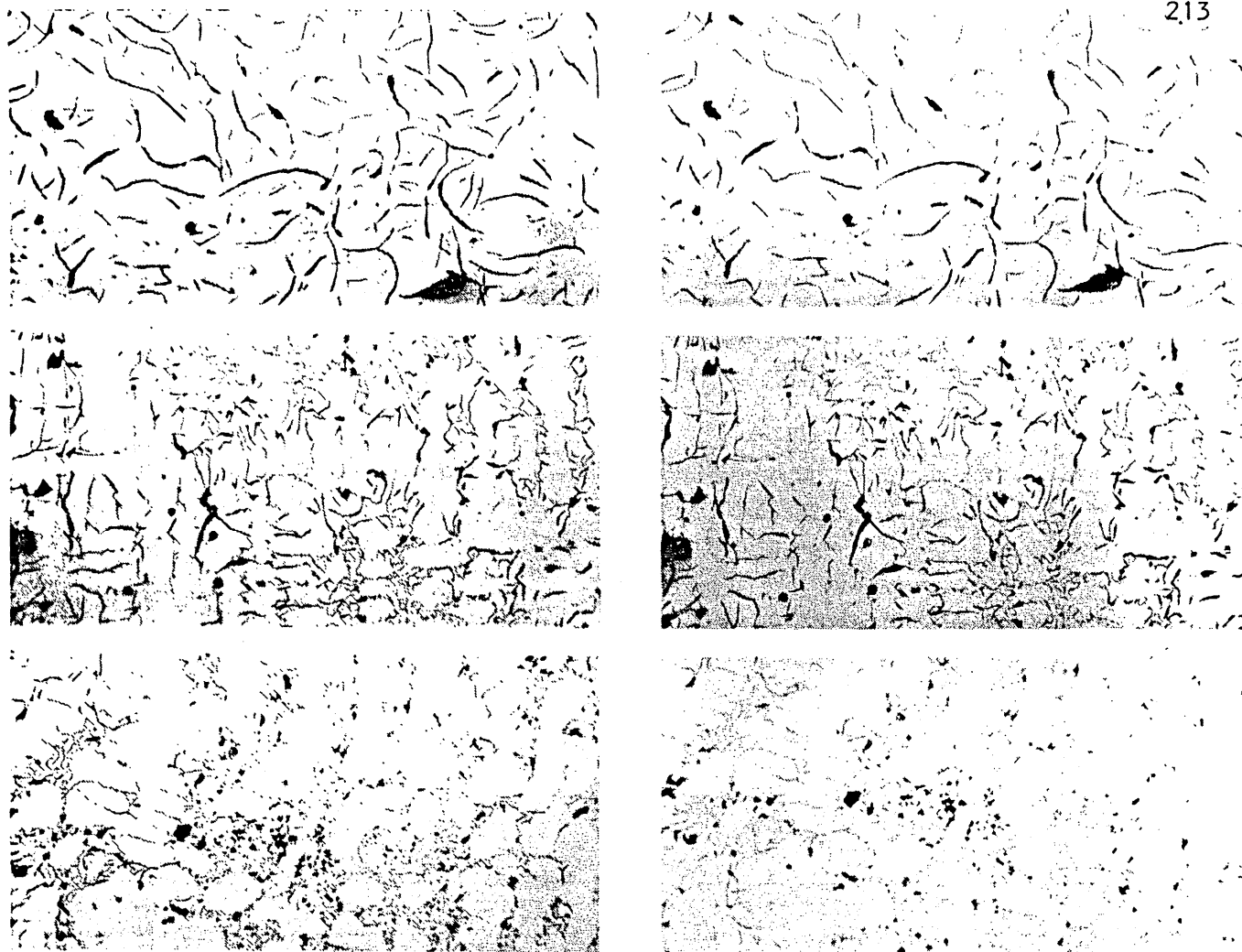


Fig. 7. Microphotographs showing the effect of Te on the microstructure.

Change in Solidification vs. Te Percent

The cooling curves for this test showed that a protective coat of core wash prevented any change in the solidification pattern at the center of the sample (Fig. 8). In addition, a chemical analysis of the sample did not show an increase in the Te concentration of the metal.

An interesting point should be noted: Although there wasn't a detectable increase in the Te content in the iron, there was a layer of chill at the core/iron interface. This indicated that the presence of the protective core wash either prevented the erosion of the Te or sufficiently delayed the gas/metal reaction.

For the percentages of Te over 4.0%, a chilled surface was produced. The 2.5% Te mixture showed evidence of Te; however, only slight amounts of chill were present and small quantities of mesh graphite to a depth of 0.4 mm were seen. The structures at the center of the sample were nearly identical for all samples. The general microstructure consisted of 95–100% type A graphite with 0–5% type D graphite. The reduction or loss of chill on the outside boundary of the test mold for percentages under 4.0% indicates

the reaction being delayed or redirected to prevent Te from being transferred to the metal at the iron/wash boundary. The fact that the Te was not mixed into the iron sample and was reduced at the iron/wash interface lends validity to the theory that the reaction is dependent on the ability of the gas to be transferred into the iron and the time the iron stays liquid.

Experiment 5. Premature Reaction

Design and Procedure

All previous tests evaluated the effectiveness of the Te wash on a horizontal surface. The horizontal surface was chosen to have the iron cover the Te as fast as possible to reduce the variable of fill rate on the effectiveness of the wash. This test was designed to determine the impact a vertical surface would have on the reactivity of the Te.

A test mold was designed to support a cylindrical core vertically in a test mold (see Fig. 9). The cylinder cores were made with a thermoplastic setting sand (shell sand) and the support mold was

SOLIDIFICATION CURVES

COOLING CUPS WITH TELLURIUM PROTECTED

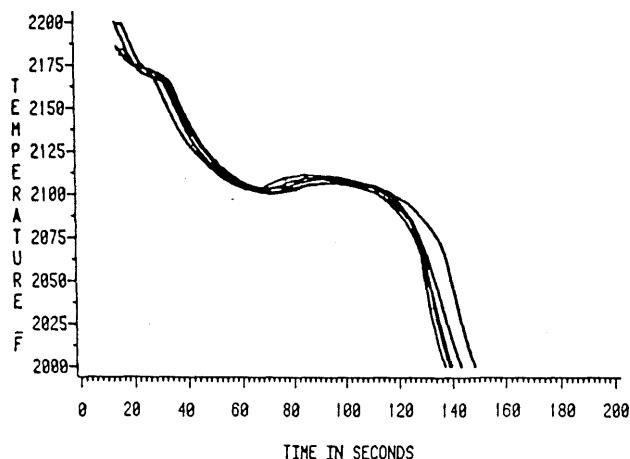
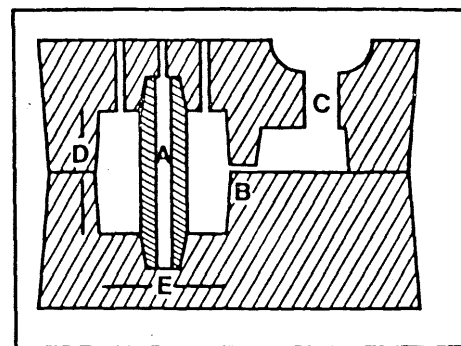


Fig. 8. Cooling curves when Te wash is protected with a ceramic wash compared to an unprotected wash.



- A 1" Shell Sand Core
- B 1/8" x 3/4" Ingate Dimensions
- C 3/4" Downsprue Diameter
- D 2 3/4" Height (7 cm.)
- E 3" Base Diameter

Fig. 9. Mold design for vertical filling test and protective overcoat of ceramic wash test.

made using the phenolic urethane cold box system. To reduce the variability of the application thickness, the cylindrical center cores were dipped into the appropriate concentration of Te wash. The fill rate of the mold was measured at one vertical centimeter per second.

Loss of Chill on a Vertical Plane

As the mold is filled with iron (7 sec.), the radiant heat of the molten iron preheats the core. As the core is heated prior to iron contact, the Te can begin to evolve (sublime) into a gas. Te oxide will evolve into a gas near 850F. If the Te reacts before any iron contact, there will not be a chill reaction. Figure 10 shows the chill depth versus the height on the vertical core for two concentrations of Te in the wash.

The amount of Te remaining to react with the iron is a function of 1) the initial amount of Te present; 2) the fill rate of the mold; and 3) how much of the Te is lost due to sublimation prior to iron contact. Figure 10 illustrates the location of the last chill reaction as the height on the core increases. If the point where the reaction stops is plotted versus the percent of initial Te concentration, a near linear relationship is seen.

Protective Washes and Vertical Applications

In an earlier experiment, it was observed that applying a protective wash over the Te wash slows down the Te/iron reaction. Various layering techniques were evaluated to determine the best protective scheme for vertical surface applications. The layering consisted of using 7.5% concentration Te wash and a silica-based ceramic wash. The following layering methods were used: 1) CORE—Te Wash—IRON; 2) CORE—Te Wash/Ceramic Wash/IRON; 3) CORE—Te Wash/Ceramic Wash/Te Wash/Ceramic Wash/IRON; and 4) CORE—Ceramic Wash/Te Wash/Ceramic Wash/Te Wash/IRON.

CHILL DEPTH COMPARISON VS CORE HEIGHT

1.0%, 5.0%, AND 7.0% TELLURIUM WASH

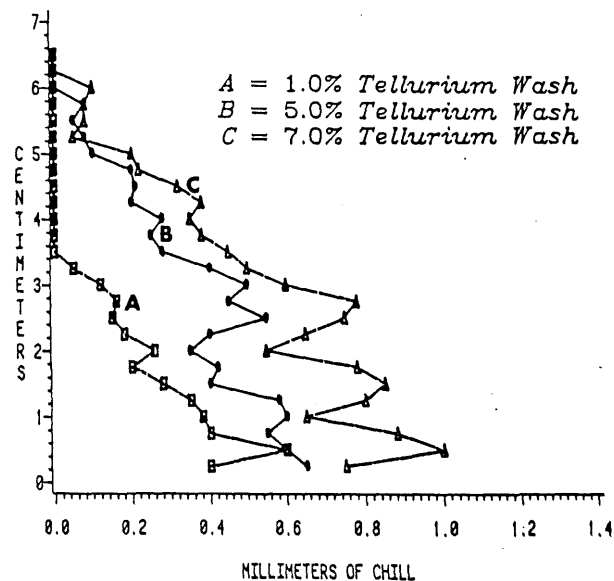


Fig. 10. Chill depth vs. height on a shell sand core for varying percents of Te.

HEIGHT OF ZERO CHILL VS TELLURIUM MEASURED FROM BASE OF CASTING.

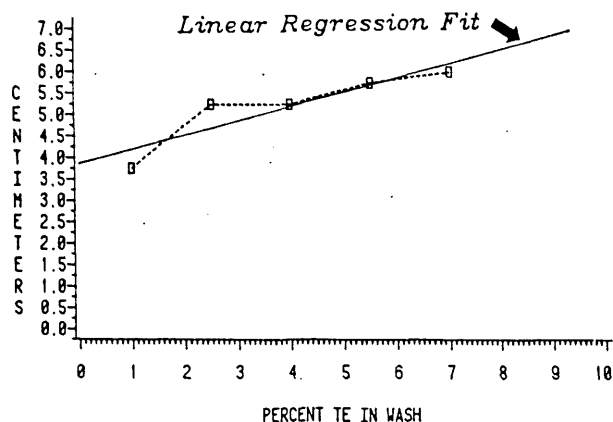


Fig. 11. Height of zero chill vs. percent of Te for various Te concentrations.

Figure 12 illustrates the chill depth for each layered method used. Note that there is some chill depth loss for all of the applications. The layering technique with the best results for consistent chill depth and minimal Te loss is the third method.

The reactivity of the Te wash can be controlled with the protection of a ceramic wash. Depending on the amount of protection and the layering used, successful depths of chill can be achieved for nearly any vertical application need. It should be noted that as the number of protection coats increases, the depth of chill provided decreases.

Experiment 6. Iron Chemistry

Design and Procedure

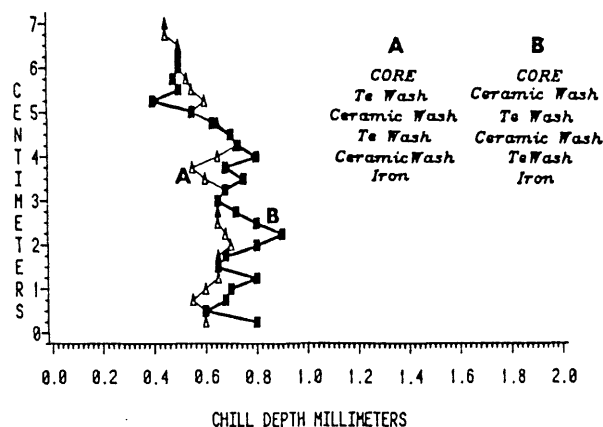
All previous tests were performed with an alloyed gray iron with a S content of 0.06–0.07%. When trying the Te wash with compacted flake graphite iron, it was noticed that regardless of the Te concentration used, no chill could be created. Based on some inferences from literature search, a test determining the effect of S content on the reactivity of the Te wash was designed.

Sulfur Content and Chill Depth

A test cylinder, 2.0 in. in dia. and 2.5 in. deep, was coated with a 3.0% Te wash. The iron was treated with iron pyrite to obtain levels of S from 0.06–0.14%. The test molds were poured and the structures were analyzed. The actual S contents and chill depth averages can be seen in Table 2.

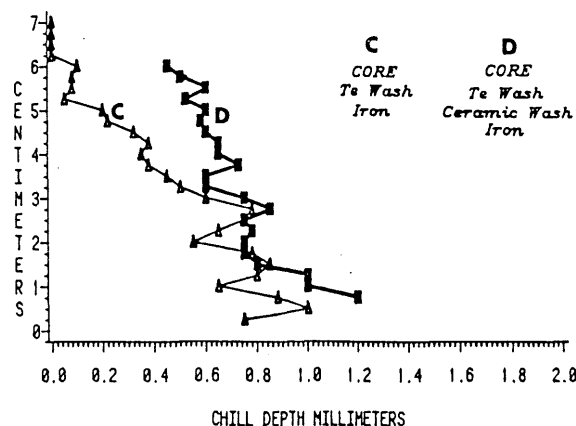
The chill depth was read six times around the circumference of the test specimen. The actual values were plotted and a linear regression performed. The results can be seen in Figure 13. This data (and the inability to get the Te to work in a low S CG iron) indicates a relationship between the S content and the effectiveness of the Te wash. It is necessary to have the correct chemical balance for the promotion of the carbide at the desired location. It is unknown at this point if the chill is a by-product of a S/Te reaction

CHILL DEPTH VS. HEIGHT OF CORE TE/WASH/TE/WASH VS. WASH/TE/WASH/TE



ACTUAL CHILL DEPTH PROFILE IN MILLIMETERS

CHILL DEPTH VS. HEIGHT OF CORE TE EXPOSED VS. TE UNDER 1 COAT OF WASH



ACTUAL CHILL DEPTH PROFILE IN MILLIMETERS

TRIANGLE = TE EXPOSED

SQUARE = UNDER 1 COAT OF WASH

Fig. 12. Chill depth vs. various methods of layering the Te wash and a ceramic core wash.

or the over abundance of Mn. Regardless of the stoichiometric relationship, Te washes do not work with nodular or compacted graphite irons.

EXPERIMENTAL RESULT DISCUSSION

Concentration

Concentration is one of the variables under the control of the user. By using a dilution wash (of the same basic materials as the Te wash), the final percent of Te can be adjusted to any desired value.

Table 2. Sulfur Content of the Iron with the Resulting Chill and Mesh Graphite Depths

Testing weight	Properties	Ref.
0.07-0.43 g/m ²	Excellent lacquer adhesion Minimum in mill. spec.	[6]
0.32-1.1 g/m ²	Top corrosion protection. Class 1	ASTM B449 72
0.11-0.38 "	Lacquer undersurface Class 2	
< 0.11 "	Transparent. Class 3	
1.08 g/m ²	chromic oxide corresponding to 6000 Å (0.6 micron thick film)	
1.08 g/m ²	chromium phosphate corresponding to 4800 Å (0.48 micron thick film)	

This is the first point of control in getting the desired chill depth and effect from the Te wash. The increase in the chill depth is 0.06 mm for each additional percentage of Te in the wash. The relationship is near linear between 1.0-10.0% Te. Below the 1.0% Te, the curve will drop dramatically to zero chill. For this reason, this range is not included in the evaluation.

Application

In addition to the concentration of the material, the application thickness is also under the control of the user. Depending on the application method and Baume, the final wash thickness can be varied as needed. Doubling the thickness of the Te wash will increase the depth of chill approximately 30%. This increase in chill depth is not dependent on the initial concentration of the Te wash. Multiple layers of a lower concentration Te wash will provide a more consistent depth of chill than a single layer of a higher concentration wash. This is primarily because the thickness of the applied wash is more consistent. It should be noted that multiple washes, unless applied and dried properly, can spall and migrate to unwanted areas. A decision must be made between the simplicity of a single (more potent) application or several less concentrated applications.

Solidification

The presence of Te as an alloy to gray iron will change the solidification of the iron. The extent of the change will depend on the concentration of the Te transferred to the metal. An increase of 0.004% Te transferred to the matrix is sufficient to increase the solidification rate of the iron. However, the amount transferred is dependent on the initial concentration, the application thickness, the presence of a protective wash and the mold fill rate. Without these variables being controlled, the amount transferred will be erratic and the results will be erratic.

At the wash interface, sufficient amounts of Te are transferred to induce primary carbide. This will decrease the time for the formation of the skin of solidified metal. The primary carbide at the wash interface should be expected as should the zone of mesh type graphite. Under tensile strength applications, careful evaluations should be made before using a Te wash. This is because the resulting boundary structure is lower in tensile strength (mesh graphite) and quite brittle (primary carbide).

Chemistry

The effectiveness of a Te based core wash is dependent on the

CHILL DEPTH AND SULFUR CONTENT TELLURIUM EXPOSED TO IRON CONTACT

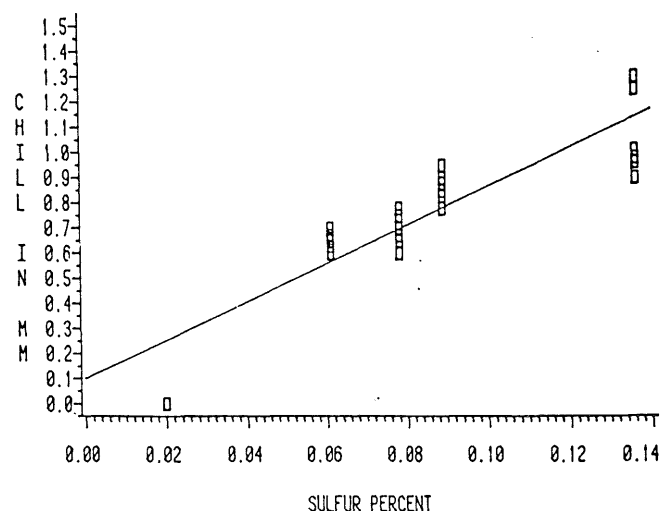


Fig. 13. Chill depth vs. the percent of S in the iron.

chemistry of the iron. Large swings in the CE will change the normal solidification of the iron and will also change the effectiveness of the Te wash. The single most important element that must be monitored is the S content. It is recommended that a minimum of 0.05% S be present in the base iron. Low sulfurs (like those seen in CG and ductile iron) are insufficient for the formation of primary carbide. High sulfurs (like those in cupola operations) require lower Te concentrations to achieve similar results. It is important to reduce large swings in the sulfur due to its effect on the reactivity of the Te.

Protective Core Wash

Because of the sublimation characteristics and the erosion properties of the Te wash, it may be necessary to protect the wash with a ceramic wash overcoat. This overcoat will delay the reaction from a time standpoint and will reduce the resulting chill depth slightly. If migration to an unwanted area or a premature reaction persists, it will be necessary to use the protective coat of ceramic core wash. Layering the Te wash with a ceramic wash can also be done to obtain the desired results. This should be performed on a part-by-part basis because the layering procedure is a labor and time intensive procedure.

SUMMARY

Te core wash is a sensitive and volatile method of solving shrinkage. Particular attention must be given to minimizing the adverse side effects and still maintain its effectiveness. With the necessary attention given to the application method and concentration, a Te-containing core wash is an effective way of preventing shrinkage.

When using a Te-containing core wash, it is necessary to evaluate the concentration of Te needed. Too much Te could result in migration to an unwanted area or too little Te could be ineffective in stopping the shrinkage. The coating thickness must also be

carefully monitored and controlled. Doubling or halving the application thickness results in a 30% change in chill depth. Protecting the Te core wash from erosion or premature reaction is also important. Without protection, the Te core wash can react prematurely or migrate to undesired areas. The pouring rate can also change the effectiveness of the chill depth. The faster the Te wash is covered, the lower the risk for erosion and the more consistent the chill depths will be.

The use of a Te-containing core wash should be evaluated on a casting-by-casting basis. Mold fill rates and solidification patterns are different for every casting and will affect the way the Te reacts. Subsequently, the application method and concentration may need to be different for individual castings.

DISCUSSIONS

Question: The author has shown how Te can be used to change chill depth but it only relocates the shrinkage. (R. C. Schnay, Pres., Robert Schnay & Assoc., Westmount, Quebec, Canada)

Answer: Being able to move or "relocate" a shrinkage defect from an area

that cannot be properly fed with liquid iron to an area that can be fed with iron does in fact solve the shrinkage problem. All a riser does is move the shrinkage from the casting into the riser. Mechanical chills provide the same function. Moving the shrinkage from an area where it cannot be fixed to an area where it can be fixed is, in effect, "solving" the problem.

Question: What analytical techniques were used to determine the concentration of elemental Te in the experimental washes? (G. Rudd, Tech. Dir., Motor Casting Co., Milwaukee, WI)

Answer: A gravimetric method is used where the Te oxide is precipitated from the solution and weighed back against the original weight.

Question: Does the treatment by Te change the graphite (chilling) sufficiently to provide machinability problems? (W. Ball, III, Cons., Hill & Griffith Co., Cincinnati, OH)

Answer: There are three zones of structure produced by a Te core wash: 1) primary carbide or chill; 2) mesh graphite (free of carbide); and 3) normal type A graphite. The primary carbide zone is not a desirable structure to machine. The carbide is brittle and hard, resulting in poor tool life. Concentrations insufficient to promote this carbidic reaction change the graphitic morphology only. As a result, there is not any appreciable change in the machining characteristics. It should be noted, however, that there is a fine line between the carbidic reaction and changing the graphite morphology and care should be exercised where determining the proper addition.