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VOLUME

6

WELDING BRAZING AND SOLDERING



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FOREWORD

COVERAGE OF JOINING TECHNOLOGIES IN THE *ASM HANDBOOK* HAS GROWN DRAMATICALLY OVER THE YEARS. A SHORT CHAPTER ON WELDING--EQUAL IN SIZE TO ABOUT 5 PAGES OF TODAY'S *ASM HANDBOOK*--APPEARED IN THE 1933 EDITION OF THE *NATIONAL METALS HANDBOOK* PUBLISHED BY THE AMERICAN SOCIETY OF STEEL TREATERS, ASM'S PREDECESSOR. THAT MATERIAL WAS EXPANDED TO 13 PAGES IN THE CLASSIC 1948 EDITION OF *METALS HANDBOOK*. THE FIRST FULL VOLUME ON WELDING AND BRAZING IN THE SERIES APPEARED IN 1971, WITH PUBLICATION OF VOLUME 6 OF THE 8TH EDITION OF *METALS HANDBOOK*. VOLUME 6 OF THE 9TH EDITION, PUBLISHED IN 1983, WAS EXPANDED TO INCLUDE COVERAGE OF SOLDERING.

THE NEW VOLUME 6 OF THE *ASM HANDBOOK* BUILDS ON THE PROUD TRADITION ESTABLISHED BY THESE PREVIOUS VOLUMES, BUT IT ALSO REPRESENTS A BOLD NEW STEP FOR THE SERIES. THE HANDBOOK HAS NOT ONLY BEEN REVISED, BUT

REFORMATTED TO MEET THE NEEDS OF TODAY'S MATERIALS COMMUNITY. OVER 90% OF THE ARTICLES IN THIS VOLUME ARE BRAND-NEW, AND THE REMAINDER HAVE BEEN SUBSTANTIALLY REVISED. MORE SPACE HAS BEEN DEVOTED TO COVERAGE OF SOLID-STATE WELDING PROCESSES, MATERIALS SELECTION FOR JOINED ASSEMBLIES, WELDING IN SPECIAL ENVIRONMENTS, QUALITY CONTROL, AND MODELING OF JOINING PROCESSES, TO NAME BUT A FEW. INFORMATION ALSO HAS BEEN ADDED FOR THE FIRST TIME ABOUT JOINING OF SELECTED NONMETALLIC MATERIALS.

WHILE A DELIBERATE ATTEMPT HAS BEEN MADE TO INCREASE THE AMOUNT OF CUTTING-EDGE INFORMATION PROVIDED, THE ORGANIZERS HAVE WORKED HARD TO ENSURE THAT THE HEART OF THE BOOK REMAINS PRACTICAL INFORMATION ABOUT JOINING PROCESSES, APPLICATIONS, AND MATERIALS WELDABILITY--THE TYPE OF INFORMATION THAT IS THE HALLMARK OF THE *ASM HANDBOOK* SERIES.

PUTTING TOGETHER A VOLUME OF THIS MAGNITUDE IS AN ENORMOUS EFFORT AND COULD NOT HAVE BEEN ACCOMPLISHED WITHOUT THE DEDICATED AND TIRELESS EFFORTS OF THE VOLUME CHAIRPERSONS: DAVID L. OLSON, THOMAS A. SIEWERT, STEPHEN LIU, AND GLEN R. EDWARDS. SPECIAL THANKS ARE ALSO DUE TO THE SECTION CHAIRPERSONS, TO THE MEMBERS OF THE ASM HANDBOOK COMMITTEE, AND TO THE ASM EDITORIAL STAFF. WE ARE ESPECIALLY GRATEFUL TO THE OVER 400 AUTHORS AND REVIEWERS WHO HAVE CONTRIBUTED THEIR TIME AND EXPERTISE IN ORDER TO MAKE THIS HANDBOOK A TRULY OUTSTANDING INFORMATION RESOURCE.

EDWARD H. KOTTCAMP, JR.
PRESIDENT
ASM INTERNATIONAL

EDWARD L. LANGER
MANAGING DIRECTOR
ASM INTERNATIONAL

PREFACE

THE *ASM HANDBOOK*, VOLUME 6, *WELDING, BRAZING, AND SOLDERING*, HAS BEEN ORGANIZED INTO A UNIQUE FORMAT THAT WE BELIEVE WILL PROVIDE HANDBOOK USERS WITH READY ACCESS TO NEEDED MATERIALS-ORIENTED JOINING INFORMATION AT A MINIMAL LEVEL OF FRUSTRATION AND STUDY TIME. WHEN WE DEVELOPED THE ORGANIZATIONAL STRUCTURE FOR THIS VOLUME, WE RECOGNIZED THAT ENGINEERS, TECHNICIANS, RESEARCHERS, DESIGNERS, STUDENTS, AND TEACHERS DO NOT SEEK OUT JOINING INFORMATION WITH THE SAME LEVEL OF UNDERSTANDING, OR WITH THE SAME NEEDS. THEREFORE, WE ESTABLISHED DISTINCT SECTIONS THAT WERE INTENDED TO MEET THE SPECIFIC NEEDS OF PARTICULAR USERS.

THE EXPERIENCED JOINING SPECIALIST CAN TURN TO THE SECTION "CONSUMABLE SELECTION, PROCEDURE DEVELOPMENT, AND PRACTICE CONSIDERATIONS" AND FIND DETAILED JOINING MATERIALS DATA ON A WELL-DEFINED PROBLEM. THIS HANDBOOK ALSO PROVIDES GUIDANCE FOR THOSE WHO NOT ONLY MUST SPECIFY THE JOINING PRACTICE, BUT ALSO THE MATERIALS TO BE JOINED. THE SECTION "MATERIALS SELECTION FOR JOINED ASSEMBLIES" CONTAINS COMPREHENSIVE INFORMATION ABOUT THE PROPERTIES, APPLICATIONS, AND WELDABILITIES OF THE MAJOR CLASSES OF STRUCTURAL MATERIALS. TOGETHER, THESE TWO MAJOR SECTIONS OF THE HANDBOOK SHOULD PROVIDE AN ENGINEER ASSIGNED A LOOSELY DEFINED DESIGN PROBLEM WITH THE MEANS TO MAKE INTELLIGENT CHOICES FOR COMPLETING AN ASSEMBLY.

FREQUENTLY, TECHNOLOGISTS ARE CALLED UPON TO INITIATE AND ADOPT WELDING PROCESSES WITHOUT IN-DEPTH KNOWLEDGE OF THESE PROCESSES OR THE SCIENTIFIC

PRINCIPLES THAT IMPACT THE PROPERTIES AND PERFORMANCE OF WELDMENTS. THE SECTIONS "FUNDAMENTALS OF JOINING" AND "JOINING PROCESSES" ARE DESIGNED TO MEET THE NEEDS OF THESE USERS, OR ANYONE WHO NEEDS BASIC BACKGROUND INFORMATION ABOUT JOINING PROCESSES AND PRINCIPLES.

WELDING, BRAZING, AND SOLDERING ARE TRULY INTERDISCIPLINARY ENTERPRISES; NO INDIVIDUAL CAN BE EXPECTED TO BE AN EXPERT IN ALL ASPECTS OF THESE TECHNOLOGIES. THEREFORE, WE HAVE ATTEMPTED TO PROVIDE A HANDBOOK THAT CAN BE USED AS A COMPREHENSIVE REFERENCE BY ANYONE NEEDING MATERIALS-RELATED JOINING INFORMATION.

MANY COLLEAGUES AND FRIENDS CONTRIBUTED THEIR TIME AND EXPERTISE TO THIS HANDBOOK, AND WE ARE VERY GRATEFUL FOR THEIR EFFORTS. WE WOULD ALSO LIKE TO EXPRESS OUR THANKS TO THE AMERICAN WELDING SOCIETY FOR THEIR COOPERATION AND ASSISTANCE IN THIS ENDEAVOR.

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Energy Sources Used for Fusion Welding

Thomas W. Eagar, Massachusetts Institute of Technology

Introduction

WELDING AND JOINING processes are essential for the development of virtually every manufactured product. However, these processes often appear to consume greater fractions of the product cost and to create more of the production difficulties than might be expected. There are a number of reasons that explain this situation.

First, welding and joining are multifaceted, both in terms of process variations (such as fastening, adhesive bonding, soldering, brazing, arc welding, diffusion bonding, and resistance welding) and in the disciplines needed for problem solving (such as mechanics, materials science, physics, chemistry, and electronics). An engineer with unusually broad and deep training is required to bring these disciplines together and to apply them effectively to a variety of processes.

Second, welding or joining difficulties usually occur far into the manufacturing process, where the relative value of scrapped parts is high.

Third, a very large percentage of product failures occur at joints because they are usually located at the highest stress points of an assembly and are therefore the weakest parts of that assembly. Careful attention to the joining processes can produce great rewards in manufacturing economy and product reliability.

The Section "Fusion Welding Processes" in this Volume provides details about equipment and systems for the major fusion welding processes. The purpose of this Section of the Volume is to discuss the fundamentals of fusion welding processes, with an emphasis on the underlying scientific principles.

Because there are many fusion welding processes, one of the greatest difficulties for the manufacturing engineer is to determine which process will produce acceptable properties at the lowest cost. There are no simple answers. Any change in the part geometry, material, value of the end product, or size of the production run, as well as the availability of joining equipment, can influence the choice of joining method. For small lots of complex parts, fastening may be preferable to welding, whereas for long production runs, welds can be stronger and less expensive.

The perfect joint is indistinguishable from the material surrounding it. Although some processes, such as diffusion bonding, can achieve results that are very close to this ideal, they are either expensive or restricted to use with just a few materials. There is no universal process that performs adequately on all materials in all geometries. Nevertheless, virtually any material can be joined in some way, although joint properties equal to those of the bulk material cannot always be achieved.

The economics of joining a material may limit its usefulness. For example, aluminum is used extensively in aircraft manufacturing and can be joined by using adhesives or fasteners, or by welding. However, none of these processes has proven economical enough to allow the extensive replacement of steel by aluminum in the frames of automobiles. An increased use of composites in aircrafts is limited by an inability to achieve adequate joint strength.

It is essential that the manufacturing engineer work with the designer from the point of product conception to ensure that compatible materials, processes, and properties are selected for the final assembly. Often, the designer leaves the problem of joining the parts to the manufacturing engineer. This can cause an escalation in cost and a decrease in reliability. If the design has been planned carefully and the parts have been produced accurately, the joining process becomes much easier and cheaper, and both the quality and reliability of the product are enhanced.

Generally, any two solids will bond if their surfaces are brought into intimate contact. One factor that generally inhibits this contact is surface contamination. Any freshly produced surface exposed to the atmosphere will absorb oxygen, water vapor, carbon dioxide, and hydrocarbons very rapidly. If it is assumed that each molecule that hits the surface will be absorbed, then the time-pressure value to produce a monolayer of contamination is approximately $0.001 \text{ Pa} \cdot \text{s}$ ($10^{-8} \text{ atm} \cdot \text{s}$). For example, at a pressure of 1 Pa (10^{-5} atm), the contamination time is 10^{-3} s , whereas at 0.1 MPa (1 atm), it is only $10 \times 10^{-9} \text{ s}$.

In fusion welding, intimate interfacial contact is achieved by interposing a liquid of substantially similar composition as the base metal. If the surface contamination is soluble, then it is dissolved in the liquid. If it is insoluble, then it will float away from the liquid-solid interface.

Energy Sources Used for Fusion Welding

Thomas W. Eagar, Massachusetts Institute of Technology

Energy-Source Intensity

One distinguishing feature of all fusion welding processes is the intensity of the heat source used to melt the liquid. Virtually every concentrated heat source has been applied to the welding process. However, many of the characteristics of each type of heat source are determined by its intensity. For example, when considering a planar heat source diffusing into a very thick slab, the surface temperature will be a function of both the surface power density and the time.

Figure 1 shows how this temperature will vary on steel with power densities that range from 400 to 8000 W/cm^2 . At the lower value, it takes 2 min to melt the surface. If that heat source were a point on the flat surface, then the heat flow would be divergent and might not melt the steel. Rather, the solid metal would be able to conduct away the heat as fast as it was being introduced. It is generally found that heat-source power densities of approximately 1000 W/cm^2 are necessary to melt most metals.

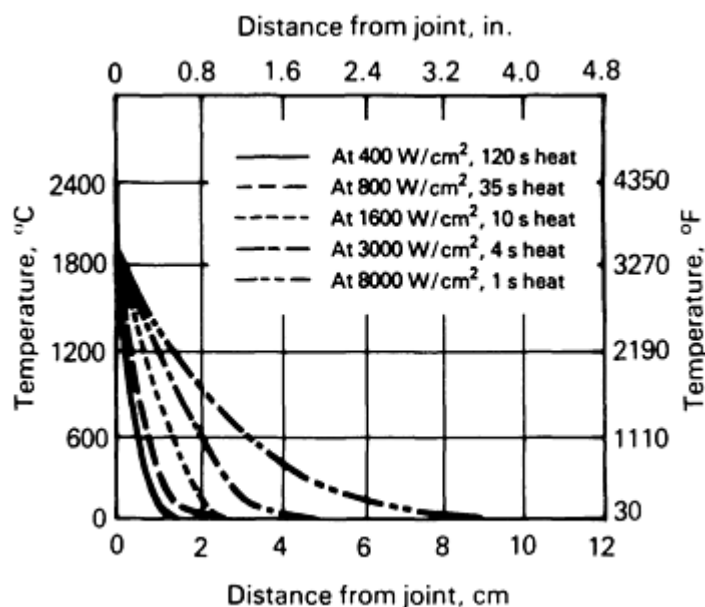


FIG. 1 TEMPERATURE DISTRIBUTION AFTER A SPECIFIC HEATING TIME IN A THICK STEEL PLATE HEATED

UNIFORMLY ON ONE SURFACE AS A FUNCTION OF APPLIED HEAT INTENSITY; INITIAL TEMPERATURE OF PLATE IS 25 °C (77 °F)

At the other end of the power-density spectrum, heat intensities of 10^6 or 10^7 W/cm² will vaporize most metals within a few microsecond. At levels above these values, all of the solid that interacts with the heat source will be vaporized, and no fusion welding can occur. Thus, the heat sources for all fusion welding processes should have power densities between approximately 0.001 and 1 MW/cm². This power-density spectrum is shown in Fig. 2, along with the points at which common joining processes are employed.

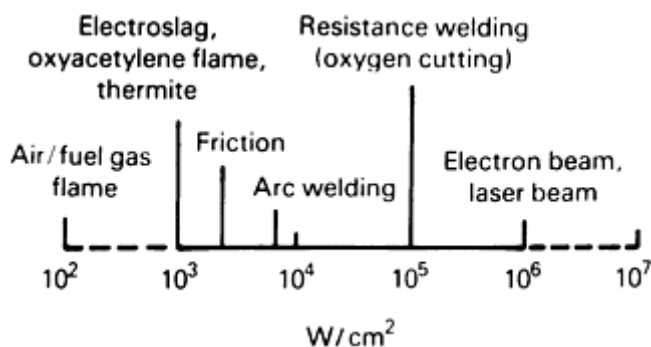


FIG. 2 SPECTRUM OF PRACTICAL HEAT INTENSITIES USED FOR FUSION WELDING

The fact that power density is inversely related to the interaction time of the heat source on the material is evident in Fig. 1. Because this represents a transient heat conduction problem, one can expect the heat to diffuse into the steel to a depth that increases as the square root of time, that is, from the Einstein equation:

$$X \sim \sqrt{\alpha t} \quad (\text{EQ 1})$$

where x is the distance that the heat diffuses into the solid, in centimeters; α is the thermal diffusivity of the solid, in cm²/s; and t is the time in seconds. Tables 1 and 2 give the thermal diffusivities of common elements and common alloys, respectively.

TABLE 1 THERMAL DIFFUSIVITIES OF COMMON ELEMENTS FROM 20 TO 100 °C (68 TO 212 °F)

ELEMENT	DENSITY		HEAT CAPACITY		THERMAL CONDUCTIVITY		mm ² /s	THERMAL DIFFUSIVITY cm ² /s
	g/cm ³	lb/in. ³	j/kg · k	cal _{it} /g · °c	w/m · k	cal _{it} /cm · s · °c		
ALUMINUM	2.699	0.098	900	0.215	221	0.53	91	0.91
ANTIMONY	6.62	0.239	205	0.049	19	0.045	14	0.14
BERYLLIUM	1.848	0.067	1880	0.45	147	0.35	42	0.42
BISMUTH	9.80	0.354	123	0.0294	8	0.020	7	0.09
CADMIUM	8.65	0.313	230	0.055	92	0.22	46	0.46
CARBON	2.25	0.081	691	0.165	24	0.057	15	0.15
COBALT	8.85	0.320	414	0.099	69	0.165	19	0.188
COPPER	8.96	0.324	385	0.092	394	0.941	114	1.14
GALLIUM	5.907	0.213	331	0.079	29-38	0.07-0.09	17	0.17
GERMANIUM	5.323	0.192	306	0.073	59	0.14	36	0.36
GOLD	19.32	0.698	131	0.0312	297	0.71	118	1.178
HAFNIUM	13.09	0.472	147	0.0351	22	0.053	12	0.12

INDIUM	7.31	0.264	239	0.057	24	0.057	14	0.137
IRIDIUM	22.5	0.813	129	0.0307	59	0.14	20	0.20
IRON	7.87	0.284	460	0.11	75	0.18	21	0.208
LEAD	11.36	0.410	129	0.0309	35	0.083	24	0.236
MAGNESIUM	1.74	0.063	1025	0.245	154	0.367	86	0.86
MOLYBDENUM	10.22	0.369	276	0.066	142	0.34	50	0.50
NICKEL	8.902	0.322	440	0.105	92	0.22	23.5	0.235
NIOBIUM	8.57	0.310	268	0.064	54	0.129	23.6	0.236
PALLADIUM	12.02	0.434	244	0.0584	70	0.168	24	0.24
PLATINUM	21.45	0.775	131	0.0314	69	0.165	24.5	0.245
PLUTONIUM	19.84	0.717	138	0.033	8	0.020	3.0	0.030
RHODIUM	12.44	0.449	247	0.059	88	0.21	29	0.286
SILICON	2.33	0.084	678	0.162	84	0.20	53	0.53
SILVER	10.49	0.379	234	0.0559	418	1.0	170	1.705
SODIUM	0.9712	0.035	1235	0.295	134	0.32	112	1.12
TANTALUM	16.6	0.600	142	0.034	54	0.130	23	0.23
TIN	7.2984	0.264	226	0.054	63	0.150	38	0.38
TITANIUM	4.507	0.163	519	0.124	22	0.052	9	0.092
TUNGSTEN	19.3	0.697	138	0.033	166	0.397	62	0.62
URANIUM	19.07	0.689	117	0.0279	30	0.071	13	0.13
VANADIUM	6.1	0.22	498	0.119	31	0.074	10	0.10
ZINC	7.133	0.258	383	0.0915	113	0.27	41	0.41
ZIRCONIUM	6.489	0.234	280	0.067	21	0.050	12	0.12

TABLE 2 THERMAL DIFFUSIVITIES OF COMMON ALLOYS FROM 20 TO 100 °C (68 TO 212 °F)

ALLOYS	DENSITY		HEAT CAPACITY		THERMAL CONDUCTIVITY		THERMAL DIFFUSIVITY	
	g/cm ³	lb/in. ³	j/kg · k	cal _{it} /g · °c	w/m · k	cal _{it} /cm · s · °c	mm ² /s	cm ² /s
ALUMINUM ALLOYS								
1100	2.71	0.098	963	0.23	222	0.53	85	0.85
2014	2.80	0.101	963	0.23	193	0.46	71	0.71
5052	2.68	0.097	963	0.23	138	0.33	54	0.54
6061	2.70	0.098	963	0.23	172	0.41	66	0.66
7075	2.80	0.101	963	0.23	121	0.29	45	0.45
COPPER ALLOYS								
COMMERCIAL BRONZE	8.80	0.318	377	0.09	188	0.45	57	0.57
CARTRIDGE BRASS	8.53	0.308	377	0.09	121	0.29	38	0.38
NAVAL BRASS	8.41	0.303	377	0.09	117	0.28	37	0.37
BERYLLIUM COPPER	8.23	0.297	419	0.1	84	0.20	24	0.24
9% ALUMINUM BRONZE	7.58	0.273	435	0.104	60	0.144	18	0.18
MAGNESIUM ALLOYS								
AZ 31	1.78	0.064	1050	0.25	84	0.20	45	0.45
AZ 91	1.83	0.066	1005	0.24	84	0.20	46	0.46
ZW 1	1.8	0.065	1005	0.24	134	0.32	74	0.74
RZ 5	1.84	0.066	963	0.23	113	0.27	64	0.64
STAINLESS STEELS								

TYPE 301	7.9	0.285	502	0.12	16	0.039	4.1	0.041
TYPE 304	7.9	0.285	502	0.12	15.1	0.036	3.8	0.038
TYPE 316	8.0	0.289	502	0.12	15.5	0.037	3.9	0.039
TYPE 410	7.7	0.278	460	0.11	24	0.057	6.7	0.067
TYPE 430	7.7	0.278	460	0.11	26	0.062	7.3	0.073
TYPE 501	7.7	0.278	460	0.11	37	0.088	10	0.10
NICKEL-BASE ALLOYS								
NIMONIC 80A	8.19	0.296	460	0.11	11	0.027	3.0	0.030
INCONEL 600	8.42	0.304	460	0.11	15	0.035	3.8	0.038
MONEL 400	8.83	0.319	419	0.10	22	0.052	5.8	0.058
TITANIUM ALLOYS								
TI-6AL-4V	4.43	0.160	611	0.146	5.9	0.014	2.1	0.021
TI-5AL-2.5SN	4.46	0.161	460	0.11	6.3	0.015	3.1	0.031

For the planar heat source on a steel surface, as represented by Fig. 1, the time in seconds to produce melting on the surface, t_m , is given by:

$$T_M = (5000/H.I.)^2 \tag{EQ 2}$$

where H.I. is the net heat intensity (in W/cm^2) transferred to the workpiece.

Equation 2 provides a rough estimate of the time required to produce melting, and is based upon the thermal diffusivity of steel. Materials with higher thermal diffusivities--or the use of a local point heat source rather than a planar heat source--will increase the time to produce melting by a factor of up to two to five times. On the other hand, thin materials tend to heat more quickly.

If the time to melting is considered to be a characteristic interaction time, t_i , then the graph shown in Fig. 3 can be generated. Heat sources with power densities that are of the order of $1000 W/cm^2$, such as oxyacetylene flames or electroslag welding, require interaction times of 25 s with steel, whereas laser and electron beams, at $1 MW/cm^2$, need interaction times on the order of only 25 μs . If this interaction time is divided into the heat-source diameter, d_H , then a maximum travel speed, V_{max} , is obtained for the welding process (Fig. 4).

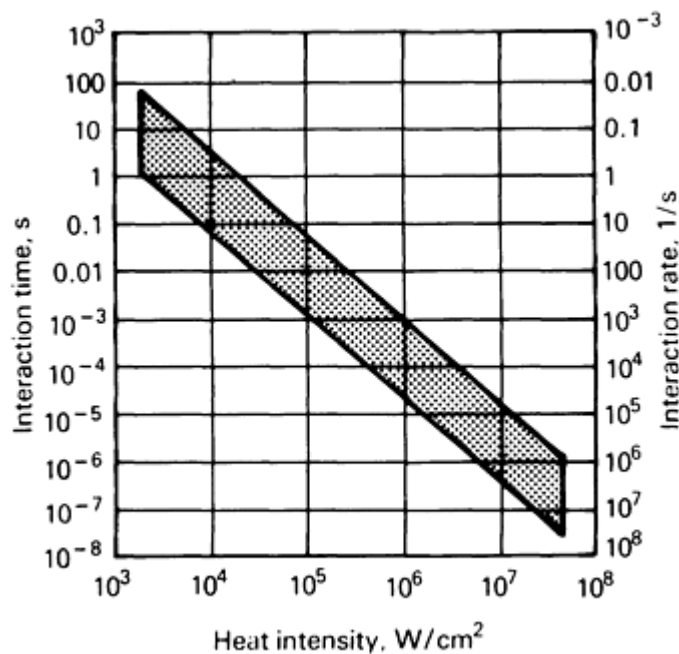


FIG. 3 TYPICAL WELD POOL-HEAT SOURCE INTERACTION TIMES AS FUNCTION OF HEAT-SOURCE INTENSITY.

MATERIALS WITH A HIGH THERMAL DIFFUSIVITY, SUCH AS COPPER OR ALUMINUM, WOULD LIE NEAR THE TOP OF THIS BAND, WHEREAS STEELS, NICKEL ALLOYS, OR TITANIUM WOULD LIE IN THE MIDDLE. URANIUM AND CERAMICS, WITH VERY LOW THERMAL DIFFUSIVITIES, WOULD LIE NEAR THE BOTTOM OF THE BAND.

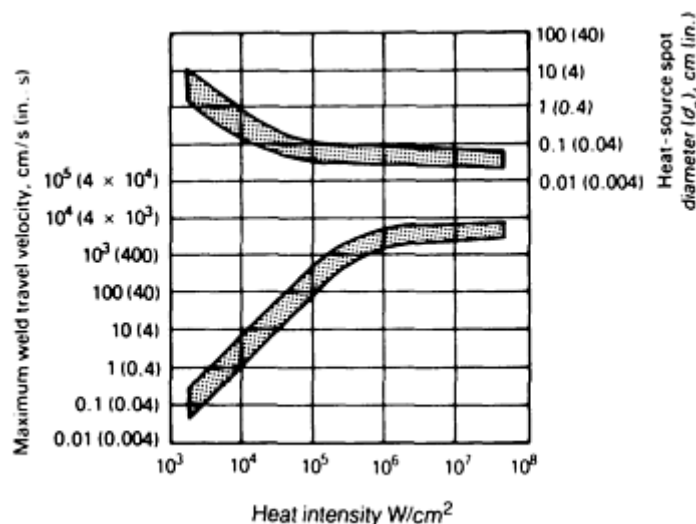


FIG. 4 MAXIMUM WELD TRAVEL VELOCITY AS A FUNCTION OF HEAT-SOURCE INTENSITY BASED ON TYPICAL HEAT-SOURCE SPOT DIAMETERS

The reason why welders begin their training with the oxyacetylene process should be clear: it is inherently slow and does not require rapid response time in order to control the size of the weld puddle. Greater skill is needed to control the more-rapid fluctuations in arc processes. The weld pool created by the high-heat-intensity processes, such as laser-beam and electron-beam welding, cannot be humanly controlled and must therefore be automated. This need to automate leads to increased capital costs. On an approximate basis, the W/cm^2 of a process can be substituted with the dollar cost of the capital equipment. With reference to Fig. 2, the cost of oxyacetylene welding equipment is nearly \$1000, whereas a fully automated laser-beam or electron-beam system can cost \$1 million. Note that the capital cost includes only the energy source, control system, fixturing, and materials handling equipment. It does not include operating maintenance or inspection costs, which can vary widely depending on the specific application.

For constant total power, a decrease in the spot size will produce a squared increase in the heat intensity. This is one of the reasons why the spot size decreases with increasing heat intensity (Fig. 4). It is easier to make the spot smaller than it is to increase the power rating of the equipment. In addition, only a small volume of material usually needs to be melted. If the spot size were kept constant and the input power were squared in order to obtain higher densities, then the volume of fused metal would increase dramatically, with no beneficial effect.

However, a decreasing spot size, coupled with a decreased interaction time at higher power densities, compounds the problem of controlling the higher-heat-intensity process. A shorter interaction time means that the sensors and controllers necessary for automation must operate at higher frequencies. The smaller spot size means that the positioning of the heat source must be even more precise, that is, on the order of the heat-source diameter, d_H . The control frequency must be greater than the travel velocity divided by the diameter of the heat source. For processes that operate near the maximum travel velocity, this is the inverse of the process interaction time, t_i (Fig. 3).

Thus, not only must the high-heat-intensity processes be automated because of an inherently high travel speed, but the fixturing requirements become greater, and the control systems and sensors must have ever-higher frequency responses. These factors lead to increased costs, which is one reason that the very productive laser-beam and electron-beam welding processes have not found wider use. The approximate productivity of selected welding processes, expressed as length of weld produced per second, to the relative capital cost of equipment is shown in Fig. 5.

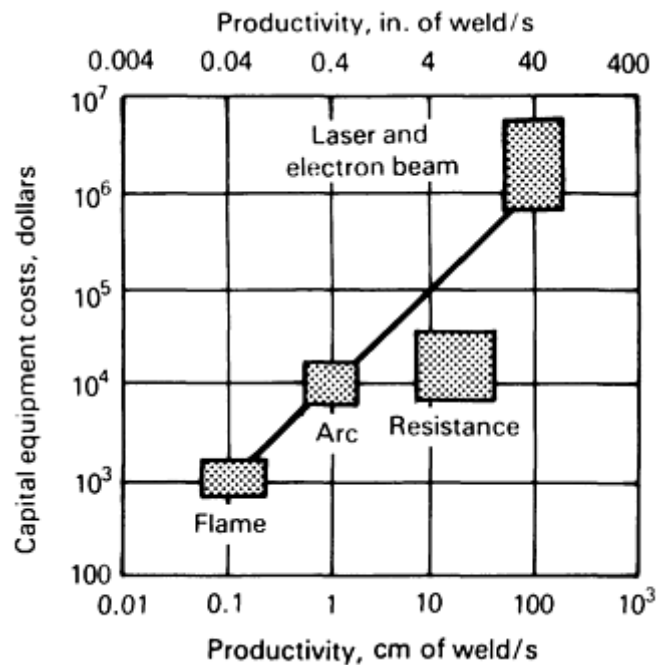


FIG. 5 APPROXIMATE RELATIONSHIP BETWEEN CAPITAL COST OF WELDING EQUIPMENT AND SPEED AT WHICH SHEET METAL JOINTS CAN BE PRODUCED

Another important welding process parameter that is related to the power density of the heat source is the width of the heat-affected zone (HAZ). This zone is adjacent to the weld metal and is not melted itself but is structurally changed because of the heat of welding. Using the Einstein equation, the HAZ width can be estimated from the process interaction time and the thermal diffusivity of the material. This is shown in Fig. 6, with one slight modification. At levels above approximately 10^4 W/cm^2 , the HAZ width becomes roughly constant. This is due to the fact that the HAZ grows during the heating stage at power densities that are below 10^4 W/cm^2 , but at higher power densities it grows during the cooling cycle. Thus, at low power densities, the HAZ width is controlled by the interaction time, whereas at high power densities, it is independent of the heat-source interaction time. In the latter case, the HAZ width grows during the cooling cycle as the heat of fusion is removed from the weld metal, and is proportional to the fusion zone width.

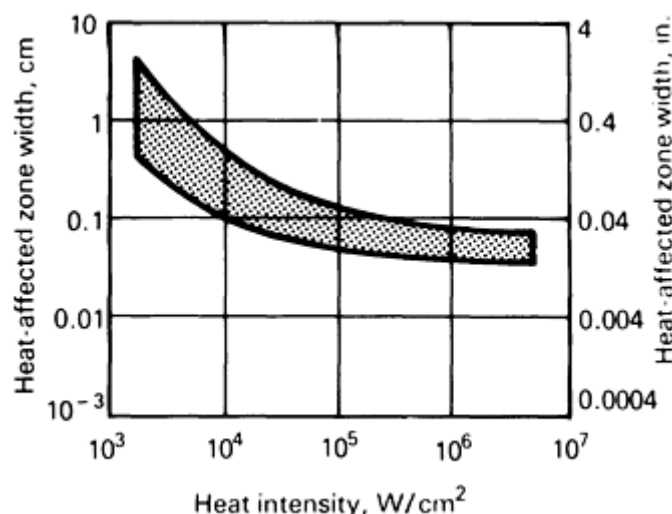


FIG. 6 RANGE OF WELD HAZ WIDTHS AS FUNCTION OF HEAT-SOURCE INTENSITY

The change of slope in Fig. 6 also represents the heat intensity at which the heat utilization efficiency of the process changes. At high heat intensities, nearly all of the heat is used to melt the material and little is wasted in preheating the surroundings. As heat intensity decreases, this efficiency is reduced. For arc welding, as little as half of the heat generated may enter the plate, and only 40% of this heat is used to fuse the metal. For oxyacetylene welding, the heat entering the metal may be 10% or less of the total heat, and the heat necessary to fuse the metal may be less than 2% of the total heat.

A final point is that the heat intensity also controls the depth-to-width ratio of the molten pool. This value can vary from 0.1 in low-heat-intensity processes to more than 10 in high-heat-intensity processes.

It should now be evident that all fusion welding processes can be characterized generally by heat-source intensity. The properties of any new heat source can be estimated readily from the figures in this article. Nonetheless, it is useful to more fully understand each of the common welding heat sources, such as flames, arcs, laser beams, electron beams, and electrical resistance. These are described in separate articles in the Section "Fusion Welding Processes" in this Volume.

Heat Flow in Fusion Welding

Chon L. Tsai and Chin M. Tso, The Ohio State University

Introduction

DURING FUSION WELDING, the thermal cycles produced by the moving heat source cause physical state changes, metallurgical phase transformation, and transient thermal stress and metal movement. After welding is completed, the finished product may contain physical discontinuities that are due to excessively rapid solidification, or adverse microstructures that are due to inappropriate cooling, or residual stress and distortion that are due to the existence of incompatible plastic strains.

In order to analyze these problems, this article presents an analysis of welding heat flow, focusing on the heat flow in the fusion welding process. The primary objective of welding heat flow modeling is to provide a mathematical tool for thermal data analysis, design iterations, or the systematic investigation of the thermal characteristics of any welding parameters. Exact comparisons with experimental measurements may not be feasible, unless some calibration through the experimental verification procedure is conducted.

Welding Thermal Process. A physical model of the welding system is shown in Fig. 1. The welding heat source moves at a constant speed along a straight path. The end result, after either initiating or terminating the heat source, is the formation of a transient thermal state in the weldment. At some point after heat-source initiation but before termination, the temperature distribution is stationary, or in thermal equilibrium, with respect to the moving coordinates. The origin of the moving coordinates coincides with the center of the heat source. The intense welding heat melts the metal and forms a molten pool. Some of the heat is conducted into the base metal and some is lost from either the arc column or the metal surface to the environment surrounding the plate. Three metallurgical zones are formed in the plate upon completion of the thermal cycle: the weld-metal (WM) zone, the heated-affected zone (HAZ), and the base-metal (BM) zone. The peak temperature and the subsequent cooling rates determine the HAZ structures, whereas the thermal gradients, the solidification rates, and the cooling rates at the liquid-solid pool boundary determine the solidification structure of the WM zone. The size and flow direction of the pool determines the amount of dilution and weld penetration. The material response in the temperature range near melting temperatures is primarily responsible for the metallurgical changes.

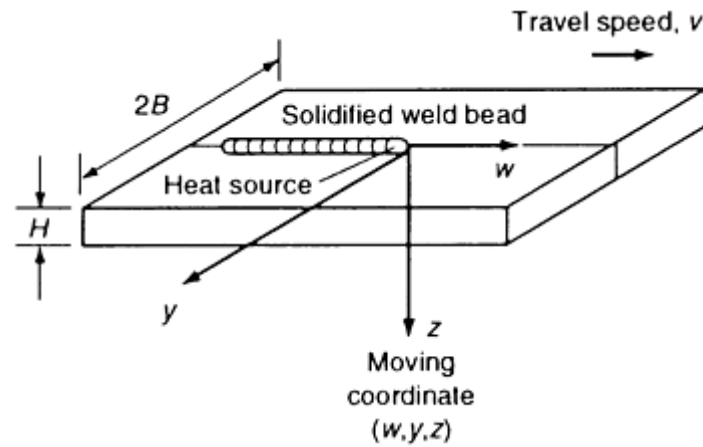


FIG. 1 SCHEMATIC OF THE WELDING THERMAL MODEL

Two thermal states, quasi-stationary and transient, are associated with the welding process. The transient thermal response occurs during the source initiation and termination stages of welding, the latter of which is of greater metallurgical interest. Hot cracking usually begins in the transient zone, because of the nonequilibrium solidification of the base material. A crack that forms in the source-initiation stage may propagate along the weld if the solidification strains sufficiently multiply in the wake of the welding heat source. During source termination, the weld pool solidifies several times faster than the weld metal in the quasi-stationary state. Cracks usually appear in the weld crater and may propagate along the weld. Another dominant transient phenomenon occurs when a short repair weld is made to a weldment. Rapid cooling results in a brittle HAZ structure and either causes cracking problems or creates a site for fatigue-crack initiation.

The quasi-stationary thermal state represents a steady thermal response of the weldment in respect to the moving heat source. The majority of the thermal expansion and shrinkage in the base material occurs during the quasi-stationary thermal cycles. Residual stress and weld distortion are the thermal stress and strain that remain in the weldment after completion of the thermal cycle.

Relation to Welding Engineering Problems. To model and analyze the thermal process, an understanding of thermally induced welding problems is important. A simplified modeling scheme, with adequate assumptions for specific problems, is possible for practical applications without using complex mathematical manipulations. The relationship between the thermal behavior of weldments and the metallurgy, control, and distortion associated with welding is summarized below.

Welding Metallurgy. As already noted, defective metallurgical structures in the HAZ and cracking in the WM usually occur under the transient thermal condition. Therefore, a transient thermal model is needed to analyze cracking and embrittlement problems.

To evaluate the various welding conditions for process qualification, the quasi-stationary thermal responses of the weld material need to be analyzed. The minimum required amount of welding heat input within the allowable welding speed range must be determined in order to avoid rapid solidification and cooling of the weldment. Preheating may be necessary if the proper thermal conditions cannot be obtained under the specified welding procedure. A quasi-stationary thermal model is adequate for this type of analysis.

Hot cracking results from the combined effects of strain and metallurgy. The strain effect results from weld-metal displacement at near-melting temperatures, because of solidification shrinkage and weldment restraint. The metallurgical effect relates to the segregation of alloying elements and the formation of the eutectic during the high nonequilibrium solidification process. Using metallurgical theories, it is possible to determine the chemical segregation, the amounts and distributions of the eutectic, the magnitudes and directions of grain growth, and the weld-metal displacement at high temperatures. Using the heating and cooling rates, as well as the retention period predicted by modeling and analysis, hot-cracking tendencies can be determined. To analyze these tendencies, it is important to employ a more accurate numerical model that considers finite welding heat distribution, latent heat, and surface heat loss.

Welding Control. In-process welding control has been studied recently. Many of the investigations are aimed at developing sensing and control hardware. However, a link between weld-pool geometry and weld quality has not been fully established. A transient heat-flow analysis needs to be used to correlate the melted surface, which is considered to be the primary control variable, to the weld thermal response in a time domain.

Welding Distortion The temperature history and distortion caused by the welding thermal process creates nonlinear thermal strains in the weldment. Thermal stresses are induced if any incompatible strains exist in the weld. Plastic strains are formed when the thermal stresses are higher than the material yield stress. Incompatible plastic strains accumulate over the thermal process and result in residual stress and distortion of the final weldment. The material response in the lower temperature range during the cooling cycle is responsible for the residual stresses and weldment distortion. For this type of analysis, the temperature field away from the welding heat source is needed for the modeling of the heating and cooling cycle during and after welding. A quasi-stationary thermal model with a concentrated moving heat source can predict, with reasonable accuracy, the temperature information for the subsequent stress and distortion analysis.

Literature Review. Many investigators have analytically, numerically, and experimentally studied welding heat-flow modeling and analysis (Ref 1, 2, 3, 4, 5, 6, 7, 8, 9, 10, 11, 12, 13, 14, 15, 16, 17, 18). The majority of the studies were concerned with the quasi-stationary thermal state. Lance and Martin (Ref 1), Rosenthal and Schmerber (Ref 2) and Rykalin (Ref 3) independently obtained an analytical temperature solution for the quasi-stationary state using a point or line heat source moving along a straight line on a semi-infinite body. A solution for plates of finite thickness was later obtained by many investigators using the imaged heat source method (Ref 3, 4). Tsai (Ref 5) developed an analytical solution for a model that incorporated a welding heat source with a skewed Gaussian distribution and finite plate thickness. It was later called the "finite source theory" (Ref 6).

With the advancement of computer technology and the development of numerical techniques like the finite-difference and finite-element methods, more exact welding thermal models were studied and additional phenomena were considered, including nonlinear thermal properties, finite heat-source distributions, latent heat, and various joint geometries. Tsai (Ref 5), Pavelic (Ref 7), Kou (Ref 8), Kogan (Ref 9), and Brody (Ref 10) studied the simulation of the welding process using the finite-difference scheme. Hibbitt and Marcal (Ref 11), Friedman (Ref 12), and Paley (Ref 13) made some progress in welding simulation using the finite-element method.

Analytical solutions for transient welding heat flow in a plate were first studied by Naka (Ref 14), Rykalin (Ref 3), and Masubuchi and Kusuda (Ref 15) in the 1940s and 1950s. A point or line heat source, constant thermal properties, and adiabatic boundary conditions were assumed. Later, Tsai (Ref 16) extended the analytical solution to incorporate Gaussian heat distribution using the principle of superposition. The solution was used to investigate the effect of pulsed conditions on weld-pool formation and solidification without the consideration of latent heat and nonlinear thermal properties.

The analysis of the transient thermal behavior of weldments using numerical methods has been the focus of several investigations since 1980. Friedman (Ref 17) discussed the finite-element approach to the general transient thermal analysis of the welding process. Brody (Ref 10) developed a two-dimensional transient heat flow model using a finite-difference scheme and a simulated pulsed-current gas-tungsten arc welding process (GTAW). Tsai and Fan (Ref 18) modeled the two-dimensional transient welding heat flow using a finite-element scheme to study the transient welding thermal behavior of the weldment.

General Approach. The various modeling and analysis schemes summarized above can be used to investigate the thermal process of different welding applications. With adequate assumptions, analytical solutions for the simplified model can be used to analyze welding problems that show a linear response to the heat source if the solutions are properly calibrated by experimental tests. Numerical solutions that incorporate nonlinear thermal characteristics of weldments are usually required for investigating the weld-pool growth or solidification behavior. Numerical solutions can also be necessary for metallurgical studies in the weld HAZ if the rapid cooling phenomenon is significant under an adverse welding environment, such as welding under water.

Thermally related welding problems can be categorized as:

- SOLIDIFICATION RATES IN THE WELD POOL
- COOLING RATES IN THE HAZ AND ITS VICINITY
- THERMAL STRAINS IN THE GENERAL DOMAIN OF THE WELDMENT

The domain of concern in the weld pool solidification is within the molten pool area, in which the arc (or other heat source) phenomena and the liquid stirring effect are significant. A convective heat-transfer model with a moving boundary at the melting temperature is needed to study the first category, and numerical schemes are usually required, as well.

The HAZ is always bounded on one side by the liquid-solid interface during welding. This inner-boundary condition is the solidus temperature of the material. The liquid weld pool might be eliminated from thermal modeling if the interface could be identified. A conduction heat-transfer model would be sufficient for the analysis of the HAZ. Numerical methods are often employed and very accurate results can be obtained.

The thermal strains caused by welding thermal cycles are caused by the nonlinear temperature distribution in the general domain of the weldment. Because the temperature in the material near the welding heat source is high, very little stress can be accumulated from the thermal strains. This is due to low rigidity, that is, small modulus of elasticity and low yield strength. The domain for thermal strain study is less sensitive to the arc and fluid-flow phenomena and needs only a relatively simple thermal model. Analytical solutions with minor manipulations often provide satisfactory results.

In this article, only the analytical heat-flow solutions and their practical applications are addressed. The numerical conduction solutions and the convective models for fluid flow in a molten weld pool are not presented.

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Chon L. Tsai and Chin M. Tso, The Ohio State University

Mathematical Formulations

Conduction Equation. A diagram of the welding thermal model is shown in Fig. 1. The origin of the moving coordinates (w, x, z) is fixed at the center of the welding heat source. The coordinates move with the source at the same speed. The conduction equation for heat flow in the weldments is:

$$\nabla \cdot (l \nabla q) + r C_p v \frac{\partial q}{\partial w} + \dot{Q} = r C_p v \frac{\partial q}{\partial t} \quad (\text{EQ 1})$$

The initial condition is:

$$\theta = \theta_0, \text{ AT } T = 0 \quad (\text{EQ 2})$$

and the general boundary condition is:

$$l \frac{\partial q}{\partial w} I_w + \frac{\partial q}{\partial y} I_y + \frac{\partial q}{\partial z} I_z - \dot{q} + h(q - q_\infty) = 0 \quad (\text{EQ 3})$$

where ∇ is a differential operator; θ is the temperature; θ_∞ is the environmental temperature; θ_0 is the initial temperature; λ is thermal conductivity; ρ is density; C_p is specific heat h is the surface heat-loss coefficient; l_w , l_y , and l_z are the direction cosines of the boundary surface; Q is the volumetric heat source, t is time, and v is welding speed.

The volumetric heat source represents the Joule heating in the weldment that is due to the electric current flow within that conducting medium. The total energy of such heating in welding is usually minimal, compared to the arc heat input. The majority of the energy is concentrated in a very small volume beneath the arc (Ref 5). In other words, a very high energy density generation exists in the weld pool, and it may have a significant effect on transient pool growth and solidification.

Heat-Source Formulation. The direction cosines on the surface that receive the heat flux from the welding source ($z = 0$) are $l_w = l_y = 0$ and $l_z = -1$. Within the significant heat-input area (to be defined later in this section), the heat loss coefficient, h , is zero.

The distribution of the welding heat flux on the weldment surface can be characterized, in a general form, by a skewed Gaussian function (Ref 19):

$$\dot{q}(r, w) = \dot{q}_0 \exp\left(-Cr^2 - \frac{bv}{2k}w\right) \quad (\text{EQ 4})$$

where β is a weight constant, κ is the thermal diffusivity of the base material, C is a shape constant, $\dot{q}$ is heat flux as a function of (r, w) , $\dot{q}_0$ is heat flux at the source center, r is the radial coordinate from the source center, and v is welding speed.

The weight constant, β , indicates the significance of the welding travel speed. A normal distribution of the welding heat flux is obtained if the weight constant is zero.

In general, the total energy input to the weldment, which is a fraction of the total welding power generated by the welding machine, is the sum of the concentrated heat and the diffused heat (Ref 20). The concentrated heat is carried by the core of the energy transmission medium, for example, the arc plasma column. The diffused heat reaches the weld surface by radiation and convection energy transport from the core surface. The heat-flux distribution is a function of the proportional values between these two types of energy. The fraction of the total welding power reaching the weldment indicates the heating efficiency of the welding process, and the fraction percentage is defined as welding heat efficiency, η .

The shape constant, C , can be obtained in terms of the core diameter, D , and the concentration factor, F . The concentration factor is defined as the ratio of the concentrated heat to the net energy reaching the weldment. The core diameter can be assumed to be the diameter of the plasma column in the arc welding process. The concentration factor and welding heat efficiency are not fully understood and have been subjected to manipulation during the mathematical analyses in order to obtain a better correlation with the experimental data.

Assuming a normal heat-flux model, two concentrations are required to determine the shape constant and the heat flux at the source center, $\dot{q}_0$. By integrating Eq 4 over the core heat area and the entire heat input domain ($r = 0 \rightarrow \infty$), the shape factor can be determined by dividing the two integrals. The heat flux at the source center can then be determined from the second integral. The two constants are expressed as:

$$C = \frac{4l_{arc}[1/(1-F)]}{D^2} \quad (\text{EQ 5})$$

where l_{arc} is the length of the arc plasma column.

$$\frac{\dot{q}_0}{\dot{Q}} = \frac{C}{p} \quad (\text{EQ 6})$$

In the case of arc welding,

$$\frac{\dot{q}_0}{hEI} = \frac{C}{p} \quad (\text{EQ 7})$$

where E is the welding arc voltage and I is the welding current.

For practical purposes, the welding heat source can be considered to be restricted within a circle of radius r_a , where the heat flux drops to 1/100 of the center flux $\dot{q}_0$. The radius of the significant heat input area can be written as:

$$r_a = \left(\frac{I_{arc} 100}{C} \right)^{0.5} \quad (\text{EQ 8})$$

Surface Heat Loss. The heat-loss coefficient, h , represents both radiation and convection heat loss from the boundary surfaces outside the significant heat input area. The formulation for both heat-loss mechanisms can be written as the radiation heat-loss coefficient (in air):

$$H_{\text{RAD}} = \varepsilon \sigma (\theta_w + \infty) (\theta_w^2 - \theta_\infty^2) \quad (\text{EQ 9})$$

or the natural convection heat-loss coefficient (in air):

$$H_{\text{AC}} = 0.00042 \frac{q_w - q_\infty}{B} \quad (\text{EQ 10})$$

or the convection heat-loss coefficient (in water):

$$H_{\text{CW}} = 0.442 (\theta_w - \theta_\infty)^{0.25} \quad (\text{EQ 11})$$

where ε is emissivity, σ is the Stefan-Boltzmann constant, θ_w is the surface temperature, θ_∞ , is the environmental temperature, and B is the characteristic surface dimension.

Natural convection is dominant at a temperature below 550 °C (1020 °F), whereas radiation becomes more important at temperatures above this level. The total heat-loss coefficient is the sum of Eq 9 and 10. The characteristic surface dimension is the effective distance from the source beyond which the temperature rises insignificantly during welding. The characteristic dimension for steel is about 150 mm (6 in.) (Ref 5). In underwater welding, heat losses are primarily due to heat transfer from the surface to the moving water environment. This motion is created by the rising gas column in the arc area (Ref 21).

For an insulated surface, no heat transfer into or out of the surface is assumed. The temperature gradient normal to the surface is zero, and can be represented by:

$$N \cdot \nabla q = 0 \quad (\text{EQ 12})$$

where n is a unit vector normal to the surface and equals $(l_w^2 + l_y^2 + l_z^2)^{0.5}$.

Other Boundary Conditions. There are several other possible boundary conditions in welding heat-flow modeling that depend on the assumptions used for model simplification. One is the condition at infinity:

$$q = q_\infty \text{ or } \lim_{r \rightarrow \infty} \frac{\partial q}{\partial r} = 0 \quad (\text{EQ 13})$$

Another is the condition near the heat source. In the case of a line source for a thin plate:

$$-2p l H \lim_{r \rightarrow \infty} r \frac{\partial q}{\partial r} = h EI \quad (\text{EQ 14})$$

In the case of a point source for a thick plate:

$$-2p l \lim_{r \rightarrow \infty} r^2 \frac{\partial q}{\partial r} = h EI \quad (\text{EQ 15})$$

In the case of a finite source for a thick plate:

$$N \cdot (-\lambda \nabla \theta) = \dot{q}; R \leq R_A \quad (\text{EQ 16})$$

Another is represented by the conditions at the solid-liquid interface:

$$\theta_1 = \theta_s = \theta_m \quad (\text{EQ 17})$$

$$n \cdot (l \nabla q)_s - n \cdot (l \nabla q)_l = \pm r_s L \frac{ds}{dt} \quad (\text{EQ 18})$$

where + indicates the melting process and - indicates the solidification process. The subscripts s and l indicate the temperature and the properties in a solid and liquid, respectively. The n is a normal vector on the boundary surface or interface, r_a is the radius of the heat-input area, L is the latent heat of the base material, and the subscript m represents the melting temperature of the base material.

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Heat Flow in Fusion Welding

Chon L. Tsai and Chin M. Tso, The Ohio State University

Engineering Solutions and Empirical Correlation

General Solutions. The general (analytical) heat-flow solutions for fusion welding can be categorized by those appropriate for a thick plate, a thin plate, or a plate with finite thickness. In most cases, the boundary surfaces (except for the heat-input area) are assumed to be adiabatic, and the thermal properties are independent of temperature. The various metallurgical zones in the weldment are assumed to be homogeneous, and the thermal model is linear.

The solutions give the temperature for a specific point if the welding velocity, v , voltage, E , and current, I , as well as the physical properties of the plate material (ρ , λ , C_p) and the welding heat efficiency, η , are known. This specific point is defined by r and w in:

$$r = \sqrt{w^2 + y^2 + z^2} \quad (\text{EQ 19})$$

where $w = x - vt$. The heat-flow solutions are not accurate at points near the welding arc, because a point source or line source is assumed for thick and thin plates, respectively.

To approximate the transient temperature changes at the start and end of a weld, Fig. 2 shows a global coordinate system (x, y, z), the origin of which is fixed at the source initiation, where t_0 is the welding time and t_1 is the time after the welding heat-source termination. The temperature solutions at t_0 and t_1 are the temperature changes at the start and end of the weld, respectively.

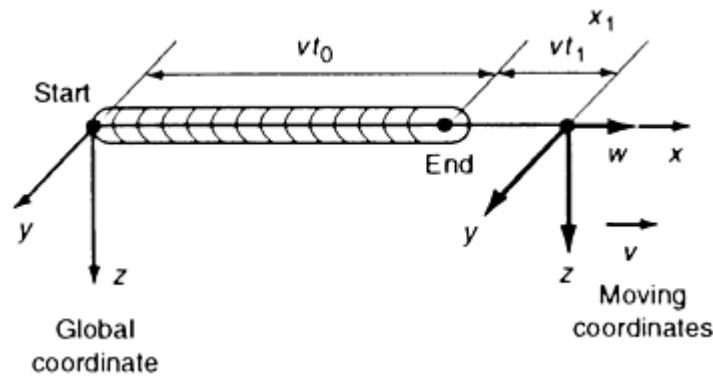


FIG. 2 GLOBAL AND MOVING COORDINATE SYSTEMS FOR WELDING HEAT CONDUCTION.

The temperature solution for thick plate at the arc start location is:

$$q - q_0 = \frac{hEI}{2plvt_0} \quad (\text{EQ 20})$$

The quasi-stationary temperature distribution is:

$$q - q_0 = \frac{hEI}{2plr} \exp\left[\frac{-v(w+r)}{2k}\right] \quad (\text{EQ 21})$$

At the arc termination location, the solution is:

$$q - q_0 = \frac{hEI}{2plvt_1} \quad (\text{EQ 22})$$

The temperature solution for thin plate at the arc start location is:

$$q - q_0 = \frac{hEI}{2plH} \exp\left[\frac{v^2t_0}{2k} K_0\left(\frac{v^2t_0}{2k}\right)\right] \quad (\text{EQ 23})$$

The quasi-stationary temperature distribution is:

$$q - q_0 = \frac{hEI}{2plH} \exp\left[\frac{-vw}{2k} K_0\left(\frac{vr}{2k}\right)\right] \quad (\text{EQ 24})$$

For the arc termination location, the solution is:

$$q - q_0 = \frac{hEI}{2plH} \exp\left[\frac{v^2t_1}{2k} K_0\left(\frac{v^2t_1}{2k}\right)\right] \quad (\text{EQ 25})$$

where K_0 is the modified Bessel function of the second kind of zeroth order and ηEI is the welding heat input rate.

Temperature for Plate With Finite Thickness. The image method enables the investigator to superimpose the solutions for an infinitely thick plate, the source of which is placed on imaginary surfaces until the proper boundary

conditions on the plate surfaces are obtained. This method is based on the premise that if a solution satisfies the governing equation and the boundary conditions, then it must be not only a correct solution, but the only solution (that is, the uniqueness of solution premise).

Using the image method, the solution for plates of finite thickness with a adiabatic surfaces can be modified from the respective temperature solutions described previously.

Let $f_0(w, y, z, t)$ be the initial solution for an infinitely thick plate. The temperature solution for a finite thick plate can be obtained by super imposing the imaginary solutions $f_{mn}(w, y_m, z_n, t)$ and $f'_{mn}(w, y'_m, z'_n, t)$ to the initial solution, and this can be written in a general form as:

$$q - q_0 = -f_0(w, y, z, t) + \sum_{m=0}^{\infty} \sum_{n=0}^{\infty} [f_{mn}(w, y_m, z_n, t) + f'_{mn}(w, y'_m, z'_n, t)] \quad (\text{EQ 26})$$

where $y_m = 2mB - y$; $y'_m = 2mB + y$; $z_n = 2nH - z$; and $z'_n = 2nH + z$, in which B is the half width and H is the thickness of the plate. The subscripts m and n are integers that vary from zero to infinity.

For a plate with sufficient width, the subscript m is zero. The solution will coverage and reach the correct adiabatic surface condition in six to ten superposition steps, depending on the thickness of the plate. The two-dimensional solution (that is, thin plate) is generally used for any solution that requires more than ten superposition steps.

Equation 26 can be expressed as:

$$q - q_0 = -\frac{hEI \exp\left(\frac{-vw}{2k}\right)}{2pl} \left\{ \frac{\exp\left(\frac{-vr}{2k}\right)}{r} + \sum_{n=1}^{\infty} \left[\frac{\exp\left(\frac{-vr_{2n}}{2k}\right)}{r_{2n}} + \frac{\exp\left(\frac{-vr_{2n+1}}{2k}\right)}{r_{2n+1}} \right] \right\} \quad (\text{EQ 27})$$

where

$$\begin{aligned} R_{2N} &= W^2 + Y^2 + (2NH - Z)^2 \text{ AND} \\ R_{2N+1} &= W^2 + Y^2 + (2NH + Z)^2 \end{aligned} \quad (\text{EQ 28})$$

Cooling Rate. Frequently, it is desirable to know the cooling rate experienced at some location in a weldment to enable a prediction of the metallurgical structure in that area. A general methodology by which cooling rate equations are obtained from the temperature-distribution equations is discussed below.

Recall that the moving coordinate w is defined by $w = x - vt$. Using this definition, it is easily shown that:

$$\frac{\partial w}{\partial t} = -v \quad (\text{EQ 29})$$

Using the chain rule, the cooling-rate equation is:

$$\frac{\partial q}{\partial t} = -v \frac{\partial q}{\partial w} \quad (\text{EQ 30})$$

Because the temperature-distribution equations are a function of w and r , the cooling-rate equations can be obtained by differentiating the temperature-distribution equations with respect to w and multiplying by $-v$.

The cooling rate is defined as the slope of a tangent line drawn on the temperature-time curve. Because the cooling rate changes with temperature, when one speaks of a cooling rate, the specific temperature, θ_c , at which it occurs must also be

given. In a weldment, the variable of interest is the cooling rate at the critical temperature that ultimately defines what type of metallurgical structure will result (if the material is heat treatable). For steels, this critical temperature is the "nose" of the continuous cooling-transformation (CCT) curve. At this temperature, the cooling rate determines if upper transformation products (pearlite, upper bainite) or lower transformation products (martensite, lower bainite) will form. For many steels, this critical temperature ranges from approximately 200 to 540 °C (400 to 1000 °F).

The cooling rate in a weldment is also a function of location. In order to find a cooling-rate equation, the particular location in the weldment that is of interest must be defined. The resulting cooling-rate equation will be applicable only to that location.

The differentiation, $\partial \theta / \partial w$, of either Eq 21 or 27, which is required to obtain the cooling-rate expression, will result in a function of w and r . The variable r can be written in terms of w if the location of interest is defined by a given set of values of y and z . This relationship for r , once formulated, can then be substituted into $\partial \theta / \partial w$, the result being a function of w alone.

To determine w corresponding to the critical temperature, θ_c , a temperature-distribution equation is required (Eq 21 or 27). The aforementioned r - w relationship and temperature distribution equation (Eq 21 or 27) where θ is equal to θ_c , critical temperature, are used to determine w . Then w is substituted into the $d\theta/dw$ expression obtained previously. The end result will be an equation that defines the cooling rate for a particular location in the weldment, and, being a function of the critical temperature, the welding conditions and thermal conductivity of the base material.

To determine the cooling rate in a thick plate along the weld centerline (that is $y = 0$) for a particular critical temperature, the cooling-rate equation can be reduced to:

$$\frac{\partial q}{\partial t} = \frac{2pIv(q_c - q_0)^2}{hEI} \quad (\text{EQ 31})$$

This equation has been used to predict weld cooling rates in shop practices. Cooling rate is inversely (that is, $\dot{Q} / V$) and is proportional to thermal conductivity and the critical temperature at which the cooling rate needs to be evaluated.

On the basis of experimental results, a cooling-rate equation was developed for the HAZ of low-carbon steel weldments (Ref 25). This equation considers the combined effects of plate thickness, H , preheating temperature, θ_0 , and welding conditions, and is given as:

$$\left. \frac{\partial q}{\partial t} \right|_{HAZ} = 0.35 \left\{ \left(\frac{q_c - q_0}{I/v} \right)^{1.7} \left[1 + \frac{2}{p} \tan^{-1} \left(\frac{H - H_0}{a} \right) \right] \right\}^{0.8} \quad (\text{EQ 32})$$

The variables α and H_0 depend on the critical temperature of interest. Several values are given in Table 1.

TABLE 1 SELECTED CRITICAL TEMPERATURE AND CORRESPONDING VALUES FOR α AND H_0

CRITICAL TEMPERATURE, θ_c		H_0		α	
°C	°F	mm	in.	mm	in.
700	1290	9.9	0.39	2.0	0.08
540	1000	14.2	0.56	4.1	0.16
300	570	19.8	0.78	9.9	0.39

The units used in Eq 32 are important, because the same units that were used in developing the equation must be employed in its application. The plate thickness, H , must be given in inches, and the travel speed, v , must be given in in./min. The temperatures θ_c and θ_0 must be given in °C, and the welding current, I , must be given in amperes. Using the

correct units, the application of Eq 32 will result in a predicted cooling rate ($^{\circ}\text{C/s}$) for the HAZ of a low-carbon steel weldment.

For low-carbon steels welded by the shielded metal arc welding (SMAW), gas-metal arc welding (GMAW), and submerged arc welding (SAW) processes, an empirical equation has been developed that correlates the weld-metal cooling rate at 538°C (1000°F) with a 95 to 150°C (200 to 300°F) preheat, the weld nugget area (Ref 26):

$$^{\circ}\text{C/s} = \left(\frac{2012}{\text{nugget area}} \right)^{1.119} \quad (\text{EQ 33})$$

where the nugget area is in mm^2 . For low-carbon steels, an empirical chart for determining nugget area for a given welding condition (Fig. 3) also has been developed (Ref 27). The straight line drawn to connect the current and travel speed intersects the nugget area at the calculated value. Welding voltage controls weld bead shape.

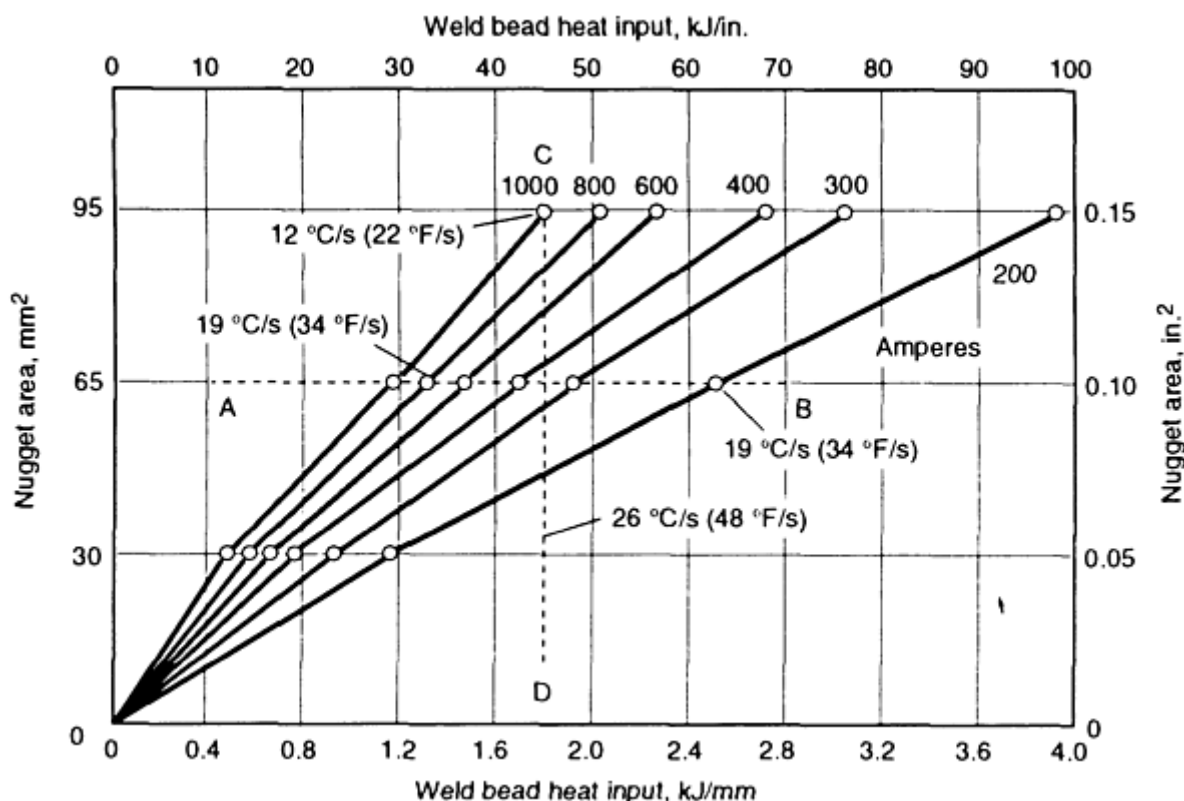


FIG. 3 RELATION BETWEEN NUGGET AREA, HEAT INPUT, AND CURRENT

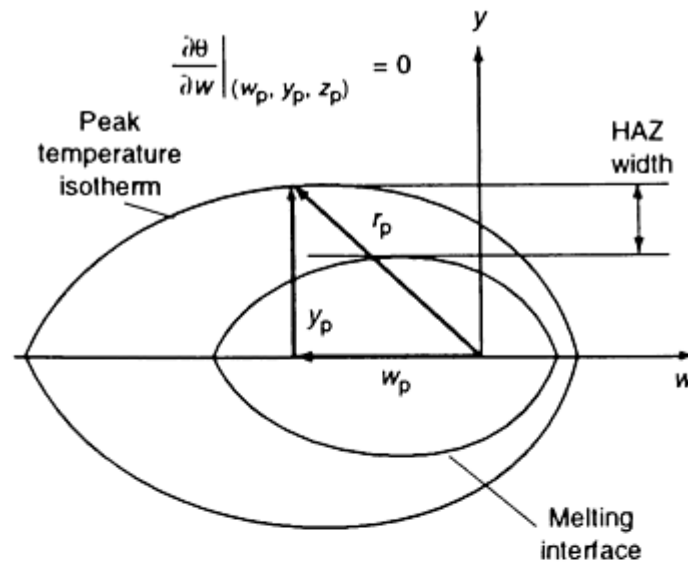
Peak Temperature. An equation to determine the peak-temperature in a weldment at a given distance y from the weld centerline would enable the prediction of HAZ sizes, as well as weld bead widths. The general concept of obtaining a peak-temperature equation, as well as some results that have been obtained, are discussed below.

Consider Fig. 4 and note that the maximum, or peak, temperature is given when $\partial \theta / \partial t = 0$. For the thick-plate model, the cooling rate can be obtained by differentiating Eq 21 and multiplying by $-v$:

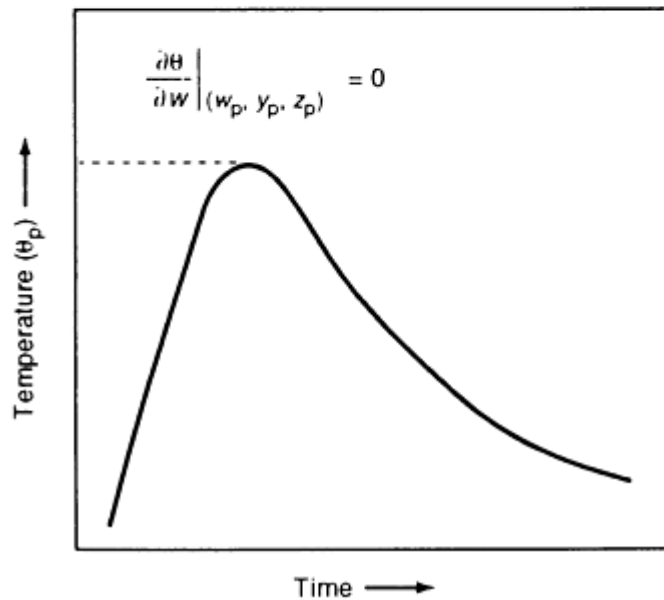
$$\frac{\partial q}{\partial t} = -v \frac{\partial q}{\partial w} = \frac{-vhEI}{2pI r} \exp \left[\frac{-v(w+r)}{2k} \right] x \left[\frac{-w}{r^2} - \frac{v}{2k} \left(1 + \frac{w}{r} \right) \right] \quad (\text{EQ 34})$$

Clearly, the only way that $\partial \theta / \partial t$ can be equal to zero is if

$$\frac{-w}{r^2} - \frac{v}{2k} \left(1 + \frac{w}{r} \right) = 0 \quad (\text{EQ 35})$$



(a)



(b)

FIG. 4 SCHEMATIC SHOWING PEAK TEMPERATURE AT (w_p, y_p, z_p) WITH w_p TO BE DETERMINED FOR A GIVEN PEAK TEMPERATURE VALUE FOR A GIVEN (y_p, z_p) LOCATION. (A) ISOTHERMS. (B) TEMPERATURE HISTORY

Equation 35 describes the relationship that must exist between the two location variables, r and w , for the temperature at the point to be equal to the peak temperature. If this expression were to be substituted into the temperature distribution equation for thick plates (Eq 21) and solved for w and r (two equations and two unknowns), then the location of the peak temperature could be determined in terms of w and r . The location given by r and w would be easily converted to y and z as:

$$R^2 = W^2 + Y^2 + Z^2 \quad (\text{EQ 36})$$

Such a solution for r and w is not explicitly possible, however, because the equations for r and w that result are not explicit. Consequently, iterative techniques are required, resulting in a solution that is both cumbersome and time-consuming.

One method of obtaining a simpler thick-plate peak-temperature equation is to assume that the heat input is from an instantaneous line on the surface of the plate, rather than from a moving point source (that is, $v \rightarrow \infty$). This allows the elimination of the time dependency in the peak-temperature evaluation. Using this assumption, the temperature distribution is given by:

$$q - q_0 = \frac{hEI}{2p l t} \exp\left(-\frac{r^2}{4kt}\right) \quad (\text{EQ 37})$$

Again, to find the peak-temperature location, $\partial \theta / \partial t$ is set equal to zero and the equation is solved for r . The result is:

$$\frac{r^2}{4kt} = 1 \quad (\text{EQ 38})$$

Substituting Eq 38 into Eq 37 yields the peak-temperature expression:

$$\frac{1}{q - q_0} = \left(\frac{erC_p p r^2}{2} \right) / \left(\frac{hEI}{v} \right) \quad (\text{EQ 39})$$

It has been found that Eq 39 gives results that are too high, but that the slope of $1/(\theta_p - \theta_0)$ versus r^2 is accurate. To rectify this situation, Eq 39 is forced to fit experimental results by specifying a known temperature/location condition. When this is done, Eq 39 becomes:

$$\frac{1}{q_p - q_0} = \left(\frac{[erC_p p (r^2 - r_t^2)]/2}{\left(\frac{hEI}{v} \right)} \right) + \frac{1}{q_r - q_0} \quad (\text{EQ 40})$$

where θ_r and r_r are the reference temperature and distance. If the peak temperature (θ_p) evaluation is restricted to locations on the plate surface ($z = 0$), and if the reference temperature and distance are assumed to be the melting temperature and the distance from the weld centerline to the fusion boundary (one half of the weld bead width), then Eq 40 can be written as:

$$\frac{1}{q_p - q_0} = \left(\frac{erC_p p (y^2 - \left(\frac{d}{2}\right)^2)}{\left(\frac{hEI}{v} \right)} \right) + \frac{1}{q_m - q_0} \quad (\text{EQ 41})$$

where θ_m is the melting temperature and d is the weld bead width. This equation gives the peak temperature θ_p in a thick plate at a distance y from the weld centerline.

Solidification Rate. The weld solidification structure can be determined by using the constitutional supercooling criterion. Three thermal parameters that influence the solidification structure are temperature gradient normal to the solid-liquid interface, G ($^{\circ}\text{C}/\text{cm}$), solidification rate of the interface, R (cm/s), and cooling rate at the interface, $d\theta/dt$ at melting temperature ($^{\circ}\text{C}/\text{s}$ and equal to the product of GR). The microstructure may change from being planar to being cellular, a columnar dendrite, or an equiaxial structure if the G/R ratio becomes smaller. The dendrite arm spacing will decrease as the cooling rate increases. The solidification structure becomes refined at higher cooling rates.

At a quasi-steady state, the weld pool solidifies at a rate that is equal to the component of the electrode travel speed normal to the solid-liquid interface. Therefore, the solidification rate varies along the solid-liquid interface from the electrode travel speed, at the weld trailing edge, to zero, at the maximum pool width. The temperature gradient and the cooling rate at the solid-liquid interface can be determined from Eq 21, 24, and 27.

Modified Temperature Solution. The temperature solutions have a singularity at the center of the heat source. This singularity causes the predicted temperatures to be inaccurate in the area surrounding the heat source. However, a condition exists in which the peak temperature along the weld bead edge, that is, the solid-liquid interface location at the maximum pool width, is the melting temperature of the material. Using this temperature condition as a boundary condition for the temperature solutions, Eq 21 and 24 can be modified as shown below for thin plate:

$$q - q_0 = B_z \exp\left(\frac{-vw}{2k}\right) K_0\left(\frac{-vr}{2k}\right) \quad (\text{EQ 42})$$

where B_z is a heat input constant to be determined from the weld bead width, d .

$$B_z = \frac{q_m - q_0 \exp(vr_B / 2k) \left[\frac{K_0(vr_B / 2k)}{K_1(vr_B / 2k)} \right]}{K_0(vr_B / 2k)} \quad (\text{EQ 43})$$

with a bead width of:

$$d = 2r_B \left\{ 1 - \left[\frac{K_0(vr_B / 2k)}{K_1(vr_B / 2k)} \right]^2 \right\}^{0.5} \quad (\text{EQ 44})$$

and for thick plate:

$$q_m - q_0 = B_z \exp\left[\frac{-v(w+r)}{2k}\right] / r \quad (\text{EQ 45})$$

$$B_z = (q_m - q_0) r_B \exp\left(\frac{-vr_B}{2k}\right) \left[\frac{1}{1 + (2k / vr_B)} - 1 \right] \quad (\text{EQ 46})$$

with a bead width of:

$$d = 2r_B \left[1 - \frac{1}{1 + (2k / vr_B)} \right]^{0.5} \quad (\text{EQ 47})$$

The welding heat input, Q , is replaced by the weld bead width.

A Practical Application of Heat Flow Equations (Ref 22). The thermal condition in and near the weld metal must be established to control the metallurgical events in welding. The particular items of interests are the:

- DISTRIBUTION OF PEAK TEMPERATURE IN THE HAZ
- COOLING RATES IN THE WELD METAL AND IN THE HAZ
- SOLIDIFICATION RATE OF THE WELD METAL

Although the following discussion primarily focuses on manual arc welding, certain general statements are applicable to all welding processes.

Peak Temperatures. The distribution of peak temperatures in the base metal adjacent to the weld is given by (Ref 23):

$$\frac{1}{T_p - T_0} = \frac{4.13 r C_p t Y}{H_{met}} + \frac{1}{T_m - T_0} \quad (\text{EQ 48})$$

where T_p is the peak temperature ($^{\circ}\text{C}$) at distance Y (mm) from the weld fusion boundary, T_0 is the initial temperature ($^{\circ}\text{C}$), T_m is the melting temperature ($^{\circ}\text{C}$), H_{net} is the net energy input equal to $\eta EI/v$ ($\text{J/s} \cdot \text{mm}$), ρ is the density of the material (g/mm^3), C_p is the specific heat of solid metal ($\text{J/g} \cdot ^{\circ}\text{C}$), and t is the thickness of the base metal (mm).

Equation 48 can be used in order to determine the:

- PEAK TEMPERATURES AT SPECIFIC LOCATIONS IN THE HAZ
- WIDTH OF THE HAZ
- EFFECT OF PREHEAT ON THE WIDTH OF THE HAZ

In addition, determination of the peak temperature at specific locations in the HAZ and the width of the HAZ can be obtained by the procedure described from Eq 34, 35, 36, 37, 38, 39, 40, and 41.

Cooling Rate. Because the cooling rate varies with position and time, its calculation requires the careful specification of conditions. The most useful method is to determine the cooling rate on the weld centerline at the instant when the metal passes through a particular temperature of interest, T_c . At a temperature well below melting, the cooling rate in the weld and in its immediate HAZ is substantially independent of position. For carbon and low-alloy steels, T_c is the temperature near the pearlite "nose" temperature on the time-temperature transformation (TTT) diagram. The value of $T_c = 550^{\circ}\text{C}$ (1020°F) is satisfactory for most steels, although not critical.

The cooling rate for thick plate (Ref 23) is:

$$R = \frac{2p l (T_c - T_0)^2}{H_{met}} \quad (\text{EQ 49})$$

Equation 49 is comparable to Eq 31, which was obtained by the procedure described in the section "Cooling Rate" in this article. Adams (Ref 24) has developed a cooling rate equation for thin plate along the centerline from Eq 24.

The cooling rate for thin plate (Ref 24) is:

$$R = 2p l r C_p \left(\frac{t}{H_{met}} \right)^2 + (T_c - T_0)^3 \quad (\text{EQ 50})$$

where R is the cooling rate ($^{\circ}\text{C/s}$) at a point on the weld centerline at just that moment when the point is cooling past the T_c , and λ is the thermal conductivity of the metal ($\text{J/mm} \cdot \text{s} \cdot ^{\circ}\text{C}$).

The dimensionless quantity t , called the "relative plate thickness," can be used to determine whether the plate is thick or thin:

$$t = \sqrt{\frac{r C_p (T_c - T_0)}{H_{met}}} \quad (\text{EQ 51})$$

The thick-plate equation applies when t is greater than 0.75, and the thin-plate equation applies when t is less than that value.

Equations 49 and 50 are used to determine the cooling rate along the centerline for thick plate and thin plate, respectively. If one is interested in the cooling rate at the location at distance y (in mm) from the centerline, iterative techniques should be used to solve the cooling rate. First, w and r can be obtained by iteration of the simultaneous equation, which consists of Eq 21 or 27, where θ equals θ_c and $r^2 = w^2 + y^2$, where y is given. Then substitute w and r into the differentiation, $\partial \theta / \partial t = -v \partial \theta / \partial w$, from the temperature from Eq 21 or 27. The result will be the cooling rate for thick or thin plate located at y distance from the centerline:

$$\frac{\partial q}{\partial t_{qc}}$$

In addition, the cooling rate for HAZ of low-carbon steel weldments can be obtained from Eq 32 directly.

The solidification rate can have a significant effect on metallurgical structure, properties, response to heat treatment, and soundness. The solidification time, S_t , of weld metal, measured in seconds, is:

$$S_t = \frac{LH_{net}}{2p l C_p (T_m - T_o)^2} \quad (\text{EQ 52})$$

where L is the heat of fusion (J/mm^3).

Example 1: Welding of 5 mm (0.2 in.) Thick Low-Carbon Steels.

The thermal properties needed for heat flow analysis are assumed to be:

MELTING TEMPERATURE (T_M), °C (°F)	1510 (2750)
AUSTENIZATION TEMPERATURE, °C (°F)	730 (1350)
THERMAL CONDUCTIVITY (λ), W/M · K ($\text{J/MM} \cdot \text{S} \cdot ^\circ\text{C}$)	11.7 (0.028)
VOLUMETRIC SPECIFIC HEAT (ρC_p), $\text{J/MM}^3 \cdot ^\circ\text{C}$	0.0044
HEAT OF FUSION (L), J/MM^3	2

The welding condition is assumed to be:

CURRENT (I), A	200
ARC VOLTAGE (E), V	20
TRAVEL SPEED (V), MM/S (IN./S)	5 (0.2)
PREHEAT (T_0), °C (°F)	25 (77)
HEAT-TRANSFER EFFICIENCY (η)	0.9
NET ENERGY INPUT, H_{NET} , J/MM (KJ/IN.)	720 (18.3)

Calculation of the HAZ Width. The value of Y at $T_p = 730^\circ\text{C}$ (1345°F) must be determined from Eq 48:

$$\frac{1}{730 - 25} = \frac{4.13(0.0044)5Y_z}{720} \cdot \frac{1}{1510 - 25} \quad (\text{EQ 53})$$

resulting in a value for Y_z (the width of the HAZ) of 5.9 mm (0.24 in.).

In addition, the HAZ width can be obtained by the procedure described in Eq 34, 35, 36, 37, 38, 39, 40, and 41. By substituting $w = w_p$, $r = r_p$, $y = y_p$, and $z = 0$ (on surface) into Eq 35 and 36, we obtain:

$$\frac{-w_p}{r_p^2} - \frac{v}{2k} \left(1 + \frac{w_p}{r_p} \right) = 0 \quad (\text{EQ 54})$$

$$r_p^2 = w_p^2 + y_p^2 \quad (\text{EQ 55})$$

The substitution of Eq 55 in Eq 54 yields:

$$\frac{-w_p}{w_p^2 + y_p^2} = \frac{v}{2k} 1 + \frac{w_p}{\sqrt{w_p^2 + y_p^2}} \quad (\text{EQ 56})$$

Inserting $v = 5 \text{ mm/s}$, $\kappa = \lambda/\rho c_p = 0.028/0.004$ into Eq 56:

$$\frac{-w_p}{w_p^2 + y_p^2} = 0.3831 + \frac{w_p}{\sqrt{w_p^2 + y_p^2}} \quad (\text{EQ 57})$$

w_p and y_p should satisfy Eq 57.

From the temperature distribution equation (Eq 21) and $E = 10\text{V}$, $I = 200 \text{ A}$, $\eta = 0.9$, $\lambda = 0.028$, $\theta_p = 730 \text{ }^\circ\text{C}$, $\theta_0 = 25 \text{ }^\circ\text{C}$, $v = 5 \text{ mm/s}$, $\kappa = \lambda/\rho c_p = 0.028/0.004$:

$$730 - 25 = \frac{0.9 \times 20 \times 200 / 5}{2p \times 0.028 r_p} \times \exp \left[\frac{-5(w_p + r_p)}{2 \times 0.028 / 0.0044} \right] \quad (\text{EQ 58})$$

$$-1.76 + \text{LNR}_p = -0.393 (W_p + R_p) \quad (\text{EQ 59})$$

Solving Eq 21 using the above values, by iteration:

- ASSUME $R_p = 3.4$ AND PUT THIS VALUE OF R_p INTO EQ 59 TO OBTAIN $W_p = -2.036$. SUBSTITUTE $R_p = 3.4$ AND $W_p = -2.036$ INTO EQ 55 AND SOLVE FOR Y_p : $Y_p = 2.7$. SUBSTITUTE Y_p AND W_p INTO EQ 56: THE LEFT SIDE OF EQ 56 = 0.176; THE RIGHT SIDE OF EQ 56 = 0.158. THE RESULTS DO NOT SATISFY EQ 56.
- ASSUME $R_p = 3.5$ AND PUT THIS VALUE OF R_p INTO EQ 59 TO OBTAIN $W_p = -2.2$. SUBSTITUTE $R_p = 3.5$ AND $W_p = -2.2$ INTO EQ 55 AND SOLVE FOR Y_p : $Y_p = 2.72$. SUBSTITUTE Y_p AND W_p INTO EQ 56: THE LEFT SIDE OF EQ 56 = 0.179; THE RIGHT SIDE OF EQ 56 = 0.146. THE RESULTS DO NOT SATISFY EQ 56.
- ASSUME $R_p = 3.3$ AND PUT THIS VALUE OF R_p INTO EQ 59 TO OBTAIN $W_p = -1.86$. SUBSTITUTE $R_p = 3.3$ AND $W_p = -1.86$ INTO EQ 55 AND SOLVE FOR Y_p : $Y_p = 2.726$. SUBSTITUTE Y_p AND W_p INTO EQ 56: THE LEFT SIDE OF EQ 56 = 0.170; THE RIGHT SIDE OF EQ 56 = 0.171. EQUATION 56 IS NOW SATISFIED.

The full HAZ width equals $2y_p = 2 \times 2.726 = 5.5 \text{ mm}$. Comparing this result, 5.5 mm, with the result obtained with Eq 48 (that is, 5.9 mm), we know that Eq 48 can be used to obtain accurate results when calculating HAZ width and peak temperature.

Effect of Tempering Temperature on Quenched and Tempered (Q&T) Steels. If the plate had been quenched and then tempered to 430 °C (810 °F), then any region heated above that temperature will have been "over-tempered" and may exhibit modified properties. It would then be reasonable to consider the modified zone as being "heat affected," with its outer extremity located where $T_p = 430$ °C (810 °F):

$$\frac{1}{430 - 25} = \frac{4.13(0.0044)5Y_z}{720} + \frac{1}{1510 - 25} \quad (\text{EQ 60})$$

resulting in a value for Y_z of 14.2 mm (0.568 in.).

Effect of Preheating Temperature on Q&T Steels. Assume that the Q&T steel described above was preheated to a temperature, T_0 , of 200 °C (390 °F):

$$\frac{1}{430 - 200} = \frac{4.13(0.0044)5Y_z}{720} + \frac{1}{1510 - 200} \quad (\text{EQ 61})$$

resulting in a value for Y_z of 28.4 mm > 14.2 mm (1.14 > 0.568 in.). Therefore, increasing the preheating temperature will increase the value of Y_z .

Effect of Energy Input on Q&T Steels. Assume that the energy input into the Q&T steel (without preheating) increases 50% (that is, 1.08 kJ/mm, or 27.4 kJ/in.):

$$\frac{1}{430 - 200} = \frac{4.13(0.0044)5Y_z}{1080} + \frac{1}{1510 - 25} \quad (\text{EQ 62})$$

resulting in a value for Y_z of 21.3 mm > 14.2 mm (0.839 > 0.568 in.). Therefore, increasing the energy input will increase the value of Y_z .

Example 2: Welding of 6 mm (0.24 in.) Thick Low-Carbon Steels.

The thermal properties needed for heat-flow analysis are assumed to be:

MELTING TEMPERATURE (T_M), °C (°F)	1510 (2750)
AUSTENIZATION TEMPERATURE, °C (°F)	730 (1346)
THERMAL CONDUCTIVITY (k), W/M · K (J/MM · S · °C)	11.7 (0.028)
VOLUMETRIC SPECIFIC HEAT (ρC_p), J/MM ³ · °C	0.0044
HEAT OF FUSION (L), J/MM ³	2

The welding condition is assumed to be:

CURRENT (I), A	300
ARC VOLTAGE (E), V	25
PREHEAT (T_0), °C (°F)	25 (77)
HEAT-TRANSFER EFFICIENCY (η)	0.9

Critical Cooling Rate at 550 °C (1020 °F) (T_c). A critical cooling rate exists for each steel composition. If the actual cooling rate in the weld metal exceeds this critical value, then hard martensitic structures may develop in the HAZ, and there is a great risk of cracking under the influence of thermal stresses in the presence of hydrogen.

The best way to determine the critical cooling rate is to make a series of bead-on-plate weld passes in which all parameters, except the arc travel speed, are held constant. After the hardness tests on the weld passes deposited at travel speeds of 6, 7, 8, 9 and 10 mm/s (0.23, 0.28, 0.32, 0.35, and 0.39 in./s), it was found that at the latter two travel speeds, the weld HAZ had the highest hardness. Therefore, the critical cooling rate was encountered at a travel speed of approximately 8 mm/s (0.32 in.s). At this speed, the net energy input is:

$$H_{net} = \frac{25(300)0.9}{8} = 8.43.75 J / mm \quad (\text{EQ 63})$$

From Eq 51, the relative plate thickness is:

$$t = 6\sqrt{\frac{0.0044(550 - 25)}{843.75}} = 0.31 \quad (\text{EQ 64})$$

Because t is less than 0.75, the thin-plate equation (Eq 50) applies:

$$\frac{R}{2pl} = 0.0044 \left(\frac{6}{843.75} \right)^2 (550 - 25)^3 = 32.2 \quad (\text{EQ 65})$$

resulting in R being equal to $2\pi(0.028)32.2$, which is equal to 5.7 °C/s (10.3 °F/s). This value is the maximum safe cooling rate for this steel and the actual cooling rate cannot exceed this value.

Preheating Temperature Requirement. Although the critical cooling rate cannot be exceeded, in the actual welding operation a preheat can be used to reduce the cooling rate to 5.7 °C/s (10.3 °F/s).

Assume that the welding condition is:

CURRENT (I), A	250
ARC VOLTAGE (E), V	25
HEAT-TRANSFER EFFICIENCY (η)	0.9
TRAVEL VELOCITY (V), MM/S (IN./S)	7 (0.3)
PLATE THICKNESS (T), MM (IN.)	9 (0.4)

The energy heat input, H_{net} , is:

$$H_{net} = \frac{25(250)0.9}{7} = 804 J / mm \quad (\text{EQ 66})$$

Assuming that the thin-plate equation (Eq 50) applies:

$$\left(\frac{R}{2pl} \right)_{\max} = 32.2 = 0.0044 \left(\frac{9}{804} \right)^2 (550 - T_0)^3 \quad (\text{EQ 67})$$

resulting in a T_0 of 162 °C (325 °F).

The relative plate thickness should be checked:

$$t = 9\sqrt{\frac{0.0044(550-162)}{804}} = 0.41 \quad (\text{EQ 68})$$

Because t is less than 0.75, the thin-plate equation does apply. If the initial plate temperature is raised either to or above 162 °C (325 °F), then the cooling rate will not exceed 5.7 °C/s (10.3 °F/s).

Effect of Joint Thickness. If the plate thickness increases from 9 to 25 mm (0.36 to 1 in.), but there is the same level of energy input, then the calculation of the initial plate temperature would be as follows. First, using the thin-plate equation (Eq 50):

$$\left(\frac{R}{2pl}\right)_{\max} = 32.2 = 0.0044\left(\frac{25}{804}\right)^2 (550 - T_0)^3 \quad (\text{EQ 69})$$

resulting in a value for T_0 of 354 °C (670 °F).

The relative plate thickness, t , should be checked:

$$t = 25\sqrt{\frac{0.0044(550-354)}{804}} = 0.82 \quad (\text{EQ 70})$$

Because t is greater than 0.75, the use of the thin-plate equation is inadequate. Using the thick-plate equation (Eq 49):

$$32.2 = \frac{(550 - T_0)^2}{804} \quad (\text{EQ 71})$$

resulting in a value for T_0 of 389 °C (730 °F).

The relative plate thickness should be checked:

$$t = 25\sqrt{\frac{0.0044(550-389)}{804}} = 0.74 \quad (\text{EQ 72})$$

Although t is less than, but near to, 0.75, using the thin-plate equation is adequate. Therefore, the initial temperature should be raised to 389 °C (730 °F) to avoid exceeding the cooling rate of 5.7 °C/s (10.3 °F).

Now, if the plate thickness increases to 50 mm (2 in.), but there is the same level of energy input, then the thick-plate equation (Eq 49) applies and, again, the value for T_0 is 389 °C (730 °F).

The relative plate thickness should be checked:

$$t = 50\sqrt{\frac{0.0044(550-389)}{804}} = 1.48 \quad (\text{EQ 73})$$

Because t is greater than 0.75, the use of the thick-plate equation is adequate.

Under some welding conditions, it is not necessary to reduce the cooling rate by using a preheat. For example, if the plate thickness is 5 mm (0.2 in.) and there is the same level of energy input:

$$\left(\frac{R}{2pl} \right)_{\max} = 32.2 = 0.0044 \left(\frac{5}{804} \right)^2 (550 - T_0)^3 \quad (\text{EQ 74})$$

resulting in a value for T_0 of -24 °C (-11 °F). Therefore, using a preheat is unnecessary.

Fillet-Welded "T" Joints. For a weld with a higher number of paths, as occurs in fillet-welded "T" joints, it is sometime necessary to modify the cooling-rate equation, because the cooling of a weld depends on the available paths for conducting heat into the surrounding cold base metal.

When joining 9 mm (0.35 in.) thick plate, where $H_{\text{net}} = 804 \text{ J/mm}$ (20.4 kJ/in.), and when there are three legs instead of two, the cooling-rate equation is modified by reducing the effective energy input by a factor of $\frac{2}{3}$:

$$H_{\text{NET}} = \frac{2}{3} (804) = 536 \text{ J/MM} \quad (\text{EQ 75})$$

Using the thin-plate equation (Eq 50):

$$\left(\frac{R}{2pl} \right)_{\max} = 32.2 = 0.0044 \left(\frac{9}{536} \right)^2 (550 - T_0)^3 \quad (\text{EQ 76})$$

resulting in a value for T_0 of 254 °C (490 °F).

The relative plate thickness should be checked:

$$t = 9 \sqrt{\frac{0.0044(550 - 254)}{536}} = 0,44 \quad (\text{EQ 77})$$

Because t is less than 0.75, using the thin-plate equation is adequate. Therefore, a higher preheat temperature is more necessary than a butt weld because of the enhanced cooling.

Example 3: Cooling Rate for the Location at Distance y (in cm) from the Centerline.

For a steel plate of 25 mm (1 in.) thickness, (t), the welding condition is assumed to be:

HEAT INPUT (ηEI), KW (CAL/S)	7.5 (1800)
TRAVEL SPEED (V), CM/S (IN./S)	0.1 (0.04)
PREHEAT (T_0), °C (°F)	20 (68)
NET ENERGY INPUT (H_{NET}), CAL/CM	18,000

The thermal properties needed for heat flow analysis are assumed to be:

MELTING TEMPERATURE (T_M), °C (°F)	1400 (2550)
THERMAL CONDUCTIVITY (λ), W/M · K (CAL _{IT} /CM · S · °C)	43.1 (0.103)
SPECIFIC HEAT (C_p), J/KG · C (BTU/LB · °F)	473 (0.113)
DENSITY (ρ), G/CM ³ (LB/IN. ³)	7.8 (0.28)

Assume that one is interested in the critical cooling rate at the location on the surface ($z = 0$) at distance $y = 2$ cm from the centerline at the instant when the metal passes through the specific temperature of 615 °C (1140 °F). Initially, the relative plate thickness should be checked. From Eq 51, the relative plate thickness (using English units) is:

$$t = 25 \sqrt{\frac{7.8 \times 0.113 (615 - 20)}{18,000}} = 4,267$$

Because t is greater than 0.75, this plate can be treated as a thick plate. From Eq 78, cooling rate for thick plate at the location where the variables are w and r and at critical temperature $\theta = \theta_c$ is:

$$\frac{\partial q}{\partial t} = -v \frac{\partial q}{\partial w} = \frac{-vhEI}{2p l r} \exp\left[\frac{-v(w+r)}{2k}\right] x \left[\frac{-w}{r^2} - \frac{v}{2k} \left(1 + \frac{w}{r}\right)\right] \quad (\text{EQ 78})$$

To solve Eq 78, we need to calculate the value of w and r first. From Eq 21, the temperature distribution of thick plate, and Eq 36 where $z = 0$, we can get:

$$q - q_0 = \frac{hEI}{2p l r} \exp\left(\frac{-v(w+r)}{2k}\right) \quad (\text{EQ 79})$$

and $r = \sqrt{w^2 + y^2}$

By substituting the welding condition and material properties into Eq 21, Eq 36, and $r = \sqrt{w^2 + y^2}$, we can obtain the following simultaneous equations:

$$615 - 20 = \frac{1800}{22p \times 0.103r} \exp\left[\frac{-0.1(w+r)}{2 \times 0.117}\right]$$

and $r = \sqrt{w^2 + 4}$

The value of w and r can be solved by using iteration techniques to solve the above simultaneous equation. The result is that $w = -3$ cm and $r = 3.606$ cm.

Substituting w and r into Eq 78:

$$\frac{\partial q}{\partial t}_{qC} = -v \frac{\partial q}{\partial w} = \frac{-0.1 \times 1800}{2p \times 0.103 \times 3.606} x \exp\left[\frac{-0.1(-3 + 3.606)}{2 \times 0.117}\right] x \left[\frac{-(-3)}{3.606^2} - \frac{0.1}{2 \times 0.117} \left(1 + \frac{-3}{3.606}\right)\right]$$

Therefore:

$$\left. \frac{\partial q}{\partial t} \right|_{qC} = -9.78^\circ\text{C} / \text{s}$$

From Eq 49 we can calculate the cooling rate along the centerline at the same temperature (615 °C, or 1140 °F):

$$R = \frac{2p \times 0.103(615 - 20)^2}{18,000} = 12.7^\circ\text{C} / \text{s}$$

Also from Eq 32, we can calculate the cooling rate in the heat-affected zone at a temperature of 615 °C (1140 °F):

$$\left. \frac{\partial q}{\partial t} \right|_{qC=615} = 0.35 \left\{ \left[\frac{615 - 20}{336 / 2.362} \right]^{1.7} \times \left[1 + \frac{2}{p} \tan^{-1} \left(\frac{0.9843 - 0.486}{0.125} \right) \right] \right\}^{0.8} = 4^\circ\text{C} / \text{s}$$

Therefore, at the same temperature, the cooling rate at the centerline is greater than the cooling rate at the location a distance y from the centerline. In addition, the cooling rate of the heat-affected zone is less than the cooling rate in the weld pool at the same temperature.

Example 4: Solidification Rate.

A weld pass of 800 J/mm (20.3 kJ/in.) in net energy input is deposited on a steel plate. The initial temperature is 25 °C (75 °F). The solidification time would be:

$$S_t = \frac{2(800)}{2p(0.028)(0.0044)(1510 - 25)^2} = 0.94(\text{s}) \quad \text{(EQ 80)}$$

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Heat Flow in Fusion Welding

Chon L. Tsai and Chin M. Tso, The Ohio State University

To show the effects of material property and welding condition on the temperature distribution of weldments, the welding of 304 stainless steel, low-carbon steel, and aluminum are simulated for three different welding speeds: 1.0, 5.0, and 8.0 mm/s (0.04, 0.02, and 0.03 in./s). The thermal conductivity and thermal diffusivity of 304 stainless steel are 26 W/m · K (0.062 cal/cm · s · °C) and 4.6 mm²/s (0.007 in.²/s), respectively. For low-carbon steel, the respective values are 50 W/m K (0.12 cal/cm s · °C) and 7.5 mm²/s (0.012 in.²/s), whereas for aluminum, the respective values are 347 W/m K (0.93 cal/cm · s · °C) and 80 mm² (0.12 in.²/s). The heat input per unit weld length was kept constant, 4.2 kJ/s (1 kcal/s), for all cases. The parametric results are described below.

Effect of Material Type. Figures 5(a), 5(b), and 5(c) depict the effect of thermal properties on isotherm contours for a heat input of 4.2 kJ/s (1 kcal/s) and travel speeds of 1.0, 5.0, and 8.0 mm/s (0.04, 0.02, and 0.3 in./s). The temperature spreads over a larger area and causes a larger weld pool (larger weld bead) for low-conductivity material. The isotherm contours also elongate more toward the back of the arc for low-conductivity material. For aluminum, a larger heat input would be required to obtain the same weld size as the stainless steel weldment.

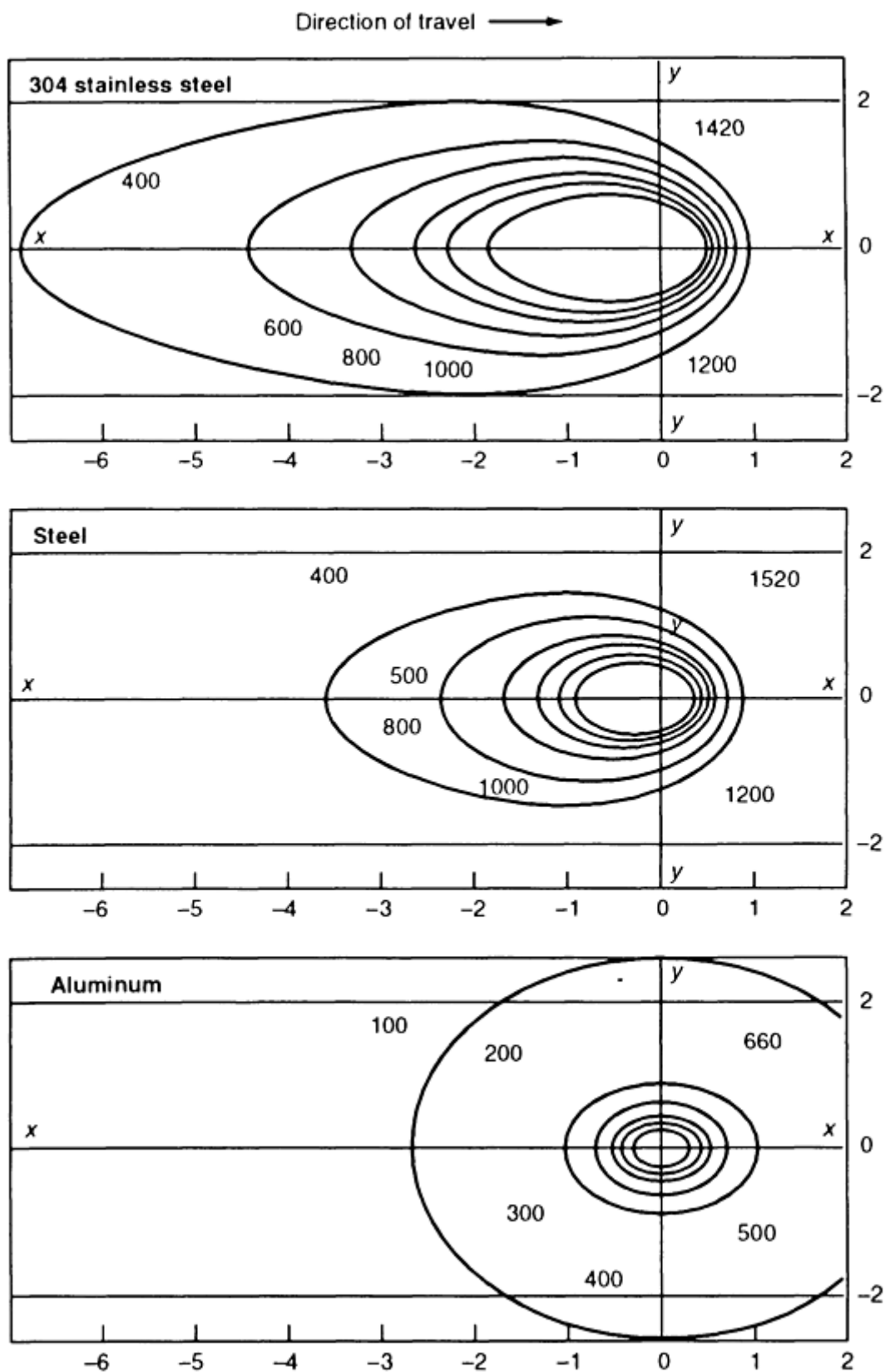


FIG. 5(A) EFFECT OF THERMAL PROPERTY ON ISOTHERM CONTOURS FOR A HEAT INPUT OF 4.2 KJ/S (1000 CAL/S) AT A WELDING SPEED, V , OF 1 MM/S (0.04 IN./S) AND THE RESPECTIVE THERMAL CONDUCTIVITIES OF EACH MATERIAL (REFER TO TEXT FOR VALUES). VALUES FOR x AND y ARE GIVEN IN CM, AND TEMPERATURES ARE GIVEN IN °C.

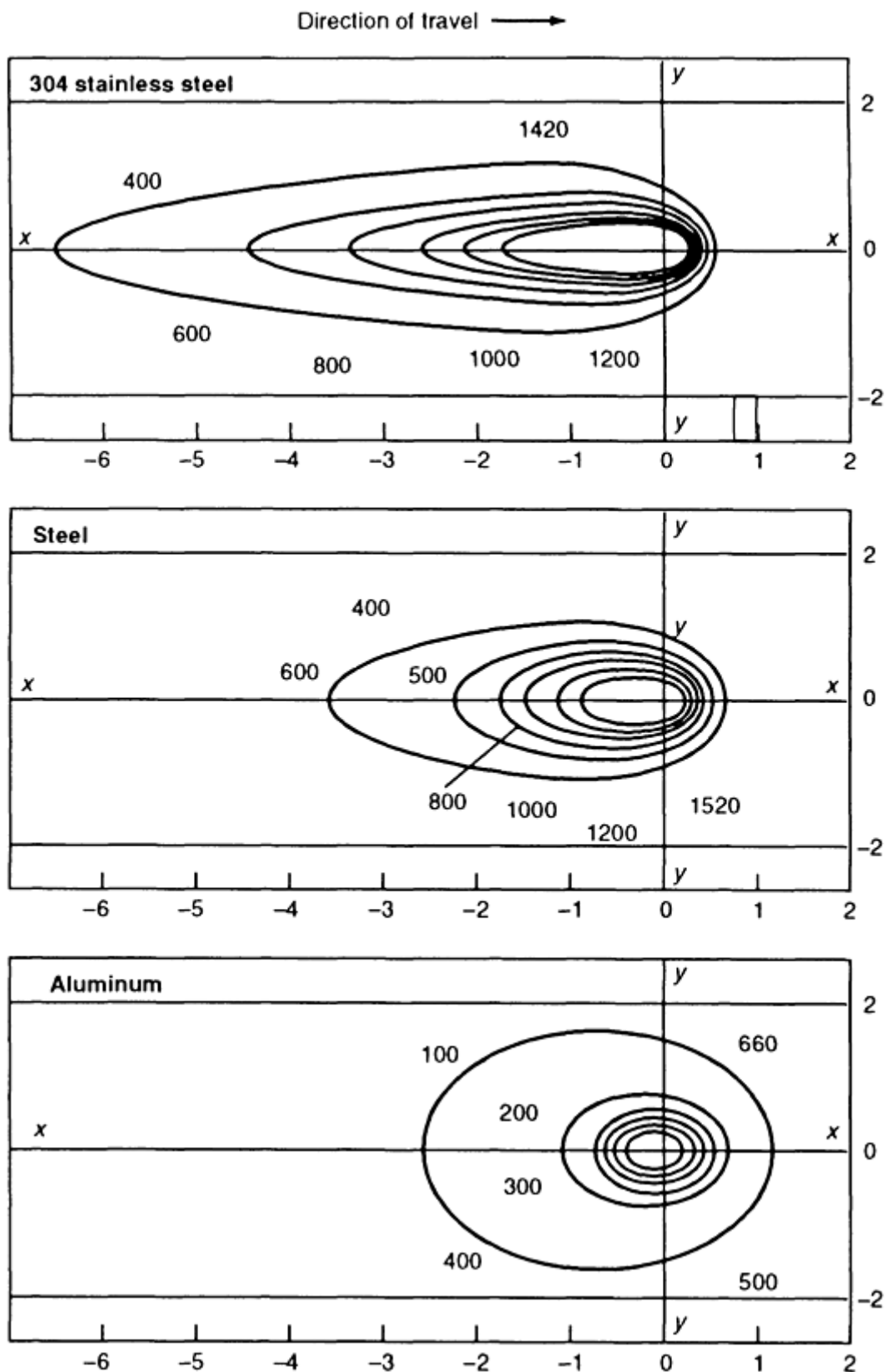


FIG. 5(B) EFFECT OF THERMAL PROPERTY ON ISOTHERM CONTOURS FOR A HEAT INPUT OF 4.2 KJ/S (1000 CAL/S) AT A WELDING SPEED, V , OF 5 MM/S (0.02 IN./S) AND THE RESPECTIVE THERMAL CONDUCTIVITIES OF EACH MATERIAL (REFER TO TEXT FOR VALUES). VALUES FOR x AND y ARE GIVEN IN CM, AND TEMPERATURES ARE GIVEN IN °C.

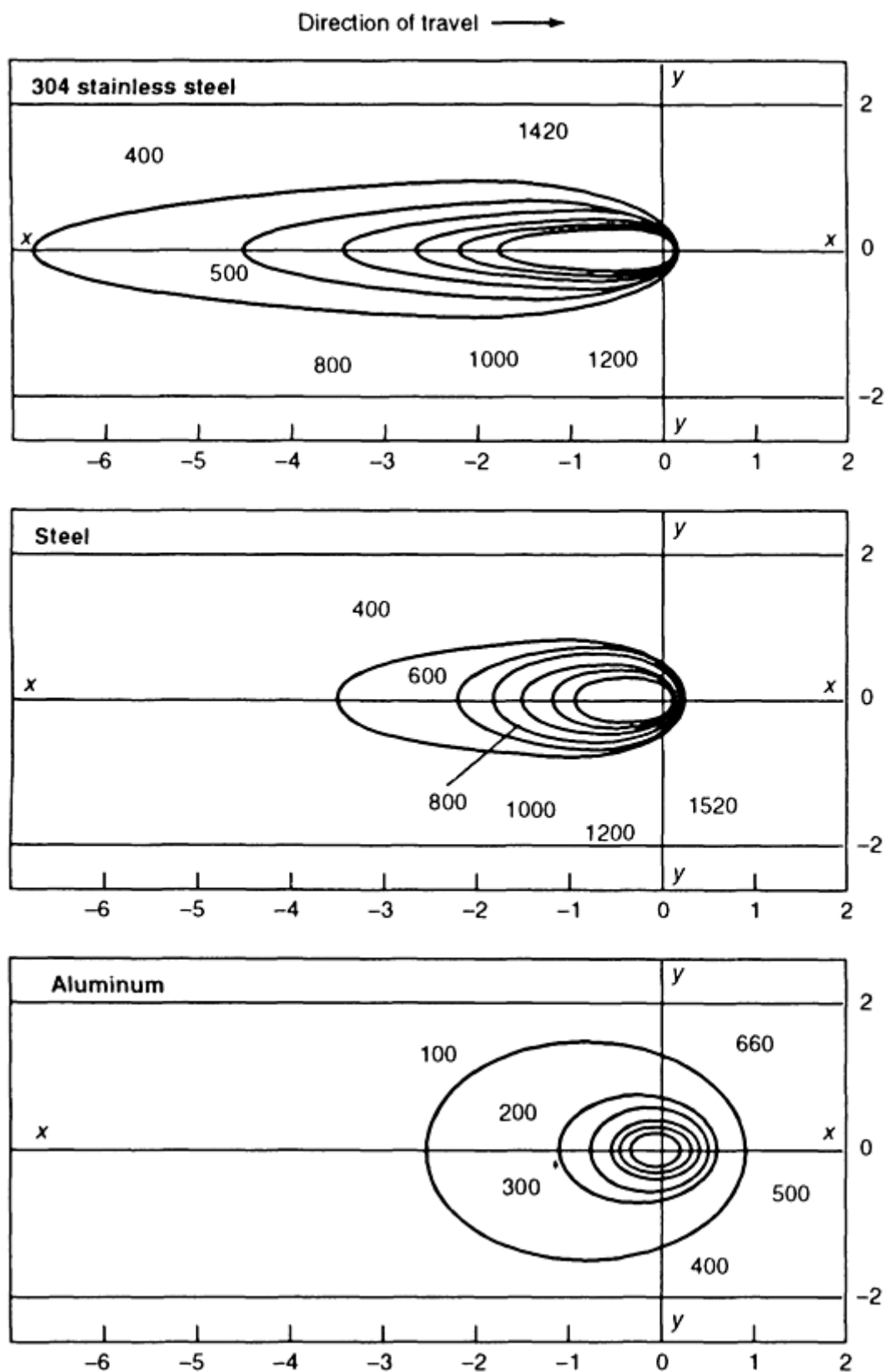


FIG. 5(C) EFFECT OF THERMAL PROPERTY ON ISOTHERM CONTOURS FOR A HEAT INPUT OF 4.2 KJ/S (1000 CAL/S) AT A WELDING SPEED, V , OF 8 MM/S (0.3 IN./S) AND THE RESPECTIVE THERMAL CONDUCTIVITIES OF EACH MATERIAL (REFER TO TEXT FOR VALUES). VALUES FOR x AND y ARE GIVEN IN CM, AND TEMPERATURES ARE GIVEN IN $^{\circ}\text{C}$.

Welding Speed. Figures 6(a), 6(b), and 6(c) show the effect of welding speed on isotherm contours. When the travel speed increases, the weld size decreases and the isotherm contours are more elongated toward the back of the arc. Larger heat inputs would be required for faster travel speeds in order to obtain the same weld size.

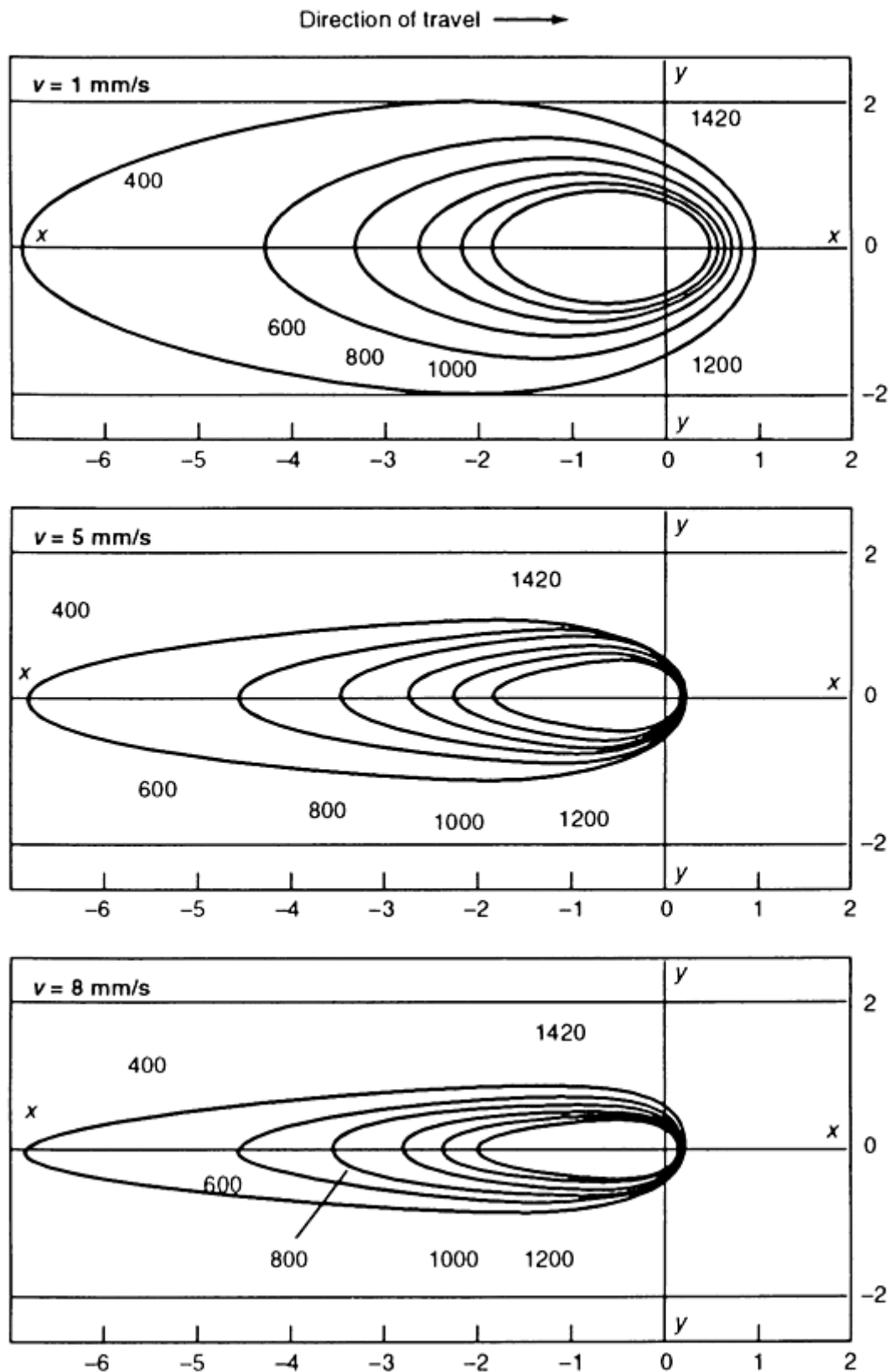


FIG. 6(A) EFFECT OF WELDING SPEED, V, ON ISOTHERM CONTOURS OF 304 STAINLESS STEEL FOR 4.2 KJ/S (1000 CAL/S) HEAT INPUT