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Optimising the quality of safe food: Computational modelling of a continuous sterilisation process

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Abstract

Continuous food sterilisation processes require that a given level of sterility is reached for minimal quality loss. Current designs are empirically based on ideas developed on batch systems, for processing at high temperature for short time (HTST). A computational model for continuous flow sterilisation has been used to test these assumptions. A model system for a laminar flow in circular pipes with uniform wall temperatures has been developed; both Newtonian and non-Newtonian viscosity models, (including temperature dependence) have been used. Temperature and velocity profiles have been modelled using a validated computational fluid dynamics (CFD) package. Results from the simulations have been used together with conventional food processing sterility and quality kinetics, adapted to the continuous flow case. Data from the model were used to study the efficiency of a continuous sterilisation process. Results have shown that the conservative approach used in the food industry can lead to significant overprocessing and thus unnecessary deterioration of the overall product quality. The conventional HTST assumption fails under some circumstances, for example when the fluid layer near the wall is overprocessed.

Keywords: Food processing; Heat transfer; Sterilisation; Optimisation; Computational; Fluid dynamics

1. Introduction

Consumer demands for food products focus on safety, product quality and cost. Thermal processing remains the most significant technique: on heating sets of reactions occur which result in microbial inactivation, quality loss and flavour/texture development (Holdsworth, 1992). Processes involve (i) a heating stage in which the food is raised to the required temperature, (ii) holding at the required temperature for long enough to ensure that the required level of sterility has been reached, and then (iii) cooling to a lower temperature. Classical thermal techniques are batch or semi-batch (Holdsworth, 1993): packaging the product before the heating stage, as in canning, allows a cheap and safe product to be produced. However, the quality is not good, because the time required to conduct or convect heat to the centre of the material means that most of it must be overcooked.

A variety of processing routes have been suggested to solve this problem. As the activation energy for the reactions which result in microbial death are higher than those which result in quality loss, HTST (high-temperature-short-time) processes offer the potential to give the same level of sterility for a reduced quality loss (Holdsworth, 1992): at ca. 140°C the rate of sterilisation is about 2000 times faster than at conventional canning temperatures in the region of 125°C (Fryer, 1997). This can be obtained by (i) continuous processing of single-phase fluids in devices such as plate heat exchangers or tubes, in which the rapid heat transfer rates required can be achieved, or (ii) for solid–liquid mixtures, using heat generation techniques by either microwave (Metaxas, 1996) or electrical resistance (Fryer, 1995) heating.

Processes for the sterilisation of single-phase mixtures are common; the design of equipment for the HTST processing of low-viscosity liquids such as milks or fruit juices is relatively straightforward. Materials of higher viscosity, such as soups and sauces, are more difficult to process. Commonly, these materials are sufficiently viscous that the flow is laminar. The presence of any residence time distribution within the flowing fluid will make

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it difficult to define the process. For canning and other packaged food products it is easy to identify the time–temperature regime experienced by each pack. The combination of flow and temperature fields within a flowing food in which there is a significant velocity profile will make the required process schedule difficult to predict. Both heating and cooling can be slower than for low-viscosity fluids; in addition different parts of the fluid will receive different temperature-time profiles, which will result in (i) a different concentration of microbes or spores, and (ii) a different extent of quality loss. The output conditions will be a mixture of fluid at different radial positions.

A number of simplified design rules have been used to ensure product safety. If it is assumed that material flows in fully developed Newtonian laminar flow through a tube, the maximum velocity is twice that of the mean. The length of the holding tube can then be calculated to ensure that even if all the liquid was flowing at twice the mean velocity, it would still be sterilised (Rees and Bettison, 1991). However, although this ensures sterility, this assumption can give product of such poor quality that the consumer is not willing to pay the price premium which can result from the use of continuous processes.

In previous work the problem of processing solid–liquid mixtures has been addressed, using both conventional and electrical heating (Zhang and Fryer, 1993; Mankad et al., 1995). The problems of modelling two-phase flows are considerable, and so very few models address the problems of different velocities (Mankad and Fryer, 1997). Single-phase flow, which is considered here, is easier to study computationally, as it is possible to calculate the velocity field as long as appropriate rheological data is available over the required temperature range. This problem has been addressed by a number of authors: Manson and Cullen (1974) continued the work of Charm (1966) and divided up a flow into a set of 11 annular shells to include the effect of the velocity profile in the evaluation of the lethal effects received by a food during laminar flow through a holding tube. One problem is that, as will be outlined in the next section, the conventional representation of sterilisation kinetics is not in terms of concentration. Previous models for the behaviour of sterilisers have ignored the RTD problem–for example Sastry (1986) gives a model which assumes one-dimensional flow.

Experimental measurement of the velocity profile of a fluid in laminar flow has been carried out using magnetic resonance imaging (McCarthy et al., 1992). The results showed good agreement with theory for non-Newtonian fluids which suggests the use of a laminar velocity profile is relevant for our modelling work. Other authors have measured experimentally temperature fields after a heating section of constant wall heat flux tubular heat exchangers (Lefebvre and Leuliet, 1997; Leuliet et al., 1993). Direct visualisation of the temperature fields in these sections gave an improved understanding of the complex effects of mixed convection on heat transfer.

The optimisation of product quality for a given sterility is essentially a reaction engineering problem. Our aim here is to show how a rheological model for a single phase food fluid can be used in design and to investigate whether the HTST assumption is an appropriate approximation in all cases. The state of commercial CFD programs is such that they can now readily be used to calculate simultaneous flow and heat transfer: we have used the finite element (FE) code FIDAP (500 Davis Street, Suite 600 Evanston, IL 60201) which has been widely used both for heat transfer and fluid dynamics problems in the food and other industries (such as, for example: Kumar et al., 1990; Engelman and Sani, 1983). The problem is one of ensuring that a given level of sterility is given to the food whilst minimising the loss of quality caused by the thermal process: the presence of the velocity field will complicate the simple HTST assumption. Before considering optimisation, it is necessary both to confirm the accuracy of the FIDAP program for these calculations and to develop an expression for the output sterility and quality of the material.

2. Kinetics of in-flow sterilisation and quality

In every thermal treatment of food products the first objective is to ensure the microbiological safety. It is important to ensure that the most heat resistant pathogenic spores (such as C. Botulinum) have been killed; in addition, with aseptic products, the stability of the product must also be achieved, so other heat resistant spores have also to be killed. During thermal treatment, the chemical and biochemical components of the products undergo some thermal destruction, but the rates and activation energies of these reactions are different to those of the processes which lead to sterility. The aim of an engineering analysis is to define the optimal thermal treatment by which the desired level of sterility is obtained with the best possible quality.

Food scientists have conventionally used a linearisation of conventional Arrhenius kinetics which is appropriate over the narrow range of temperature over which processing is carried out. This is difficult to apply for the case of flow profiles within process plant: we follow the analysis of Baetson (1971) in developing an appropriate expression. If it is assumed that microbial destruction is first order, with an initial number of micro-organisms, N^0 then the reaction rate is expressed as:

$$\frac{\mathrm{d}N}{\mathrm{d}t} = -k_\theta N \tag{1}$$

so that D_θ, the decimal reduction time required to reduce the initial micro-organism population by a factor of 10 at temperature θ ($N^f = N^0/10$) is

$$t = \frac{2.303}{k_\theta} \operatorname{Log}_{10}\left(\frac{N^0}{N^f}\right) = D_\theta \operatorname{Log}_{10}(10) = D_\theta \tag{2}$$

D_θ varies with the micro-organism and is also a function of the biological variables of the micro-organism environment such as pH, temperature, and water activity. The variation of D_θ with temperature is incorporated via z, the temperature difference over which D_θ itself varies by a factor of 10, so that the change in D-value between temperatures θ_1 and θ_2 is given by

$$D_{\theta_1} = D_{\theta_2} 10^{(\theta_2 - \theta_1)/z} \tag{3}$$

The number of decimal reductions defines the efficiency of a process: in practice, these are carried out under conditions of varying temperatures and the efficiency of a given treatment is estimated by comparing it to a process at some reference temperature, θ_{ref}. The sterilising value F_{ref} is the process time needed at the reference temperature to give the same number of decimal reductions as a given thermal treatment. For a treatment at constant temperature θ therefore, as the ratio of concentrations is the same for each treatment, manipulation of eq. (2) gives

$$\frac{t_\theta}{D_\theta} = \frac{F_{\text{ref}}}{D_{\theta\text{ref}}} \tag{4}$$

so that

$$F_{\text{ref}} = t_\theta 10^{(\theta - \theta_{\text{ref}})/z}. \tag{5}$$

For processes in which temperature θ varies with t, this equation can be developed; the total F accumulated over a time t_p for which the temperature $\theta(t)$ varies with time is

$$F = \int_0^{t_p} 10^{(\theta - \theta_{\text{ref}})/z} \, \mathrm{d}t \,. \tag{6}$$

The above applies to materials where all the food has the same T–t profile; in a continuous process different elements of fluid will have different thermal histories. As kinetics are expressed in terms of process time rather than concentrations, to find the exit F-value of the fluid the expression must be rewritten. Consider i subdivisions of the flow, where each of the i follows a trajectory for which $\theta_i(t)$, and remains in the equipment for a time t_{si}, the concentration of organisms in the ith element, N^{fi}, is thus calculated from

$$F^i_{\text{ref}} = \int_0^{t_{si}} 10^{(\theta i(x) - \theta_{\text{ref}})/z} \, \mathrm{d}t = D_{\text{ref}} \operatorname{Log}\left(\frac{N^0}{N^{fi}}\right). \tag{7}$$

In most cases, the product can be assumed to be thoroughly mixed between the exit of the process and the packaging stage. The output concentration is therefore the sum of those from the i segments, weighted by the volumetric flow:

$$\bar{N}^f = \frac{\sum_i N^{fi} v_i S_i}{\sum_i v_i S_i} \quad \text{i.e.} \quad \bar{N}^f = \frac{\sum_i N^0 10^{-F^i_{\text{ref}}/D_{\text{ref}}} v_i S_i}{\sum_i v_i S_i} \tag{8}$$

where v_i is the flow velocity and S_i the cross-section area of the flow of the ith element, so that $\sum_i v_i S_i = \dot{Q}$ represents the volumetric flow.

The overall exit microbial reduction is thus

$$\operatorname{Log}\left(\frac{\bar{N}^f}{N^0}\right) = \operatorname{Log}\left(\frac{1}{\dot{Q}} \sum_i v_i S_i 10^{-1/D_{\text{ref}} \int_0^{t_{si}} 10^{(\theta_i(x) - \theta_{\text{ref}})/z} \, \mathrm{d}t}\right). \tag{9}$$

For a tube, a series of annular elements of area $2\pi r \, \mathrm{d}r$ can be taken; each moving at $v(r)$, so that in the limit of $\mathrm{d}r \to 0$

$$\operatorname{Log}\left(\frac{\bar{N}^f}{N^0}\right) = \operatorname{Log}\left(\frac{1}{\dot{Q}} \int_0^R v(r) 2\pi r 10^{-1/D_{\text{ref}} \int_0^{t_s(r)} 10^{(\theta_i(x) - \theta_{\text{ref}})/z} \, \mathrm{d}t} \, \mathrm{d}r\right). \tag{10}$$

From either of these equations, the mean integrated sterilising value can be deduced as

$$\bar{F}_{\text{ref}} = D_{\text{ref}} \operatorname{Log}\left(\frac{N^0}{\bar{N}^f}\right). \tag{11}$$

This is identical though more complex than the standard expression for the mean outlet concentration, i.e. the convolution integral of concentration and residence time.

Conventionally the θ_{ref} is taken as 121.1°C and the value of z was found experimentally to be 10°C for C. Botulinum (Stumbo et al., 1950). A similar expression, for which $\theta^c_{\text{ref}} = 100$°C and $z^c = 30$°C can be defined to calculate the 'C value', an approximate measure of quality (Holdsworth, 1992). Hence the quality analogue to Eq. (6) is

$$C = \int_0^{t_p} 10^{(\theta - \theta^c_{\text{ref}})/z^c} \, \mathrm{d}t \tag{12}$$

and the mean C at the outlet can be found by equations analogous to Eqs. (10) and (11).

Equations for $\bar{C}$ and $\bar{F}$ can then be used as the basis for optimisation: for a given situation, it is required to reach a set $\bar{F}$ which guarantees process sterility; we seek a situation that minimises $\bar{C}$.

It should be noted that the F and C concepts are approximations to Arrhenius behaviour, i.e. to conventional kinetics. The relationship between z and the activation energy E in the range of temperatures T_1 to T_2 is

$$z = 2.303R \frac{T_1 T_2}{E} \tag{13}$$

(Loncin and Merson, 1979) so that D and z are accurate over only the narrow range of temperature for which they were measured. In this paper, data is presented using food processing kinetics: the analysis is equally valid for Arrhenius parameters.

3. Validation of FIDAP: fluid mechanics and heat transfer

Before investigating the effects of the coupling between fluid mechanics and heat transfer on sterility and quality, the ability of the FIDAP package to model this problem must be confirmed. Analytical fluid velocity profiles were obtained in circular ducts under isothermal conditions, using a power law rheological equation to relate shear stress and shear rate:

$$\tau = k\dot{\gamma}^n. \tag{14}$$

A range of flow-behaviour index n was investigated ($n = 0.15$ pseudoplastic fluid, $n = 1$ Newtonian fluid, and $n = 3$ and 100 dilatant fluids).

Equation (14) can be used with basic fluid flow equations to give the dimensionless velocity profile (15) of a power law fluid in a circular duct:

$$\frac{v(r)}{v_{\text{ave}}} = \left(\frac{3n+1}{n+1}\right)\left[1 - \left(\frac{r}{R\,\text{max}}\right)^{n+1/n}\right]. \tag{15}$$

The code used 50 grid points across the radius, with more points near the wall. The comparison of Eq. (15) with velocity profiles obtained using FIDAP is shown in Fig. 1(a). The results show excellent agreement (in any case to more than three significant figures) with the analytical predictions. The convergence of the FE code was difficult to achieve for the extreme power law indexes ($n = 0.15$ and 100), which are unlikely to be seen in practice. FIDAP can be further checked by comparing calculated values of the wall shear rates and the analytical shear rate

$$\dot{\gamma}_{\text{wall}} = 8\left(\frac{3n+1}{4n}\right)\left(\frac{v_{\text{ave}}}{2R_{\text{max}}}\right). \tag{16}$$

The results agree to more than four significant figures.

The velocity distribution is more readily solved than the temperature distribution, which can only be solved analytically when the physical properties of the fluid are assumed temperature independent. Analytical solutions to heat transfer to Newtonian and non-Newtonian fluids in piston or fully developed laminar flow have been found by a number of workers (Jakob, 1949; Lyche and Bird, 1956; Metzner et al., 1957). Although the assumptions made in these models are too restrictive for use in the food industry (e.g. constant physical properties, uniform inlet temperatures); they can be used to validate the FE model. The temperature field for a fully developed velocity profile of a Newtonian fluid in a circular pipe is obtained by solving:

$$2v_{\text{ave}}\left[1 - \left(\frac{r}{R_{\text{max}}}\right)^2\right]\frac{\partial\theta}{\partial z} = \alpha\left[\frac{\partial^2\theta}{\partial r^2} + \frac{1}{r}\frac{\partial\theta}{\partial r}\right] \tag{17}$$

using separation of variables (Jakob, 1949). The result of this was expressed by Jakob (1949) in terms of a dimensionless sum with tabulated coefficients $R_n(r)$ and m_n:

$$\frac{\theta(x,r) - \theta_{\text{wall}}}{\theta_{\text{in}} - \theta_{\text{wall}}} = 1.447\,\mathrm{e}^{-m_0 x}R_0(r) - 0.810\,\mathrm{e}^{-m_1 x}R_1(r) + 0.385\,\mathrm{e}^{-m_2 x}R_2(r) - \cdots. \tag{18}$$

The temperature profiles obtained by FIDAP and eq. (18) for the process conditions of Table 1 (a) are presented in Fig. 1 (b). They show a very good fit of the FE code to the analytical solutions (to more than three significant figures for the cases of $x = 4$, 6, 8, and 10 m and to more than one significant figure for $x = 2$ m). Some problems are found at the centre of the pipe inlet where the analytical method fails to converge to 1, whereas FIDAP does; this error is on the order of 1% and may well be due to rounding errors, as coefficients are given only to the fourth decimal place in Jakob.

Another approximate solution (Wilkinson, 1960) gives the average heat transfer coefficient $h_{\text{ave}}(x)$ along a duct of length x as

$$\frac{h_{\text{ave}}(x)\,D}{\lambda} = 1.75\left(\frac{\dot{m}Cp}{\lambda x}\right)^{1/3} \tag{19}$$

where $\dot{m}$ is the mass flow rate, and $(\dot{m}\,Cp/\lambda x)$ is the Graetz number. Here $h_{\text{ave}}(x)$ is defined as (Wilkinson, 1960)

$$h_{\text{ave}}(x) = \rho v_{\text{ave}}Cp\,\frac{R\,\text{max}}{2x}\,\frac{\theta(x) - \theta_{\text{in}}}{\Delta\theta} \tag{20}$$

where $\theta(x)$ is the average temperature within a given section, and $\Delta\theta$ is the arithmetic mean temperature difference, i.e. $\Delta\theta = \theta_{\text{wall}} - \frac{1}{2}(\theta_{\text{in}} + \theta(x))$

This type of equation can be used to define a process. To assess its accuracy, FIDAP was used to calculate $\theta(x)$ as an integral mean temperature:

$$\theta(x)_{\text{FIDAP}} = \frac{1}{\dot{Q}}\int_0^{R\text{max}} 2\pi r v(x,r)\,\theta(x,r)\,\mathrm{d}r. \tag{21}$$

Equation (20) and (21) have been compared for the conditions of Table 1 (a). Fig. 2 shows the variation of $\theta(x)$ from both methods as a function of the axial position. The difference between the FE code and the approximation of Eq. (20) is less than 1.0°C along the full length of the heater. The difference between the two methods is due to

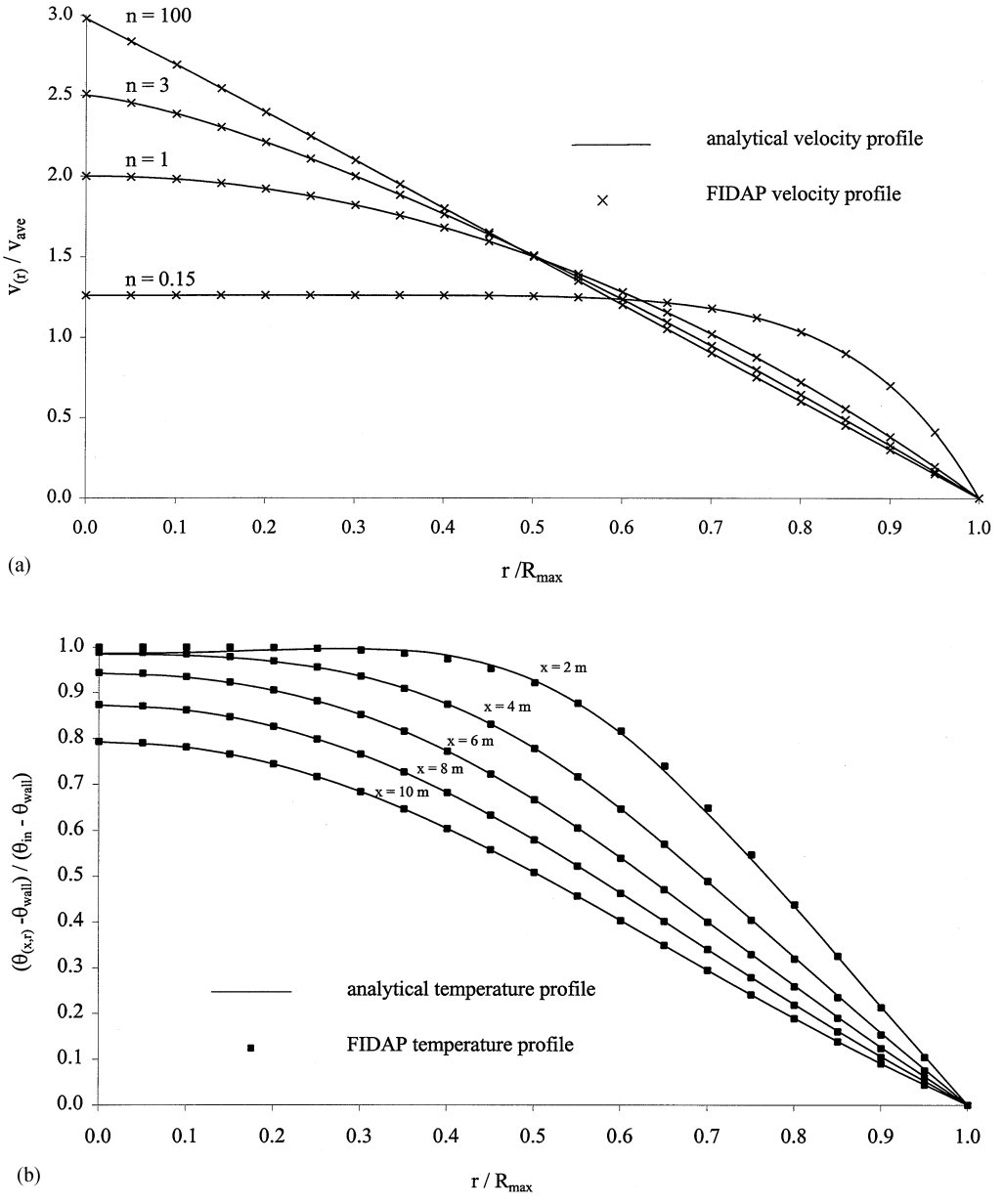

Fig. 1. (a) Comparison of velocity profiles in circular pipes predicted by FIDAP and calculated from Eq. (15) for various power-law indices n (non-Newtonian fluid, laminar régime, isothermal flow). (b) Comparison of temperature profiles in circular pipes predicted by FIDAP and calculated from Eq. (18). (Newtonian fluid, laminar régime, constant wall temperature).

the nature of the approximation used to obtain Eq. (20). Wilkinson (1960) notes that Eq. (19) assumes that the velocity gradient is linear near the wall; and that it is accurate in the entry region, for which $Gz > 100$. Figure 2 shows that the fit between the two is much better at the heater inlet than after 2 meters where the Graetz number drops below 100. It is likely here that FIDAP is more accurate than the usual analytical approximation used in the food industry; the analytical approximation slightly over predicts the mean temperature and therefore the sterility value is likely to be more overestimated than with the FE code.

4. Thermal processing in flow

4.1. Effect of temperature on flow profile and its consequences

There is no analytical solution to heat transfer problems when physical properties vary with temperature; in most cases, real foods have complicated non-Newtonian viscosities, such that the temperature dependence of the viscosity is of great importance in predicting temperature profiles in flowing liquids. The effect of temperature on velocity profiles has been shown by simulation of a

Table 1
Process parameters used in simulations

Process parameters	Simulation (a)	Simulation (b)	Simulation (c)
Product flow rate ($l\,h^{-1}$)	100 ($Re = 882$)	100 ($Re < 900$)	100 ($Re = 1176$)
Viscosity (Pa s)	0.001 (constant)	$\eta = A\,e^{E/RT}$	0.001
Specific heat ($J\,kg^{-1}\,K^{-1}$)	4180	4180	4180
Thermal conductivity ($W\,m^{-1}\,K^{-1}$)	0.6	0.6	0.6
Density ($kg\,m^{-3}$)	998	998	998
Pipe radius (m)	0.02	0.02	0.015
Heater wall temperature (°C)	140	140	140
Cooler wall temperature (°C)	–	20	20
Product inlet temperature (°C)	60	60	60
Heater total length (m)	10	10	12
Holding tube total length (m)	–	10	12
Cooler total length (m)	–	10	12

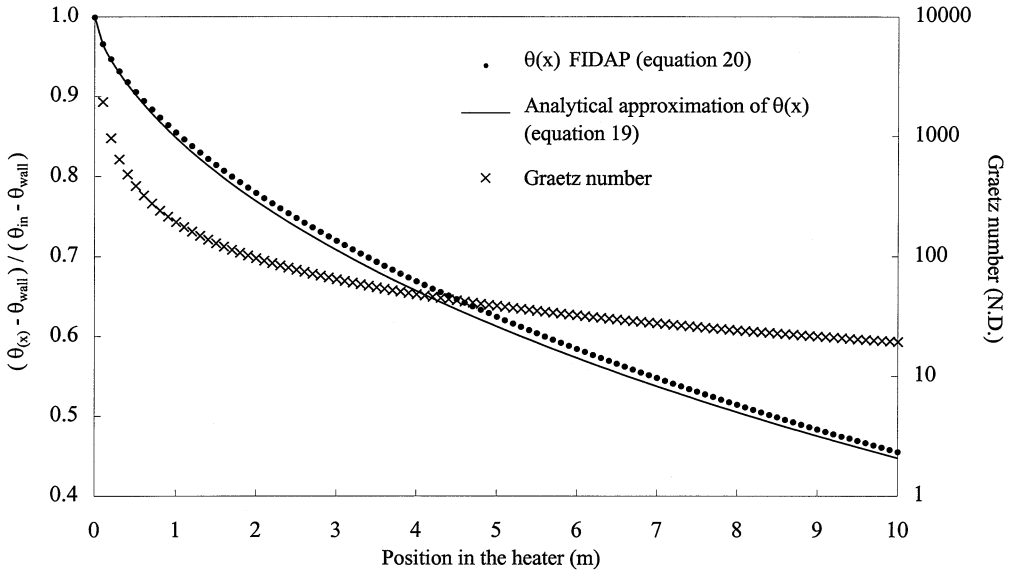

Fig. 2. Comparison of the average section temperature in a heater predicted by FIDAP and calculated from Eq. (20). (Newtonian fluid, laminar régime, constant wall temperature).

system with Newtonian viscosity which varies with temperature. The temperature dependence of viscosity was taken as (Holdsworth, 1993):

$$\eta = A\,e^{E/RT} \tag{22}$$

where $E = 15.6 \times 10^3\ J\,mol^{-1}$ (from Loncin, 1993 for water), $A = 1.72 \times 10^{-5}\ Pa\,s^n$ (10 times greater than water to ensure a laminar flow) and T is the temperature (in Kelvin). FIDAP was used to solve for temperature and velocity profiles in the heating, holding and cooling sections of an aseptic process, for the parameters of Table 1(b).

As a result of the temperature (and thus viscosity) profile, the velocity profile varies through the process (Fig. 3):

- In the *heater* and more especially during the first few meters, the temperature is much higher at the wall than in the bulk; the wall viscosity is lower so the resulting velocity is higher than would be predicted by an analytical profile based on an isothermal flow.
- At the *holding* tube outlet, the flow is almost isothermal again so that the velocity profile is close again to the analytical isothermal profile.
- The *cooler* gives the opposite situation to the heater; the fluid is colder near the wall, so the viscosity is higher than in the bulk and the velocity lower than the predicted analytically.

These temperature and flow variations are crucial in considering the evaluation of total integrated sterility or quality parameters. Here this has been done in two ways:

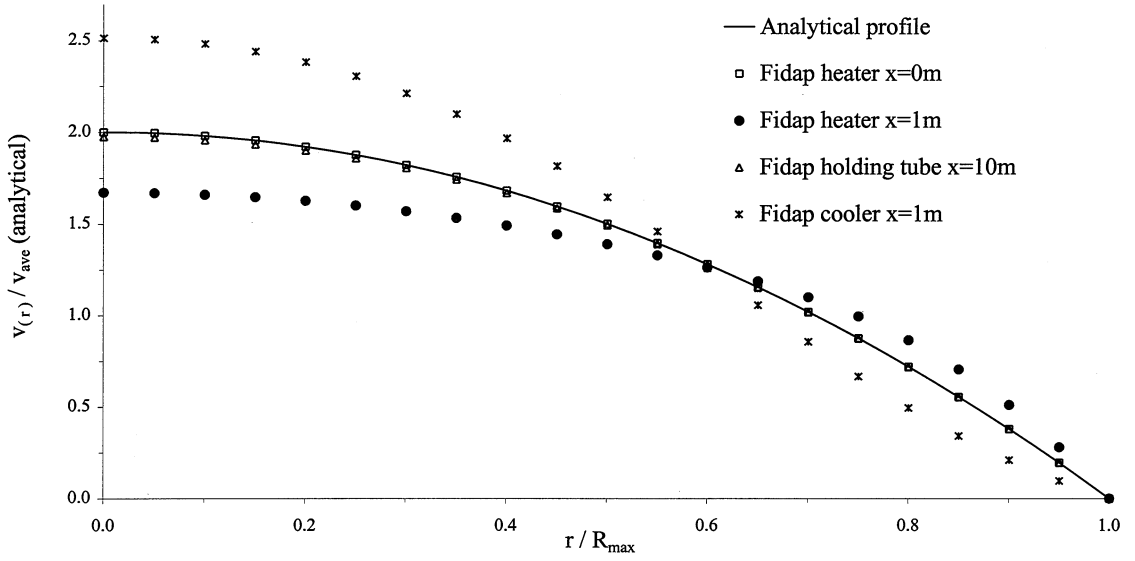

Fig. 3. Influence of the temperature dependence of the viscosity on velocity profiles for the processing conditions of Table 1 (b).

(i) FIDAP has been used to calculate the velocity and temperature fields. The temperature profile at a certain radius can thus be predicted, so that Eq. (7) and its counterpart for C-value can be solved at all radii. This allows the average expression (Eqs. (10) and (11) to be used at any axial distance along the system.
(ii) $\bar{F}$ and $\bar{C}$ can also be estimated at any point using the mean temperature over the cross section, calculated either by FIDAP (Eq. 21)) or the approximations in Eqs. (19) and (20). This approach is common industrially.

In both cases, details of the kinetic parameters for sterility and quality are given in Table 2 and the systems simulated are given in Table 1 (c). Two different fluids were modelled, a Newtonian fluid with constant viscosity (0.001 Pa s), and a model food fluid with temperature dependent power law parameters, shown in Table 3. The thermal conductivity of the fluid is $0.6\ \mathrm{W\,m^{-1}\,K^{-1}}$ throughout.

Table 2
Kinetic parameters used for sterility and quality values

Sterility (C. Botulinum) Stumbo et al. (1950)	Quality (Thiamine destruction) Mulley et al. (1975)
$D_{121.1} = 12.4$ s	$D_{121.1} = 13800$ s
$z = 10°C$	$z = 48°C$

4.2. Results for a Newtonian fluid of constant viscosity

The temperature profile of the fluid is shown in Fig. 4, in terms of (i) the centre line ($r = 0.0$ m), (ii) 2 mm from the wall ($r = 0.013$ m) and (iii) the mean temperature, calculated using Eq. (20). The three regions of the process can be seen clearly:

(i) In the *heater*, with a wall temperature of 140°C, the wall region responds very quickly, with a temperature above 120°C after only about 2 m. The centre line, however, takes more than 4 m to respond, and at the exit of the heater has only reached 83°C, whilst the wall region is at 131°C. The mean temperature at 106°C is the mean of a very wide range.
(ii) Within the adiabatic *holding* section, the mean temperature, of course, is uniform. The wall region cools rapidly towards the mean whilst the centre heats up, reaching 105°C at the exit.
(iii) The *cooler* shows the effect of the thermal conduction in the fluid most clearly. The wall region cools rapidly, but the centre of the fluid *continues to heat* slightly over 3 m, until the effect of the cooler propagates into the central region. After that point, it begins to cool. The average temperature is below 60°C at the exit of the cooler, but the centre line is still only at 82°C.

These results demonstrate the problems of processing a flowing homogeneous viscous food material, and the potential danger of using the mean temperature. Figure 5 (a) and (b) show the corresponding sterility and quality values, $\bar{F}$ and $\bar{C}$.

(i) The sterility in the wall region rises steeply in the heating section. The mean value is always much lower than the value in the wall region; the mean sterility increases significantly only in the holding

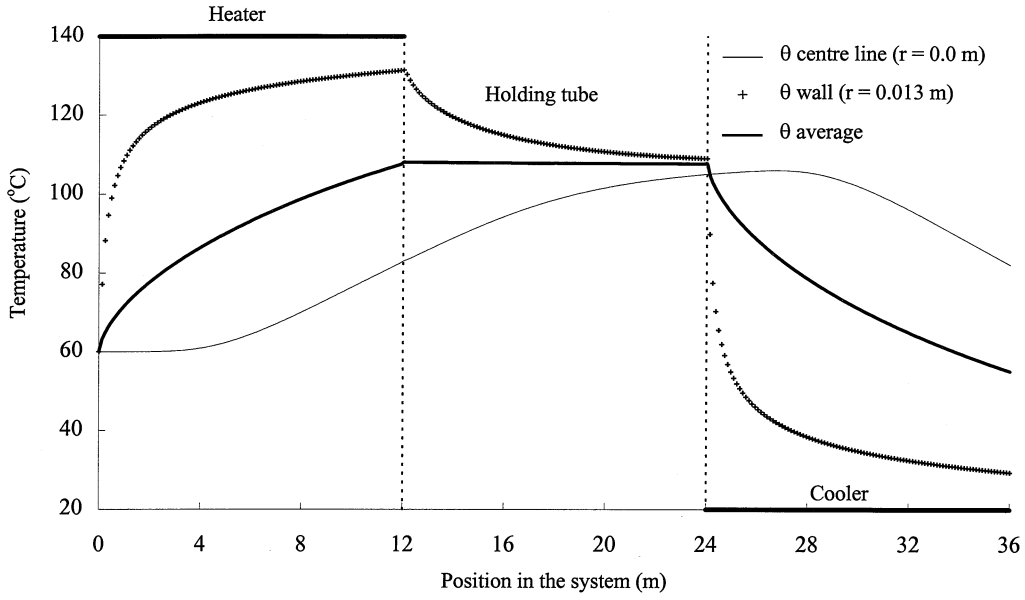

Fig. 4. Temperature profile for the processing conditions of Table 1 (c) (Newtonian fluid-constant viscosity).

section. The values of sterility in the centre line region continue to increase in the cooling section, because of the time required to conduct heat away from the centre.

(ii) Similar effects are seen for quality. The C value for the wall region builds up rapidly in the heater and holding section, but does not change in the cooler. The C value for the centre line increases through the flow so that the mean C continues to increase.

This example demonstrates the range of F and C that can be found in realistic situations. The chosen case would not be commercially acceptable, because of the low centre line F-value: however the principles still apply. Increasing the wall temperature will increase the centre line F, but at the cost of even greater loss of quality in the wall region. A series of simulations have been carried out to study a range of wall temperatures, to identify the differences between the full and the partial solutions.

Fig. 6 (a) shows the effect of changing the wall temperature in the range 110–190°C. In both cases, the increase in sterility with temperature is larger than the quality, reflecting the difference in activation energies. However, when the results from FIDAP and that by assuming the mean temperature are compared, it seems that:

(i) the sterility of the product is significantly overestimated by the mean-temperature approximation, i.e. the cold centre of the tube, of low sterility, is not fully accounted for.

(ii) the quality of the product is also overestimated — i.e. the mean-temperature approximation gives a lower $\bar{C}$ than the more accurate computational situation. In this case it is the rapid loss of quality in the hot, near wall region of low velocity which is neglected by the approximation.

The proportion of the fluid in the wall region (experiencing a 'high temperature – long time' treatment) is responsible for the important losses in quality, whereas the centre region (experiencing a 'lower temperature – shorter time' treatment) is responsible for the underestimation of the sterility of the final product. This clearly shows the danger implicit in the conventional approach. Accurate understanding of temperature profiles is key to predicting output conditions: solving for flow and temperature will give a better representation than any mean flow assumption because of the strong temperature dependence of the reaction processes.

As noted in the introduction, it is conventional in the food industry to ensure product safety by assuming that the fluid travels twice as fast as the mean (Rees and Bettison, 1991). $\bar{F}$ and $\bar{C}$ have been calculated on that basis. The process tube lengths have been set by assuming that the flow is perfectly mixed and all the fluid travels at twice the true mean velocity (i.e. the centreline maximum of a Newtonian fluid); but the temperature profile is that given by Eq. (20). The residence time distribution of the fluid is thus taken as the most conservative in terms of process safety. Fig. 6 (b) compares this assumption with the full solution of FIDAP for the real case of a velocity profile. The graphs show that the assumption is indeed a safe one: the predicted $\bar{F}$ is below the more accurate version calculated by FIDAP. However, because all of the fluid is assumed to spend much less time in the process than in reality, the predicted

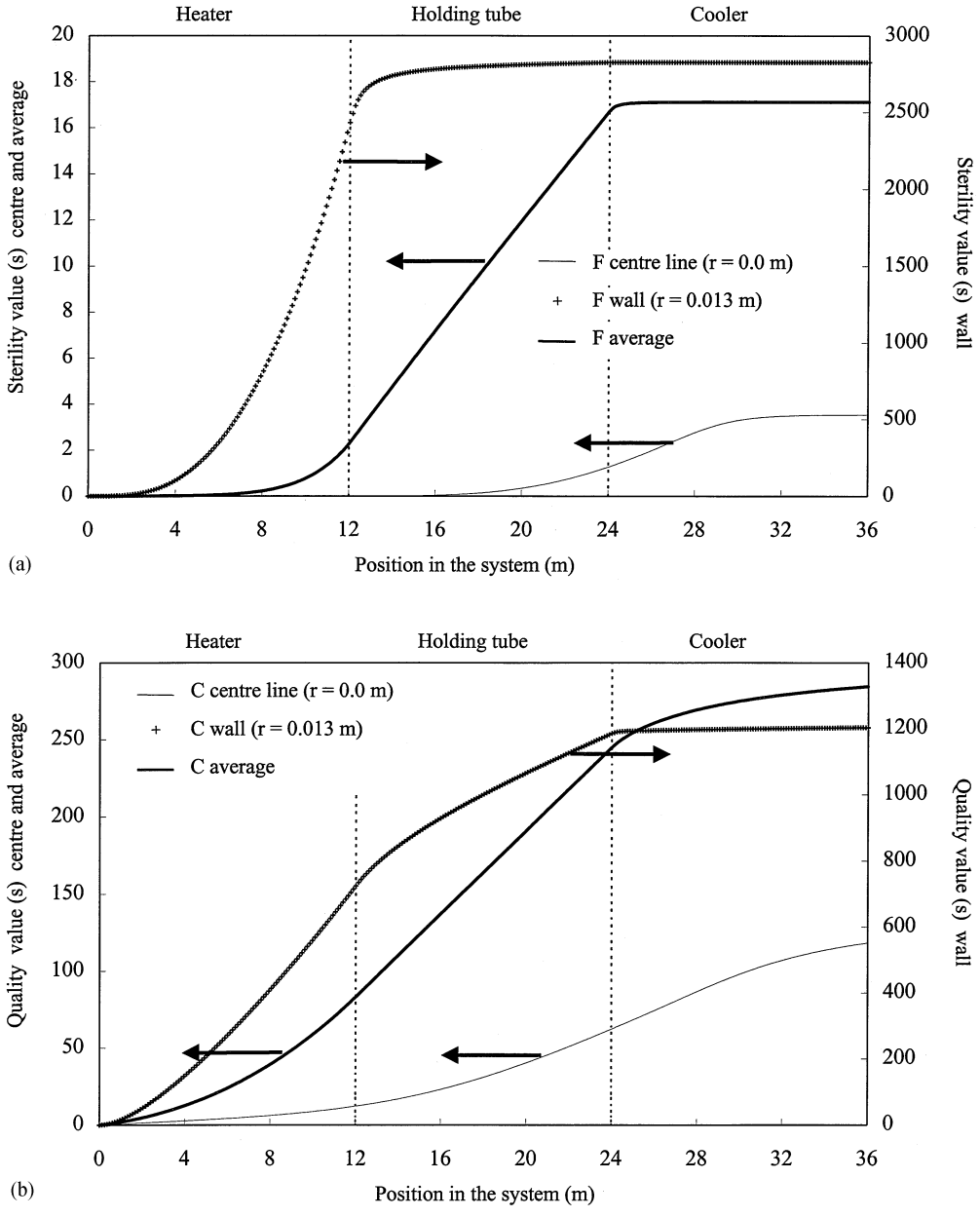

Fig. 5. (a) Sterility profile for the processing conditions of Table 1 (c) (Newtonian fluid-constant viscosity) (b) Quality profile for the processing conditions of Table 1 (c) (Newtonian fluid-constant viscosity).

quality value using this assumption is much greater than the actual case. For processes designed using this assumption, products will have a greater $\bar{F}$, i.e. higher sterility, but a much higher $\bar{C}$, i.e. much worse quality, than the approximate calculations suggest. For example, if an $\bar{F}$ of 180 s is required, FIDAP suggests a wall temperature of 176°C, whilst the twice-mean assumption suggests a temperature above 200°C. The actual quality value in the latter case will be above 2300 s, whilst that using FIDAP as a design tool will be 1200 s. The results show that products of much lower quality than necessary are made by using this assumption.

4.3. Results for a power-law fluid of temperature-dependent viscosity

The same procedure was applied to a different type of fluid (power law) more frequently found in the food industry. The effect of temperature was taken into account for both the power law index (n) and the consistency (k). The variation of these parameters is reported in Table 3, and is similar to the usual effects of temperature on the viscosity of power-law fluids (Holdsworth, 1993).

Fig. 7 shows the effect of changing the wall temperature in the same range as in the previous Section (4.2). In

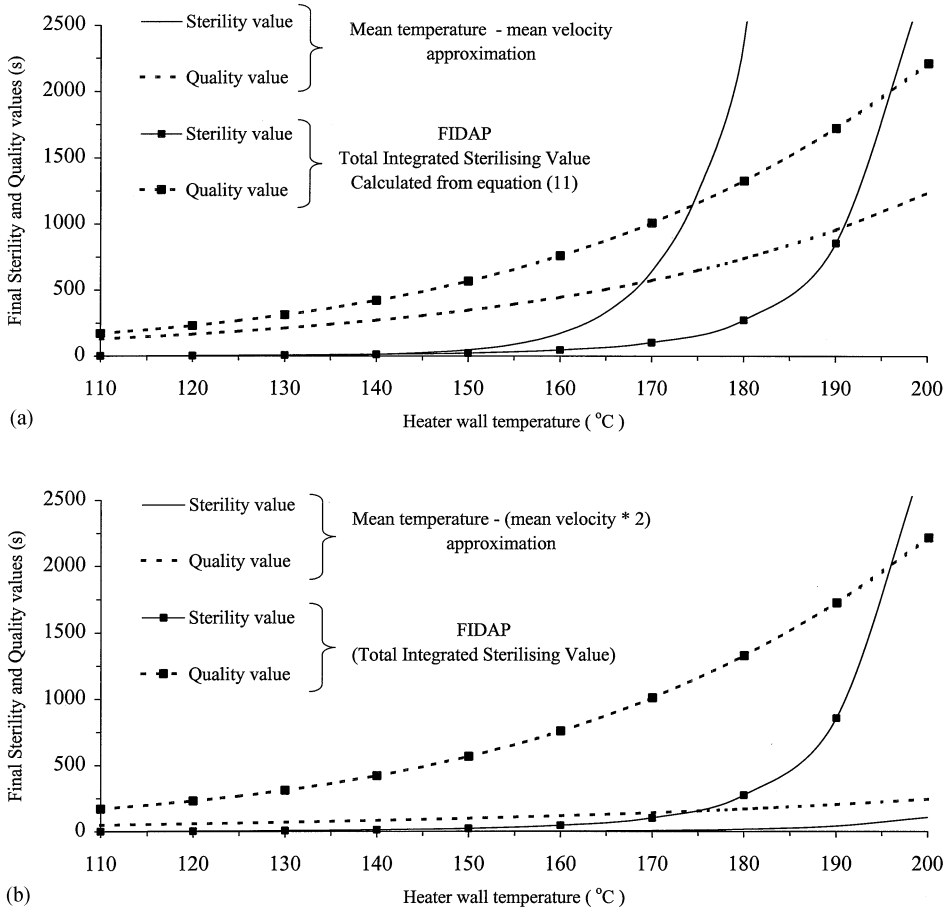

Fig. 6. (a) Quality and sterility values after processing for the conditions of Table 1 (c) (Newtonian fluid-constant viscosity). (b) Quality and sterility values after processing for the conditions of Table 1 (c) Using the conservative approach (Newtonian fluid-constant viscosity).

Table 3
Temperature dependence of the viscosity parameters (Power-law fluid)

$\eta = k \exp(A_1\theta + A_2\theta^2)\dot{\gamma}^n$
$n = a + b\theta$
where
$k = 6.64$ Pas
$A_1 = -1.19 \times 10^{-2}\,^{\circ}\mathrm{C}^{-1}$
$A_2 = -4.07 \times 10^{-6}\,^{\circ}\mathrm{C}^{-2}$
$a = 0.281$ N.D.
$b = 1.4 \times 10^{-3}\,^{\circ}\mathrm{C}^{-1}$

this case the mean temperature/mean flow assumption overpredicts both the sterility and quality values, i.e. the approximation is still not safe in terms of sterility (same situation as in Section 4.2) but here, the quality is better than predicted with the approximation.

The high wall temperatures in the heater will cause an increase (of up to 20 to 30%, shown in Fig. 3) in the fluid velocities near the wall. As previously explained, the losses in quality are most significant near the wall: here, the fluid in this region is thus less exposed to a High Temperature *Long Time* treatment and the final product quality is better than predicted with the approximation. The centre region of lower temperatures is however still not fully accounted for, so that the resulting sterility is still overestimated by the mean temperature assumption.

4.4. Breakdown of the HTST approximation

Conventional food processing has always used the high-temperature–short-time concept, i.e. assuming that a short process time at a high temperature will give the highest-quality product. Assuming a mean temperature will, as found above, result in the omission of the high- and low-temperature parts of the stream and their effects on sterility and quality. The full flow solution can be used to test the consequences of the omission of these various fractions.

A series of simulations were thus carried out to study the effect of varying the process conditions. It was assumed that a constant heater length of 12 m was available; the objective of the thermal treatment was to reach a final sterility of 3 min at the exit of the holding section.

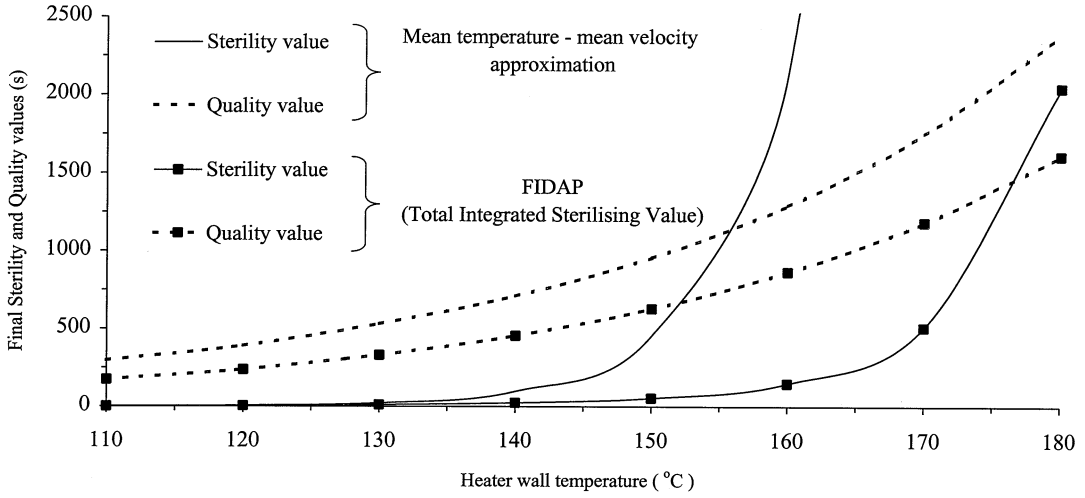

Fig. 7. Quality and sterility values after processing for the conditions of Table 3 (Power-law fluid-viscosity = f(temperature)).

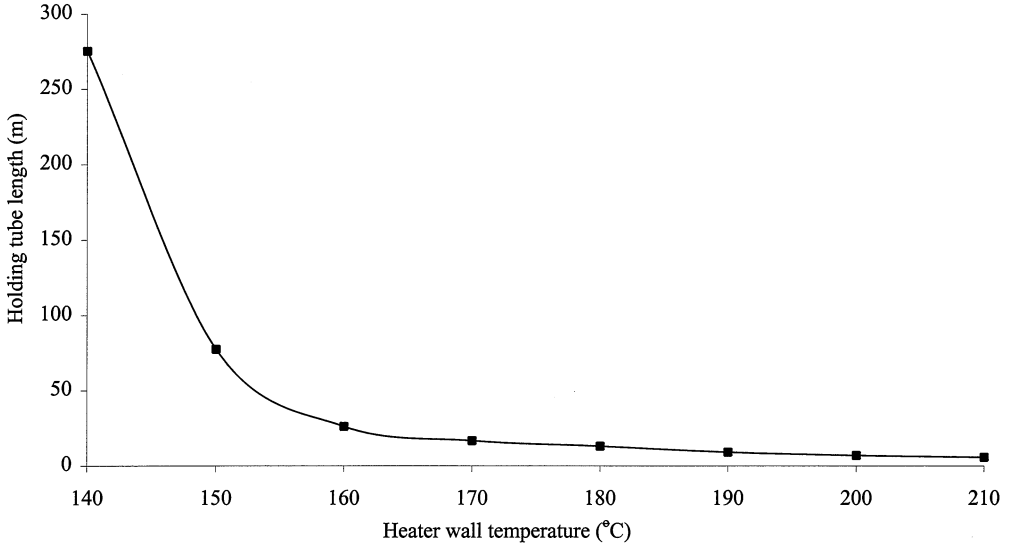

Fig. 8. The effect of the heater wall temperature on the holding tube length required to obtain a sterility value of 180 s at the holding tube outlet.

Details of the process parameters and the fluid used for this case are given in Table 1 (c). The two variables are thus the heater wall temperature and the corresponding holding tube length. This situation is analogous to that of a true industrial problem: frequently the cooling section is not taken into account in calculating the product sterility, and the holding tube length can readily be changed within the factory environment.

Fig. 8 (a) shows the variation in the required holding tube length with the heater wall temperature: the length becomes significantly shorter as the temperature increases, but with little changes in length for temperatures above 170°C. The corresponding values for the sterility and quality throughout the system are given in Figs. 8 (b)–(d). Within the heater, little sterility accumulates, but the quality falls as the temperature increases as a result of the increasingly high thermal processing of the wall region. Fig. 8 (a) shows that the quality values can vary by a factor of five. This effect carries on into the holding tube, at which the two effects compete:

(i) for the coolest heater wall temperatures, the holding tube is long and the residence time of fluids correspondingly high. Most of the processing is done within the holding tube when the temperature has equilibrated and the system is essentially isothermal. Thus in this limit the HTST effect is seen: increasing the heater wall temperature will allow the use of shorter

holding tubes and will result in products of better quality (lower C) for a constant sterility requirement (same F).

(ii) as the temperature increases, however, the effect of overcooking of the wall layer can be identified: despite the shorter hold tubes, the effect of the wall layer overheating is such that the final product quality falls (higher C). The shortening of the hold tube length is not sufficient to overcome this problem as most of the quality losses have occurred within the heater: for example, for a wall temperature of 200°C 65% of the quality loss occurs within the heating section; the damage has been done before the holding section. The size of the hold tube length is essentially controlled by the need to get the centre of the fluid up to a temperature where the sterility is at the required value, and this results in excessive processing of the wall region. Fig. 8 (c) thus shows the quality optimum at which a minimum C is found. For given process parameters, below the optimum heater wall temperature the quality losses are due to the longer holding time required whereas above the optimum the quality decreases because of the overprocessing in the wall region occurring mostly in the heater. Such an effect will of course not be predicted by the mean-temperature assumption.

The effect of the cooling section is shown in Fig. 8 (c) and shows the effect of the previous two stages are continued: the quality optimum remains. However, the effect of the problems in conducting heat to the centre of the fluid can clearly be seen: as the temperature increases the slowness of thermal conduction results in an increase in the sterility of the system above the target value (180 s); in this case, heating above the optimal value for the heat-hold combination also gives excessive F throughout the rest of the system. This further emphasises the problems inherent in designing sterilisation processes. The heating, holding and cooling sections should not be simply considered as individual or distinct sections: the heater wall temperature has not only an importance on the required holding time but also affects the overall quality and sterility of the product at the exit of the holding and cooling sections. Considering the accumulation of sterility in the cooling section as a security factor will in most cases result in unnecessary losses in the final product quality (Fig. 9).

5. Discussion and conclusions

Food processing presents a series of reaction engineering problems: optimising product quality whilst maintaining product safety. This work has developed computational models for the common industrial process of the sterilisation of a flowing food fluid. The basic equations for flow and heat transfer have been solved using a commercial FE code together with the equations for predicting sterility and quality. The program accurately predicts analytical solutions for flow and heat transfer, suggesting it can be used in this case. Time-temperature profiles obtained using FIDAP successfully showed the potential errors that could be made when estimating the sterility and quality of a liquid food in a continuous flow sterilisation process. The common safety margin used in the food industry leads to significant overprocessing and thus unnecessary losses in product quality. Under some process parameters (such as high heater wall temperature), up to 90% of the accumulation of product sterility can occur in the cooler, often neglected or taken as a safety margin. Assuming that all the fluid travels at the maximum velocity (twice the mean velocity for a Newtonian fluid) will lead in any case to a safe product but the degradation of the product quality can be as high as 10 times the necessary to ensure a safe product.

The model suggests that under some conditions the HTST approximation is invalid: the temperature gradients within a flowing system are such that near-wall regions are overprocessed. The work suggests that computer codes such as those used here could be used both to analyse actual homogeneous product quality and sterility from given equipments but also as a design tool for potential equipment to obtain the maximal product quality for a given required sterility. The natural and mixed convection effects observed experimentally in the work of Leuliet et al. (1993), will have to be accounted for in further developments of the model. Clearly this work has been a paper study alone; experimental validation will be necessary too, in order to demonstrate the range of quality on a real material.

This work has considered the laminar flow of a homogeneous fluid. The problem will be further complicated by the presence of particulates. These will also change the flow profile but may enhance radial mixing in the fluid (Lareo et al., 1997). Heat transfer into and from particles will be controlled by thermal conduction, the coldest region of the flow may be the particle centre rather than the fluid centre line. The analysis will in principle still apply however.

The greater the degree of radial mixing the greater the homogeneity of the material. Turbulent flows might thus be expected to show these effects to a lesser extent: however, few foods, such as milk, have a viscosity low enough to allow them to be processed in turbulent flow. Even for turbulent flows, such as for UHT processing of milk in plate heat exchangers, 'burnt' flavours are a problem. The addition of static mixers will decrease radial variation in velocity and temperature. However, such devices are difficult to keep clean and hygienic and may create contamination problems. More work is necessary to devise practical solutions for the continuous processing of foods.

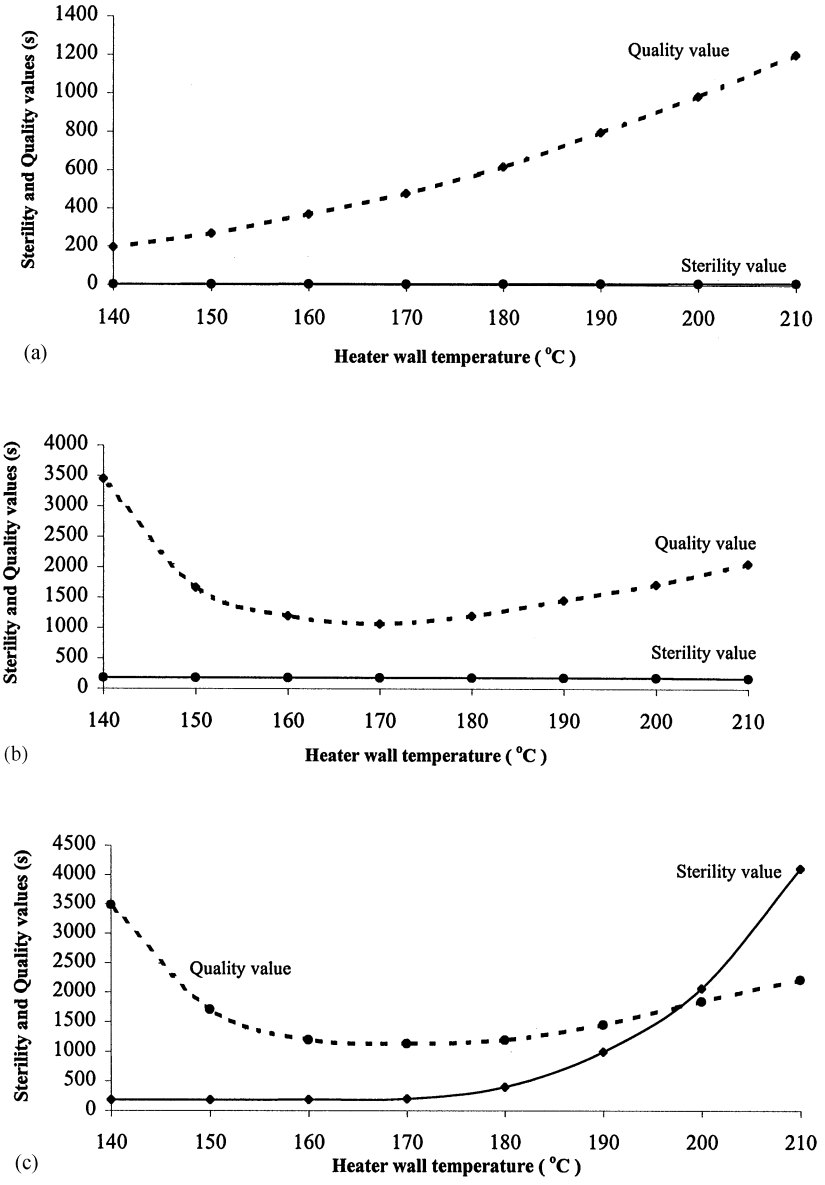

Fig. 9. Calculation of the effect of heater wall temperature on the Sterility and Quality values for the processing conditions of Table 1 (c). (a) at heater exit, (b) at holding tube exit, (c) at cooler exit.

Notation

C	quality value (s) as defined in Eq. (12)
C_{ref}	quality value (s) at reference temperature
$\bar{C}_{\mathrm{ref}}$, $\bar{C}$	mean integrated quality value (s) at reference temperature as defined in Eq. (10)
Cp	fluid specific heat, J kg^{-1} K^{-1}
D	pipe diameter, m
D_θ	decimal reduction time, s
D_{ref}	decimal reduction time, s at reference temperature
E	activation energy, J kmol^{-1}
F	sterility value, (s) as defined in Eq. (6)
$\bar{F}_{\mathrm{ref}}$, $\bar{F}$	mean integrated sterility value (s) at reference temperature as defined in Eq. (10)
F_{ref}	sterility value (s) at reference temperature
F^i_{ref}	sterility value (s) at reference temperature at the radial subdivision (i) in a pipe
$h_{\mathrm{ave}}(x)$	average wall-to-liquid heat transfer coefficient after x m of a pipe
k	fluid consistency, Pa s^n
k_θ	reaction rate constant of micro-organism heat inactivation at temperature θ
$\dot{m}$	fluid mass flow rate, kg s^{-1}
m_0, m_1, m_2	coefficient of the dimensionless sum expressed in Jakob (1949)

N	number of micro-organism
N^0	initial number of micro-organism
N^f	final number of micro-organism
$\dot{Q}$	fluid volumetric flow rate, $m^3\ s^{-1}$
r	radial position in a pipe, m
R	universal gas constant, $J\ kmol^{-1}\ K^{-1}$
R_0, R_1, R_2	coefficient of the dimensionless sum expressed in Jakob (1949)
R_{max}	maximum pipe radius, m
S_i	cross section area of the radial subdivision of a pipe, m^2
t	time, s
T	temperature in Kelvin
v_{ave}	fluid average velocity within a section of a pipe, $m\ s^{-1}$
$v(r)$	fluid velocity at the radial position (r) in a pipe, $m\ s^{-1}$
v_i	fluid velocity at the radial subdivision i in a pipe, $m\ s^{-1}$
$v(x, r)$	fluid velocity at the radial position (r) and axial position (x) in a pipe, $m\ s^{-1}$
x	axial position in a pipe, m
z	temperature change, °C giving 10-fold difference in decimal reduction time

Greek letters

$\dot{\gamma}$	shear rate, s^{-n}
$\dot{\gamma}_{wall}$	shear rate at the pipe wall, s^{-n}
θ	temperature, °C
θ_{ref}	reference temperature, 121.1°C for sterility values and 100°C for quality values
θ_{wall}	temperature at pipe wall, °C
θ_{in}	initial homogeneous product temperature, °C
$\theta(x)$	average fluid temperature in a section of a pipe at the axial position (x) obtained from Eq. (20)
$\theta(x)_{FIDAP}$	average fluid temperature in a section of a pipe at the axial position (x) obtained from Eq. (21)
$\theta(x, r)$	fluid temperature at the radial position (r) and axial position (x) in a pipe
$\theta_i(r)$	fluid temperature at the radial subdivision i and axial position (x) in a pipe
$\Delta\theta$	arithmetic mean fluid temperature difference as defined for Eq. (20)
λ	fluid thermal conductivity, $W\ m^{-1}\ K^{-1}$
τ	shear stress, Pa

Abbreviations

HTST	high-temperature–short-time
FE	finite element
RTD	residence time distribution
CFD	computational fluid dynamics

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