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TECHNICAL DEVELOPMENT OF DOUBLE-CLAD
PROCESS FOR THIN-STRIP CASTING OF CARBON STEEL

H. L. Brown
C. E. Forke1
D. L. Knudson

Idaho National Engineering Laboratory
Operated by the U.S. Department of Energy

Informal Report

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H. L. Brown
C. E. Forke
D. L. Knudson

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EG&G Idaho, Inc.
Idaho Falls, Idaho 83415

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ABSTRACT

This report documents the technical development for a patent disclosure of a double-clad process for the continuous casting of thin-strip carbon steel. The fundamental idea of the disclosure is to form a product strip by depositing molten steel between two, cooled, clad strips of the same material. The claimed benefits include; (a) the conservation of energy in steel making through the elimination of soaking pits and reheat cycles, and (b) an improved surface on both sides of the as-cast product such that it will be suitable for direct feed to a cold-reduction mill. However, the process as conceived is not necessarily limited to the casting of carbon steel, but may be also applied to other metals and alloys.

The work is described under three headings as follows:

"Preliminary Considerations and Scoping Analysis" presents the basic idea of the double-clad, thin-strip casting process; the energy conservation potential; scoping heat transfer calculations for the casting process; and independent review of this work.

"Thermal Analysis for Roller Configuration of Double-Clad Process," presents the development, results, and independent review of a finite-element thermal analysis for the casting process as originally conceived (using only chilled rollers in direct contact with the clad material of the product strip).

"Further Considerations for Belt Configuration of Double-Clad Process," deals with a modified equipment design which interposes two product support belts, one on each side of the product, between the clad strip and the rollers. In addition to the process description, this section presents the preliminary mechanical calculations for the endless metal belts and the work scope and results for the computer model revision and thermal analysis for the modified concept.

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- C. EG&G Idaho, Inc., RE-A-83-026, Thermal Analysis of A Proposed Continuous Thin-Strip Casting Process, D. L. Knudson, May 1983 .....
- D. Keverian International, Preliminary Report, Program Review: Mathematical Analysis of Thin-Strip Continuous Casting, D. L. Knudson, May 1983 .....
- E. EG&G Idaho, Inc., EDF TSC-2. Thin Strip Casting - Mechanical Scoping Calculations and Belt Selection for Belt Configuration Thermal Analysis, C. E. Forkel, November 16, 1983 .....
- F. EG&G Idaho, Inc., RE-A-83-026-SUP. 2, Thermal Analysis of a Proposed Belt-Supported Continuous Thin-Strip Steel Casting Process, D. L. Knudson, February 1984 .....

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EXECUTIVE SUMMARY

THERMAL ANALYSIS OF A CONCEPT FOR THIN STRIP STEEL CASTING

Introduction

A novel concept for production of thin metal sheet and strip by direct continuous casting has been investigated by the U.S. Department of Energy and a U.S. patent applied for. The concept has substantial energy saving potential over conventional strip/sheet production technology and represents a major step forward, since the expected product thickness falls between the lower limits of continuous casting as now practiced and the upper limits of rapid solidification technology (RST). Surface quality problems associated with some current RST products should be negligible since two strips of the composition being cast serve as integral molds for the molten steel. Product recycling to provide the strip material is approximately 20%.

Conventional strip/sheet production from molten steel proceeds from either an ingot or a section of a continuous-cast billet, with successive hot-working reductions to a thickness suitable for cold reduction and finishing. Either route generally involves reheat and temperature equalization (soaking) steps between reduction operations. The twin-strip approach promises to eliminate essentially all reheat requirements by casting a near-net thickness product which should require only minimal finishing. The energy requirements per ton of finished product are thus reduced substantially, perhaps by as much as 5×10^6 Btu compared with current continuous casting practice. This energy reduction translates

directly to an appreciable economic saving. Other economic benefits may also accrue, particularly for new or substantially refurbished strip/sheet production facilities, where both the capital and the operating costs of reheat and hot-working equipment can be avoided. However, no economic analysis of the twin-strip casting concept was performed during this study.

Concept Description

As previously stated, the concept involves casting molten steel into a mold partially comprised of two parallel metallic strips of suitable composition (normally the same as the molten steel). Edge containment for the molten metal is also provided. During temperature equalization, the hot metal is expected to bond to the strips, which then become an integral part of the product. The casting operation and the initial product cooling take place in an inert gas environment, to minimize oxidation.

Two equipment configurations have been considered. In the first, force-cooled twin rollers maintain product thickness and provide a primary heat sink; product support and supplemental cooling are provided by a large secondary roller and a series of positioning idlers. It must be emphasized that the product is cooled in this manner only enough to permit subsequent handling.

The second proposed equipment configuration uses twin belts, rather than rollers, for heat transfer and product support. Large, force-cooled drive rollers position the twin belts; belt support and partial cooling along the casting span is achieved by idlers or rigid support slabs. Again, only the minimum necessary cooling occurs.

For optimum thermal efficiency, direct finishing is desirable. Limited mechanical working of the hot product, if required, benefits its metallurgical structure while approaching the desired thickness; further thickness reduction by cold working as needed can follow.

Scoping Evaluation for Concept Patentability

The initial work on the concept was to support a patent application. First, possible physical configurations were examined in the context of expected product cooling and handling problems, and a practical-appearing version was selected. Nominal dimensions and limited process parameters for subsequent work were then established.

Next, an elementary thermal analysis (hand calculations), based on simplified assumptions, was performed. The results of nine different cases appeared to indicate that (1) the process was feasible, and (2) practical strip thicknesses could be produced when using 0.008 in. thick strips for the mold. This strip thickness is commonly used for can stock.

Independent Review

A detailed review of the concept and the scoping evaluation was performed by an independent consultant, who confirmed the technical feasibility of the concept and identified major technical issues to be resolved for concept development. The consultant concurred with the analytical approach and verified the calculations, but noted the need for computer simulation of the solidification process to examine a broad range of thermal and material property variations.

Detailed Thermal Analysis, Part I

Following the independent review of the concept and the scoping evaluation, a more detailed computer-based analysis was undertaken to refine the thermal aspects of the concept. All other technical aspects were beyond the scope of the overall investigation.

Approach: A two-part computer model of this process was developed to permit in-depth analysis of the thermal behavior of such a system. The first part of the model described the molten pool portion of the system where molten steel is poured between the clad; the second part

described the product (consisting of the cast core and clad). Figure 1 shows the primary equipment and its configuration for this process. The thermophysical properties used in conjunction with the model for this analysis were those of AISI-1020 steel, the largest-tonnage material for strip/sheet fabrication.

Tasks: The principal tasks for this analytical effort included:

- A. Development of the two-part model incorporating the following process parameters:
  1. Thermophysical properties for the desired steel composition
  2. Depth of the molten pool
  3. Casting roller radii
  4. Product width
  5. Bulk thermal conductivity of the molten pool
  6. Convection heat transfer coefficient from molten to solid steel
  7. Roller coolant temperature
  8. Convection heat transfer coefficient from the product to the surroundings
  9. Radiant emissivity to the surroundings
  10. Initial clad temperature
  11. Initial molten metal superheat temperature

12. Clad thickness
13. Product thickness
14. Product linear speed, in./sec
15. Molten pool boundary layer conductivity
16. Molten pool boundary layer profile
17. Ambient temperature
18. Contact coefficient between the product and the casting and the cooling rollers.

B. Use of the model to analyze the casting process for products in the thickness range 0.064 to 0.125 in. and nominal clad thickness of 0.008 in., and compare the results, assumptions, and method with those for the previous hand calculations.

C. A sensitivity analysis was performed to examine the effects of the last nine Task A parameters (10-18) on the process (without investigation of any interrelationships). For this task, the first nine parameters were fixed at presumably reasonable values based on the logic that their variation would have little effect on the process. Those parameters studied were varied as follows:

<u>Parameter</u>	<u>Nominal Value</u>	<u>Additional Values Considered</u>
Initial clad temperature, °F	500	1000, 1500
Initial molten metal superheat, °F	100	200, 300

Clad thickness, in.	0.008	0.012, 0.016
Product thickness	0.125	0.064, 0.070, 0.0775, 0.080
Product linear speed, in./sec	64	32
Molten pool boundary layer conductivity multiplier	2	1, 4
Molten pool boundary layer profile factor	1	0.25, 0.50
Ambient temperature, °F	500	100
Contact coefficient, Btu/hr-ft ² -°F	500	100, 2500

Results: The two-part model was developed and applied as planned. Examination of results, assumptions, and method used in the hand calculations versus the computer analysis indicated that the assumptions and method for the hand calculations were oversimplified, and therefore, a valid comparison of results from the two methods (hand versus computer calculations) was not possible. For this reason, the documented analytical work exclusively addresses the formal computer-based effort.

Variation of the Task 3 parameters over their stated ranges revealed that, for most of these parameters, the effects on the process are minimal. A definite interrelationship does exist between product thickness and clad thickness (hereafter referred to in terms of the molten-to-solid ratio), and has an appreciable influence on the overall process. The effect is not uniform for a given ratio as total product thickness is varied, since the heat removal requirement changes while heat transfer capability remains constant.

Calculations made with the model indicated that a 0.125 in. thick product cannot be cast with 0.008 in. clad because of clad failure by melt-through. A clad thickness of 0.012 in. is required in casting a 0.125 in. thick product to prevent melt-through although the minimum tensile strength available for product handling during casting may be

marginal in the proposed configuration. Product integrity and what was assumed to be more than sufficient minimum tensile strength during casting can be achieved using 0.016 in. clad to cast 0.125 in. thick strip. When the clad thickness was held at 0.008 in., the maximum product thickness which can be cast without clad melt-through was found to be 0.080 in. The current data base, however, is insufficient to make a prediction of product thickness versus clad thickness for lesser or greater product thicknesses, and the limited scope of this part of the analysis precluded further determinations.

Plots of product thickness versus clad thickness and molten/solid ratio are presented in Figures 2 and 3, respectively. (These plots and their interpretation were prepared specifically for use in an Executive Summary, and do not appear in the report of the computer modeling effort.) The nonlinear behavior with changing product thickness is apparent in both plots. In Figure 2, the solid curve represents a molten/solid ratio of 4.2, calculated from the minimum 0.012 in. clad thickness required in casting a 0.125 in. product. The broken curve connects the two data points which identify minimum clad thicknesses for 0.125 in. and 0.080 in. product thicknesses. The physical concept requires that this curve also pass through the origin. Any combination of clad thickness versus product thickness which lies above the broken curve could be cast without clad failure by melt-through. The minimum tensile strength encountered during casting of a particular clad/product thickness combination increases with distance from the broken curve.

Figure 3 shows more dramatically, for the same data, how the molten/solid ratio deviates from a constant value. In a practical sense, however, the trend of this curve for near-zero product thickness is uncertain and not meaningful. In this case, combinations of clad thickness versus product thickness which lie below the broken curve could be cast without clad failure by melt-through.

It should be noted that some of the legends on these plots have been abbreviated; "minimum clad thickness" is the minimum required for casting without clad melt-through, and "max m/s ratio for product thickness" is the maximum molten/solid ratio for casting a product of a given thickness without clad melt-through. A fine-scale evaluation of the process as a function of time indicated that partial (surface) melting of the clad would occur in any case, a desirable state in terms of achieving a good metallurgical bond between the solidifying metal and the clad.

Detailed Thermal Analysis, Part II

An alternate equipment configuration (Figure 4) proposed for the twin-strip casting concept necessitated an additional thermal behavior analysis to determine the effects of the equipment changes on product temperature equalization and heat transfer to the equipment.

The configuration change did not drastically alter the basic problem nor require modification of the existing model. The thermophysical and thermal property data already at hand from the earlier work were readily utilized. The principal equipment configuration change involved the use of twin belts (rather than rollers) for casting support. The belts studied were either 2 1/4 Cr 1Mo or Type 304 stainless steel. Either small support rollers or (optionally) a rigid support slab were used beneath the load-carrying portion (casting span) of each belt. One portion of the results of this analysis relates to maximum and minimum average service temperatures experienced by the two belt materials. This information is of major value for subsequent design engineering.

Case 1 assumed a Type 304 stainless steel support belt and support rollers beneath the casting span, while Case 2 substituted a 2 1/4 Cr-1Mo belt. (Belt thickness for the analysis was determined for an essentially infinite mechanical fatigue life from ASME Boiler and Pressure Vessel Code compilations). Use of the stainless belt was found to represent a more severe condition, for both temperature gradients and maximum belt and product takeoff temperatures. Case 3 substituted rigid support slabs for

the casting span support rollers, with appreciable reduction of the Type 304 belt temperature gradient and maximum temperatures. The cyclic belt service temperature ranges for the three cases, and the corresponding product takeoff average temperatures, are presented in Figure 5.

(Note: Figure 5 was prepared specifically for this summary after the final report of the computer modeling of the alternate equipment configuration was completed.)

Since formal data were not available, thermal properties of stainless steel were extrapolated over a nominal 200 to 300°F interval, and 2 1/4 Cr-1Mo thermal properties and yield strength data over a nominal 400 to 500°F interval. However, the availability of firm data for AISI-1020 steel for comparison purposes reduces errors from this source. Other factors and modeling assumptions (identified in the final report of the Part II effort) seem to have more of an impact on the results than these extrapolations.

Conclusions and Recommendations

The conclusions from these analyses are:

Part I - Wheel Casting

1. Continuous casting of thin-strip steel in the manner proposed is feasible from the thermal analysis standpoint. Changes in process equipment configuration and/or other non-thermal factors could affect these results and would have to be examined on a case-by-case basis.
2. The analysis was based on the best available thermophysical property data for the material considered. Since these properties are measured values, as used by the steel industry, it is felt that they are acceptable for use in this study. For certain complex thermal phenomena associated with the process, values were assumed and then bracketed to determine their effect

on the analysis. The assumed values appeared reasonable, since the results were essentially insensitive to variations of these values.

3. Further use of the thermal model in its present form appears desirable in two areas:

First, the maximum product thickness should be determined for additional clad thickness values outside the current 0.008 to 0.016 in. range. The benefits from this effort would be (1) an advance correlation of clad and product thickness dimensions within the range of anticipated continuous strip casting product development and (2) an effective means for guiding such development.

Second, the model should be used for a pre-operational check on a prototype continuous casting system, using actual design parameter values wherever possible. Such systems are now being studied under DOE contract. The proposed check could, at no physical risk, identify potential problem areas of operation. An additional benefit would be at least partial verification of the model through experimental investigation.

4. The model could also be expanded and refined economically to support evaluations of other systems such as those considered in the DOE-sponsored thin-strip casting development program now in progress. The primary benefit of this application would be an analytical check of other system designs, plus a cross-comparison of models where the contractor has developed his own process model.

Part II - Twin-Belt Casting

- o Casting 0.080 in. thick strip using 0.008 in. clad strips in the manner proposed is thermally feasible.

- o Belt heat capacitance is an important consideration for minimizing thermal stress effects.
- o The optional heat slabs appear more effective than the idler rollers in reducing temperature effects in the belts.
- o This equipment configuration promises substantially fewer problems with maintaining integrity of the solidifying product than the previous (all-roller) configuration.

Recommendations for Further Work on the Twin-Strip Concept

The following additional efforts are considered necessary for further development of this concept:

1. Experimental program design to verify analytical results
2. A laboratory-scale investigation to confirm concept feasibility.

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Figure 1. Proposed twin-roller continuous thin-strip casting equipment configuration (not to scale and not based on calculated thermal requirements).

Figure 2. Analytically derived product thickness versus clad strip thickness for continuous cast AISI-1020 steel strip.

Figure 3. Analytically derived product thickness versus molten/solid ratio for continuous cast AISI-1020 steel strip.

Figure 4. Proposed belt-supported thin-strip steel casting equipment configuration (not to scale). Belt orientation shown 45° from vertical.

Figure 5. Average belt cyclic temperature range and product average takeoff temperature for continuous casting support belt materials.

I. INTRODUCTION

Conventional production of steel strip or sheet proceeds from either an ingot or a section of a continuous-cast billet, with successive hot-working reductions to eventually achieve a thickness suitable for cold reduction and finishing. Either route generally involves reheat and temperature equalization steps between reduction operations. Elimination of these steps by direct casting to nearly finished dimensions would result in substantial energy savings, perhaps as much as 5×10^6 Btu per ton of finished strip or sheet product compared with the current continuous casting route. Related economic benefits include not only the value of the energy saved but also the capital and operating costs of both the reheat facilities and the hot-working equipment which are no longer needed.

A novel direct-casting concept which may permit achievement of these objectives has been investigated by the U.S. Department of Energy and a U.S. patent has been applied for. (See Exhibit 1) The concept has substantial energy saving potential over conventional technology and also represents a major step forward, since the expected product thickness falls between the lower thickness limits of continuous casting as now practiced and the upper thickness limits of rapid solidification technology (RST).

Concept Description

The concept involves casting molten steel into a moving mold partially comprised of two continuous thin carrier strips of the material being cast or other suitable composition. This approach eliminates two major problems: (a) Improper release of the product from the mold, with consequent damage to mold or product surface, and (b) inadequate surface quality on the unrestrained side of a product made on a single casting roll.

In the first of two configurations, each strip has a cooled backup roll. These rolls provide:

1. Edge constraint to the molten metal by suitable geometry.
2. Thickness dimension control to the mold
3. Initial heat removal capability through water or other liquid cooling.

Casting and primary cooling are done in an inert atmosphere. The major portion of primary cooling is achieved by transferring the strip in close contact with a large continuously cooled roll located generally below and close to the two casting rolls. Secondary cooling is by air.

In the second configuration, metal belts are substituted for the two casting rolls, and the large cooled roll is eliminated. Heat is transferred through the endless belts to the cooled drive system rolls, to cooled belt-support slabs (if used), and to the inert atmosphere surrounding the casting operation.

The cast strip can be turned from the original more-or-less vertical orientation to a horizontal attitude by means of guide rolls, which may also be cooled. Alternatively, the strip can be removed vertically through restraining rolls, with supplemental cooling as required. The assumed strip travel speed range is 1900 to 7600 in. per min, with 3800 in. per min the optimum rate. The strip is moved through the casting and cooling system by means of powered pinch rollers located beyond the cooling zone.

Thermal analysis of this concept was based on a carrier strip thickness of 0.008 in. Typical casting thicknesses were assumed to range from 0.060 to 0.125 in., giving molten-to-solid-metal ratios of 2.75 and 6.5, respectively.

Equipment Description

A tundish delivers molten metal through a suitable nozzle to the gap between two carrier strips of the same material being cast. In the twin-roll configuration, each strip is backed by a continuously cooled quench roll. A third continuously cooled roll is positioned closely below

to receive and cool the product strip. Auxiliary support rolls are used to hold the strip against the surface of the third roll and subsequently change the direction of strip travel as needed. The twin-roll equipment arrangement has been shown previously in Figure 1. Figures 6 and 7 present some equipment details.

In an alternate configuration, each carrier strip is backed by a thin continuous metal belt which travels over large-diameter cooled support rolls. Additional belt support within the casting zone is provided by either small support rolls or cooled support slabs which act as supplemental heat sinks. The twin-belt equipment arrangement has been shown previously in Figure 4.

In general, the ancillary systems needed to support operation of the twin-strip caster have their counterparts in conventional steel mill operations. A possible exception is the inert environment for casting and primary cooling, discussed only in principle. Conceptual design and development of such a system is outside the scope of the work reported here.

Note: Strip width exceeds short roll length and strip edges are crimped by quench roll pressure. Strips not shown in above sketch.

Edge of strip is tensioned between short quench roll and floating seal at pressure roller

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Figure 6a. Equipment details.

DETAIL OF FLOATING SEAL

INEL 3 0784

Figure 6b. Equipment details.

Cross Section of Edge - Quenching Rolls

Cross Section of Edge - Sealing Rolls

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Figure 7. Possible roll configurations for thin strip continuous casting.

Concept Applicability

The described concept of continuous casting strip feedstock for cold-rolling mills represents a new technology development and provides an intermediate product size range. Current technology produces billet sizes (for rework) exceeding 4-in. minimum cross section, or finished products (sheet or strip, wire, etc.) directly from rapid solidification technology less than 0.004 in. thick.

The new approach also permits improvement of surface condition on the very thin strip made by single wheel casting. Typically, the side which did not contact the quench roll has irregularities. Joining of two such strips in the manner described herein (irregular surfaces inward) with a minimum amount of molten metal yields an approximately double-thickness product with two good surfaces which retains most of the original structure and physical/mechanical properties. Repetition of the sequence permits production of thicker strip as needed.

Energy Conservation Potential

The estimated U.S. market for thin-strip (less than 0.030 in. thick) steel products is 10 million tons per year. Substantial energy savings (perhaps as much as 10 million Btu per ton) may be possible if continuous casting processes, now used for heavy sections, can be extended to direct production of thin strip.

Although the direct casting approach initially requires a supply of conventional cold finished strip, major energy savings would result from sharply reduced volume of ingot casting, soaking pit, and hot-rolling operations. Greater savings can be achieved if at least one of the carrier strips in the mold is continuous cast material. However, since this approach requires additional capital equipment based on a different (rapid solidification) technology and its output is currently limited to less than 0.003 in. thickness, the net benefit increase may be limited.

Patentable Features

Several aspects of the concept appear novel:

1. Continuous casting of molten metal in thin sections by using a mold comprised partly of carrier strips of the same material being cast.
2. Partial confinement of the molten metal by proper design of quench roll shape, either to eliminate exposure of molten metal at the strip edge by mechanically closing the gap or to accelerate cooling of the molten metal at the strip edge.
3. Use of argon or other inert gas for casting pressurization and supplemental heat removal.
4. Improvement of surface condition of single-wheel cast strips while retaining favorable physical and mechanical properties.
5. Buildup of continuous-cast very thin strip to desired thickness.

The notice of filing the invention disclosure in the U.S. Patent and Trademark Office on November 1, 1983 is contained in Exhibit 1.

Department of Energy
Washington, D.C. 20585

November 8, 1983

Mr. Harry L. Brown
Senior Engineer
EG&G Idaho, Inc.
P. O. Box 1625
Idaho Falls, Idaho 83415

DOE CASE S-57,789 - "THIN STRIP STEEL CASTING" - Ralph L. Sheneman

Dear Mr. Brown:

The above-identified invention disclosure was filed in the U. S. Patent and Trademark Office on November 1, 1983. At present, we have not received notification of the assigned serial number from the USPTO.

Sincerely,

A handwritten signature in cursive script, reading "Michael F. Esposito".

Michael F. Esposito
Patent Attorney
Office of the Assistant General
Counsel for Patents

Exhibit 1. Notice of Patent Filing.

References

A number of patents on continuous casting have been examined. These represent a variety of techniques and mechanical features which have been tried. None appear to involve the concept described herein, although such subjects as nozzle and tundish design, protective gas cover, and pouring parameter control may be applicable when the present concept is reduced to practice. A partial list of patents related to continuous casting of thin strip metal is given below.

<u>Patent Number</u>	<u>Subject</u>	<u>Inventor</u>
2 099 208	Gas Cover	--
3 284 859	Gas Cover	--
3 522 836	Meniscus Casting	King
3 605 863	Equipment	King
3 811 491	Method and Equipment	Gerding
3 831 659	Method	Gerding
3 845 810	Equipment	Gerding
3 895 673	Gas Cover	Gerding
3 952 928	Equipment	Gerding
4 007 462	Equipment	--
4 142 571	Equipment	--
0 040 069 (Europe)	Nozzle Design, etc.	Maringer
0 040 070 (Europe)	Nozzle Design, etc.	Maringer
0 404 071 (Europe)	Strip Marking	Maringer
0 040 072 (Europe)	Nozzle Design, etc.	Maringer

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II. PRELIMINARY CONSIDERATIONS AND SCOPING ANALYSIS

Scoping Analysis Summary and Conclusions

A process for continuous casting of thin metal strip by means of cooled quench rolls and integral carrier strips has been proposed. Thermal aspects of this concept and some possible process equipment configurations have been examined on a limited basis. Calculations were based on steel as the material, an assumed equipment configuration, and a product thickness which permits its use as input to a cold-finishing operation for thin-strip production. Because intermediate operations involving heavy sections of hot material are eliminated, the concept has promise for substantial energy saving. Novel features of the concept and some potential applications are identified.

The thermal behavior was examined for the material as it solidified upon passing through the primary (quench) rolls. The necessary cooling rates appear to be achievable, and the equipment configuration assumed permits secondary cooling as required.

Further analysis is needed to refine and extend the calculations. Such work should examine the effects of varying the essential parameters:

- o Casting speed (quench roll speed)
- o Quench roll diameter
- o Ratio of molten metal volume to carrier strip volume.

Computer programs are available which can be adapted to this work and would facilitate the reiterative calculations associated with thermal analysis.

In addition, the required amount of heat removal through additional cooling must be established for the expected range of quench roll speeds. This information is needed in order to design the heat removal capability and length of supported arc of the cast strip against the secondary roll.

Elevated temperature tensile strength of the cast strip against the secondary roll will partially control the ultimate configuration.

Scoping Analysis Statement of Work

A limited technical evaluation of a conceptual metallurgical process involving continuous casting of thin strip was performed. The process has potential for substantial energy savings over conventional ingot/hot-reduction processes. The investigation reported here is primarily thermal analysis of the process, with some work on equipment configuration and process cooling requirements.

Thermal behavior was studied for the interval from first pouring of liquid metal until the product has cooled sufficiently that its strength is adequate for subsequent mechanical handling. The assumptions on which the calculations are based represent practical conditions. A possible equipment configuration for the process has been developed as part of the necessary basis for the calculations. Figures 8, 9, and 10 and Tables 1 and 2 contain physical and mechanical property data used for this evaluation.

Figure 8. Tensile strength of carbon steel vs temperature.

Figure 9. Mechanical properties of steel from 70 to 2000°F. (From "Steels for High Temperature Service," U.S. Steel, 1952)

Figure 10. Enthalpy versus temperature for iron. (From "Handbook on Material and Energy Balance Calculations in Metallurgical Processes:", by H. Alan Fine and Gordon H. Geiger)

TABLE 1. THERMAL PROPERTIES OF METALS (FROM "HEAT TRANSFER," 2ND ED., BY ALAN CHAPMAN)

Metal	Properties at 68°F				k, Btu/hr-ft-°F									
	ρ $\frac{\text{lb}}{\text{ft}^3}$	c_p $\frac{\text{Btu}}{\text{lb}_m\text{-}^\circ\text{F}}$	k $\frac{\text{Btu}}{\text{hr-ft-}^\circ\text{F}}$	α $\frac{\text{ft}^2}{\text{hr}}$	-148°F -100°C	32°F 0°C	212°F 100°C	392°F 200°C	572°F 300°C	752°F 400°C	1112°F 600°C	1472°F 800°C	1832°F 1000°C	2192°F 1200°C
Aluminum														
Pure	169	0.214	132	3.665	134	132	132	132	132					
Al-cu (Duralumin) 94-96 Al, 3-5 Cu, trace Mg	174	0.211	95	2.580	73	92	105	112						
Al-Mg (Hyronalium) 91-95 Al, 5-9 Mg	163	0.216	65	1.860	54	63	73	82						
Al-Si (Silumin) 87 Al, 13 Si	166	0.208	95	2.773	86	94	101	107						
Al-Si (Silumin, copper bearing) 86.5 Al; 12.5 Si; 1 Cu	166	0.207	79	2.311	69	79	83	88	93					
Al-Si (Alusil) 78-80 Al; 20-22 Si	164	0.204	93	2.762	83	91	97	101	103					
Al-Mg-Si 97 Al; 1 Mg; 1 Si; 1 Mn	169	0.213	102	2.859	--	101	109	118						
Lead	710	0.031	20	0.924	21.3	20.3	19.3	18.2	17.2					
Iron														
Pure	493	0.108	42	0.785	50	42	39	36	32	28	23	21	20	21
Wrought iron (C<0.50%)	490	0.11	34	0.634	--	34	33	30	28	26	21	19	19	19
Cast iron (C~4%)	454	0.10	30	0.666										
Steel (C max~1.5%)														
Carbon steel C~0.5%	489	0.111	31	0.570	--	32	30	28	26	24	20	17	17	18
1.0%	487	0.113	25	0.452	--	25	25	24	23	21	19	17	16	17

TABLE 2. MECHANICAL PROPERTIES OF STEEL FROM 70 TO 2100 F (FROM "STEELS FOR ELEVATED TEMPERATURE SERVICE," U.S. STEEL, 1952)

Similar Specification Numbers	Heat Treatment	Chemical Composition	
A 161-46 A 192-44 A 106-47T A 201-47, A A 285-47, A & B A 53-47	Anneal 1400-1600F or Anneal 1200-1400F or Normalize 1650-1750F or Hot Roll	Carbon	0.08-0.20
		Manganese	0.30-0.80
		Phosphorus	0.045 max.
		Sulphur	0.060 max.
		Silicon	0.25 max.

TENSILE PROPERTIES (1)

Test Temperature F	0.2% Offset Yield Strength 1000 psi	Tensile Strength 1000 psi	Elongation % in 2 in.	Reduction of Area %
70	39.0	60.0	35	66
300	36.5	67.0	28	60
500	32.0	67.5	29	62
700	26.0	59.5	36	68
900	19.5	41.0	45	76
1100	12.5	21.0	57	86
1300	6.0	10.5	69	96
1500	2.0	5.5		
1700		3.5		
1900		2.6		
2100		2.3		
2300				

CREEP AND RUPTURE PROPERTIES (1)

Test Temperature F	Stress (1000 psi) for a Creep Rate of		Stress (1000 psi) for Rupture in	
	0.0001% per hr. (1% in 10,000 hrs.)	0.00001% per hr. (1% in 100,000 hrs.)	1,000 hrs.	10,000 hrs.
800	19.0	12.4		
900	9.0	6.7	19.0	
1000	3.5	2.8	11.7	6.8
1100	1.4	0.8	6.0	3.6
1200	0.6	0.2	2.6	1.5
1300			1.4	0.8
1400			0.8	0.5
1500				
1600				

EFFECT OF TIME AND TEMPERATURE ON NOTCH IMPACT STRENGTH AND HARDNESS (2)

	Unexposed	Exposed 1000 hrs at			Exposed 10,000 hrs. at		
		900F	1050F	1200F	900F	1050F	1200F
Charpy Keyhole Notch Impact Values (ft-lbs)	51	54	55	12	51	49	65
Brinell Hardness	112	108	101	96	100	89	80

Notes: (1)-These data represent not only tests conducted in various laboratories throughout the United States Steel Corporation, but also data reported in the literature.

(2)-All testing done at room temperature.
Material exposed without stress.

Scoping Heat Transfer Calculations and Review

In order to obtain a perspective of the practicability of the proposed process, a series of scoping heat transfer calculations was performed, assuming molten metal cast between two clad strips of 0.008 in. thick carbon steel. The calculations are divided into nine cases with assumptions and results as follows:

- Case I   The clad and melt come to adiabatic equilibrium 110°F below the melting temperature from an initial condition of the clad (carrier) at a temperature of 75° and the melt at 2910°F (110°F above the melting temperature). This results in a 2.9 ratio of melt-to-clad thickness; so that for 0.008 in. thick clad, the total thickness is 0.062 in.
- Case II   The same as Case I, except the final bulk average temperature of the clad is 2700°F (100°F below the melting temperature) while the melt is half solid and half molten. This results in a 5.5 ratio of melt-to-clad thickness; so for 0.008 in. clad, the total thickness is 0.103 in.
- Case III   A 12-in. deep pool of melt is assumed on top of two 24-in. diameter copper rolls and the throughput of 0.125 in. thick by 24-in. wide sandwich is equivalent to a volume of $8 \times 24 \times 60$ in. in one minute. This results in a sandwich speed of 3840 in./min (3.64 mph) which is used for all subsequent cases. The rolls turn at 50.9 rpm and the clad contacts them for one-fourth of their circumference for an arc length of 18.84 in. and a duration of 0.294 sec. The midplane temperature of the clad leaving the rolls is 2910°F (the same as the melt) and inspection of the published data implies that the residence time of clad contact with the molten metal would have to be reduced by a factor of ~ 200 in order to have any effect on the midplane clad temperature. It also implies that the clad must be cooled.

- Case IV This case is similar to Case III except that the depth of molten pool is reduced to 1.00 in. and the outer surfaces of the clad are maintained at a temperature of 125°F. Assuming the inner surface of the clad is maintained at the melting temperature of 2800°F, but solid, a pseudo-steady state condition of the clad is reached, having a linear temperature gradient after traveling 0.064 in. into the molten pool. This distance corresponds to a time interval of 0.001 sec and 0.3 degree of rotation of the rolls.
- Case V This case extends Case IV by examining a 24 in. wide by 1 in. long segment of the sandwich on one side of the sandwich midplane for the interval up to the pseudo-steady state condition. The heat removed from the melt during this 0.001-sec time interval is 15.7 Btu, and 3.6 Btu of this is transferred to one of the copper rolls.
- Case VI This case extends Case V for one side of the sandwich midplane to the condition of this half of the melt being solid and a linear temperature gradient from 125°F at the clad outer surface to 2800°F (melting point) at the sandwich midplane. The heat removed during this extension is an additional 114.8 Btu in a time interval of 0.064 sec. The total time, travel, and roll rotation to this point are 0.065 sec, 4.16 in., and 19.9 degrees, respectively.
- Case VII This case continues to extend Cases V and VI, cooling from only one side of the sandwich until all of the melt has solidified. There is a linear temperature gradient from 125°F at the outer surface of the cooled clad to 2800°F (melting temperature) at the opposite melt/clad interface and a constant temperature of 2800°F through the uncooled clad. The heat removed during this extension is an additional 57.1 Btu in a time interval of 0.182 sec. The total time, travel, and roll rotation at this point are 0.247 sec, 15.8 in., and 75.4 degrees, respectively.

Case VIII This case determines the cooling water rate required to cool the sandwich from both sides, leaving solid metal at 2800°F at the sandwich midplane. It involves two concurrent/series cooling processes; each consisting of Cases IV, V, and VI combined. The water rate for a temperature rise of 10, 20, and 30°F is 10,900 gpm, 5,450 gpm, and 3,630 gpm, respectively.

Case IX This case determines the cooling water rate required to cool the sandwich from only one side, leaving 2800°F solid metal at the uncooled melt/clad interface. This case is a combination of Cases IV, V, VI, and VII. The water rate for a temperature rise of 10, 20, and 30°F is 10,300 gpm, 5,150 gpm, and 3,430 gpm, respectively.

In-house EG&G review comments on these calculations include:

1. There should be enhanced heat transfer between the melt and the clad due to the hydrodynamic layer between the molten pool and the clad.
2. Heat transfer between the clad and the copper roll will be significantly affected by the pressure and surface finish. It has not been considered in these calculations; the assumption is made that the clad surface is maintained at 125°F.
3. Cases VI and VII use a product steel conductivity coefficient of $17.1 \text{ Btu/ft}^2\text{hr}^\circ\text{F}$ at the average temperature of 1460°F. This value, taken from Table 1 of EDF TSC-1, should be conservative: heat transfer will be enhanced at the lower temperatures in the gradient because the coefficient at the lower temperatures is as high as $31 \text{ Btu/ft}^2\text{hr}^\circ\text{F}$.
4. In view of the above considerations, the accuracy of the results is estimated to be within $\pm$ one order of magnitude.

The complete Engineering Design File, EDF No. TSC-1, for these calculations and the EG&G in-house review is attached as Appendix A.

Independent Review of Scoping Analysis

Dr. J. Keverian, Professor and Head, Department of Materials Engineering, Drexel University, Philadelphia, Pennsylvania was commissioned by the Department of Energy to perform a study to obtain an independent assessment of the technical feasibility of the double clad thin strip casting process. The objectives of the study as modified were:

1. Assess the technical feasibility of the proposed process.
2. Assess whether the process is new and/or innovative.
3. Assess which process features represent critical issues acting as obstacles in the reduction of the process to practice.
4. Assess the claimed energy conservation potential.
5. Provide solidification computer program listings.
6. Provide thermal and physical properties of steel.

The scope of the study was to review the foregoing articles of the scoping analysis to provide the technical opinion of Dr. Keverian whose experience and judgments provide the sole basis of the assessments.

As a result of his review, Dr. Keverian stated, "There were no errors found in the Forkel and Brown calculations. These methods were adequate for an initial design study; however, computer simulation of the solidification process is essential to examine a broad range of thermal and material property variations." His report further provides the following summary and conclusions:

"The Double Thin Strip Casting Process holds promise for eliminating the hot working operation in the production of steel resulting in significant energy conservation claims. In order to accomplish this objective while retaining present levels of productivity of conventional concast processes (i.e. same production rates) it is necessary to attain high heat transfer

and strip casting rates. These process requisites demand the solution to the following critical technical issues which can otherwise limit technical feasibility of the process:

- o Provide uniform molten metal flow control synchronized with casting rate without gas entrapment or gas film formation.
- o Provide non-gas-evolving carrier strip, traveling at high speed with tension control to allow cavity dimension control and avoidance of strain transmittal to solidifying steel.
- o Provide uniform heat transfer rates high enough to produce fine dendrite arm spacings or other structures to enable the elimination of hot working and attainment of productivity objectives.
- o Provide quench roll location and pressure to seal structural deficiencies resulting from shortfalls in solidification and/or molten metal processing.

It is the assessment of the author that the process features as listed on page 7 of the Design File are new and/or innovative. This judgment is not based on a Patent Search which should be conducted to establish the validity of this assessment.

The author has confirmed approximately 2×10^6 Btu/ton of the 10×10^6 Btu/ton energy conservation claimed by Forkel and Brown.

Solidification computer program listings and selected physical properties of steel have been located and made available in this report.

It is the assessment of the author (Appendix - Resume) that the process can be made to be technically feasible with the expenditure of significant development effort."

The full text of the body of Dr. Keverian's report is contained in Appendix B.

The authors note that the critical technical issues raised by Dr. Keverian are essential to a successful development and demonstration program. However, these issues lie beyond the assigned scope of the original effort as documented herein. For example, in the order presented, the probable causes of action are as follows:

- o Metal flow control will require a coordinated design of instrumentation and the tundish that will measure the liquid level of the molten pool so that it can be maintained by the quiescent addition of melt. (This may be state-of-the-art for conventional continuous strand casting.)
- o Achieving a satisfactory carrier strip surface condition may be approached by having frictional contact between the clad strips and the casting rollers upstream of the molten pool sufficient to develop the required tension and without assistance from the clad entering the pool and by previously degassing the clad strip--possibly by preheating.
- o Heat transfer problems will require special attention to the design and maintenance of a clean roller surface to minimize the clad/roller contact resistance combined with suitable design of the roller for transfer of the heat to the cooling medium.
- o Product integrity can best be ensured by a suitable solution to the first problem listed above to eliminate gas pockets from the product strip. Additionally, pressure can be applied by downstream rollers that produce some reduction of the thickness of the product after it has gained sufficient strength for slight hot working. (Again, this is state-of-the-art for thicker sections.)

III. THERMAL ANALYSIS FOR ROLLER CONFIGURATION OF DOUBLE-CLAD PROCESS

Roller Configuration Summary and Conclusions

As recommended in the previous section, a computer model of the roller configuration process was developed to permit prediction of the thermal behavior. The model was then used to assess the thermal effects of changing any one of nine different process parameters while holding all others constant. The model was also used to determine the maximum product thickness that could be continuously cast using 0.008 in. thick clad material.

On the basis of this analysis, it was concluded that:

1. Continuous casting of thin-strip steel in the manner proposed is feasible from the thermal analysis standpoint for a maximum product thickness of 0.080 in. when using 0.008 in. thick clad material. Changes in process equipment configuration and/or other non-thermal factors could affect these results and would have to be examined on a case-by-case basis.
2. The analysis was based on the best available thermophysical property data for the material considered. Since these properties are measured values, as used by the steel industry, it is felt that they are acceptable for use in this study. For certain complex thermal phenomena associated with the process, values were assumed and then bracketed to determine their effect on the analysis. The assumed values appeared reasonable, since the results were essentially insensitive to variations of these values.
3. Further use of the thermal model in its present form appears desirable in two areas:

First, the maximum product thickness should be determined for additional clad thickness values outside the current

0.008 to 0.016 in. range. The benefits from this effort would be: (1) an advance correlation of clad and product thickness dimensions within the ranges of anticipated continuous strip casting development under current DOE-funded contracts and (2) an effective means for guiding such development.

Second the model should be used for a pre-operational check on a prototype continuous casting system, using actual design parameter values wherever possible. Such systems are now being developed under DOE contract. The proposed check could, at no physical risk, identify potential problem areas of operation. An additional benefit would be at least partial verification of the model through experimental investigation.

4. The model could also be expanded and refined economically to support evaluations of other systems such as those considered in the DOE-sponsored thin-strip casting development program now in progress. The primary benefit of this application would be an analytical check of other system designs, plus a cross-comparison of models where the contractor has developed his own process model.

The full text of the technical report of this thermal analysis is contained in Appendix C.

Roller Configuration Statement of Work

The job description for the thermal analysis comprising this part of the work was modified to some extent according to the results obtained as the work progressed.

The original job description was as follows:

1. Verify Hand Calculations

- a. Prepare plan for modeling the molten pool contained within two bounding strips of 0.0015 to 0.015 in. thick clad which, in turn, are bounded by copper quench rolls. The depth of pool, superheat of the melt, production rate, total thickness of the produced strip, diameter of the quench rolls, and temperature of the quench rolls or (selectively) the temperature of the clad outer surface should be variable parameters. Also, the thickness of the clad used on one side of the product may be different from that used on the other side of the product. The results of the combined model and program shall be the temperature profile, ultimate strength profile, and the derivative or the integral of the ultimate strength profile across the thickness of the product strip. The results shall be given at regular intervals along the length of the product strip. The results shall also include the rate of heat removal by each of the quench rolls.

Provide design review of modeling plan including the value, source, or method of development of needed input data such as enthalpy, thermal conductivity coefficients, clad/roll contact resistance, etc.

- b. Prepare plan for modeling the product strip beyond the line-of-centers of the two quench rolls in "1.a" above with simultaneous cooling by a cooled copper roll on one side and by pressurized inert gas (argon) on the other side of the

product strip. The total thickness of the produced strip, selection of the sides being cooled by the copper roll and gas (respectively), residence time in contact with the roll and gas, temperature and pressure of the inert gas, and the temperature of the clad outer surface or roll temperature should be variable parameters. The results of the combined model and program shall be the same as listed in "1.a" above except that the rate of heat removal shall be for the roll on one side of the product strip and the inert gas on the other side for each application of this model.

Provide design review of modeling plan, including the same information as detailed in "1.a" above.

- c. Upon approval of the modeling plans described in "1.a" and "1.b" above, develop models for the two processes except for those aspects relating to the effects of the inert gas and roll contact resistance.
- d. Apply models to repeat the hand calculations already done, with assumed boundary conditions identical with the original assumptions on boundary conditions.

Provide a design review comparing the results and reconciling the discrepancies.

Transmit results by letter to DOE.

2. Expand Model to Describe Secondary System

Expand model to include the effects of the inert gas and apply roll contact resistance to the system and equipment schematic, again assuming a melt superheat of 100°F, a throughput per EDF Case III, a total product thickness of 0.125 in., but introducing the effects of contact resistance in combination with a roll temperature of 212°F.

Provide a design review, presenting the resulting temperature profile, strength profile, and derivative or integral of the strength profile, through the thickness of the product sheet at regular time intervals for a given point on the product.

3. Evaluation or Identification of Limits

- a. Repeat "2" above, varying the various parameters as requested to include a total of 12 cases, one-third of which require remeshing and two-thirds do not.
- b. Prepare and issue a report of the results from items "2" and "3a" above.
- c. Present status and results to DOE.

The job description for the thermal analysis was modified per the following excerpts from EG&G Idaho internal correspondence with regard to the Thin Strip Casting Program Status Report of 11/7/82:

"Progress:

Subtask 1c: Thermal analysis models were completed for the molten pool and for the product strip beyond the roll pinch point as previously planned in Subtasks 1a and 1b.

The model for the molten pool treated the pool as a heat source having convective heat transfer to the clad. This approach provided enhanced heat transfer, above pure conduction, to the clad - thus having the maximum potential for melting into the clad. However, it does not address solidification of the melt at the clad/pool interface. The model for the product strip beyond the pinch point was the same model without the convection heat transfer subroutine.

Subtask 1d: Application of the computer model to the hand calculations was unsuccessful in that it was demonstrated that the temperature gradient assumed for the clad could not be obtained in practice. For an outside clad temperature of 125°F, a melt temperature of 3054°F (300°F superheat), a clad thickness of 8 mils, and a pool depth of 3 in., the inside clad temperature was only 540°F maximum, and this was accompanied by a heat flux of 16×10^6 Btu/ft²hr. This flux is limited by the heat

transfer across the convective boundary layer in the molten pool. These results present the dual problem of (1) non-melt into the clad and (2) a heat flux more than a decade above what can be achieved with direct water cooling.

Task 2: The models were expanded to add roller contact resistance but not convective transfer to the inert gas. This was applied to a case having a pool temperature of 3054°F (300°F superheat), a clad thickness of 8 mils, and a pool depth of 3 in. This resulted in an inside clad temperature of 2419°F maximum and an outside clad temperature of 2350°F. These results were extrapolated to an inside clad temperature of 2754°F (melting) and a corresponding outside clad temperature of 2685°F (10°F below the solidus temperature).

Planned Action:

Further attempts to verify the hand calculations have been abandoned because of the demonstrated incompatibility of the assumed parameters.

Because of the very narrow margin between melting into the clad and melting through the clad, and on the assumption that it may not be necessary to actually melt into the clad to produce a useful product, it was decided to drop this requirement from the thermal analysis. Instead, the molten pool will be modeled in a detail not previously anticipated in order to better study the melt/solidification phenomenon. The revised model will predict (1) the solidification of the melt on the clad during the initial stages of contact and (2) any remelt and resolidification of the deposited metal, including melt into the original clad if such occurs, at a later time in the process. The convective boundary layer of the molten pool will then follow the location of the solid/molten interface.

The model beyond the roller pinch point will be revised to be compatible with the revised molten pool model.

A test case will be run through both revised models assuming a 2-in. deep pool of 3054°F melt, a product thickness of 0.125 in. proceeding at maximum speed, an 8 mil thick clad each side having an initial temperature of 500°F, and a surface contact coefficient to a steel roller and fixed coolant temperature."

Independent Review of Roller Configuration Thermal Analysis

The DOE contract with Dr. Keverian, Drexel University also included independent review of the thermal analysis performed by EG&G Idaho. His assessment of this analysis is contained in the following excerpt from his preliminary report.

"This is a preliminary report on EG&G activities in the thermal analysis of the double thin-strip casting process. The technical work of EG&G reviewed to date meets high professional standards. This assessment indicates that the model developed can meet its initial objectives for determining whether a cast thin strip has sufficient mechanical integrity to withstand the stress of passing through the casting machine. As a result of this review, additional effort, within the scope of this contract, will allow an assessment of the internal integrity/quality of the cast strip.

A detailed outline of work to complete this contract is included herein and is summarized as follows:

1. Compute the freezing front, freezing rate and temperature gradients to enable an assessment of the extent of centerline porosity/shrinkage and other aspects of the cast product as related to casting integrity and quality.
2. Confirm SINGLE as an acceptable computer program for continuous casting applications.
3. Review basic calculations and assumptions used in the EG&G thermal analysis with emphasis on the validity and effect of variations in boundary conditions.

Other critical issues determining the technical feasibility of the Double Thin Strip Casting Process are reviewed within the context of this project. These critical issues include:

- o Uniformity of molten metal flow without gas entrapment.
- o Non-gas-evolving carrier strip with tension control.
- o Uniform and high heat transfer rates.
- o Quench roll location and pressure to seal cast structural deficiencies."

The full text of Dr. Keverian's report and his transmittal letter of April 27, 1983 are contained in Appendix D.

IV. FURTHER CONSIDERATIONS FOR BELT CONFIGURATION OF DOUBLE-CLAD PROCESS

Belt Configuration Summary and Conclusions

This section presents details and results of a study to determine the thermal effects of adding two, endless, metal, product-support belts to the equipment configuration for a double-clad, thin-strip casting process. The belts are located on each side of the product strip, between the strip and the cooling rollers, as previously shown in Figure 4.

Preliminary mechanical calculations (Ref. Appendix E) were made to determine the thickness of the belt based on bending fatigue life (thermal stress fatigue was not considered), allowable belt tension, and a variety of belt materials. This work provides input to the thermal analysis. These calculations were based on ASME Pressure Vessel Code, Sec. III, Div. 1, procedures and data which cover austenitic stainless steel metal temperatures up to 800°F and ferritic steel metal temperatures up to 700°F. The results indicated:

- a. A Type 304 austenitic stainless steel belt subjected to bending fatigue at the specified temperatures could be 0.103 in. thick for a life of two weeks and 0.096 in. thick for infinite life.
- b. A 2 1/4 Cr, 1 Mo ferritic steel belt in this service could be 0.043 in. thick for a life of two weeks and 0.040 in. thick for infinite life.
- c. A 13 Cr ferritic steel belt offers no benefit over a 2 1/4 Cr, 1 Mo ferritic steel belt up to the 700°F limit of the available data.
- d. The 0.032 in. thick by 24 in. wide belt selected for the thermal analysis is adequate for 5,000 pounds tension with Type 304 austenitic steel up to 800°F and with 2 1/4 Cr, 1 Mo ferritic steel up to 700°F.

A thermal analysis was performed on three cases of the belt configuration per Figure 4. In each case the clad was 0.008 in. thick and the product thickness was 0.080 in. The product speed was 64 in./sec. A brief summary of the results appears below.

- a. Case 1, using Type 304 austenitic stainless steel belts 0.032 in. thick without heat sink slabs, yielded an average temperature through the thickness of the belt alternating between 858 and 1799°F and a 2123°F final average temperature through the thickness of the product. For a 24 in. wide caster the minimum single belt yield strength is 1926 lb and the exit product ultimate strength is 4230 lb.
- b. Case 2, using 2 1/4 Cr, 1 Mo ferritic steel belts 0.032 in. thick without heat sink slabs, yielded an average temperature through the thickness of the belt alternating between 992 and 1713°F and a 2109°F final average temperature through the thickness of the product. For a 24 in. wide caster the minimum single belt yield strength is 7278 lb and the exit product ultimate strength is 4331 lb.
- c. Case 3, using Type 304 austenitic stainless steel belts 0.032 in. thick in combination with heat sink slabs, yielded an average temperature through the thickness of the belt alternating between 715 and 1447°F and a 1984°F final average temperature through the thickness of the product. For a 24 in. wide caster the minimum single belt yield strength is 4738 lb.

The final report of this thermal analysis is contained in Appendix F.

The conclusions from this work are:

- a. The computer model developed for thermal analysis of the belt configuration of the double-clad, thin-strip casting process is now an operational tool for further studies of the process in various modifications of the configuration.

- b. The heat slab provides an appreciable advantage in reducing belt temperatures through effective supplemental cooling.
- c. Both the belt material selected and the total cooling capability for the casting system can influence the thermal cycling range (and its end points) for the support belts.
- d. The feasibility of the belt configuration of double-clad, thin-strip casting has been demonstrated from the perspective of heat transfer for product solidification. However, since in even the best case the thermal analysis indicates belt temperatures far in excess of those assumed for the preliminary mechanical calculations in Appendix E, the mechanical feasibility remains to be fully demonstrated.
- e. The mechanical feasibility of the belt should be the subject of further studies that reiterate the results of the thermal analysis. The following considerations are suggested in this regard:
  - o Since the exit strength of the product appears to be more than needed for further handling, decreasing its residence time in the caster by shortening the belt/product contact distance should reduce belt temperature.
  - o Larger cooling rolls would lower the belt temperatures by increasing residence time for belt/roller contact. They would also reduce the bending stress for belts of a given thickness - extending their lives.
  - o Longer belts, with the additional length occurring from the belt/product separation point to the belt/product recontact points, would extend belt life through a reduction of the bending cycle rate for a given product speed.

- o Thicker belts should reduce the range between the minimum and maximum belt temperatures by virtue of providing greater thermal capacitance for the product solidification part of the cycle. However, this must be balanced against the deleterious effects of increased bending stress range.
- o The cyclic nature of the temperature gradients (through the belt thickness) which are quantified by the thermal analysis should be used to determine its effect on belt fatigue.
- o The cyclic shear stress at the roller/belt interface caused by differential expansion/contraction of the roller/belt will likely affect the life of the belt, but the solution to this problem may lie beyond the scope of analysis and should be examined by experiment.

Belt Configuration Statement of Work

Based on the preliminary mechanical calculations discussed in the Summary and Conclusions, a thermal analysis encompassing the following work was undertaken.

The objective of this effort was to determine the thermal effects of adding two endless metal product-support belts to a double-clad, thin-strip casting process. This work involved modification of an existing computer model to predict the steady-state location of the liquidus and solidus temperatures in the strip as well as the temperature profiles in the product and the belts. Three test cases were analyzed, and the results documented. The work was pursued as described below.

Task 1 - Model Development

In this task, an existing computer model was revised to predict the thermal performance of a double-clad, thin-strip caster that includes product support belts. This revision also provided a capability of comparing the thermal performance of a heat sink slab versus belt support rollers in the caster. Validation of the revised model was also a part of this task.

Task 2 - Thermal Analysis/Heat Transfer Rate

This task investigated the heat transfer rates in the thin strip under the conditions given below.

Subtask 2.1 - The belts (supported by rollers) were assumed to be made of austenitic stainless steel.

Parameters of interest were:

Belt Material	Type 304 stainless steel
Belt Thickness	0.032 in.
Product Thickness	0.080 in.
Clad Thickness	0.008 in. each

Product Speed   64 in./sec.
Environmental Temp.   500°F

The heat transfer rate for the double-clad, thin-strip casting process at the above conditions was identified.

Subtask 2.2 - The support belts were assumed to be made of ferritic steel material, with support rollers as in 2.1. The belt material was 2 1/4 Cr, 1 Mo steel. Other parameters of interest were unchanged. The heat transfer rate for the double-clad, thin-strip casting process for these conditions was identified.

Subtask 2.3 - An additional analysis was performed to evaluate the effects of a heat sink slab versus the support rollers. This analysis was performed on the worst-case heat transfer rate as identified in Subtask 2.1 or 2.2. The heat sink slab temperature was maintained at a nominal 120°F. The heat transfer rate for this configuration was identified.

Task 3 - Reporting

This task involved the preparation and publication of a report presenting the findings of this effort.

APPENDIX A

EG&G Idaho, Inc., EDF TSC-1 THIN STRIP CASTING -- SCOPING
CALCULATIONS FOR HEAT TRANSFER, C. E. Forkel, July 1982

ENGINEERING DESIGN FILE

PROJECT FILE NO. _____

EDF SERIAL NO. TSC-1

FUNCTIONAL FILE NO. _____

DATE July 9, 1983

PROJECT/TASK THIN STRIP CASTING

SUBTASK Heat Transfer

EDF PAGE NO. 1 OF 43

SUBJECT

Scoping Calculations for Heat Transfer

ABSTRACT

This EDF presents scoping calculations for the heat transfer for molten metal cast between two clad strips of 0.008 inch thick carbon steel. The calculations are divided into nine cases with assumptions and results as follows:

Case I The clad and melt come to adiabatic equilibrium 110°F below the melting temperature from an initial condition of the clad (carrier) at a temperature of 75°F and the melt at 2910°F (110°F above the melting temperature). This results in a 2.9 ratio of melt-to-clad thickness; so that for 0.008 in. thick clad, the total thickness is 0.062 in.

Case II The same as Case I, except the final bulk average temperature of the clad is 2700°F (100°F below the melting temperature) while the melt is half solid and half molten. This results in a 5.5 ratio of melt-to-clad thickness; so for 0.008 in. clad, the total thickness is 0.103 in.

Case III A 12-in.-deep pool of melt is assumed on top of two 24-in. diameter copper rolls and the throughput of 0.125 in. thick by 24 in. wide sandwich is equivalent to a volume of 8 x 24 x 60 in. in one minute. This results in a sandwich speed of 3840 in./min (3.64 mph) which is used for all subsequent cases. The rolls

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AUTHOR <u>CE FORNELL</u>	DEPT. <u>5510</u>	REVIEWED <u>R. L. Moore</u>	DATE <u>7/9/82</u>	APPROVED	DATE

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ABSTRACT

turn at 50.9 rpm and the clad contacts them for 1/4 of their circumference for an arc length of 18.84 in. and a duration of 0.294 s. The midplane temperature of the clad leaving the rolls is 2910°F (the same as the melt) and inspection of the published data implies that the residence time of contact with the molten melt would have to be reduced by a factor of ~200 in order to have any effect on the midplane clad temperature. It also implies that the clad must be cooled.

Case IV This case is similar to Case III except that the depth of molten pool is reduced to 1.00 in. and the outer surfaces of the clad are maintained at a temperature of 125°F. Assuming the inner surface of the clad is maintained at the melting temperature of 2800°F, but solid, a pseudo steady state condition of the clad is reached, having a linear temperature gradient after traveling 0.064 in. into the molten pool. This distance corresponds to a time interval of 0.001 s and 0.3 degree of rotation of the rolls.

Case V This case extends Case IV by examining a 24 inch wide by 1 inch long segment of the sandwich on one side of the sandwich midplane for the interval up to the pseudo steady state condition. The heat removed from the melt during this 0.001 s time interval is 15.7 Btu, and 3.6 Btu of this is transferred to one of the copper rolls.

Case VI This case extends Case V for one side of the sandwich midplane to the condition of this half of the melt being solid and a linear temperature gradient from 125°F at the clad outer surface to 2800°F (melting point) at the sandwich midplane. The heat

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EDF PAGE NO. 3 OF 43**ABSTRACT**

removed during this extension is an additional 114.8 Btu in a time interval of 0.064 s. The total time, travel, and roll rotation to this point are 0.065 s, 4.16 in., and 19.9 degrees, respectively.

Case VII This case continues to extend Cases V and VI, cooling from only one side of the sandwich until all of the melt is solid. There is a linear temperature gradient from 125°F at the outer surface of the cooled clad to 2800°F (melting temperature) at the opposite melt/clad interface and a constant temperature of 2800°F through the uncooled clad. The heat removed during this extension is an additional 57.1 Btu in a time interval of 0.182 s. The total time, travel, and roll rotation at this point are 0.247 s, 15.8 in., and 75.4 degrees, respectively.

Case VIII This case determines the cooling water rate required to cool the sandwich from both sides, leaving solid melt at 2800°F at the sandwich midplane. This consists of two concurrent/series cooling processes; each consisting of Cases IV, V, and VI combined. The water rate for a temperature rise of 10, 20, and 30°F is 10,900 gpm, 5,450 gpm, and 3,630 gpm, respectively.

Case IX This case determines the cooling water rate required to cool the sandwich from only one side, leaving 2800°F solid melt at the uncooled melt/clad interface. This consists of a combination of Cases IV, V, VI, and VII. The water rate for a temperature rise of 10, 20, and 30°F is 10,300 gpm, 5,150 gpm, and 3,430 gpm, respectively.

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EDF PAGE NO. 4 OF 43**ABSTRACT**

Review comments on these calculations include:

1. There should be enhanced heat transfer between the melt and clad due to the hydrodynamic layer between the molten pool and the clad.
2. Heat transfer between the clad and copper roll will be significantly affected by the pressure and surface finish. It has not been considered in these calculations; the assumption is made that the clad surface is maintained at 125°F.
3. Cases VI and VII use a conductivity coefficient of 17.1 Btu/ft²hr°F at the average temperature of 1460°F. This should be conservative because heat transfer will be enhanced at the lower temperatures in the gradient because the coefficient at the lower temperatures is as high as 31 Btu/ft²hr°F..
4. In view of the above considerations, the accuracy of the results are estimated to be within $\pm$ one order of magnitude.

42 381 50 SHEETS 5 SQUARE
42 382 100 SHEETS 5 SQUARE
42 383 200 SHEETS 5 SQUARE

ENTHALPY OF IRON

Case I

Assume:

1. Two layers of clad material each having a thickness of $l_c = 0.008$ in.
2. The melt and clad are completely adiabatic.
3. The melt has a superheat of 60°C so that the temperature of the melt is $T_m = T_{\text{melt}} + 60$
 $= 1540 + 60 = 1600^\circ\text{C} = 2910^\circ\text{F} = (2800 + 110)^\circ\text{F}$
4. The combined final bulk temperature of the clad and melt is 60°C below the melting temperature so $T_{c+m_2} = T_{\text{melt}} - 60 = 1540 - 60$
 $= 1480^\circ\text{C}$
5. The initial temperature of the clad is $T_c = 25^\circ\text{C}$
6. The thermal expansion of the material can be neglected so that: $\text{mole} \times h$, or $h = K \times \text{mole}$

Then the enthalpy of the material at the above temperatures is:

$$H_{m_1} = 18.3 \text{ k cal/mol}$$

$$H_{c+m_2} = 13.6 \text{ k cal/mol}$$

$$H_{c_1} = 0.0 \text{ k cal/mol}$$

The thickness of the melt is then:

$$N_m = \frac{2 l_c (H_{c+m_2} - H_{c_1})}{(H_{m_1} - H_{c+m_2})}$$

$$h_m = \frac{2k_c (13.6 - 0.0)}{(18.3 - 13.6)}$$

$$= 5.79 k_c$$

$$= 5.79 \times 0.008 = 0.046 \text{ in.}$$

The total thickness is then:

$$h_t = 2k_c = h_m$$

$$= 2 \times 0.008 + 0.046 = 0.062 \text{ in.}$$

42 384 50 SHEETS 5 SQUARE
42 384 50 SHEETS 5 SQUARE
42 384 50 SHEETS 5 SQUARE
42 384 50 SHEETS 5 SQUARE

NATIONAL

$$4750 \text{ in/min} = 27 \text{ mph}$$

8

Case II

$$h_c = 0.103$$

$$h_c = 0.008$$

$$h_m = 0.087$$

$$h_c = 0.008$$

Assume same as Case I except:

1. The final average bulk temperature of the clad is $T_{c2} = T_{\text{melt}} - 60 = 1540 - 60 = 1480^\circ\text{C}$
2. Half of the melt remains molten.

Then the enthalpy of the material at the various state points is:

$$H_{m1} = 18.3 \text{ k cal/mol}$$

$$H_{m2} = 14.1 + \frac{1}{2}(17.6 - 14.1) = 15.8 \text{ k cal/mol}$$

$$H_{c1} = 0.0 \text{ k cal/mol}$$

$$H_{c2} = 13.6 \text{ k cal/mol}$$

The thickness of the melt is then:

$$\begin{aligned} h_m &= \frac{2h_c(H_{c2} - H_{c1})}{(H_{m1} - H_{m2})} \\ &= \frac{2h_c(13.6 - 0.0)}{(18.3 - 15.8)} = 10.9 h_c = 10.9 \times 0.008 = 0.087 \text{ in} \end{aligned}$$

The total thickness is then:

$$\begin{aligned} h_2 &= 2h_c + h_m \\ &= 2 \times 0.008 + 0.087 = 0.103 \text{ in} \end{aligned}$$

Ex. III

Req'd minimum speed

Current capacity handles the above quantity of steel at the rate of one per minute.

assuming the actual feed to the strip mill is 0.125 in. thick and 24 in. wide, the rate of speed is:

$$R = \frac{8 \times 24 \times 60}{0.125 \times 24} = 3840 \text{ in/min} = 320 \text{ ft/min}$$

$$= 19,200 \text{ ft/hr} = 3.64 \text{ mph}$$

assuming the actual strip is 0.100 in thick,

$$R_{A=0.100} = \frac{0.125}{0.100} R_{A=0.125} = \frac{0.125}{0.100} \times 3840 = 4800 \text{ in/min}$$

$$= 400 \text{ ft/min} = 24,000 \text{ ft/hr} = 4.55 \text{ mph}$$

$$R_{A=0.090} = \frac{0.125}{0.090} R_{A=0.125} = \frac{0.125}{0.090} \times 3840 = 5333 \text{ in/min}$$

$$= 444 \text{ ft/min} = 26,667 \text{ ft/hr} = 5.05 \text{ mph}$$

42 381 50 SHEETS 3 SQUARE
42 381 100 SHEETS 3 SQUARE
42 389 100 SHEETS 5 SQUARE
NATIONAL

Determine temperature profile in lad for the conditions shown below. This is a worst case for heating lad for the size roller involved

finished lad/melt sandwich
 $R_{c+m} = 0.125$ in = R

speed = R = 320 ft/min
 $R = 0.125$

C = arc of contact

$$= \frac{\pi d}{4} = \frac{\pi \times 2}{4} = 1.57 \text{ ft} =$$

τ = duration of contact

$$= \frac{C}{R} = \frac{1.57}{320} = 0.00491 \text{ min} = 0.294 \text{ sec}$$

$$= 0.0000818 \text{ hr}$$

$$n = \frac{R}{\pi d} = \frac{320}{\pi \times 2} = 50.9 \text{ rpm}$$

should be reduced. The next iteration will provide both by assuming the outside surface of the clad is maintained at 125°F by the copper rolls and that the effective depth of melt pool is only one inch.

Case IV

assume:

1. O throughput and speed at the rate of Case III; i.e.,

$$A = 0.125 \text{ in. (2 clads + melt)}$$

$$R = 3840 \text{ in/min} = 640 \text{ in/sec}$$

2. Copper rolls are per Case III

3. Clad thickness = $A_c = 0.008 \text{ in}$

4. Melt temp = $T_m = 2910^\circ\text{F}$ ($T_{\text{melting}}|_{\text{@ interface}} = 2899^\circ\text{F}$)

5. Initial clad bulk temp = $T_c = 125^\circ\text{F}$

6. Clad surface temp = $T_c|_{x=0} = 125^\circ\text{F}$

7. Depth of melt pool = $d_p = 1.0 \text{ in.}$

8. Franks depth of melt pool involved, the curvature of the copper rolls is negligible, so the pool and clad appears as below:

using the method of Section 4-8 (Schmidt Plot).
 Heat Transfer, J. P. Holman, Mac Graw Hill, Pg. 136
 to determine the progress of the temperature
 gradient through the clad; and taking data
 from Table A.1 (Thermal Properties of Metals),
 Heat Transfer, Alan J. Chapman, Macmillan
 Publishing Co., Inc.:

For steel $\alpha \approx 0.5\%$,

$$\begin{aligned}\alpha &= \text{thermal diffusivity} \\ &= 0.570 \text{ ft}^2/\text{hr} \times \frac{144}{3600} \\ &= 0.0228 \text{ in}^2/\text{sec}\end{aligned}$$

$$\begin{aligned}\rho &= 487 \text{ lb/ft}^3 \times \frac{1}{1728} \\ &= 0.28 \text{ lb/in}^3\end{aligned}$$

$$\begin{aligned}k &= 31 \text{ Btu/hr ft } ^\circ\text{F} \\ A &= 100^\circ\text{F}\end{aligned}$$

$$\begin{aligned}k &= 18 \text{ Btu/hr ft } ^\circ\text{F} \\ A &= 2260^\circ\text{F}\end{aligned}$$

$$c_p = 0.111 \text{ Btu/lb } ^\circ\text{F}$$

From Section 4-8 above:

$$\frac{(\Delta x)^2}{2(\Delta T)} = 2 \quad \text{where: } \Delta x = \text{the incremental thickness of clad between data points, in.}$$

$$\Delta T = \frac{(\Delta x)^2}{2 \times}$$

Δt = time interval
between jumps.

grandest literature,
see.

Then: $\Delta T = \frac{(0.001)^2}{2 \times 0.02288}$

$$= \frac{(1 \times 10^{-3})^2}{2 \times 0.0238} = 21.9 \times 10^{-6} \text{ m.}$$

The number of iterations for the temperature gradient when the sandwich has progressed $x_p = 1$ in. is based on the time for this progression t_p :

$$\tau = \frac{L_p}{2}$$

$$= \frac{1.00}{64.0} = 0.0156 \text{ sec}$$

$$= 15,000 \times 10^{-6} \text{ sec}$$

25. No. of iterations = $\frac{1}{4\pi}$

$$= \frac{15,600 \times 10^{-6}}{21.9 \times 10^{-6}} = 712$$

This is far too many to work to completion, but $\Delta x = 0.001$ in. and $\Delta T = 21.9 \times 10^{-6}$ sec are useful to determine the very short time effects of the melt-contract/roll - cooling phenomenon.

To determine the temperature gradient at each 100 μ sec, the number of iterations per gradient is:

$$\left. \begin{array}{l} \text{no. of iterations} \\ \tau = 100 \mu \text{ sec} \end{array} \right\} = \frac{100 \times 10^{-6}}{\Delta T} = \frac{100 \times 10^{-6}}{21.9 \times 10^{-6}} = 4.57 \approx 5$$

The iterations are shown on the following pages, and the results are tabulated on Pg. 18. The temperature gradient at initial clad "steady state" (lowest temperature gradient) is also tabulated for $\tau|_{ss}$.

Based upon the time history of the midpoints of the clad ($x = 4$ mils), the time to initial clad steady state ($\tau|_{ss}$) is estimated by plotting as shown on Pg. 19. The value found is:

$$\tau|_{ss} = 1000 \mu \text{ sec} = 0.001 \text{ sec.}$$

In this time the sandwich travels:

$$L|_{ss} = A|_{u=0.125} \tau|_{ss} = \left(\frac{3840}{60} \right) \times 0.001 = 0.064 \text{ in.}$$

This is equivalent to a subtended angle on a 24" dia drum of

$$\phi|_{ss} = \frac{L|_{ss}}{\pi d} \times 360 = \frac{0.064}{\pi \times 24} \times 360 = 0.3^\circ$$

TEMPERATURE AT DESIGNATED LOCATIONS, OF
 DISTANCE FROM CLAD OUTER SURFACE, IN IN.

TIME, μSEC.	0	1	2	3	4	5	6	7	8
0	125	125	125	125	125	125	125	125	2800
100	125	130	150	200	370	700	1230	2170	2800
200	125	205	295	480	730	1010	1600	2180	2800
300	125	270	460	680	970	1350	1780	2280	2800
400	125	340	560	830	1140	1500	1910	2350	2800
500	125	380	650	940	1260	1610	1980	2390	2800
T_{ss}	125	460	795	1130	1460	1795	2130	2465	2800

A-22

$$T_{ss} = 1,000 \mu \text{ SEC}$$

32-A-23

Case V

Determine the heat extracted from the melt and the melt/clad system and the heat gained by the clad during the time, $\tau = 1000 \mu\text{sec} = 0.001 \text{ sec}$, for the clad to come to initial equilibrium.

For this analysis we will consider a section of the composite clad/melt product as illustrated below, assuming symmetry and dealing with only one side of the composite product.

The considered volumes and weights are:

$$V_c = \text{vol. of clad} = 1 \times 24 \times 0.008 = 0.192 \text{ in}^3$$

$$W_c = \text{wt. of clad} = \rho V_c = 0.28 \times 0.192 = 0.0538 \text{ lb}$$

$$V_m = \text{vol. of melt} = 1 \times 24 \times 0.054 = 1.296 \text{ in}^3$$

$$W_m = \text{wt. of melt} = \rho V_m = 0.28 \times 1.296 = 0.3629 \text{ lb}$$

The temperature of the slab changes from condition "0" at $T_0 = 0$ to condition "1" at $\tau = 0.001$ sec during this time period as shown below:

The heat gained by the slab is:

$$Q_c \Big|_{\tau=0 \text{ to } 0.001} = \frac{1}{2} W_c \left[(h_{c1} \Big|_{x=0.008} - h_{c0} \Big|_{x=0.008}) + (h_{c1} \Big|_{x=0} - h_{c0} \Big|_{x=0}) \right]$$

$$h_{c1} \Big|_{x=0.008} = 14.1 \text{ kcal/mol} \times 32.23 = 454.4 \text{ Btu/lb}$$

$$h_{c0} \Big|_{x=0.008} = 0.2 \text{ kcal/mol} \times 32.23 = 6.4 \text{ Btu/lb}$$

$$h_{c1} \Big|_{x=0} = 0.2 \text{ kcal/mol} \times 32.23 = 6.4 \text{ Btu/lb}$$

$$h_{c0} \Big|_{x=0} = 0.02 \text{ kcal/mol} \times 32.23 = 0.6 \text{ Btu/lb}$$

$$\left[\frac{\text{kcal}}{\text{mol}} \times \frac{1000 \text{ cal}}{\text{kcal}} \times \frac{\text{mol}}{55.85 \text{ g}} \right] \times 4186.8 \frac{\text{J}}{\text{cal}} =$$

$$\left[74,965 \frac{\text{J}}{\text{kg}} \right] \div 2,326 \frac{\text{J/kg}}{\text{Btu/lb}} = 32.23 \text{ Btu/lb}$$

Therefore the above enthalpies can be converted to English units

$$\text{and: } Q_c = \frac{1}{2} \times 0.0538 \left[(454.4 - 6.4) + (6.4 - 6.4) \right]$$

$$\tau = 0 \text{ to } 0.001 \text{ sec}$$

$$= 12.1 \text{ Btu}$$

The heat transferred to the roll during time $\tau = 0$ to 0.001 sec can be determined from the temperature gradient between $x = 0$ and $x = 1 \text{ mil}$ by interpolating on Pg 18 to obtain values at 50 to 335 μsec .

$$\Delta T_{\text{avg}} = \frac{\Delta T_0 + 2 \left(\sum_{m=1}^9 \Delta T_m \right) + \Delta T_{10}}{20}$$

$$= \frac{0 + 2(5 + 80 + 145 + 215 + 255 + 285 + 305 + 320 + 328) + 335}{20}$$

$$= 211 ^\circ\text{F}$$

$$Q_{c-d} = \text{heat transferred from the lead to the roll during the period } \tau = 0 \text{ to } 0.001 \text{ sec.}$$

$$= A k (\Delta T_{\text{avg}}) (\Delta \tau) \left(\frac{1}{\Delta x} \right)$$

$$= \left(\frac{24}{144} \right) 31 \times 2.11 \times \left(\frac{0.001}{3600} \right) \left(\frac{12}{0.001} \right)$$

$$= 3.6 \text{ Btu}$$

The total heat extracted from the metal during the period $\tau = 0 - 0.001 \text{ sec}$ is:

$$Q_m = Q_{c-d} + Q_c$$

$$\tau = 0 \text{ to } 0.001 \quad \tau = 0 \text{ to } 0.001 \quad \tau = 0 \text{ to } 0.001$$

$$= 3.6 + 12.1 = 15.7 \text{ Btu}$$

Case VI

For the period of time beyond $\tau = 0.001$ sec, the cooling of the melt will be approximated by averaging the heat transfer rates for two boundary conditions. The first of these is shown below and represents the condition at $\tau = 0.001$ sec.

The heat transfer rate will be based on the temperature gradient across the clad.

$$q = Ak(\Delta T)\left(\frac{1}{\Delta x}\right) \quad \text{where } k \text{ is for the avg temp}$$

of $\frac{2800 + 125}{2} = 1460^\circ\text{F}$

or $k|_{1460^\circ\text{F}} = 17.1 \text{ Btu/hr ft } ^\circ\text{F}$

$$\left. \frac{q}{J} \right|_{\tau=0.001} = \left(\frac{24}{144} \right) \times 17.1 (2800 - 125) \left(\frac{12}{0.008} \right) = 11.44 \text{ MBtu/hr}$$

$$= 3,177 \text{ Btu/sec}$$

The result of these conditions is shown below and represents the condition at $\tau = \tau_f = \tau_{\text{melt}}$.

$$\left. \frac{q}{J} \right|_{\tau=\tau_f} = \left(\frac{24}{144} \right) \times 17.1 (2800 - 125) \left(\frac{12}{0.062} \right) = 1.48 \text{ MBtu/hr}$$

$$= 410 \text{ Btu/sec}$$

The average rate of heat removal is:

$$\begin{aligned} \dot{Q}_{\text{avg}} &= \frac{\dot{Q}_{\text{in}} = 0.001 \cdot \frac{1}{2} \cdot T = T_1}{2} \\ &= \frac{3,177 + 410}{2} = 1794 \text{ Btu/sec} \end{aligned}$$

The total heat entering the system with the clad and melt for the one inch strip on Pg 20 is:

$$\dot{Q}_{\text{entering}} = \dot{w}_c h|_{T=125^\circ\text{F}} + \dot{w}_m h|_{T=2910^\circ\text{F} = 1600^\circ\text{C}}$$

$$\dot{h}|_{T=125^\circ\text{F}} = 0.2 \cdot \frac{\text{kcal}}{\text{mol}} \times 32.23 = 6.4 \text{ Btu/lb}$$

$$\dot{h}|_{T=2910^\circ\text{F}} = 18.3 \cdot \frac{\text{kcal}}{\text{mol}} \times 32.23 = 589.8 \text{ Btu/lb}$$

$$\therefore \dot{Q}_{\text{entering}} = 0.0538 \times 6.4 + 0.3629 \times 589.8 = 214.4 \text{ Btu}$$

The total heat leaving the system with the clad and melt for the one inch wide strip on Pg 20 is:

$$\dot{Q}_{\text{leaving}} = (\dot{w}_c + \dot{w}_m) \frac{(\dot{h}_{\text{solid}}|_{T=1500^\circ\text{C} = 2800^\circ\text{F}} + \dot{h}_{\text{melt}}|_{T=2800^\circ\text{F}})}{2}$$

$$\dot{h}_{\text{solid}}|_{T=2800^\circ\text{F}} = 14.1 \cdot \frac{\text{kcal}}{\text{mol}} \times 32.23 = 454.4 \text{ Btu/lb}$$

$$Q_{\text{bearing}} = \frac{(0.0538 + 0.3629) \cdot 45.44 + 6.4}{2} = 96.0 \text{ lbs}$$

The total load to be removed by central bearing is

$$Q_{\text{removed}} = Q_{\text{bearing}} - Q_{\text{bearing}}$$

$$= 214.4 - 96.0 = 118.4 \text{ lbs}$$

The load to be removed from the sandwood bearing is given $\tau = 0.001 \text{ ksi}$ is

$$Q = Q_{\text{removed}} - Q_{\text{bearing}} = 118.4 - 3.6 = 114.8 \text{ lbs}$$

The time interval of required cutting of the $\tau_{0.001}$ is then approximately:

$$\Delta T = \frac{Q}{Q_{\text{bearing}}} = \frac{114.8}{1794} = 0.064 \text{ sec}$$

The total residence time is then

$$\tau_{\text{total}} = \Delta T + \tau = 0.001 + 0.064 = 0.065 \text{ sec}$$

The distance traveled by the sandwich during this time interval is:

$$L = R \left| \frac{\tau_r}{A} \right|$$

$$A = 0.125$$

$$= \left(\frac{3840}{60} \right) \times 0.065 = 4.16 \text{ in.}$$

This is the minimum sandwich/roll contact length for each of the two surfaces of the sandwich.

The arc of travel on a 24 inch dia. drum subtends an angle of:

$$\phi = \frac{L}{\pi d} \times 360 = \frac{4.16}{\pi \times 24} \times 360 = 19.9^\circ$$

Case VII

Consider an extension of the previous case in which the other half of the melt has been solidified by cooling from the same side of the sandwich previously considered. Treating this extension in the same manner as in the last calculation, the initial temperature gradient and heat transfer rate corresponds to the second condition of the previous calculation. Thus, the transfer rate is:

$$\left. \frac{q}{A} \right|_{x=0.066} = 410 \text{ Btu/sec.} \quad (Pg 25)$$

The result of these conditions is shown below:

The rate of Transfer at this condition is:

$$q_{\tau=T_f'} = \left(\frac{24}{144}\right)(17.1)(2800-125)\left(\frac{12}{0.117}\right) = 0.7819 \text{ Btu/hr}$$

$$= 217 \text{ Btu/sec}$$

The average rate of heat removal is:

$$q_{\text{avg}} = \frac{q_{\tau=0.066} + q_{\tau=T_f'}}{2}$$

$$\tau = 0.066 \text{ to } T_f'$$

$$= \frac{410 + 217}{2} = 314 \text{ Btu/sec}$$

The heat entering the system at $\tau = 0.065$ sec will consist of the sum of the heat entering with the two halves of the complete sandwich.

For the cooled half ($x = 0$ to 0.062 in.), the entering heat is the value of the leaving heat in the previous calculation. That is, from Case VI:

$$q_{\text{entering}} \Big|_{x=0 \text{ to } 0.062} = 96.0 \text{ Btu} \quad (\text{Pg 27})$$

For the remaining half of the sandwich between $x = 0.062$ and 0.125 in., the entering heat content will be that existing after the sandwich has passed the line-of-centers between the two copper drums, or after the sandwich has travelled:

$$\Delta_p = 1.00 \text{ in.} \quad (\text{Case II})$$

At a speed of:

$$\left. \frac{S}{\Delta} \right|_{\Delta=0.125} = 3840 \text{ in/min} = 64.0 \text{ in/sec} \quad (\text{Case III})$$

The first part of this $\Delta_p = 1.00$ in. travel is treated in Case II to the first "steady state condition" of the clad. The results are:

$$\left. \tau \right|_{ss} = 0.001 \text{ sec}$$

$$\left. L \right|_{ss} = 0.064 \text{ in}$$

$$\left. \phi \right|_{ss} = 0.3^\circ$$

From Case I, the heat transferred the clad to the drums, Q_{c-d} , and the total heat extracted from the metal, Q_m , are:

$$Q_{c-d} \Big|_{\tau=0 \text{ to } 0.001} = 3.6 \text{ Btu} \quad (Pg. 23)$$

$$Q_m \Big|_{\tau=0 \text{ to } 0.001} = 15.7 \text{ Btu} \quad (Pg. 23)$$

The total residence time of contact between the side of the sandwich and the drum is:

$$\tau = \frac{dp - L/2}{R} = 0.125$$

$$L = 0.064 \text{ to } 1.00$$

$$= \frac{1.000 - 0.064}{64.0} = 0.015 \text{ sec}$$

The rate of heat transfer for $L = 1.00$ in and $\tau = (0.001 - 0.015) = 0.016$ sec will be interpolated from previous data.

$$q|_{\tau=0.001} = 3,177 \text{ Btu/sec} \quad (\text{Case VI})$$

$$q|_{\tau=0.066} = 410 \text{ Btu/sec} \quad (\text{Case VII})$$

Then:

$$q|_{\tau=0.016} = 3,177 - \left(\frac{0.016 - 0.001}{0.066 - 0.001} \right) (3,177 - 410)$$

$$= 2,538 \text{ Btu/sec}$$

And the average rate of heat transfer is:

$$q_{\text{avg}}|_{\tau=0.001 \text{ to } 0.016} = \frac{q|_{\tau=0.001} + q|_{\tau=0.016}}{2}$$

$$= \frac{3,177 + 2,538}{2} = 2,858 \text{ Btu/sec}$$

and the first transfer is:

$$Q_1 = \Delta T_{\text{avg}} = \frac{1}{2} (T_1 + T_2) = \frac{1}{2} (0.016 + 0.016) = 0.016$$

$$= 42.9 \text{ BTU}$$

For the relatively unsteady flow of air conditions, the unitary heat is:

$$Q_{\text{unitary}} = Q_{\text{unitary}} - Q_{\text{unitary}} = 0.062 \text{ BTU} \quad \text{Ex. VII}$$

$$= 2144 - 3.6 - 42.9 = 167.9 \text{ BTU} \quad (P_{224,23})$$

The total heat entering the system at time $t = 0.065 \text{ sec}$ is therefore:

$$Q_{\text{unitary}} = Q_{\text{unitary}} + Q_{\text{unitary}} = 0.062 \text{ BTU} \quad \text{Ex. VII}$$

$$= 96.0 + 167.9 = 263.9 \text{ BTU}$$

The condensed volume and weight of the gas must be long by 84 in. with total condensation components (see Ex. V) are:

$$V_2 = \text{vol of fresh air} = 1 \times 24 \times 0.008 = 0.192 \text{ m}^3$$

$$V_2 = \text{vol of fresh air} = (V_2 = 0.28 \times 0.192 = 0.0538 \text{ m}^3)$$

$$w_{m+} = \text{total vol of metal} = 1 \times 24 \times 0.109 = 2.616 \text{ in}^3$$

$$w_{m+} = \text{total vol of metal} = \rho_{m+} = 0.28 \times 2.616 = 0.7325 \text{ lb}$$

The total heat leaving the system with the sand which at $T = T_f$ is:

$$\begin{aligned} Q_{\text{leaving}} \Big|_{\tau=0 \text{ to } 0.125} &= (w_c + w_{m+}) \frac{(h_{\text{solid}} \Big|_{T=2200^\circ\text{F}} + h \Big|_{T=125})}{2} + w_c h_{\text{solid}} \Big|_{2200^\circ\text{F}} \\ &= (0.0538 + 0.7325) \frac{(454.4 + 6.4)}{2} + 0.0538 \times 454.4 \\ &= 181.2 + 24.4 = 205.6 \text{ Btu} \end{aligned}$$

The heat removed from the sand which in the interval $\tau = 0.065 \text{ to } T_f$ is:

$$\begin{aligned} Q_{\text{removed}} \Big|_{\tau=0 \text{ to } 0.125} &= Q_{\text{entering}} \Big|_{\tau=0 \text{ to } 0.125} - Q_{\text{leaving}} \Big|_{\tau=0 \text{ to } 0.125} \\ &= 262.7 - 205.6 = 57.1 \text{ Btu} \end{aligned}$$

At the average rate of heat transfer the duration of this interval is:

$$\begin{aligned} \Delta \tau \Big|_{\tau=0.065 \text{ to } T_f} &= \frac{Q_{\text{removed}} \Big|_{\tau=0 \text{ to } 0.125}}{\dot{q}_{\text{avg}} \Big|_{\tau=0.065 \text{ to } T_f}} \\ &= \frac{57.1}{314} = 0.182 \text{ sec} \end{aligned}$$

and the total residence time to this point is:

$$\tau' = \tau + \Delta \tau$$

$$\tau = 0.15 \text{ to } \tau'$$

$$= 0.065 + 0.182 = 0.247 \text{ sec.}$$

The total distance traveled by the sandwich during this time interval is:

$$L = 2 \tau'$$

$$\tau = 0 \text{ to } 0.247 \quad L = 0.125$$

$$= \left(\frac{3840}{60} \right) \times 0.247 = 15.80 \text{ in.}$$

The arc of travel on a 24 inch dia. roll subtends an angle of:

$$\phi = \frac{L}{\pi d} \times 360$$

$$\tau = 0 \text{ to } 0.247$$

$$= \frac{15.80}{\pi \times 24} \times 360 = 75.4^\circ$$

Case VIII

Determine the water rate required to cool the clad and melt from both sides, leaving solid melt at the center of the sandwich. This consists of two concurrent/series cooling processes; each consisting of Cases IV, V, and VI combined. The total heat to be removed is then:

$$Q_r = 2 \left(Q_{c-d} \Big|_{\tau=0 \text{ to } 0.001} + Q \Big|_{\tau=0.001 \text{ to } 0.065} \right)$$

$$= 2 Q_{\text{removed}} \quad (\text{Pg 27})$$

$$= 2 \times 118.4 = 236.8 \text{ Btu (per inch length)} \quad (\text{Pg 27})$$

Since this heat is removed from each inch of sandwich and the speed of the $t = 0.125$ in. thick sandwich is $2 \Big|_{t=0.125} = 3840 \text{ in/min}$, the rate of heat removal by cooling water is:

$$\dot{Q} \Big|_{t=0.125} = Q_r \cdot 2 \Big|_{t=0.125}$$

$$= 236.8 \times 3840$$

$$= 909,300 \text{ Btu/min}$$

$$= 15,200 \text{ Btu/sec.}$$

For a water temperature rise of $\Delta T = 10^\circ\text{F}$, the weight rate of water flow is:

$$\begin{aligned} w &= \frac{q \cdot \eta = 0.125}{\eta = 0.125 \cdot C_p \Delta T} \\ \Delta T &= 10^\circ\text{F} \\ &= \frac{909,300}{1.0 \times 10} = 90,930 \text{ lb/min} \end{aligned}$$

The volume rate of flow is:

$$\begin{aligned} v &= \frac{w}{\rho} \\ \eta &= 0.125 \\ \Delta T &= 10^\circ\text{F} \\ &= \frac{90,930}{62.3} = 1,460 \text{ ft}^3/\text{min} \\ &= 10,900 \text{ gpm} \end{aligned}$$

Extending the data for $\Delta T = 20^\circ\text{F} + 30^\circ\text{F}$:

$$\begin{aligned} v &= 10,900 \left(\frac{10}{20} \right) = 5,450 \text{ gpm} \\ \eta &= 0.125 \\ \Delta T &= 20^\circ\text{F} \end{aligned}$$

$$\begin{aligned} v &= 10,900 \left(\frac{10}{30} \right) = 3,630 \text{ gpm} \\ \eta &= 0.125 \\ \Delta T &= 30^\circ\text{F} \end{aligned}$$

Case IX

Determine the water rate required to cool the clad from one side only, leaving solid melt at the melt/clad interface at $x = 0.0117$ in. from the cooled surface of the clad. This consists of a combination of Case IV, V, VI, and VII. The total heat to be removed is then:

$$Q'_1 = 2Q_{\text{entering}} \Big|_{\substack{x=0-0.062 \\ T=0}} - Q_{\text{leaving}} \Big|_{\substack{x=0-0.125 \\ T=0.182 \text{ sec.}}}$$

$$= 2 \times 214.4 - 205.6 \quad (\text{Pg 26, 34})$$

$$= 223.3 \text{ Btu (per inch length)}$$

Since this heat is removed from each inch of sandwich and the speed of the $t = 0.125$ in. thick sandwich is $\frac{1}{t} = 3840$ in./min, the rate of heat removal by cooling water is:

$$\begin{aligned} q \Big|_{t=0.125} &= Q'_1 \frac{1}{t} \Big|_{t=0.125} \\ &= 223.3 \times 3840 \\ &= 857,500 \text{ Btu/min} \\ &= 14,300 \text{ Btu/sec.} \end{aligned}$$

For a water temperature rise of $\Delta T = 10^\circ\text{F}$, the weight rate of water flow is:

$$\begin{aligned} w|_{\substack{A=0.125 \\ \Delta T=10^\circ\text{F}}} &= \frac{q|_{A=0.125}}{C_p \Delta T} \\ &= \frac{857,500}{1.0 \times 10} = 85,750 \text{ lb/min} \end{aligned}$$

The volume rate of flow is:

$$\begin{aligned} v|_{\substack{A=0.125 \\ \Delta T=10^\circ\text{F}}} &= \frac{w}{\rho} \\ &= \frac{85,750}{62.3} = 1,380 \text{ ft}^3/\text{min} \\ &= 10,300 \text{ gpm} \end{aligned}$$

Extending the data to $\Delta T = 20^\circ\text{F}$ and 30°F :

$$v|_{\substack{A=0.125 \\ \Delta T=20^\circ\text{F}}} = 10,300 \left(\frac{10}{20} \right) = 5,150 \text{ gpm}$$

$$v|_{\substack{A=0.125 \\ \Delta T=30^\circ\text{F}}} = 10,300 \left(\frac{10}{30} \right) = 3,430 \text{ gpm}$$

APPENDIX B

J. Keverian, TECHNICAL FEASIBILITY ASSESSMENT OF THE DOUBLE
THIN STRIP CASTING PROCESS, January 1983

TECHNICAL FEASIBILITY ASSESSMENT
OF THE
DOUBLE THIN STRIP CASTING PROCESS

ASSESSMENT

by

Dr. J. Keverian

Professor and Head
Department of Materials Engineering
Drexel University
Philadelphia, PA 19104

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[Author's Note: The appendices to Dr. Keverian's original report have been omitted, either as duplications of information presented elsewhere in this technical development summary or as not directly applicable to this summary.]

I. EXECUTIVE SUMMARY

A. Objectives

This study was commissioned by the Department of Energy to obtain an independent assessment of the technical feasibility of a process developed by the Department of Energy. This process is proposed for the continuous casting of thin steel strip at production rates comparable to conventional continuous casting processes. Since the new process would cast the steel at dimensions close to the semi-finished sheet product, significant potential for energy savings are claimed. The objectives of this study as originally discussed were to:

1. Assess the technical feasibility of the proposed process.
2. Assess whether the process is new and/or innovative.
3. Assess which process features represent critical issues acting as obstacles in the reduction of this concept to practice.
4. Assess the claimed energy conservation potential.

Later the objectives were expanded to provide the following information:

5. Solidification computer program listings
6. Thermal and physical properties of steel.

B. Scope

The scope of this study was limited to a review of the Engineering Design File compiled by C.E. Forkel and H.L. Brown at EG & G Idaho, Inc. as amplified in a conference call with these individuals on December 14, 1982. This scope does not include a study of the economic feasibility of the process or materials other than that included in this design file (i.e. steel). Due to the confidential nature of this new process as well as the limitation of funding, this scope is limited to the technical opinion of the author whose experience and judgments provide the sole basis of the

assessments included in this study. The author's resume is included in the Appendix B.

C. Approach

The approach taken in the conduct of this study within the above-indicated scope was to critically analyze each portion of and calculation contained within the Engineering Design File. As each feature of this proposed process was analyzed, specific issues that might prove critical were identified and recorded. Following this detailed review of the entire Engineering Design File, the entire group of issues were reviewed, and classified under appropriate technical and other headings. These notes were carefully scrutinized to eliminate minor issues and eliminate redundancies thereby leading to a set of major technical and other issues. Judgments were made to summarize this list into the key critical technical issues. At this point an opinion of the technical feasibility of the process was made.

D. Summary and Conclusions

The Double Thin Strip Casting Process holds promise for eliminating the hot working operation in the production of steel resulting in significant energy conservation claims. In order to accomplish this objective while retaining present levels of productivity of conventional concast processes (i.e. same production rates) it is necessary to attain high heat transfer and strip casting rates. These process requisites demand the solution to the following critical technical issues which can otherwise limit technical feasibility of the process:

- Provide uniform molten metal flow control synchronized with casting rate without gas entrapment or gas film formation.
- Provide non-gas evolving carrier strip, traveling at high speed with tension control to allow cavity dimension control and avoidance of strain transmittal to solidifying steel.

- Provide uniform heat transfer rates high enough to produce fine dendrite arm spacings or other structures to enable the elimination of hot working and attainment of productivity objectives.
- Provide quench roll location and pressure to seal structural deficiencies resulting from shortfalls in solidification and/or molten metal processing.

It is the assessment of the author that the process features as listed on page 7 of the Design File are new and/or innovative. This judgment is not based on a Patent Search which should be conducted to establish the validity of this assessment.

The author has confirmed approximately 2×10^6 BTU/ton of the 10×10^6 BTU/ton energy conservation claimed by Forkel and Brown.

Solidification computer program listings and selected physical properties of steel have been located and made available in this report.

It is the assessment of the author (Appendix - Resume) that the process can be made to be technically feasible with the expenditure of significant development effort.

II. INTRODUCTION

The Double Thin Strip Casting Process conceives the continuous casting of a thin metal strip by the flowing of molten metal between moving carrier strips where solidification takes place as the sensible heat in the molten metal and the heat of fusion is absorbed by the carrier strip and quench rolls. In order to assess the overall feasibility of this process, it is necessary to consider those factors that determine the utility of the product produced by this process. Utility is determined by the property/performance and cost of the output product of this process. This present assessment is concerned primarily with the properties and performance of the output product and those factors that determine same. This study is concerned primarily with an assessment of the technical feasibility of this process.

The processing of materials determines the structure which in turn determines properties and performance of the final output product. For this reason, it is appropriate to start this assessment by categorizing the overall process into its key processing steps as follows:

- A. Molten Metal Processing
- B. Solidification Processing
- C. Fabrication Processing

Although it is recognized that the Double Thin Strip Casting Process is not limited to a single material, the scope of this study is limited to the Engineering Design File which analyzes steel as the material of the process. It is recognized that the other features of the process as presented in the Design File can be changed to accommodate some of the critical technical issues identified in the following sections. Future work can be initiated to identify conditions that would enhance the technical feasibility of the process or extend this study to include economic feasibility.

III. IDENTIFICATION OF TECHNICAL AND OTHER ISSUES

A. Molten Metal Processing

The assessment of technical feasibility of producing an output steel product from this process comparing favorably to products produced by alternate processes starts with assessment of the processing of molten metal.

1. Gas Content of Molten Metal

The gas content of molten steel can be especially important in this process as compared to conventional concast or ingot production processes. This is due to the thin cast strip sections. Gas evolution or gas entrapment can severely effect the integrity of the cast steel strip. Since an essential feature of the new process is to cast close to shape, there is little or no opportunity to weld shut gas holes or other defects present in the solidified structure. The molten metal processing must therefore take this process feature into account and produce molten metal with low gas content or treated with additions to prevent gas evolution and/or processed to yield steel that will not evolve gas upon solidification.

2. Gas Entrapment

Gas can also appear in the solidified structure as a result of entrapment due to turbulent flow conditions. The schematic of the equipment (APPENDIX-A) shows a nozzle channeling the molten metal directly to the space between the carrier strips. If this molten metal is exposed to air or any other gas, the high velocities calculated in the Design File (i.e., 3840"/min) can cause entrapment unless special precautions are exercised.

3. Gas Films Restricting Heat Flow

The gas content of the molten metal and/or gas from entrapment and/or gas evolved from the surface of the carrier strip, can severely effect the heat transfer from the molten metal to the carrier strip and on through to

the quench rolls. The G.E. Dip Forming process was almost abandoned after considerable development expenditures because of gas films impeding the flow of heat to a heat sink. In the G.E. Dip Forming case, the gas content of the molten metal was reduced as a key part of successful process development. In addition, steps were taken to reduce gas entrapment and minimize gas evolution from the surface of the metal exposed to the molten bath.

4. Molten Metal Flow Control

From the viewpoint of reduced gas entrapment, it would be desirable to design the Double Thin Strip Casting Process, to allow its operation as a closed head system. This would require that molten metal be channelled from its containment vessel to a point between the carrier strips so that it is not exposed to the surrounding atmosphere thereby precluding the possibility of gas entrapment.

In addition to minimizing the propensity of gas entrapment, proper flow control mechanism(s) must be established to allow for accurate metering of molten steel at 1600°C to synchronize with casting rate. Excessive flow in relationship to casting rate means overflow (depends on actual design configuration) or will result in contact with the quench rolls. This latter condition can result in melt back or melt through of the carrier strip.

Due to these considerations, the materials of such a nozzle/channel and its design is one of the critical issues determining the technical feasibility of this process.

Finally, design of this metal flow control mechanism must allow for uniform distribution of the molten metal across the carrier strip. Without this uniformity inhomogeneities in structure and properties could result or, in the extreme case, the melt through of the carrier strip could cause break-out and a termination of the production run.

B. Solidification Processing

Solidification occurs as thermal energy is extracted from molten metal. At cooling rates presently attainable in the vast majority of continuous casting machines, solidification produces a crystallized solid metallic structure which can be fabricated into a semi-finished steel product. Steel is an iron-base alloy containing carbon and other alloying elements which tend to redistribute as the phase changes from the molten to the solid state. This basic tendency for certain elements to redistribute between the molten and solid phase leads to dendrite formation as well as micro- and macro-segregation of these elements. Compositional gradients, temperature gradients, solidification rate and other technical features all come together in determining the microstructural features of a strip cast steel product. The structure resulting from solidification processing is influenced by the factors identified in the prior section on molten metal processing. These factors together determine the structure and therefore the properties/performance of continuous cast strip.

1. Microsegregation vs. Cooling Rate

The feature of this process which is a distinct advantage over conventional concast or ingot/rolling processes is its significantly higher solidification rate. Of course experimental values are not available to estimate the actual solidification rate. An estimate is possible using calculations as contained in the Design File. From an initial temperature of 1600°C to the temperature where solidification is complete (i.e. 1540°C), Forkel and Brown estimate a time lapse of 0.001 seconds plus 0.064 seconds or a total of 0.065 seconds (Case VI). This translates to an estimated cooling rate of 5.5×10^4 °C/min. and is comparable to the cooling rate estimated for Chill Block Melt Spinning method (Appendix C, Figure 11).

The cooling rate will determine the solidified structure. It is known that dendrite arm spacing becomes finer as cooling rate increases. As a result, constitutional elements become redistributed on a finer scale in the as-cast structure. Diffusion distances are reduced, lowering the time for complete homogenization during subsequent heat treatment. As a result, it may be possible to eliminate the entire hot working operational step. Elimination of hot working, which is an energy intensive processing operation, is a major goal of this new process. The possibility of accomplishing such a goal will be discussed further in subsequent sections of this report.

2. Solidification Shrinkage

All metals experience volumetric shrinkage when changing from the molten state to a crystalline, solidified state. This shrinkage will appear as voids in the as-cast structure unless molten metal can flow to the point of solidification to avoid shrinkage void formation.

In this process, solidification shrinkage may be readily fed by the solidifying surface being in direct contact with the molten metal depending on the depth of the "mushy" region. The high temperature gradients associated with this process would reduce this source of shrinkage voids although this point has not been studied in the Forkel and Brown analysis and should be included in subsequent computer analyses.

3. Quench Rolls

The feature of the Double Thin Strip Casting Process which can be used to advantage, is the location and pressure applied to the quench rolls in relationship to the location of intersection of the solidification fronts in the interior of the casting. It may be possible to control these variables to produce a sound casting with high physical/mechanical properties. On the

other hand, control of the process variables would likely require a level of sophistication that is at or beyond the frontier of present technology.

Unless process and design variables are properly controlled it is possible that a narrow channel of molten metal may be formed between the solidifying surfaces requiring the flow of molten feed metal down the channel. This could lead to the formation of shrinkage and/or gas cavities that might not be welded shut depending on its time of formation in relation to quench roll location, and pressure applied to these rolls.

Forkel and Brown call for subsequent computer based analysis to clarify these important process interactions. Strong support is offered for this approach.

4. Hot Tearing

Another phenomenon commonly encountered in continuous casting, as well as statically produced castings, is hot tearing. This is a rupture of the casting while it is undergoing solidification. The strain for this rupture can occur if the solidifying casting is restrained so that it cannot shrink as it must when heat is extracted and solidification progresses. Hot tearing is often accompanied by segregation in the vicinity of the tear. This arises as the interdendritic liquid, flows under the influence of strain somewhat analogous to green liquor filling the cavity caused by pushing aside crushed ice in a mixture of crushed ice and creme-de-menthe.

A major advantage of the Double Thin Strip Casting Process is that is a moving belt process, in which the container and the solidifying metal are moving together at virtually the same speed. However, there is still a strain that arises from volumetric shrinkage. Furthermore, because the carrier strip must be held under tension to allow for high expected speed (i.e. 3840 in/min) movement of 0.008" steel, transmittal of tensile strain

to the solidifying metal could make this a critical technical issue. This matter is examined further in the section entitled Speed and Productivity.

5. Carrier Strips and Heat Transfer Rates

In this process, carrier strip must be considered a critical technical issue from several viewpoints. The primary functions of the carrier strip is to contain the molten metal and act as heat sink enabling high cooling rates. Clearly the ratio of carrier strip to molten metal thicknesses as well as initial strip temperatures, thermal conductivities, density, etc. all come together to determine whether these primary functions of the carrier strip can be met. Forkel and Brown have made estimates of the thermal conditions necessary to achieve high cooling rates thereby enabling casting rates comparable to conventional continuous casting processes (i.e. 8" thick x 24" wide, cast at a speed of 60 inches/min.).

The new process would not be feasible if the carrier strips were melted before a cohesive solidified layer were formed. The calculations made by Forkel and Brown were analytical in nature and were checked manually. There were no errors found in the Forkel and Brown calculations. These methods were adequate for an initial design study however computer simulation of the solidification process is essential to examine a broad range of thermal and material property variations. Enclosed in this report is a description of computer programs that can be used for such simulation (APPENDIX-D).

The assumption incorporated in the Forkel and Brown calculation is that there is an instantaneous, intimate and uniform contact of molten metal over the width of the carrier strip. There are certain obstacles to the practiced realization of this ideal. The condition of the molten metal was discussed earlier. Gas films from the molten metal or resulting by gas entrapment can result in a barrier to heat flow thereby adversely affecting cooling rate.

The uniform distribution of molten metal over the carrier strip was discussed earlier in this report. Non-uniformity of metal flow would lead to non-uniformity of heat transfer rates, non-uniform cooling rates and non-uniform structures, properties and product performance. Since a specific fluid flow design does not yet exist, an assessment will be made as to the feasibility of developing such a design.

6. Cavitation Resulting from High Velocity Molten Metal Flow

Molten metal flowing in constricted channels can result in a sufficient pressure drop as to cause dissolved gas to come out of solution resulting in a cavity. Computer simulation of the solidification profiles in combination with analytical methods can indicate whether this is a critical technical issue.

C. Fabrication Processing

The primary benefit expected with the Double Thin Strip Casting Process is the elimination of hot work and related operation now practiced with conventional continuous casting/rolling and conventional ingot/rolling operations.

1. Elimination of Hot Working

Hot working is presently practiced to reduce the cross section of as-cast structures. As a result sheet and plate are produced as semi-finished products to be used either in the hot rolled condition or are subsequently cold rolled to develop appropriate properties. Casting directly to the desired cross section eliminates a major justification for hot rolling. However, there are other purposes served by hot rolling of steel.

- a) Heating for rolling allows diffusion to homogenize the segregated structure formed in the original.

Comment:

The new process would reduce and possibly eliminate this justification for hot rolling by producing a fine dendrite arm spacing due to rapid cooling or by producing other structures whereby segregation is minimized or eliminated altogether. Compositional changes may be needed to take full advantage of the rapid cooling inherent in this process.

- b) Hot rolling "breaks-up" the as-cast dendritic structure.

Comment:

Very rapid cooling has the effect of forming a very fine dendrite arm spacing or forming other structures whereby this need is minimized or eliminated.

- c) Hot working can be used to control recrystallization and austenitic grain size.

Comment:

The grain size of the new process is expected to be relatively fine in the original as-cast condition. Grain size control would be achieved by subsequent cold rolling/heat treatment sequences.

- d) Hot working orients dendrites and inclusions so that the final ductility in the rolling direction is markedly improved.⁽¹⁾

Comment:

Benefits of this type would not be available if hot rolling were eliminated. However, by the same token, the need for such would be reduced by the rapid cooling associated with the new process. It may be necessary to use other means to control inclusions.

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(1) The Making, Shaping and Treating of Steel, U.S. Steel, Ninth Edition, p.1122.

- e) Hot working closes or welds shut shrinkage, gas, hot tears or other discontinuities formed as a result of earlier stages of processing.

Comment:

The new process must operate at a higher standard of perfection during the molten metal and solidification processing stages, since the elimination of hot working precludes the use of this operation to correct prior deficiencies.

2. Changes in Cold Rolling

Although the Double Thin Strip Casting Process, if practiced in the ideal, will not require subsequent hot fabrication, cold rolling would be required to produce a variety of steel products. Since the as-cast structure comprises approximately $\frac{0.125}{(2)(0.008)} \times 100 = 78\%$ of the clad strip, its properties will be a strong determinant of overall properties of the composite. Subsequent cold forming can strain harden the clad material to obtain the level of strength required for various commercial applications.

3. Carrier Strip Production

Since carrier strips are an integral part of the process it is necessary to observe that approximately 20% of the output process would be carrier strip produced for input. The cost of such production must be considered in any evaluation of overall feasibility. For purpose of this study, which is limited to an assessment of technical feasibility, this aspect has not been examined. It is recommended, however, that the economic aspects of this process be combined with this technical assessment to arrive at an assessment of overall feasibility.

4. Casting/Rolling System

In order to optimize overall output of the casting and fabrication system, it is necessary to examine the technical and cost interaction between them. In order to optimize total output both must be considered together. Trade-offs between casting and fabrication unit costs can be significant. However, costs are beyond the scope of this study and an extension is recommended to make a techno-economic analysis as the basis for assessing overall feasibility.

5. Speed and Productivity

The production rate of processes devised to produce small cross sections has been found to be low as compared to conventional processes (Exhibit I). Extremely high speeds are required to attain equivalent production rates. Forkel and Brown have calculated the conditions required to attain production rates equivalent to conventional continuous casting processes (See Section B-4). These casting rates require movement of metal carrier strips and solidified metal at rates in the vicinity of 1900 to 7600 in/min. It is expected that carrier strips of 0.008" thickness moving at these rates will represent a significant handling problem.

In order to allow for the handling of this material, it will be necessary to keep the carrier strip under tension. As the carrier strip performs its functions (i.e. container and heat sink) it may deform at elevated temperature and transmit strains to the solidifying metal resulting in possible hot tearing and/or fracture at elevated temperature.

D. Energy Conservation Potential

The full and successful implementation of the Double Thin Strip Casting Process has the potential of eliminating the hot rolling operation and the reheating furnace operation preceding hot rolling. The Design File

(page 6) estimates that "as much as 10×10^6 BTU/ton" can be conserved by this new process. Approximately 2×10^6 BTU/ton of this amount could be confirmed. It is requested that the back up to the 10×10^6 BTU/ton be provided for independent review.

IV. TECHNICAL FEASIBILITY ASSESSMENT

The following is a list of technical issues summarized from the discussion in Section III of this report. These issues are assessed as follows:

Assessment of Process

A. Molten Metal Processing

1. Gas Content of Molten Metal	1. Process is more sensitive to gas than conventional concasting. Expect solutions can be found within limits of current steel-making technology.
2. Gas Entrappment	2. Molten metal flow control is critical (See A 4).
3. Gas Films Restricting Heat Flow	3. Elimination of sources of gas is important. a. Gas Content of Molten Metal. (See #1). Within current technology. b. Gas Entrappment (See A 4). c. Off Gassing of Carrier Strip • May require unusual and costly surface preparation of carrier strip, thereby impacting process economics. • Expect solution can be found within limits of current technology insofar as technical feasibility is concerned.
4. Molten Metal Flow Control	4. Critical technical issue. Solution depends of materials and designs. Probably at limits of current technology.

B. Solidification Processing

1. Microsegregation & Cooling Rate	1. Distinct advantage of process.
2. Solidification Shrinkage	2. Expect that shrinkage can be controlled by changes in process operation which may impact process economics but not its technical feasibility.
3. Quench Rolls	3. Quench roll location and pressure applied to strip can be used to weld shut shrinkage and gas cavities. May also heal hot tear problems although associated segregation may remain.

4. Hot Tearing

4. Moving belt system reduces propensity to hot tearing. Volumetric shrinkage per se is probably not a major factor due to high temperature gradient during solidification. Carrier strip tension can be a source of hot tear formation.

5. Carrier Strips and Heat Transfer Rates

5. Critical technical issues. Control of thermal conditions, speed of carrier belts, molten metal flow control, quench roll location and roll pressure must be closely integrated and controlled. Probably at the limits of current technology.

6. Cavitation Resulting from High Velocity Molten Metal Flow.

6. Same assessment as A 1.

C. Fabrication Processing

1. Elimination of Hot Working

1. Expect that this can be accomplished provided heat transfer rates can be achieved. (See B 5).

2. Changes in Cold Rolling

2. Cold rolling needed to meet property requirements of product output. Any changes to accommodate to process are likely within limits of current technology.

3. Carrier Strip Production

3. Probably a critical part of the economic feasibility of the process. Not part of technical feasibility.

4. Casting/Rolling System

4. Should evaluate trade offs between casting and rolling operation for optimum output. Part of economic feasibility - not part of technical feasibility.

5. Speed and Productivity

5. Theoretically, productivity is comparable to conventional concast provided heat transfer rates are attainable (See B 5). Handling of carrier strip can be a critical technical issue.

The following items represent critical technical issues in determining the technical feasibility of the Double Thin Strip Casting Process.

- A 3. Gas Films Restricting Heat Flow
- A 4. Molten Metal Flow Control,
- B 5. Carrier Strips and Heat Transfer Rates,
- C 5. Speed and Productivity.

These critical technical issues are related to the control of molten metal with the attendant materials problems in channeling steel at elevated temperatures (i.e. 1600°C) so as to prevent gas entrapment. Further, this molten steel must be directed in such a way as to obtain uniform flow over a width of a thin steel strip (i.e. ~0.008" thick) without melt through. In order to achieve the prime objective of this process, eliminate hot working and achieve significant energy conservation goals, heat transfer rates must be high and uniform. The same requirements of high speed are an essential ingredient for achieving productivity goals. High carrier strip speeds needed to allow achievement of productivity and high cooling rates can cause hot tear problems in the solidifying strip.

These critical technical issues are formidable and solutions are at or somewhat beyond the limits of current technology.

It is the assessment of the author (APPENDIX B) that the process can be made to be technically feasible with the expenditure of significant development effort.

V. SUMMARY AND CONCLUSIONS

The Double Thin Strip Casting Process holds promise for eliminating the hot working operation in the production of steel resulting in significant energy conservation claims. In order to accomplish this objective while retaining present levels of productivity equipment to conventional concast processes (i.e. same production rates) it is necessary to attain high heat transfer and strip casting rates. These process requisites demand solution to the following critical technical issues which can otherwise limit technical feasibility of the process:

- Provide uniform molten metal flow control synchronized with casting rate without gas entrapment or gas film formation.
- Provide non-gas evolving carrier strip, traveling at high speed with tension control to allow cavity dimension control and avoidance of strain transmittal to solidifying steel.
- Provide uniform heat transfer rates high enough to produce fine dendrite arm spacings or other structures to enable the elimination of hot working and attainment of productivity objectives.
- Provide quench roll location and pressure to seal structural deficiencies resulting from shortfalls in solidification and/or molten metal processing.

It is the assessment of the author that the process features as listed on page 7 of the Design File are new and/or innovative. This judgment is not based on a Patent Search which should be conducted to establish the validity of this assessment.

The author has confirmed approximately 2×10^6 BTU/ton of the 10×10^6 BTU/ton energy conservation claimed by Forkel and Brown.

Solidification computer program listings and selected physical properties of steel have been located and made available in this report.

It is the assessment of the author (APPENDIX B) that the process can be made to be technically feasible with the expenditure of significant development effort.

APPENDIX C

EG&G Idaho, Inc., RE-A-83-026, THERMAL ANALYSIS OF A PROPOSED
CONTINUOUS THIN-STRIP CASTING PROCESS, D. L. Knudson, May 1983

Report No. RE-A-83-026

Date: MAY 1983

THERMAL ANALYSIS OF A PROPOSED CONTINUOUS
THIN-STRIP CASTING PROCESS

Thermal and Process Analysis Branch

D. L. Knudson
D. L. Knudson

Checked By: _____

H. G. Kraus
H. G. Kraus

Approved By: _____

N. E. Pace *J. L. Liebenthal*
N. E. Pace J. L. Liebenthal

SUMMARY

A proposed process for continuous casting of thin-strip steel was thermally analyzed. The process would provide a final product with two good surfaces at substantial energy savings over conventional methods of strip production. The desired surface finish would be achieved by using two continuous steel strips (clad) as the casting mold. A computer model of this process was developed to permit prediction of the thermal behavior. The model was then used to assess thermal effects of changing any one of nine different process parameters (while holding all others constant). The model was also used to determine the maximum product thickness which could be continuously cast with 0.008 in. thick clad. Based on the results of the computer runs made to assess the effects of the nine process parameters, the molten/solid ratio, determined by the product and clad thicknesses, appears to be the most critical. This ratio determines the relative chilling effect of the clad on the melt and the total initial product energy content. Although other parameters may prove to be more important when changed in combinations, a parametric analysis was beyond the scope of work presented here. Model predictions also indicated that an 0.080 in. product is the maximum thickness which can be continuously cast using 0.008 in. clad. Based on a review of all results obtained in this study, the casting of thin-strip steel in the proposed manner was found to be thermally feasible.

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THERMAL ANALYSIS OF A PROPOSED CONTINUOUS
THIN STRIP CASTING PROCESS

1. INTRODUCTION

Currently, the estimated U.S. market for thin-strip steel products is 10 million tons per year. Most of this demand is met by hot rolling relatively thick (approximately 4 in.) cross-sections to the desired dimensions. Some melt drag casting is also done to produce very thin strips (approximately 0.003 in.). Although melt drag casting produces strip with desirable mechanical/electrical properties, the condition of the surface adjacent to the melt is poor.

A new/unique process where steel could be cast directly into thin cross-sections, while retaining two good surfaces, has been conceived. The primary equipment and its configuration for this process is shown in Figure 1. Some of the more important and novel features of this process are discussed below.

The proposed process will use moving steel strips as the casting mold. These steel strips, also referred to as clad, become an integral part of the final product. As shown in Figure 1, two casting rollers support two continuous steel strips. The steel strips and casting rollers will move at the same speed. In other words, there will be no slip between clad and roller. Molten steel will be continuously poured between the steel strips, and the "sandwich", the steel strips and the molten steel, will travel through the roller system in the direction shown. Therefore, the steel strips, which are relatively thin compared to the "sandwich" dimension, serve as the casting mold as well as provide the desired final product surface finish.

Molten steel will be poured through a nozzle and into the crevice between the steel strips at a rate consistent with the clad (or casting roller) speed and the desired product thickness. To help prevent formation of voids, the molten steel liquid level will be maintained above the casting rollers line-of-centers, hereafter referred to as the casting

Figure 1. Continuous thin-strip casting equipment configuration (not to scale).

rollers pinch point or simply the pinch point. With the molten steel liquid level above the pinch point, the resulting ferrostatic head will tend to feed and fill any potential voids prior to "sandwich" passage through the casting rollers pinch point. (Note that the product dimension will be determined by the spacing of the casting rollers.)

To minimize oxygen contamination and oxidation problems in the process, the equipment shown in Figure 1 will be housed in an inert atmosphere.

The casting and cooling rollers will be water-cooled through internal passages to serve as heat sinks during product solidification. As the product moves through the roller system as shown in Figure 1, heat will also be rejected to the inert atmosphere. For a desired product size, process variables (roller speed, melt superheat, clad thickness, etc.) will be adjusted to prevent clad melt-through and to insure solidification before cooling roller contact is lost.

Although the proposed process will initially require a supply of conventionally produced strip, substantial energy savings would result from the sharply reduced volume of ingot casting, soaking pit, and hot rolling operations required to meet the demand for thin-strip steel products. It has been estimated that 2 to 10 million Btu per ton of thin-strip steel produced could be saved through direct casting.^{1,2} This tremendous energy savings potential could dramatically change the U.S. steel industry and is the justification for the analysis documented here.

2. THERMAL MODEL DESCRIPTION

Based on the proposed configuration shown in Figure 1, the process, from the initial clad/molten steel point of contact to the product/cooling roller separation point, was modeled to permit prediction of the quasi steady-state thermal behavior. (Note that the figure does not show the clad supply rolls, the associated paths from the supply rolls to the casting rollers, or the details of the final product handling downstream of the cooling roller. However, it is clear that the configuration permits a continuous casting of thin strip.) As discussed below, the model can provide such information as temperature profiles, product tensile strength profiles, solidification rates, and heat loss rates from the product. Using the model, sensitivity to variations in process parameters can be studied to help identify acceptable operating conditions. Finally, and most importantly, an assessment of the thermal feasibility of the casting process can be made based on model predictions.

Although any castable material could be used, this analysis was specifically concerned with the casting of AISI-1020 steel because it represents the largest tonnage material for strip/sheet fabrication. The properties of this alloy used in modeling the process are given in Table 1. These properties are also plotted for reference in Appendix A. It should be noted that the properties used seemed to be the best data currently available for AISI-1020 steel, although no further claim is made. Any discrepancies which may be present in the property data will likely have some effect on the results obtained.

From a physical standpoint, the proposed process can be logically separated into two distinct parts at the casting rollers pinch point. In the first part of the process, or the region from the initial clad/molten steel point of contact to the pinch point, heat transfer from the molten core to the clad (or solid surface) is by forced convection. The forced convection heat transfer between molten and solid regions is the result of relative motion between the phases. The clad and any solidified steel always moves at the casting roller speed. However, at the top of the

TABLE 1. PROPERTIES OF AISI-1020 STEEL

Viscosity³ (in cP):

$$\mu = 10.0 \left(\frac{2385.2}{T} - 0.5958 \right)$$

where T is in degrees Kelvin.

Thermal conductivity⁴ (in Btu/hr·ft·°F):

$$k = 36.767 - 1.026 \times 10^{-2}T - 1.323 \times 10^{-5}T^2 \\ + 1.003 \times 10^{-8}T^3 - 1.680 \times 10^{-12}T^4$$

where T is in degrees Fahrenheit.

Ultimate tensile strength⁵:

Temperature (°F)	Strength (psi x 10 ⁻³)
70	60.0
300	67.0
500	67.5
700	59.5
900	41.0
1100	21.0
1300	10.5
1500	5.5
1700	3.5
1900	2.6
2100	2.3
T _S	0.0

where T_S is the solidus temperature in degrees Fahrenheit as defined below.

Specific heat⁴ (in Btu/lbm·°F):

$$c_p = 0.1283 - 2.438 \times 10^{-4}T + 9.372 \times 10^{-7}T^2 \\ - 1.102 \times 10^{-9}T^3 + 4.424 \times 10^{-13}T^4, \quad T \leq 1400$$

TABLE 1. (continued)

$$c_p = 3.949 - 5.640 \times 10^{-3}T + 2.880 \times 10^{-6}T^2 - 5.567 \times 10^{-10}T^3 + 2.638 \times 10^{-14}T^4, \quad 1400 < T \leq 1700$$

$$c_p = 0.168, \quad 1700 < T < T_S$$

$$c_p = \frac{h_{fg}}{T_L - T_S} + 0.170, \quad T_S \leq T \leq T_L$$

$$c_p = 0.172, \quad T > T_L$$

where T is in degrees Fahrenheit, T_S is the solidus temperature in degrees Fahrenheit as defined below, T_L is the liquidus temperature in degrees Fahrenheit also as defined below, and h_{fg} is the heat of fusion in Btu/lbm as given below.

Density⁴ (in lbm/ft³):

$$\rho = 490.0 - 0.01147 T, \quad T < 1700$$

$$\rho = 460.0 - 0.01818 (T - 1700.0), \quad 1700 \leq T \leq 2800$$

$$\rho = 435.0, \quad T > 2800$$

where T is in degrees Fahrenheit.

Solidus temperature⁴ (in °F):

$$\begin{aligned} T_S = & 2793 - 300 (\text{wt\% C}) - 54 (\text{wt\% Mn}) \\ & - 55.8 (\text{wt\% Si}) - 441 (\text{wt\% P}) - 7.2 (\text{wt\% Cr}) \\ & - 5.4 (\text{wt\% Mo}) - 7.2 (\text{wt\% V}) - 18 (\text{wt\% Cu}) \\ & - 16.2 (\text{wt\% Ni}) - 1242 (\text{wt\% S}) \end{aligned}$$

TABLE 1. (continued)

For the AISI-1020 alloy considered here, $T_S = 2695^\circ\text{F}$.

Liquidus temperature⁴(in °F):

$$\begin{aligned} T_L = & 2793 - 147.6 (\text{wt\% C}) - 7.2 (\text{wt\% Mn}) \\ & - 23.4 (\text{wt\% Si}) - 11.2 (\text{wt\% P}) - 1.2 (\text{wt\% Cr}) \\ & - 3.1 (\text{wt\% Mo}) - 3 (\text{wt\% V}) - 5.9 (\text{wt\% Cu}) \\ & - 3.6 (\text{wt\% Ni}) - 72 (\text{wt\% S}) \end{aligned}$$

For the AISI-1020 alloy considered here, $T_L = 2754^\circ\text{F}$.

Heat of fusion⁴:

119 Btu/lbm

---

molten steel pool, the average or bulk molten velocity is less than the solid velocity because of the increased flow area provided by casting roller curvature. The difference in velocity, or the relative velocity, diminishes to zero at the pinch point where the molten steel flow rate into the pool and the flow area are consistent with the solid (or casting roller) speed. In the second part of the process, or the region from the casting rollers pinch point to the product/cooling roller separation point, all heat transfer from the hot core to the clad surfaces is by conduction. Internal relative motion, and therefore, forced convection, does not occur in the second part of the process since the clad and the hot core move together through the rollers as a "sandwich" after passing the pinch point.

In addition to the differences in internal heat transfer, symmetry considerations also make the pinch point a logical dividing point. The first part of the process was assumed to be symmetric from a heat transfer viewpoint. In other words, the thermal effects of the molten pool orientation were neglected. However, the second part of the process is entirely asymmetric with respect to boundary conditions as shown in Figure 1.

This logical division of the process led to the development of two different thermal models. Both models are one-dimensional in space with time-dependent boundary conditions used to simulate conditions associated with a particular location in the process. Both models use SINGLE/MOD2,^{6,7} a finite element heat conduction code, as the driving routine to predict thermal behavior. Continuity of modeling was achieved by using the conditions associated with the final state of the first model as the initial conditions for the second model. These models, hereafter referred to as the molten pool and pinch point models, are discussed in detail in the sections which follow.

2.1 Molten Pool Model

A sketch of the molten pool region is shown in Figure 2. For modeling purposes, a line of symmetry was assumed as shown. The model of this region was required to permit prediction of the thermal behavior of the process as the clad moves from the initial point of contact with the molten steel to the casting rollers pinch point.

A fixed-depth reservoir of molten steel as shown in Figure 2, is continuously "drained" through the pinch point and continuously replenished, at the same mass flow rate, from the tundish. (The molten pool depth, although fixed for a given configuration, is an input variable to the molten pool model.)

A one-dimensional nodalization through one-half of the final product thickness (the clad thickness plus one-half of the hot core at the pinch point) was used to represent the molten pool region. Nodalization of the entire product was unnecessary because of the assumed symmetry. Due to the casting roller curvature, more melt is adjacent to the clad at the top of the pool than at the pinch point. However, because a quasi steady-state process with a fixed molten pool depth was modeled, the additional melt at the top of the pool cannot contribute to the heat transferred to the clad. Therefore, nodalization of this additional melt was also unnecessary. (The node density desired is another input variable to the model.)

The molten pool model simulates thermal conditions which occur as the clad moves from the initial point of contact with the molten steel to the casting rollers pinch point. These location-dependent conditions were treated as time-dependent conditions to permit prediction of the thermal behavior of the process with the one-dimensional model. These conditions and their time simulations are described below.

Figure 2. Molten pool region (not to scale).

2.1.1 Boundary Conditions

As the clad moves from the initial point of contact with the molten steel to the pinch point, heat is transferred from the melt through the clad to the casting roller. Based on the application and the probable size, the casting roller was assumed to be made of low-carbon steel with a wall thickness of 1 in. It was further assumed that the inner surface of the casting roller was maintained at 125°F by the cooling water flow. Therefore, the heat transfer path from the clad consisted of a clad/roller contact coefficient in series with the roller wall conductance to a constant sink temperature of 125°F.

The clad/roller interface was modeled as a convective boundary using an equivalent convection heat transfer coefficient derived from the series resistances. For the Nominal case, a constant clad/roller contact coefficient of $500 \text{ Btu/hr}\cdot\text{ft}^2\cdot^\circ\text{F}^8$ and a constant roller wall conductivity of $29.5 \text{ Btu/hr}\cdot\text{ft}^2\cdot^\circ\text{F}^9$ was assumed. As part of this analysis and as discussed in Section 3.2.4, contact coefficients a factor of 5 smaller and a factor of 5 greater than the Nominal case coefficient were also considered. The resulting equivalent convection heat transfer coefficients have been calculated in Appendix B. The molten pool model then calculates the rate of heat rejection to the casting roller as a function of the equivalent heat transfer coefficient (a constant), the time-varying clad surface temperature, and the constant sink temperature of 125°F.

The remaining model boundary, at the product centerline, was treated as an adiabatic boundary as a result of the symmetry assumption.

2.1.2 Internal Heat Transfer Calculations

As the clad moves from the initial point of contact with the molten steel to the pinch point, changes in phase as well as two modes of heat transfer between the melt and the clad occur simultaneously. The molten pool model must properly account for these phenomena to permit an accurate thermal representation of this portion of the process.

Before discussing details of this part of the molten pool model, one important point must be made. The modes of heat transfer occurring internally include conduction and forced convection. A modeling decision was made to treat both as conductive modes through the substitution of equivalent or effective thermal conductivities at the appropriate internal locations. For this particular application, this approach seemed to be the most convenient. However, the driving routine SINGLE/MOD2^{6,7} is completely capable of handling a variety of boundary conditions internal to a model which would permit other modeling approaches.

An important and significant portion of the molten pool model was dedicated to the tracking of the product phase changes. Often, freezing/melting is thought of as occurring at a single temperature. Although this is true of most pure substances and some very specific alloy compositions, the freezing/melting of most metal alloys, including the alloy considered here, occurs between solidus and liquidus temperatures. (Table 1 provides equations for the determination of these points as well as specific temperatures for the material of interest in this analysis.) Therefore, during each iteration of each time step, the model scans the "sandwich" temperature profile to determine the locations of the solidus and liquidus temperatures. Linear interpolation for location was used whenever either the solidus or liquidus temperature was found between two nodes. All elements, either entirely or partially bounded by the determined locations, were assumed to be undergoing some degree of phase change. For each phase change element, the model then calculated an appropriate increase in heat capacitance based on the specific element temperature to account for the latent heat of fusion accompanying phase change.

In addition to tracking phase change in the "sandwich," the location of the solid/melt interface was also important. To identify this location, it was necessary to choose a point between the solidus and liquidus temperatures at which solidification could be assumed. For this purpose, the "pinch-off" temperature was selected since it is commonly accepted as the temperature at which movement of the liquid due to convective currents,

gravity, etc., ceases as a result of the increased amount of solid phase in the liquid/solid mixture.⁴ The pinch-off temperature, T_{po} , is given by⁴

$$T_{po} = T_S + 2/3 (T_L - T_S) \quad (1)$$

where

T_S = solidus temperature,

T_L = liquidus temperature.

For the alloy considered here, $T_{po} = 2734^\circ\text{F}$.

When the clad makes initial contact with the molten steel pool (time = 0 sec), the model recognized a solid/liquid boundary at the clad/molten steel interface. Thereafter, the location of the solid boundary was assumed to coincide with the location of the pinch-off temperature. (If the pinch-off temperature was found between two adjacent nodes; linear interpolation was used to establish the location of the solid boundary.) Combined with the phase change logic, this enables prediction (by the molten pool model) of freezing onto or melting into the clad.

With the location of the solid boundary known, the model then calculated the conduction of heat (from the melt) through the solid region to the clad/casting roller boundary as a function of the temperature dependent properties listed in Table 1. This heat is rejected at the clad/casting roller interface through the previously discussed boundary condition.

Figure 3 shows the location of this solid region in the molten pool model. However, the figure is not to scale nor do region boundaries remain fixed as implied by the figure. Figure 3 also illustrates other regions internal to the molten pool model which are described below.

Figure 3. Four regions internal to the molten pool model (not to scale).

The relative velocity between the solid surface and the melt provides the mechanism for the development of thermal and hydrodynamic boundary layers adjacent to the solid surface, leading to forced convection heat transfer at the molten/solid interface. The forced convection heat transfer coefficient, h_x , for this internal boundary was calculated as⁸

$$h_x = 0.530 \left(\frac{\rho c_p k u}{x} \right)^{0.5} \quad (2)$$

where

- ρ = density,
- c_p = specific heat,
- k = thermal conductivity,
- u = relative velocity between solid and molten phases at x ,
- x = distance from the initial clad/molten steel point of contact.

All thermal properties in Equation (2) were evaluated at the bulk molten pool temperature using the equations given in Table 1. The relative velocity, u , was calculated as the speed at the solid location less the bulk molten speed evaluated at a distance x from the initial clad/molten steel point of contact.

Equation (2) represents an analytical correlation for forced convection heat transfer from a liquid metal in slug flow across a flat plate.⁸ Although Equation (2) is not specifically tailored for the flow situation which occurs as the clad moves from the initial point of contact with the molten steel to the casting rollers pinch point, this correlation was used for the following reasons. Based on the magnitude of the relative velocities expected, the flow adjacent to the solid surface should be laminar. Since Equation (2) was developed for slug flow, the convection heat transfer coefficients predicted by the equation may be somewhat high.

However, the molten steel pouring action will tend to enhance the convective heat transport in spite of the velocities. In addition, using coefficients which may be high is conservative as far as prediction of clad melt-through is concerned. Since the boundary layer and the distance from the initial point of contact to the pinch point (or the "plate length") is relatively small compared to the probable casting roller diameter, this part of the process is not completely unlike flow across a flat plate. And finally, this flow situation is quite unusual and as such, represents a departure from the flow problems typically studied. As a result, there are no specific correlations applicable to this problem nor is there evidence that would preclude the use of Equation (2) for the estimation of the heat transfer coefficient.

To model this internal forced convection heat transfer, Fourier's law of conduction was equated to Newton's law of cooling to yield an equivalent thermal conductivity, k_e , given by

$$k_e = \frac{h(T_\infty - T^*)}{(dT/dx)} \quad (3)$$

where

- h = forced convection heat transfer coefficient from Equation (2),
- T_∞ = bulk molten steel temperature,
- T^* = temperature at the solid surface--by model definition, the pinch-off temperature,
- $\frac{dT}{dx}$ = temperature gradient evaluated at the molten/solid interface.

The equivalent conductivity was then assigned to a region of molten steel adjacent to the solid front location as shown in Figure 3. Using k_e , the model calculated a conductive heat flux through this region equal to the convective heat flux implied by the heat transfer coefficient. If the solid boundary coincided with the location of a node, k_e was applied to the entire element adjacent to the solid node. If the solid boundary was located between two nodes, k_e was applied only to the portion of the element from the solid front location to the adjacent (and internal) node. In this manner, the conductive heat flux, as implied by the convection heat transfer coefficient, was concentrated over no more than one element thickness.

Boundary layer growth onto the solid surface, which should otherwise be similar to the growth on a flat plate, is complicated by the fact that molten steel is continuously poured into the pool from the tundish. As a result of this pouring action, some boundary layer disturbance will undoubtedly occur. It is reasonable to expect that this boundary layer disturbance will tend to thin the layer.

Hand-calculated estimates of the boundary layer thickness on a flat plate with similar flow conditions (velocities, densities, viscosities, etc.), indicated layer thicknesses would exceed the product midpoint for the range of molten pool depths (or "plate lengths") considered. However, with boundary layers developing simultaneously from the opposing solid surfaces, the product centerline obviously represents a physical limit on layer growth. Therefore, ignoring the pouring action effects, the estimates indicate that the opposing boundary layers should meet at the product centerline at some point above, or upstream from, the casting rollers pinch point.

Based on the foregoing discussion, the following assumptions on the boundary layer and its profile were made. Without melting into or solidification onto the clad surface, it was assumed that the boundary layer would just reach the product centerline at the casting rollers pinch point. This provides a thinner boundary layer than indicated by the hand

calculations consistent with the thinning expected as a result of the molten steel pouring action. In addition, this assumption accounts for the physical limit on layer growth imposed by the product centerline. A linear variation of boundary layer thickness as a function of the position in the pool was also assumed. This provides additional boundary layer thinning near the top of the pool where the pouring disturbance should be most significant. Recognizing that the boundary layer thickness is zero at the initial clad/molten steel point of contact (or the "leading edge of the plate"), the model then calculated the boundary layer thickness at a given location in the molten pool, δ_x , as

$$\delta_x = \delta_{\max} \left(\frac{x}{PL} \right) \quad (4)$$

where

$\delta_{\max}$ = maximum boundary layer thickness,

x = distance from the initial clad/molten steel point of contact,

PL = "plate length" or the distance from the initial clad/molten steel point of contact to the casting rollers pinch point.

During each iteration of each time step, the boundary layer thickness, as calculated using Equation (4), was then added to the solid/molten interface location to determine the position of the boundary layer front shown in Figure 3. If solidification onto the clad was predicted, the boundary layer thickness calculated was reduced, as necessary, so that the product centerline limitation was not exceeded by the boundary layer front. If melting into the clad was predicted, the boundary layer thickness calculated was increased, as necessary, to allow the boundary layer front to just reach the product centerline at the pinch point. In other words, if melting of the clad was predicted, the location of the boundary layer front was established as if melting had not occurred.

To conductively model the boundary layer heat transfer in this problem, the Nusselt number based on the boundary layer thickness, Nu_δ , was estimated. For the range of boundary layer thicknesses, flow velocities, molten flow temperatures and associated thermal/transport properties expected, Nu_δ was found to be between 2 and 4.

It has been shown that⁸

$$Nu_\delta = \frac{k_e}{k} \quad (5)$$

where

k_e = effective conductivity from boundary layer effects,

k = thermal conductivity.

Therefore, an effective conductivity, k_e , was used here to compute the conduction of heat through the boundary layer region. As part of this analysis and as discussed in Section 3.2, variations of k_e were studied. Based on Nu_δ estimates, cases where k_e had values of 2 and 4 times the thermal conductivity as calculated with the equation given in Table 1 were considered. As a lower bound, a case where k_e was equal to the thermal conductivity ($Nu_\delta = 1$) was also examined.

The remaining portion of the model, or that region from the boundary layer front to the product centerline, was, of course, molten steel as shown in Figure 3. In practice, all of the molten steel between the opposing boundary layers should be at a nearly uniform temperature due to the molten steel pouring action and the resulting mixing. With the one-dimensional model, however, a temperature gradient in one direction was unavoidable. Therefore, an attempt was made to eliminate (or at least minimize) radial temperature gradients while accepting a molten steel gradient from the top of the pool to the pinch point (or the point where the opposing boundary layers meet at the product centerline).

To eliminate/minimize the radial temperature gradient, a sufficiently large factor was applied to the conductivity of the melt beyond the boundary layer front. This factor was somewhat arbitrary since its only requirement was to readily promote the flow of heat from the hot core to the boundary layer. In other words, this factor was required to reduce the thermal resistance of this region compared to the other resistances in the model so that only a minimal temperature gradient from the product centerline to the boundary layer front could exist. For this application, a factor of 1000 times the conductivity as calculated with the equation in Table 1 was used to minimize the gradient and simulate mixing in the radial direction.

As the clad moves from the initial point of contact with the molten steel towards the pinch point, the temperature of the molten region is reduced proportional to the amount of heat transferred to the boundary layer. This establishes a temperature gradient from the top of the molten region towards the pinch point. As discussed above, this temperature gradient is probably not physically accurate since the molten steel pouring action will tend to mix the molten region. However, the effects of this temperature gradient on the thermal solution have been judged to be small.

Obviously, the boundaries of the regions shown in Figure 3 could fall between node locations. In such cases, a linearly weighted thermal conductivity was calculated for the elements which may be partly composed of solid, molten, boundary layer, or forced convection regions.

As the clad moves from the initial point of contact with the molten steel towards the casting rollers pinch point, the thermal response is predicted and tracked by the molten pool model. At the pinch point, the final conditions predicted by the molten pool model became the initial conditions for the pinch point model as described below. This provides the modeling continuity required to thermally map the entire casting process.

2.2 Pinch Point Model

The pinch point model was needed to predict the thermal behavior of the product after it leaves the casting rollers pinch point and travels through a series of cooling and supporting rollers (see Figure 1). Through this model, temperature and temperature-dependent strength profiles were established. This information may help determine when material handling is feasible, how much product cooling is required, etc.

As evident in Figure 1 (and as noted previously), heat transfer from the product to the environment and the casting and cooling rollers results in asymmetric boundary conditions. Therefore, nodalization across the entire product thickness (one-dimension) was required. As with the molten pool model, node density is an input variable to the model.

The initial temperature profile through the product thickness was taken from the final temperature profile predicted by the molten pool model. Since the molten pool model assumed symmetry, the initial pinch point model temperature profile was also symmetric. However, that symmetry was immediately lost since the product rejects heat to the casting roller from one surface and to the inert gas and the environment from the other surface after passing the pinch point (see Figure 1).

As with the molten pool model, time-dependent boundary conditions were used in the pinch point model to simulate conditions at a particular location in the process. These boundary conditions are discussed in the sections which follow.

2.2.1 Cooling Roller Boundary Condition

Except for the overall diameter, the cooling roller was assumed to be identical to the casting rollers. Therefore, the boundary condition from the product to the cooling roller was the same as the boundary condition from the clad to the casting roller (see Section 2.1.1).

Obviously, the pinch point model must apply this boundary condition to the appropriate product surface at the instant such condition is encountered. In addition, the model must maintain the condition for the appropriate time duration.

2.2.2 Forced Convection Boundary Condition

Except for the time which the product surface is in contact with the rollers, forced convection heat transfer from the product surface to the inert atmosphere occurs. For the proposed process, the inert atmosphere is expected to be argon gas. As shown in Figure 1, there are two regions where forced convection is possible. The right-hand product surface rejects heat to the inert gas between the casting rollers pinch point and the casting/cooling rollers pinch point. In the second region, the left-hand product surface rejects heat to the inert gas by forced convection between the casting/cooling rollers pinch point and the product/cooling roller separation point.

In both regions, the convective heat loss is complicated. In the first region, the rotating rollers form an apex in which an unknown and complex flow pattern most likely exists. In the second region, the convective heat transport from the product is disturbed by the presence of the rotating supporting rollers. Due to the complexity in both cases, the resulting heat transfer coefficients cannot be calculated (with any accuracy) through existing analytical and/or empirical correlations. Because of this fact, the following assumptions were made.

For gases, forced convection heat transfer coefficients typically range between 2 and 50 Btu/hr·ft²·°F.¹⁰ (For comparison, Reference 8 indicates forced convection heat transfer coefficients in air alone fall between 2 and 100 Btu/hr·ft²·°F.) Therefore, for this analysis, the coefficient was assumed to be in the range of 2 to 50 Btu/hr·ft²·°F.

In both regions, the presence of equipment would seem to enhance the transfer of heat from the product by forced convection. In the first region, flow created by the adjacent rotating rollers will disturb boundary layer development on the right-hand product surface. The rotating supporting rollers, both by their presence and their motion, will break up boundary layer growth on the left-hand product surface. By thinning and disturbing the boundary layer, the resistance to heat flow is reduced and a relatively large coefficient would be expected.

On the other hand, based on the velocities considered here and a variety of different dimensions which might otherwise be used as characteristic lengths, the undisturbed flow would seem to be in the laminar flow regime. This implies a relatively low coefficient.

As a compromise, it was assumed that the forced convection heat transfer coefficients from all surfaces in contact with the inert atmosphere was $15 \text{ Btu/hr}\cdot\text{ft}^2\cdot^\circ\text{F}$. This value, which is in the lower quarter of the expected range, appears reasonable and, if anything, conservative for the application at hand. It was also assumed that the inert atmosphere was maintained at 500°F . In the first region (or the region bounded by the casting and cooling rollers), it was assumed that the inert gas was at 1000°F . This assumption was based on the fact that inert gas movement in and out of the apex is restricted. In addition, the rollers themselves should be hotter than the rest of the surroundings by virtue of their contact with the product.

The pinch point model calculates the rate of heat rejection to the inert gas as a function of the constant convection heat transfer coefficient, the time-varying product surface temperatures, and the constant inert gas temperatures. The model accounts for the time at which inert gas contact is made, the duration of the contact, and the appropriate sink temperature for each product surface.

2.2.3 Radiation Boundary Condition

In addition to forced convection heat transfer, the product also rejects heat to the surroundings through radiation at any time that the product is not in contact with the rollers. The temperature assumptions discussed in Section 2.2.2 were used here. Specifically, the surroundings were assumed to be at 500°F (960°R) except for the region bounded by the casting and cooling rollers. In the region bounded by the casting and cooling rollers, the product "sees" the adjacent roller surfaces and virtually nothing else. These roller surfaces should be hotter than the rest of the surroundings. Therefore, a sink temperature of 1000°F (1460°R) was used for radiant heat transfer in this region.

The radiant heat transfer calculations performed by the pinch point model also required a surface emissivity. The emissivity is a function of the product surface finish, condition, and temperature. Although a relatively wide scatter of data exists, an emissivity of 0.5 seemed to best represent sheet steel at elevated temperatures.^{8,9} This value was used in all radiant heat transfer calculations along with the time-varying product surface temperatures and the appropriate sink temperatures.

2.2.4 Internal Heat Transfer Calculations

Since the product may or may not be solidified by the pinch point, the model must account for internal phase change. Like the molten pool model, this model determined the locations of the solidus and liquidus temperatures each iteration of each time step. Linear interpolation for location was used whenever either the solidus or liquidus temperature was found between two nodes. All elements, either entirely or partially bounded by the determined locations, were then assumed to be undergoing some degree of phase change. For each phase change element, the model calculated an appropriate increase in heat capacitance based on the specific element temperature to account for the latent heat of fusion accompanying phase change.

Beyond the casting rollers pinch point, the entire "sandwich", the clad and the hot core, moves through the roller system as a unit. Therefore, any relative motion ceases at the casting rollers pinch point, even though some molten phase may exist, since the product is reduced to a uniform thickness which travels together from that point. Without internal relative motion between the phases, all heat transfer within the pinch point model occurs by conduction. With that simplification, the modeling detail required by the molten pool model was unnecessary in the second part of the process. The pinch point model merely calculates the conduction of heat from the hot core toward the product boundaries as a function of the temperature-dependent properties listed in Table 1.

This completes the descriptions of the molten pool and pinch point models. Listings of these models, along with the required job control cards, have been provided in Appendix C.

3. ANALYSIS PERFORMED

The thermal model of the proposed thin-strip casting process, consisting of the molten pool and pinch point models as just described, was used to predict thermal behavior. Obviously, the behavior of the process depends on the initial conditions, process configuration and geometry, boundary conditions, etc. A list of parameters was assembled which seemed to cover all aspects of the proposed process. The sections which follow describe these parameters in terms of those that were fixed and those that were allowed to vary. As a result, these sections actually define the scope and range of this analysis.

3.1 Fixed Process Parameters

This section describes ten different process parameters that were identified but not studied. These parameters were grouped into the four categories described below. Included is a brief discussion of why each parameter was fixed. It should be noted that the model developed here is completely capable of considering variations of these parameters if an interest should develop.

3.1.1 Thermophysical Properties

As discussed in Section 2, the proposed thin-strip casting process could be used to produce strip composed of any castable material. However, this analysis restricted its range to the study of the process using AISI-1020 steel. With that limitation, considerable effort was made to obtain thermophysical properties for the alloy of interest. The property data given in Table 1 represents the results of this search. As such, it also represents what was felt to be the best data available at the time. Therefore, no further study of the effects of property data variation was considered.

3.1.2 Process Configuration

To help prevent void formation in the product, some molten pool depth (see Figure 2) appears necessary. However, the pool depth was limited by ferrostatic head considerations. In addition, a deep pool could be substantially solidified (with adequate casting roller cooling) before passage through the casting rollers pinch point. In that case, the casting rollers would have to serve as hot working devices which was not desired in this study. Based on some preliminary scoping runs, a molten pool depth of 2 in. was selected as an acceptable compromise and used throughout this analysis.

The casting rollers were assumed to be 24 in. in diameter while the cooling roller was assumed to be 48 in. in diameter. The selection of these roller sizes was somewhat arbitrary. However, fixing these parameters seemed prudent in light of the total number of variables in the process. In addition, changing roller sizes is essentially equivalent to changing the product speed (which was allowed to vary as discussed later.) To be consistent with other standard sheet steel sizes, the product width was assumed to be 24 in.

3.1.3 Molten Pool Heat Transfer Parameters

The thermal conductivity of the molten region (or the region between the opposing boundary layers as discussed in Section 2.1.2) was assumed to be 1000 times larger than the equation in Table 1 would predict. It is believed that mixing in this region, as a result of the molten steel pouring action, will virtually eliminate any temperature gradient. As part of the preliminary scoping runs, a factor of 1000 seemed to eliminate radial temperature gradients as desired. Therefore, the factor was used in all cases.

The correlation used to predict the convection heat transfer coefficient between the molten and solid phases in the molten pool model was fixed. Changing correlations was not done since no analytical or empirical data exists to justify such a change.

3.1.4 Boundary Conditions

The casting and coolant rollers coolant temperature was assumed to be constant at 125°F. A temperature range of between 100 and 200°F is most likely. However, selecting some other temperature in this range would not alter the product-to-cooling-water temperature difference appreciably. Therefore, only minor difference in thermal behavior should occur.

Heat transfer from the product to the environment is a function of the forced convection heat transfer coefficient and the product surface emissivity (see Sections 2.2.2 and 2.2.3, respectively). The values selected for these two parameters appear to be reasonable and were not varied during this study.

3.2 Process Parameters Studied

This section describes nine different process parameters that were identified and allowed to vary in this analysis. These parameters were grouped into the four categories described below. These parameters were selected for study because there is considerable uncertainty in their probable range and/or because their probable effect on results was significant.

3.2.1 Initial Conditions

The clad, at the time initial contact with the molten steel was made, was assumed to be at a uniform temperature of 500°F. This temperature was considered to be the nominal value since it is consistent with the assumed ambient temperature. Degassing of the clad may be required to prevent bubble formation during casting. One degassing method involves heating the clad. To scope the effects of higher clad temperature on the overall process, initial clad temperatures of 1000 and 1500°F were also considered.

Some molten steel superheat, or temperature in excess of the liquidus temperature, is required to permit pouring and achieve the liquidity felt

necessary to uniformly fill the crevice between the casting rollers. However, any superheat represents energy which must be added to the melt and then removed from the product prior to solidification. As a nominal value, a molten steel superheat of 100°F (or an initial melt temperature of $T_L + 100 = 2854^\circ\text{F}$) was assumed. Molten steel superheats of 200 and 300°F were also studied.

3.2.2 Process Configuration

A nominal clad thickness of 0.008 in. and a nominal product thickness of 0.125 in. were used. These two parameters, which jointly determine the molten/solid ratio, were varied as discussed in Section 3.3.

A nominal product speed of 192 in³/sec was assumed. This value represents the current capacity of a typical cold rolling mill.² For economic reasons, it was felt that the output of the proposed process should be maximized without exceeding the capacity of the cold finishing plants. One-half of the nominal product speed was also considered.

3.2.3 Molten Pool Heat Transfer Parameters

As discussed in Section 2.1.2, the boundary layer thermal conductivity was based on estimates of the boundary layer Nusselt number. With Nu_δ ranging between 2 and 4, the nominal boundary layer thermal conductivity was assumed to be two times the conductivity predicted by the equation in Table 1. To cover the range of Nu_δ , a boundary layer thermal conductivity equal to four times the value predicted by the Table 1 equation was also used. Finally, a lower bound was studied by allowing the boundary layer thermal conductivity to be equal to the value predicted by the Table 1 equation (which is equivalent to $Nu_\delta = 1$).

For the Nominal case, Equation (4) was used to define the boundary layer profile. Due to the complexity of the internal flow situation, the boundary layer profile is not well known. Therefore, Equation (4) was modified to allow definition of other possible boundary layer profiles.

The boundary layer thickness at a given location in the molten pool, δ_x , was given by

$$\delta_x = \delta_{\max} \left(\frac{x}{PL} \right)^n \quad (6)$$

where

$\delta_{\max}$ = maximum boundary layer thickness,

x = distance from the initial clad/molten steel point of contact,

PL = "plate length" or the distance from the initial clad/molten steel point of contact to the casting rollers pinch point,

n = variable power coefficient.

As with the Nominal case (or $n = 1$), the maximum boundary layer thickness was assumed to be equal to one-half of the product less the clad thickness. For purposes of this study, n was allowed to take on values of 0.50 and 0.25.

3.2.4 Boundary Conditions

The proposed process will be enclosed and surrounded by an inert atmosphere. With the casting process in full operation, the temperature inside the enclosure will undoubtedly be high. For the Nominal case, the surrounding temperature was assumed to be 500°F. To assess the effects on product heat removal, the ambient temperature was also studied at 100°F.

As discussed in Section 2.1.1, a nominal clad/casting roller contact coefficient of 500 Btu/hr·ft²·°F was assumed. This coefficient was also applied to the clad/cooling roller boundary since the casting and

cooling rollers were assumed to be identical except for outside diameters. The contact coefficient is a function of contact pressure, temperature, and surface condition. Because of the uncertainty in all of these areas, particularly in the condition of the roller surfaces with time, the nominal value was increased and decreased by a factor of 5.

3.3 Cases Run

The thin-strip casting model, as described in Section 2, was used to make two series of computer runs to determine the effects of the parameters discussed in Section 3.2 on the casting process.

In the first series of computer runs, the product thickness was fixed at 0.125 in. This thickness was selected because it is consistent with the dimension of cold rolling feedstock.² The remaining eight process parameters were set equal to their nominal values and a run was made to define the Nominal case. With the Nominal case established, 14 additional computer runs were made. Fourteen runs were required to accommodate one process parameter change per run for the eight remaining parameters. A summary of this series of computer runs is provided in Table 2.

The basis for the first series of computer runs is quite simple. Specifically, by varying only one process parameter at a time, conclusions can be made with respect to the effects produced by the parameter change as compared to the Nominal case. Further, by assessing all of these effects collectively, trends affecting the casting process, as the result of a change in a parameter, can be determined. Results generated and the trends indicated by the thin-strip casting model for the first series of computer runs will be discussed, in detail, in Section 4.1.

In the second series of computer runs, the clad thickness was fixed at 0.008 in. This thickness was selected because it is readily available in quantity.² In addition, the product linear speed was set equal to the nominal product linear speed. For example, the Nominal case product speed was 192 in³/sec, the product thickness was 0.125 in., and the product

TABLE 2. COMPUTER RUNS MADE WITH A FIXED PRODUCT THICKNESS OF 0.125 IN.

Case	Process Parameter Considered ^a
Nominal	All parameters at nominal values ^b
1A	Molten pool internal boundary layer conductivity = Table 1 equation value x 1
1C	Molten pool internal boundary layer conductivity = Table 1 equation value x 4
2B	Molten pool internal boundary layer profile = Equation (6) with $n = 0.50$
2C	Molten pool internal boundary layer profile = Equation (6) with $n = 0.25$
3B	Initial uniform clad temperature = 1000°F
3C	Initial uniform clad temperature = 1500°F
4A	Ambient temperature = 100°F
5B	Clad thickness = 0.012 in.
5C	Clad thickness = 0.016 in.
6B	Product speed = 192 in ³ /sec x 0.5
7A	Clad/roller contact coefficient = 100 Btu/hr·ft ² ·°F
7C	Clad/roller contact coefficient = 2500 Btu/hr·ft ² ·°F
8B	Initial molten steel superheat = 200°F
8C	Initial molten steel superheat = 300°F

a. All process parameters were fixed (see Section 3.1) or set equal to their nominal values except as indicated.

b. Nominal case parameters:

- Molten pool internal boundary layer conductivity = Table 1 equation value x 2
- Molten pool internal boundary layer profile = Equation (6) with $n = 1.0$.
- Initial uniform clad temperature = 500°F
- Ambient temperature = 500°F
- Steel clad thickness = 0.008 in.
- Product speed = 192 in³/sec
- Clad/roller contact coefficient = 500 Btu/hr·ft²·°F
- Initial molten steel superheat = 100°F.

width was 24 in. Therefore, the nominal product linear speed was 64 in/sec. If an 0.080 in. product thickness was selected as one of the second series of runs, the required product speed would be $122.88 \text{ in}^3/\text{sec}$. This would provide the desired product linear speed of 64 in/sec ($= 122.88 \text{ in}^3/\text{sec} \div 24 \text{ in.} \div 0.080 \text{ in.}$).

The objective of the second series of computer runs was to determine the maximum product thickness which can be "successfully" cast with 0.008 in. clad. The term "successfully" is used to imply that certain criteria must be met. Obviously, melt-through of the clad at any point is unacceptable. Specifically, however, the minimum integrated strength for either side of the product was assumed to be 100 lbf. (This implies that the minimum total product strength should be 200 lbf.) It was felt that this minimum strength was required to drive the process. The results from the second series of runs will be discussed in Section 4.2.

3.4 Model Solution Convergence Check

The accuracy of any finite element computer code depends on a prudent selection of both the time step and mesh interval. The mesh interval used throughout this analysis was 4000 elements per inch (or an element width of 0.00025 in.). This small mesh size was chosen to aid in accurately tracking the internal phase change. With the mesh interval set, preliminary scoping runs were made to help select a reasonable time step. Based on these runs, a time step of 0.0001 sec was chosen for the molten pool model and 0.01 sec was chosen for the pinch point model.

Normally, accuracy can only be checked by reducing both time step and mesh size to see if results are altered. In this case, the mesh interval was significantly smaller than normally required. As a result, reductions in mesh size would not affect results until the time step was reduced to approximately 2.5×10^{-7} sec. Therefore, accuracy was checked by reducing time step alone.

With the Nominal case results as a benchmark, the time step was reduced in Case II to 1×10^{-5} sec for a period of time covering the thermal shock of initial clad/molten steel contact. No significant changes in overall results were noticed. The time step was then reduced in Case III to 1×10^{-6} sec for the same initial time period. Again, the overall results compared closely with the Nominal case results. In tracking the thermal response during the initial clad/molten steel contact, however, the smaller time steps did provide better results. (A more detailed discussion of these results will be provided in Section 4.3.) However, since the overall results were not improved, the smaller time steps were not justified and the nominal time steps were judged to be acceptable.

4. RESULTS

Two series of computer runs were made with the thin-strip casting model in addition to some runs checking solution convergence. Results are discussed in the three sections which follow.

4.1 Results Obtained with a Fixed Product Thickness of 0.125 in.

In the first series of computer runs, a Nominal case was established for comparison to 14 additional cases. The first plots shown below present pertinent Nominal case results. Thereafter, comparisons and plots from the other cases are provided.

Figure 4 illustrates the solid front and boundary layer front locations in relation to the clad thickness and the product centerline for the Nominal case. The time axis in Figure 4 is analogous to the clad/casting roller boundary. Time = 0 sec corresponds to the initial clad/molten steel point of contact and time = 0.0314 sec corresponds to the location of the casting rollers pinch point. This time/space relationship exists because all location-dependent conditions were modeled as time-dependent conditions. As a result, the one-dimensional model effectively "traveled" through the roller system via the appropriate time simulation of conditions.

Curve 1 in Figure 4 is a horizontal line, 0.008 in. from the clad/roller interface, representing the clad thickness. Curve 4 is a horizontal line, 0.0625 in. from the clad/roller interface, representing the product centerline. At the initial point of contact with the molten steel (time = 0 sec), a zero boundary layer thickness and a solid front location coincident with the clad/molten steel interface are shown. As the clad moves towards the casting rollers pinch point (or as time progresses), molten steel from the core solidified onto the clad surface as indicated by Curve 2. At the pinch point, a solidified layer 0.0192 in. thick exists.

MOLTEN POOL MODEL - CASE - BASE CASE.

Figure 4. Solid front and boundary layer front locations in relation to clad thickness and product centerline in the molten pool region for the Nominal case.

During the same period of time, the boundary layer (Curve 3) grew from a zero thickness until it reached the physical limitation of the product centerline.

The relative speed between the molten and solid phases for the Nominal case is shown in Figure 5. The time axes in Figures 4 and 5 are identical. As expected, the maximum relative velocity occurred at the point of maximum roller curvature (and maximum molten flow area). In addition, the relative velocity decreased to zero at the pinch point (time = 0.0314 sec) because the entire product moves together at a uniform speed at that location.

The relative speed shown in Figure 5 was used to calculate the forced convection heat transfer coefficient plotted in Figure 6. This coefficient was then used to imply an equivalent thermal conductivity [through Equation (3)] for the internal molten/solid convective boundary. As shown (and by definition), the coefficient went to zero with relative speed. At the initial clad/molten steel point of contact (time = 0 sec), the coefficient was large. This is completely consistent with the fact that the boundary layer thickness, and therefore, the resistance to convective heat flow, is essentially zero at the same time (see Figure 4).

Figure 7 illustrates the temperature profile, from the clad/casting roller interface at $x = 0$ in. to the product centerline at $x = 0.0625$ in., at 0.0001 sec after initial clad/molten steel contact. The initial molten steel superheat and the initial clad temperature for this Nominal case are quite clearly shown. As time progresses, energy is removed from the melt (resulting in lower core temperatures) and transferred to the clad. As a function of the clad surface temperature (or the temperature at $x = 0$ in.), heat is also simultaneously rejected to the casting roller. The effects of these phenomena are shown in Figures 8 and 9. Figures 8 and 9 represent Nominal case temperature profiles 0.004 and 0.01 sec after initial clad/molten steel contact, respectively. Figures 7, 8, and 9 also

MOLTEN POOL MODEL - CASE - BASE CASE.

Figure 5. Relative speed between the molten and solid phases in the molten pool region for the Nominal case.

Figure 6. Forced convection heat transfer coefficient from the melt to the solid front in the molten pool region for the Nominal case.

SINGLE/MOD2 Time Step No. = 1 Time = .0001000

Figure 7. Nominal case temperature profile in $^{\circ}\text{F}$, from the clad/casting roller interface at $x=0$ in. to the product centerline at $x=0.0625$ in., 0.0001 sec after initial clad/molten steel contact.

SINGLE/MOD2 Time Step No. = 40 Time = .0040000

Figure 8. Nominal case temperature profile in °F, from the clad/casting roller interface at x=0 in. to the product centerline at x=0.0625 in., 0.004 sec after initial clad/molten steel contact.

Figure 9. Nominal case temperature profile in $^{\circ}\text{F}$, from the clad/casting roller interface at $x=0$ in. to the product centerline at $x=0.0625$ in., 0.01 sec after initial clad/molten steel contact.

illustrate the desired flat temperature gradients in the molten region. These gradients were achieved by imposing large thermal conductivities to simulate thorough mixing in the melt.

Figure 10 illustrates the Nominal case temperature profile, from the clad/casting roller boundary to the product centerline, at the casting rollers pinch point (or time = 0.0314 sec). This figure represents both the final molten pool model temperature profile and the initial pinch point model temperature profile. Comparing Figures 7 and 10, travel to the pinch point resulted in a core temperature reduction from 2854 to 2760°F and a clad surface temperature increase from 500 to approximately 2500°F. Figures 11 and 12 illustrate this temperature change from a different perspective. Figure 11 shows the response of the clad surface temperature (Node 1) and the product centerline temperature (Node 251). Figure 12 shows the temperature responses of the clad surface adjacent to the casting roller (Node 1) and the clad surface which initially forms the molten/solid interface (Node 33). The latter figure also shows the immediate and dramatic temperature response of Node 33 (virtually a step change from 500 to approximately 2195°F) upon molten steel contact.

Figure 13 illustrates the entire product temperature profile at the product/cooling roller separation point (or time = 1.3114 sec). The surface at $x = 0$ in. represents the product surface adjacent to the supporting rollers while the surface at $x = 0.125$ in. represents the product surface adjacent to the cooling roller. (See Figure 1). Between the casting rollers pinch point (with the associated temperature profile given in Figure 10) and the product/cooling roller separation point (with the temperature profile shown in Figure 13), only a slight reduction and flattening of the profile occurred. However, Figure 13 does illustrate the effects on the product of the non-symmetric boundary conditions. Obviously, the cooling roller was a better heat sink than the inert environment. In fact, heat rejection to the inert environment was not sufficient, in this case, to prevent melt-through of the product surface at $x = 0$ in. As shown in Figure 13, the surface at $x = 0$ in. had a temperature of approximately 2700°F which is above the solidus temperature (of 2695°F) where melting begins.

SINGLE/MOD2 Time Step No. = 314 Time = .0314000

Figure 10. Nominal case temperature profile in $^{\circ}\text{F}$, from the clad/casting roller interface at $x = 0$ in. to the product centerline at $x = 0.0625$ in., at the casting rollers pinch point or 0.0314 sec after initial clad/molten steel contact.

PLOT 17 17.45.38 TIME 3 NOV. 1963 JOB-DLKPED . IL DISPLN VER 0.2

Symbol = Node Number Correspondence

□ = 1   ○ = 251

Figure 11. Nominal case clad surface (Node 1) and product centerline (Node 251) temperatures from initial clad/molten steel contact (time = 0 sec) to the casting rollers pinch point (time = 0.0314 sec).

Symbol = Node Number Correspondence

□ = 1   ○ = 33

Figure 12. Nominal case outside clad surface (Node 1) and inside clad surface (Node 33) temperatures from initial clad/molten steel contact (time = 0 sec) to the casting rollers pinch point (time = 0.0314 sec).

Figure 13. Nominal case temperature profile in °F at the product/cooling roller separation point (time = 1.3114 sec).

Figures 14, 15, and 16 also illustrate the melt-through failure of the surface adjacent to the supporting rollers. Figures 14, 15, and 16 show, for the Nominal case, product tensile strength profiles at 0.1914, 0.4714, and 1.3114 sec after initial clad/molten steel contact, respectively. (The ultimate tensile strength was temperature-dependent as shown in Table 1.) As in Figure 13, the surface at $x = 0.125$ in. in Figures 14, 15, and 16 represents the product surface which eventually contacts the cooling roller. The surface at $x = 0$ in., of course, represents the surface which eventually rejects heat to the inert environment.

The product tensile strength profile shown in Figure 14 occurred just before cooling roller contact was made. At this point in time, the product surface at $x = 0$ in. was in contact with the casting roller while the other product surface was rejecting heat to the inert environment in the apex formed by the rollers (see Figure 1). As far as the product was concerned, the thermal boundaries provided by the casting and cooling rollers were identical. Therefore, the surface adjacent to the casting roller (or the surface at $x = 0$ in.) was relatively cool. Note the minimum strength on the opposite side of the product.

After cooling roller contact with the surface at $x = 0.125$ in. was made, the improved heat rejection cooled that surface and, as a result, the strength was increased. At the same time cooling roller contact was made, however, contact with the inert environment replaced casting roller contact on the product surface at $x = 0$ in. As a result of this decrease in heat rejection capability, the material around the surface at $x = 0$ in. began to heat and lose strength as shown in Figure 15. The profile in the figure occurred 0.4714 sec after initial clad/molten steel contact. Shortly after this time, the product surface at $x = 0$ in. failed by melting. Of academic interest, Figure 16 indicates no "re-cooling" of the surface between the time of failure and the product/cooling roller separation point.

In addition to the information just presented, the molten pool and pinch point models also integrated the rate of heat loss from the product to all "sinks". These results provide estimates of the total rate of heat rejection for the continuous process.

AT TIME - 0.1914 SEC

PINCH POINT MODEL - CASE - BASE CASE.

Figure 14. Nominal case tensile strength profile 0.1914 sec after initial clad/molten steel contact.

AT TIME = 0.4714 SEC

PINCH POINT MODEL - CASE = BASE CASE.

Figure 15. Nominal case tensile strength profile 0.4714 sec after initial clad/molten steel contact.

AT TIME - 1.3114 SEC

PINCH POINT MODEL - CASE - BASE CASE.

Figure 16. Nominal case tensile strength profile at the product/cooling roller separation point (time = 1.3114 sec).

For the continuous process with parameters set as previously described for the Nominal case, a total of 2483 Btu/sec was rejected to all sinks. Of that total, 390 Btu/sec was rejected, by forced convection and radiation, to the inert environment. The heat load on the upper casting roller was 63 Btu/sec while the lower casting roller received a continuous load of 376 Btu/sec. And finally, the heat load to the cooling roller was 1655 Btu/sec. As mentioned, these are estimates--primarily because an undetermined portion of the inert environment load is undoubtedly distributed to the rollers. Tables in Appendix D summarize these results for all cases considered in this analysis.

Cases 1A and 1C were run to investigate the effects of molten pool internal boundary layer thermal conductivity on process behavior (see Table 2). Changing the boundary layer conductivity alters the thermal conductance of the molten pool model. Case 1A decreased the boundary layer conductance by a factor of 2 while Case 1C increased the conductance by a factor of 2 as compared to the Nominal case.

Case 1A results showed slightly slower clad temperature response up to the casting rollers pinch point while Case 1C results showed the opposite trend as compared to the Nominal case. The slight differences observed were quickly and completely "washed-out" beyond the casting rollers pinch point. Plots of these results were not included here because they are essentially identical to the Nominal case.

Cases 2B and 2C were run to investigate the effects of molten pool boundary layer profile on process behavior. In both cases, the boundary layer profiles were thinner than the Nominal case and the boundary layer profile in Case 2C was thinner than the profile in Case 2B. Comparisons of Figures 4, 17, and 18 graphically illustrate this statement.

Thinning the boundary layer, which could potentially be accomplished by the molten steel pouring action, is equivalent to increasing the molten pool model conductance. Therefore, as with Case 1C, Cases 2B and 2C results showed slightly quicker clad temperature responses up to the

1: CLAD THICKNESS   2: SOLID FRONT
3: BOUNDARY LAYER   4: PRODUCT CENTERLINE

MOLTEN POOL MODEL - CASE - 2B.

Figure 17. Solid front and boundary layer front locations in relation to clad thickness and product centerline in the molten pool region for Case 2B.

1: CLAD THICKNESS   2: SOLID FRONT
3: BOUNDARY LAYER   4: PRODUCT CENTERLINE

MOLTEN POOL MODEL - CASE - 2C.

Figure 18. Solid front and boundary layer front locations in relation to clad thickness and product centerline in the molten pool region for Case 2C.

casting rollers pinch point as compared to the Nominal case. With thinner boundary layers, higher peak clad temperatures in response to the thermal shock of initial clad/molten steel contact were also observed. Peak clad temperatures of 2195 and 2500°F were achieved in the Nominal case (see Figure 12) and Case 2B, respectively. In comparison, Figure 19 shows a peak clad temperature of 2600°F for Case 2C.

As with Cases 1A and 1C, the differences observed in Case 2B and 2C results, as compared to the Nominal case, were otherwise slight. Furthermore, these differences were quickly "washed-out" beyond the casting rollers pinch point. As with all previously discussed cases, failure by clad melt-through occurred in Cases 2B and 2C.

Degassing of the clad strips prior to their use in the proposed process may be required. One method of degassing involves heating the clad. Cases 3B and 3C were run to determine the effects of higher initial clad temperatures on process behavior. In Case 3B, an initial and uniform clad temperature of 1000°F was used and in Case 3C, a temperature of 1500°F was used.

In Cases 3B and 3C, tensile strength reductions were observed as compared to the Nominal case. With the clad temperatures initially elevated, all temperature profiles in the process were slightly higher than in the Nominal case. (The temperature profiles were higher, but not significantly higher, because the bulk of the product energy lies in the latent heat of the melt. The contribution to initial total product energy by the increase in clad temperatures was relatively unimportant). With higher product temperature profiles, lower strength profiles were predicted. As an example, at the product/cooling roller separation point, the Nominal case total product strength (through integration of the profile given in Figure 16), was 235 lbf. At the same location in the process, Cases 3B and 3C had integrated product strengths of 148 and 77 lbf, respectively.

Symbol = Node Number Correspondence

□ = 1   ○ = 33

Figure 19. Case 2C outside clad surface (Node 1) and inside clad surface (Node 33) temperatures from initial clad/molten steel contact (time = 0 sec) to the casting rollers pinch point (time = 0.0314 sec).

In addition to the differences in product tensile strength, the rates of heat rejection from the product were slightly higher in Cases 3B and 3C than in the Nominal case (see Table D-1 in Appendix D). Since the Case 3B and 3C product temperature profiles were slightly higher than the Nominal case, the product surface temperatures were also higher. With the Case 3B and 3C product surface temperatures slightly higher than the Nominal case, the rates of heat rejection or heat loss from the product were also higher.

The results from Case 4A were virtually identical to the Nominal case results with one exception. Specifically, the inert environment heat load in Case 4A was estimated to be 415 Btu/sec while the Nominal case estimate was 390 Btu/sec. This slight increase, of course, resulted from the fact that the inert environment temperature was reduced from 500 to 100°F in Case 4A. With this increased temperature difference between the product and the inert environment, the heat loss from the product increased.

Figure 20 illustrates the solid front and boundary layer front locations in relation to the clad thickness and product centerline for Case 5B. Except for the increase in clad thickness from 0.008 to 0.012 in., Figure 20 is very similar to Figure 4. However, the effect of the additional clad thickness in Case 5B becomes more evident in comparing the product temperature profiles at the casting rollers pinch point shown in Figures 21 and 10.

Two phenomena occur by increasing the clad thickness. With the bulk of the product energy content in the latent heat of the melt, the total product energy in Case 5B was significantly lower than the Nominal case. In addition, more energy from the melt was required to achieve a given temperature profile as the clad thickness increased. The molten to solid ratio is a parameter which seems to be an indicator of these effects. The Nominal case molten/solid ratio of 6.8 was reduced to 4.2 in Case 5B.

Figure 22 shows the Case 5B product temperature profile at the product/cooling roller separation point. This profile is noticeably lower

Figure 20. Solid front and boundary layer front locations in relation to clad thickness and product centerline in the molten pool region for Case 5B.

Figure 21. Case 5B temperature profile in $^{\circ}\text{F}$, from the clad/casting roller interface at $x=0$ in. to the product centerline at $x=0.0625$ in., at the casting rollers pinch point or 0.0314 sec after initial clad/molten steel contact.

SINGLE/MOD2 Time Step No. = 128 Time = 1.3113999

Figure 22. Case 5B temperature profile in °F at the product/cooling roller separation point (time = 1.3114 sec).

than the Nominal case profile shown in Figure 13. As a result, the Case 5B tensile strength profile was significantly higher than the Nominal strength profile as shown in Figures 23 and 16.

Casting failure by clad melt-through did not occur in Case 5B. However, the product surface adjacent to the supporting rollers approached melt-through at 0.7514 sec after initial clad/molten steel contact. Through integration of the profile shown in Figure 24, the total strength of the side of the product adjacent to the supporting rollers was only 20 lbf at this time. This was the minimum total strength encountered by either side of the product in the process. Therefore, of all cases considered to this point, Case 5B is the first case to demonstrate integrity through the roller system.

Figure 25 shows the location of the solidus temperature (2695°F) on the either side of the product strip for Case 5B as a function of time. Note that at time = 0 sec, the solidus temperature was located very near both 0.012 in. clad surfaces. The curves then move toward the center of the product as solidification occurs. The location of the intersection of these curves represents the point in time at which solidification of product is complete. As indicated, the product is solid 0.83 sec after initial clad/molten steel contact or 51 in. after passing the casting rollers pinch point. The shape of the curves is a result of the effect of the boundary conditions on the product temperature profile. The sharp inflection at 0.27 sec coincides with cooling roller contact time. The effectiveness of the cooling roller as a heat sink compared to the inert environment is apparent through examination of the curves from 0.27 sec to solidification time.

Figure 26 shows the cooling rate achieved during the solidification of the product for the Case 5B. As used here, the cooling rate, CR, was defined as:

$$CR = \frac{T_L - T_S}{t_L - t_S} \quad (7)$$

Figure 23. Case 5B tensile strength profile at the product/cooling roller separation point (time = 1.3114 sec).

ULTIMATE TENSILE STRENGTH (PSI)

Figure 24. Case 5B tensile strength profile 0.7514 sec after initial clad/molten steel contact.

CASE 5B

Figure 25. Solidus temperature (2695°F) locations in the product strip as a function of time for Case 5B.

CASE 5B - 12 MIL CLAD

Figure 26. Cooling rate for Case 5B.

where

T_L = liquidus temperature (2754°F),

T_S = solidus temperature (2695°F),

t_L = time at which T_L occurred,

t_S = time at which T_S occurred.

Note the abrupt change in cooling rates at 0.040 and 0.087 in. in the product. These changes are the result of the boundary condition effects on the product temperature. These effects are indicated as flat or divergent solidus temperature location curves in Figure 25. As indicated, the minimum cooling rate in the product was 74°F/sec.

Figure 27 shows the solid front and boundary layer front locations in relation to the clad thickness and product centerline for Case 5C. Note the similarities between Figures 4, 20, and 27.

Figure 28 shows the Case 5C product temperature profile at the casting rollers pinch point. Comparisons of Figures 10, 21, and 28 illustrate the effect of increasing clad thickness on process behavior. For a fixed product thickness, increasing the clad thickness increases the clad's chilling effect and decreases the product energy content. The molten/solid ratio for Case 5C was 2.9 as compared to 4.2 for Case 5B and 6.8 for the Nominal case.

Figure 29 illustrates the Case 5C product temperature profile at the product/cooling roller separation point. Again, the effect of increasing clad thickness on process behavior becomes quite evident through comparisons of Figures 13, 22, and 29. Obviously, the product tensile strength profile of Case 5C at the product/cooling roller separation point was greater than both Case 5B and the Nominal case. Furthermore, Case 5C results indicated tensile strengths greater than those of Case 5B (and

MOLTEN POOL MODEL - CASE - 5C.

Figure 27. Solid front and boundary layer front locations in relation to clad thickness and product centerline in the molten pool region for Case 5C.

Figure 28. Case 5C temperature profile in $^{\circ}\text{F}$, from the clad/casting roller interface at $x=0$ in. to the product centerline at $x=0.0625$ in., at the casting rollers pinch point or 0.0314 sec after initial clad/molten steel contact.

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SINGLE/MOD2 Time Step No. = 128 Time = 1.3113999

Figure 29. Case 5C temperature profile in $^{\circ}\text{F}$ at the product/cooling roller separation point (time = 1.3114 sec).

those of the Nominal case) at all points in the process. Figure 30 shows the Case 5C tensile strength profile 0.1914 sec after initial clad/molten steel contact. This figure also represents the minimum tensile strength profile encountered by either side of the product during the process. Integration of the weak side of the product profile indicates a minimum strength of 610 lbf.

Figure 31 shows the location of the solidus temperature (2695°F) on either side of the product strip for Case 5C as a function of time. The curves intersect at 0.17 sec after initial clad/molten steel contact indicating complete product solidification. Note that with the decreased total energy content and the increased clad thickness, Case 5C solidified quicker than Case 5B (see Figures 31 and 25).

Figure 32 illustrates the cooling rate achieved during the solidification of the product for Case 5C. The minimum cooling rate, which occurred in the center of the product, was approximately 446°F/sec.

Case 6B was run to assess the effects of decreasing the product speed on the behavior of the process (see Table 2). By decreasing the product speed by a factor of 2, the time between the initial clad/molten steel point of contact and the product/cooling roller separation point was doubled as compared to the Nominal case. (In the Nominal case, the time between the initial clad/molten steel point of contact and the product/cooling roller separation point was 1.3114 sec.)

Case 6B results closely matched the Nominal case results for the first 1.3114 sec of the Case 6B run. The reason for this phenomenon is quite easily explained. The Nominal case and Case 6B had the same initial total product energy content. (In other words, the same molten/solid ratio was used and the two cases had the same initial molten and solid temperatures.) Furthermore, the boundary conditions to the respective heat sinks for the two cases were also identical. (In other words, the boundary conditions were not functions of product speed.) Therefore, approximately

AT TIME - 0.1914 SEC

PINCH POINT MODEL - CASE - 5C.

Figure 30. Case 5C tensile strength profile 0.1914 sec after initial clad/molten steel contact.

CASE 5C

Figure 31. Solidus temperature (2695°F) locations in the product strip as a function of time for Case 5C.

CASE 5C - 16 MIL CLAD

Figure 32. Cooling rate for Case 5C.

the same amount of energy was removed from the same "heat sources", namely, the products, in both cases. Comparisons of Figures 33 and 10 and Figures 34 and 13 illustrate this fact.

After the first 1.3114 sec of the Case 6B run, the product continued to reject heat and, as a result, cooled considerably by the time the product/cooling roller separation point was reached. Unfortunately, like the Nominal case, Case 6B suffered a casting failure by clad melt-through at approximately 0.35 sec after initial clad/molten steel contact. Therefore, the extended cooling time was of no value since the energy removal rate was not fast enough to prevent energy redistribution in the product and ultimately, melting of the clad.

The clad/roller contact coefficient used in Case 7A was $100 \text{ Btu/hr}\cdot\text{ft}^2\cdot^\circ\text{F}$. This coefficient was one-fifth the value of the coefficient used in the Nominal case. As a result, the resistance to heat flow between the product and the rollers increased.

The immediate effect of the increased clad to roller resistance was higher clad surface temperatures for all surfaces in contact with a roller. Temperature profiles, of course, shifted up with the surface temperatures. The effect on temperature profiles was minimal up to the casting rollers pinch point. However, by the time the product/cooling roller separation point was reached, differences in temperature profiles became more significant. As shown in Figure 35, the surface at $x = 0.125 \text{ in.}$ (or the product surface adjacent to the cooling roller) had increased from 2617°F (see Figure 13) to 2717°F as compared to the Nominal case.

As a result of the increase in temperature of surfaces adjacent to rollers, tensile strength reductions were also observed. In fact, temperatures around the surface adjacent to the cooling roller increased enough to allow clad melt-through 0.35 sec after initial clad/molten steel contact. In addition, Case 7A results also indicated a clad melt-through on the opposite side of the product at approximately the same time.

Figure 33. Case 6B temperature profile in $^{\circ}\text{F}$, from the clad/casting roller interface at $x=0$ in. to the product centerline at $x=0.0625$ in., 0.03 sec after initial clad/molten steel contact.

Figure 34. Case 6B temperature profile in °F 1.2228 sec after initial clad/molten steel contact.

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SINGLE/MOD2 Time Step No. = 128 Time = 1.3113999

Figure 35. Case 7A temperature profile in °F at the product/cooling roller separation point (time = 1.3114 sec).

Although product surface temperatures adjacent to rollers were higher in Case 7A than in the Nominal case, heat rejection to the rollers was significantly reduced (see Table D-1 in Appendix D). With higher clad surface temperatures, the temperature difference between the clad surface and the roller coolant increased as compared to the Nominal case. However, the boundary coefficient decreased sufficiently to cause the reduction in heat rejection.

The clad/roller contact coefficient used in Case 7C was 2500 Btu/hr·ft²·°F. This coefficient was five times the value of the coefficient used in the Nominal case. As a result, the resistance to heat flow between the product and the rollers decreased. Therefore, Cases 7A and 7C had opposite effects on clad to roller thermal resistance. As a result, all effects produced by Case 7A were reversed in Case 7C.

For example, the effect of the reduced clad to roller resistance was lower clad surface temperatures for all surfaces in contact with a roller. By the time the product/cooling roller separation point was reached, the product surface adjacent to the cooling roller (or the surface at $x = 0.125$ in.) had decreased from 2617°F to 2517°F as shown in Figure 36 and as compared to the Nominal case. Therefore, at the same point in time, Case 7A results showed a 100°F product surface temperature increase while Case 7C results showed a 100°F temperature decrease as compared to the Nominal case (see Figures 35, 13, and 36).

As a result of the decrease in temperatures of surfaces adjacent to rollers in Case 7C, tensile strength increases were observed. In Case 7A, the product surface adjacent to the cooling roller melted. However, in Case 7C, not only was the surface integrity adjacent to the cooling roller maintained, but the melt-through on the opposite product surface was delayed for approximately 0.3 sec. This delay was due to the fact that the decreased resistance to heat flow at the clad/cooling roller boundary was affecting temperatures to a slight degree on the opposite side of the product.

Figure 36. Case 7C temperature profile in $^{\circ}\text{F}$ at the product/cooling roller separation point (time = 1.3114 sec).

Heat rejection to the roller was significantly increased in Case 7C as compared to the Nominal case. As previously mentioned, Case 7A results indicated reductions in product heat loss.

Cases 8B and 8C were run to assess the effects of initial melt superheat on the behavior of the process (see Table 2). Case 8B used 200°F and Case 8C used 300°F of initial melt superheat (which represented 100 and 200°F of additional superheat as compared to the Nominal case, respectively).

The additional superheat of Cases 8B and 8C contributed to the total initial product energy content. However, as noted earlier, most of the initial product energy is due to the latent heat of the melt. This fact provides some insight as to why results showed only minor effects.

Throughout the process, Case 8B and 8C temperature profiles were slightly higher than those of the Nominal case. The differences were most pronounced in the region between the initial clad/molten steel point of contact and the casting rollers pinch point. Thereafter, time and cooling rolling contact helped "wash-out" the differences. For example, Figure 37 shows the Case 8C product temperature profile at the product/cooling roller separation point. (The Case 8B profile was not shown since it was less severe.) The surface temperature at $x = 0$ in. (or the product surface temperature adjacent to the supporting rollers) was 2711°F and the surface temperature at $x = 0.125$ in. (or the product surface temperature adjacent to the cooling roller) was 2661°F. For the Nominal case as shown in Figure 13, temperatures of 2700 and 2617°F were indicated. Therefore, the average deviation in the profiles was approximately 28°F.

Although temperature profiles in Cases 8B and 8C were only slightly higher than those of the Nominal case, tensile strength reductions were observed. In fact, both cases indicated clad melt-through on both product surfaces. (The Nominal case maintained product surface integrity on one side of the product.)

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Figure 37. Case 8C temperature profile in °F at the product/cooling roller separation point (time = 1.3114 sec).

In addition to the temperature profile differences, Cases 8B and 8C also indicated higher peak clad temperatures in response to the initial thermal shock of molten steel contact than the Nominal case. Figure 38 shows the temperature responses of the clad surface adjacent to the casting roller (Node 1) and the clad surface which initially forms the molten/solid interface (Node 33) for Case 8C. As indicated, a peak clad temperature of 2584°F occurred. (In Case 8B, the peak clad temperature was 2453°F.) The peak clad temperature in the Nominal case was 2195°F as shown in Figure 12. Although a significant difference in peak clad temperatures occurred, the differences were substantially reduced by the pinch point (time = 0.0314 sec) as shown in Figures 12 and 38.

4.2 Results Obtained with a Fixed Clad Thickness of 0.008 in.

In the second series of runs, the clad thickness and the product linear speed were fixed at 0.008 in. and 64 in/sec, respectively. With the remaining parameters set at nominal values, the objective of this series was to determine the maximum product thickness. The criteria for acceptance of a given product thickness required product integrity throughout roller system. In addition, the minimum integrated strength on the either side of the product centerline was set at 100 lbf. (This implied that the minimum total product strength was 200 lbf.)

Based on a trial-and-error approach, a product thickness of 0.070 in. (or a molten/solid ratio of 3.4) was found to meet the criteria (within acceptable limits). Some of the pertinent results for this case are presented below.

Figure 39 shows the solid front and the boundary layer front locations in relation to the clad thickness and the product centerline. As shown, the solid front had propagated to within 0.006 in. of the product centerline by the time the casting rollers pinch point was reached (time = 0.0314 sec).

PLOT 18 15.36.19 TUE 5 8 MAR, 1983 JOB-DLXHP40 IL DISPLN VCR 8.2

Symbol = Node Number Correspondence

□ = 1   ○ = 33

Figure 38. Case 8C outside clad surface (Node 1) and inside clad surface (Node 33) temperatures from initial clad/molten steel contact (time = 0 sec) to the casting rollers pinch point (time = 0.0314 sec).

Figure 39. Solid front and boundary layer front locations in relation to clad thickness and product centerline in the molten pool region for a 0.070 in. product thickness.

Figure 40 illustrates the temperature profile, from the clad/casting roller interface to the product centerline, at the casting rollers pinch point. Between the pinch point and the product/cooling roller separation point, a general flattening and reduction of this profile occurred as heat was rejected from the product. Figure 41 shows the resulting product temperature profile at the product/cooling roller separation point.

Figure 42 shows the product tensile strength profile 0.1914 sec after initial clad/molten steel contact (and just before cooling roller contact). At this time, the surface at $x = 0$ in. was in contact with the casting roller. The opposite side of the product (or the surface at $x = 0.125$ in.) was rejecting heat to the inert environment in the apex formed by the rollers (see Figure 1).

Figure 42 is shown here because it also represents the minimum product tensile strength profile encountered in the process. Integration of the curve gives a total product strength of 266 lbf. Integration of the weak side only, which is also at its minimum value for this process, gives a strength of 81 lbf. For purposes of this analysis, it was assumed that the 81 lbf side was sufficiently close to the 100 lbf minimum to be acceptable.

Figure 43 shows the location of the solidus temperature (2695°F) on either side of the product strip for this case. As indicated, the product solidification was complete 0.15 sec after initial clad/molten steel contact or 7.7 in. beyond the casting rollers pinch point.

Figure 44 illustrates the cooling rate achieved during the solidification of the 0.070 in. product. As indicated, the minimum cooling rate was 414°F/sec . The sharp decrease in cooling rate shown at 0.045 in. in the product was induced by boundary conditions. Solidification from 0.070 to 0.045 in. was complete at the time casting roller contact was replaced with inert environment contact on the product surface at 0.070 in.

SINGLE/MOD2 Time Step No. = 314 Time = .0314000

Figure 40. Temperature profile in $^{\circ}\text{F}$; from the clad/casting roller interface at $x = 0$ in. to the product centerline at $x = 0.035$ in., at the casting rollers pinch point or 0.0314 sec after initial clad/molten steel contact for a 0.070 in. product thickness.

Figure 41. Temperature profile in $^{\circ}\text{F}$ at the product/cooling roller separation point (time = 1.3114 sec) for a 0.070 in. product thickness.

AT TIME - 0.1914 SEC

PINCH POINT MODEL - CASE - 8 MIL C.

Figure 42. Tensile strength profile 0.1914 sec after initial clad/molten steel contact for a 0.070 in. product thickness.

CASE C - 70 MIL PRODUCT

Figure 43. Solidus temperature (2695°F) locations in the product strip as a function of time for a 0.070 in. product thickness.

CASE C - 70 MIL PRODUCT

Figure 44. Cooling rate for 0.070 in. product thickness.

(see Figure 42 at time = 0.0314 sec). With the associated reduction in heat rejection from the product surface at 0.070 in., solidification slowed dramatically.

The 0.070 in. product thickness case just presented was one of several cases made in an attempt to satisfy the minimum strength criteria. Taken together, these cases provide some limited insight into the variation of minimum integrated strength with product thickness (or molten/solid ratio). Therefore, these data are plotted in Figure 45 for reference.

4.3 Early Transient Thermal Shock Investigation

Before the first series of computer runs with the thin-strip casting model were made, an examination of the adequacy of the time step used was performed. This is an essential part of any numerical analysis. In this case, however, some interesting results were obtained in the process.

Based on preliminary scoping runs, time steps of 0.0001 and 0.01 sec seemed adequate for use in the molten pool and pinch point models, respectively. The concern here, however, was that the molten pool model time step was too large to permit accurate tracking of the initial thermal shock of molten steel and clad contact. It was assumed that if the thermal shock was not well mapped, that temperature errors in the initial prediction would be transmitted through all subsequent temperature results.

To check the molten pool model time steps effect on accuracy, a base case was needed. Therefore, using the time steps as discussed above, the Nominal case as described in Section 3.3 was run.

A second run was then made with a time step reduction during the period of initial thermal shock. In the second run, or Case II, the molten pool model time step was reduced by one order of magnitude (to 1×10^{-5} sec). The reduced time step was used for calculations during

Figure 45. Minimum integrated strength for one side of a continuous cast product using 0.008 in. steel clad strips.

the initial thermal shock. Thereafter, the Nominal case time steps were used. (All other Case II process parameters were identical to the values used in the Nominal case.)

The results of the Nominal case and Case II were then compared. Figure 46 illustrates the temperature responses of the clad surface adjacent to the casting roller (Node 1) and the clad surface which initially forms the molten/solid interface (Node 33) for Case II. As shown, a peak clad temperature of 2464°F was predicted in response to the initial thermal shock of clad/molten steel contact. This peak temperature was considerably above the Nominal case peak clad temperature of 2195°F (see Figure 12).

The effects of the higher peak clad temperature in Case II on the overall product temperature profiles were small as compared to the Nominal case. In addition, all differences between the cases were "washed-out" well ahead of the casting rollers pinch point.

Convergence (and accuracy) of a finite element solution has normally been achieved if an order of magnitude reduction in time step does not affect results. In this case, however, the initial time step used in Case II was reduced by another order of magnitude (to 1×10^{-6} sec). Case III was then run using this smallest time step during the period of initial thermal shock with Nominal case time steps used thereafter. (As before, all Case III process parameters were identical to the values used in the Nominal case.)

Figure 47 illustrates the temperature responses of the clad surface adjacent to the casting roller (Node 1) and the clad surface which initially forms the molten/solid interface (Node 33) for Case III. As shown, a peak clad temperature of 2771°F was predicted in response to the initial thermal shock of clad molten steel contact. This peak temperature was well above the peaks predicted in either the Nominal case or Case II.

Symbol = Node Number Correspondence

□ = 1   ○ = 33

Figure 46. Case II outside clad surface (Node 1) and inside clad surface (Node 33) temperatures from initial clad/molten steel contact (time = 0 sec) to the casting rollers pinch point (time = 0.0314 sec).

Symbol = Node Number Correspondence

□ = 1   ○ = 33

Figure 47. Case III outside clad surface (Node 1) and inside clad surface (Node 33) temperatures from initial clad/molten steel contact (time = 0 sec) to the casting rollers pinch point (time = 0.0314 sec).

In fact, the peak temperature predicted by Case III was above the material liquidus temperature (2754°F) indicating that some clad surface melting had occurred.

As with Case II, the effects of the higher peak clad temperature in Case III on the overall product temperature profiles were still relatively small as compared to the Nominal case. The differences between Case III and the Nominal case were "washed-out" approximately 0.005 sec ahead of the casting rollers pinch point.

The results of Cases II and III (as compared to the Nominal case) indicated that accuracy in the thermal shock mapping had virtually no effect on results. Therefore, the Nominal case time steps were completely adequate for use in this analysis.

In addition, the results of Cases II and III indicated a trend of increasing peak clad temperatures with decreasing time step. Obviously, convergence in the prediction of the peak clad temperature had not been achieved. It is believed that continued time step reduction in combination with mesh size reductions would eventually allow accurate mapping of this part of the process. This was not done, however, because the affect on overall thermal results appears to be small.

A very important result from the limited number of cases was revealed, however. That result was the fact that some limited clad surface melting may actually occur on initial contact with the molten steel. This fact may have bearing on the quality of the clad to cast bond achieved in the proposed process.

5. CONCLUSIONS AND RECOMMENDATIONS

In the first series of computer runs made with the thin-strip casting model, the product thickness was fixed at 0.125 in. while eight other process parameters were varied. Process parameter variation was accomplished through 14 computer runs and then compared to a Nominal case. Through this series of runs, a number of trends became apparent with respect to the effect of these eight parameters on process behavior. The following paragraphs summarize these conclusions.

The results from the Nominal case indicate that successful continuous casting with parameters as defined (in Section 3.3) is impossible due to clad failure by melt-through. Therefore, the Nominal case is not presented here as an optimum or even an acceptable case. It has value only as a benchmark or basis for the conclusions which follow.

The effect of initial conditions on the proposed process was studied through Cases 3B, 3C, 8B, and 8C (see Table 2). Cases 3B and 3C increased initial clad temperatures from the nominal 500°F to 1000 and 1500°F, respectively. Cases 8B and 8C increased initial melt superheat from the nominal 100°F to 200 and 300°F, respectively. Therefore, the initial product energy content was increased (through sensible energy addition) in these cases.

In Cases 3B, 3C, 8B, and 8C, a slight increase in product temperature profiles was observed. These temperature increases were accompanied by slight increases in heat rejection rates and led to slight decreases in product tensile strength. In all cases, differences observed were relatively insignificant because total product energy content was not substantially increased as compared to the Nominal case. (The bulk of the initial total product energy content was in the latent heat of the melt.)

The parameter variation in Cases 3B, 3C, 8B, and 8C seems to be more than adequate in describing the possible or probable ranges. Therefore, at least within the variations considered, these initial conditions produce little effect on the process behavior.

Product speed and clad thickness were configuration parameters considered in the first series of runs. In Case 6B, the nominal product speed of 192 in³/sec was reduced by a factor of 2. In other words, the amount of time required for the clad to travel from the initial clad/molten steel point of contact to the product/cooling roller separation point was doubled in Case 6B as compared to the Nominal case. Therefore, the Case 6B residence time in roller contact and in contact with the inert atmosphere was doubled. As a result, the product left the roller system at a considerably cooler temperature than in the Nominal case.

Although reducing the product speed allows more time for heat transfer from the product, the rate of heat rejection, as controlled by the boundary conditions, ultimately determines whether the extra residence time is of any value. In Case 6B, for example, the boundary conditions were identical to those used in the Nominal case. In both cases, the rate of heat transfer was not sufficient to prevent energy redistribution within the product resulting in clad failure by melt-through.

The clad thickness was increased from the nominal 0.008 in. to 0.012, and 0.016 in. in Cases 5B and 5C, respectively. In both cases (and as contrasted with the Nominal case), product integrity was maintained throughout the roller system, indicating that these cases may offer a combination of parameters which permit continuous casting of thin strip. In Case 5B (0.012 in. clad), the model indicated that a minimum tensile strength of 20 lbf will occur on one side of the product during casting. This minimum strength may be marginal in the proposed configuration. However, the model indicated a minimum tensile strength of 610 lbf during casting of a 0.125 in. product with 0.016 in. clad (Case 5C). This should be more than enough strength in the proposed configuration.

Altering the clad thickness while maintaining a product thickness (thereby changing the molten/solid ratio) directly impacts the chilling effect of the clad on the melt and the total initial product energy content. Because of these effects, the molten/solid ratio was found to be a critical consideration in the proposed process.

Cases 1A, 1C, 2B, and 2C were run to examine the effects produced on the process by internal molten pool region heat transfer assumptions. The boundary layer thermal conductivity was reduced by a factor of 2 and increased by a factor of 2 (as compared to the Nominal case) in Cases 1A and 1C, respectively. Cases 2B and 2C successively thinned the nominal boundary layer profile.

In Cases 1A, 1C, 2B, and 2C, effects on the behavior of the process as compared to the Nominal case were minor. Differences that were observable were essentially eliminated beyond the casting rollers pinch point. Therefore, for the range of variation considered, the internal molten pool boundary layer thermal conductivity and boundary layer profile were found to have little effect on the proposed process. Furthermore, since the residence time in the molten pool region is relatively short compared to the time between the initial clad/molten steel point of contact and the product/cooling roller separation point, substantial deviation from the internal molten pool region heat transfer assumptions could be tolerated without significant impact on the overall process.

The effect of boundary conditions on the proposed process was studied through Cases 4A, 7A, and 7C (see Table 2). Case 4A reduced the nominal ambient temperature from 500 to 100°F. The clad/roller contact coefficient was reduced by a factor of 5 and increased by a factor of 5 (as compared to the Nominal case) in Cases 7A and 7C, respectively.

Based on Case 4A results, decreasing the ambient temperature from 500 to 100°F has no effect on the proposed process behavior.

With increased resistance to clad-to-roller heat flow, Case 7A results showed higher product temperature profiles, lower product strength profiles, and lower rates of heat rejection from the product as compared to the Nominal case. In Case 7C, with decreased resistance to clad-to-roller heat flow, the opposite trends were observed, as expected. In spite of the large increase in the clad/roller contact coefficient and the associated decreases in product temperature, a clad failure by melt-through still

occurred in Case 7C. Therefore, this substantial change in contact coefficient is not sufficient, by itself, to permit continuous casting of thin strip.

In the first series of computer runs and as discussed in the foregoing, a single parameter change was always compared to the Nominal case. It should be noted that the interrelated effects produced by changing more than one parameter at a time were not studied here nor can these effects be directly inferred from the results presented.

In the second series of computer runs made with the thin-strip casting model, the clad thickness and the product linear speed were fixed at 0.008 in. and 64 in/sec, respectively. Based on the results of this series, continuous casting of thin strips 0.070 in. thick appears possible. For this thickness, product integrity was maintained throughout the roller system (product temperatures were low enough that clad failure by melt-through did not occur). Further, the minimum integrated strength encountered on either side of the product during roller system passage was 81 lbf. (The total minimum product strength was 266 lbf.) This appears to be sufficient to insure process operation. Although the minimum strength encountered during casting was marginal, this series of runs also indicated that 0.080 in. thick strip could be cast (with 0.008 in. clad) without clad melt-through.

Based on results of computer runs made to check the applicability of the time step used in this study and the accuracy of the solutions obtained, evidence of limited clad surface melting on initial contact with the molten steel was observed. Although this phenomenon may be very important in determining the quality of the bond at the clad/molten steel interface, quantitative prediction of this effect was not essential to the mapping of the overall thermal process.

The approach used in this analysis was to explicitly model the proposed process through subroutines added to the SINGLE/MOD2^{6,7} computer code. These subroutines (listed in Appendix C) represent approximations to

the actual phase change/boundary layer interactions (which primarily occur in the molten pool region). This approach is in contrast to the use of a generalized computer code which could implicitly solve the differential equations describing the phase change/boundary layer phenomena. The former approach was adopted because no code currently exists which can perform the required calculations. Although such a code could have been developed and then used to analyze the proposed process, financial and task-oriented constraints did not permit this course.

The advantage in explicit modeling is primarily economic. Computer code development can be expensive, particularly when the physics of a problem are not well understood and documented. Further, if the process or others similar to it are not subjects of continuing interest, the development of a generalized code would not be cost-effective.

The disadvantage with the explicit approach is in modeling accuracy and applicability. In general, an implicit numeric solution of appropriate differential equations is better than the approach taken. Obviously, the sensitivity of the solution to the approximations determines how much better the implicit method is. If a generalized code existed, this analysis (or the analysis of any other similar thermal-hydraulic system) would have consisted of a description of the physical configuration (input data) to the code. Instead, the physics of the problem along with a description of the configuration had to be built into the model of the system. Therefore, the model developed and used here is specific to the proposed process as defined.

Beyond this discussion of advantages and disadvantages, the approach used is basically sound and the results obtained are believed to be representative for the following reasons. First, where modeling assumptions were required due to a lack of information or complete understanding of complex physical phenomenon, a best estimate was made and then conservatively bracketed. Computer runs were made to predict thermal behavior with the best estimate and the bracket end points. In all cases, the results were found to be relatively insensitive to the assumptions

made. Second, essentially all critical modeling assumptions required due to a lack of information or complete understanding of complex physical phenomenon were made in the molten pool portion of the model. Although an understanding of the thermal behavior in the molten pool portion of the process is important in prediction of the initial and limited clad surface melting, for example, relatively large deviations in assumptions and/or conditions were found to have minimal effects on the process. The insensitivity of process behavior to molten pool assumptions and/or conditions stems from the fact that the molten pool residence time is only 0.0314 sec out of a total transient time of 1.3114 sec. Therefore, variations in the molten pool portion of the process are generally "washed-out" downstream of the casting rollers pinch point. And finally, a number of parameters were varied to assess their effects on process behavior. Except for clad/product thickness parameters (or the molten/solid ratio), the variation of parameters produced minimal effects on the process. Therefore, any inaccuracies in assignment of these parameters should not greatly impact the results presented.

Based on a review of the results presented (and the confidence in these results as discussed), the process as proposed is thermally feasible. Obviously, the most desirable results will be obtained with a prudent selection of process parameters. The model could be very useful in setting parameters for a specific prototype. This approach would provide a relatively inexpensive way to avoid mistakes in designing prototype operation. Once in operation, prototype data could be compared with model predictions to benchmark the capability of the model or provide guidelines for enhancement.

The model could also be modified to permit evaluation of other thin-strip casting systems. Based on the results of this analysis, it is obvious that a configuration which provides cooling rollers (or other equivalent heat sinks) to both sides of the solidifying product downstream of the casting rollers pinch point would allow continuous casting of more attractive molten/solid ratio products. The model could be modified to consider this case or others of interest.

6. REFERENCES

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Sub appendix A of Appendix C

PROPERTIES OF AISI-1020 STEEL

Figure A-1. Viscosity as a function of temperature.³

Figure A-2. Thera1 conductivity as a function of temperature.⁴

Figure A-3. Ultimate tensile strength as a function of temperature.⁵

Figure A-4. Specific heat as a function of temperature.⁴

Figure A-5. Density as a function of temperature.⁴

Sub appendix B of Appendix C

CLAD/ROLLER BOUNDARY COEFFICIENT CALCULATION

Nominal case :

Calculate an equivalent boundary coefficient for the following heat flow path.

clad/roller
interface
temperature
(time varying)

inside roller
surface
temperature
(at 125 °F)

$$R_1 = \text{clad/roller contact resistance} = 0.002 \frac{\text{hr-ft}^2\text{-}^\circ\text{F}}{\text{Btu}}$$

$$R_2 = \text{roller wall resistance} = \frac{\Delta x}{K} = \frac{1/12 \text{ ft}}{29.5 \text{ Btu/hr-ft-}^\circ\text{F}}$$

$$= 2.825 \times 10^{-3} \frac{\text{hr-ft}^2\text{-}^\circ\text{F}}{\text{Btu}}$$

$$h = \frac{1}{R_1 + R_2} = \frac{1}{0.002 + 2.825 \times 10^{-3}} = 207.3 \frac{\text{Btu}}{\text{hr-ft}^2\text{-}^\circ\text{F}}$$

$$h = 3.998 \times 10^{-4} \frac{\text{Btu}}{\text{sec-in}^2\text{-}^\circ\text{F}}$$

22-141 50 SHEETS
22-142 100 SHEETS
22-144 200 SHEETS

Case 7A :

Calculate an equivalent boundary coefficient for the following heat flow path.

$$R_1 = \text{clad/roller contact resistance} = 0.01 \frac{\text{hr-ft}^2\text{-}^\circ\text{F}}{\text{Btu}}$$

$$R_2 = \text{roller wall resistance} = \frac{\Delta x}{K} = \frac{1/2 \text{ ft}}{29.5 \text{ Btu/hr-ft-}^\circ\text{F}} = 2.825 \times 10^{-3} \frac{\text{hr-ft}^2\text{-}^\circ\text{F}}{\text{Btu}}$$

$$h = \frac{1}{R_1 + R_2} = \frac{1}{0.01 + 2.825 \times 10^{-3}} = 78.0 \frac{\text{Btu}}{\text{hr-ft}^2\text{-}^\circ\text{F}}$$

$$h = 1.504 \times 10^{-4} \frac{\text{Btu}}{\text{sec-in}^2\text{-}^\circ\text{F}}$$

Case 7C :

Calculate an equivalent boundary coefficient for the following heat flow path.

clad/roller
interface
temperature
(time varying)

inside roller
surface
temperature
(at 125 °F)

$$R_1 = \text{clad/roller contact coefficient} = 0.0004 \frac{\text{hr-ft}^2\text{-}^\circ\text{F}}{\text{Btu}}$$

$$R_2 = \text{roller wall resistance} = \frac{\Delta x}{K} = \frac{1/12 \text{ ft}}{29.5 \text{ Btu/hr-ft-}^\circ\text{F}}$$

$$= 2.825 \times 10^{-3} \frac{\text{hr-ft}^2\text{-}^\circ\text{F}}{\text{Btu}}$$

$$h = \frac{1}{R_1 + R_2} = \frac{1}{0.0004 + 2.825 \times 10^{-3}} = 310.1 \frac{\text{Btu}}{\text{hr-ft}^2\text{-}^\circ\text{F}}$$

$$h = 5.982 \times 10^{-4} \frac{\text{Btu}}{\text{sec-in}^2\text{-}^\circ\text{F}}$$

Sub appendix C of Appendix C

THIN-STRIP CASTING THERMAL MODEL
(MOLTEN POOL AND PINCH POINT MODELS)

```

DLKMP, T77, P1, STANY.
ACCNT, ID=DLK, C=61A007203.
ATTACH, OKWLIB, ID=OKW.
LIBRARY, OKWLIB.
REQUEST, TAPE4, *PF.
REQUEST, TAPE12, *PF.
SINGLE, FICHE=0, FN=SLG02, IDS=HGK, USUB=2.
REWIND, TAPE4.
REWIND, TAPE12.
CATALOG, TAPE4, MPPAR8, ID=DLK, PW=DLK, XR=DLK, RP=30.
CATALOG, TAPE12, MPNTB, ID=DLK, PW=DLK, XR=DLK, RP=30.
BEGIN, FR80POP.
ATTACH, PPOECK, ID=DLK.
PPDECK, TYPE=6A, RSF=MPNTB, NTF=NTB, PPPAR=PPPAR8, FMPSD=$1.03,
PTD=$0.125003, NND=500, LMPN=249, LMPX=$0.062253, HMPN=251,
HMPX=$0.062753, NED=499, CC1=$3.998E-043, CC2=$2.894E-053,
AMBL=0500, AMBH=1000, MPND=250, LCN=033, RCN=400, TESTIT=.
*DK USERSUB
SUBROUTINE USEROT (NITER, ISTEP, T, DT, TIME)
C ... NITER = ITERATION NUMBER FOR "ISTEP" **DO NOT CHANGE**
C ... ISTEP = SOLUTION TIME STEP NUMBER **DO NOT CHANGE**
C ... T = VECTOR OF NODE TEMPERATURES **DO NOT CHANGE**
C ... DT = SOLUTION TIME STEP INCREMENT
C ... TIME = CURRENT TIME PRIOR TO DT CHANGE **DO NOT CHANGE**
C ... USER SUPPLIED PORTION OF USEROT FROM THIS POINT
COMMON/USEDT/NPASS, NSTEP, TSTEP
TSTEP = DT
NSTEP = ISTEP
NPASS = NITER
RETURN
END
SUBROUTINE USERP (N, I, J, MAT, RHO, CONDR,
*CAPC, CAPRHO, ITRAN, TBAR, TIME, R, T)
C ... N = ELEMENT NUMBER **DO NOT CHANGE**
C ... I = I-TH NODE OF ELEMENT **DO NOT CHANGE**
C ... J = J-TH NODE OF ELEMENT **DO NOT CHANGE**
C ... MAT = MATERIAL NUMBER **DO NOT CHANGE**
C ... RHO = DENSITY
C ... CONDR = THERMAL CONDUCTIVITY
C ... NOTE: THERMAL CONDUCTIVITY MUST BE .GE. ZERO
C ... CAPC = SPECIFIC HEAT
C ... CAPRHO = CAPC*RHO
C ... NOTE: AFTER THIS SUBROUTINE IS CALLED, CAPRHO
C ... IS USED BUT CAPC AND RHO ARE NOT
C ... ITRAN = 0, TBAR MEANINGLESS; = 1, TBAR OKAY **DO NOT CHANGE**
C ... TBAR = AVERAGE ELEMENT TEMPERATURE **DO NOT CHANGE**
C ... TIME = TIME **DO NOT CHANGE**
C ... R = VECTOR OF NODE R OR X VALUES **DO NOT CHANGE**
C ... T = VECTOR OF NODE TEMPERATURES **DO NOT CHANGE**
C ... USER SUPPLIED PORTION OF USERP FROM THIS POINT
COMMON/CELEM/NE, IOUM1, IOUM2
COMMON/USEDT/NPASS, NSTEP, TSTEP
DIMENSION R(1), T(1)
INTEGER C
C ... PARAMETER INPUT DATA
DATA CMFBL/2.0/, CMFM/1000.0/, CT/0.008/, DMP/2.0/, EF/119.0/
DATA FMPS/1.0/, INDEXP/-1/, NR/5/, PT/0.125/, RR/12.0/
DATA SHR/3.998E-04/, TL/2754.0/, TRC/125.0/, TS/2695.0/
C ... CMFBL = (THERMAL) CONDUCTIVITY MULTIPLICATION FACTOR (B.L.)
C ... CMFM = (THERMAL) CONDUCTIVITY MULTIPLICATION FACTOR (MOLTEN)
C ... CT = CLAD THICKNESS
C ... DMP = DEPTH OF MOLTEN POOL
C ... EF = ENERGY OF FUSION
C ... FMPS = FRACTION OF MAXIMUM PRODUCT SPEED
C ... IF INDEXP .LT. 0, NO PRINT; IF .GT. 0, PRINT
C ... NE = NUMBER OF ELEMENTS
C ... NPASS = ITERATION NUMBER FOR "NSTEP"
C ... NR = NODE RANGE (OF INTEREST)
C ... NSTEP = SOLUTION TIME STEP NUMBER
C ... PT = PRODUCT THICKNESS
C ... RR = ROLL RADIUS

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C ... SHR = SURFACE CONVECTION HEAT TRANSFER COEFFICIENT TO ROLLER
C ... TL = MATERIAL LIQUIDUS TEMPERATURE
C ... TRC = TEMPERATURE OF ROLLER COOLANT
C ... TS = MATERIAL SOLIDUS TEMPERATURE
C ... TSTEP = SOLUTION TIME STEP INCREMENT
C ... CAPRHD CALCULATIONS THRU STATEMENT 130
C ... CHECK TO SEE IF ELEMENT IS SOLID & BRANCH ACCORDINGLY
C ... IF (T(I).LT.TS.AND.T(J).LT.TS) GO TO 110
C ... CHECK TO SEE IF ELEMENT IS MOLTEN & BRANCH ACCORDINGLY
C ... IF (T(I).GT.TL.AND.T(J).GT.TL) GO TO 90
C ... BRANCH FOR A ZERO ELEMENT GRADIENT
C ... IF (T(I).EQ.T(J)) GO TO 81
C ... DETERMINATION OF CAPRHD IN A TWO-PHASE ELEMENT
C ... ELEMENT TEMPERATURES ORDERED FOR INTEGRATION
C ... IF (T(I).GT.T(J)) GO TO 10
C ... T1 = T(I)
C ... T2 = T(J)
C ... GO TO 20
10 T1 = T(J)
C ... T2 = T(I)
C ... INTEGRATION OF CAPRHD THRU STATEMENT 70
20 IF (T1.LT.TS) GO TO 30
E1 = ((T1-TS)/(TL-TS))*EF*(460.0-(0.01818*(T1-1700.0)))/1728.0
INDEXR = 2
GO TO 40
30 E1 = 0.0
INDEXR = 1
40 IF (T2.LE.TL.AND.INDEXR.EQ.2) GO TO 70
IF (T2.LE.TL.AND.INDEXR.EQ.1) GO TO 60
IF (INDEXR.EQ.2) GO TO 50
E2 = ((0.168*490.906*TS)-(0.00909*0.168*TS*TS)
*-(0.168*490.906*T1)+(0.00909*0.168*T1*T1)
*+(460.0-(0.01818*(TL-1700.0)))*EF)
*+(0.172*490.906*T2)-(0.00909*0.172*T2*T2)
*-(0.172*490.906*TL)+(0.00909*0.172*TL*TL))/1728.0
GO TO 80
50 E2 = (((460.0-(0.01818*(TL-1700.0)))*EF)
*+(0.172*490.906*T2)-(0.00909*0.172*T2*T2)
*-(0.172*490.906*TL)+(0.00909*0.172*TL*TL))/1728.0
GO TO 80
60 E2 = ((0.168*490.906*TS)-(0.00909*0.168*TS*TS)
*-(0.168*490.906*T1)+(0.00909*0.168*T1*T1)
*+(((T2-TS)/(TL-TS))*EF*(460.0-(0.01818*(T2-1700.0)))))/1728.0
GO TO 80
70 E2 = ((T2-TS)/(TL-TS))*EF*(460.0-(0.01818*(T2-1700.0)))/1728.0
80 CAPRHD = (E2-E1)/(T2-T1)
GO TO 140
C ... DETERMINATION OF CAPRHD FOR A ZERO ELEMENT GRADIENT
81 CP = (EF/(TL-TS))+0.170
DEN = (460.0-(0.01818*(TBAR-1700.0)))/1728.0
CAPRHD = CP*DEN
GO TO 140
C ... DETERMINATION OF CAPRHD FOR MOLTEN ELEMENT
90 E1 = 0.0
E2 = 0.0
IF (TBAR.GT.2800.0) GO TO 100
CAPRHD = 0.172*(460.0-(0.01818*(TBAR-1700.0)))/1728.0
GO TO 140
100 CAPRHD = 0.172*435.0/1728.0
GO TO 140
C ... DETERMINATION OF CAPRHD FOR SOLID ELEMENT
110 E1 = 0.0
E2 = 0.0
IF (TBAR.GT.1700.0) GO TO 130
IF (TBAR.GT.1400.0) GO TO 120
CP = 0.1283-(2.438E-04*TBAR)+(9.372E-07*TBAR*TBAR)
*-(1.102E-09*TBAR*TBAR*TBAR)+(4.424E-13*TBAR*TBAR*TBAR*TBAR)
DEN = (490.0-(0.01147*TBAR))/1728.0
CAPRHD = CP*DEN
GO TO 140
120 CP = 3.949-(5.640E-03*TBAR)+(2.880E-06*TBAR*TBAR)

```



```

C ... IF (T(C).GT.TL) CP = 0.172
C ... THERMAL CONDUCTIVITY CALCULATION - FROM BETHLEHAM STEEL
TC = (36.767-(1.026E-02*T(C))-(1.323E-05*T(C)*T(C))+(1.003E-08*
* T(C)*T(C)*T(C))-(1.680E-12*T(C)*T(C)*T(C)*T(C)))/43200.0
C ... CALCULATION OF SOLID FRONT SPEED
SFS = PS*(RR+SFL)/RR
C ... CALCULATION OF BULK SPEED
WMP = PT+(2.0*RR)-(2.0*((RR*RR)-(CPMP*CPMP))*0.5))
BS = PS*PT*COS(ASIN(CPMP/(RR+SFL)))/WMP
C ... CALCULATION OF RELATIVE SPEED
RS = SFS-BS
C ... CONVECTION HEAT TRANSFER COEFFICIENT CALCULATION
CPL = RR*ASIN(CPMP/RR)
H = 0.530*((DEN*CP*TC*RS/(TPL-CPL))*0.5)
HSAVED = H
CECONDNR = H*(T(C)-TPO)*(R(J)-SFL)/(T(J)-TPO)
C ... INITIAL CECONDNR CALCULATION ONLY
IF (NPASS.LT.1.AND.NSTEP.LT.2) CECONDNR = H*
* (T(C)-T(I))*(R(J)-R(I))/(T(J)-T(I))
IF (R(I).LT.SFL.AND.R(J).LE.(SFL+CBLT)) GO TO 280
IF (R(I).LT.SFL.AND.R(J).GT.(SFL+CBLT)) GO TO 260
IF (R(J).LE.(SFL+CBLT)) GO TO 300
GO TO 260
250 IF (R(I).GE.(SFL+CBLT)) GO TO 260
C ... BOUNDARY LAYER CONDUCTIVITY CALCULATIONS (BLCONDNR)
TAV = T(I)+(0.5*(T(J)-T(I))*(SFL+CBLT-R(I))/(R(J)-R(I)))
IF (R(J).LE.(SFL+CBLT)) TAV = TBAR
BLCONDNR = CMFBL*(36.767-(1.026E-02*TAV)-(1.323E-05*TAV*TAV)+
* (1.003E-08*TAV*TAV*TAV)-(1.680E-12*TAV*TAV*TAV*TAV))/43200.0
IF (R(J).LE.(SFL+CBLT)) GO TO 310
C ... MOLTEN CONDUCTIVITY CALCULATIONS (CONDNR)
260 TAV = 0.5*(T(I)+T(J)+(T(J)-T(I))*(SFL+CBLT-R(I))/(R(J)-R(I)))
IF (R(I).GE.(SFL+CBLT)) TAV = TBAR
CONDNR = CMFM*(36.767-(1.026E-02*TAV)-(1.323E-05*TAV*TAV)+
* (1.003E-08*TAV*TAV*TAV)-(1.680E-12*TAV*TAV*TAV*TAV))/43200.0
IF (R(I).GE.(SFL+CBLT)) GO TO 320
IF (R(I).GT.SFL.AND.R(J).GT.(SFL+CBLT)) GO TO 290
IF (R(I).LT.SFL.AND.R(J).GT.(SFL+CBLT)) GO TO 270
C ... CONVECTION ENHANCED + MOLTEN ELEMENT
CONDNR = 0.0
CECONDNR = CECONDNR*CBLT/(R(J)-R(I))
BLCONDNR = 0.0
CONDNR = CONDNR*(R(J)-SFL-CBLT)/(R(J)-R(I))
GO TO 340
C ... SOLID, CONVECTION ENHANCED, + MOLTEN ELEMENT
270 CONDNR = CONDNR*(SFL-R(I))/(R(J)-R(I))
CECONDNR = CECONDNR*CBLT/(R(J)-R(I))
BLCONDNR = 0.0
CONDNR = CONDNR*(R(J)-SFL-CBLT)/(R(J)-R(I))
GO TO 340
C ... SOLID + CONVECTION ENHANCED ELEMENT
280 CONDNR = CONDNR*(SFL-R(I))/(R(J)-R(I))
CECONDNR = CECONDNR*(R(J)-SFL)/(R(J)-R(I))
BLCONDNR = 0.0
CONDNR = 0.0
GO TO 340
C ... BOUNDARY LAYER + MOLTEN ELEMENT
290 CONDNR = 0.0
CECONDNR = 0.0
BLCONDNR = BLCONDNR*(SFL+CBLT-R(I))/(R(J)-R(I))
CONDNR = CONDNR*(R(J)-SFL-CBLT)/(R(J)-R(I))
H = 0.0
GO TO 340
C ... CONVECTION ENHANCED ELEMENT
300 CONDNR = 0.0
BLCONDNR = 0.0
CONDNR = 0.0
GO TO 340
C ... BOUNDARY LAYER ELEMENT
310 CONDNR = 0.0
CECONDNR = 0.0

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CONDNR = 0.0
H = 0.0
GO TO 340
C *** MOLTEN ELEMENT
320 CONDNR = 0.0
CECONDNR = 0.0
BLCONDNR = 0.0
H = 0.0
GO TO 340
C *** SOLID ELEMENT
330 CECONDNR = 0.0
BLCONDNR = 0.0
CONDNR = 0.0
H = 0.0
340 CONDNR = CONDNR+CECONDNR+BLCONDNR+CONDNR
IF (INDEXP.LT.0) GO TO 400
IF (N.LT.(K-NR).OR.N.GT.(K+NR)) GO TO 400
PRINT 350,N,CAPRHO,E1,E2,T(I),T(J)
350 FORMAT (3X,"N = ",I15," CAPRHO = ",E10.3," E1 = ",E14.3,
* " E2 = ",E14.3," T(I) = ",E12.5," T(J) = ",E12.5)
PRINT 360,CONDNR,CONDNR,CECONDNR,BLCONDNR,CONDNR,SFL
360 FORMAT (3X,"CONDNR = ",E11.3," CONDNR = ",E10.3," CECONDNR = ",
* E9.3," BLCONDNR = ",E9.3," CONDNR = ",E10.3," SFL = ",E13.5)
PRINT 370,CBLT,CPMP,CPL,WMP,SFS,BS
370 FORMAT (3X,"CBLT = ",E12.5," CPMP = ",E12.5," CPL = ",E13.5,
* " WMP = ",E13.5," SFS = ",E13.5," BS = ",E14.5)
PRINT 380,RS,T(C),DEN,CP,TC,H
380 FORMAT (3X,"RS = ",E14.5," T(C) = ",E12.5," DEN = ",E13.3,
* " CP = ",E14.3," TC = ",E14.3," H = ",E15.4)
PRINT 390
390 FORMAT (3X,"*****")
400 CONTINUE
RETURN
END

```

TITLE : THIN-STRIP CASTING - MOLTEN POOL MODEL
- INPUT DATA REQUIRED IN DECK & USERP -
UNITS - BTU,IN,SEC,LBM,DEG F

MATERIALS

1 1

LOW-CARBON STEEL BY USERP

NODES

251 0
1 250 0.0
0.0625

ELEMENTS

250
1 1 2 1
250 250 251 1

STEP

0.0 0.0314001 *TIME END = 0.031397*
0.5 0.0001 1 500.0 -50 1
0.5 0.5

37

0.0001 4 0.0001
0.0010 30 0.0010
0.0314

INITIAL

0
34 2854.0 217 1

CONVECTION

1
1 3.998E-04 125.0

PLOTS

$$\begin{array}{r} 1 \\ + 300 \\ \hline 314 \end{array}$$

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LEICE

OF DATA

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.PROC, PPDECK, TYPE=6A, RSF=MPNTB, NTF=NTB, PPPAR=PPPARB, FMPSD=31.03,
PTD=30.125003, MND=500, LMPN=249, LMPX=30.062253, HMPN=251,
HMPX=30.062753, NED=499, CCL=33.998E-043, CC2=32.894E-053,
AMBL=0500, AMBH=1000, MPND=250, LCN=033, RCN=466, TESTIT.
TESTIT-RETURN, JOBIN.
TESTIT-REQUEST, JOBIN, *Q.
IF, 3, TYPE, 3, EQ, 36A3, SKIP01.
TESTIT-COPY, JDATA1, JOBIN.
ELSE, SKIP01.
TESTIT-COPY, JDATA2, JOBIN.
ENDIF, SKIP01.
TESTIT-ROUTE, JOBIN, DC=IN, TID=AH.
TESTIT-RETURN, JDATA1, JDATA2.
REVERT.
.DATA, JDATA1.
DLKPP, T77, P1, STANY.
ACCN, ID=DLK, C=61A007203.
ATTACH, OKWLIB, ID=OKW.
LIBRARY, CKWLIB.
ATTACH, TAPE12, RSF, ID=DLK.
REQUEST, TAPE13, *PF.
REQUEST, TAPE14, *PF.
SINGLE, FICHE=0, FN=SLG02, IDS=HGK, USUB=4.
REWIND, TAPE13.
REWIND, TAPE14.
CATALOG, TAPE13, NTF, ID=DLK, PW=DLK, XR=DLK, RP=10.
CATALOG, TAPE14, PPPAR, ID=DLK, PW=DLK, XR=DLK, RP=30.
RETURN, TAPE12.
PURGE, RSF, ID=DLK, PW=DLK.
BEGIN, FR80POP.
ATTACH, PPDECK, ID=DLK.
BEGIN, TMAKE, PPDECK, NTF.
*END

```

```

*OK USERSUB
SUBROUTINE USEROT (NITER, ISTEP, T, DT, TIME)
C ... NITER = ITERATION NUMBER FOR "ISTEP" **DO NOT CHANGE**
CC ... ISTEP = SOLUTION TIME STEP NUMBER **DO NOT CHANGE**
CC ... T = VECTOR OF NODE TEMPERATURES **DO NOT CHANGE**
CC ... DT = SOLUTION TIME STEP INCREMENT
C ... TIME = CURRENT TIME PRIOR TO DT CHANGE **DO NOT CHANGE**
C ... USER SUPPLIED PORTION OF USEROT FROM THIS POINT
COMMON/USEDOT/NPASS, NSTEP, TSTEP
DIMENSION T(1)
TSTEP = DT
NSTEP = ISTEP
NPASS = NITER
RETURN
END
SUBROUTINE USERP (N, I, J, MAT, RHO, CONOR,
*CAPC, CAPRHO, ITRAN, TBAR, TIME, R, T)
C ... N = ELEMENT NUMBER **DO NOT CHANGE**
CC ... I = I-TH NODE OF ELEMENT **DO NOT CHANGE**
CC ... J = J-TH NODE OF ELEMENT **DO NOT CHANGE**
CC ... MAT = MATERIAL NUMBER **DO NOT CHANGE**
CC ... RHO = DENSITY
CC ... CONOR = THERMAL CONDUCTIVITY
CC ... NOTE: THERMAL CONDUCTIVITY MUST BE .GE. ZERO
CC ... CAPC = SPECIFIC HEAT
CC ... CAPRHO = CAPC*RHO
CC ... NOTE: AFTER THIS SUBROUTINE IS CALLED, CAPRHO
CC ... IS USED BUT CAPC AND RHO ARE NOT
CC ... ITRAN = 0, TBAR MEANINGLESS; = 1, TBAR JKAY **DO NOT CHANGE**
CC ... TBAR = AVERAGE ELEMENT TEMPERATURE **DO NOT CHANGE**
CC ... TIME = TIME **DO NOT CHANGE**
CC ... R = VECTOR OF NODE R OR X VALUES **DO NOT CHANGE**
CC ... T = VECTOR OF NODE TEMPERATURES **DO NOT CHANGE**
C ... USER SUPPLIED PORTION OF USERP FROM THIS POINT
COMMON/CELEM/NE, IOUN1, IOUN2
COMMON/USEDOT/NPASS, NSTEP, TSTEP
COMMON/USERP/NN, PS
DIMENSION R(1), T(1), JTS(12), UTST(12)

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```

C ... PARAMETER INPUT DATA
DATA EF/119.0/, FMPS/FMPSD/, INDEXP/-1/, KPI/14/, KPT/2/
DATA NSTEPS/128/, PT/PTD/, TL/2754.0/, TS/2695.0/
DATA UTS/60000.0, 67000.0, 67500.0, 59500.0, 41000.0,
*21000.0, 10500.0, 5500.0, 3500.0, 2600.0, 2300.0, 0.0/
DATA UTST/70.0, 300.0, 500.0, 700.0, 900.0, 1100.0,
*1300.0, 1500.0, 1700.0, 1900.0, 2100.0, 2695.0/
C ... EF = ENERGY OF FUSION
C ... FMPS = FRACTION OF MAXIMUM PRODUCT SPEED
C ... IF INDEXP .LT. 0, NO DEBUG PRINT; IF .GT. 0, DEBUG PRINT
C ... KPI = ULTIMATE TENSILE STRENGTH PRINT INTERVAL
C ... KPT = ULTIMATE TENSILE STRENGTH PRINT TIME
C ... NE = NUMBER OF ELEMENTS
C ... NN = NUMBER OF NODES
C ... NPASS = ITERATION NUMBER FOR "NSTEP"
C ... NSTEP = SOLUTION TIME STEP NUMBER
C ... NSTEPS = TOTAL NUMBER OF TIME STEPS IN TRANSIENT
C ... PS = PRODUCT SPEED
C ... PT = PRODUCT THICKNESS
C ... TL = MATERIAL LIQUIDUS TEMPERATURE
C ... TS = MATERIAL SOLIDUS TEMPERATURE
C ... TSTEP = SOLUTION TIME STEP INCREMENT
C ... UTS = ULTIMATE TENSILE STRENGTH (PSI)
C ... UTST = ULTIMATE TENSILE STRENGTH TEMPERATURES (F)
C ... CAPRHO CALCULATIONS THRU STATEMENT 130
C ... CHECK TO SEE IF ELEMENT IS SOLID & BRANCH ACCORDINGLY
IF (T(I).LT.TS.AND.T(J).LT.TS) GO TO 110
C ... CHECK TO SEE IF ELEMENT IS MOLTEN & BRANCH ACCORDINGLY
IF (T(I).GT.TL.AND.T(J).GT.TL) GO TO 90
C ... BRANCH FOR A ZERO ELEMENT GRADIENT
IF (T(I).EQ.T(J)) GO TO 81
C ... DETERMINATION OF CAPRHO IN A TWO-PHASE ELEMENT
C ... ELEMENT TEMPERATURES ORDERED FOR INTEGRATION
IF (T(I).GT.T(J)) GO TO 10
T1 = T(I)
T2 = T(J)
GO TO 20
10 T1 = T(J)
T2 = T(I)
C ... INTEGRATION OF CAPRHO THRU STATEMENT 70
20 IF (T1.LT.TS) GO TO 30
E1 = ((T1-TS)/(TL-TS))*EF*(460.0-(0.01818*(T1-1700.0)))/1728.0
INDEXR = 2
GO TO 40
30 E1 = 0.0
INDEXR = 1
40 IF (T2.LE.TL.AND.INDEXR.EQ.2) GO TO 70
IF (T2.LE.TL.AND.INDEXR.EQ.1) GO TO 60
IF (INDEXR.EQ.2) GO TO 50
E2 = ((0.168*490.906*TS)-(0.00909*0.168*TS*TS)
*-(0.168*490.906*T1)+(0.00909*0.168*T1*T1)
*+((460.0-(0.01818*(TL-1700.0)))*EF)
*+(0.172*490.906*T2)-(0.00909*0.172*T2*T2)
*-(0.172*490.906*TL)+(0.00909*0.172*TL*TL))/1728.0
GO TO 80
50 E2 = (((460.0-(0.01818*(TL-1700.0)))*EF)
*+(0.172*490.906*T2)-(0.00909*0.172*T2*T2)
*-(0.172*490.906*TL)+(0.00909*0.172*TL*TL))/1728.0
GO TO 80
60 E2 = ((0.168*490.906*TS)-(0.00909*0.168*TS*TS)
*-(0.168*490.906*T1)+(0.00909*0.168*T1*T1)
*+(((T2-TS)/(TL-TS))*EF*(460.0-(0.01818*(T2-1700.0)))))/1728.0
GO TO 80
70 E2 = ((T2-TS)/(TL-TS))*EF*(460.0-(0.01818*(T2-1700.0)))/1728.0
80 CAPRHO = (E2-E1)/(T2-T1)
GO TO 140
C ... DETERMINATION OF CAPRHO FOR A ZERO ELEMENT GRADIENT
81 CP = (EF/(TL-TS))+0.170
DEN = (460.0-(0.01818*(T3AR-1700.0)))/1728.0
CAPRHO = CP*DEN
GO TO 140

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C ... DETERMINATION OF CAPRHO FOR MOLTEN ELEMENT
90 E1 = 0.0
   E2 = 0.0
   IF (TBAR.GT.2800.0) GO TO 100
   CAPRHO = 0.172*(460.0-(0.01818*(TBAR-1700.0)))/1728.0
   GO TO 140
100 CAPRHO = 0.172*435.0/1728.0
   GO TO 140
C ... DETERMINATION OF CAPRHO FOR SOLID ELEMENT
110 E1 = 0.0
   E2 = 0.0
   IF (TBAR.GT.1700.0) GO TO 130
   IF (TBAR.GT.1400.0) GO TO 120
   CP = 0.1283-(2.438E-04*TBAR)+(9.372E-07*TBAR*TBAR)
   *-(1.102E-09*TBAR*TBAR*TBAR)+(4.424E-13*TBAR*TBAR*TBAR*TBAR)
   DEN = (490.0-(0.01147*TBAR))/1728.0
   CAPRHO = CP/DEN
   GO TO 140
120 CP = 3.949-(5.640E-03*TBAR)+(2.880E-06*TBAR*TBAR)
   *-(5.567E-10*TBAR*TBAR*TBAR)+(2.638E-14*TBAR*TBAR*TBAR*TBAR)
   DEN = (490.0-(0.01147*TBAR))/1728.0
   CAPRHO = CP/DEN
   GO TO 140
130 CAPRHO = 0.168*(460.0-(0.01818*(TBAR-1700.0)))/1728.0
C ... CONDOR CALCULATION
140 CONDOR = (36.767-(1.026E-02*TBAR)-(1.323E-05*TBAR*TBAR)+(1.003
   *E-08*TBAR*TBAR*TBAR)-(1.680E-12*TBAR*TBAR*TBAR*TBAR))/43200.0
C ... BRANCH TO BYPASS INITIALIZATION CALCULATIONS
   IF (N.GT.1.OR.NPASS.GT.0.OR.NSTEP.GT.1) GO TO 150
C ... CALCULATION OF NUMBER OF NODES
   NN = NE+1
C ... CALCULATION OF PRODUCT SPEED
   PS = (192.0*FMPS)/(24.0*PT)
C ... ULTIMATE TENSILE STRENGTH CALCULATIONS AND OUTPUT FOR PLOTTING
150 IF (N.GT.1.OR.NPASS.GT.0.OR.(NSTEP-1).NE.KPT) GO TO 200
   WRITE (14,160) KPT
160 FORMAT (I3)
   KPT = KPT+KPI
   DO 180 L = 1,NN
   CALL TABLE (12,UTST,UTS,T(L),Y)
   WRITE (14,170) R(L),Y
170 FORMAT (E11.5,1X,E11.5)
180 CONTINUE
   IF (KPT.LE.NTSTEPS) GO TO 200
   WRITE (14,190)
190 FORMAT ("END")
200 IF (INDEXP.LT.0) GO TO 220
   PRINT 210,N,CAPRHO,E1,E2,T(I),T(J)
210 FORMAT (3X,"N =",I15," CAPRHO =",E10.3," E1 =",E14.3,
   *E2 =",E14.3," T(I) =",E12.5," T(J) =",E12.5)
220 CONTINUE
   RETURN
   END
SUBROUTINE USERH (I,BH,BTF,AREA,TIME,T)
C ... I = NODE NUMBER **DO NOT CHANGE**
C ... BH = CONVECTION HEAT TRANSFER COEFFICIENT
C ... BTF = CONVECTION TEMPERATURE
C ... AREA = CONVECTION HEAT TRANSFER AREA **DO NOT CHANGE**
C ... TIME = TIME **DO NOT CHANGE**
C ... T = VECTOR OF NODE TEMPERATURES **DO NOT CHANGE**
C ... USER SUPPLIED PORTION OF USERH FROM THIS POINT
COMMON/USEDT/NPASS,NSTEP,TSTEP
COMMON/USEP/NN,PS
DIMENSION T(1)
C ... NODE 1 HEAT LOSS CALCULATIONS
   IF (I.GT.1.OR.NPASS.GT.0) GO TO 30
C ... INITIALIZATION OF HEAT LOSS PARAMETERS
   IF (NSTEP.GT.1) GO TO 10
   BH1 = BH
   BTF1 = BTF
   T1 = T(1)

```



```

20 PRINT 20,Q1,TME
   FORMAT (3X,"THE INTEGRATED RATE OF HEAT LOSS FROM NODE 1 BY RA
C *** "DIATION = ",E12.5," BTU/SEC THRU ",E11.5," TRANSIENT SECONDS."
30 IF (I.LT.NN.OR.NPASS.GT.0) GO TO 60
C *** INITIALIZATION OF HEAT LOSS PARAMETERS
   IF (NSTEP.GT.1) GO TO 40
   BRNN = BR
   BTRNN = BTR
   TNN = T(NN)
   QNN = 0.0
C *** AVERAGING PARAMETERS
40 IF (NSTEP.LT.2) GO TO 60
   TME = TIME-TSTEP
   BRA = (BR+BRNN)/2.0
   BTRA = (BTR+BTRNN)/2.0
   TNNA = ((T(NN)+TNN)/2.0)+RAOC
   QNN = QNN+(BRA*PS*TSTEP*24.0*((TNNA**4)-(BTRA**4)))
   BRNN = BR
   BTRNN = BTR
   TNN = T(NN)
   PRINT 50,NN,QNN,TME
50 FORMAT (3X,"THE INTEGRATED RATE OF HEAT LOSS FROM NODE ",I3,
   " BY RADIATION = ",E12.5," BTU/SEC THRU ",E11.5," TRANSIENT ",
   "SECONDS.",///)
60 CONTINUE
   RETURN
   END

```

.EOR
 TITLE : THIN-STRIP CASTING - PINCH POINT MODEL
 - INPUT DATA REQUIRED IN DECK & USERP -
 UNITS - BTU,IN,SEC,LBM,DEG F

MATERIALS
 1
 1 LOW-CARBON STEEL BY USERP

NODES
 NND 0
 1 LMPN 0.00000
 LMPX
 HMPN LMPN HMPX
 PTD

ELEMENTS
 NED
 1 1 2 1
 NED NED NND 1

STEP *TIME END = 1.31252*
 0.0313999 1.3114 0.010 1 500.0 -50 1
 0.5 0.5 0.5
 33
 0.0414 31 0.040
 1.3114

FUNCTIONS
 8
 1 3 1 CONVECTION COEFFICIENT FOR NODE 1 (BTU/SEC*IN*IN*F)
 0.0 CC1 0.25209 CC1 0.26210 CC2
 2 3 1 CONVECTION TEMPERATURE FOR NODE 1 (F)
 0.0 125.0 0.26209 125.0 0.26210 AMBL 0
 3 3 1 CONVECTION COEFFICIENT FOR NODE NN (BTU/SEC*IN*IN*F)
 0.0 CC2 0.26209 CC2 0.26210 CC1
 4 3 1 CONVECTION TEMPERATURE FOR NODE NN (F)
 0.0 AMBH 0 0.26209 AMBH 0 0.26210 125.0
 5 3 1 RADIATION FACTOR FOR NODE 1 (BTU/SEC*IN*IN*R*R*R*R)
 0.0 0.0 0.26209 0.0 0.26210 1.653E-15
 6 3 1 RADIATION TEMPERATURE FOR NODE 1 (F)


```

0.0 0.0 0.26209 0.0 0.26210 AMBL*0
3 0.3 1.653E-15 RADIATION FACTOR FOR NODE NN (BTU/SEC*IN**R**R)
7 0.0 1.653E-15 RADIATION TEMPERATURE FOR NODE NN (F)
8 0.0 1.653E-15 AMBL*0
0.0 0.0 0.26209 0.0 0.26210

```

INITIAL

CONNECTION
2

1	0	0	1	1	2
2	0	0	0	0	4
3	0	0	0	0	4
4	0	0	0	0	4
5	0	0	0	0	4
6	0	0	0	0	4
7	0	0	0	0	4
8	0	0	0	0	4
9	0	0	0	0	4
10	0	0	0	0	4
11	0	0	0	0	4
12	0	0	0	0	4
13	0	0	0	0	4
14	0	0	0	0	4
15	0	0	0	0	4
16	0	0	0	0	4
17	0	0	0	0	4
18	0	0	0	0	4
19	0	0	0	0	4
20	0	0	0	0	4
21	0	0	0	0	4
22	0	0	0	0	4
23	0	0	0	0	4
24	0	0	0	0	4
25	0	0	0	0	4
26	0	0	0	0	4
27	0	0	0	0	4
28	0	0	0	0	4
29	0	0	0	0	4
30	0	0	0	0	4
31	0	0	0	0	4
32	0	0	0	0	4
33	0	0	0	0	4
34	0	0	0	0	4
35	0	0	0	0	4
36	0	0	0	0	4
37	0	0	0	0	4
38	0	0	0	0	4
39	0	0	0	0	4
40	0	0	0	0	4
41	0	0	0	0	4
42	0	0	0	0	4
43	0	0	0	0	4
44	0	0	0	0	4
45	0	0	0	0	4
46	0	0	0	0	4
47	0	0	0	0	4
48	0	0	0	0	4
49	0	0	0	0	4
50	0	0	0	0	4
51	0	0	0	0	4
52	0	0	0	0	4
53	0	0	0	0	4
54	0	0	0	0	4
55	0	0	0	0	4
56	0	0	0	0	4
57	0	0	0	0	4
58	0	0	0	0	4
59	0	0	0	0	4
60	0	0	0	0	4
61	0	0	0	0	4
62	0	0	0	0	4
63	0	0	0	0	4
64	0	0	0	0	4
65	0	0	0	0	4
66	0	0	0	0	4
67	0	0	0	0	4
68	0	0	0	0	4
69	0	0	0	0	4
70	0	0	0	0	4
71	0	0	0	0	4
72	0	0	0	0	4
73	0	0	0	0	4
74	0	0	0	0	4
75	0	0	0	0	4
76	0	0	0	0	4
77	0	0	0	0	4
78	0	0	0	0	4
79	0	0	0	0	4
80	0	0	0	0	4
81	0	0	0	0	4
82	0	0	0	0	4
83	0	0	0	0	4
84	0	0	0	0	4
85	0	0	0	0	4
86	0	0	0	0	4
87	0	0	0	0	4
88	0	0	0	0	4
89	0	0	0	0	4
90	0	0	0	0	4
91	0	0	0	0	4
92	0	0	0	0	4
93	0	0	0	0	4
94	0	0	0	0	4
95	0	0	0	0	4
96	0	0	0	0	4
97	0	0	0	0	4
98	0	0	0	0	4
99	0	0	0	0	4
100	0	0	0	0	4

RADIATION
2

460.00
11.00
57

PLOTS	
10	1
16	PTD
30	44
58	0.0
72	3100.0
86	500.0
100	114
128	

TPLOTS 999 40
(P)PRODUCT CENTERLINE AND CLAD SURFACE TEMPERATURES.

MPND	MPND	MPND
0.0	500.0	3100.0
1.0		
1.3114		

(1) TEMPERATURE IN SECONDS
(T) TEMPERATURE IN DEGREES (F) AHRENHEIT
(L) LEFT-HAND CLAD TEMPERATURES.

LCN	500.0	3100.0
1	1.3114	
2	0.0	

(T) TIME IN SECONDS
(T) TEMPERATURE IN DEGREES (F) AHRENHEIT
(T) TEMPERATURE IN DEGREES (C) CENTIGRADE
(R) RIGHT-HAND CLAD TEMPERATURES.

2	RCH	NND	500.0	3100.0
	0.0	1.3114		

(T) TIME IN SECONDS
(T) TEMPERATURE IN DEGREES (F) AHRENHEIT

SINGLE/CE

[illegible]


```

E1 = ((T1-TS)/(TL-TS))*EF*(460.0-(0.01818*(T1-1700.0)))/1728.0
INDEXR = 2
GO TO 40
30 E1 = 0.0
INDEXR = 1
40 IF (T2.LE.TL.AND.INDEXR.EQ.2) GO TO 70
IF (T2.LE.TL.AND.INDEXR.EQ.1) GO TO 60
IF (INDEXR.EQ.2) GO TO 50
E2 = ((0.168*490.906*TS)-(0.00909*0.168*TS*TS)
*-(0.168*490.906*T1)+(0.00909*0.168*T1*T1)
*+((460.0-(0.01818*(TL-1700.0)))*EF)
*-(0.172*490.906*T2)-(0.00909*0.172*T2*T2)
*-(0.172*490.906*TL)+(0.00909*0.172*TL*TL))/1728.0
GO TO 80
50 E2 = ((460.0-(0.01818*(TL-1700.0)))*EF)
*-(0.172*490.906*T2)-(0.00909*0.172*T2*T2)
*-(0.172*490.906*TL)+(0.00909*0.172*TL*TL))/1728.0
GO TO 80
60 E2 = ((0.168*490.906*TS)-(0.00909*0.168*TS*TS)
*-(0.168*490.906*T1)+(0.00909*0.168*T1*T1)
*+(((T2-TS)/(TL-TS))*EF*(460.0-(0.01818*(T2-1700.0)))))/1728.0
GO TO 80
70 E2 = ((T2-TS)/(TL-TS))*EF*(460.0-(0.01818*(T2-1700.0)))/1728.0
80 CAPRHO = (E2-E1)/(T2-T1)
GO TO 140
C ... DETERMINATION OF CAPRHO FOR A ZERO ELEMENT GRADIENT
81 CP = (EF/(TL-TS))+0.170
DEN = (460.0-(0.01818*(TBAR-1700.0)))/1728.0
CAPRHO = CP*DEN
GO TO 140
C ... DETERMINATION OF CAPRHO FOR MOLTEN ELEMENT
90 E1 = 0.0
E2 = 0.0
IF (TBAR.GT.2800.0) GO TO 100
CAPRHO = 0.172*(460.0-(0.01818*(TBAR-1700.0)))/1728.0
GO TO 140
100 CAPRHO = 0.172*435.0/1728.0
GO TO 140
C ... DETERMINATION OF CAPRHO FOR SOLID ELEMENT
110 E1 = 0.0
E2 = 0.0
IF (TBAR.GT.1700.0) GO TO 130
IF (TBAR.GT.1400.0) GO TO 120
CP = 0.1283-(2.438E-04*TBAR)+(9.372E-07*TBAR*TBAR)
*-(1.102E-09*TBAR*TBAR*TBAR)+(4.424E-13*TBAR*TBAR*TBAR*TBAR)
DEN = (490.0-(0.01147*TBAR))/1728.0
CAPRHO = CP*DEN
GO TO 140
120 CP = 3.949-(5.640E-03*TBAR)+(2.880E-06*TBAR*TBAR)
*-(5.567E-10*TBAR*TBAR*TBAR)+(2.038E-14*TBAR*TBAR*TBAR*TBAR)
DEN = (490.0-(0.01147*TBAR))/1728.0
CAPRHO = CP*DEN
GO TO 140
130 CAPRHO = 0.168*(460.0-(0.01818*(TBAR-1700.0)))/1728.0
C ... CONDR CALCULATION
140 CONDR = (36.767-(1.026E-02*TBAR)-(1.323E-05*TBAR*TBAR)+(1.003
*E-08*TBAR*TBAR*TBAR)-(1.680E-12*TBAR*TBAR*TBAR*TBAR))/43200.0
C ... BRANCH TO BYPASS INITIALIZATION CALCULATIONS
IF (N.GT.1.OR.NPASS.GT.0.OR.NSTEP.GT.1) GO TO 150
C ... CALCULATION OF NUMBER OF NODES
NN = NE+1
C ... CALCULATION OF PRODUCT SPEED
PS = (192.0*FMPS)/(24.0*PT)
C ... ULTIMATE TENSILE STRENGTH CALCULATIONS AND OUTPUT FOR PLOTTING
150 IF (N.GT.1.OR.NPASS.GT.0.OR.(NSTEP-1).NE.KPT) GO TO 200
WRITE (14,160) KPT
160 FORMAT (I3)
KPT = KPT+KPI
DO 180 L = 1,NN
CALL TABLE (12,UTST,UTS,T(L),Y)
WRITE (14,170) R(L),Y

```



```

C ... BR = SIGMA*SHAPE FACTOR*EMISSIVITY FOR NODE
C ... BTR = T-INFINITY IN ABSOLUTE UNITS
C ... AREA = RADIATION HEAT TRANSFER AREA **DO NOT CHANGE**
C ... RADC = ADDITIVE TO CHANGE TEMPERATURE **DO NOT CHANGE**
C ... TO ABSOLUTE UNITS
C ... TIME = TIME **DO NOT CHANGE**
C ... T = VECTOR OF NODE TEMPERATURES - **DO NOT CHANGE**
C ... NOT IN ABSOLUTE UNITS UNLESS RADC = 0
C ... USER SUPPLIED PORTION OF USERP FROM THIS POINT
COMMON/USEDT/NPASS,NSTEP,TSTEP
COMMON/USEP/NN,PS
DIMENSION T(1)
C ... NODE 1 HEAT LOSS CALCULATIONS
IF (I.GT.1.OR.NPASS.GT.0) GO TO 30
C ... INITIALIZATION OF HEAT LOSS PARAMETERS
IF (NSTEP.GT.1) GO TO 10
BR1 = BR
BTR1 = BTR
T1 = T(1)
Q1 = 0.0
C ... AVERAGING PARAMETERS
10 IF (NSTEP.LT.2) GO TO 30
TME = TIME-TSTEP
BRA = (BR+BR1)/2.0
BTRA = (BTR+BTR1)/2.0
T1A = ((T(1)+T1)/2.0)+RADC
Q1 = Q1+(BRA*PS*TSTEP*24.0*((T1A**4)-(BTRA**4)))
BR1 = BR
BTR1 = BTR
T1 = T(1)
PRINT 20,Q1,TME
20 FORMAT (3X,"THE INTEGRATED RATE OF HEAT LOSS FROM NODE 1 BY RA",
* "RADIATION" = ",E12.5," BTU/SEC THRU ",E11.5," TRANSIENT SECONDS.")
C ... NODE NN HEAT LOSS CALCULATIONS
30 IF (I.LT.NN.OR.NPASS.GT.0) GO TO 60
C ... INITIALIZATION OF HEAT LOSS PARAMETERS
IF (NSTEP.GT.1) GO TO 40
BRNN = BR
BTRNN = BTR
TNN = T(NN)
QNN = 0.0
C ... AVERAGING PARAMETERS
40 IF (NSTEP.LT.2) GO TO 60
TME = TIME-TSTEP
BRA = (BR+BRNN)/2.0
BTRA = (BTR+BTRNN)/2.0
T1A = ((T(NN)+TNN)/2.0)+RADC
QNN = QNN+(BRA*PS*TSTEP*24.0*((T1A**4)-(BTRA**4)))
BRNN = BR
BTRNN = BTR
TNN = T(NN)
PRINT 50,NN,QNN,TME
50 FORMAT (3X,"THE INTEGRATED RATE OF HEAT LOSS FROM NODE ",I3,
* " BY RADIATION" = ",E12.5," BTU/SEC THRU ",E11.5," TRANSIENT ",
* "SECONDS.",///)
60 CONTINUE
RETURN
END

```

.EOR
 TITLE : THIN-STRIP CASTING - PINCH POINT MODEL
 - INPUT DATA REQUIRED IN DECK & USERP -
 UNITS - BTU, IN, SEC, LBM, DEG F

MATERIALS

1 1

LOW-CARBON STEEL BY USERP

NODES

NND

1

0

LMPN

0.00000

HMPN LMPN LMPX
HMPX
PTD

ELEMENTS

NED
1 1 2 1
NED NED NND 1

STEP 0.0627999 2.6228 *TIME END = 2.62504* 1 500.0 -50 1
0.5 0.5 0.5

33
0.0828 31 0.080
2.6228

FUNCTIONS

8
1 3 1 CONVECTION COEFFICIENT FOR NODE 1 (BTU/SEC*IN*IN*F)
0.0 CC1 0.52418 CC1 0.52419 CC2
2 3 1 CONVECTION TEMPERATURE FOR NODE 1 (F)
0.0 125.0 0.52418 125.0 0.52419 AMBL 0.0
3 3 1 CONVECTION COEFFICIENT FOR NODE NN (BTU/SEC*IN*IN*F)
0.0 CC2 0.52418 CC2 0.52419 CC1
4 3 1 CONVECTION TEMPERATURE FOR NODE NN (F)
0.0 AMBH 0.0 0.52418 AMBH 0.0 0.52419 125.0
5 3 1 RADIATION FACTOR FOR NODE 1 (BTU/SEC*IN*IN*R*R*R*R)
0.0 0.0 0.52418 0.0 0.52419 1.653E-15
6 3 1 RADIATION TEMPERATURE FOR NODE 1 (F)
0.0 0.0 0.52418 0.0 0.52419 AMBL 0.0
7 3 1 RADIATION FACTOR FOR NODE NN (BTU/SEC*IN*IN*R*R*R*R)
0.0 1.653E-15 0.52418 1.653E-15 0.52419 0.0
8 3 1 RADIATION TEMPERATURE FOR NODE NN (F)
0.0 AMBH 0.0 0.52418 AMBH 0.0 0.52419 0.0

INITIAL

1

CONVECTION

2

1 1.0 1 1.0 2
NND 1.0 3 1.0 4

RADIATION

2

1 460.0 5 1.0 6
NND 1.0 7 1.0 8

PLOTS

10 PTD 0.0 3100.0 500.0
2 32 60 88 116 144 172 200 228 256

TPLOTS

3 999 40

(P)RODUCT CENTERLINE AND CLAD SURFACE TEMPERATURES.

3

1 MPND NND 500.0 3100.0
0.0 2.6228

(T)IME IN SECONDS

(T)EMPERATURE IN DEGREES (F)AHRENHEIT

(L)EFT-HAND CLAD TEMPERATURES.

2

1 LCN 500.0 3100.0
0.0 2.6228

(T)IME IN SECONDS

(T)EMPERATURE IN DEGREES (F)AHRENHEIT

(R)IGHT-HAND CLAD TEMPERATURES.

2

RCN NND 500.0 3100.0
0.0 2.6228

(T)IME IN SECONDS

(T)EMPERATURE IN DEGREES (F)AHRENHEIT

SINGLE/CE

END OF DATA
.PROC,TPMAKE,NTF.
RETURN,JOBIN.
REQUEST,JOBIN,*Q.
COPY,JDATA3,JOBIN.
ROUTE,JOBIN,DC=IN,TID=AH.
RETURN,JDATA3.
REVERT.
.DATA,JDATA3.
CLKTP,T37,P1,STANY,GE1.
ACCNT,ID=DLK,C=61AC07203.
ATTACH,JCWLIB,ID=JCW.
LIBRARY,JCWLIB.
ATTACH,NTF,ID=DLK.
TAPEMK,NTF,NTF.
RETURN,NTF.
PURGE,NTF,ID=DLK,PW=DLK.

Sub appendix D of Appendix C

HEAT LOADS

TABLE D-1. INTEGRATED HEAT LOSS RATES (IN BTU/SEC)
FOR 0.125 IN. PRODUCT THICKNESS CASES

Case	Upper Casting Roller	Lower Casting Roller	Cooling Roller	Inert Atmosphere	Total
Nominal ^a	63.1	375.7	1654.6	389.8	2483.2
1A ^a	62.4	374.4	1654.9	389.8	2481.5
1C ^a	63.9	377.2	1655.0	389.9	2486.0
2B ^a	63.8	376.6	1654.2	389.7	2484.3
2C ^a	65.4	379.6	1656.1	390.1	2491.2
3B ^a	67.6	383.3	1664.7	392.1	2507.7
3C ^a	72.8	391.1	1676.5	395.6	2536.0
4A ^a	64.0	375.7	1656.5	414.8	2511.0
5B ^a	51.3	350.4	1609.8	373.1	2384.6
5C ^a	40.6	320.5	1481.5	306.4	2149.0
6B ^a	69.6	391.3	1604.3	368.5	2433.7
7A ^a	40.3	143.1	652.7	387.7	1223.8
7C ^a	80.9	555.6	2404.0	391.5	3432.0
8B ^a	66.2	382.2	1667.3	392.8	2508.5
8C ^a	68.7	386.9	1677.8	396.0	2529.4
II ^b	63.8	376.9	1655.8	390.1	2486.6
III ^b	64.5	378.0	1657.3	390.4	2490.2

a. See Table 2 for case definition.

b. See Section 3.4 for case definition.

22-141 50 SHEETS
22-142 100 SHEETS
22-144 200 SHEETS

TABLE D-2. INTEGRATED HEAT LOSS RATES (IN BTU/SEC)
FOR 0.008 IN. CLAD THICKNESS CASES

<u>Case^a</u>	<u>Upper Casting Roller</u>	<u>Lower Casting Roller</u>	<u>Cooling Roller</u>	<u>Inert Atmosphere</u>	<u>Total</u>
9A	62.5	373.5	1561.7	339.8	2337.5
9B	62.5	373.2	1545.3	331.1	2312.1
9C	62.2	371.4	1495.3	305.3	2234.2
9D	60.2	362.1	1424.6	271.9	2118.8

a. Cases defined as follows :

- 9A - 0.080 in. product thickness (M/S = 4.0)
- 9B - 0.0775 in. product thickness (M/S = 3.8)
- 9C - 0.070 in. product thickness (M/S = 3.4)
- 9D - 0.064 in. product thickness (M/S = 3.0)

with all other parameters set as discussed in Section 3.3.

APPENDIX D

Keverian International, PRELIMINARY REPORT, PROGRAM REVIEW:

MATHEMATICAL ANALYSIS OF THIN-STRIP CONTINUOUS CASTING,

J. Keverian Ltr. to C. D. McMurtrey, April 27, 1973

KEVERIAN INTERNATIONAL
55 West Shore Drive
Pennington, NJ 08534
(609) 737-2909

April 27, 1983

Mr. C. D. McMurtrey
EG & G Idaho, Inc.
P.O. Box 1625
Idaho Falls, Idaho

Dear Mr. McMurtrey:

Enclosed find the report identified as Task 3 in Contract No. C82-110057 Modification Agreement No. 1. This is related to the project entitled: "Mathematical Analysis of Thin Strip Continuous Casting - HLB-8-83". This reports the results of my consultation activities as authorized and requested under Tasks 1 and 2 in this same contract.

If you have any questions regarding the contents of the enclosed or the list of items included for the balance of the work under this contract, do not hesitate to call.

Very truly yours,

Jack Keverian

PRELIMINARY REPORT

PROGRAM REVIEW:
MATHEMATICAL ANALYSIS
OF
THIN-STRIP CONTINUOUS CASTING

Submitted by:

DR. J. KEVERIAN
55 West Shore Drive
Pennington, N.J. 08534

Submitted to:

EG & G IDAHO, INC.

Consulting Contract No. C82-110057
Modification Agreement No. 1

I. EXECUTIVE SUMMARY

This is a preliminary report on EG & G activities in the thermal analysis of the double thin-strip casting process. The technical work of EG & G reviewed to date meets high professional standards. This assessment indicates that the model developed can meet its initial objectives for determining whether a cast thin strip has sufficient mechanical integrity to withstand the stress of passing through the casting machine. As a result of this review, additional effort, within the scope of this contract, will allow an assessment of the internal integrity/quality of the cast strip.

A detailed outline of work to complete this contract is included herein and is summarized as follows:

1. Compute the freezing front, freezing rate and temperature gradients to enable an assessment of the extent of centerline porosity/shrinkage and other aspects of the the cast product as related to casting integrity and quality.
2. Confirm SINGLE as an acceptable computer program for continuous casting applications.
3. Review basic calculations and assumptions used in the EG & G thermal analysis with emphasis on the validity and effect of variations in boundary conditions.

Other critical issues determining the technical feasibility of the Double Thin Strip Casting Process is reviewed within the context of this project. These critical issues include:

- Uniformity of molten metal flow without gas entrapment.
- Non-gas evolving carrier strip with tension control.
- Uniform and high heat transfer rates.
- Quench roll location and pressure to seal cast structural deficiencies.

II. PROGRAM REVIEW - PRELIMINARY

The following is a preliminary review of First Draft of the report entitled: "Thermal Model Description" (for Analysis of a Thin Strip Casting Process) as well as the technical presentation made on March 24, 1983 at Idaho Falls, Idaho. Both the report and the presentation assessed herein were made by D. L. Knudson - EG & G.

1. Thermal Model Objectives (D. Knudson - EG & G)

- A. Model the process to predict quasi-steady state thermal behavior of the system.
- B. Use the model to study sensitivity to variations in process variables.
- C. Assess the casting thermal feasibility of the process.

Items A, B and C were extracted from Mr. Knudson's presentation notes. Subsequent discussion further focused on these objectives by indicating that it was a major purpose of this thermal analysis to determine whether the cast thin strip would possess sufficient mechanical integrity to remain intact as the strip passed through the casting machine.

a. Review Comments

Based on the knowledge possessed by the reviewer, it is believed that the model developed can predict the quasi-state thermal behavior of the system. This is a preliminary assessment and additional effort will be included as part of the final review report to confirm or revise this preliminary assessment.

Although the original intent of the thermal analysis was to determine whether the thin cast strip had sufficient mechanical integrity to pass through the casting machine, it was noted that the thermal model could be extended readily to assess other important aspects of this process. Specifically, calculations of the start and end of freeze wave fronts will be made to assess the extent of centerline porosity/shrinkage in the thin strip. In addition, EG & G will calculate the freezing rate and temperature gradient at the liquid/solid interface. This information will be used to assess the nature of solidification i.e. plane front vs. cellular vs. dendritic solidification and/or the extent of mushy freezing and its effect on integrity/quality of the cast strip.

b. Recommended Actions and Responsibilities

<u>Responsibility</u>	<u>Action</u>
EG & G	Include in Final Report: 1. Start and End of Freeze Fronts 2. Freezing Rate at Liquid/Solid Interface 3. Temperature Gradient in Front of the Liquid/Solid Interface
JK	Include in Final Report Review: 1. Assess Extent of Centerline Porosity/Shrinkage and Judge Effect on Casting Integrity/Quality 2. Assess Solidification Type and Judge Effect on Casting Integrity/Quality.

2. Thermal Model Developed by EG & G

A two part model was developed by EG & G. The first part simulated thermal conditions in the molten pool up to the pinch point i.e. the intersection of the centerlines of the casting rollers with the thin strip.

The second part of the model extended from the pinch point through the balance of the cast strip.

A one-dimensional finite element mesh was used on the SINGLE computer program developed by EG & G. The assumptions used in the analysis were presented by Mr. Knudson.

a. Review Comment

Although there was no reason, on a preliminary basis, to question either the computer program, the material properties, the boundary conditions or other assumptions used, each of these items will be included in a final review following receipt of the report now under preparation at EG & G.

The analysis conducted by EG & G handled a difficult thermal problem by making several assumptions which appeared reasonable.

Obviously the entire analysis is based on the ability of the SINGLE program to handle solidification-type transient heat flow analyses. This will be confirmed by independently solving a simplified solidification problem and comparing results with EG & G calculations using SINGLE.

A search will be conducted to determine whether the physical properties used in the EG & G thermal analysis is reasonable. Critical attention will be given to confirming the boundary condition assumptions used in the EG & G model. The effort outlined below is anticipated as part of the present contract.

b. Recommended Actions and Responsibilities

<u>Responsibility</u>	<u>Actions</u>
JK	1. Search literature to determine whether a solidification problem similar to Double Thin Strip Casting has been solved.
JK	2. Set up simplified thermal analysis problem and solve independent of SINGLE program at EG & G.

<u>Responsibility</u>	<u>Actions</u>
EG & G	3. Solve same problem described in 2 using SINGLE (EG & G) program.
EG & G	4. Publish basic calculations and assumptions used in establishing boundary conditions.
JK	5. Assess boundary conditions and assumptions.
JK	6. Assess effect of the relative motion between liquid and solid metal as liquid moves to feed the solidifying metal.

3. Convection Modelling

The heat flow associated with relative motion of the liquid with the solidified portions of the thin strip was handled by developing an equivalent conductive heat flow analytical formulation. The assumptions used appeared reasonable but will be reviewed carefully upon receipt of the final EG & G report.

In addition, an assumption was made that there was no further relative flow between molten metal and solidified metal beyond the pinch point. It was noted that as long as solidification is taking place with attendant shrinkage, there will be relative motion between molten and solid metal as the molten metal moves to feed the shrinkage.

--. Other Issues Associated with Thermal Modelling

A discussion took place at the Idaho Falls meeting on March 24, 1983 regarding other critical issues that may determine the technical feasibility of the Double Thin Strip Casting Process. An overall assessment of these issues were presented in a previous report (1) by this reviewer and are reiterated as follows:

- i. Provide uniform molten metal flow control synchronized with casting rate without gas entrapment or gas film formation.
- ii. Provide non-gas evolving carrier strip, traveling at high speed with tension control to allow cavity dimension control and avoidance of strain transmittal to solidifying steel.

- iii. Provide uniform heat transfer rates high enough to produce fine dendrite arm spacings or other structures to enable the elimination of hot working and attainment of productivity objectives.
- iv. Provide quench roll location and pressure to seal structural deficiencies resulting from shortfalls in solidification and/or molten metal processing.

Item i: The need for uniform molten metal flow was recognized but considered beyond the scope of this review. The thermal model developed by EG & G assumes uniform metal delivery. A non-uniform metal delivery to the space between the clad can materially effect heat transfer and lead to an aborted casting run by melt-through at localized regions of the strip. It is not recommended that such non-uniformities be simulated as part of the present modelling effort.

Item ii: The need for a non-gas evolving carrier strip with tension control was recognized as critical. This subject also was considered beyond the scope of the present modelling effort. Exhibit I describes the Phases and Tasks included in a separate proposed effort to identify the sources of gas and its potential remedies in the Double Thin Strip Casting Process.

Item iii: The need for uniform and high heat transfer rates was recognized but considered beyond the scope of this project.

Item iv: The need for the possibility of in-line hot rolling was discussed and was not considered to be a direct part of this thermal modelling effort.

REFERENCES

1. Technical Feasibility Assessment of the Double Thin Strip Casting Process, by Dr. J. Keverian. Conducted under Consultant Agreement No. C82110057 for EG & G Idaho, January, 1983.

EXHIBIT I

PHASES & TASKS INCLUDED IN PROPOSED PROJECT:

"Identification of Sources of Gas and Its Potential Remedies in the Double Thin Strip Casting Process"

Phase

I Literature Review

- .1 Gas Entrainment due to Metal Flow Dynamics
- .2 Gas Film Formation and Its Influence on Heat Flow Rates
- .3 Sources of Gas from Metal/Refractory Reactions
- .4 Sources of Gas from Liquid Metal/Gas Reactions
- .5 Sources of Gas from Clad/Metal Reactions - Desorption of Gas Films
- .6 Sources of Gas from Clad/Metal Reactions - Foreign Films (i.e., Oil, Oxides, etc.)

II Gas Entrainment

- .1 Propose Two (2) Possible Tundish/Nozzle Designs
- .2 Mathematical Analysis of Flow Dynamics
- .3 Identify Possible Sources of Gas Entrainment and Its Effects
- .4 Propose Remedies to Reduce or Eliminate Gas Entrainment Problems

III Liquid Metal/Refractory Reactions

- .1 Propose Four Possible Refractories to be Used in Process
- .2 Propose Three Environments to be Used in Process (i.e., Air, Vacuum, Inert Gas)
- .3 Conduct Thermodynamic Calculations to Identify Possible Sources of Gas
- .4 Propose Remedies to Reduce or Eliminate Gas Sources

IV

Liquid Metal/Gas Reactions

- .1 Propose Candidates of Gas as Sources of Problems
- .2 Conduct Thermodynamic Calculations to Identify Possible Magnitude of Potential Problem
- .3 Propose Remedies to Reduce or Eliminate Gas Problems

V

Liquid Metal/Clad Reactions

- .1 Propose Three (3) Candidate Foreign Films on Clad Surface
- .2 Conduct Thermodynamic and other Calculations to Establish Possible Magnitude of Potential Problem
- .3 Propose Remedies to Reduce or Eliminate Gas Problems

VI

Gas Porosity Resulting from Solidification Processing

- .1 Propose Two (2) Possible Gases in Solution from Phases I through V
- .2 Conduct Calculations to Outline Conditions Leading to Gas Evolution during Solidification
- .3 Propose Remedies to Reduce or Eliminate Gas Problems During Solidification under Double Thin Strip Casting Conditions

VII

Prepare Preliminary and Final Reports

- .1 Prepare Preliminary Report
- .2 Distribute Preliminary Report
- .3 Prepare Final Report

APPENDIX E

EG&G Idaho, Inc., EDF TSC-2, THIN STRIP CASTING - MECHANICAL SCOPING

CALCULATIONS AND BELT SELECTION FOR BELT CONFIGURATION THERMAL

ANALYSIS, C. E. Forkel, November 16, 1983

ENGINEERING DESIGN FILE

PROJECT FILE NO. _____

EDF SERIAL NO. TSC-2

FUNCTIONAL FILE NO. _____

DATE November 16, 1983

PROJECT TASK Thin Strip Casting

SUBTASK Belt Configuration

EDF PAGE NO. 1 OF 10

SUBJECT **MECHANICAL SCOPING CALCULATIONS AND BELT SELECTION FOR BELT CONFIGURATION THERMAL ANALYSIS**

ABSTRACT

This is a preliminary calculation to determine the belt materials and dimensions to be used in the thermal analysis to determine the effects of adding two, endless, metal, product-support belts to a double-clad, thin-strip casting process.

Strength and bending fatigue calculations are based on ASME Code, Section III, Div. 1 allowable stress intensities and alternating stresses. Three belt materials are considered - Type 304 austenitic stainless steel; 2 1/4 Cr, 1 Mo ferritic steel; and 13 Cr ferritic steel. The spacial configuration of the caster is illustrated in Figure 1 and the product linear speed is consistent with that in EG&G Report RE-A-83-026, "Thermal Analysis of a Proposed Continuous Thin Strip Casting Process."

The assumed belt requirements were a bending fatigue life ≥ 2 weeks and a tension ≥ 5000 pounds on a 24-inch wide belt at the maximum temperature for the material given by the Code.

The data and results of the calculation are:

Number of cooling rollers	3
Cooling roller diameter	48 in.
Belt length	397 in.
Product speed	3840 in./min = 64 in./sec
Belt speed	9.67 rpm
Belt bending rate	29.0 cycles/min

Case	1	2	3
Belt material	2 1/4 Cr, 1 Mo.	Type 304 ss	13 Cr
Max. Code Temperature	700°F	800°F	700°F
Belt thickness:			
2-week life	0.043 in.	0.103 in.	0.043 in.
Infinite life	0.040 in.	0.096 in.	0.040 in.
Selected for analysis	0.032 in.	0.032 in.	NA

DISTRIBUTION (COMPLETE PACKAGE):

H. L. Brown, C. D. McMurtrey

DISTRIBUTION (COVER SHEET ONLY): PROJECT EDF FILE LOG, EDF SERIAL NO. LOG

AUTHOR <u>cef</u>	DEPT. <u>2740</u>	REVIEWED _____	DATE _____	APPROVED _____	DATE _____
C. E. Forkel					

ENGINEERING DESIGN FILE

PROJECT FILE NO. _____

EDF SERIAL NO. TSC-2

FUNCTIONAL FILE NO. _____

DATE November 16, 1983

PROJECT/TASK Thin Strip Casting

SUBTASK Belt Configuration

EDF PAGE NO 2 OF 10

ABSTRACT

It is to be noted that the Type 304 stainless steel belt has the potential of being three times as thick as has been selected if this is later found to be beneficial.

Assume roll/belt configuration shown below and use ASME Code Rec III, Div. 1 as a design guide.

$$\text{Belt length} = L = 72 + 72 + 102 + \pi \times 48 = 397 \text{ in.}$$

$$\text{Product speed} = S_p = 64 \text{ in/sec} = 3840 \text{ in/min}$$

$$\text{Belt revs/min} = N_b = \frac{S_p}{L} = \frac{3840}{397} = 9.67 \text{ rpm}$$

$$\text{Belt bending rate} = f_b = 3N_b = 3 \times 9.67 = 29.0 \text{ rpm}$$

Fig 1

$$\epsilon = \text{unit strain} = \frac{\Delta L}{L} = \left(a \frac{L}{R} \right) = \frac{a}{R} = \frac{a}{a+r}$$

$$S = E\epsilon = \frac{Ea}{a+r} = \text{total stress range}$$

$$S_a = \frac{S}{2} = \frac{Ea}{2(a+r)} = \text{alternating stress amplitude}$$

$$2S_a a + 2S_a r = E a$$

$$2S_a a - E a = -2S_a r$$

$$a = \frac{-2S_a r}{2S_a - E} = \frac{2S_a r}{E - 2S_a} = \frac{1}{2} \text{ thickness of belt}$$

$$t = 2a = \frac{4S_a r}{E - 2S_a} = \text{belt thickness}$$

22-141 50 SHEETS
22-142 100 SHEETS
22-144 200 SHEETS

Assume bolt will be changed one weekly, but that it will be changed to have a life of 2 weeks for fatigue due to bending stress.

$$T = 2 \text{ mths} \times 7 \times 24 \times 60 = 20,160 \text{ min}$$

The total number of design cycles is:

$$N = T f_t = 20,160 \times 29.0 = 5.85 \times 10^5 \text{ cycles}$$

Case 1 Assume 2 1/4 Cr, 1 mat full material (A-382, Dr. 22, Class 1)

$$S_y = 30.0 \text{ ksi}$$

$$S_u = 60.0 \text{ ksi}$$

$$S_m |_{700^\circ F} = 17.9 \text{ ksi}$$

$$S'_1 = 13.5 \text{ ksi}$$

5.85 cycles

$$S'_1 = 12.5 \text{ ksi}$$

1.6 cycles

Interpolating per ASME Code Sec. III Div. 1, Table I-9.1:

$$S'_1 = \frac{\log^{-1} \left[\frac{\log \left(\frac{5.85 \times 10^5}{5.00 \times 10^5} \right) \log \left(\frac{10.00 \times 10^5}{5.00 \times 10^5} \right)}{\log \left(\frac{13.5}{12.5} \right)} \right]}{13.5} = 5.85 \text{ cycles}$$

$$= 13.3 \text{ ksi} = \text{allowable alternating stress}$$

Setting $s_a = s_a'$ for Case 1:

$$A = \frac{4 \times 13.3 \times 24}{30,000 - 2 \times 13.3} = 0.043 \text{ in. to } 700^\circ \text{F}$$

5.85 E5 yd/hr

For infinite life, setting $s_a = s_a' = 12.5 \text{ ksi}$

$$A = \frac{4 \times 12.5 \times 24}{30,000 - 2 \times 12.5} = 0.040 \text{ in. to } 700^\circ \text{F}$$

1E6 yd/hr

Case 2 Assume Type 304 stainless steel ball mill
(A-240 Type 304 Class 1)

$$s_y = 30.0 \text{ ksi}$$

$$s_u = 75 \text{ ksi}$$

$$s_m \Big|_{700^\circ \text{F}} = 16.0 \text{ ksi}$$

$$s_m \Big|_{800^\circ \text{F}} = 15.2 \text{ ksi}$$

$$s_a' = 28.5 \text{ ksi}$$

5E5 yd/hr

$$s_a' = 26.0 \text{ ksi}$$

1E6 yd/hr

$$s_a' = \frac{5.85 \text{ E5 yd/hr} \log \left[\frac{\log \left(\frac{5.00 \times 10^5}{5.85 \times 10^5} \right) \log \left(\frac{5.00 \times 10^5}{10.0 \times 10^5} \right) \log \left(\frac{26.0}{28.5} \right) \right]}{28.5} = 27.9 \text{ ksi}$$

Setting $S_a = S'_a$ for Case 2:

$$A| = \frac{4 \times 27.9 \times 24}{26,000 - 2 \times 27.9} = 0.103 \text{ in. to } 800^\circ\text{F}$$

5.75E5 cycles

For infinite life, setting $S_a = S'_a|_{1E6} = 26.0 \text{ ksi}$

$$A| = \frac{4 \times 26.0 \times 24}{26,000 - 2 \times 26.0} = 0.096 \text{ in. to } 800^\circ\text{F}$$

1E6 cycles

22-141 50 SHEETS
22-142 100 SHEETS
22-144 200 SHEETS

Case 3 assume 13 Cr. belt metal.

(A-240 At 410 S)

$$S_y = 30.0 \text{ ksi}$$

$$S_m = 60.0 \text{ ksi}$$

$$S_m \Big|_{700^\circ\text{F}} = 16.4 \text{ ksi}$$

$$S'_a \Big|_{5 \times 10^5 \text{ cycles}} = 13.5 \text{ ksi}$$

$$S'_a \Big|_{1 \times 10^6 \text{ cycles}} = 12.5 \text{ ksi}$$

This problem reduces to identity with Case 1. Therefore:

$$S'_a \Big|_{5 \times 10^5 \text{ cycles}} = 13.3 \text{ ksi}$$

$$A \Big|_{5 \times 10^5 \text{ cycles}} = 0.043 \text{ in. to } 700^\circ\text{F}$$

$$A \Big|_{1 \times 10^6 \text{ cycles}} = 0.040 \text{ in. to } 700^\circ\text{F}$$

And, therefore, it is no benefit over 2 1/4 Cr 1 M.

For thermal analysis system, assume:

$$P = \text{Belt tension} = 5.00 \text{ kips}$$

$$W = \text{Belt width} = 24 \text{ in.}$$

$$S_m = \text{Belt tension stress}$$

$$= \frac{1}{4} \left(S_m \Big|_{\substack{700^\circ\text{F} \\ 2\frac{1}{4}\text{Cr-1Ni}}} + S_m \Big|_{\substack{800^\circ\text{F} \\ \text{Type 304}}} \right)$$

$$= \frac{1}{4} (17.9 + 15.2) = 8.28 \text{ ksi}$$

$$A = \text{Belt thickness}$$

$$= \frac{P}{W S_m} = \frac{5.00}{24 \times 8.28} = 0.025 \xrightarrow{\text{use}} 0.032 \text{ in.}$$

22-141 50 SHEETS
22-142 100 SHEETS
22-143 200 SHEETS

22-144 200 SHEETS

1. Revise thermal analysis model for thin strip casting to include belt with three rollers as shown in figure 1.

2. Perform thermal analyses as follows:

a. Belt material: Type 304 stainless steel

Belt thickness: 0.032 in

Product thickness: 0.080 in

Clad thickness: 0.008 in

Product speed: 64 in/sec

Environment temp: 500°F

Other parameters: normal case

b. Same as "a" above except:

Belt material: 2 1/4 Cr 1 Mo ferritic steel

c. Repeat worst case above with 500°F environment and the optional heat sink slab at 120°F.

d. Additional analysis with parameters TBD.

Cy 2/3/83

APPENDIX F

EG&G Idaho, Inc., RE-A-83-026, Supp. 2, THERMAL ANALYSIS OF A PROPOSED
BELT-SUPPORTED CONTINUOUS THIN-STRIP STEEL CASTING PROCESS,

D. L. Knudson, February 1984

RE-A-83-026, Supple. 2

February 1984

THERMAL ANALYSIS OF A PROPOSED BELT-SUPPORTED
CONTINUOUS THIN-STRIP STEEL CASTING PROCESS

Thermal and Process Analysis Branch

D. L. Knudson
D. L. Knudson

R. L. Moore/ *RL Moore*

N. E. Pace/ *N E Pace*
J. L. Liebenthal/ *J L Liebenthal*

Distribution

H. L. Brown (6)
C. E. Forkel
D. L. Knudson (2)
M. M. Laughlin (3)
J. L. Liebenthal
C. D. McMurtrey
R. L. Moore
N. E. Pace

SUMMARY

A proposed process for continuous casting of thin-strip steel was thermally analyzed. The process will incorporate many unique features including water-cooled rollers, water-cooled heat slabs, endless steel belts for casting support, and two relatively thin moving steel strips (clad) which will serve as the casting mold on either side of the product. Edge control will be provided by roller end geometry. In operation, the casting mold (the thin steel strips) will "sandwich" the molten core and become an integral part of the final product thereby providing the desired surface finish. Substantial energy savings over conventional methods of strip production are expected through use of this direct casting scheme.

A computer model was developed to permit prediction of the quasi steady-state thermal behavior of the proposed process. The model was then used to analyze three different cases assuming that a 2-ft wide, 0.080-in. thick strip of AISI-1020 steel would be cast at a continuous linear rate of 64 in./sec using 0.008-in. thick clad supported by 0.032-in. thick endless steel belts. In Case 1, the endless steel casting support belts were assumed to be made of Type 304 stainless steel and the water-cooled heat slabs were not used. In Case 2, the endless steel casting support belts were assumed to be made of 2-1/4 Cr, 1 Mo steel and the water-cooled heat slabs were not used. Case 3 was identical to Case 1 except that the water-cooled heat slabs were used in the proposed process.

In all cases, the results indicated that continuous casting of thin-steel strip in the manner proposed is thermally feasible. However, a comparison of the results of Cases 1 and 2 indicate the importance of heat capacitance in reducing the severity of belt thermal cycling. Specifically, cyclic belt temperature variations of 941°F and 721°F were calculated in Cases 1 and 2, respectively. Although the heat absorbed by the belts was essentially the same in the two cases, the fact that the heat capacitance of 2-1/4 Cr, 1 Mo steel is approximately 40% greater than the heat capacitance of Type 304 stainless steel accounted for the reduction in cyclic belt temperature variation. As expected, Case 3 results indicated

that the heat slabs were effective in reducing all process temperatures. Furthermore, the heat slabs also provide a means of reducing the severity of belt thermal cycling. Compared with Case 1 results, the cyclic belt temperature variation in the proposed process was reduced by 210°F through the use of heat slabs. Since lower cyclic belt temperature variations should extend belt life and since increases in belt and product strength accompany general decreases in process temperatures, optimum performance of the proposed process should be obtained by maximizing the belt heat capacitance (through appropriate belt material selection) and using heat slabs to enhance product heat removal.

The modeling techniques used here are sound and the assumptions made and the results obtained appear reasonable. However, a sensitivity study of the effect of the assumptions on the results was not conducted.

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THERMAL ANALYSIS OF A PROPOSED BELT-SUPPORTED CONTINUOUS THIN-STRIP STEEL CASTING PROCESS

1. INTRODUCTION

As part of the current DOE-funded thin-strip casting development program, a new/unique process for direct casting of steel into thin cross-sections has been conceived. A schematic of the primary equipment configuration for this process is shown in Figure 1. Associated novel features are discussed below.

The proposed process will use two moving thin steel strips as the casting mold. These thin steel strips, also referred to as clad, will be continuously fed to the casting rollers. Molten steel, having the same composition as the clad, will be poured from a tundish between the strips as shown. Product edge control will be provided by the geometry of the roller ends. The "sandwich," consisting of the clad and the cast core, will then move together in the direction indicated.

From the point of initial clad/molten-steel contact to the product take-off point, energy will be transferred from the cast core through the clad. Upon reaching the product take-off point, the sandwich will be an integral product primarily through fusion bonding at the clad/cast-core interfaces. A limited amount of clad surface melting (followed by solidification of all molten steel) is also expected to contribute to the clad/cast-core bond.¹ Therefore, the final product will have two surfaces with the desired finish through incorporation of the clad into the cast strip.

As shown in Figure 1, two endless steel belts will be provided for casting support. In operation, there will be no slip between the clad and belt surfaces or between the belt and roller surfaces. The static frictional forces at the roller/belt interfaces will be used to drive the belts (from the rollers) while frictional forces at the belt/clad interfaces will drive the product.

Figure 1. Proposed belt-supported thin-strip steel casting equipment configuration (not to scale).

To help prevent formation of voids in the cast product, the molten steel liquid level will be maintained above the casting rollers line-of-centers, hereafter referred to as the casting rollers pinch point or simply the pinch point. With the molten steel liquid level above the pinch point, the resulting ferrostatic head will tend to feed and fill any potential voids prior to sandwich passage through the casting rollers pinch point. The final product dimension is then determined by the belt thicknesses and the spacing of the casting rollers.

To minimize oxygen contamination and oxidation problems during casting, the equipment shown in Figure 1 will be housed in an inert atmosphere.

The casting, take-off, and tension rollers will be water-cooled through internal passages to serve as heat sinks. As needed (or desired), heat transfer from the solidifying product can be enhanced between the pinch point and the take-off point through use of the optional heat slabs shown in Figure 1. Without the optional heat slabs, which will also be water-cooled, small (non-cooled) support rollers will be required. In this standard configuration, the product will reject heat through the belts (primarily) to the inert gas after passing the pinch point. At the take-off point, the product will be sufficiently cooled for handling without belt support.

The product and the casting support belts separate from each other at the take-off point. The product will be directed to a coiler or a mechanical reduction process while the belts will follow paths around the take-off rollers and the tension rollers back to the casting rollers for the start of another cycle. The energy absorbed from the solidifying product by the belts will be rejected (to the inert gas and the cooled rollers) as the belts travel from the take-off point to the point of initial clad/molten-steel contact.

Although the proposed process will initially require a supply of conventionally produced strip, substantial energy savings will result through direct casting to meet the demand for thin-strip steel products.

This energy saving potential provides the justification for the analysis which follows. The analysis then provides a data base for assessment of the thermal feasibility of the proposed process.

2. THERMAL MODEL DESCRIPTION

The proposed continuous thin-strip steel casting process described in Section 1 is quite similar to a previously proposed and analyzed process.¹ The major differences include the roller arrangements and the use of endless casting support belts in the proposed process of current interest. However, these differences do not significantly alter the general thermal phenomena which must be modeled to predict process behavior (and assess thermal feasibility). Therefore, assumptions and modeling techniques used here were extracted from the previous analysis whenever possible.

The dimensions shown in Figure 1 were fixed for purposes of this study. As in the previous analysis,¹ the capability to vary any process parameter, including dimensions, was specifically built into this model. However, fixing process parameters simplifies and narrows the scope of this effort.

In terms of model development, a further simplification arises from the proposed process configuration. Except for the effect of orientation, the proposed process shown in Figure 1 is symmetric. The orientation does allow slightly more surface wetting (by the molten steel) on the lower clad as compared to the upper clad. However, this effect is relatively unimportant making symmetry a legitimate assumption.

A model capable of predicting the quasi steady-state thermal behavior of the product from the point of initial clad/molten-steel contact to the take-off point was needed to satisfy the objectives of this analysis. However, such a model is somewhat complicated by the presence of the endless casting support belts.

Unlike the rollers, each belt will have a relatively long contact time with the product (during casting and solidification), a relatively long time to reject energy absorbed (as a result of product contact), and a

relatively thin cross-section. Therefore, the temperature profile through the belt thickness will vary substantially as a function of position in the belt revolution cycle.

Because of the expected belt temperature variation and because belt thicknesses will not be insignificant compared to the product thickness, neglecting the effects of the belts on the proposed process could be overly conservative (with respect to product temperatures). Therefore, four distinct components were developed to incorporate belt effects in a model of the process.

The first component of the model was used to track the thermal response of a belt, clad, and one-half of the cast core from the point of initial clad/molten-steel contact to the product take-off point. This component provides the entire product analysis based on "initial" belt and clad temperature profiles. The second component of the model was used to extract the take-off point belt temperature profile from the first component temperature predictions and arrange the data for use as the beginning belt temperature conditions in the third component. The third component of the model was then used to track the belt thermal response from the product take-off point around the take-off roller and the tension roller back to the point of initial clad/molten-steel contact on the casting roller. The fourth component of the model was used to extract the belt temperature profile at the point of initial clad/molten-steel contact from the third component predictions. Based on the extracted belt temperature profile, the inert gas temperature, and the associated boundary condition, the fourth component was also used to establish a corresponding steady-state clad temperature profile. (The time duration of belt/clad contact and the fact that the clad will be relatively thin justifies this steady-state approximation to the actual clad temperature profile at the point of initial clad/molten-steel contact.) The "resulting" belt and (implied) clad temperature profiles were also arranged for use as "initial" temperature profiles in the first component if necessary.

After execution of the model, the "initial" and the "resulting" belt temperature profiles must be compared. If the profiles are not equal, the

product analysis generated by the first component of the model is incorrect. Such an indication requires another model execution using the "resulting" belt and clad temperature profiles as the "initial" profiles. Details of these model components follow.

2.1 Casting Component

The first component of the model consisted of a one-dimensional nodalization from the roller/belt interface through the clad to the product centerline. Based on previous experience,¹ a uniform element size of 0.00025 in. through the clad and the cast core was used. A uniform element size of 0.001 in. was used through the belt thickness based on sensitivity calculations with the model. For purposes of this analysis, each belt was assumed to be 0.032-in. thick, the clad thickness was fixed at 0.008 in., and the product thickness was assumed to be 0.080 in. (Simple calculation indicates a required cast core thickness of 0.064 in.)

This component of the model utilized SINGLE/MOD2,^{2,3} with appropriate and necessary user modifications, to perform all thermal and associated calculations. SINGLE/MOD2 is a general purpose one-dimensional finite element heat conduction code. User modifications were required to permit simulation of process specific details.

This component was needed to predict the thermal behavior of the belt, clad, and cast core from the point of initial clad/molten-steel contact to the product take-off point (see Figure 1). Therefore, it is logical to refer to this part of the model as the casting component. Although the casting component is one-dimensional, the desired thermal representation was accomplished through time variation of boundary conditions simulating travel of the cross-section of interest between the stated end points as discussed below.

2.1.1 Boundary Conditions

Consistent with the previous analysis,¹ the molten pool depth (or the distance from the pinch point to the molten steel surface) was assumed

to be 2 in. It was further assumed that the product velocity (or the tangential velocity of the belt surfaces) was 64 in./sec. Based on these assumptions and the previously stated dimensions, 0.0313 sec will be required for a cross-section through the belts, clad, and cast core to move from the point of initial clad/molten-steel contact to the pinch point.

During this period of time, heat will be transferred from the molten steel through the clad and the belts to the water-cooled casting rollers. The boundary condition applied at the roller/belt interface (or Node 1) to simulate this transfer of heat was an equivalent convection coefficient derived from a roller/belt contact coefficient in series with the conductive resistance of the casting roller wall. As in the previous analysis,¹ the contact coefficient was assumed to be constant at 500 Btu/hr-ft²-°F, the roller wall was assumed to be low-carbon steel and 1-in. thick, and the inside surface of the roller wall was assumed to be at a constant 125°F as a result of the water cooling. The variation of belt surface temperature was accounted for within the casting component in making this heat loss calculation.

Between the pinch point (or time = 0.0313 sec) and the product take-off point (which will be reached at time = 1.6223 sec), heat will continue to be rejected from the belt surfaces. However, the boundary condition applied to the belt surface depends on the configuration being considered as follows.

In the standard configuration, the optional heat slabs shown in Figure 1 will be replaced with a series of small support rollers. For purposes of this analysis, it was assumed that the support rollers will not be water cooled. Furthermore, they will provide only line contact with the belt surfaces. Therefore, virtually all of the exterior belt surfaces from the pinch point to the product take-off point will be exposed to the inert gas environment.

Exposure to the inert gas environment allows forced convection (by virtue of belt speed) and radiation heat transfer from the belt surfaces. The boundary conditions (assumed to be constant) which were applied to the inert gas/belt interface (or Node 1) for this standard configuration

include a forced convection heat transfer coefficient of $15 \text{ Btu/hr-ft}^2\text{-}^\circ\text{F}$, a radiant emissivity of 0.5, and a sink temperature of 500°F (960°R).¹ Again, the variation of belt surface temperature was appropriately accounted for.

If the optional heat slabs are used, forced convection to the inert gas and radiation from the belt surfaces will be replaced with sliding contact at the heat-slab/belt interface. It was assumed that the contact pressure at the heat-slab/belt interface will most likely be lower than the contact pressure at the roller/belt interface to allow sliding contact. Because contact pressure inversely affects the contact resistance, the coefficient used at the heat-slab/belt interface should be lower than the coefficient used at the roller/belt interface. However, quantifying contact coefficients for either interface is difficult because of numerous uncertainties and generally sparse experimental data.

A contact coefficient of $500 \text{ Btu/hr-ft}^2\text{-}^\circ\text{F}$ was used at all roller surfaces here and in the previous analysis¹ because it appears to be reasonable. Based on this value, the fact that quantification is uncertain, and the relationships between contact pressures and coefficients, a contact coefficient of $250 \text{ Btu/hr-ft}^2\text{-}^\circ\text{F}$ was adopted for use at the heat-slab/belt interface. It is acknowledged that this value may be somewhat low based on the differences in contact pressure which may exist. However, belt and product temperature predictions will be conservative as a result of any underestimation.

An equivalent convection coefficient derived from the heat-slab/belt contact coefficient in series with the conductive resistance of the heat slab wall was applied to the heat-slab/belt interface (or Node 1) for the optional configuration. It was assumed that the heat slab wall was low-carbon steel and 1/2-in. thick. As with all water-cooled components here and in the previous analysis,¹ the inside surface of the heat slab wall was assumed to be at a constant temperature of 125°F as a result of the water cooling. The variation of belt surface temperature was accounted for within the casting component.

The remaining model boundary, at the product centerline, was treated as an adiabatic surface as a result of the symmetry assumption.

2.1.2 Internal Heat Transfer Calculations

Between the point of initial clad/molten-steel contact (or time = 0) and the product take-off point (or time = 1.6223 sec), changes in phase occur in the clad/cast core. Because the steel being cast is not a pure substance, these phase changes occur between solidus and liquidus temperatures. As in the previous analysis,¹ the locations of the solidus and liquidus temperatures were determined each iteration of each time step from the casting component temperature predictions. All elements entirely or partially bounded by the determined locations were assumed to be undergoing phase change. The heat capacitance of the affected elements was then appropriately modified to account for the latent heat of fusion accompanying phase change. This scheme is described in detail in Reference 1. (Given sufficiently high belt temperatures, changes in phase could also occur in the belts. However, temperatures high enough to result in belt phase change indicate an unworkable casting configuration due to belt failure. Instead of modeling such an occurrence, a set of different process parameters must be selected so that predicted belt temperatures are appropriately reduced.)

These clad/cast core phase changes occur as a result of the chilling effect of the belts and the clad on the molten steel and as a result of the heat rejected from the belt surfaces. The heat rejection from the belt surfaces was modeled as outlined in Section 2.1.1. The simulation of the heat transfer to the belt surfaces is described below.

From the point of initial clad/molten-steel contact (time = 0) to the pinch point (time = 0.0313 sec), relative motion between the solid and molten phases occurs as a result of casting roller curvature. This relative motion provides the mechanism for forced convection heat transfer at the solid/molten interface with attendant boundary layer growth. The pouring action of the molten steel from the tundish tends to retard boundary layer growth and minimize thermal gradients in the remaining

molten portion of the pool. Therefore, enhanced conduction from the cast core through the boundary layer to the solid front occurs. At the solid/molten interface, heat transfer to the solid phase is by forced convection. Heat is transferred through the solid phase by conduction, across the clad/belt interface, and then by conduction to the belt surfaces.

With the exception of the belt effects (which will be discussed later), the foregoing description is identical to the physics of the previously proposed and analyzed process between the point of initial clad/molten-steel contact and the pinch point.¹ This similarity permitted the direct use of the appropriate part of the previously developed model in the casting component. Section 2.1.2 of Reference 1 can be consulted for a complete description of the pertinent thermal modeling. At this point, it is sufficient to state that the initial clad temperature used here was not fixed but calculated (using methods to be described) and the volumetric product speed was assumed to be 122.88 in.³/sec (based on a 24-in. wide product with a velocity of 64 in./sec). All other process parameters took on nominal values or were fixed as discussed in Reference 1.

Although molten steel may exist at the casting rollers pinch point (time = 0.0313 sec), relative motion ceases because the product assumes a uniform cross-section with a uniform velocity at that location. Without relative motion, forced convection from the molten steel to the solid front must also cease. Therefore, heat transfer to the solid/molten interface within the casting component appropriately shifts from a combined forced-convection/conduction heat transfer mode to a conduction heat transfer mode at the pinch point.

Regardless of how heat reaches the solid front, conduction heat transfer through the solid phase to the clad/belt interface and conduction heat transfer through the belt was computed within the casting component. Furthermore, a convective junction was used in all casting component calculations to represent the clad/belt interface as follows.

The clad/belt and the roller/belt interfaces are similar in terms of contact pressures, the bounding gas medium, and the mating surface finishes. Therefore, it was (reasonably) assumed that the contact

coefficients for the two interfaces were equal. As a result, a constant convection coefficient of $500 \text{ Btu/hr-ft}^2\text{-}^\circ\text{F}$ was applied to the clad/belt interface (or Node 34/33). To complete the convective connection, the temperature of the belt surface (or Node 33) was assigned as the boundary temperature for the clad surface (or Node 34) and the temperature of the clad surface (or Node 34) was assigned as the boundary temperature for the belt surface (or Node 33).

The heat load for the process was integrated, through casting component calculations, to provide information relative to the required cooling water flow rates, inert gas temperature control, etc. In addition, the belt yield strength and the product ultimate strength as functions of the respective temperature profiles were calculated at selected times between the point of initial clad/molten-steel contact (time = 0) and the product take-off point (time = 1.6223 sec). The results of these calculations then serve as one indicator of process feasibility in that certain minimum strengths are believed to be required to drive the process and allow necessary product handling. And finally, the casting component automatically effects execution of the second component of the model after compiling all pertinent data required as input.

A complete listing of the casting component of the model is provided in Appendix A.

2.2 Casting-to-Belt Component

At the take-off point (time = 1.6223 sec), the product will be sufficiently cooled for handling without support, allowing separation from the belts. Since the clad and cast core exist in an integral form, the thermal modeling interest in the product ends. However, the thermal response of the belts beyond the product take-off point is important since the belt temperatures at the point of initial clad/molten-steel contact (time = 0) affect the overall process.

Therefore, the second component of the model was needed to extract the belt temperature profile from the temperature profile of the combined belt,

clad, and cast core (predicted by the casting component) at the take-off point. The extracted temperature profile was arranged in a form suitable for use as initial belt temperature conditions in the third component of the model. The second component then automatically effects execution of the third component. Therefore, the second component provides the interface between the casting (or the first) component and the belt (or the third) component which, appropriately, leads to the descriptive name of casting-to-belt component.

The casting-to-belt component utilized FORTRAN 5 (FORTRAN 77 with CDC extensions) to perform the required calculations. A complete listing of this component is given in Appendix B.

2.3 Belt Component

The third component of the model consisted of a one-dimensional nodalization through a belt thickness. Based on sensitivity calculations with the model (and consistent with the casting component), a uniform element size of 0.001 in. was used.

This component of the model utilized SINGLE/MOD2,^{2,3} a general purpose one-dimensional finite element heat conduction code, to perform all thermal calculations.

This component was needed to predict the thermal behavior of the belt from the product take-off point (time = 1.6223 sec) to the point of initial clad/molten-steel contact. (With a tangential belt velocity of 64 in./sec, a cross-section through the belt thickness will reach the point of initial clad/molten-steel contact at time = 6.2003 sec. For process modeling purposes, however, this location also corresponds to time = 0.) Therefore, it is logical to refer to this part of the model as the belt component. Like the casting component, the desired thermal representation was accomplished with the one-dimensional nodalization through time variation of boundary conditions simulating belt travel between the stated end points. These boundary conditions are discussed below.

Figure 1 clearly indicates that the inside surface of each belt is subject to roller contact and inert gas environment exposure between the points (or within the time frame) of interest. The figure also indicates that the outside surface of each belt is subject to inert gas environment exposure and clad contact during the same period. Therefore, there are three different boundary conditions which must be appropriately applied.

During roller/belt contact, an equivalent convection coefficient derived from a contact coefficient of $500 \text{ Btu/hr-ft}^2\text{-}^\circ\text{F}$ in series with a low-carbon steel roller wall thickness of 1 in. to a constant temperature of 125°F (on the inside surface of the roller wall) was applied to the appropriate belt surface. This heat loss was calculated as a function of the time-varying belt temperature. (Note that this boundary condition is consistent with the previous analysis¹ and the casting component of the present analysis.)

During inert gas environment exposure, a combination of forced convection and radiation coefficients was applied to the belt surface consistent with similar boundary treatment in the previous analysis¹ and in the casting component. A forced convection heat transfer coefficient of $15 \text{ Btu/hr-ft}^2\text{-}^\circ\text{F}$ and a radiant emissivity of 0.5 with a sink temperature of 500°F (960°R) were used. Again, the variation of belt surface temperature was accounted for.

At time = 5.9367 sec, the outside surface of the belt contacts the clad. The flow of heat from the belt is then restricted by the contact coefficient at the belt/clad interface, the conductive resistance through the clad, and the forced convection heat transfer coefficient to the inert gas from the clad surface. (In reality, the forced convection heat flow to the inert gas is in parallel with the heat flow by radiation from the clad surface. However, assuming that 960°R reasonably represents the radiation heat sink temperature for the surface, neglecting the radiation heat flow path produces only slightly higher, or more conservative, belt and product temperatures.) Because the conductive resistance through the clad is insignificant, an equivalent coefficient was calculated based on a contact coefficient of $500 \text{ Btu/hr-ft}^2\text{-}^\circ\text{F}$ in series with a forced convection heat

transfer coefficient of $15 \text{ Btu/hr-ft}^2\text{-}^\circ\text{F}$. This equivalent coefficient was then applied to the belt surface and connected to a constant inert gas temperature of 500°F with appropriate accounting for the time variation of belt surface temperature.

The belt component automatically effects execution of the fourth component of the model after compiling all pertinent data required as input.

A complete listing of the belt component of the model is provided in Appendix C.

2.4 Belt-to-Casting Component

At the point of initial clad/molten-steel contact (time = 6.2003 sec), the fourth component of the model was needed to extract the corresponding belt temperature profile from the belt model predictions. The extracted belt temperature profile was then used to imply an associated steady-state clad temperature profile as follows.

At the point of initial clad/molten-steel contact, the heat loss from the belt surface through the clad to the inert gas can be determined from the calculated belt surface temperature, the constant inert gas temperature, and the constant equivalent boundary coefficient (see Section 2.3). Using the known heat flux, the belt surface temperature, and the belt/clad contact coefficient of $500 \text{ Btu/hr-ft}^2\text{-}^\circ\text{F}$, the temperature of the clad surface in contact with the belt was readily established. Similarly, using the known heat flux, the constant inert gas temperature, and the forced convection heat transfer coefficient from the clad surface to the inert gas of $15 \text{ Btu/hr-ft}^2\text{-}^\circ\text{F}$, the temperature on the remaining clad surface was determined. A linear temperature profile between the established clad surface temperatures was then assumed.

If the extracted belt temperature profile (or the "resulting" belt temperature profile) at the point of initial clad/molten-steel contact does not equal the "initial" belt temperature profile used in the model execution, the "resulting" belt and (implied) clad temperature profiles

have to be input as "initial" profiles to the casting component and the model must be re-run. However, the casting component requires input temperatures from the roller/belt interface through the belt and the clad to the product centerline. Therefore, assuming that the molten steel will be poured between the clad at 100°F superheat,¹ an appropriate number of superheated nodes representing the molten region from the clad surface to the product centerline were added to the "resulting" belt and (implied) clad temperature profiles and arranged in a form suitable for use as initial temperature conditions in the casting component of the model. Obviously, this component provides the required interface between the belt and casting components which, appropriately, leads to the descriptive name of belt-to-casting component.

A manual comparison of "initial" and "resulting" belt temperature profiles was made after each model execution. If the maximum difference between any "initial" and "resulting" belt node temperature was 0.1°F or less, it was assumed that the temperature profiles were equal and that the product analysis (provided by the casting component) had reached a converged solution. Otherwise, the temperature profile developed by the belt-to-casting component was used as the initial temperature profile in the next model execution.

The belt-to-casting component utilized FORTRAN 5 (FORTRAN 77 with CDC extensions) to perform the required calculations. A complete listing of this component is provided in Appendix D.

3. ANALYSIS

The thermal model described in Section 2 was used to predict the quasi steady-state thermal behavior of the proposed thin-strip casting process for the three different cases described below.

In all cases, it was assumed that the material being cast was AISI-1020 steel, the largest-tonnage material for strip/sheet fabrication. (However, the developed model is completely adaptable to the analysis of the proposed process using any castable material.) All thermal and mechanical properties used to represent this material are given in Reference 1. As in the previous analysis,¹ the model described in Section 2 accounted for the temperature dependence of all properties.

In all cases, time steps of 0.0001 sec were used from the point of initial clad/molten-steel contact (time = 0) to just beyond the pinch point (time = 0.0323 sec) with time steps of 0.01 sec used from time = 0.0323 sec to the product take-off point (time = 1.6223 sec). Reference 1 details the justification for the use of these time steps based on solution convergence checking with the previously developed model. Time steps of 0.1 sec used between the product take-off point (time = 1.6223 sec) and the point of initial clad/molten-steel contact (time = 6.2003 sec and time = 0) were selected on the basis of sensitivity runs made with the model.

In Case 1, it was assumed that 0.080-in. thick strip would be cast using 0.008-in. thick clad at a linear rate of 64 in./sec with the proposed process in standard configuration (without the optional heat slabs). It was also assumed that each endless casting support belt was 0.032-in. thick and made of Type 304 stainless steel. The thermal and mechanical properties used to represent this material are given in Tables 1 and 2, respectively. Property values intermediate to those listed were obtained through linear interpolation within the model.

As indicated in Table 1, some of the thermal properties of Type 304 stainless steel were extrapolated from available data. This extrapolation was necessary because the required data were not readily available and the

TABLE 1. TYPE 304 STAINLESS STEEL THERMAL PROPERTIES

Temperature (°F)	Thermal Conductivity ⁴ (Btu/hr-ft-°F)	Heat Capacitance ⁴ (Btu/ft ³ -°F)
100	8.7	57.9
200	9.3	60.0
300	9.8	61.8
400	10.4	63.2
500	10.9	64.4
600	11.3	65.4
700	11.8	66.2
800	12.3	66.8
900	12.7	67.4
1000	13.2	67.9
1100	13.6	68.4
1200	14.0	68.9
1300	14.5	69.5
1400	14.9	70.2
1500	15.3	71.1
1600	15.7	72.1 ^a
1700	16.2 ^a	73.3 ^a
1800	16.6 ^a	74.8 ^a
1900	17.0 ^a	76.6 ^a

a. Values extrapolated from available data.

TABLE 2. TYPE 304 STAINLESS STEEL MECHANICAL PROPERTIES

<u>Temperature (°F)</u>	<u>Yield Stress⁴ (1000 psi)</u>
150	27.5
350	21.4
550	18.4
750	17.1
950	16.1
1150	14.7
1350	12.4
1550	9.0
1750	4.8
1850	0.

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scope of this effort did not allow time for in-depth review of all potential sources. Implications of such extrapolation on the results of this analysis will be provided in the Conclusions and Recommendations section of this report.

In Case 2, it was assumed that each endless casting support belt was made of 2-1/4 Cr, 1 Mo steel. All other process parameters were identical to those used in Case 1. The thermal and mechanical properties used to represent the 2-1/4 Cr, 1 Mo steel are given in Tables 3 and 4, respectively. Again, values intermediate to those listed were determined by linear interpolation.

As indicated in Tables 3 and 4, some thermal and mechanical properties of 2-1/4 Cr, 1 Mo steel were also extrapolated from available data. As in Case 1, the required property data at elevated temperatures were not readily available. A discussion of the effects on results produced by these data extrapolations will be provided later in this report.

The results of Cases 1 and 2 were generated and compared to determine which case produced the more severe belt and product temperatures. All of the process parameters associated with the more severe case were then used to define Case 3 with the exception that the small support rollers were replaced with the optional water-cooled heat slabs. Therefore, Case 3 will provide the data needed to evaluate the effectiveness of the heat slabs in the proposed process.

TABLE 3. 2-1/4 Cr, 1 Mo STEEL THERMAL PROPERTIES

Temperature (°F)	Thermal Conductivity ⁴ (Btu/hr-ft-°F)	Heat Capacitance ⁴ (Btu/ft ³ -°F)
100	21.1	53.0
200	21.4	55.5
300	21.6	57.9
400	21.5	60.3
500	21.4	62.8
600	21.1	65.3
700	20.7	68.0
800	20.3	71.0
900	19.8	74.6
1000	19.2	79.2
1100	18.6	85.3
1200	18.0	93.9
1300	17.4	104.1
1400	16.7 ^a	121.2 ^a
1500	16.0 ^a	103.6 ^a
1600	15.3 ^a	92.7 ^a
1700	14.5 ^a	92.4 ^a
1800	13.8 ^a	92.2 ^a

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a. Values extrapolated from available data.

TABLE 4. 2-1/4 Cr, 1 Mo STEEL MECHANICAL PROPERTIES

Temperature (°F)	Yield Stress ⁴ (1000 psi)
100	36.6
200	34.9
300	33.8
400	33.2
500	32.7
600	32.4
700	31.9
800	31.3
900	30.2
1000	28.6
1100	26.3
1200	23.1
1300	21.0 ^a
1400	18.7 ^a
1500	16.1 ^a
1600	13.2 ^a
1700	9.9 ^a
1800	6.1 ^a

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a. Values extrapolated from available data.

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4. DISCUSSION OF RESULTS

The model described in Section 2 is composed of four different components. The first component of the model provides the entire analysis of the cast product and the proposed process as needed to fulfill the objectives of this study. The three remaining model components serve only to support the calculations made by the first component through accounting for the thermal effects of the belts. Therefore, the only results pertinent to this analysis and the only results presented in this section were produced by the first (or the casting) component of the model.

The casting component of the model utilized the thermal symmetry of the proposed process. In the majority of the graphical presentations of results which follow, plots are extended to the product centerline only. In these cases, the omitted portion of the profile would merely be a mirror image of the data presented.

Figure 2 illustrates the solid front and boundary layer locations in relation to the clad thickness and the product centerline for Case 1. Because all location-dependent conditions were modeled as time-dependent conditions, the time axis in Figure 2 is analogous to the travel of the modeled cross-section. In other words, time = 0 corresponds to the conditions which exist in the modeled cross-section at the point of initial clad/molten-steel contact. Similarly, time = 0.0313 sec corresponds to the conditions which exist in the modeled cross-section at the casting rollers pinch point.

Curve 1 in Figure 2 is a horizontal line, 0.008 in. from the belt/clad interface, representing the clad thickness. The horizontal line at 0.040 in. represents the product centerline. At the initial point of contact with the molten steel (time = 0), a boundary layer thickness of zero and a solid front location coincident with the clad/molten-steel interface are shown. As time advances (or as the modeled cross-section "travels" toward the casting rollers pinch point), molten steel solidifies onto the clad surface as indicated by curve 2. At the same time, the boundary layer (curve 3) grows from a thickness of zero until it reaches the

Figure 2. Solid front and boundary layer front locations in relation to the clad thickness and the product centerline for Case 1.

product centerline limitation. Obviously, the boundary layer shown meets an identical boundary layer growing from the other solid surface at the centerline, limiting further growth of either.

The relative speed between the molten and solid phases for Case 1 is shown in Figure 3. The time axes in Figures 2 and 3 are identical. As expected, the maximum relative speed occurs at the point of maximum molten flow area (or at the point of initial clad/molten-steel contact). In addition, the relative speed diminishes to zero at the pinch point because the product assumes a uniform cross-section with a uniform velocity at that point.

The relative speed shown in Figure 3 was used to calculate the forced convection heat transfer coefficient plotted in Figure 4. This coefficient was used to derive an equivalent thermal conductivity at the molten/solid interface in Case 1. The coefficient calculation was based on the temperature of the bulk molten pool approximated by the temperature at the product centerline. When the centerline node temperature decreases to the liquidus temperature (2754°F), the specific heat of the molten steel, also used in the coefficient calculations, dramatically increases to account for the latent heat of fusion as the steel begins to solidify. This specific heat increase manifests itself as the spike shown in the convection coefficient curve. (Although inelegant, this spike has very little effect on the solution.) As shown (and by definition), the coefficient goes to zero with relative speed. At the point of initial clad/molten-steel contact (time = 0), the coefficient is very large. This is completely consistent with the fact that the boundary layer thickness and, therefore, the resistance to convective heat flow, is essentially zero at the same time.

Figure 5 illustrates the temperature profile, from the casting-roller/belt interface at $x = 0$ through the belt and the clad to the product centerline at $x = 0.072$ in., 0.0001 sec after initial clad/molten-steel contact for Case 1. Just prior to molten steel contact, heat flows from the belt surface to the casting roller and across the belt/clad interface

RELATIVE SPEED - CASE 1L

Figure 3. Relative speed between the molten and solid phases for Case 1.

Figure 4. Forced convection heat transfer coefficient from the molten steel to the solid front for Case 1.

Figure 5. Case 1 temperature profile in °F, from the casting-roller/belt interface at $x = 0$ through the belt and the clad to the product centerline at $x = 0.072$ in., 0.0001 sec after initial clad/molten-steel contact.

and through the clad to the inert gas. These initial heat flow directions are still apparent in Figure 5, notably at the belt/clad interface (at $x = 0.032$ in.).

As time progresses, heat is transferred from the molten core through the clad and the belt to be rejected at the belt surface. As a result, the cast core decreases in temperature while the belt and clad increase in temperature. A comparison of Figures 5 and 6 illustrates the general trends. [Figure 6 represents the temperature profile, from the casting-roller/belt interface at $x = 0$ through the belt and the clad to the product centerline at $x = 0.072$ in., at the casting rollers pinch point (time = 0.0313 sec).] As compared to Figure 5, Figure 6 clearly shows the effect of the contact resistance at the belt/clad interface (at $x = 0.032$ in.). Figure 6 also indicates that a portion of the cast core is still molten at the pinch point.

Figure 7 shows the temperature profile at the product take-off point (time = 1.6223 sec). The surface at $x = 0$ represents the surface of the belt adjacent to the take-off roller while the surface at $x = 0.072$ in. represents the product centerline. The belt/clad interface (at $x = 0.032$ in.) is prominent due to the effect of the contact resistance. The figure indicates an average product temperature of 2123°F and an average belt temperature of 1799°F. Obviously, the entire product has solidified and is well below the solidus temperature (2695°F). Since the average belt temperature is 858°F at the point of initial clad/molten-steel contact (time = 0), each endless casting support belt is subjected to a cyclic temperature amplitude of 941°F. In other words, a given belt cross-section heats up from 858°F to 1799°F between time = 0 and time = 1.6223 sec and then cools from 1799°F to 858°F between time = 1.6223 sec and time = 6.2003 sec (or time = 0).

The locations of the liquidus (2754°F), pinch-off (2734°F), and solidus (2695°F) temperatures in the product as a function of time for Case 1 are shown in Figures 8, 9, and 10, respectively. Figure 8 indicates that the centerline temperature is 2754°F at 0.0006 sec after initial clad/molten-steel contact. In other words, the initial molten steel

Figure 6. Case 1 temperature profile in °F, from the casting-roller/belt interface at $x = 0$ through the belt and the clad to the product centerline at $x = 0.072$ in., at the casting rollers pinch point (time = 0.0313 sec).

Figure 7. Case 1 temperature profile in °F, from the take-off roller/belt interface at $x = 0$ through the belt and the clad to the product centerline at $x = 0.072$ in., at the product take-off point (time = 1.6223 sec).

Figure 8. Liquidus temperature (2754°F) location in the product strip as a function of time for Case 1L.

Figure 9. Pinch-off temperature (2734°F) location in the product strip as a function of time for Case 1.

Figure 10. Solidus temperature (2695°F) location in the product strip as a function of time for Case 1.

superheat of 100°F is almost instantly lost to the relatively cool clad and belt. Figure 10 indicates that the centerline temperature decreases to 2695°F at 0.2723 sec after initial clad/molten-steel contact. In other words, the product cross-section is entirely solidified 0.2723 sec after casting.

The product solidifies between the liquidus (2754°F) and the solidus (2695°F) temperatures or over a 59°F range. (Since the steel being cast is not a pure substance, phase change does not occur at a single temperature.) The cooling rate for the steel is then defined as the solidification temperature range divided by the amount of time required for the temperature to decrease from the liquidus to the solidus temperature (or the amount of time required to solidify).¹ This cooling rate for Case 1 is shown in Figure 11 as a function of location in the product. (Note that the cooling rate is a manipulation of the data presented in Figures 8 and 10.)

The cooling rate curve shown in Figure 11 is symmetric because the proposed process is thermally symmetric. A cooling rate is not shown (or defined) for the clad on either side of the product since the clad remains solid throughout the process. The minimum cooling rate of 223°F/sec occurs at the product centerline since that is the last point in the product to complete phase change. As expected, the cooling rate of the steel near the clad is very rapid due to the chilling effect of the clad and belt. The cooling rate then decreases toward the product centerline in response to the increasing thermal resistance and resulting increases in the time required for the necessary heat rejection. Two factors contribute to the symmetric step decreases in cooling rate shown. First, the belt surface contact with the casting roller is replaced by inert gas contact. This produces a step decrease in the rate of heat rejection from the product (through the belt). Second, transfer of the latent heat of fusion from the the cast core tends to support the temperature of the remaining molten material. After sufficient latent heat is removed from the core center (which also flattens the molten steel gradient consistent with the reduction in the rate of heat rejection), progression of the solidus temperature towards the centerline resumes (see Figure 10).

Figure 11. Case 1 cooling rate.

The belt temperature increases from a minimum at the point of initial clad/molten-steel contact (time = 0) to a maximum at the product take-off point (time = 1.6223 sec). Since the belt yield strength is inversely related to temperature (as indicated in Table 2), the maximum belt yield strength occurs at time = 0 and the minimum belt yield strength occurs at time = 1.6223 sec. Figure 12 illustrates the minimum belt yield strength for Case 1. Integration of the profile shown in Figure 12 provides a useful indication of the tensile force required to cause the belt to yield. In this case, the integrated yield strength of each belt is 1935 lbf (assuming that the belt width is 24 in.).

Figures 13 and 14 illustrate ultimate strength profiles for the product at two points during the proposed casting process for Case 1. In both figures, the surface at $x = 0$ represents the belt/clad interface while the surface at $x = 0.040$ in. represents the product centerline. Figure 13 is provided here because it shows the product minimum ultimate strength profile encountered during the proposed casting process. Therefore, integration of this profile gives the minimum load required to cause product failure. In this case, that minimum ultimate load is 164 lbf (assuming a product width of 24 in.) which occurs 0.1523 sec after initial clad/molten-steel contact.

Since the product is supported by the endless belts at its "weakest" point, the minimum ultimate load may be less important than the condition of the product when belt support terminates (at the take-off point). Figure 14 shows the product ultimate strength profile at the take-off point. Integration of the profile shown indicates an ultimate load of 4230 lbf at the end of the proposed casting process will cause product failure.

For continuous operation of the proposed process as configured for Case 1, a total of 4078 Btu/sec is rejected from the product during casting. Obviously, that entire heat load must be absorbed by the inert gas, the water-cooled rollers, and other support structures. An approximate break-down of the heat load to all components is provided in Appendix E for all cases considered. Component values provided are

AT TIME - 1.6223 SEC

BELT YIELD STRENGTH - CASE 1L

Figure 12. Yield strength through the belt thickness at the product take-off point (time = 1.6223 sec) for Case 1.

AT TIME - 0.1523 SEC

PRODUCT ULTIMATE STRENGTH - CASE 1L

Figure 13. Product ultimate strength profile 0.1523 sec after initial clad/molten-steel contact for Case 1.

AT TIME - 1.6223 SEC

Figure 14. Product ultimate strength profile at the product take-off point (time = 1.6223 sec) for Case 1.

approximate primarily because an undetermined portion of the inert gas heat load is undoubtedly distributed to water-cooled components. However, it is known that each casting belt rejects 142 Btu/sec between the point of initial clad/molten-steel contact and the product take-off point. Each belt then rejects 1897 Btu/sec in traveling from the take-off point back to the point of initial clad/molten-steel contact.

In Case 2, the solid front propagation and the boundary layer growth in the molten pool (upstream of the pinch point) were virtually identical to those shown in Figure 2 for Case 1. Therefore, the relative speed between the molten and solid phases and the associated convection heat transfer coefficients were also comparable to the Case 1 values (shown in Figures 3 and 4).

A significant result is apparent in comparing Case 2 temperature profiles shown in Figures 15, 16, and 17 with Case 1 temperature profiles shown in Figures 5, 6, and 7. In both cases, an initial molten steel superheat of 100°F was assumed and average product temperatures at the take-off point were essentially equal (at 2123°F for Case 1 versus 2109°F for Case 2). However, the average belt temperatures for the two cases differ appreciably. In Case 2, the average belt temperature at the point of initial clad/molten-steel contact is 992°F compared to a temperature of 858°F for Case 1. At the product take-off point, the Case 2 average belt temperature is 1713°F while the associated Case 1 temperature is 1799°F. Therefore, the cyclic belt temperature amplitude for Case 2 is 721°F as compared to 941°F for Case 1. Obviously, this result is important since reducing the magnitude of the temperature variation will reduce the thermal fatigue and extend belt life.

Since the initial belt temperature in Case 2 is higher than in Case 1, the chilling effect of the belt and clad on the molten steel in Case 2 is less than in Case 1. As a result, complete solidification of the product does not occur until 0.3223 sec as shown in Figure 18. In addition, the Case 2 cooling rate is lower than the Case 1 cooling rate (see Figures 11 and 19). In Case 2, a minimum cooling rate of 188°F was predicted.

Figure 15. Case 2 temperature profile in °F, from the casting-roller/belt interface at $x = 0$ through the belt and the clad to the product centerline at $x = 0.072$ in., 0.0001 sec after initial clad/molten-steel contact.

Figure 16. Case 2 temperature profile in °F, from the casting-roller/belt interface at $x = 0$ through the belt and the clad to the product centerline at $x = 0.072$ in., at the casting rollers pinch point (time = 0.0313 sec).

Figure 17. Case 2 temperature profile in °F, from the take-off roller/belt interface at $x = 0$ through the belt and the clad to the product centerline at $x = 0.072$ in., at the product take-off point (time = 1.6223 sec).

Figure 18. Solidus temperature (2695°F) location in the product strip as a function of time for Case 2.

Figure 19. Case 2 cooling rate.

However, the Case 2 belt temperatures at the product take-off point are lower than the Case 1 belt temperatures. In addition to being cooler, the 2-1/4 Cr, 1 Mo steel belt has higher yield stresses than 304 stainless steel at all temperatures. Therefore, the tensile force required to cause the belt to yield is significantly higher in Case 2 as compared to Case 1. Assuming a 24 in. wide belt, the yield strength is 7278 lbf. The associated yield strength profile is shown in Figure 20.

Because the Case 1 and 2 product temperatures are essentially equal, the product ultimate strengths will also be equal. For a 24-in. wide product, the ultimate loads at the product take-off point were predicted to be 4230 lbf and 4329 lbf in Cases 1 and 2, respectively. The minimum loads required to cause product failure were 164 lbf and 122 lbf in Cases 1 and 2, respectively.

A total of 4314 Btu/sec is rejected from the product during continuous casting in Case 2. Of that total, 136 Btu/sec is rejected by each casting belt between the point of initial clad/molten-steel contact and the product take-off point while 2021 Btu/sec is rejected from each belt between the take-off point and the point of initial clad/molten-steel contact. Although approximately the same amount of energy is transferred from the product to the belt in Cases 1 and 2, the change in belt temperature in the two cases is significantly different as previously noted. This phenomenon arises from the fact that the heat capacitance of 2-1/4 Cr, 1 Mo steel is approximately 40% greater than the heat capacitance of Type 304 stainless steel over the pertinent temperature range.

After examination of the foregoing results, it was clear that Case 1 is more severe than Case 2 in terms of product temperatures and, particularly, in terms of belt temperatures. Therefore, in Case 3, all of the parameters defining Case 1 were adopted along with the incorporation of the optional heat slabs in the proposed process. Therefore, a comparison of Case 1 and 3 results provides an indication of the effectiveness of the heat slabs. Pertinent details follow.

AT TIME - 1.6223 SEC

BELT YIELD STRENGTH - CASE 2J

Figure 20. Yield strength through the belt thickness at the product take-off point (time = 1.6223 sec) for Case 2.

The time between the point of initial clad/molten-steel contact and the casting rollers pinch point is relatively short. For that reason, the effects produced by initial conditions, including belt and clad temperatures, are not well established prior to reaching the pinch point. Therefore, the Case 1 thermal behavior depicted in Figures 2, 3, and 4 closely represents the corresponding Case 3 (and, as previously noted, the Case 2) predictions.

Figures 21 and 22 illustrate the Case 3 temperature profiles at the point of initial clad/molten-steel contact (time = 0) and at the product take-off point (time = 1.6223 sec), respectively. As compared with Figures 5 and 7 (for Case 1), Figures 21 and 22 clearly indicate lower temperatures can be achieved through the use of the water-cooled heat slabs. Specifically, the minimum average belt temperature decreases from 858°F to 715°F, the maximum average belt temperature decreases from 1799°F to 1446°F, and the average product temperature at the take-off point decreases from 2123°F to 1983°F. In addition, the cyclic belt temperature amplitude decreases from 941°F to 731°F.

As expected, complete production solidification in Case 2 occurs sooner than in Case 1 as a result of the lower belt temperatures. As shown in Figure 23, product solidification is complete 0.2523 sec after initial clad/molten-steel contact.

Figure 24 illustrates the product cooling rate for Case 3. A minimum cooling rate of 237°F/sec at the product centerline is shown. As compared with Figure 11, the loss of casting roller contact in Case 3 produces a relatively smooth change in the slope of the cooling rate curve. This phenomenon is quite reasonable since the transition from a casting-roller/belt boundary to a heat-slab/belt boundary is more gradual than the transition from a casting-roller/belt boundary to an inert-gas/belt boundary (see Section 2.1.1).

One of the desirable side effects of lower belt and product temperatures (resulting from the use of the optional heat slabs) is higher belt yield and product ultimate strengths. Figure 25 represents the

Figure 21. Case 3 temperature profile in °F, from the casting-roller/belt interface at $x = 0$ through the belt and the clad to the product centerline at $x = 0.072$ in., 0.0001 sec after initial clad/molten-steel contact.

Figure 22. Case 3 temperature profile in °F, from the take-off roller/belt interface at $x = 0$ through the belt and the clad to the product centerline at $x = 0.072$ in., at the product take-off point (time = 1.6223 sec).

Figure 23. Solidus temperature (2695°F) location in the product strip as a function of time for Case 3.

PRODUCT COOLING RATE - CASE 3I

Figure 24. Case 3 cooling rate.

AT TIME - 1.6223 SEC

Figure 25. Yield strength through the belt thickness at the product take-off point (time = 1.6223 sec) for Case 3.

minimum belt yield strength profile for Case 3. Integration of the profile indicates that the yield load for each 24-in. wide belt is 8278 lbf. (In Case 1, the maximum yield load on each belt was 1935 lbf.) Figure 26 illustrates the product ultimate strength profile at the take-off point. From the predicted profile, the Case 3 ultimate product load is 4742 lbf. (In Case 1, the ultimate product load was 4230 lbf.) Although the minimum product ultimate strength profile is not shown, the minimum load required to cause product failure in Case 3 is 200 lbf.

In Case 3, a total of 4826 Btu/sec is rejected from the product during continuous casting. Of that total, 1441 Btu/sec is rejected from each endless casting support belt between the product take-off point (time = 1.6223 sec) and the point of initial clad/molten-steel contact (time = 6.2003 sec and time = 0). However, the impact of the heat slabs is most noticeable in the amount of heat rejected between the point of initial clad/molten-steel contact (time = 0) and the product take-off point (time = 1.6223 sec). With the heat slabs (Case 3), 972 Btu/sec is rejected from each belt while only 142 Btu/sec is rejected from each belt without the benefit of the heat slabs (Case 1).

PRODUCT ULTIMATE STRENGTH - CASE 3I

Figure 26. Product ultimate strength profile at the product take-off point (time = 1.6223 sec) for Case 3.

5. CONCLUSIONS AND RECOMMENDATIONS

Based on a review of the limited number of cases considered, continuous casting of thin-strip steel in the manner proposed is thermally feasible. Obviously, the most desirable results will be obtained through a prudent selection of process parameters. The model developed here is an ideal tool for parameter selection.

The importance of one process parameter was indicated through the results of Cases 1 and 2. Essentially the same amount of heat was transferred to the belts in the two cases. However, a difference in cyclic belt temperature variation of 220°F was observed. This effect is due to the fact that the heat capacitance of 2-1/4 Cr, 1 Mo steel is (approximately 40%) greater than the heat capacitance of Type 304 stainless steel. Therefore, the belt heat capacitance is an important parameter and belt materials used in the proposed process should have relatively high heat capacitance since minimizing the cyclic temperature variation should extend service life.

As compared with Case 1, all Case 3 predictions of belt and product temperatures were reduced, as expected. In addition, the (average) belt operating temperature range and the cyclic belt temperature variation were also reduced. Since minimizing the cyclic temperature variation and the operating temperature range should extend belt service life and since belt and product strengths increase as process temperatures decrease, the use of heat slabs is also an important parameter having beneficial effects on the proposed process. In the actual design of this process, consideration should be given to the use of the water-cooled heat slabs in combination with belt materials having relatively high heat capacitance.

The results of all three cases analyzed indicate that the presence of the endless steel casting support belts is an important part of the proposed process. The belts provide an initial chill and a good sink for a considerable amount of heat rejection. As compared to a proposed process without belts,¹ the current configuration offers several advantages.

(1) The boundary conditions on the solidifying product are symmetric

throughout the entire casting process. (2) The resistance to heat flow from the product surface to the belt is relatively low. In the previously analyzed proposed process,¹ heat rejection was slowed considerably on surfaces exposed to the inert gas. (3) The belts provide continuous support during product solidification while product strength becomes adequate for handling. (4) Based on the limited number of cases considered, it appears that continuous casting of thin-strip steel with greater (initial) molten-to-solid ratios will be possible with the current configuration as compared to the previously analyzed process.¹ However, it is suspected that a relationship between the (initial) molten-to-solid ratio achievable and the belt thickness exists. This aspect of the proposed process was not investigated here.

As noted in Section 3, belt material property data were not readily available at the elevated belt temperatures predicted. Extrapolations were performed as required with guidance from available data for AISI-1020 steel at elevated temperatures. On this basis, it is believed that the extrapolations made are reasonable and the impact of these extrapolations, therefore, should be minimal. In addition, other modeling assumptions, primarily boundary conditions, have the potential for much greater impact on the results.

The boundary conditions assumed in this analysis were fixed. However, there is a considerable amount of uncertainty in these figures, particularly in the contact coefficients. The sensitivity of results to variations in boundary conditions was not studied. Therefore, a review of the literature should be performed to better identify appropriate contact coefficient values or range of values. The model can then be used to establish the effect of varying these boundary conditions.

The results of this analysis indicate several desirable criteria for belt material selection including a relatively high heat capacitance and a relatively high yield strength at elevated temperatures. An effort should be undertaken to identify other appropriate materials. Reliable property data should then be obtained. Incorporation of the resulting data into the model would eliminate any concerns about belt material data. The model

should then be used to determine the effects produced by variation of key parameters. The results of that effort will provide guidelines or "operating windows" for actual prototypes.

6. REFERENCES

1. D. L. Knudson, Thermal Analysis of a Proposed Continuous Thin Strip Casting Process, RE-A-83-026, May 1983.
2. H. G. Kraus, SINGLE/CE, A One-Dimensional Finite Element Steady State and Transient Heat Conduction Code with Contiguum Element Capabilities for Use on CDC Cyber 76/173 Computer, RE-A-78-210, June 28, 1978.
3. H. G. Kraus letter to SINGLE Users, "Release of SINGLE/MOD2--HGK-1-83," February 15, 1983.
4. Nuclear Systems Materials Handbook, Oak Ridge National Laboratory, TID 26666.

Sub appendix A of Appendix F

CASTING COMPONENT

```

DLKCM,T77,P2,STANY.
ACCN1,1D=DLK,C=61ATC7001.
ATTACH,OKWLIB,1D=OKW.
LIBRARY,OKWLIB.
REQUEST,TAPE13,*PF.
COMMENT, TAPE13: CASTING MODEL OUTPUT TEMP FILE
REQUEST,TAPE14,*PF.
COMMENT, TAPE14: PARAMETERS IN TYP7 FORMAT
REQUEST,TAPE15,*PF.
COMMENT, TAPE15: PRODUCT UTS IN TYP1 FORMAT
REQUEST,TAPE16,*PF.
COMMENT, TAPE16: BELT YS IN TYP1 FORMAT
ATTACH,TAPE12,CMT1L,1D=DLK.
REWIND,TAPE12.
SINGLE,USUB=4.
REWIND,TAPE13,TAPE14,TAPE15,TAPE16.
CATALOG,TAPE13,CMT1L,1D=DLK,RP=30.
CATALOG,TAPE14,CMT1L,1D=DLK,RP=30.
CATALOG,TAPE15,PUTS1L,1D=DLK,RP=30.
CATALOG,TAPE16,BYSDC1L,1D=DLK,RP=30.
RETURN,TAPE12,TAPE13,TAPE14,TAPE15,TAPE16.
PURGE,CMT1L,1D=DLK.
PURGE,CMT1K,1D=DLK.
PURGE,CMT1K,1D=DLK.
PURGE,PUTS1K,1D=DLK.
PURGE,BYSDC1K,1D=DLK.
BEGIN,FR80POP.
ATTACH,BMIT,1D=DLK.
BMIT,TYPE=1,IN=L,OUT=M,NNB=33,TRST=$6.2003$,UT=$2.797E-05$,
BCH=$9.645E-04$,NCS=66,CAH=$2.894E-05$,NN=194,NCB=32,TEND=$1.6223$.
00
BELT SUPPORTED THIN STRIP CASTING MODELS
304 STAINLESS STEEL BELT
*DECK USERSUB
SUBROUTINE USERDT (NITER,ISTEP,T,DT,TIME)
C ... NITER = ITERATION NUMBER FOR "ISTEP" **DO NOT CHANGE**
C ... ISTEP = SOLUTION TIME STEP NUMBER **DO NOT CHANGE**
C ... T = VECTOR OF NODE TEMPERATURES **DO NOT CHANGE**
C ... DT = SOLUTION TIME STEP INCREMENT
C ... TIME = CURRENT TIME PRIOR TO DT CHANGE **DO NOT CHANGE**
C ... USER SUPPLIED PORTION OF USERDT FROM THIS POINT
COMMON/USEDT/NPASS,NSTEP,TSTEP
TSTEP = DT
NSTEP = ISTEP
NPASS = NITER
RETURN
END
SUBROUTINE USERH (I,BH,BTF,AREA,TIME,T)
C ... I = NODE NUMBER **DO NOT CHANGE**
C ... BH = CONVECTION HEAT TRANSFER COEFFICIENT
C ... BTF = CONVECTION TEMPERATURE
C ... AREA = CONVECTION AREA **DO NOT CHANGE**
C ... TIME = TIME **DO NOT CHANGE**
C ... T = VECTOR OF NODE TEMPERATURES **DO NOT CHANGE**
C ... USER SUPPLIED PORTION OF USERH FROM THIS POINT
COMMON/USEDT/NPASS,NSTEP,TSTEP
COMMON/USEP/NNB,PS
DIMENSION T(1)
IF (I.EQ.NNB) BTF = T(NNB+1)
IF (I.EQ.(NNB+1)) BTF = T(NNB)
C ... CALCULATION OF HEAT LOSS FROM INSIDE OF BELT
C ... IF (I.GT.1.OR.NPASS.GT.0.0) GO TO 30
C ... INITIALIZATION OF HEAT LOSS PARAMETERS
IF (NSTEP.GT.1) GO TO 10
BH1 = BH
BTF1 = BTF
T1 = T(1)
Q1 = 0.0
C ... AVERAGING PARAMETERS

```



```

10 IF (NSTEP.LT.2) GO TO 30
   TIME = TIME-TSTEP
   BHFA = (BH+BHF1)/2.0
   BTFA = ((BT+BTFA)/2.0
   T1A = ((T1+T11)/2.0
   Q1 = Q1+(BHFA*PS*TSTEP*24.0*(T1A-BTFA))
   BHF1 = BH
   BTFA = BT
   T11 = T(1)
20 PRINT 20,Q1,TIME
   FORMAT (3X, BY THE INTEGRATED RATE OF HEAT LOSS FROM THE INSIDE OF ",
   * "THE SECOND ", " OF HEAT LOSS FROM OUTSIDE OF BELT
   * "CALCULATION ", " OF HEAT LOSS FROM OUTSIDE OF BELT
30 IF (I.NE.NNB) OR (NPASS.GT.0) GO TO 60
   C ... IF (NSTEP.GT.1) GO TO 40
   BHNNB = BH
   BTNNB = BT
   TNNB = T(NNB)
   QNNB = 0.0
40 AVERAGEING PARAMETERS
   IF (NSTEP.LT.2) GO TO 60
   BHFA = (BH+BHNNB)/2.0
   BTFA = (BT+BTNNB)/2.0
   TNNBA = (T(NNB)+TNNB)/2.0
   QNNBA = QNNB+(BHFA*PS*TSTEP*24.0*(TNNBA-BTFA))
   BHNNB = BH
   BTNNB = BT
   TNNB = T(NNB)
50 PRINT 50,QNNB,TIME
   FORMAT (3X, BY THE INTEGRATED RATE OF HEAT LOSS FROM THE OUTSIDE OF ",
   * "THE SECOND ", " OF HEAT LOSS FROM THE OUTSIDE OF ",
   * "CALCULATION ", " OF HEAT LOSS FROM THE OUTSIDE OF "
60 RETURN
END SUBROUTINE USER (I,BR,BTR,AREA,RADC,TIME,T)
   I BR = NODE NUMBER
   BTR = SIGMA SHAPE FACTOR*EMISSIVITY FOR NODE
   AREA = T-INFINITY IN ABSOLUTE UNITS
   RADC = ADDITIVE TO HEAT TRANSFER AREA
   TIME = TIME TO ABSOLUTE UNITS
   T = VECTOR OF NODE TEMPERATURES
   BR = NOT IN ABSOLUTE UNITS UNLESS RADC = 0
   BTR = SUPPLIED/NPASS, NSTEP, TSTEP
   AREA = COMMON/USEP/NNB,PS
   RADC = COMMON/USEP/NNB,PS
   TIME = CALCULATION OF HEAT LOSS FROM INSIDE OF BELT
   BR = IF (I.GT.1) OR (NPASS.GT.0) GO TO 30
   BTR = IF (NSTEP.GT.1) GO TO 10
   T11 = T(1)
   Q1 = 0.0
10 AVERAGEING PARAMETERS
   IF (NSTEP.LT.2) GO TO 30
   BHFA = (BH+BTR1)/2.0
   BTFA = ((BT+BTFA)/2.0
   T1A = ((T1+T11)/2.0
   Q1 = Q1+(BTRA*PS*TSTEP*24.0*(T1A-BTFA))
   BTR1 = BTR
   T11 = T(1)

```

```

20 PRINT 20,Q1,THE
  FORMAT (3X,"THE INTEGRATED RATE OF HEAT LOSS FROM THE INSIDE OF ",
  * "THE BELT BY RADIATION" = "E11.5," BTU/SEC THRU"E11.5,
  * " SECONDS." ,//)
30 RETURN

```

```

END
SUBROUTINE USERP (N,I,J,MAT,RHO,CONDR,
  *CAPC,CAPRHO,ITRAN,TBAR,TIME,R,T)

```

```

C ... N = ELEMENT NUMBER **DO NOT CHANGE**
C ... I = I-TH NODE OF ELEMENT **DO NOT CHANGE**
C ... J = J-TH NODE OF ELEMENT **DO NOT CHANGE**
C ... MAT = MATERIAL NUMBER **DO NOT CHANGE**
C ... RHO = DENSITY
C ... CONDR = THERMAL CONDUCTIVITY
C ... NOTE: THERMAL CONDUCTIVITY MUST BE .GE. ZERO
C ... CAPC = SPECIFIC HEAT
C ... CAPRHO = CAPC*RHO
C ... NOTE: AFTER THIS SUBROUTINE IS CALLED, CAPRHO
C ... IS USED BUT CAPC & RHO ARE NOT
C ... ITRAN = 0, TBAR MEANINGLESS; = 1, TBAR OKAY **DO NOT CHANGE**
C ... TBAR = AVERAGE ELEMENT TEMPERATURE **DO NOT CHANGE**
C ... TIME = TIME **DO NOT CHANGE**
C ... R = VECTOR OF NODE R OR X VALUES **DO NOT CHANGE**
C ... T = VECTOR OF NODE TEMPERATURES **DO NOT CHANGE**
C ... USER SUPPLIED PORTION OF USERP FROM THIS POINT
C ... COMMON/CELEM/NE,IV1,IV2
C ... COMMON/CSTEP/RV3,RV4,RV5,IV6,IV7,RV8,IV9,IV10,RV11,RV12,RV13,
  *RV14,IV15,NTSP,TPRNT(500),IV16
C ... COMMON/USEDT/NPASS,NSTEP,TSTEP
C ... COMMON/USEP/NNB,PS
C ... DIMENSION CMBHC(18),CMBT(18),CMBTC(18),CMBYS(18),CMBYST(18)
C ... DIMENSION PUTS(12),PUTST(12),R(1),SSBHC(19),SSBT(19),SSBTC(19)
C ... DIMENSION SSBYS(10),SSBYST(10),T(1)
C ... PARAMETER INPUT DATA
C ... DATA BT/0.032/
C ... DATA CMBHC/3.069E-02,3.209E-02,3.348E-02,3.469E-02,3.635E-02,
  *3.778E-02,3.933E-02,4.108E-02,4.320E-02,4.586E-02,4.938E-02,
  *5.432E-02,6.027E-02,7.016E-02,5.993E-02,5.364E-02,5.349E-02,
  *5.333E-02/
C ... DATA CMBT/100.0,200.0,300.0,400.0,500.0,600.0,700.0,800.0,900.0,
  *1000.0,1100.0,1200.0,1292.0,1400.0,1500.0,1600.0,1700.0,1800.0/
C ... DATA CMBTC/4.876E-04,4.959E-04,4.991E-04,4.983E-04,4.943E-04,
  *4.877E-04,4.791E-04,4.688E-04,4.572E-04,4.446E-04,4.310E-04,
  *4.166E-04,4.028E-04,3.860E-04,3.649E-04,3.535E-04,3.367E-04,
  *3.195E-04/
C ... DATA CMBYS/36600.0,34940.0,33840.0,33160.0,32720.0,32370.0,
  *31940.0,31280.0,30220.0,28600.0,26280.0,23050.0,21030.0,18700.0,
  *16090.0,13170.0,9850.0,6130.0/
C ... DATA CMBYST/100.0,200.0,300.0,400.0,500.0,600.0,700.0,800.0,900.0,
  *1000.0,1100.0,1200.0,1300.0,1400.0,1500.0,1600.0,1700.0,1800.0/
C ... DATA CMFBL/2.0/,CMFM/1000.0/,CT/0.008/,DMP/2.0/,EF/119.0/
C ... DATA FMPS/0.64/,INDEXP/1/,KPI/21/,KPT/314/,MATTYPE/1/
C ... DATA NNB/33/,NR/2/,NTSTEPS/482/,PI/0.080/
C ... DATA PUTS/60000.0,67000.0,67500.0,59500.0,41000.0,21000.0,
  *10500.0,5500.0,3500.0,2600.0,2300.0,0.0/
C ... DATA PUTST/70.0,300.0,500.0,700.0,900.0,1100.0,1300.0,
  *1500.0,1700.0,1900.0,2100.0,2695.0/
C ... DATA RR/24.0/
C ... DATA SSBHC/3.351E-02,3.473E-02,3.574E-02,3.658E-02,3.726E-02,
  *3.782E-02,3.830E-02,3.865E-02,3.898E-02,3.927E-02,3.957E-02,
  *3.988E-02,4.023E-02,4.062E-02,4.112E-02,4.170E-02,4.241E-02,
  *4.329E-02,4.431E-02/
C ... DATA SSBT/100.0,200.0,300.0,400.0,500.0,600.0,700.0,800.0,900.0,
  *1000.0,1100.0,1200.0,1300.0,1400.0,1500.0,1600.0,1700.0,1800.0,
  *1900.0/
C ... DATA SSBTC/2.024E-04,2.152E-04,2.275E-04,2.396E-04,2.512E-04,
  *2.623E-04,2.734E-04,2.840E-04,2.944E-04,3.049E-04,3.148E-04,
  *3.248E-04,3.347E-04,3.444E-04,3.544E-04,3.641E-04,3.739E-04,
  *3.838E-04,3.939E-04/

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DATA SSBYS/27500.0,21400.0,18400.0,17100.0,16100.0,14700.0,
*12400.0,9000.0,4800.0,0.0/
DATA SSBYST/150.0,350.0,550.0,750.0,950.0,1150.0,1350.0,
*1550.0,1750.0,1850.0/
DATA TL/2754.0/TS/2695.0/
C ... BT = BELT THICKNESS
C ... CMBHC = 2.25 CR, 1 MO STEEL BELT HEAT CAPACITANCE : TO 1292 F
C ... CMBT = 2.25 CR, 1 MO STEEL BELT TEMPERATURE
C ... CMBTC = 2.25 CR, 1 MO STEEL BELT THERMAL CONDUCTIVITY : TO 1292 F
C ... CMBYS = 2.25 CR, 1 MO STEEL BELT YIELD STRENGTH : TO 1200 F
C ... CMBYST = 2.25 CR, 1 MO STEEL BELT YIELD STRENGTH TEMPERATURE
C ... CMFBL = (THERMAL) CONDUCTIVITY MULTIPLICATION FACTOR (B.L.)
C ... CMFM = (THERMAL) CONDUCTIVITY MULTIPLICATION FACTOR (MOLTEN)
C ... CT = CLAD THICKNESS
C ... DMP = DEPTH OF MOLTEN POOL
C ... EF = ENERGY OF FUSION
C ... FMPS = FRACTION OF MAXIMUM PRODUCT SPEED
C ... IF INDEXP .LT. 0, NO PRINT; IF .GT. 0, PRINT
C ... KPI = STRENGTH PRINT INTERVAL
C ... KPT = STRENGTH PRINT TIME
C ... MATTYPE = (BELT) MATERIAL TYPE: IF 1, 304 SS; IF 2, 2.25 CR, 1 MO
C ... NE = NUMBER OF ELEMENTS
C ... NNB = NUMBER OF NODES IN THE BELT (= FIRST PRODUCT ELEMENT NUMBER)
C ... NPASS = ITERATION NUMBER FOR "NSTEP"
C ... NR = NODE RANGE (OF INTEREST)
C ... NSTEP = SOLUTION TIME STEP NUMBER
C ... NTSP = NUMBER OF TIME STEPS FOR (SOLUTION) PRINTING
C ... NTSTEPS = TOTAL NUMBER OF TIME STEPS IN THE TRANSIENT
C ... PT = PRODUCT THICKNESS
C ... PUTS = PRODUCT ULTIMATE TENSILE STRENGTH
C ... PUTST = PRODUCT ULTIMATE TENSILE STRENGTH TEMPERATURE
C ... RR = ROLL RADIUS
C ... SSBHC = 304 STAINLESS STEEL BELT HEAT CAPACITANCE : TO 1500 F
C ... SSBT = 304 STAINLESS STEEL BELT TEMPERATURE
C ... SSBTC = 304 STAINLESS STEEL BELT THERMAL CONDUCTIVITY : TO 1600 F
C ... SSBYS = 304 STAINLESS STEEL BELT YIELD STRENGTH
C ... SSBYST = 304 STAINLESS STEEL BELT YIELD STRENGTH TEMPERATURE
C ... TL = MATERIAL LIQUIDUS TEMPERATURE
C ... TPRNT = VECTOR OF (SOLUTION) PRINT TIMES
C ... TS = MATERIAL SOLIDUS TEMPERATURE
C ... TSTEP = SOLUTION TIME STEP INCREMENT
C ... BRANCH FOR BELT ELEMENTS
C ... IF (MAT.EQ.1) GO TO 430
C ... PRODUCT CAPRHO CALCULATIONS
C ... CHECK TO SEE IF ELEMENT IS SOLID & BRANCH ACCORDINGLY
C ... IF (T(I).LT.TS.AND.T(J).LT.TS) GO TO 120
C ... CHECK TO SEE IF ELEMENT IS MOLTEN & BRANCH ACCORDINGLY
C ... IF (T(I).GT.TL.AND.T(J).GT.TL) GO TO 100
C ... BRANCH FOR A TWO-PHASE ZERO GRADIENT ELEMENT
C ... IF (T(I).EQ.T(J)) GO TO 90
C ... DETERMINATION OF CAPRHO IN A TWO-PHASE ELEMENT
C ... ELEMENT TEMPERATURES ORDERED FOR INTEGRATION
C ... IF (T(I).GT.T(J)) GO TO 10
C ... T1 = T(I)
C ... T2 = T(J)
C ... GO TO 20
10 T1 = T(J)
C ... T2 = T(I)
C ... INTEGRATION OF CAPRHO THRU STATEMENT 70
20 IF (T1.LT.TS) GO TO 30
E1 = ((T1-TS)/(TL-TS))*EF*(460.0-(0.01818*(T1-1700.0)))/1728.0
INDEXR = 2
GO TO 40
30 E1 = 0.0
INDEXR = 1
40 IF (T2.LE.TL.AND.INDEXR.EQ.2) GO TO 70
IF (T2.LE.TL.AND.INDEXR.EQ.1) GO TO 60
IF (INDEXR.EQ.2) GO TO 50
E2 = ((0.168*490.906*TS)-(0.00909*0.168*TS*TS)

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      *-(0.168*490.906*T1)+(0.00909*0.168*T1*T1)
      *+((460.0-(0.01818*(TL-1700.0)))*EF)
      *+(0.172*490.906*T2)-(0.00909*0.172*T2*T2)
      *-(0.172*490.906*TL)+(0.00909*0.172*TL*TL))/1728.0
      GO TO 80
50  E2 = (((460.0-(0.01818*(TL-1700.0)))*EF)
      *+(0.172*490.906*T2)-(0.00909*0.172*T2*T2)
      *-(0.172*490.906*TL)+(0.00909*0.172*TL*TL))/1728.0
      GO TO 80
60  E2 = ((0.168*490.906*TS)-(0.00909*0.168*TS*TS)
      *-(0.168*490.906*T1)+(0.00909*0.168*T1*T1)
      *+(((T2-TS)/(TL-TS))*EF*(460.0-(0.01818*(T2-1700.0)))))/1728.0
      GO TO 80
70  E2 = ((T2-TS)/(TL-TS))*EF*(460.0-(0.01818*(T2-1700.0)))/1728.0
80  CAPRHO = (E2-E1)/(T2-T1)
      GO TO 150
C ... DETERMINATION OF CAPRHO FOR A TWO-PHASE ZERO GRADIENT ELEMENT
90  E1 = 0.0
      E2 = 0.0
      CP = (EF/(TL-TS))+0.170
      DEN = (460.0-(0.01818*(TBAR-1700.0)))/1728.0
      CAPRHO = CP*DEN
      GO TO 150
C ... DETERMINATION OF CAPRHO FOR MOLTEN ELEMENT
100 E1 = 0.0
      E2 = 0.0
      IF (TBAR.GT.2800.0) GO TO 110
      CP = 0.172
      DEN = (460.0-(0.01818*(TBAR-1700.0)))/1728.0
      CAPRHO = CP*DEN
      GO TO 150
110 CP = 0.172
      DEN = 435.0/1728.0
      CAPRHO = CP*DEN
      GO TO 150
C ... DETERMINATION OF CAPRHO FOR SOLID ELEMENT
120 E1 = 0.0
      E2 = 0.0
      IF (TBAR.GT.1700.0) GO TO 140
      IF (TBAR.GT.1400.0) GO TO 130
      CP = 0.1283-(2.438E-04*TBAR)+(9.372E-07*TBAR*TBAR)
      *-(1.102E-09*TBAR*TBAR*TBAR)+(4.424E-13*TBAR*TBAR*TBAR*TBAR)
      DEN = (490.0-(0.01147*TBAR))/1728.0
      CAPRHO = CP*DEN
      GO TO 150
130 CP = 3.949-(5.640E-03*TBAR)+(2.880E-06*TBAR*TBAR)
      *-(5.567E-10*TBAR*TBAR*TBAR)+(2.638E-14*TBAR*TBAR*TBAR*TBAR)
      DEN = (490.0-(0.01147*TBAR))/1728.0
      CAPRHO = CP*DEN
      GO TO 150
140 CP = 0.168
      DEN = (460.0-(0.01818*(TBAR-1700.0)))/1728.0
      CAPRHO = CP*DEN
C ... PRODUCT COND. CALCULATIONS
C ... BRANCH TO BYPASS INITIALIZING CALCULATIONS
150 IF (N.GT.NNB.OR.NPASS.GT.0.OR.NSTEP.GT.1) GO TO 160
C ... CALCULATION OF PINCH-OFF TEMPERATURE
      TPO = TS+(2.0*(TL-TS))/3.0
C ... CALCULATION OF PRODUCT SPEED
      PS = (192.0*FHPS)/(24.0*PT)
C ... CALCULATION OF INITIAL ANGLE
      THETA1 = ASIN(DMP/(RR+BT+CT))
      THETA = THETA1
C ... CALCULATION OF ANGULAR VELOCITY
      OMEGA = PS/(RR+BT)
C ... INITIAL COORDINATE CALCULATION
      R1 = (RR+BT+CT)*COS(THETA1)
      Z1 = (RR+BT+CT)*SIN(THETA1)
      PL = 0.0

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C ... BRANCH TO BYPASS TIME-DEPENDENT CALCULATIONS
160 IF (N.GT.NNB.OR.NPASS.GT.0) GO TO 180
    IF (NSTEP.LT.2) GO TO 170
C ... SUM PLATE LENGTH
    PL = PL+CPL
    R1 = R2
    Z1 = Z2
C ... CALCULATION OF ANGLE
170 THETA = THETA-(OMEGA*TSTEP)
    IF (THETA.LE.0.0) GO TO 180
C ... CALCULATION OF CURRENT BOUNDARY LAYER THICKNESS
    CBLT = ((PT/2.0)-CT)*(1.0-(THETA/THETA1))
C ... CALCULATION OF TS, TPO, & TL LOCATIONS ONCE EACH ITERATION
180 IF (N.GT.NNB) GO TO 300
    DO 190 K1 = NNB,NE
    IF (T(K1+1).LE.TS.AND.T(K1+2).GT.TS) GO TO 200
190 CONTINUE
    TSL = R(NNB+1)
    IF (T(NE+2).LE.TS) TSL = R(NE+2)
    K1 = NNB
    GO TO 210
200 TSL = R(K1+1)+((R(K1+2)-R(K1+1))*(TS-T(K1+1)))/(T(K1+2)-T(K1+1))
210 DO 220 K2 = K1,NE
    IF (T(K2+1).LE.TPO.AND.T(K2+2).GT.TPO) GO TO 230
220 CONTINUE
    TPOL = R(NNB+1)
    IF (T(NE+2).LE.TPO) TPOL = R(NE+2)
    K2 = NNB
    GO TO 240
230 TPOL = R(K2+1)+((R(K2+2)-R(K2+1))*(TPO-T(K2+1)))/(T(K2+2)-T(K2+1))
240 DO 250 K3 = K2,NE
    IF (T(K3+1).LE.TL.AND.T(K3+2).GT.TL) GO TO 260
250 CONTINUE
    TLL = R(NNB+1)
    IF (T(NE+2).LE.TL) TLL = R(NE+2)
    GO TO 270
260 TLL = R(K3+1)+((R(K3+2)-R(K3+1))*(TL-T(K3+1)))/(T(K3+2)-T(K3+1))
C ... ASSIGNMENT OF SOLID FRONT LOCATION
270 SFL = TPOL
C ... PARAMETER PLOT INFORMATION TO TAPE14 ONCE EACH STEP
    IF (NPASS.GT.0.OR.NSTEP.LT.2) GO TO 290
    TME = TIME-TSTEP
    BLLP = BLL-BT
    IF (BLLP.LT.0.0) BLLP = 0.0
    TSLP = TSL-BT
    TPOLP = TPOL-BT
    TLLP = TLL-BT
    WRITE (14,280) TME,RS,HSaved,BLLP,TSLP,TPOLP,TLLP
280 FORMAT (7(E13.6))
290 IF (THETA.LE.0.0) GO TO 420
C ... RESET CBLT IF PRODUCT IS SOLID OR MOLTEN
    IF ((SFL+1.0E-09).GE.R(NE+2).OR.(SFL-1.0E-09).LE.R(NNB+1)) CBLT=0.0
C ... RESET CBLT IF BOUNDARY LAYER REACHES PRODUCT MIDPOINT
    IF ((SFL+CBLT).GE.R(NE+2)) CBLT = R(NE+2)-SFL
C ... RESET CBLT IF CLAD MELT OCCURS
    IF (SFL.LE.(R(NNB+1)+CT)) CBLT = CBLT+(R(NNB+1)-SFL)
    BLL = SFL+CBLT
300 IF (THETA.LE.0.0) GO TO 420
    IF (R(I).GE.SFL) GO TO 310
C ... SOLID CONDUCTIVITY CALCULATIONS (CONDRS)
    TAV = (T(I)+TPO)/2.0
    IF (R(J).LE.SFL) TAV = TBAR
    CONDRS = (.36.767-(1.026E-02*TAV)-(1.323E-05*TAV*TAV)+
    * (1.003E-08*TAV*TAV*TAV)-(1.680E-12*TAV*TAV*TAV*TAV))/43200.0
    IF (R(J).LE.SFL) GO TO 400
310 IF (R(I).GT.SFL) GO TO 320
C ... CONVECTION ENHANCED CONDUCTIVITY CALCULATIONS (CECOND)
C ... DENSITY CALCULATION - FROM BETHLEHAM STEEL
    DEN = (460.0-(0.01818*(T(NE+2)-1700.0)))/1728.0

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C ... IF (T(NE+2).GT.2800.0) DEN = 435.0/1728.0
SPECIFIC HEAT CALCULATION - FROM BETHLEHAM STEEL
CP = (EF/(TL-TS))+0.170
IF (T(NE+2).GT.TL) CP = 0.172
C ... THERMAL CONDUCTIVITY CALCULATION - FROM BETHLEHAM STEEL
TC = (36.767-(1.026E-02*T(NE+2))-(1.323E-05*T(NE+2)*T(NE+2))+
*(1.003E-08*T(NE+2)*T(NE+2)*T(NE+2))-(1.680E-12*T(NE+2)*T(NE+2)*
*T(NE+2)*T(NE+2)))/43200.0
C ... CALCULATION OF SOLID FRONT SPEED
SFS = PS*(RR+SFL)/(RR+BT)
C ... CALCULATION OF SFL COORDINATES
R2 = (RR+SFL)*COS(THETA)
Z2 = (RR+SFL)*SIN(THETA)
C ... CALCULATION OF BULK SPEED
TW = PT+(2.0*(RR+BT))-(2.0*(((RR+BT)*(RR+BT))-(Z2*Z2))*0.5))
BS = PS*PT*COS(THETA)/TW
C ... CALCULATION OF RELATIVE SPEED
RS = SFS-BS
C ... CALCULATION OF PLATE LENGTH
CPL = (((R2-R1)*(R2-R1))+((Z2-Z1)*(Z2-Z1)))*0.5
C ... CONVECTION HEAT TRANSFER COEFFICIENT CALCULATION
H = 0.530*((DEN*CP*TC*RS/(PL+CPL))*0.5)
HSAVED = H*518400.0
CECONDR = H*(T(NE+2)-TPO)*(R(J)-SFL)/(T(J)-TPO)
C ... INITIAL CECONDR CALCULATION ONLY
IF (NPASS.LT.1.AND.NSTEP.LT.2) CECONDR = H*
*(T(NE+2)-T(I))*(R(J)-R(I))/(T(J)-T(I))
IF (R(I).LT.SFL.AND.R(J).LE.(SFL+CBLT)) GO TO 320
IF (R(I).LT.SFL.AND.R(J).GT.(SFL+CBLT)) GO TO 330
IF (R(J).LE.(SFL+CBLT)) GO TO 370
GO TO 330
320 IF (R(I).GE.(SFL+CBLT)) GO TO 330
C ... BOUNDARY LAYER CONDUCTIVITY CALCULATIONS (BLCONDR)
TAV = T(I)+(0.5*(T(J)-T(I))*(SFL+CBLT-R(I))/(R(J)-R(I)))
IF (R(J).LE.(SFL+CBLT)) TAV = TBAR
BLCONDR = CMFBL*(36.767-(1.026E-02*TAV)-(1.323E-05*TAV*TAV)+
*(1.003E-08*TAV*TAV*TAV)-(1.680E-12*TAV*TAV*TAV*TAV))/43200.0
IF (R(J).LE.(SFL+CBLT)) GO TO 380
C ... MOLTEN CONDUCTIVITY CALCULATIONS (CONDRM)
330 TAV = 0.5*(T(I)+T(J)+((T(J)-T(I))*(SFL+CBLT-R(I))/(R(J)-R(I))))
IF (R(I).GE.(SFL+CBLT)) TAV = TBAR
CONDRM = CMFM*(36.767-(1.026E-02*TAV)-(1.323E-05*TAV*TAV)+
*(1.003E-08*TAV*TAV*TAV)-(1.680E-12*TAV*TAV*TAV*TAV))/43200.0
IF (R(I).GE.(SFL+CBLT)) GO TO 390
IF (R(I).GT.SFL.AND.R(J).GT.(SFL+CBLT)) GO TO 300
IF (R(I).LT.SFL.AND.R(J).GT.(SFL+CBLT)) GO TO 340
C ... CONVECTION ENHANCED + MOLTEN ELEMENT
CONDRS = 0.0
CECONDR = CECONDR*CBLT/(R(J)-R(I))
BLCONDR = 0.0
CONDRM = CONDRM*(R(J)-SFL-CBLT)/(R(J)-R(I))
GO TO 410
C ... SOLID + CONVECTION ENHANCED + MOLTEN ELEMENT
340 CONDRS = CONDRS*(SFL-R(I))/(R(J)-R(I))
CECONDR = CECONDR*CBLT/(R(J)-R(I))
BLCONDR = 0.0
CONDRM = CONDRM*(R(J)-SFL-CBLT)/(R(J)-R(I))
GO TO 410
C ... SOLID + CONVECTION ENHANCED ELEMENT
350 CONDRS = CONDRS*(SFL-R(I))/(R(J)-R(I))
CECONDR = CECONDR*(R(J)-SFL)/(R(J)-R(I))
BLCONDR = 0.0
CONDRM = 0.0
GO TO 410
C ... BOUNDARY LAYER + MOLTEN ELEMENT
360 CONDRS = 0.0
CECONDR = 0.0
BLCONDR = BLCONDR*(SFL+CBLT-R(I))/(R(J)-R(I))
CONDRM = CONDRM*(R(J)-SFL-CBLT)/(R(J)-R(I))

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      H = 0.0
      GO TO 410
C   ... CONVECTION ENHANCED ELEMENT
370 CONDRS = 0.0
      BLCONDR = 0.0
      CONDRM = 0.0
      GO TO 410
C   ... BOUNDARY LAYER ELEMENT
380 CONDRS = 0.0
      CECONDR = 0.0
      CONDRM = 0.0
      H = 0.0
      GO TO 410
C   ... MOLTEN ELEMENT
390 CONDRS = 0.0
      CECONDR = 0.0
      BLCONDR = 0.0
      H = 0.0
      GO TO 410
C   ... SOLID ELEMENT
400 CECONDR = 0.0
      BLCONDR = 0.0
      CONDRM = 0.0
      H = 0.0
410 CONDR = CONDRS+CECONDR+BLCONDR+CONDRM
      GO TO 520
C   ... ELEMENTS BEYOND PINCH POINT
420 CONDRS = 0.0
      CECONDR = 0.0
      BLCONDR = 0.0
      CONDRM = 0.0
      H = 0.0
      HSAVED = H*518400.0
      THETA = 0.0
      CBLT = 0.0
      BLL = 0.0
      PL = 0.0
      CPL = 0.0
      R1 = 0.0
      R2 = 0.0
      Z1 = 0.0
      Z2 = 0.0
      TW = PT
      RS = 0.0
      CONDR = (36.767-(1.026E-02*TBAR)-(1.323E-05*TBAR*TBAR)+(1.003E-08*
*TBAR*TBAR*TBAR)-(1.680E-12*TBAR*TBAR*TBAR*TBAR))/43200.0
      GO TO 520
C   ... BELT, CAPRHQ & CONDR CALCULATIONS
430 IF (MATTYPE.EQ.2) GO TO 440
      CALL TABLE (19,SSBT,SSBHC,TBAR,CAPRHQ)
      CALL TABLE (19,SSBT,SSBTC,TBAR,CONDR)
      GO TO 450
440 CALL TABLE (18,CMBT,CMBHC,TBAR,CAPRHQ)
      CALL TABLE (18,CMBT,CMBTC,TBAR,CONDR)
C   ... STRENGTH CALCULATIONS
450 IF (N.GT.1.OR.NPASS.GT.0.OR.(NSTEP-1).NE.KPT) GO TO 520
      WRITE (15,460) KPT
      WRITE (16,460) KPT
460 FORMAT (I3)
      KPT = KPT+KPI
      DO 490 KB = 1,NNB
      IF (MATTYPE.EQ.2) GO TO 480
      CALL TABLE (10,SSBYST,SSBYS,T(KB),Y)
      WRITE (16,470) R(KB),Y
470 FORMAT (E11.5,1X,E11.5)
      GO TO 490
480 CALL TABLE (18,CMBYST,CMBYS,T(KB),Y)
      WRITE (16,470) R(KB),Y
490 CONTINUE

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      NI1 = NNB+1
      NI2 = NE+2
      DD 500 KP = NI1,NI2
      CALL TABLE (12,PUTST,PUTS,T(KP),Y)
      WRITE (15,470) R(KP),Y
500   CONTINUE
      IF (KPT.LE.NTSTEPS) GO TO 520
      WRITE (15,510)
      WRITE (16,510)
510   FORMAT ('MENDM')
520   IF (INDEXP.LT.0.OR.NPASS.GT.0) GO TO 620
      DD 530 LPT = 1,NTSP
      IF (TIME-TSTEP-1.0E-06.LT.TPRNT(LPT).AND.TIME-TSTEP+1.0E-06.GT.
      *TPRNT(LPT)) GO TO 540
530   CONTINUE
      GO TO 620
540   IF (N.GT.1) GO TO 560
      PRINT 550,N,MATTYPE,TBAR,CAPRHO,CONOR
550   FORMAT ('1N,2X,N' = ",I12," MATTYPE = ",I10," TBAR = ",
      *E10.4," CAPRHO = ",E10.4," CONOR = ",E11.4)
      PRINT 610
560   IF (N.LT.(K2-NR).OR.N.GT.(K2+NR).OR.MAT.EQ.1) GO TO 620
      PRINT 570,N,CAPRHO,E1,E2
570   FORMAT (3X,"N" = ",I12," CAPRHO = ",E10.4," E1 = ",
      *E10.4," E2 = ",E10.4)
      PRINT 580,CONDR,CONDRS,CECONDR,BLCONDR,CONDRM,THETA
580   FORMAT (3X,"CONDR = ",E12.4," CONDRS = ",E10.4," CECONDR = ",
      *E10.4," BLCONDR = ",E10.4," CONDRM = ",E11.4," THETA = ",E12.4)
      PRINT 590,CBLT,SFL,BLL,PL,R2,R1
590   FORMAT (3X,"CBLT = ",E12.4," SFL = ",E10.4," BLL = ",
      *E10.4," PL = ",E10.4," R2 = ",E11.4," R1 = ",E12.4)
      PRINT 600,Z2,Z1,CPL,TW,RS,H
600   FORMAT (3X,"Z2 = ",E12.4," Z1 = ",E10.4," CPL = ",
      *E10.4," TW = ",E10.4," RS = ",E11.4," H = ",E12.4)
      PRINT 610
610   FORMAT (3X,"*****")
620   RETURN
      END

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TITLE : THIN-STRIP BELT CASTING MODEL
 - INPUT DATA REQUIRED IN DECK 6 USERP -
 UNITS - BTU,IN,SEC,LBM,DEG F

MATERIALS

```

      2 1
      1 BELT MATERIAL BY USERP
      2 LOW-CARBON STEEL BY USERP

```

NODES : BELT/CLAD AT NODES 33/34; CLAD/MELT AT NODE 66

```

194 0
1 32 0.0
0.032
34 160 0.032
0.072

```

ELEMENTS : 1 PER MIL IN BELT, 4 PER MIL IN CAST(CLAD+MELT)

```

192
1 1 2 1
32 32 33 1
33 34 35 2
192 193 194 2

```

STEP : PINCH POINT REACHED AT 0.0313 SEC

```

0.0 1.6223001 1 1 50 1
0.3 0.7

```

```

77
0.0001 12 0.0001

```


0.0023	30	0.001
0.0423	18	0.01
0.3223	13	0.1

FUNCTIONS

4					
1	2	1	TIME STEP DATA		
0.03225		0.0001	0.03226	0.01	
2	2	1	CONVECTION COEFFICIENT INSIDE BELT		
0.03127	3.998E-04	1	0.03128	2.894E-05	
3	2	1	BOUNDARY TEMPERATURE INSIDE BELT		
0.03127	125.0	1	0.03128	500.0	
4	2	1	RADIATION COEFFICIENT INSIDE BELT		
0.03127	0.0	1	0.03128	1.653E-15	

INITIAL

CONVECTION

3					
1	1.0	2	1.0	3	
33	9.645E-04				
34	9.645E-04				

RADIATION

1	460.0	4	1.0	3	
1	1.0				

PLOTS

10	0.072	0.0	2900.0	440.0	
1	10	50	100	200	313 355 397 440 482

TPLOTS

(P)RODUCT CENTERLINE AND CLAD SURFACE TEMPERATURES.

2	34	194		
0.0	1.6223	440.0	2900.0	

(T)IME IN SECONDS

(T)EMPERATURE IN DEGREES (F)AHRENHEIT

(C)LAD TEMPERATURES.

2	34	66		
0.0	1.6223	440.0	2900.0	

(T)IME IN SECONDS

(T)EMPERATURE IN DEGREES (F)AHRENHEIT

(B)ELT TEMPERATURES.

2	1	33		
0.0	1.6223	440.0	2900.0	

(T)IME IN SECONDS

(T)EMPERATURE IN DEGREES (F)AHRENHEIT

SINGLE/MOD2

END OF DATA

Sub appendix B of Appendix F

CASTING-TO-BELT COMPONENT

```

.PROC,BMIT,TYPE=1,IN=A,OUT=B,NNB=33,TRST=$6.2003$,UT=$2.797E-05$,
BCH=$9.645E-04$,NCS=66,CAH=$2.894E-05$,NN=194,NEB=32,
TEND=$1.6223$,TESTIT=.
TESTIT-RETURN,JOBIN.
TESTIT-REQUEST,JOBIN,*Q.
TESTIT-COPY,JDATA,JOBIN.
TESTIT-ROUTE,JOBIN,DC=#1N,TID=GU.
TESTIT-RETURN,JDATA,JOBIN.
REVERT.
.DATA,JDATA.
DLKBT,T37,P2,STANY.
ACCNT,ID=DLK,C=61ATC7001.
REQUEST,TAPE12,*PF.
ATTACH,TAPE13,CMT_TYPE_IN,ID=DLK.
REWIND,TAPE13.
FTN5,ET,L=0,ROUND,GO.
REWIND,TAPE12.
CATALOG,TAPE12,BMIT_TYPE_IN,ID=DLK,RP=30.
RETURN,TAPE12,TAPE13.
ATTACH,BELTS,ID=DLK.
BELTS,#TYPE=TYPE,#IN=IN,#OUT=OUT,#NNB=NNB,#TRST=$TRST$,#UT=$UT$,
#BCH=$BCH$,#NCS=NCS,#CAH=$CAH$,#NN=NN,#NEB=NEB,#TEND=$TEND$.
REWIND,OUTPUT.
KILLJOB.
.EOR

```

```

PROGRAM IBELTT
DIMENSION T(500)
OPEN (7,FILE='INPUT')
OPEN (6,FILE='OUTPUT')
OPEN (13,FILE='TAPE13',STATUS='OLD')
OPEN (12,FILE='TAPE12')
DO 10 I = 1,1000
READ (13) TIME,(T(J), J = 1,NN)
10 IF (TIME.GT.TEND-1.0E-06.AND.TIME.LT.TEND+1.0E-06) GO TO 20
GO TO 30
20 WRITE (12) TEND+1.0E-09,(T(I), I = 1,NNB)
30 RETURN
END

```

Sub appendix C of Appendix F

BELT COMPONENT

```

.PROC,BELTS,TYPE=1,IN=A,OUT=8,NNB=33,TRST=$6.2003$,UT=$2.797E-05$,
BCH=$9.645E-04$,NCS=66,CAH=$2.894E-05$,NN=194,NEB=32,
TEND=$1.6223$,TESTIT=.
TESTIT-RETURN,JOBIN.
TESTIT-REQUEST,JOBIN,*Q.
ITE,TYPE.EQ.1.OR.TYPE.EQ.3,SKIP01.
TESTIT COPY,JDATA1,JOBIN.
ELSE,SKIP01.
TESTIT COPY,JDATA2,JOBIN.
ENDIF,SKIP01.
TESTIT-ROUTE,JOBIN,DC=#IN,TID=AH.
TESTIT-RETURN,JDATA1,JDATA2,JOBIN.
REVERT.
.DATA,JDATA1.
DLKBM,T37,P2,STANY.
ACCNT,ID=DLK,C=61ATC7001.
ATTACH,OKWLIB,ID=OKW.
LIBRARY,OKWLIB.
REQUEST,TAPE13,*PF.
ATTACH,TAPE12,BMIT_TYPE_IN,ID=DLK.
REWIND,TAPE12.
SINGLE,FICHE=0,USUB=0.
REWIND,TAPE13.
CATALOG,TAPE13,BMT_TYPE_IN,ID=DLK,RP=30.
RETURN,TAPE12,TAPE13.
PURGE,BMIT_TYPE_IN,ID=DLK.
ATTACH,CMIT,ID=DLK.
CMIT,*TYPE=TYPE,*IN=IN,*OUT=OUT,*NNB=NNB,*TRST=TRST,*UT=UT,
*BCH=BCH,*NCS=NCS,*CAH=CAH,*NN=NN.
EOR
TITLE : THIN-STRIP (304 STAINLESS STEEL) CASTING BELT
- INPUT DATA REQUIRED THROUGH BOUNDARY CONDITIONS & TEMPERATURE TAPE -
UNITS - BTU,#IN,SEC,LBM,DEG F

```

MATERIALS

```

1 1 1.0 304 STAINLESS STEEL 2
1.0 1.0 1

```

NODES

```

NNB 0
1 NEB 0.0
0.0_NEB

```

ELEMENTS - 1 MIL THICK

```

NEB
1 1 2 1 1
NEB NEB NNB 1

```

```

STEP - TIME=0 AT TOP OF MOLTEN POOL WITH TRST SEC/REVOLUTION
TEND TRST 3 1 10 1

```

```

46
1.7223 44 0.1
6.2003001

```

FUNCTIONS

```

9
1 19 2 304 SS THERMAL CONDUCTIVITY
100.0 2.024E-04 200.0 2.152E-04 300.0 2.275E-04
400.0 2.396E-04 500.0 2.512E-04 600.0 2.623E-04
700.0 2.734E-04 800.0 2.840E-04 900.0 2.944E-04
1000.0 3.049E-04 1100.0 3.148E-04 1200.0 3.248E-04
1300.0 3.347E-04 1400.0 3.444E-04 1500.0 3.544E-04
1600.0 3.641E-04 1700.0 3.739E-04 1800.0 3.838E-04
1900.0 3.939E-04
2 19 2 304 SS HEAT CAPACITANCE
100.0 3.351E-02 200.0 3.473E-02 300.0 3.574E-02

```

400.0	3.658E-02	500.0	3.726E-02	600.0	3.782E-02
700.0	3.830E-02	800.0	3.865E-02	900.0	3.898E-02
1000.0	3.927E-02	1100.0	3.957E-02	1200.0	3.988E-02
1300.0	4.023E-02	1400.0	4.062E-02	1500.0	4.112E-02
1600.0	4.170E-02	1700.0	4.241E-02	1800.0	4.329E-02
1900.0	4.431E-02				

```

3      2      1      TIME STEP DATA
6.0723      1      0.1      6.0724      0.0780001
4      8      1      CONVECTION COEFFICIENT INSIDE BELT
2.5069      3.998E-04      2.5070      2.894E-05      3.6320      2.894E-05
3.6321      3.998E-04      4.2218      3.998E-04      4.2219      2.894E-05
5.3469      2.894E-05      5.3470      3.998E-04
5      8      1      BOUNDARY TEMPERATURE INSIDE BELT
2.5069      125.0      2.5070      500.0      3.6320      500.0
3.6321      125.0      4.2218      125.0      4.2219      500.0
5.3469      500.0      5.3470      125.0
6      2      1      CONVECTION COEFFICIENT OUTSIDE BELT
5.9367      2.894E-05      5.9368      2.797E-05
7      2      1      BOUNDARY TEMPERATURE OUTSIDE BELT
5.9367      500.0      5.9368      500.0
8      8      1      RADIANT FACTOR INSIDE BELT
2.5069      0.0      2.5070      1.653E-15      3.6320      1.653E-15
3.6321      0.0      4.2218      0.0      4.2219      1.653E-15
5.3469      1.653E-15      5.3470      0.0
9      2      1      RADIANT FACTOR OUTSIDE BELT
5.9367      1.653E-15      5.9368      0.0

```

INITIAL

1

CONVECTION - NODE 1 INSIDE & NODE NNB OUTSIDE BELT

2

1	1.0	4	1.0	5
NNB	1.0	6	1.0	7

RADIATION - NODE 1 INSIDE & NODE NNB OUTSIDE BELT

2

1	460.0	8	1.0	5
NNB	1.0	9	1.0	7

SINGLE/CE

END OF DATA

.DATA, JDATA2.

DLKBM, T37, P2, STANY.

ACCNT, ID=DLK, C=61ATC7001.

ATTACH, OKWLIB, ID=OKW.

LIBRARY, OKWLIB.

REQUEST, TAPE13, *PF.

ATTACH, TAPE12, BMIT_TYPE_IN, ID=DLK.

REWIND, TAPE12.

SINGLE, FICHE=0, USUB=0.

REWIND, TAPE13.

CATALOG, TAPE13, BMT_TYPE_IN, ID=DLK, RP=30.

RETURN, TAPE12, TAPE13.

PURGE, BMIT_TYPE_IN, ID=DLK.

ATTACH, CMIT, ID=DLK.

CMIT, #TYPE=TYPE, #IN=IN, #OUT=OUT, #NNB=NNB, #TRST=\$TRST\$, #UT=\$UT\$,

#BCH=\$BCH\$, #NCS=NCS, #CAH=\$CAH\$, #NN=NN.

*EOR

TITLE : THIN-STRIP (2.25 CR, 1 MD STEEL) CASTING BELT

- INPUT DATA REQUIRED THROUGH BOUNDARY CONDITIONS & TEMPERATURE TAPE -

UNITS - BTU, #IN, SEC, LBM, DEG F

MATERIALS

1	1	1.0	2.25 CR, 1 MD STEEL	2
1.0	1	1.0	1.0	1

NODES
 NNB 0
 1 NEB 0.0
 0.0_NEB

ELEMENTS - 1 MIL THICK

NEB
 1 2 1
 NEB NEB NNB 1

STEP - TIME=0 AT TOP OF MOLTEN POOL WITH TRST SEC/REVOLUTION
 TEND TRST 3 1 10 1

46
 1.7223 44 0.1
 6.2003001

FUNCTIONS

9
 1 18 2 2.25 CR, 1 MO STEEL THERMAL CONDUCTIVITY
 100.0 4.876E-04 200.0 4.959E-04 300.0 4.991E-04
 400.0 4.983E-04 500.0 4.943E-04 600.0 4.877E-04
 700.0 4.791E-04 800.0 4.688E-04 900.0 4.572E-04
 1000.0 4.446E-04 1100.0 4.310E-04 1200.0 4.166E-04
 1292.0 4.028E-04 1400.0 3.860E-04 1500.0 3.699E-04
 1600.0 3.535E-04 1700.0 3.367E-04 1800.0 3.195E-04
 2 18 2 2.25 CR, 1 MO STEEL HEAT CAPACITANCE
 100.0 3.069E-02 200.0 3.209E-02 300.0 3.348E-02
 400.0 3.489E-02 500.0 3.635E-02 600.0 3.778E-02
 700.0 3.933E-02 800.0 4.108E-02 900.0 4.320E-02
 1000.0 4.586E-02 1100.0 4.938E-02 1200.0 5.423E-02
 1292.0 6.027E-02 1400.0 7.016E-02 1500.0 5.993E-02
 1600.0 5.364E-02 1700.0 5.349E-02 1800.0 5.333E-02
 3 2 1 TIME STEP DATA
 6.0723 0.1 6.0724 0.0780001
 4 8 1 CONVECTION COEFFICIENT INSIDE BELT
 2.5069 3.998E-04 2.5070 2.894E-05 3.6320 2.894E-05
 3.6321 3.998E-04 4.2218 3.998E-04 4.2219 2.894E-05
 5.3469 2.894E-05 5.3470 3.998E-04
 5 8 1 BOUNDARY TEMPERATURE INSIDE BELT
 2.5069 125.0 2.5070 500.0 3.6320 500.0
 3.6321 125.0 4.2218 125.0 4.2219 500.0
 5.3469 500.0 5.3470 125.0
 6 2 1 CONVECTION COEFFICIENT OUTSIDE BELT
 5.9367 2.894E-05 5.9368 2.797E-05
 7 2 1 BOUNDARY TEMPERATURE OUTSIDE BELT
 5.9367 500.0 5.9368 500.0
 8 8 1 RADIANT FACTOR INSIDE BELT
 2.5069 0.0 2.5070 1.653E-15 3.6320 1.653E-15
 3.6321 0.0 4.2218 0.0 4.2219 1.653E-15
 5.3469 1.653E-15 5.3470 0.0
 9 2 1 RADIANT FACTOR OUTSIDE BELT
 5.9367 1.653E-15 5.9368 0.0

INITIAL

1

CONVECTION - NODE 1 INSIDE & NODE NNB OUTSIDE BELT

2

1 1.0 4 1.0 5
 NNB 1.0 6 1.0 7

RADIATION - NODE 1 INSIDE & NODE NNB OUTSIDE BELT

2

1 460.0 8 1.0 5
 NNB 1.0 9 1.0 7

SINGLE/CE

END OF DATA

Sub appendix D of Appendix F

BELT-TO-CASTING COMPONENT

```

*PRUC,CMIT,TYPE=1,IN=A,OUT=B,NNB=33,TRST=$6.2003$UT=$2.797E-05$,
BCH=$9.645E-04$,NCS=66,CAH=$2.894E-05$,NN=194,TESTIT=.
TESTIT-RETURN,JOBIN,#Q.
TESTIT-COPY,JOBIN,JOBIN.
TESTIT-ROUTE,JOBIN,DC=#IN,TID=GU.
REVERT.
*DATA,JDATA.
DLKCT,137,P2,STANY.
ACCNT,137,DLK,C=61AIC7001.
REQUESH,1,TAPE12,*PF.
ATTACH,TAPE13,BMT_TYPE_IN,ID=DLK.
REWIN,ET,TAPE12,ROUND,GO.
REWIN,ET,TAPE12,ROUND,GO.
CATALOG,TAPE12,CMIT_TYPE_OUT,ID=DLK,RP=30.
RETURN,TAPE12,TAPE13.
PURGE,BMT_TYPE_IN,ID=DLK.
REWIN,ET,TAPE12,ROUND,GO.
KILLJO3.

```

```

PROGRAM ICASIT
DIMENSION T(500)
OPEN (7,FILE='INPUT')
OPEN (6,FILE='OUTPUT')
OPEN (13,FILE='TAPE13',STATUS='OLD')
OPEN (12,FILE='TAPE12')
DO 10 I=1,1000
  READ (13) TIME,(T(J),J=1,NNB)
  IF (TIME.GT.TRST-1.0E-06.AND.TIME.LT.TRST+1.0E-06) GO TO 20
  GO TO 50
20 TIME=0.0
  T(NNB+1)=T(NNB)-((T(NNB)-500.0)*UT/BCH)
  T(NCS)=((T(NNB)-500.0)*UT/CAH)+500.0
  DO 30 I=NNB+2,NCS-1
    T(I)=T(NNB+1)+((T(NCS)-T(NNB+1))*(I-NNB-1)/(NCS-NNB-1))
  DD 40 I=NCS+1,NN
  T(I)=2854.0
  WRITE (12) TIME+1.0E-09,(T(I),I=1,NN)
50 RETURN
END

```

Sub appendix E of Appendix F
HEAT LOADS

TABLE E-1. INTEGRATED HEAT LOSS RATES (IN BTU/SEC)^a

	Case 1	Case 2	Case 3
Absorbed by each casting roller	245	253	187
Absorbed by each take-off roller	183	195	139
Absorbed by each tension roller	122	130	93
Absorbed by each heat slab	0	0	961
Absorbed by the inert atmosphere	1489	1569	1033
Total	2039	2157	2413

a. Ultimately, all process heat loss is the result of energy rejection from the solidifying product during continuous (quasi-steady-state) casting. The energy rejected from the product becomes a heat gain to the components (as listed) during the process. Because the process is symmetric, all values given, including that absorbed by the inert atmosphere and the total, represent one-half of the actual process-generated heat loads.