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Predicting Product Temperatures and Lethality In Hydrostatic Retorts

presented by

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PREDICTING PRODUCT TEMPERATURES AND LETHALITY IN HYDROSTATIC RETORTS

By

Kathleen Elizabeth Young

A THESIS

Submitted to Michigan State University in partial fulfillment of the requirements for the degree of

MASTER OF SCIENCE

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ABSTRACT

PREDICTING PRODUCT TEMPERATURES AND LETHALITY IN HYDROSTATIC RETORTS

By

Kathleen Elizabeth Young

A computer model was developed to predict geometric center temperatures in cylindrical containers based on standard heat penetration data (for determination of thermal diffusivity), container dimensions, and factory time-temperature profiles (i.e., heat distribution data) for a hydrostatic sterilizer. The program utilized a numerical solution of the general differential equation for two-dimensional, unsteady heat conduction in a finite cylinder with time-varying boundary conditions. To test, adapt, and confirm the model, retort time-temperature profiles for a set of hydrostatic simulated, heat penetration tests (a condensed cream soup, 211 X 400 can size) were employed. The program-generated product temperatures correlated well with those measured experimentally. Lethality calculations determined from the respective temperature profiles also agreed well confirming the applicability of the program to both heating and step-change cooling environments. This model has the potential, not shared by conventional thermal process calculation methods, of predicting the response of the center-can temperature to normally and abnormally varying environmental temperatures.

Dedicated

to William E. Perkins my mentor and dearest friend

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NOMENCLATURE

B = processing time (min)

CCT = center can temperature (°F)

CH = can height (in)

CW = can width (in)

CWT = cooling water temperature (°F)

 $f_{C} = slope of the logarithmic part of the cooling curve (min)$

FOCT = final center can temperature at the end of a heat process (°F)

 $f_h =$ slope of the logarithmic portion of the heating curve (min)

F Value = the symbol used in comparing the relative efficiency of

thermal process

 $F_{O} \text{ Value = the number of minutes required to destroy a specified number$ $of spores at 250°F when z = 18°F (<math>F_{250}^{18}$) i = sequence of radial increments j = sequence of vertical increments j_c = cooling lag (refer to Fig. 5.2) j_h = heating lag (refer to Fig. 5.2) k = thermal conductivity LR = lethal rate (eq. 3.24) r = radius at any point (r_o = can radius) (in) RT = retort temperature (°F) t = time (min) T = temperature (°F) Ti = initial product temperature (°F)

- $T_{(i,j,)} = \text{temperature at node } (i,j) (°F)$
- z = reciprocal of the slope of a thermal death time curve of an organism (°F)

$$\alpha$$
 = thermal diffusivity (in²/min)

- $\Delta t = selected time increment (min)$
- Δx = selected element size (in)
- $\Delta r = RINCR = radial increment size (in)$
- $\Delta y = ZINCR = vertical increment size (in)$

INTRODUCTION

1.1 General Remarks

One of the most promising and readily implemented strategies for minimizing the thermal energy required to commercially sterilize food products in the canning industry is to take advantage of currently wasted or unaccounted-for energy. Singh (1977) discussed various steam-retort heat losses, among the most critical being energy leaving with product and condensate. In the continuous hydrostatic retort ("hydro"), this dissipated energy could be utilized to raise the temperature of the water in the discharge leg, thus retarding cooling rates of conduction-heating products enough to permit process time reductions of 10-20%, without compromising lethality. These reductions in steam requirements could mean yearly savings, in fuel costs alone, of approximately \$30,000 for a typical hydro (Appendix A).

The substantial contribution that a programmed cooling phase can impart to the inactivation of bacterial spores was first elucidated by Board et al. (1960). Several efforts to mathematically predict the centercan temperature profile for this cooling phase, particularly during the well known curvilinear segment characteristic of initial cooling, have been published (Hayakawa, 1970; Griffin et al., 1971; Stumbo, 1973). These and other authors have tried to improve on the first formula method

(Ball, 1923) based on heating studies of canned corn. Ball erroneously assumed a constant cooling lag (j_c) of 1.41, and a cooling rate (f_c) equal to the heating rate (f_h) . Despite these imperfect assumptions, Ball's method is today the most widely accepted procedure employed by the canning industry for process lethality evaluation because of its relative simplicity.

Ball's formula method provides a reasonable, though invariably low, estimate of the overall lethality of a batch retorted, conduction-heating product in a typical shelf-size can (nominally 8-19 ounces net weight) that is cooled, without overriding air pressure, in 65-85°F water. This method, however, grossly underestimates the sterilizing values associated with conduction-heating products processed in hydros due to its inability to take into account the high temperature cooling cycle and gradient water pressure inherently imposed by the discharge leg. In numerous cases, the actual accumulated process lethality for the hydro has been found to be as much as two times that predicted by Ball's formula.

1.2 Objectives

The specific objectives of this study were to:

 Develop a computer model for predicting thermal center temperatures in a conduction-heating product based on the heating characteristics of the product (thermal diffusivity), the specified can dimensions, and the factory time-temperature profile of the

hydrostatic or other type of retort being evaluated.

- 2. Test/adapt/confirm the model and its ability to predict center-can temperatures that correlate well with those measured during simulated hydrostatic heat penetration tests.
- 3. Investigate the influence of the inherent characteristics of the hydrostatic discharge leg (high temperature cooling water and gradient overriding pressure) on lethality prediction for conduction-heating type foods.
- 4. Compare sterility values (F_0) computed from measured and mathematically predicted hydrostatic retort simulated temperatures using this model and other process calculation methods.

2. LITERATURE REVIEW

2.1 Current Industry Practice

Foodborne botulism is a syndrome resulting from the action of a preformed neurotoxin produced by one or another of the four serotypes of <u>Clostridium botulinum</u> toxic for humans (Kautter and Lynt, 1971; Sugiyama, 1980). These anaerobic, sporulating microorganisms, indigenous to the soil, are of obvious concern to the food industry, particularly the canning industry, because of the nearly oxygen free environment provided by a hermetically sealed can. The toxin formed by <u>C. botulinum</u> is one of the most potent poisons known. Its interference with the passage of stimuli via the motor nerves can, within three to ten days of ingestion, cause paralysis of the diaphragm, and in the absence of mechanical ventilation assistance, result in death due to respiratory failure (Center for Disease Control, 1979).

The awareness of <u>C.</u> botulinum has locked the heat sterilization technology of the food industry and its regulating agencies in a state of safe conservatism. A review of the literature reveals that current industrial methods introduced in the twenties still enjoy the virtually unqualified acceptance of the industry because incidences of spoilage during the nearly six decades of their use have been relatively rare.

It is current industrial practice to base canned food thermal

process specifications solely on a desired reduction in microbial population (typically by an arbitrarily-chosen twelve log cycles). Such reductions are assayed by one of the following industry-employed protocols (Townsend et al., 1968).

- 1. The experimental pack, which involves inoculating food containers with a set number of selected organisms of known resistance; processing at different levels of time, or temperature, or both; and determining the degree of spoilage after a minimum time period (usually four weeks) by incubating or subculturing. This procedure provides biological verification, and is conducted for new product lines or in a case of a significant process modification (e.g., process reduction, new starch system, etc.).
- 2. Mathematical methods based on two considerations: (a) The thermal death time characteristics of the microorganisms the thermal process is intended to kill, and (b) the description of the temperature profile in the container at its slowest heating point as a function of process time.

2.1.1 The General Method

The general method, one of the two most commonly employed mathematical methods in the canning industry, was developed over sixty years ago

by Bigelow, Bohart, Richardson, and Ball (1920). Despite the precision of the method, its utility is limited in that this procedure cannot predict lethality values for process times other than those tested experimentally.

Bigelow et al. (1920) conceived the idea of a "lethal rate curve" that related time-temperature events with the relative inactivation of spoilage bacteria (Perkins, 1964). By this classical method, the lethal rate for each slowest heating point measured during the course of an entire process is plotted on rectangular coordinate paper. The area beneath this curve represents the sterilizing value of the process in terms of the thermal resistance of the spores in question. This graphical method can be applied to any type product whether it heats by convection, conduction, or a combination of the two (Perkins, 1964).

Since 1920, the general method has been improved and simplified. Schultz and Olson (1940) developed the use of lethal rate paper (for a z value of 18°F only) to decrease the potential of human plotting error. At the same time, these two scientists introduced a formula for simple and rapid conversion from one initial product temperature and/or retort temperature to another for any set of heat penetration data. Patashnik (1953) reported an additional application of method which permits estimation of ultimate lethal rates during the course of the process.

Despite the wide applicability of the Bigelow method and its enhancements, sterility value calculations using this method are still laborious. Before this method can be applied, the actual time/temperature profile for a product experiencing a given thermal process must be generated by tedious factory thermocouple tests. In instances of frequently changing environmental conditions (e.g., the hydro, with environments of steam, water immersion, and sprays), accurate time/product temperature profiles become very difficult to obtain.

2.1.2 Ball's Formula Method

The second and only other method approved by the canning industry for process lethality evaluation is Ball's formula method, published in 1923. This method was the first formula method reported in the literature, and in a time before the advent of computers, a mathematical wonder.

An excellent review of the theoretical development of Ball's method has been presented by Merson et al. (1978). Its approach reduced the lethality calculation of a sterilization process to a single formula by combining the equation for the rate of destruction of bacterial spores (thermal death time curve) with the equation describing the heating rate of a canned food. Both of these rates are assumed to be logarithmic, a premise that can be validated experimentally within the temperature limits of conventional canned food sterilizers.

The method is very versatile in providing a means of predicting either the time required to obtain a given lethality value of the F value that would be derived from a given processing time. In either case, the z value (temperature dependence of the destruction rate of a specified organism), the heating charateristics of the product (the lag (j_h) , and the slope of the heating curve (f_h) , the cooling and heating media temperatures, and the initial product temperature must be specified. Ball simplified the solution by incorporating several assumptions and empirical factors into his method. Of these assumptions, four most often compromise the accuracy of lethality values.

First, Ball "...assumed that the cooling curve is the exact reverse of the heating curve...[which] meant that the two curves had the same slope..." (Ball, 1923, p.13). On the same page, however, Ball notes

that "This is...known to be false in a majority of the cases; but is a convenient assumption upon which to base the calculations."

Industrial experience verifies Ball's statements that the slope of the cooling curve (f_c) is rarely if ever equivalent to the slope of the heating curve (f_c) , and thus should not be assumed to be equal if an accurate lethality value is to be predicted. Despite the modifications introduced by Ball and Olson (1957) for incorporating the actual f_c value into Ball's formula method, the tedium of computing an f_c value by hand, and the complications of evaluating the cooling slope by computer have precluded the use of such an "Improved Formula Method" by the industry.

The second inaccuracy in Ball's 1923 formula is the empirical selection of a constant cooling lag factor of 1.41. This value was based by Ball on "...experimentally determined heating curves...principally those of corn" (p.19), noting later, "...[it was] realized that this $[j_c]$ value should have been based upon cooling curves rather than heating curves". Ball chose a j_c constant in order to simplify the imposing task of preparing parametric charts and tables for estimating lethality by means of his single equation. The assumption of a constant j_c introduces an error when the cooling lag factor differs from 1.41 (often true for products with thermal diffusivity values that differ from that of Ball's "canned corn", or for products processed in sterilizers other than batch retorts), and when the relative sterilizing effect during cooling cannot be neglected (Hayakawa, 1969).

The initial portion of the cooling curve (immediately following "steam-off" was further characterized by Ball as follows (Ball, 1923, p.11):

"Rather than use for the cooling curve the complicated expression [analytical solution] given by Thompson [Thompson, 1919], it has been assumed that, in all cases, the first part of the cooling curve satisfies the equation of a hyperbola until it passes into the logarithmic [part of the] curve...".

Depending on the methods of cooling used (e.g., cooling water temperatures and overriding air pressure conditions), the shape of the initial segment of the cooling curve will take on forms not always approximated well by a hyperbola.

The last assumption that introduces significant error in calculating lethality values for sterilizing systems other than batch retorts is that "...the temperature of the cooling water remains constant during the cooling of the can." (1923, p.13). Ball's tables and derived f_h/U versus log g curves (Townsend et al., 1968), which relate the thermal center temperature at "steam-off", the heat resistance of the relevant spoilage microorganisms, and the cooling water temperature, provide only for temperature difference values (between the cooling water temperature and the retort steam temperature) of 130°F, 160°F, and 180°F. Thus, Ball's estimate of cooling lethality relates only to cooling water temperatures between 70°F and 120°F (for a 250°F steam temperature). No provisions can be made for cooling media temperatures that exceed 120°F.

The limitations of Ball's formula method, from the standpoint of efficiency, are particularly evident when evaluating the lethality of a hydro process, where the unique characteristics of the cooling cycle can contribute up to one third of the total process lethality.

2.2 Hayakawa's Methods

A comprehensive review of the english language literature revealed no truly versatile method for predicting process lethality. Hayakawa's

empirical formulas (1970), the most flexible for constant heating and cooling conditions, can calculate the lethality at the slowest heating point given any experimentally determined j_c , f_c , and cooling water temperature combination. However, this method has limited use in that it cannot <u>predict</u> the thermal center cooling patterns of products processed under conditions other than those measured experimentally. This method is a combination of the finite cylinder heat-conduction formulae proposed earlier by Gillespie (1951) for predicting temperature distribution during heating of conductive canned products and Hayakawa's own empericallyderived cooling formula.

Hayakawa later derived (1971) analytical formulas for predicting transient temperature distributions for conduction-heating canned products subjected to five empirically selected, surface-varying temperature functions. Only two of these temperature functions, however, are applicable to standard retort processing, and neither of these is adaptable to the step-change cooling conditions of the hydrostatic retort.

2.3 The Design of the Hydrostatic Retort

The hydrostatic sterilizer, standing as high as 60 feet and accomodating up to 1200 cans per minute, is the most widely used of continuous sterilizers. This machine operates on the hydrostatic principle, with the pressure of the saturated steam exactly balanced by the hydrostatic pressure exerted at the base of the two water legs (Stork-Amsterdam, 1977).

The cans are introduced into the chain at the carrier feed/discharge station, typically by rolling into a canted "T"-shaped carrier in groups of approximately twenty cans (Perkins, 1978). From this point, the cans

traverse a series of four-to-five chambers which comprise this ingenious retort system.

Initially, the nonsterile product cans are passed through an infeed hydro leg (Fig. 2.1), a water immersion phase where the temperature may range from ambient to just below boiling (although usually set between 130°F-190°F). Products that heat predominately or completely by conduction experience no significant heating while in this phase of the cooker which would, however, provide sufficient heat to prevent lowering of initial product temperature (Perkins, 1978).

The cans next enter the steam chamber, which may contain anywhere from two-to-ten sterilizing passes, depending on the specified processing time and can speed. The pressure exerted by the water legs at the base of the steam dome dictates the temperature in this section (ranging from 230°-265°F), and may be regulated by raising or lowering the height of the water legs. For example, a thirty-nine foot column of water would exert a pressure at its base of 15 psig, resulting in a steam chamber temperature of 250°F. The provision thus made for a continuous can feed in and out of the sterilizing section effects significant energy savings by eliminating the need for repeatedly heating and cooling the steam chamber. Reportedly, fifty percent less steam is consumed, and seventy percent less water than in a batch retort (Stork-Amsterdam, 1977).

After leaving the steam dome, the containers pass through a two-to three phase cooling section. The first is the exit hydro leg, another immersion phase (Fig. 2.1), typically maintained at 130°F-165°F. Currently, the thermal energy leaving with product and condensate is expended by one of two hydrostatic designs. In one design, the water build-up in the manometric infeed/exit leg column is conveyed by gravity from near the top of the infeed leg to a collection tank and pumped to the top of



Fig. 2.1 General schematic of a hydrostatic retort (from Perkins, 1978).

the exit hydro leg as required to maintain the necessary water level. In doing so, the heat of the condensate and that given up by the cans leaving the steam is uniformly distributed between the legs. In the second type of design, the hydrostatic system pumps water off the base of the discharge leg through a heat exchanger. This procedure cools the exit leg water further, and effects an even more rapid and energy dissipating cooling of the containers when the steam cycle is completed.

The uninterruped cooling cycle, initiated in the exit hydro leg, is continued in a series of cooling spray towers, where a combination of fresh and recirculated water is cascaded or sprayed over the containerconveyor chain at temperatures of 85-100°F. At the end of the cycle, the cans pass underneath the entire system, where a third, immersion, stage of cooling may if necessary, be effected (e.g., for large can sizes, to bring the slowest cooling point temperature below 110°F precluding thermophilic spoilage during storage).

Pressure, as well as temperature, is an important variable during cooling. Associated with steam processing at 250°F, the cans are subjected to a gradual increase in external pressure from zero to 15 psig in the infeed leg, a constant pressure of 15 psig in the steam dome, and a gradual decrease in external pressure from 15 to zero psig in the exit leg. The very precise reverse pressure gradient provided by the discharge hydro leg can be beneficial in preventing buckling during cooling of large cans (i.e., exceeding 303X406).

It should be noted here that contrary to common belief, the hydrostatic process is effectively a "still process" because of the slow can velocity (typically 2.5 to 3 inches/minute) through the multiple chain passes and the transitional overbends/underbends. Such a steady progres-

sion through the sterilizer induces <u>no</u> measureable convection currents for thickened and/or highly garnished products that heat by conduction.

Several types of agitating hydros, manufactured in Europe, are employed primarily for sterilization of milk and infant formula. Their appeal is limited, however, because of their high initial cost and mechanical complexity.

2.4 Factors Affecting Lethality Predictions During Cooling

The substantial lethality contribution that can be associated with the cooling phase of a thermal process designed for a conduction-heating product was first elaborated on by Board (et al., 1960). In accurately predicting the potential of a hydro process in terms of spore inactivation, it is essential to account for the unique attributes of the exit hydro leg that can perhaps exert a positive influence on lethality.

2.4.1 Exit Hydro Leg Cooling Water Temperatures

When hydrostatic sterilizers were first introduced over thirty years ago, promotional material suggested possible reductions in steam times as compared with the same product/container combination processed in a batch retort (Perkins, 1978). The basis for this suggestion was the influence of the hot exit leg water on retarding cooling rates relative to typical retort cooling conditions of 65-85°F.

Comprehensive studies of the influence of higher temperature cooling on process lethality have not been published, however, presumably because of the difficulty and expense of performing precise simulations and con-

firming factory tests.

2.4.2 The Gradient Pressure of the Exit Hydro Leg

Heat penetration tests performed in the laboratory still retorts (Board et al., 1960; Helmer at al., 1952) have demonstrated that constant pressure during the cooling cycle of a conduction-heating product plays a vital role in increasing the lethality manifested at the can center. This phenomenon is apparently related to internal can pressure associated with the temperature-dependent expansion of the product and of the headspace air during heating (Herson and Hulland, 1969). This internal pressure, in the absence of applied air pressure, results in "ebullition" (i.e., mixing) of the can contents during initial cooling (Gillespy, 1962). Turbulence thus created rapidly mixes the cooler-central portion of the product with the hotter-peripheral contents, effecting a reduction in the sterilizing value at the can center (Helmer et al., 1952)

On the other hand, when pressure is mechanically controlled in the retort, the internal pressure in normally filled cans is likewise maintained, and the mixing of the container contents is precluded. Under these conditions, lethality values were found by Helmer et al. (1952) to be as much as double in the 603 X 700 can size. Similar lethality enhancements were observed by Board et al. (1960) as a result of pressure cooling with smaller cans. Thus, the smaller the container size, the lesser the influence of pressure cooling on the F value.

The possibility of taking advantage of this sterility-enhancingeffect seems most applicable to the hydro, with its gradient pressure exit leg. If "ebullition" can be prevented by the gradual decrease of

external pressure of the hydro leg, the rate of temperature change at the geometric center of the can would be governed by the laws of the relatively slow process of pure conduction cooling. By retarding the cooling rates, lethality values would increase, and most likely, significant reductions in processing times would be feasible.

3. THEORETICAL DEVELOPMENT

3.1 Transient Numerical Heat Transfer Model for Cans

When a temperature gradient exists between a canned product and its immediate environment, there is an energy transfer from the hightemperature region to the low-temperature region (Holman, 1972). According to Fourier's Law of heat conduction [eq. 3.1], energy transfer is by conduction and the heat-transfer rate per unit area is proportional to the temperature gradient.

$$q = -kA \ \partial T / \partial x \tag{3.1}$$

Here, q is the heat-transfer rate, $\partial T/\partial x$ is the existing temperature gradient, and k is the thermal conductivity of the material. The negative sign indicates that the heat flux is in the direction opposite the temperature gradient.

When investigating the rate of heat transfer into a can of product during the course of a sterilization process (heating and cooling), the differential equation defining two-dimensional, unsteady-state heat conduction in a finite cylinder is employed [eq. 3.2] (Carslaw and Jaeger, 1959). This equation represents a composite of the solutions for an infinite slab and an infinite cylinder:

$$\partial^{2} T/\partial r^{2} + (1/r) (\partial T/\partial r) + \partial^{2} T/\partial y^{2} = (1/\alpha) (\partial T/\partial t)$$
 [3.2]

Where:
$$T = temperature at any point, at any time (°F)
r = radial distance from the centerline (in)
y = vertical distance from the mid-plane (in)
 $\alpha = thermal diffusivity of the food product (in2/min)
t = time (min)$$$

Figure 3.1 depicts the placement of r and y with respect to the centerline and mid-plane (Orlowski, 1979).

When boundary conditions vary with time, analytical solutions to equation [3.2] are very complex, not available, or overly simplified to be useful. For these special cases, the solution to equation [3.2] is best handled using numerical methods and the aid of a computer.

The following terms in equation [3.2] can be rewritten in a finite difference form using central difference operators (Fig. 3.1) (Orlowski, 1979):

$$\partial^2 T / \partial r^2 = [T_{(i-1,j)} - 2T_{(i,j)} + T_{(i+1,j)}] / \Delta r^2$$
 [3.3]

$$\partial^2 T / \partial y^2 = [T_{(i,j-1)} - 2T_{(i,j)} + T_{(i,j+1)}] / \Delta y^2$$
 [3.4]

$$\partial T/\partial r = [T_{(i-1,j)} - T_{(i+1,j)}]/2\Delta r$$

(3.5)

$$\partial T/\partial t = [T_{(i,j)} - T_{(i,j)}]/\Delta t$$
 [3.6]

and rearranged to obtain the general algebraic equation [3.7] for the temperature at a selected point after a selected time interval in terms of the temperatures at surrounding points at the beginning of the given time interval (Teixeira et al., 1969). Hence, the general solution to



Fig. 3.1 Finite cylinder node labeling for numerical heat transfer analysis (from Orlowski, 1979).

equation [3.2] is:

$$T_{(i,j)}^{(T+\Delta t)} = T_{(i,j)}^{t} + [\alpha \Delta t / \Delta r^{2}] [T_{(i-1,j)} - 2T_{(i,j)}]^{t} + T_{(i+1,j)}^{t} + (\alpha \Delta t / 2r \Delta r) [T_{(i-1,j)} - T_{i+1,j}]^{t} + (\alpha \Delta t / \Delta y^{2}) [T_{(i,j-1)} - 2T_{(i,j)} + T_{(i,j+1)}]^{t}$$

$$(3.7)$$

Where: i = radial element sequence j = vertical element sequence Δt = a selected time increment (min) Δx = a selected element size (in) T_(i,j) = temperature at node (i,j) (°F) superscript t = at time t subscript t + time increment (Δt) = at time t + Δt

Modifications (required because of geometrical considerations) in equation [3.7] result in three separate solutions that can be used in combination with this equation to predict the temperature distribution profile for one quarter of a canned product (Arpaci, 1966). Due to the symmetry of the cylindrical coordinate system (typical matrix -Fig. 3.2), only one quarter of the can was evaluated.

At the centerline, equation [3.7] is incorrect since the fourth term is not defined when r equals zero. However, by using L'Hospital's Rule the $(1/r)(\partial T/\partial r)$ term can be computed by taking the limits as r approaches zero, and rewritten as (Arpaci, 1966):

$$\lim_{r \to 0} \frac{\partial T/\partial r}{r} = \frac{\partial^2 T}{\partial r^2}$$
[3.8]

Sinœ

$$T_{(i+1,j)} = T_{(i-1,j)}$$
 when $r = 0$ [3.9]

Boundary Temperature Nodal Points
 Matrix Center Temperature Nodal Points
 Mid-Plane Temperature Nodal Points
 Centerline Temperature Nodal Points
 Geometric Center Temperature Nodal Points



Fig. 3.2 Square matrix system (10 X 10) for a finite numerical solution.
tion [3.3] for the fourth term in equation [3.7] becomes:

$$T_{(i,j)}^{(t+\Delta t)} = T_{(i,j)}^{t} + (2\Delta t\alpha/\Delta r^{2}) [2T_{(i-1,j)} - 2T_{(i,j)}]^{t} + (\alpha\Delta t/\Delta y^{2}) [T_{(i,j-1)} - 2T_{(i,j)} + T_{(i,j+1)}]^{t}$$

$$(3.10)$$

For the mid-plane, y equals the half-height of the can (L/2) for all values of r except for r equal to zero. Using equation [3.7] as a basis, and acknowledging that due to the symmetry of the problem:

$$T_{(i,j-1)} = T_{(i,j+1)}$$
 when $y = L/2$ [3.11]

The mid-plane solution is:

$$T_{(i,j)}^{(t+\Delta t)} = T_{(i,j)}^{t} + (\alpha \Delta t / \Delta r^{2}) [T_{(i-1,j)} - 2T_{(i,j)}]^{t}$$

+ $T_{(i+1,j)}^{t} + (\alpha \Delta t / 2r \Delta r) [T_{(i-1,j)} - T_{(i+1,j)}]^{t}$
+ $(\alpha \Delta t / \Delta y^{2}) [2T_{(i,j-1)} - 2T_{(i,j)}]^{t}$ [3.12]

Finally, the numerical finite difference solution at the geometric center (based on equation [3.7], with equations [3.9] and [3.11] both holding true due to symmetry) is represented by:

$$T_{(i,j)}^{(t+\Delta t)} = T_{(i,j)}^{t} + (2\Delta t\alpha/\Delta r^{2}) [2T_{(i-1,j)} - 2T_{(i,j)}]^{t} + (\alpha\Delta t/\Delta y^{2}) [2T_{(i,j-1)} - 2T_{(i,j)}]^{t}$$
(3.13]

A summary of the node point equations derived here are given in Table 3.1 for a 10 X 10 matrix.

Table 3.1 Summary of the node point solutions employed in the numerical heat transfer analysis for a finite cyclinder (10 X 10 Matrix).

NODE POINTS	SOLUTIONS		
(i,j) for (1,j) $j = 1,11$ for (i,1) $i = 1,11$	Boundary Condition Equations [3.15], [3.16], and [3.17]		
(i,j) for $i = 2,10, j = 2,10$	General Solution Equation [3.7]		
(i,j) for $(i,11)$ $i = 2,10$	Mid-Plane Solution Equation [3.12]		
(i,j) for (11,j) j = 2,10	Centerline Solution Equation [3.10]		
(i,j) for (11,11)	Geometric Center Solution Equation [3.13]		

The boundary and initial conditions for this transient numerical heat transfer model were: (1) uniform initial product temperature at the onset of processing (eq. [3.14]); and (2) varying surface temperatures at the can sides (eq. [3.15]), can bottom (eq. [3.16]), and can top (eq. [3.17]).

$$T(r,y,0) = Ti$$
 [3.14]

$$T(r_{2}, y, t) = f(t)$$
 for $t > 0$ [3.15]

T(r,0,t) = f(t) for t > 0 [3.16]

$$t(r,L,t) = f(t)$$
 for $t > 0$ [3.17]

Where: $r = r_{c} = can radius$

y = 0 or L, referring to can bottom and top, respectively

$$T(r,y,t) = temperature at$$

 $T_i = initial product temperature (°F)$

In summary, the mathematical heat conduction model involved the solution to the general differential equation for a finite cylinder (equation [3.2]) with the initial condition stated in equation [3.14], and the boundary conditions stated by equations [3.15], [3.16], and [3.17]. In applying the model, the temperatures related to the time dependent boundary conditions are numerically specified from measured heating or cooling media temperatures.

Assumptions made in the construction of this model were:

1. Negligible heat transfer resistance at the can surface (possible influence of headspace void on heat transfer ignored), i.e., infinite convective heat transfer coefficient.

- 2. Constant thermal diffusivity over the temperature ranges under consideration (heating and cooling).
- 3. No internal heat generation.
- 4. Conduction heating and cooling only, with no induced convection.
- 5. Internal volume unaffected by changing external pressure.
- 6. Homogeneous, isotropic material.
- 7. No circumferential heat flow.
- 3.2 Least Squares Prediction of Thermal Diffusivity

The thermal diffusivity of a food product plays a preeminent role in the prediction of temperature distribution during food processing (equations [3.7], [3.10], [3.12], and [3.13]). For this study, a computer program (Larkin, 1981) was employed for estimating thermal diffusivities of conduction-heating food products based on actual time/temperature heat penetration profiles measured under laboratory conditions.

Using an initial diffusivity estimate (based on the moisture content of the food; if unknown, the program estimates it at 50%), a three point grid of diffusivity values is produced to determine the direction of the minimum sum of squared error (SSE). SSE is computed as the difference between the actual heat penetration points and the calculated temperature data points. New grids are then created until the difference between the diffusivity is smaller than an error factor set at 1.0E-5.

To determine the predicted temperatures used in this least squares procedure, analytical solutions of an infinite slab and infinite cylinder were employed. Equation [3.2], subject to the initial (eq. [3.14]) and following boundary conditions constituted the mathematical model.

 $T(r_{0}, y, t) = RT$ for t > 0 [3.18]

$$T(r,0,t) = RT$$
 for $t > 0$ [3.19]

$$T(r,L,t) = RT$$
 for $t > 0$ [3.20]

Where: RT = retort temperature (°F)

The solution of this problem may be represented as the analytical solution for the temperature distribution in an infinite slab and an infinite cylinder given similar boundary conditions (Myers, 1971).

The solution for the infinite slab is:

$$\frac{T(y,t) - RT}{Ti - RT} = \frac{4}{\pi} \frac{\tilde{\Sigma}}{n=0} \frac{\sin((2n+1)\pi y/L)}{2n+1} \exp \frac{-(2n+1)^2 \pi^2 \alpha t}{L^2}$$
[3.21]

Where: L =length of the can (in)

- α = thermal diffusivity (in²/min)
- t = time (min)
- T(y,t) = surface temperature at point y at time t (°F)
 Ti = initial temperature (°F)
 y = axial position or slab thickness where the

and the infinite cylinder solution is:

$$\frac{\mathbf{T}(\mathbf{r},\mathbf{t}) - \mathbf{R}\mathbf{T}}{\mathbf{T}\mathbf{i} - \mathbf{R}\mathbf{T}} = 2\sum_{m=1}^{\infty} (\mathbf{J}_{0}(\lambda_{m}\mathbf{r}) / [(\lambda_{m}\mathbf{r}_{0}) \mathbf{J}_{1}(\lambda_{m}\mathbf{r}_{0})]$$
$$\exp \left[-((\lambda_{m}\mathbf{r}_{0})^{2} \alpha \mathbf{t}) / \mathbf{r}_{0}^{2}\right]$$
[3.22]

Where:
$$Jo(\lambda_{mo} r) = 0$$
 for $m = 1, 2, 3, ...$
 $r = radial position where 0.0 = center and$
 $r_o = surface$
 $J_o = Bessel function of the first kind of$
order zero
 $J_1 = Bessel function of the first kind of$
order one

In solving for actual temperatures in the finite cylinder, the product solution is used, i.e.,

$$\frac{T(r,y,t) - RT}{Ti - RT} = \frac{(T(r,t) - RT)(T(y,t) - RT)}{(Ti - RT)(Ti - RT)}$$
[3.23]

Equation [3.23] was used to determine predicted temperatures in computing thermal diffusivity values from standard heat penetration data (refer to Section 4.2.1.).

3.3 Lethality Evaluation By The General Method

3.3.1 The Lethal Rate Concept

Lethality is the integrated spore inactivation potential of a thermal process (including heating and cooling). By convention, it is usually

expressed (for low acid processes) as equivalent mintes at 250°F. The symbol for lethality, the F value, permits comparisons of the relative efficacies of varying processes. If the sterilizing effect of a thermal process is evaluated for a z value of 18°F (z representing the relative resistance of microorganism expressed in terms of the number of Fahrenheit degrees required for the thermal death time curve to traverse one log cycle) and a reference temperature of 250°F, the sterilizing value ($F \frac{18}{250}$) is referred to as the F_0 value.

The procedure used in applying the general method (Bigelow et al., 1920) requires the conversion of thermal center product temperatures measured or predicted at various time intervals throughout a process to lethal rates (LR):

$$LR = 10^{(T - T_{ref})/z}$$
[3.24]

Where: T = thermal center product temperature (°F)

A curve resembling that presented in Fig. 3.3 results when the lethal rates determined by equation [3.24] are plotted as a function of time. The area under this curve represents the lethality of the total process in terms of equivalent time (min) at the reference temperature.

3.3.2 Conversion of Initial Temperatures

To compare lethality values determined from various sets of heat penetration tests, all initial product temperatures were converted to



Product:	Cut green beans, No. 5 sieve, blanched 11/2 minutes at 180°F.
Can size:	603 by 700.
Fill:	75 ounces of beans. Cans filled with 2% salt brine.
Thermocouple position:	%" above bottom on longitudinal axis.
Come-up time:	101/2 minutes.
Retort temperature:	250°F.
Process times	20 minutes.

HEAT PENETRATION DATA		Lethai Rate	HEAT PENETRATION DATA		Lathai Rate	
Time, min	Temperature, 'F		Time, min	Temperature, 'F		
0	60 (IT)		19	24314	0.402	
ĩ	60		20	24334	0.450	
2	59		21	24434	0.512	
3	60		22	2454	C.545	
4	70		23	24534	0.580	
5	35		25	24544	0.620	
6	108		27	24634	0.660	
7	133		29	247	0.681	
8	156					
9	175		Steam o	iff and cooling begut	1	
10	192		at 301/2	minutes.		
11	207	0.004	301/2	247	0.581	
12	2171/2	0.015	31	247	0.681	
13	225	0.041	32	24674	0.660	
14	2301/2	0.083	33	23342	0.121	
15	2341/2	0.138	34	2171/2	0.016	
16	23814	0.222	35	203	0.002	
17	240	0.278	36	189		
18	2411/2	0.337				

Fig. 3.3 Lethal rate curve for an organism with a z of 18°F in 603 X 700 cut green beans (from, Townsend et al., 1968).

150°F by the following equation (Schultz and Olson, 1940):

$$NCT = RT - [(RT - NIT)/(RT - AIT)](RT - ACT)$$
[3.25]

Use of equation [3.25] assumes that product heating is by conduction and/ or convection (Ball and Olson, 1957).

3.3.3 Application of the Trapezoidal Rule

The numerical integration method employed in this study for computing the area under the lethal rate curve was the well-known Trapezoidal Rule:

Area =
$$(1/2) [(b-a)/n] [f(x_0) + f(x_1)] + (1/2) [(b-a)/n]$$

 $[f(x_1) + f(x_2)] + \dots + (1/2) [(b-a)/n] [f(x_n-1) + f(x_n)]$
= $(1/2) [(b-a)/n] [f(x_0) + f(x_1) + f(x_1) + f(x_2) + f(x_2) + f(x_2) + \dots + f(x_n)]$
= $[(b-a)/2n] [f(x_n) + f(x_n)]$
= $[(b-a)/2n] [f(x_0)] + [(b-a)/n] [f(x_1) + \dots + f(x_n-1)] + [(b-a)/2n] [f(x_n)]$
[3.26]

Where (as applied to lethal rate curves):

 $[(b-a)/n] = \Delta t = time between successive temperature$

measurements

$$f(x_0)$$
, $f(x_1)$, $f(x_2)$,... $f(x_n)$ = lethal rate of each measured
thermal center product temperature

Patashnik (1953) pointed out that if the first and last ordinates of the lethality curve are equal to zero, equation [3.26] can be simplified to:

area =
$$\Delta t(f(x_1) + f(x_2) + \dots + f(x_n-1))$$
 [3.27]

The lethality values calculated by this method were considered the "actual" geometric center F_0 values. The error estimate for this "general method" calculation is a function of the unit trapezoidal width, of the order of ± 0.10 % (Cedar and Outcalt, 1977).

4. METHODS

4.1 Heat Penetration Tests In A Hydrostatic Simulator

To properly characterize the influence of a high temperature cooling cycle and a gradual pressure diminution (inherently imposed by the discharge leg of a hydro) on overall process lethality, heat penetration tests were conducted in the hydrostatic simulator represented in Fig. 4.1. The tests were performed at the pilot plant scale to permit variable separation and precise monitoring. The main purpose of these simulations was to provide an empirical basis for comparison of the mathematical temperature predicting model developed in this study.

4.1.1 Kitchen Batch Preparation

The model food system selected was a condensed cream soup. It was chosen because of its highly reproducible conduction-heating/cooling characteristics and its relatively few ingredients. The product contained (in order of concentration): water, mushrooms, wheat flour, partially hydrogenated vegetable oils (soybean oil, palm, or cottonseed oil), cream, salt, modified food starch, dried dairy blend (whey, calcium caseinate), margarine (partially hydrogenated soybean oil, nonfat milk, water, natural flavoring, vitamin A palmitate), whey, monosodium glutamate, soy protein isolate, natural flavoring, yeast extract, and dehydrated garlic. The



thickners (wheat flour and modified food starch) were increased 15% to represent the least favorable factory product in terms of lethality evaluation.

Each heat penetration test employed copper-constantan, needle-type thermocouples (Ecklund, 1978), positioned at the geometric center of the 211 X 400 can (standard condensed soup can size). Four additional retort thermocouples were wired at the top and the bottom of the hydrostatic simulator, as well as immediately above the thermocouple can area, to measure the ambient temperatures throughout the simulator during processing. The thermocouple wires were connected to a high-sensitivity datalogger, which compensated, linearlized, and digitized the type T analog millivolt signal and simultaneously printed the temperatures at selected intervals, recording them on tape for subsequent computer analysis (Doric Scientific, 1980). The overall measuring error for this system was approximately $\frac{1}{2}$ 0.5°F.

The cans were filled to a constant weight (315 grams), and sealed at about $160^{\circ}F$ to achieve an actual minimum factory MIT (minimum initial temperature) of $150^{\circ}F$ at the start of the process.

The thermocouple cans were placed in a tray situated at the midpoint of the lower half of the hydro simulating vessel (Fig. 4.2). The can level in the tray was marked on the water gauge sight glass on the side of the retort (Fig. 4.1) for use as a reference point to track conversion from immersion to spray.

4.1.2 Hydrostatic Simulator Features

Because of the obvious complexities of moving a thermocouple equipped can successively through water immersion/steam/water immersion/water spray, the cans were fixed in a 60-inch diameter, 31-inch deep, modified FMC "Steritort" (Figures 4.1 and 4.2) in which the environment was successively changed. The infeed leg-immersion phase was not part of the simulations due to its negligible effect on lethality (during this phase of the hydrostatic process, the center-can temperatures are less than $200^{\circ}F$; by equation [3.24], the lethal rate is less than 0.002).

Accurate simulation of the hydrostatic process required a heated water reservoir (held at 10°F above actual exit leg temperatures) with constant temperature control (thermostatically controlled steam sparger), and a high capacity transfer/recirculation pump to effect an almost instantaneous transition from the steam phase to the hydrostatic exit leg phase of a commercial sterilizer. The heated water reservoir had a capacity (approx. 250 gallons) more than double the volume required to immerse the test cans in the simulator. The tempered "exit leg" water was recirculated during the pre-heat and processing period through the 4-inch line connecting the reservoir tank to the steam vessel to minimize convection and radiation heat loss during the transfer at the end of the steam process (Perkins, 1978). Regulation of the simulated exit leg temperature was accomplished by steam or cold water injection into the immersion water (Fig. 4.1), which was mixed by continuous recirculation through a centrifugal pump (referred to as the steritort pump).

The residence time in the discharge hydro leg was based on a typical factory ratio of hydro exit leg-hot spray capacity (Fig. 2.1), to steam



Fig. 4.2 INTERIOR VIEW OF THE HYDROSTATIC SIMULATOR

chamber capacity and process time calculated as follows:

[# carriers in exit leg + # carriers in hot-sprays][Process]
carriers in steam
minutes in immersion phase (exit leg) [4.1]

Applying typical values:

10.5 min

Make-up hot water for the hydrostatic system is continuously circulated to and from a feed-balance tank and introduced through a spray header at the top of the exit hydro leg tower. The result is nearequilibration between the leg-immersion water and the initial spray cooling water temperatures. Therefore, the number of carriers in the discharge leg used for this estimate included those in the area between the water immersion/spray interface; and the overbend between the hydrostatic exit leg tower and the spray cooling tower.

The hydro leg pressure gradient was simulated by compressed air introduced into the Steritort headspace above the immersion water level (Fig. 4.2). The pressure was diminished from the process steam pressure of 15 to zero psig at a rate comensurate with that experienced by the can in a factory hydro leg. It was essential to control this pressure gradient accurately, since an abrupt loss of external pressure would cause unaccountable, nonconductive heat transfer due to induced movement of the food in the can (Board et al., 1960). The final three minute passage of the cans through the cascading hydro, hot water supply to the top of the hydro tower was simulated by eliminating overriding air pressure, but maintaining the same immersion water temperature.

At the appropriate time for entering the cooling spray towers (for this experimental system, 10.5 minutes after "steam-off") the water level was rapidly dropped to the spray cooling level, and the cooling water spray header was activated (Fig. 4.2). The spray phase was continued until the thermal center reached a non-lethal temperature.

The environmental temperatures chosen for each simulated hydrostatic phase were based on thermocouple records collected from actual factory tests by an Acurex-Model 6000 Data Retriever System (Acurex Corporation, Autodata Division, 1978).

4.1.3 Test Run Procedures

A summary of the eight tests performed and their conditions (six hydrostatic tests simulating varying exit leg temperatures (130°F, 160°F, and 190°F) with gradient or constant external pressure, and two batch retort runs) can be found in Table 4.1. A description of the procedures (Fig. 4.1) involved in conducting these tests follows.

4.1.3.1 Hydrostatic Retort Simulations

A. Gradient Pressure

- The cans were held in pure steam for 65 minutes, timed from "steam-up".
- 2. The bottom bleeders were closed 20-30 minutes before the end of the process (to permit condensate build-up necessary to prime the Steri-

Table 4.1 Summary of hydrostatic and batch retort experimental

tests and cooling conditions.

BATCH RETORT CONTROLS	Test Cooling Conditions after a 65 min Process at 250°F	Time and in Eac Steam Process	Temperature th Processing Water Imm. Cool	e (min/°F) g Phase Spray Water Cooling	bbrevlated est Name
	Standard Batch Retort Heat Penetration Test - 65-70°F Immersion Cool, No Pressure	0-65 250	65-76.2 65-70		70 NP A
	Batch Retort - 65-70°F Immersion Cool, Constant Overriding Air Pressure (15 psig)	0-65 250	65-79.1 65-70	-	70CP
HYDROSTATIC SIMULATIONS	130 ⁺ 2°F Immersion Cooling for 10.5 Min, Gradient Pressure (15 to 0 psig) 95 [±] 5°F Spray Water Cooling	0-66 250	66-76.5 130	76.5-82.9 95 ⁺ 5	130œ (S)
	130 ⁺ 2°F Immersion Cooling Constant Pressure (15 psig) No Sprays	0-66 250	66-84 130		130CP (NS)
	160 ⁺ 2°F Immersion Cooling For 10.5 Min, Gradient Pressure (15 to 0 psig) 95 [±] 5°F Spray Water Cooling	0-66 250	66-76.5 160	76.5-87 95 + 5	160œ (S)
	160 ⁺ 2°F Immersion Cooling Constant Pressure (15 psig) No Sprays	0-66	66-90 160		160CP (NS)
	190 [±] 2°F Immersion Cooling For 10.5 Min, Gradient Pressure (15 to 0 psig) 95 [±] 5°F Spray Water Cooling	0-66	66-76.5 190	76.5-88.1 95 ⁺ 5	190œ (S)
	190 ⁺ 2°F Immersion Cooling Constant Pressure (15 psig) No Sprays	0-66	66-101 190		190CP (NS)

tort-centrifugal pump).

- 3. One minute prior to the end of the heat process:
 - a. Digital temperature recorder converted to continuous scan (readings taken every four seconds).
 - b. Tank water recirculation turned off.
- 4. Twenty seconds before cooling:
 - a. Top bleeder closed.
 - b. Air pressure controller set to 15 psig.
 - c. Steam turned off.
- 5. At 65 minutes (end-of-process):
 - a. Tempered tank water (pre-set at $115^{\pm}2^{\circ}F$, $140^{\pm}2^{\circ}F$, and $180^{\pm}2^{\circ}F$ for respective hydro exit leg simulations of $130^{\pm}2^{\circ}F$, $160^{\pm}2^{\circ}F$, and $190^{\pm}2^{\circ}F$) was immediately transferred from the water reservoir to the hydro simulator.
 - b. Steritort vessel vented as needed (cracking open top vent) when first
 bringing in the tank water to prevent
 pressure from rising above the processing steam pressure (15 psig).
 - c. As soon as the water appeared in the sight glass (ca. 10 seconds), recirculation of the "exit leg" water was started within the Steritort by activating the Steritort pump (bottom circulation only, no sprays, for best

temperature control).

- d. When the cans were immersed (water level slightly above reference mark on sight glass), the tank water valve was closed.
- One minute was allowed for temperature equilibration (transferred tank water contacting 250°F vessel) before making any temperature adjustments.
 - a. The high side of the simulated exit leg temperature range (132°F, 162°F, or 192°F) was approximated in the immediate post-steam minutes to mimic the higher leg temperatures actually experienced at the steam exit leg interface.
 - b. The simulated exit leg temperature of $130^{+}2^{\circ}$ F, $160^{+}2^{\circ}$ F, and $190^{+}2^{\circ}$ F for the first 10.5 minutes of cooling was maintained by injection of steam or cold water into the immersion water. A retort thermocouple, positioned at the bottom of the retort, was used as a reference (digital temperature printout) to predict any necessary temperature adjustments.
- 7. For the tests modeling actual hydro simulations, the external overriding air pressure was steadily dropped from 15 to zero psig in the first 7.5 minutes of the 130[±]2°F, 160[±]2°F, and 190[±]2°F immersion cooling phases. For the last three minutes of this high temperature immersion phase, the pressure was held at 0 psig (simulating the conditions the can would experience from the immersion/spray interface to the top of the

spray cooling leg tower). Tables of actual cooling times and proportional pressure regressions were prepared as soon as the exact "steam-off" time was known.

- 8. After ten minutes into the cooling cycle (half minute prior to the simulated spray cooling leg):
 - a. The water was rapidly drained below the cans (water level under the tray mark on sight glass).
 - b. This water was then directed from the Steritort pump to the Steritort spray (valve 7 open, valve 8 closed Fig. 4.1). This resulted in full sprays concentrated at the center of the tray or thermo<u>couple</u> area of the simulator vessel (Fig. 4.2).

9. At the end of the simulated immersion phase:

- a. Cold city water was rushed into the vessel by holding valve 4 open.
- b. The drain was worked simultaneously with the cold water value to keep the water level below the cans.
- c. Step 9b. was continued until the thermocouple at the bottom of the vessel dropped to 80°F.
- 10. After 10.5 minutes of cooling:
 - a. Spray water temperature was equilibrated to $95^{-}5^{\circ}F$.
 - b. The digital printouts of the two retort thermocouples wired in the thermocouple can area were constantly referenced for $95-5^{\circ}F$ readings.
- 11. Termination of the spray phase occurred when the product thermal center reached 200°F.

B. Constant Pressure

Steps 1 through 6a. were the same as in A. However, for these tests, the pressure was maintained at 15 psig (employing no sprays) until the center-can product temperature was approximately 200°F.

4.1.3.2 Batch Retort Tests

- A. Constant Pressure
 - At 65 minutes (end-of-process), 65-70°F cooling water (city water supply) was rushed into the vessel.
 - 2. Process steam pressure was maintained until the center-can temperature reached 200°F.
- B. No Pressure
 - 1. Same as A.1.
 - Overriding air pressure was dropped to zero psig by opening the top vent wide immediately at the start of cooling.
 - 3. Immersion cooling continued until all product thermocouples read 200°F.
- 4.2 Development Of The Hydro Lethality Prediction Model

A computer program was developed to predict the temperatures at any point within one quarter of a can, at any time, given the thermal diffusivity of the product, the initial product temperature, the dimensions of the can, and the time-varying boundary conditions of the sterilizer being evaluated (Appendix B). The center-can temperatures from this analysis were saved for later conversion to lethal rates, which were integrated over the entire heating and cooling period to yield a lethality value (F_{o}) for the thermal process in question.

4.2.1 Description of the Temperature Prediction Model

One quarter of the can was subdivided into a number of volume elements of small but finite size (Fig. 3.1) that were defined by can height (4.00inches), can width (2.6875 inches), and the size grid selected. For the 211 X 400 can size, a 10 X 10 matrix, as defended by Teixeira et al. (1969), was found more accurate and precise in predicting thermal center temperatures than a 5 X 5, 15 X 15, or 20 X 20 matrix system. Using twodimensional Cartesian geometry (to account for radial and vertical heat transfer only: circumferential heat transfer was disregarded), the grid evaluated by the program was identical to that depicted in Fig. 3.2. If one considers this grid to represent the right lower quarter of the can, the remaining three quarters (i.e., lower left, top right, and top left) are, by symmetry, mirror images of each other. Therefore, it is possible to generate the entire cylindrical container temperature profile on the basis of events in one quarter of the container.

If one were to use this model to evaluate nutrient degradation during a thermal process, it would be necessary to retain all the temperatures generated for each time interval through-out the container (Teixeira et al., 1969). But, for purposes of estimating thermal center lethality, the basis for the comparisons reported here, computation of the nodal point temperatures in a 10 X 10 matrix comprising one quarter of the container was sufficient (Fig. 3.1). Because of the symmetry of the

four half-height quadrants, the geometric center temperature profile during heating and cooling could be accurately estimated on the basis of a single quadrant.

The intersections of the networks drawn in Fig. 3.2, called nodal points, are defined in the program (Appendix B) by two subscripts, i and j, to indicate the row and the column of the point, respectively. For example, the descriptor (11,11) (i.e., with the boundary values set at one, the geometric center represents the eleventh nodal point from the can end and side) would identify the geometric center of the can. Each nodal point temperature was classified according to its location as interior or boundary nodal points, and calculated by a series of numerical operations (equations [3.7], [3.10], [3.12], and [3.13]) that approximated the general differential equation for two-dimensional, unsteady-state heat conduction in a finite cylinder (eq. [3.2]) using a finite difference technique (eq. [3.6]).

At the beginning of the process (identified as either a hydrostatic or batch retort simulation), all interior nodal points (identified in Table 3.1) were set to the initial product temperature, while the boundary nodes were set to the retort temperature at the start of processing ("steam-up"). The drop below factory minimum initial temperature $(150^{\circ}F)$, due to unavoidable delay in transporting the cans from the closing machine to the Steritort, was accounted for by extending the commercially prescribed process time from 65 minutes to 66 minutes for the hydro simulations only. The batch retort tests, conducted primarily for the determination of thermal diffusivity, were processed for 65 minutes.

As a basis for numerically predicting temperatures, it was necessary to determine two factors: (1) the diffusivity constant (α), and (2) the

time interval (Δt) between each temperature prediction.

The thermal diffusivity values were computed by a least squares program developed by Larkin (1981) (equations [3.21] to [3.23]), which employed the measured center-can temperatures of only the heating cycle of each simulation. The program, together with a sample output, is presented in Appendix C. The thermal diffusivity values calculated for each experiment showed excellent agreement among themselves $(0.0169in^2/$ min ± 2.5 %), and with those cited by Lenz and Lund (1977) (e.g., pureed peas - $0.0169in^2/$ min, and pureed lima beans - $0.0167in^2/$ min). The thermal diffusivity employed by this numerical heat transfer model for all the hydro temperature profile predictions was based on the mean value derived from the two standard batch, heat penetration tests - $0.0166in^2/$ min.

The numerical stability of the can model is dependent on the size of the container (can height and width), the corresponding selected element size (Δx , based here on a 10 X 10 matrix), the thermal diffusivity of the food product, and the specified time increment. The following stability equation for a two-dimensional system (Holman, 1972) limits the magnitude of the time increment that may be used relative to the element size:

 $\alpha \Delta t / (\Delta x)^2 \leq 2$ [4.2]

If the time increment is large (e.g., 0.20 minute with a radial increment (Δr) of 0.173 in. and a height increment (Δy) of 0.457 in.), the system becomes unstable and unacceptable oscillation (i.e., heat flowing in the direction of the temperature gradient) occurs (Orlowski, 1979). A time increment should be selected which maximizes accuracy, and minimizes digital computer running time. The most appropriate

value for the 0.134 in. radial and 0.201 in. height increment employed for these studies was the time increment of the recorded cooling environment, 0.0625 minutes, double the increments/minute recommended by Teixeira (et al., 1969). Since retort temperatures were only recorded every minute during the processing phase of the pilot plant tests, a polynomial interpolation function subroutine is called by the program to "create" additional data points for this phase only (Appendix D).

After the program initializes the boundary and interior nodes, time (Δt) is incremented by 0.0625 minutes, and the boundary nodes are set to a new environmental temperature. The interior nodal points are then predicted in the sequence: matrix center and midplane nodes (equations [3.7] and [3.12]), centerline nodes (equation [3.10]), and finally the single geometric center nodal point (equation [3.13]), which is retained by the program in a file for later lethality analysis (Fig. 3.2 and Table 3.1). The calculation of each successive node temperature is based on the previous time increment temperature of its surrounding nodes (Fig. 3.1). The new temperature distribution replaces the initial temperature distribution, and the procedure is repeated to predict temperature distribution after another time interval.

The program reads from appropriate tapes (Appendix B) the time/ retort temperature profiles as measured, in the case of a hydro simulation, for each of the three phases, processing, immersion cooling, and spray cooling (the environmental temperature profile for spray cooling was not needed for the batch-retort control tests). As time is incremented, all boundary conditions are changed to the surface condition equivalent to the environment under investigation. This method, which accounts for the several temperature transitions which occur in the

factory, was the most appropriate for testing the applicability of the model to hydrostatic processes. A typical temperature hydrostatic profile is illustrated in Fig. 4.3.

4.2.2 The General Method Program

F values were estimated by a general method program (Appendix B). This program (Perkins, 1980) reads the center-can, temperatures for each process phase, and calculates the lethal rate (equation [3.24]) for a given z value (e.g., 18° F), and reference temperature (e.g., 250° F). Lethality is integrated using a simplified Trapezoidal Rule (equation [3.27]) for a specified initial temperature (150° F) and retort temperature (250° F) to yield an F value.

4.3 Confirmation of the Models

The numerical series of solutions (equations [3.7], [3.10], [3.12], and [3.13]) employed in the transient heat transfer model (Appendix B) were verified by comparison with:

- Conduction-heating product center temperatures measured in cans subjected to precisely controlled temperature and pressure conditions (Table 4.1, and Fig. 5.3 - 5.17).
 - a. Heating profiles (Figures 5.3, 5.5, 5.7, 5.9, 5.11,
 5.13, 5.15, and 5.17):

Coordinate plots of actual center-can temperatures versus those predicted mathematically by the model



Fig. 4.3 Typical time/temperature profile for a commercial hydrostatic retort.

developed in this study showed essentially perfect agreement during the heating phase.

b. Cooling profiles (Figures 5.4, 5.6, 5.8, 5.10, 5.12, 5.14, 5.16, and 5.18):

Similar coordinate plots for cooling manifested exceptional agreement for conditions of constant overriding pressure (Figures 5.6, 5.14; particularly Figures 5.10 and 5.18). In simulations with no or gradient overriding air pressure (respectively, Fig. 5.4 - a batch retort test; and Figures 5.8, 5.12, and 5.16 - hydro simulations), the correlation between actual and predicted thermal center temperatures was acceptable in the critical area of highest lethality (i.e., above 240°F). The curves, unfortunately, do not tract each other as well below 240°F when the external overriding air pressure was dropped to zero (after 7.5 minutes).

- 2. Thermal center temperatures predicted by heating and cooling condition simulations using the analytical solutions described in equations [3.21], [3.22], and [3.23]. The initial and boundary conditions assumed for these two profile predictions were:
 - a. Heating profile (Fig. 4.4):
 - (1) Retort temperature = $250^{\circ}F$
 - (2) Initial product temperature = 150°F
 - (3) Thermal diffusivity = $0.0166 \text{ in}^2/\text{min}$

b. Cooling profile (Fig. 4.5):

- (1) Cooling water temperature = $65^{\circ}F$
- (2) Final center can temperature = $245^{\circ}F$
- (3) Thermal diffusivity = $0.0166 \text{ in}^2/\text{min}$

The two sets of profiles generated by the analytical and numerical solutions demonstrated essentially perfect agreement.

The lethality estimation program (Appendix E) was verified by hand calculations utilizing equations [3.24], [3.25], and [3.27].



Fig 4.4



5. RESULTS

The influence of cooling water temperature and pressure on the cooling rate of cylindrical containers of a conduction-heating food is depicted in Fig. 5.1. The measured thermal center temperatures plotted versus time on coordinate paper for a standard batch retort test (as described in Sec. 4. 1. 3. 2. B.) and a hydrostatic retort simulation (Sec. 4. 1. 3. 1. A.) illustrates the degree to which the temperature and pressure patterns that are characteristic of hydrostatic retorts retard the decline of center-can temperature (refer to section of plot circled in Fig. 5.1).

When, for example, the exit hydro leg water is maintained at 190°F, the thermal center product temperature remains above 245°F for about eight minutes. The center temperature in this canned product heated identically to (as nearly as possible) the same final center can temperature (FCCT), and cooled in 70°F water with no overriding pressure, stays at 245°F or above for less than three minutes. The five minute temperature discrepancy represented by the more precipitous center temperature decline under retort cooling conditions is equivalent, in terms of minutes at 245°F, to 2.6 $F_{\rm o}$ units (62%) of unrealized cooling lethality.

5.1 Effect of Cooling Water Temperature on Lethality

The independent effect of cooling water temperature on process lethality was examined in this study by means of four control simulations:



Fig. 5.1 The effect of water temperature and pressure on product cooling rate.

all with constant pressure (15 psig) during the entire water immersion cooling phase (no sprays); and with independently varying cooling water temperature of, respectively, 70°F, 130°F, 160°F, and 190°F (illustrated in Figs. 5.6, 5.10, 5.14, and 5.18). Progressive F_0 value increases were observed as the cooling water temperature was increased (Table 5.1).

The 160°F simulations, the first tests conducted, were carried out before the procedures described in Sec. 4.1.3.1 were fully mastered. Therefore, the negligible difference demonstrated between the F_0 values obtained during cooling for the 130°F and 160°F constant pressure control simulations may not be accurate.

There is no evident relationship between either individuals or pairs representing the two conventional slope-characterizing factors, jc and fc (Fig. 5.2) that suggests a means for predicting the relative effect of cooling water temperature on thermal center lethality, (i.e., neither value varies consistently with respect to increasing cooling temperature). Thus, cooling parameters measured at one temperature (e.g., 70°F), cannot be applied to predict cooling lethality when a significantly higher cooling media temperature is employed.

This limits the practical applications of Hayakawa's (1977) process calculation method, which computes cooling lethality by means of a system of equations utilizing experimentally determined jc/fc values. Hayakawa's method can only confirm the general method lethality of a given experimental cooling condition. It cannot predict, on the basis of this experiment, the lethality that would accrue under other cooling conditions.
Table 5.1 Actual (General Method) Vs. Predicted (Ball's Formula Method,

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Penetration Data*	£ \	fc	G	E	Method	FOCT	G.M.	Model	G.H.	Model	G.M.	Model
70%	38.5	1.06 70.6	250	65	12.0	12.0 246.4	10.2	10.6	2.5	4.7	12.7	15.3
7002	1.88	1.17	250	65 150	9.11	14.6 246.7	10.2	10.3	4.7	5.0	14.9	15.3
130æ(S)	1.77	1.23	250	66	12.6	12.3 246.0	1.01	1,6	4.5	5.5	14.6	14.6
130CP (NS)	1.92 36.7	1.35	250	66 150	13.3	15.9 247.0	1.11	11.3	6.1	6.1	17.2	17.4
160œ (S)	1.66	1.17 B6.4	250	66 150	13.7	16.0 246.5	10.7	11.6	6.3	7.0	17.0	18.6
160CP (NS)	1.74	1.39	250	66 150	13.0	13.0 245.7	9.6	0.11	6.0	6.9	15.8	17.9
190æ (S)	1.70	1.18	250 1904	66 150	12.5	18.0 246.3	10.2	0.11	1.7	7.7	17.3	18.7
190CP (NS)	1.81 37.8	1.59	250 190	150	13.0	17.0 246.7	11.0	9.0I	1.1	7.8	18.7	18.7

* Abbreviated Test Name Used in Table 4.1 ** 100 Sprays

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Plotting of heating and cooling heat penetration data.

Fig. 5.2

5.2 Effect of Varying External Pressure on Lethality

To determine the effect of the imposed gradient pressure of the hydrostatic exit leg on the rate of cooling, tests comparing gradient and constant air pressure were performed for three discharge leg cooling water temperatures: 130°F, 160°F, and 190°F. The effects of these pressure conditions are reflected in the jc and fc values cited in Table 5.1

When a control condition of constant pressure during cooling was maintained, the cooling lag (jc) significantly exceeded that in a gradient pressure environment. The exact reason(s) for this retarded cooling can only be conjectured in the absence of transducer-measured internal can pressures during cooling.

During the heat process, expansion of product-entrained gases and headspace air retained after sealing is approximately balanced by the external steam pressure. When cooling is initiated, without external pressure control, this counter-balance is instantly dissipated, and the can ends are free to buldge. This allows rapid product expansion and attendant internal turbulence, which circulates the coolest product away from the geometric center (exaggerated in Fig. 5.4 for a "blow down" retort test). The effect of dropping pressure at the start of cooling proved, in the extreme case (70NP - Table 5.1), to reduce the spore inactivation potential of the cooling process phase 47% (i.e., cooling cycle F_0 values for 70 NP and 70CP were, respectively, 2.5 and 4.7).

Whether some convection currents also result from the external gradient pressure (as experienced in the exit hydro leg; Figs. 5.8, 5.12, and 5.16), cannot be deduced in the absence of internal pressure measurements during heating and cooling. The lag values (jc) of the gradient -

pressure cooling curves are, however, consistently smaller and the slope values (fc), larger than the respective lag and slope parameters associated with constant-pressure cooling. As a result, the cooling F_0 values were generally smaller during gradient, relative to pressure cooling, although the 160°F test data are again equivocal, in that the gradient cooling pressure test indicated more lethality than the constant pressure test.

An examination of the gradient versus constant pressure cooling curves (Figs. 5.7-5.18), within any of the temperature groups, reveals no obvious temperature-related basis for predicting the combined effects of temperature and pressure on cooling rates in a viscous, conductiveheating liquid food (i.e., a condensed cream soup). Process calculation methods (Ball and Olson, 1957; Hayakawa, 1977) employing constant jc and fc values <u>cannot</u>, therefore, be used to predict the effect of a pressure condition other than that used experimentally.

5.3 Comparison Of The Model To Conventional And Other Methods

The reliability of the mathematical process evaluation method developed in this study was tested by analyzing the degree of accordance between the mathematically predicted F values and the true F values based on measured temperatures. Sterilizing parameters calculated by a general method program (Appendix E) from measured geometric center temperatures (Sec. 4.1.3) and from center-can temperatures (Sec. 4.2.1) predicted by the mathematical model are compared in Table 5.1.

Heating, cooling, and consolidated F values predicted by this model are also compared (Table 5.1) with "total" F values computed from the same experimental temperature data by Ball's formula method (1923);

generally employed by industry) and Hayakawa's (1977) analytical method (the most recently developed technique for process lethality evaluation).

Represented also in this table (5.1) are the slope/intercept characteristics of each experimental heating (jh/fh) and cooling (jc/fc) cycle, the initial product temperature (mathematically standardized at 150°F; Schultz and Olson, 1940), the retort temperature, and the cooling water temperatures for the hydro simulations.

5.3.1 Ball's Formula Method Vs. Actual Lethality

Prediction of an F value by Ball's formula method requires that the following parameters be known: retort temperature, initial product temperature, cooling water temperature, processing time, fh, jh, and the z value. The lethality values estimated by Ball's formula (Table 5.1), slightly understated the lethality (5.5% lower than actual general method values) for the standard retort test, with a significant variation identified for the retort test applying constant pressure (20% lower).

In predicting lethality values for the hydrostatic simulations, a cooling water temperature of 120°F was assumed (highest cooling water temperature permitted by Ball's charts). The estimated sterilizing values varied insignificantly with changing cooling water temperature and pressure conditions because of the constraints imposed by Ball's assumptions of a constant jc of 1.41 (jc range in these tests: 1.17-1.59), and mirror image heating and cooling slopes (experimental heating and cooling slope temperature discrepancy range: 16.8-55.3°F, with fc invariably exceeding fh).

A Ball equation lethality estimate for the hydro simulation testing

an exit leg temperature of 190°F was, for example, 30% lower than the actual general method value. This method does not, therefore, account well for hydrostatic retort cooling conditions.

5.3.2 Hayakawa's Method Vs. Actual Lethality

The following information was employed in the application of Hayakawa's heat process evaluation program for purposes of critical point lethality estimation: initial temperature, constant cooling and heating media temperatures, z, fh, jh, fc, jc, and the final thermal center temperature at the end of the heating phase. This last value was set equivalent to the FCCT determined by the general method (Appendix E) for an initial product temperature of $150^{\circ}F$ (eq. [3.25]).

The total lethality values predicted by Hayakawa's method showed fair agreement. F values for the standard heat penetration test (70NP) and the batch retort test with controlled pressure cooling (70CP) were, respectively, 5.5% and 2.0% lower than the general method values. The sterility values determined by this method for the hydrostatic simulations were less accurate: 7.6-9.0% lower for the constant pressure tests and as much as 16% lower for the gradient pressure tests (i.e., 130GP(S)).

As a method of estimating lethality from known heat penetration tests, Hayakawa's method accounts fairly well for varying cooling water temperature and gradient pressure (i.e., characteristics of hydro cooling) by incorporating actual jc and fc values into his lethality computation. But, in predicting hydro retort lethality on the basis of standard heat penetration data alone (Table 5.2), Hayakawa's empirical approach is not much better than Ball's formula method. Table 5.2 Predicting hydrostatic retort lethality values by Hayakawa's method using standard heat penetration heating and cooling parameters.

Test Conditions	Total Lethal Value (F _o)
130 œ(s)	12.6
130 CP (NS)	
160 GP(S)	13.5
160 CP (NS)	13.5
190 œ(s)	14.7
190 CP (NS)	

The jh,jc,fh,fc, and FCCT values used for the above computations were, respectively, 1.77, 1.06, 38.5 min, 70.6 min., and 246.4°F. The initial temperature was set equal to 150°F, and the cooling water temperature was assumed to be equivalent to the specified immersion cooling temperature. 5.3.3 The Model Vs. Actual Lethality

Sterilizing values derived from the model's predicted center-can temperatures are cited in Table 5.1 for the individual heating/cooling phases of the experimental tests conducted in this study, and for the total process. The predicted temperatures were based solely on a standard heat penetration thermal diffusivity value for a condensed cream soup (0.0166 in²/min), and simulated factory, sterilizer-surface temperatures. These center-can temperatures demonstrated (in every case but the 160°F tests and the "blow-down" cooling batch-retort test) excellent agreement with actual cooling temperature profiles (Figs. 5.3-5.18).

Lethality calculations determined from the respective model-generated temperatures also correlated well with F values based on measured temperatures (again, with the exception of the 70NP test, and the 160°F set of hydro simulations). F_{o} values for conditions of constant overriding air pressure during cooling were 2.6% higher for the batch-retort test (70CP) and 0-1.1% higher for the hydro simulations. The lethality values predicted by the model for the gradient pressure hydro simulations showed the best agreement of any of the process calculation methods evaluated in this study (0-7.5% higher).

These results confirm the unique applicability of the model developed herein to both heating and step-change cooling environments (e.g., hydrostatic retort processes). Using this model, lethality values may be predicted for any set of processing conditions once the rate of heat penetration (thermal diffusivity) for the canned, conduction-heating food has been determined.



HEATING CURVE PRESSURE BATCH RETORT -CWT=70 NO



































Fig. 5.17





6. CONCLUSIONS

- 1. The higher the cooling water temperature, the greater the spore inactivation contribution of the cooling cycle. Such an effect could not be reliably evaluated by the empirical cooling curve lag (jc) and slope (fc) values, which demonstrated no obvious trends when plotted from varying cooling water heat penetration data. Thus, cooling parameters measured at one temperature (i.e., a standard retort test cooled at a constant 70°F), cannot be applied to predict cooling lethality when a significantly hotter, step-changing cooling (hydrostatic retort) environment is employed.
- 2. F values calculated from heat penetration data associated with gradient pressure and a range of cooling water temperatures were 15% and 7% lower (130°F and 190°F, respectively) than those determined from constant pressure tests. The reason for these discrepancies is unclear without a better understanding of the exact pressure changes occurring within a canned conductionheating product during heating and cooling.
- 3. Ball's formula method predicted F values that were as much as 30% lower than F values determined by the General Method using

experimentally measured product temperatures.

- 4. Hayakawa's method provides reasonable lethality estimates when the heating and cooling product profiles for a thermal process can be characterized. In cases where the heat penetration data are not available (e.g., converting a batch retort process to a hydrostatic retort process), Hayakawa's empirical estimates are no better than those projected by Ball's formula method.
- 5. Thermal center temperatures associated with conduction product cooling can be accurately predicted by the model developed in this study for varying boundary and external pressure conditions. These predictions can be based solely on standard retort, heat penetration tests (i.e., cooling water temperatures of 65-85°F), and simulated factory, sterilizer-surface temperatures.
- 6. Complicated process deviations involving multiple environmental temperature changes presently can be evaluated only by tedious and expensive physical simulations. The results of this study (Table 5.1) indicate that the model currently developed has the potential, not shared by any other thermal process calculation method, of predicting the response of the center-can temperatures to normally and abnormally (i.e., during process irregularities) varying environments.

7. FUTURE RESERACH

Subjects for future study are:

- 1. To validate the computer method developed in this project with microbiological tests (e.g., inoculated tests).
- 2. To evaluate the potential of applying the model to process irregularity lethality predictions. By changing the time/temperature sterilizer profile read by the program to the actual time/temperature, surface profile experienced during the aberrant process (indicated on the process-recording chart), the lethality value may be accurately computed, and not "guess estimated", as unfortunately is frequently the case today.
- 3. To quantify the effect of varying external pressure conditions on the cooling rate of conduction heating/cooling food products, and to develop compensating constants for use in the model designed in this study.
- To measure the influence of high cooling water temperature and varying external pressure conditions for a wide variety of container sizes and food products.

5. To investigate possible means of utilizing the model developed herein to optimize the thermal process design of hydrostatic retort sterilizers, thus, reducing the energy requirements per can. APPENDICES

APPENDIX A

ENERGY COST SAVINGS EQUATIONS

Appendix A - Energy Cost Savings Equations

(Using Typical Values for a Single Hydrostatic Retort)

- 1. (15% Reduction) (66 min process) = 9.9 min reduction or a 56.1 min process
- 2. 1265 carriers in steam = 25,300 cans in steam at 20 cans/carrier
- 3. a. $\frac{25,300 \text{ cans}}{66 \text{ min}} = 383.33 \text{ cans/min}$
 - b. $\frac{25,300 \text{ cans}}{56.1 \text{ min}} = 450.98 \text{ cans/min}$
 - c. 450.98 383.33 = 67.65 cans/min improvement
- 4. [80% line efficiency] (67.65) = 54.12 cans/min improvement (capacity of the line relative to stops and starts)
- 5. Converting to pounds of product:

 $\frac{10.75 \text{ oz. in a typical can}}{16.0 \text{ oz/lb}} = 0.67 \text{ lbs}$ So:(0.67 lbs) (54.12 cans/min) = 36.36 lbs of product/min improvement

6. Converting to lbs of product/year:

(60 min/hr) (16 hrs/day) (260 working days/year) = 249,600 min/year So: (36.36 lbs of product/min) (249,600 min/year) = 9,075,456 lbs/year improvement 7. Converting extra lbs of product to conserved lbs of steam:

448.22 BTU's /lb product for a hydro retort

Since:

368.18 BTU's/lb product in an agitating cooker (Singh et al., 1980) And:

[56 (Factor for a hydro) 46 (Factor for a continuous sterilizer] (368.18) = 448.22 BTU's/lb product for hydro (Singh, 1977)

So:

(9,075,456 lbs/year) (448.22 BTU's/lb)= 4.07 X 10 BTU's/year

8. In dollars:

Since:

Then:

(29,304 Gal Fuel) (\$1.20/Gal Diesel) = \$35,165

9. If based on one quarter natural gas (typical for American industry):

$$\frac{(7.2 \text{ Gal})(\$1.20/\text{Gal})}{10^6 \text{BTU's}} = \$8.64/\text{million BTU's for Fuel Oil}$$

Estimated \$4.00/million BTU's for Natural Gas:

Then:

(.75)(8.64) + (.25)(4.00) = \$7.48/million BTU's

So:

APPENDIX B

COMPUTER PROGRAM TO PREDICT THE THERMAL CENTER, PRODUCT TIME/TEMPERATURE PROFILE FOR A HYDROSTATIC OR OTHER TYPE RETORT 110=C TRANSIENT NUMERICAL HEAT TRANSFER MODEL FOR 120=C FOOD PRODUCTS HEATING BY CONDUCTION 132=0 ADAPTED TEIXEIRA ET AL. (1969) 140=0 BY KATHLEEN E. YOUNG 15C=C 170=C THIS PROGRAM PREDICTS THE TIME/TEMPERATURE PROFILE AT ANY FOINT WITHIN 193=C GNE QUARTER OF A CAN+ AT ANY TIPE+ FOR A CONDUCTION-HEATING FOOD PRO-193=C DUCT PROCESSED IN A BATCH OR HYDROSTATIC RETORT. THE DATA REGUIRED ARE 200=C THE THERMAL DIFFUSIVITY OF THE FOOD PRODUCT. THE INITIAL PRODUCT TEM-213=C PERATURE, THE DIMENSIONS OF THE CAN, AND THE TIME-VARYING BOUNDARY CON 220=C -DITIONS OF THE STERILIZER BEING EVALUATED. THE GEGMETRIC CENTER TEM-23C=C PERATURES ARE THE ONLY TEMPERATURES RETAINED BY THIS PROGRAM (TAPE9), 240=C WHICH ARE LATER CONVERTED TO LETHAL PATES BY A SEPARATE GENERAL 250=C METHOD PROGRAM (GENM2). TO DETERMINE NUTRIENT DEGRADATION DURING 4 263=C THERMAL PROCESS. IT WOULD BE NECESSARY TO SAVE ALL THE TEMPERATURES 270=C GENERATED FOR EACH TIME INTERVAL THROUGHOUT THE CONTAINER (PEFER TO 280=0 TEIXEIRA ET AL. (1969)). 230=C 300=0 ONE GUARTER OF THE CAN IS SUBDIVIDED INTO A NUMBER OF VOLUME ELEMENTS 313=C THAT ARE DEFINED BY THE CAN HEIGHT, CAN WIDTH, AND GRID SIZE (10 X 10 323=C MATRIX). THE INTERSECTIONS OF THIS MATRIX NETWORK, CALLED NODAL POINTS 331=C ARE DEFINED IN THE PROGRAM BY TWO SUBSCRIPTS. I AND J. TO INDICATE THE 34G=C ROW AND THE COLUMN OF THE POINT, RESPECTIVELY, FOR EXAMPLE, THE DES-351=C CRIPTOR (11,11) IDENTIFIES THE GEOMETRIC CENTER OF THE CAN. EACH NODAL 360=C POINT TEMPERATURE IS CLASSIFIED ACCORDING TO ITS LOCATION AS INTERIOR 375=C OR BOUNDARY NODAL POINTS. AND CALCULATED BY A SERIES OF NUMERICAL 3:0=C OPERATIONS THAT APPROXIMATE THE GENERAL DIFFERENTIAL EQUATION FOR TWO-390=C DIMENSIONAL. UNSTEADY-STATE HEAT CONDUCTION IN A FINITE CYLINDER USING 351=C A FINITE DIFFERENCE TECHNIQUE. 400=C 413=C AT THE REGINNING OF THE PROCESS, ALL INTERIOR NODAL POINTS ARE SET TO 420=0 INITIAL PRODUCT TEMPERATURE WHILE THE BOUNDARY NODES ARE SET TO THE 430=C RETORT TEMPERATURE. AFTER THE PROGRAM INITIALIZES THE BOUNDARY AND IN-44C=C TERIOR NODES. TIME IS INCREMENTED BY A CHOSEN TIME. AND THE BOUNDARY 450=C NODES ARE SET TO A NEW ENVIRONMENTAL TEMPERATURE. THE PROGRAM READS 469=C FROM APPROPRIATE TAPES (TAPE1.TAPE2.TAPE3) THE TIME/RETORT TEMP. PRO-473=C FILES AS MEASURED. IN THE CASE OF A HYDRO SIMULATION. FOR EACH OF 472=C THE THREE PHASES. PROCESSING. IMMEPSION COOLING. AND SPRAY COOLING 485=C (A TEMPERATURE PROFILE FOR SPPAY COOLING FOR A BATCH RETORT WOULD 490=C NOT EXIST). AS TIME IS INCREMENTED, ALL BOUNDARY CONDITONS ARE 500=0 CHANGED TO THE SURFACE CONDITION EQUIVALENT TO THE ENVIRONMENT UNDER 510=C INVESTIGATION. FOR CASES WHERE THE SURFACE PROFILES WAS NOT RECORD. 523=C ED EVERY G.0625 MIN. A POLYNOMIAL INTERPOLATION FUNCTION SUBROUTINE 533=C IS CALLED BY THE PROGRAM (TERP1) TO "CREATE" ADDITIONAL DATA POINTS. 550=C 36"=C THE INTERIOR NODAL POINTS ARE PREDICTED IN SEQUENCE: MATPIX CENTER/ 374=C MIOPLANE NODES, CENTERLINE NODES, AND FINALLY THE CENTER NODAL POINT. 339=C THE CALCULATON OF EACH SUCCESSIVE NODE IS BASED ON THE PREVIOUS TIME SUC=C INCREMENT TEMPERATURE OF ITS SURROUNDING NODES. THE NEW TEMPERATURE 600=C DISTRIBUTION REPLACES THE INITIAL TEMPERATURE DISTRIBUTION, AND THE 619=C PRUCEDURE IS REPEATED TO PREDICT TEMPERATURE DISTRIBUTION AFTER 620 = C ANOTHER TIME INTERVAL. 630=C++++ 64C=C VARIABLE LIST - TEMPERATURE DISTRIBUTION PREDICTION 650=C 563=C TDIFF=THERMAL DIFFUSIVITY VALUE (CM2/MIN) 673=C CH=CAN HEIGHT (INCHES) 683=C CW=CAN VIDTH (INCHES) 690=C RERADIUS AT ANY POINT 700=C RTOT=RADIAL DISTANCE FROM THE CENTER LINE 710=C ZTOT=VERTICAL DISTANCE FROM THE MIDPLANE

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720=C RINCR=RADIAL INCREMENT SIZE (CM)
7:3=C ZINCR=VERTICAL INCREMENT SIZE (CM)
740=C RT=RETORT TEMPERATURE, F
754=C ART=AVERAGE RETORT TEMPERATURE, F
76?=C CUTI=COOLING WATER TEMPERATURE - IPMERSION PHAST. F
770=C ACWTI=AVERAGE COOLING WATER TEMPERATURE - IMMERSION PHASE, F
780=C CWTS=COOLING WATER TEMPERATURE - SPRAY PHASE, F
790=C ACWTS=AVERAGE COOLING WATER TEMPERATURE - SPRAY PHASE. F
SCJ=C TI=INITIAL PRODUCT TEMPERATURE. F
910=C TINCR=TIME INCREMENT (MIN)
A20=C NH=NUMBER OF VERTICAL INCREMENTS
83:=C NR=NUMBER OF RADIAL INCREMENTS
940=C I=SEQUENCE OF RADIAL INCREMENTS
859=C J=SEQUENCE OF VERTICAL INCREMENTS
960=C TA=OLD TEMPERATURE 4T EACH POINT
870=0 TB=NEW TEMPERATURE AT EACH POINT
822=C PROCESS=PROCESSING TIME FROM STEAM UP TO BEGINNING COOL (MIN)
890=C COOLI=TIME OF IMMERSION COOLING (MIN)
900=C COCLS=TIME OF SPRAY COOLING (MIN)
31J=C CCT=CENTER CAN TIMENTEMPERATURE PROFILE (SEC/C OR F)
929=C ICUT. IPROCESS. ICOOLI. AND ICOCLS ARE COUNTERS
937=C INT=NUMBER OF REQUESTED INTERPOLATIONS
940=0++++
                                                  ********
950=
          PROGRAM TMPPRED(INPUT, OUTPUT, TAPE1, TAPE2, TAPE3
         + • TAPE6=OUTPUT • TAPE8 • TAPE9)
96C=
970=C
986=
          DIMENSION T4(25,25), TB(25,25), T(25,25)
990=
          DIMENSION TIME (750), TEMP (750), CCT (1000,2)
1000=
           CHARACTER CTYPE+3, ONE+1, TVC+1
1010=
           DATA ONE/ +1 +/ + TH0/+2+/
1920=
           IGNOTI=0
1030=C
1040=C READ IN CONSTANT VALUES
1050=C
1 760=
           WRITE(6,50)
           FORMAT(*1*,//,* IS THIS & BATCH RETORT OR HYDROSTATIC COOKER*
1070=50
          +, SIMULATION? ,/, (1=BATCH, 2=HYDROSTATIC))
1380=
1090=
           PRINT 60
1100=60
           FORMAT(1(/))
           READ(+.+(A)+) CTYPE
1110=
1120=
           WRITE(6.70)
           FORMAT( * +,/, * ENTER THE TIME INCREMENT (MIN) FOR EACH OF THE *
1 130=70
1140=
          +, FOLLOWING PROCESS PHASES: *,/.* PROCESSING. *,
1150=
          + IMMERSION COOLING, AND SPRAY COOLING, /... (SUBSTITUTE O FOR)
1160=
          +, PHASES OMITTED) *)
1170=
           PRINT 60
1130=
          . READ # TINCR1 TINCR2 TINCR3
           WRITE(6,80)
1190=
1200=80
           FORMAT( +,/, ENTER CAN HEIGHT AND WIDTH (INCHES) +)
           PRINT 60
1210=
1220=
           READ +.CH.CW
           WRITE(6,120)
1230=
           FORMAT(* *,/,* ENTER THE THERMAL DIFFUSIVITY VALUE (CM2/*
1240=120
1250=
          +. * MIN) *)
1260=
           PRINT 60
           READ *.TDIFF
1270=
1290=
           WRITE(6.130)
1290=130
           FORMAT( +,/, + ENTER INITIAL PRODUCT TEMPERATURE (F)+)
1300=
           PRINT 60
1310=
           READ *.TI
1320=C
```

1330=C READ IN TIME/TEMPERATURE ENVIRONMENT PROFILE OF EACH PROCESS PHASE 1 24 2 30 1350=C PROCESSING PHASE 1360=C 1370= ICUT=3 IPROCES=0 1380= 1390= ICOOLI=0 1400= ICOCLS=0 CALL READ(1,TIME,TEMP, IPROCES,ICUT) 1410= 1420=C 1430=C IMMERSION COOLING 1440=C 1450= ICOOLI=IPROCES CALL READ(2.TIME.TEMP.ICOOLI.IPROCES) 1450= 1470=C 1450=C SPRAY COOLING 1490=C IF (CTYPE.EG.ONE) GO TO 21 15603 IF(TINCR3.E0.J.0) GO TO 21 1510= ICOOLS=ICOOLI 1520= 1533= CALL READ(3.TIME.TEMP.ICOOLS.ICOOLI) 1540=C 1550=C PREDICT CENTER CAN TEMPERATURES FOR EACH PHASE OF THE PROCESS 1560=C 1570=C PROCESS PHASE 1590=C 1590=21 CALL TEMPGIS(TIME.TEMP.IPROCES.ART.CCT.ICUT.CW.CH.TDIFF 1600= +,TI,S,TINCR1,TINCR5,IGNOTI) 1610= IF(S.LE.2.0) GO TO 2 1620= WRITE(6+4)S GO TO 3 1630= CONTINUE 1640=2 1650=C 1667=C IMMERSION COOLING 1679=C 1680= IGNOTI=1 169 2= CALL TEMPOIS(TIME, TEMP, ICCOLI, ACWTI, CCT, IPROCES, CW 1700= +,CH,TDIFF,TI,S,TINCR2,TINCR5,IGNOTI) 1713=C 1720=C SPRAY COOLING 1730=C IF(CTYPE.EQ.ONE) GO TO 36 1740= 1750= IF(TINCR3.E0.0.0) GO TO 36 CALL TEMPOIS(TIME, TEMP, ICOOLS, ACWTS, CCT, ICOOLI, CW 1760= 1770= +.CH.TDIFF.TI.S.TINCR3.TINCR5.IGNOTI) 1780=C 1790=C RESULTS OF TEMPERATURE DISTRIBUTION AND NUTRIENT RETENTION PREDICTION 1300=C 1 210 =C 1920=C WPITE CENTER CAN TIME/TEMPERATURE PROFILE TO FILE 9 CALLED CCTSAVE 1930=C 1940=36 **REVIND 9** IF(ICOOLI.GT.ICOOLS) ICOUNT=ICOOLI 1350= IF(ICOOLI.LT.ICOOLS) ICOUNT=ICOOLS 1360= 1370= 00 10 II=1, ICOUNT ¥RITE(9+8) (CCT(II+JJ)+JJ=1+2) 1830=10 FORMAT(* STABILITY CRITERION NOT MET, S= *+F11+7) 1390=4 FORMAT(2F1C+2) 1900=8 1910=3 CONTINUE 1920= STOP 1930= END

,
1740=C 1950=C. 1960=C SUBROUTINE THAT READS IN TIME/TEMPERATURE STERILIZER PROFILES 1370=C OF THE VARIOUS PHASES OF A PRCCESS 1980=C 1990=C SUBROUTINE READ(IFILE.A.B.I.IPOS) 2000= 2010= DIMENSION A(750).8(750) 2020= N085=0 2)30= REWIND IFILE DO 10 I=I+1+750 2540= 2050= READ(IFILE. +. END=15) A(I). E(I) 2060= KOBS=NOBS+1 2 179=10 CONTINUE WRITE(9,20) (A(J),B(J),J=IPOS+1,NOBS+IPOS) 2080=15 2090=20 FORMAT(2F10.2) 2100= I=NO8S+IPOS 2110= RETURN 2120= END 2130=C 2140=C 2150=C SUBROUTINE TO CALCULATE THE TEMPERATURE DISTRIBUTION AND SLOVEST 2160=C HEATING POINT TEMPERATURES DURING EACH PHASE OF THE PROCESS 2170=C 2160=C SUBROUTINE TEMPDISCTIME, TEMP, ICOUNT, AMEDIUM, CCT, NPOS, CW 2190= 22:0= +,CH,TDIFF,TI,S,TINCR,TINCR5,IGNOTI) 2210= DINENSION TIME (750), TEMP (750), CCT (1000,2) 2220= DIMENSION TA(25,25),TB(25,25),T(25,25) 2239=C 2240=C EVALUATE ALL CONSTANTS 2250=C 2260= NR=10 2270= NH=10 2280= SUMRT=0.0 2290= RTOT=CV+2.54/2.0 2360= RINCR =RTOT/NR ZTOT=CH+2.54/2.0 2310= 2320= ZINCR=ZTOT/NH 2333= NP 1=NR+1 NR2=NR+2 2340= 2350= NH1=NH+1 2360= NH2=NH+2 237C=C 2380=C DETERMINE IF STABILITY CRITERIA MET 2390=C 2400= TINCR5=0.0625 2410= 0=TDIFF+TINCR5/ZINCR++2.0 2420= P=(TDIFF+2.0+TINCR5)/RINCF++2.0 q=TDIFF+TINCR5/(2.0+RINCR) 2430= S=TDIFF+TINCR5/RINCR++2.0 24403 2450= U=(TDIFF+2.0+TINCR5)/ZINCR++2.0 2460= WRITE(6.1)0.P.Q.S.U 2479=1 FORMAT(* 0=*,F11.7,2X,*P=*,F11.7,2X,*Q=*,F11.7,2X,*S=*,F11.7,2X,*U 2460= +=*,F11.7) 2490=C 2500=C PRESET INTERIOR TEMPERATURES 2510=C 2520= IF(IGNOTI.EQ.1) GO TO 15 2530= DJ 10 I=2.NR2 2540= 00 10 J=2.NH2

90

255J=10 IT=(L+I)4T 256 0=C 2573=C 2580=C GENERATION OF TEMPERATURE DISTRIBUTION IN ONE QUARTER OF THE CAN 2 59 0 =C 2600=C 2610=C DETERMINATION OF THE NUMBER OF NECESSARY INTERPOLATIONS 2 €2 0 =C 2630=15 IF(TINCR.E0.0.5) INT=4 2640= IF(TINCR.EG.1.0) INT=16 2650= IF(TINCR.EQ.0.333) INT=5 IF(TINCR.E0.5.0) INT=80 2660= 2670= IF (TINCR . 20.10.0) INT=160 2660= IF (TINCR .LE.J.125) INT=1 2690=C 27:0=C POLYNOMIAL INTERPOLATION OF THE ENVIRONMENT TEMPERATUPE USING 2710=C THE FUNCTION SUBROUTINE TERP1 2720=C 2730= DO 2C K=NPOS+1.ICOUNT 2 :40= IF (K.EQ.ICOUNT) INT=1 2750= 00 25 L=1.INT 2760= IF(INT.E0.1) GO TO 26 2770= RT=TERP1(TIME(K)+(1/FLOAT(INT))+(L-1),TIME,TEMP,ICOUNT.0.5) 27903 GO TO 27 2790=26 RT=TEMP((K+L)-1) 2810=0 2210=C SET UP BOUNDARY CONDITIONS 2 82 0 =C 2830=27 00 30 I=1.NR2 2840= T(I,1)=RT 2950=30 TA(1,1)=RT 2960= 00 40 J=1+NH2 2878= T(1+J)=RT 288 0=40 TA(1,J)=RT 2990=C 2500=C DETERMINE CENTER AND MIDPLANE NODE POINT TEMPERATURES USING 2910=C THE GENERAL EQUATION 2920 =C 2930= Q0 60 JJ=2,NH1 2940=C 2950=C RESET INITIAL R FOR EACH NEW J 2969=C 2970= RERTOT 2980= D0 65 II=2.NR 2990= R=R-RINCR 3002= TB(II,JJ)=TA(II,JJ)+S+(TA(II-1,JJ)-2.0+TA(II,JJ)+TA(II+1,JJ)) 3710= ++Q/R+(TA(II-1,JJ)-TA(II+1,JJ)+0+(TA(II,JJ-1)-2.0+TA(II,JJ)+ 3020= +TA(II,JJ+1)) 3030=65 CONTINUE 3240=C 3050=C OUE TO SYMMETRY, THE FIRST INCREMENTS ON EITHER SIDE OF THE 3360=C CENTER LINE ARE EQUAL 3770=C 3080= TB(NR2 , JJ) = TB(NR , JJ)3098=60 CONTINUE 3100=C 3119=C DETERMINE CENTER LINE NODE POINT TEMPERATURES (EXCLUDING THE 3120=C GEOMETRIC CENTER) BY THE CENTER LINE EQUATION WITH R=0 3130=C 3140= 00 70 J3=2.NH 3153=70 TB(NR1+J3)=TA(NR1+J3)+2+3+F+(TA(NR+J3)-TA(NR1+J3))+0+(TA(NR1+

+J3-1)-2.0+TA(NR1,J3)+TA(NR1,J3+1)) 3160= 3170=C 3150=C CALCULATE TEMPERATURES IN THE ROW ABOVE THE MIDPLANE 3190=C 3200= 00 87 13=2,NR1 TB(13,NH2)=TB(13,NH) 3210=80 3229=C 3234=C DETERMINE THE GEOMETRIC CENTER NOCE POINT TEMPERATURE 3 24 0=C 3250= TB(NR1.NH1)=TA(NR1.HH1)+2.0+P+(TA(NR.H1)-TA(NR1.H1))+U+(TA(NR1. 3260= +~++)-TA(NR1,NH1)) 327:=C 3280=C STORE CENTER CAN TEMPERATURE IN CCT VARIABLE FOR EACH TIME 329G=C 3300= CCT((K+L)-1+1)=TIME(K)+(1/FLOAT(INT))+(L-1) 3310= CCT((K+L)-1,2)=TB(NR1,NH1) 3320=C 3330=C REPEAT FOR NEXT TIME 3340=C LET NEW TEMPERATURES BECOME OLD TEMPERATURES 3350=C 00 110 14=2.MR2 3360= 3370= 00 110 J4=2.NH2 3380=110 TA(I4,J4)=TB(I4,J4)3390 =C 3410=C DETERMINE AVERAGE PROCESS PHASE TEMPERATURE 3419=C 3420=115 SUMRT=SUMRT+RT 3437=25 CONTINUE 3440=20 CONTINUE 3450= IF(TINCR.EG.1.0) INT=16 IF(TINCR.EQ.0.333) INT=5 3460= 3470= AMEDIUM=SUMRT/((ICOUNT-NPOS)+INT) PRINT ++3365+ *AMEDIUM= * +AMEDIUM 3490= 3490= RETURN 3500= END

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APPENDIX C

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THERMAL DIFFUSIVITY ESTIMATION PROGRAM BASED ON THE LEAST SQUARES PROCEDURE (INCLUDING A SAMPLE OUTPUT)

100=C DIFFUSIVITY ESTIMATION PROGRAM WRITEN BY JOHN W. LARKIN (JAN. 1981) 110=C 120=C FORTRAN PROGRAM THAT WILL ESTIMATE THERMAL DIFFUSIVITY OF A SOLID 130=C FOOD PRODUCT FROM TIME, TEMPERATURE DATA, COLLECTED FOR, ANY 140=C POINT IN THE FOOD SYSTEM. THE CALCULATION IS DONE USING A INITIAL 150=C ESTIMATION OF THE DIFFUSIVITY FROM THE MOISTURE CONTENT OF THE FOOD PRODUCT. IF THE MOISTURE IS NOT KNOWN THEN THE PROGRAM ESTIMATES 160=C IT AS 50 (0/0) BY THE USER ENTERING 0.0 (0/0). THEN FROM THIS 170=C INITIAL ESTIMATION A THREE POINT GRID IS PRODUCED TO DETERMINE THE 190=C 198=C DIRECTION OF LEAST ERROR. THE ERROR IS CALCULATED FROM THE DIFFERENCE 200=C BETWEEN THE ACTUAL AND CALCULATED TEMPERATURE DATA POINTS FOR EACH 210=C INPUT TIME DATA POINT. NEW GRIDS ARE CREATED TO FIND THE MINIMUM ERROR UNTIL THE CHANGE IN THE ESTIMATED DIFFUSIVITY IS SMALLER THEN 220=C 2303C THE "ERROR" FACTOR - CURRENTLY SET AT 1.0E-5. 240=C 2503C INPUT IS DONE USING READ UNIT 5. FOR IBM COMPUTERS CARDS CAN BE USED. FOR COC COMPUTERS THIS MUST BE DIFFINED IN THE PROGRAM STATMENT 2603C 270=C PROGRAM DIFFUS(INPUT, OUTPUT, TAPE6, TAPE5=INPUT) 280= 2903C 300sC FOR IBM COMPUTERS THIS PROGRAM STATMENT MUST BE REMOVED. 310=C SO THIS CAN BE DONE BY PLACING A "C" INFRONT OF THIS CARD. THE INPUT 320=C IS TO BE ENTERED AS FOLLOWS: 330=C 340=C CARD 1 = Z - AXIS POSITION WHERE TEMPERATURE WAS 350=C MEASURED; RADIAL POSITION WHERE TEMPERATURE 360=C WAS MEASURED (IN CENTIMETERS). 370=C 380=C NOTE THE Z - AXIS IS DIFFINED AS THE MIDDLE BEING 390=C 0.0 AND THE TOP BEING THE HALF LENGTH OF THE CAN. 400=C THE RADIAL POSITION IS DIFFINED AS STARTING ON 410=C THE Z - AXIS AND GOING OUT TO THE RADIUS OF THE 420=C CAN. 430=C 440=C CARD 2 = LENGTH OF CAN; DIAMETER OF CAN (IN CENTIMETERS). 450=C 460=C CARD 3 = HOISTURE CONTENT OF FOOD PRODUCT IN PERCENT OF WET WEIGHT BASES; AND THE INVERSE BIOT NUMBER. 470=C 480=C 490±C CARD 4 = INITIAL TEMPERATURE; HEATING MEDIUM TEMPERATURE 500×C (SAME UNITS AS THAT USED FOR THE DATA INPUT). 510=C 520=C CARD 5 - N = TIME(HR); TEMPERATURE, DATA POINTS 530=C (ONE CARD FOR EACH DATA POINT). MAXIMUM 540=C = 300. 550=C 580=C 590= DIMENSION TIME(300) + TEMP(300) + TERR(3) + SLABT(300+1) + CYLT(300+1) + TEM 600= +PA(300).W(1) 610= COMMON SLABT, CYLT 620=C TIME(300) = TIME DATA POINTS 630=C TEMP(300) = ACTUAL TEMPERATURE DATA POINTS 640=C TEMPA(300) = CALCULATED TEMPERATURE DATA POINTS 650=C TERR(3) = ARRAY OF TOTAL SUMMED ERROR OF 3 GRID DIFFUSIVITY VALUES. 660= IVARN=0. 670=C WARN IS A VARABLE USED TO LIMIT THE NUMBER FO WARNINGS ISSUED 680=C - NOW SET AS 5. SEE CLY SUBROUTINE. 690= WRITE(6+100) 700=100 FORMAT(*1*+20X+*ESTIMATION OF DIFFUSIVITY FROM TIME TEMPERATURE 710= +* ./.* *.35X.*DATA FOR A CAN*) 750= REWIND 5

```
760=
          NUMBO
770=C READ POINT WERE TIME, TEMPERATURE DATA WAS COLLECTED.
780=
          READ(5.+)Z.R
790=C
      CHANGE TO METERS.
800=
          ZM=Z/100.
810=
          RM=R/100.
820=C
      READ DIMENSIONS OF CAN
8302
          READ(5,+)CANL,CANR
840=C
      CHANGE TO METERS AND DIAMETER TO RADIUS.
850=
          CANLM=CANL/(2+100.)
          CANRM=(CANR/100.)/2.
8603
870=C
      READ MOISTURE CONTENT.
380≈
          READ(5,+)PMO.H
890=
          IF(PM0.EQ.0.0) PM0=50.
900=C
      READ INITIAL AND BATH TEMPERATURES.
9103
          READ(5,+)TEI.TEO
920=
          00 10 I=1.300
930=C
       READ TIME. TEMPEATURE DATA.
940=
          READ(5.+.END=20)TIME(I).TEMP(I)
950=10
          NUMENUM+1
960=
          WRITE(6.120)
          FORMAT( -- + - 5X + + + + + WARNING +++ THIS PROGRAM WILL ONLY HANDLE + -
970=120
980=
         + A MAXIMUM OF 300 DATA POINTS )
990=C
      ESTIMATE INITIAL DIFFUSIVITY VALUE.
1000=20
           SDIFF=ESTC(PHO)
1010=C
1020=C
        CALCULATE A CLOSE ESTIMATE OF THE ACTUAL DIFFUSIVITY FROM
10303C
       A MIDDEL POINT.
10403C
1050=
           TIN=.24
1060=
           NUMHENUM/2. + 1
1070=
           CALL CALDIF(DIFF,SDIFF,TIME(NUMH),TEPP(NUMH),TERR,ZM,RM,CANLM,CANR
1080=
          +M.TEI.TEO.1.IWARN.H.TIN)
1090=
           SOIFF=OIFF
1100=
           TIN=.002
1110=C
        USING THIS VERY CLOSE ESTIMATE OF THE THERMAL DIFFUSIVITY
1120=C
1130=C
        CALCULATE NOW THE DIFFUSIVITY WITH ALL THE POINTS.
1140=C
1150=
           CALL CALDIF(DIFF,SDIFF,TIME,TEMP,TERR,ZM,RM,CANLM,CANRM,TEI,TEO,
          +NUM.IWARN.H.TIN)
1160=
1170=C
        PRINT ALL RESULTS.
1180=900
           WRITE(6.130)CANL,CANR,PHO,TEI,TEO,Z,R
1190=130
           FORMAT("1",/,"0",38X,"INPUT DATA",/,"-",30X,"LENGTH OF CAN="
1200=
          ++G20-5+2X++(CH)++/++ ++30X++DIAMETER OF CAN=++G18-5+2X++(CM)++/++
1210=
          +*,30X.
1220=
          + MOISTURE CONTENT= +,G17.5,2X,+ (0/0) +,/,+ +,30X,+ INITIAL TEMPERATUR
1230=
          +E=*,
          +G14.5,/.* *,30X,*BATH TEMPERATURE=*,G17.5,/.* *,30X.*2-AXIS*.
1240=
1250=
          + POSITION= ,G18.5,2X, (CM) ,/. , ,G0X, RADIUS POSITION= ,G18.5,2X
1260=
          +,*(CM)*,///,*-*,T30,
          **TIME*•T45•*TEMPERATURE*•T60•*TEMPERATURE*•/•* *•T30•*(HR)*•T47•*A
1270=
1280=
          +CTUAL .T60.
1290=
          +*CALCULATED*,//)
           WRITE(6.2)
1292=
1296=2
           FORMAT(*1*,/)
1300=
           SEE=0.
1310=
           ₩(1)=ZM
1320=
           CALL SLAB(SLABT.W.1.TIME.NUM.CANLM.DIFF.H)
1330=
           U(1)=RM
1340=
           CALL CYL(CYLT+W+1+TIME+NUM+CANRM+DIFF+H+IWARN)
```

```
1350=
           00 90 I=1+NUM
1360=
           TR=SLABT(I,1)+CYLT(I,1)
1370=
           TEMPA(I)=(TEI-TEO) +TR+TEO
1380=90
           SEE'= (TEMP(I) - TEMPA(I)) + (TEMP(I) - TEMPA(I)) + SEE
1390=
           00 80 I=1.NUM
1400=
           WRITE(6,140)TIME(I),TEMP(I),TEMPA(I)
1410=140
           FORMAT( * +,25%,612.5,T45,612.5,T60,612.5)
1420=80
           CONTINUE
1430=
           SOIFFF=SDIFF+10.7639
           WRITE(6+150)SDIFF,SDIFFF
1440=
1450=150
           FORMAT( -- + 20X, + CALCULATED DIFFUSIVITY VALUE= +, G20.6, 3X, + (M2/HR) +,
1460=
          + /.* *.20X.*CALCULATED DIFFUSIVITY VALUE=*.G20.6.2X.*(FT2/HR)*)
           WRITE(6,160)SEE
1470=
1480=160
            FORMAT(* *.T22.*SUM OF SQUARED ERROR=*.628.6)
           STOP
1490=
1500=
           END
1510=C
1520=C
        SUBROUTINE TO CALCULATE THE DIFFUSIVITY OF THE FOOD PRODUCT.
1530=C
           SUBROUTINE CALDIF(DIFF,SDIFF,TIME,TEMP,TERR,Z,R,CANL,CANR,TEI,
1540=
1550=
          +TEO.NUM.IVARN.H.TIN)
1560=
           DIMENSION TIME(300) • TEMP(300) • TERR(3) • SLABT(300 • 1) • CYLT(300 • 1) • W(1
1570=
          +)
1580=
           COMMON SLABT.CYLT
1590=C. ERROR = THE DIFFERENCE BETWEEN THE ESTIMATED DIFFUSIVITY VALUES IN
1600=C
                THE GRID AT WHICH TIME THE COMPUTATION WILL STOP.
1610=
           NI=1
1620=
           NS=3
           ERROR=1.0E-5
1630=
1640=C INITIAL INCREAMENT OF DIFFUSIVITY USED IN GRID SEARCH.
1650=
           DINC=SDIFF+TIN
1660=30
           DIFF=SDIFF
1670=
           DIFFF=0IFF-2+0INC
           00 35 I=NI,NS
1680=
1690=35
           TERR(1)=0.0
1700=C CALCULATE 3 GRID POINTS.
1710=
           DO 40 I=NI.NS
           DIFF=DIFFF+I+OINC
1720=
1730=C
       FOR EACH TIME DATA POINT CALCULATE NEW TEMPERATURE PROFILE.
       FIND INFINIT SLAB TEMPERATURE RATIO.
1740=C
1750=
           4(1)=7
           CALL SLAB(SLABT, W.1.TIME, NUM, CANL, DIFF, H)
1760=
1770=C FIND INFINIT CYLINDER TEMPERATURE RATIG.
1780=
           W(1)=R
1790=
           CALL CYL(CYLT, W, 1, TIME, NUH, CANR, DIFF, H, IWARN)
1800=
           DO 50 J=1.NUM
1810=
           TR=SLABT(J.1)+CYLT(J.1)
1820=
           TE=TEMP(J)-((TEI-TEO)+TR+TEO)
1830=C
       SUM THE SQUARE OF THE DIFFERENCE BETWEEN THE ACTUAL AND CALCULATED
1840=C
        TEMPERATURES FOR THE THREE GRID PCINTS.
1850=
           TERR(I)=TE+TE+TERR(I)
1860=50
           CONTINUE
1870=40
           CONTINUE
1880=C
        IF THE ERROR OF THE LOW GRID POINT IS LESS THAN THE MIDDLE GRID
1890sC
        POINT THAN MOVE THE GRID SO THAT THE LOWER POINT IS NOW THE
1900=C
        MIDDLE AND START THE SEARCH OVER.
1910=
           IF(TERR(1).GE.TERR(2)) GOTO 55
1920=
           SDIFF=SDIFF-DINC
1930=
           TERR(3)=TERR(2)
1940=
           TERR(2)=TERR(1)
1950=
           NI=1
```

1960= NS=1 1970= GOTO 30 1980=C IF THE ERROR OF THE HIGH GRID POINT IS LOVER THEN THAT OF THE MIDDLE GRID POINT THAN NOVE THE GRID SO THAT THE HIGH POINT IS NOW 1990=C THE MIDDLE AND START THE SEARCH OVER. 2000=C 2010=55 IF(TERR(3).GE.TERR(2)) GOTO 60 2020= SOIFF=SDIFF+OINC 2030= TERR(1)=TERR(2) 2040= TERR(2)=TERR(3) 2050= NI=3 20603 NS=3 2070= GOTO 30 2080=C IF THE CHANGE IN DIFFUSIVITY IS LESS THAN THE SET ERROR VALUE STOP . 2090=60 IF(DINC.LT.(SDIFF+ERROR)) GOTO 900 2100=C SINCE THE MIDDLE GRID POINT HAS THE LOVEST ERROR CUT THE GRID INCREAMENT IN HALF AND START THE SEARCH OVER NEAREST THE LOVEST 2110=C 2120=C ERROR CHANGE. 2130= DINC=DINC/2.0 IF((TERR(1)-TERR(2)).LT.(TERR(3)-TERR(2))) GOTO 70 2140= 2150= SDIFF=SDIFF+OINC 2160= TERR(1)=TERR(2) 2170= NI=2 21803 NS=2 2190= GOTO 30 2200=70 SDIFF=SDIFF-DINC 2210= TERR(3)=TERR(2) 2220= NI=22230= NS=2 GOTO 30 2240= 2250=900 DIFF=DIFF-DINC 2260= RETURN 2270= END 2280=C 2290=C 2300=C FUNCTION TO CALCULATE THE INITIAL DIFFUSIVITY ESTIMATE FROM THE MOISTURE OF THE FOOD PRODUCT. BY THE USE OF ESTIMATION EQUATIONS 2310=C 2320=C FOR SPECIFIC HEAT (CP), THERMAL CONDUCTIVITY (CON) AND DENSITY (DEN). 2330= FUNCTION ESTD(PMO) 2340= CP=PM0/100.+0.2+(100.-PM0)/100. CON=.48+PH0/100.+.22+(100.-PH0)/100. 2350= 2360= DEN=PM0/100.+1.32+(100.-PM0)/100. 2370= ESTD=(CON)/(CP+(DEN+1.E3)) 2380= RETURN 2390= END

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ESTIMATION	OF	DIFF	USIVI A FOR	TY	FROM Can	TIME	TEMP	ERATUR	E
	LEN DIA Moi Ini Rati	GTH C Meter Sture Tial	INPUT F CAN OF C CCNT TEMPE	DA AN= ENT RAT	TA URE=	10 60 14	• 160 8253 • 000 9 • 70		(CM) (CM) (0/0)
	Z-A RAD	XIS F IUS F	OSITI OSITI	ON=	•	0.			(CM) (CM)
T I ¢ H	ME IR)			TEM	PERA	TURE L	TE Ca	MPERAT	URE
0 13568111112222222233335444455555588013556801135568013556801355680135568013556801135568013556801135568001110000000000000000000000000000000	000000000000000000000000000000000000000	E-01 E-01 E-01 E-01		111111111111111111111111111111111111111	444444UUUUU466777788999990000111112222222771467777542963061714605826936 98877802591592693603692479246802356890123545677789001122233344455 98877802591592693603692479246802356890123545677789001122233344455 988778026936036936924792468023556890123545677789001122233344455 9887780269365936936936936936936936 988778026936593658383702355555555555555555555555555555555555				

•

1.0330 245.80 245.92 1.0500 246.10 246.17 1.0670 246.40 246.41 1.0830 246.70 246.62 CALCULATED DIFFUSIVITY VALUE= .639754E-03 (M2/HR) CALCULATED DIFFUSIVITY VALUE= .688624E-02 (FT2/HR) SUM OF SQUARED ERROR= .23.2043

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APPENDIX D

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POLYNOMIAL INTERPOLATION FUNCTION SUBROUTINE

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FUNCTION TERP1(X.XI.YI.N.F)
100=
110=C
          X IS THE INDEPENDENT VARIABLE
          XI IS AN ARRAY OF VALUES OF THE INDEPENDENT VARIABLE
120=C
130=C
          YI IS AN ARRAY OF CORRESPONDING VALUES OF DEPENDENT VARIABLE
140=C
          N IS THE SIZE OF THE ARRAYS
150=C
          F IS A FACTOR FOR THE END SEGMENTS; BALANCE OF FIRST OND SECOND
160=C
               ORDER INTERPOLATION
170=C
          ALL VALUES OUTSIDE THE LIMITS OF THE ARRAY ARE COMPUTED BY
180=C
             FIRST ORDER EXTRAPOLATION
190=C
          FUNCTION RETURNS INTERPOLATED VALUE OF DEPENDENT VARIABLE
210=
          DIMENSION XI(N).VI(N).P(2).E(2).IS(4.2)
220=
          LOGICAL OUT
2303
          DATA IS /-1,0,-2,-1,0,1,-1,0/
240=
          OUT = .FALSE.
250=
          J=1
260=
          IF (N-2) 1.12.3
270= 1
          TERP1=YI(J)
          RETURN
280=
290=
     3
          KPL=1
200=
          KPU=2
310=
          00 4 J=1.N
          IF (XI(J) - X) 4.1.6
320=
     7
330=
          CONTINUE
     .
340=
          J = N
350=
          GO TO 2
360=
          IF (J-2) 12,8,9
     6
                                                    .
370=
      8
          KPL =2
          GO TO 10
380=
390=
          IF (J - N) 10.11.2
      9
400= 12
         J=2
4103
          OUT=.TRUE.
     2
420=
     11
          KPU=1
430= 10
          AL = (X-XI(J-1)) / (XI(J)-XI(J-1))
440=
          TERP1=AL+Y1(J)+(1.0-AL)+Y1(J-1)
450=
          IF (OUT) RETURN
                                                           1
460=
          00 16 KP=KPL,KPU
470=
           P(KP)=0.0
480=
           00 15 K=1.3
490=
            J0=J+KP + K - 4
500=
            XO=XI(JO)
510=
            (0L) I Y=0Y
520=
            J1=J+IS(K+KP)
530=
            J2=J+IS(K+1,KP)
540=
            P(KP) = P(KP) + YO + (X - XI(J1)) / (XO - XI(J1))
     15
550=
                          +(X-XI(J2))/(X0-XI(J2))
         ٠
560=
           IF (KPL .NE. KPU) GO TO 16
570=
           J1=3-KPL
2802
           P(J1)=TERP1+F+(P(KP)-TERP1)
590=
           E(J1)=A85(P(J1)-TERP1)
600=
           E(KP)=A8S(P(KP)-TERP1)
     16
           IF (E(1)+E(2) .EQ. 0.0) RETURN
610=
6203
          TERP1=((E(1)+AL)+P(2)+(E(2)+(1.0-AL))+P(1)) /
630=
                ((E(1)+AL)
                              +(E(2)+(1.0-AL))
                                                     )
          RETURN
640=
650=
          END
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APPENDIX E

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LETHALITY ESTIMATION PROGRAM EMPLOYING THE GENERAL METHOD (INCLUDING A SAMPLE OUTPUT) 1JC=C+++++++ GENERAL METHOD PPOGRAM ******************** 101=C+++++++ 19230**************** 103=C THIS PROGRAM READS THE CENTER-CAN TEMPERATURES FOR EACH PROCESS 154=C PHASE AND CALCULATES LETHAL RATE FOR A SELECTED Z VALUE, AND REFER-105=C ENCE TEMPERATURE. LETHALITY IS INTEGRATED USING A SIMPLIFIED TRAPE-195=C ZOIDAL RULE FOR A SPECIFIED INITIAL TEMPERATURE AND RETORT TEMPERA-197=C TURE TO YIELD A F VALUE. 108=0 139=C THE FIRST CARD OF THE ENTERED DATA FILE (TAPE1) MUST CONTAIN THE FOL-110=C LOWING INFORMATION IN THIS ORDER: 111=C 112=C 1) RUN NUMBER (K) 2) NUMBER OF TIME/TEMPERATURE POINTS IN DATA FILE (J) 113=C 3) REFERENCE TEMPERATURE - USUALLY 250F OR 212F (R) 114=C 115=C 4) Z VALUE (Z) 116=C 5) RETORT TEMPERATURE (RT) 117=0 6) INITIAL PRODUCT TEMPERATURE (TI) 112=0 119=C FOLLOWING THE FIRST CARD IS THE NOSEQUENCE TIME/TEMPERATURE 12G=C PRODUCT PROFILE(S) TO BE EVALUATED FOR LETHALITY. 121=C 122=C THE POSSIBLE COURSES OF ACTION THAT MAY BE TAKEN ARE: 123∓C 0= NO TI OR RT CONVERSION 1= CONVERT TI 124=C 2= CONVERT RT 125=C 3= PRINT OUT ARRAYS (MIN,CCT,LR/MIN) 126=C 127=C 4= ENTER A NEW DATA SET 129=0 5= PLOT LETHAL RATE VS TIME 129=C 6= CHANGE Z 7= EXIT THE PROGRAM 13030 150=C SCHULTZ-OLSON IT AND RT CONVERSION OPTIONAL: J=NUMBER OF COOTDINATES 16C=C R=REFERENCE TEMPERATURE,Z=SLOPE OF TDT CURVE,X=ELAPSED TIME,Y=COLD-170=C POINT TEMP., KSET=DATA SET NUMBER, K=CAN NUMBER, RT=ORIGINAL RT, RT2=NEW 133=C RT.TI=ORIGINAL IT.ST2=NEW IT.C=LFTHAL RATE PROGRAM GENMETH(INPUT,OUTPUT,TAPE1,TAPE2,TAPE6) 197= 200= DIMENSION X(650), Y(650), C(650) 219= DIMENSION AFILE(650+2)+ICX(1)+ICY(1)+ICP(1)+SCX(2)+SCY(2) 220= + , CHAR(2) 23]= EQUIVALENCE(AFILE(1,1),X(1)),(AFILE(1,2),C(1)) CHARACTER +3 CHAR 240= 250=C PRINT TITLE AND DIRECTIONS 2603 PRINT 5 270=5 FORMAT(1H-,36X,46HG E N E R A L M E T H O D F VALUE// 287= +/) 290=C INITIALIZE DATA SET NUMBER 300= KSET = 1310=C PRINT GATA SET SUB-HEAD PRINT 39.KSET 320= 330=39 FORMAT(1H0+46X+12HDATA SET NO++I3+10H PROBLEMS:///) 340= I6 = 1350=C REGUEST COURSE OF ACTION 360=37 PPINT++*WHAT NEXT?* 370= READ+.KR PRINT 83 383= 390= 12 = 14003 IF(K8.EQ.0) GO TO 57 GO TO (57, 57, 77, 57, 57, 63, 77), K8 410= 420=77 IF(K8.EQ.7) STOP PRINT+. PENTER NEW Z* 430=

.

```
440=
          READ=+Z
450=
          GO TO 17
46:=83
          FORMAT(/)
470=63
          IF(16.LE.1)G0 T0 22
480=C INCREMENT DATA SET
          KSET = KSET + 1
4 90 2
530=C PRINT DATA SET SUB-HEAD
          PRINT39-KSET
515=
520=
          60 TO 22
532=57
          IF(KE.EQ.5) 60 TO 1122
540=
          IF(16.GT.1)G0 T0 59
559=
          REWIND 1
563=22
          READ(1.+.END=77)K.J.R.Z.RT.TI
57"=C SET X ARRAY TO -1
580=
          DO 1 JZ = 1,650
590=
          X(JZ) = -1.
          CONTINUE
600=1
610=C READ TIME VS. TEMPERATURE COORDINATES
629=
          00 41 N = 1.J
630=
          READ(1++)X(N)+Y(N)
640=41
          CONTINUE
650=
          16 = 16 + 1
66:=
          IF(K8.LT.1)G0 T0 59
671=
          IF(K8-4)29,59,37
620=59
          IF(K8.LE.2)60 TO 29
690=
          REWIND 6
700=
          WRITE(6.52)
710=C PRINT X.Y.C ARRAYS----X = -1. IS A RECORD FILLER
723=52
          FORMATCIN +19X+24HMIN+ CT+ LR/HIN ARRAYS:///19X+3HMIN+5X+2HCT+8X+
739=
         +6HLR/MIN.6X.3HMIN.5X.2HCT.8
         *X+6HLR/MIN+6X+3HMIN+5X+2HCT+7X+6HLR/MIN//)
740=
759=
          00 58 NA = 1, J, 3
760=
          ¥7.ITE(6+71)X(NA)+Y(NA)+C(NA)+X(NA+1)+Y(NA+1)+C(NA+1)+X(NA+2)+Y(NA+
771=
         +2).C(NA+2)
789=71
          FOR 4AT(12X+3(5X+F6+2+3X+F6+2+3X+F8+3))
          CONTINUE
790=5A
800=
          PRINT 83
a10=
          GO TO 37
92:=29
          GT=RT
8 20 =
          FT=TI
8403
          IF(K8.GE.1)60 TO 99
950=17
          00 \ 65 \ I3 = 1.J
960=C CALCULATE LETHAL RATE
879=
          C(I3) = 1 \cdot (EXP(2 \cdot 3026 + (R - Y(I3))/Z))
          IF(12.6T.1)G0 T0 38
381=
3953
          S = X(I3)
900=
          T = C(I3)
910=
          F0 = 0.
920=88
          A = (X(13)-S)+T+(X(13)-S)+(C(13)-T)/2.
930=C ACCUMULATE LETHALITY
948=
          FO = FO + A
950=
          I2 = I2 + 1
960=
          S = X(I3)
970=
          T = C(I3)
          CONTINUE
980=65
990=C OUTPUT RESULTS
1960=
           PRINT66.K.Z.R.FO.GT.FT
1010=66
           FORMAT(1H++25X+12HFOR CAN NO. +12+3H F(+F4+1+1+++F5+1+4H) = +
1020=
          *F8.2.19H FOR RT = .F5.1.7H. IT = .F5.1///)
1030=
           RT=GT
           TI=FT
1040=
```

```
GO TO 37
1050=
1060=99
           IF(K8.6T.1) GO TO 33
1070=C INPUT NEW IT
1080=
           PRINT *. *NEW IT = *
1390=
           READ++ST2
           PRINT 83
1100=
1110=
           FT = ST2
1120=
           00 79 L = 1.J
1130=C CONVERT TEMPERATURES FOR NEW IT
1140=
           CALL ITEMP(RT.TI.ST2.Y(L))
1150=79
           CONTINUE
1163=
           GO TO 17
1170=C INPUT NEW RT
1180=33
           PRINT+. *NEW RT = *
1190=
           READ++RT2
1200=
           PRINT83
1210=
           GT = RT2
1220=C CONVERT TEMPERCATURES FOR NEW PT
           DO 9 LZ = 1.J
1230=
1240=
           CALL RTEMP(RT.TI.RT2.Y(LZ))
1250=9
           CONTINUE
12603
           60 TO 17
1273=1122
           CALL PLOTA(50,50,2,1,6,0,0,0,0,0,0)
1280=
           ICX(1)=1
1290=
           ICY(1)=2
1300=
           ICP(1)=J
131)=
           CHAR(1)=***
1329=
           IND=0
1330=
           WRITE(6.102)
1340=102
           FORMAT(*1*,26X,* LETHAL RATE VS. TIME*)
1350=
           CALL PLOTB(AFILE, CHAR, ICX, ICY, ICP, SCX, SCY, 2, 1, 650, IND)
1369=
           GO TO 37
1379=
           END
1380=C SUBROUTINE ITEMP CONVERTS TEMPERATURES FOR NEW IT
1390=
           SUBROUTINE ITEMP (RT.TI.ST2.Y)
           X8 = RT - ST2
1400=
1410=
           X7 = RT - TI
142?=
           X6 = X8 / X7
                                                          3
1430=
           X9 = RT - Y
1440=
           Y = RT - XG + X9
1450=
           RETURN
1460=
           END
1479=C SUBROUTINE RTEMP CONVERTS TEMPERATURES FOR NEW RT
1450=
           SUBROUTINE RTEMP(RT.TI.RT2.Y)
1493=
           XE = RT2 - TI
1529=
           X7 = RT - TI
1510=
           x_6 = x_8 / x_7
1523=
           X9 = RT - Y
           Y = RT2 - X6 + X9
1530=
1540=
           RETURN
1550=
           ΕΝΟ
```

• (-

NIN, CT. LR/MIN APRAYS: CT LR/MIN LR/MIN 814 LP/HIN MIN CT MIN CT - 0 0 0 - 0 0 0 - 0 0 0 - 0 0 0 - 0 0 0 1.20 . .

75.00

225.37

103

.

.000

-255 -321 -379 -454 -522

•522 •585 •656 •616 •616

•682 •656 •665 •656

-43389854942C24601477

053 647 043

.0.0

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APPENDIX F

CONVERSION FACTORS: ENGLISH TO S.I. UNITS

APPENDIX F

Conversion Factors English to S.I. Units

1 inch = 2.54 centimeters 1 lb = 0.4536 kilogramgs Fahrenheit (°F) to Centigrade(°C): (°F - 32) (5/9) = °C Diffusivity: $in^2/min = 1.08 \times 10^{-5} m^2/sec$

English units were used in this study due to the conventions of the food industry (e.g., Fahrenheit thermometers in processing plants are more typical than not).

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