THE EFFECTS OF HEATING MODE AND INTERNAL ROLL TEMPERATURE
ON ROLL DURABILITY AND EFFICIENCY OF IMPULSE DRYING

Project 3470

Report 5

to

MEMBER COMPANIES
of the
INSTITUTE OF PAPER SCIENCE AND TECHNOLOGY

December 1991

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THE EFFECTS OF HEATING MODE AND INTERNAL ROLL TEMPERATURE ON ROLL DURABILITY AND EFFICIENCY OF IMPULSE DRYING

Project 3470
Report 5

A Progress Report
to
MEMBER COMPANIES
OF THE
INSTITUTE OF PAPER SCIENCE AND TECHNOLOGY

By
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SUMMARY

Vacuum deposited surface thermocouples have been used to measure heat flux during impulse drying with ceramic coated press surfaces. The measured heat flux was used as a boundary condition to a numerical heat transfer model to predict internal press roll temperatures and roll heating efficiency. The simulations demonstrate that maximum roll durability and heating efficiency can be realized when the press roll is externally heated and when internal roll temperatures are maximized.

INTRODUCTION

In current practice, energy intensive evaporative drying is used to dry paper. Early research showed that a significant fraction of that energy could be saved by impulse drying(1). Unfortunately, implementation of the technology was halted as impulse drying was found to induce defects termed "sheet delamination"(2).

Ongoing research at the Institute of Paper Science and Technology has focused on process design modifications that eliminate sheet delamination. By replacing metallic press surfaces with low heat capacity, low thermal conductivity ceramics, sheet delamination can be avoided(3-6). While additional research is underway to expand the operating range of these ceramics, the present ceramic design will be evaluated on a pilot-scale press in the near future. This paper focuses on a number of design issues relating to the demonstration of impulse drying on a commercially configured press section.

Figure 1 shows a crown-compensated extended-nip press configured with a ceramic coated press roll. In the impulse drying mode, the hydrostatic support element will load the roll shell, which revolves at a speed of 760 m/min, to a peak pressure of 6 MPa. An oil film between the element and the moving shell provides lubrication and acts as a heat sink for heat lost to the interior of the roll. An extended nip of 40 ms duration is developed by using an elongated press shoe.

Wet paper carried on a felt enters the extended nip at 40% dryness at location A and leaves the nip at 60% dryness at location C. The roll is heated in a zone from location D to E such that the temperature of the roll surface at the entrance to the nip at A is 370°C.
With impulse drying, water is pressed out of the sheet by a combination of mechanisms that enhance normal wet pressing. These include a reduction in liquid water viscosity and increased fiber conformability due to higher sheet temperatures and increased internal hydraulic pressures resulting from vapor formation at the interface of the press roll and the sheet.

Incremental improvements in water removal over conventional pressing achieved by raising the roll temperature can not be accounted for based on the amount of liquid water that is evaporated in the process. Hence, impulse drying results in improved water removal and evaporative energy savings. To achieve this energy savings in practice, process equipment must be designed to facilitate efficient roll heating. This means that the ceramic coating press roll should be designed to maximize roll heating efficiency.

As the ceramic coatings will be applied to a steel or cast iron shell, the ceramic coating and roll heating technology should also be chosen to minimize thermal stresses resulting from excessive temperature fluctuations at the ceramic/metallic interface.

A combined experimental and analytical research program has been undertaken to address these design issues. The experimental work consisted of measurements of heat flux from the ceramic coating to the paper during impulse drying using a low mass, high response, vacuum deposited surface thermocouple. The analytical work consisted of the development of a numerical heat transfer simulation of the press roll using the experimental heat flux data as a boundary condition.
EXPERIMENTAL MEASUREMENT OF HEAT FLUX

Impulse Drying Simulation

Fig. 2 shows a schematic of the electrohydraulic press used to simulate impulse drying and to measure heat flux.

Figure 2. Schematic of the electrohydraulic press.

In the experiments wet sheets of paper on felts were placed onto a wire felt support attached to a steaming ring. A radiation shield was automatically positioned between the heated platen and the sheet to reduce dry-out of the top surface of the sheet. Steam exiting the ring flowed upward through the felt and the sheet. By controlling steam pressure and adjusting the steaming time the initial temperature in the sheet was raised to 85°C.

Once the sheet was heated the hydraulic system was activated to give a haversine pressure pulse of 40 ms duration and 6.2 MPa peak pressure. The temperature at the surface of the ceramic platen was recorded as a function of time during the impulse drying event. As the structure and thermal properties of the ceramic coating were known, heat flux to the paper sheet could be calculated as a function of time.
Ceramic Thermal Properties

The ceramic coating consisted of plasma sprayed zirconium oxide layers with controlled porosity. The surface layer was 0.05 mm thick with 7% porosity, while the inner layer, in contact with the steel platen, was 0.38 mm thick with 15% porosity. Porous ceramics have thermal properties that are dependent on the properties of the solid ceramic and the void fraction as follows.

\[ \rho = \rho_{so}(1-v) \]
\[ C_p = C_{pso} \]
\[ k = \frac{k_{so}}{(1+C'v)} \]
\[ \alpha = \frac{k}{\rho C_p} = \frac{\alpha_{so}}{(1-v)(1+C'v)} \]

To determine the properties of the two ceramic layers, three plasma sprayed zirconium oxide samples were prepared at three different void fractions. Void fraction was measured from cross-sectional micrographs while specific heat was measured by differential scanning calorimetry. Thermal diffusivity was measured by a laser flash diffusivity method.

The specific heat data were fit to a polynomial in temperature,

\[ C_{pso} = \sum_{n=0}^{3} \beta_n (T_a)^n \]

where the coefficients \( \beta_n \) in units of J/(g\(^\circ\)K) were,

\[ \beta_0 = 0.12161 \]
\[ \beta_1 = 1.84 \times 10^{-3} \]
\[ \beta_2 = -2.60 \times 10^{-6} \]
\[ \beta_3 = 1.3277 \times 10^{-9} \]

The diffusivity data were scaled with the following equation.

\[ \alpha_{so} = \alpha (1-v) (1+C'v) \]

Where \( C' \) was introduced as an empirical constant. The scaled data were then fit to the following polynomial in temperature.

\[ \alpha_{so} = \sum_{n=0}^{4} \gamma_n (T_a)^n \]

A value of \( C' \) of 8.52 maximized the fit. The corresponding coefficients to give \( \alpha_{so} \) in units of cm\(^2\)/s are
Thermocouple Design

Accurate calculations of heat flux require accurate surface temperature measurements during the impulse drying event. For steel platens an eroding tip probe thermocouple may be mounted in the platen with the tip flush with the surface. For ceramic coated platens that thermocouple type cannot be used because the thermal properties of the probe do not match that of the ceramic.

Design requirements for a thermocouple for use with a ceramic coating include:

1) The thermocouple must be exactly at the ceramic surface.
2) The thermocouple must be constructed to assure one-dimensional heat flow.
3) The response time must be fast, 10-20μs.
4) There must be no thermal contact resistance between the thermocouple and the ceramic.
5) The thermocouple must have low mass.

Preliminary experiments show that ribbon thermocouples stretched across the ceramic surface can yield questionable heat flux due to failure to meet the last two conditions(5). In this work all of the criteria were met by using a small vacuum deposited thermocouple which was applied directly to the ceramic surface.

Because alloys do not deposit uniformly, pure copper and nickel were chosen as the thermocouple components. This choice gave the best millivolt response of commonly available materials. To make the thermocouple the leads were mounted in the platen with a separation of 1.5 mm with the exposed ends flush with the ceramic surface. A copper film was deposited over the copper lead. Then a nickel film was deposited over the copper film and nickel lead yielding a thermocouple having a thickness of 0.001 mm and a surface area of 3 mm$^2$.

A copper/nickel reference junction was also constructed and was kept in an ice bath. The completed thermocouple was calibrated using known temperatures over a range of temperatures expected in the impulse drying experiments.

Heat Flux Calculations

For one-dimensional heat flow in a semi-infinite solid with constant thermal properties, constant initial temperature, and a time variant surface temperature, the temperature profile within the solid was given by(7),
\[ T(x,t) = \frac{x}{2\sqrt{\pi}a} \int_0^t \Phi(\eta) \frac{\frac{x^2}{4a(t-\eta)} e^{\frac{4\alpha(t-\eta)}{(t-\eta)^{3/2}}} d\eta}{(t-\eta)^{3/2}} \]

where,

- \( x \) = distance from surface
- \( \Phi(t) \) = function defining surface temperature
- \( \alpha \) = thermal diffusivity
- \( \eta \) = variable of integration
- \( k \) = thermal conductivity.

The corresponding heat flux was then,

\[ q(x,t) = -k \frac{\partial T(x,t)}{\partial x} \]

Differentiation at the surface yields,

\[ q(t) = q(0,t) = \frac{k}{\sqrt{\pi}a} \int_0^t \frac{d\Phi(\eta)}{d\eta} \frac{d\eta}{(t-\eta)^{1/2}} \]

Experimental data can not be accurately described by a simple function \( \Phi(t) \). However, for small time intervals, \( \frac{d\Phi(t)}{dt} \) can be approximated by the local slope of the data, \( m = \frac{\Delta T}{\Delta t} \).

\[ q(t) = \frac{k}{\sqrt{\pi}a} \sum_{i=1}^n m_i \int_{t_i-1}^{t_i} \frac{d\eta}{(t_n-\eta)^{1/2}} \]

Solving the integral,

\[ q(t) = \frac{2k}{\sqrt{\pi}a} \sum_{i=1}^n m_i \left[ \frac{(t_n-t_{i-1})^{1/2} - (t_n-t_i)^{1/2}}{1/2} \right] \]

Experimental temperature data were collected every 100\( \mu \)s. Also, enough data points were collected before and after the impulse drying event to insure accurate calculations.

The local slopes were calculated using a seven point least squares routine centered on the desired data point.
The initial assumption of constant physical properties was relaxed in that it was assumed that the properties were "locally" constant. An array of the local constants was calculated using the thermal properties defined previously.

\[ C_i = \frac{2k_i}{\sqrt{\pi \alpha_i}} = 2\rho C_{pi} \sqrt{\frac{\alpha_i}{\pi}} \]

where,

\[ \rho = \rho_{so}(1-v) \]
\[ C_{pi} = C_{psoi} \]
\[ \alpha_i = \frac{\alpha_{soi}}{(1-v)(1+C'v)} \]

It was assumed that the platen was a semi-infinite ceramic solid with a constant average void fraction, \( v = 0.1 \). As is shown in a subsequent section, this assumption was reasonable because the cooling wave does not penetrate into the steel during a single drying event.

After all the array's were calculated, the instantaneous heat flux was calculated for each data point. Cumulative energy transfer was calculated from the heat flux by a trapezoidal integration technique.

\[ E_i = E_{i-1} + \Delta t \frac{(q_i + q_{i-1})}{2} \]

Results Of Heat Flux Measurements

Sheets of single-ply linerboard, at a basis weight of 205 gsm were formed on a slow speed web former from virgin southern pine kraft which had been refined to either 550 or 650 ml Canadian standard freeness. The sheets were pressed to either 35% solids or 42% solids and preheated to 85°C prior to impulse drying on the hydraulic press over a range of ingoing ceramic surface temperatures from 150°C to 400°C. Impulse drying was simulated for the case of a 40 ms nip at peak pressures of both 3.1 and 6.2 MPa.

Typical platen surface temperature versus time data at three different ingoing platen surface temperatures are shown in Fig. 3. Heat flux calculated from that data are shown in Fig.4.

As had been previously observed(5), heat flux was only dependent on the initial temperature of the ceramic surface. Fig.5 shows a correlation of heat flux as a function the initial temperature difference between the ceramic press surface and the sheet as a function of time.
Figure 3. Platen Surface Temperature Versus Time During Impulse Drying.

Figure 4. Heat Flux Versus Time During Impulse Drying.
Figure 5. Heat Flux Correlation.

NUMERICAL HEAT TRANSFER MODEL

The heat transfer model was developed to determine the temperature profiles within the roll and the distribution of heat from the heat source to the nip, to the ambient, and to the internal heat transfer medium.

The model was based on the roll structure shown in Figure 6 consisting of two relatively thin layers of ceramic over a cylindrical metal (cast iron) shell approximately 0.1016 m thick. At the commercial scale the roll will be in contact with a heat transfer fluid to control and cool the inside temperature.
In the nip zone heat is transferred from the roll to the paper and the general direction of heat flow is from the interior of the roll toward the surface. Heat is added to the roll over a heating zone near the inlet to the nip to balance heat transfer to the nip and other heat losses and to return the surface temperature to a desired set point. For the infrared heating mode, where heat is added to the external surface, the general direction of heat flow is from the surface into the interior of the roll. For the alternate induction heating mode, where heat is generated within a thin metal layer at the ceramic-steel interface, heat flows both toward the surface and into the metal. Between the nip and heating zones the flow of heat may be in either direction thus equalizing the temperature profiles in the roll. Despite the fluctuations in the surface temperature there is a time-averaged thermal gradient from the ceramic-metal interface to the heat transfer medium in the center of the roll resulting in a substantial heat loss.

In the cylindrical coordinate system shown, the Z or cross-machine direction is assumed to be infinite and heat transfer takes place in the radial, R, direction. The direction of rotation is in the θ direction. The transient model is based on an angular slice of the roll of arbitrary angular extent, Δθ, rotating at a constant velocity, V, experiencing a transient heat flux at the outer surface and transferring heat to a heat transfer medium.

Heat is transferred to the roll in one of two ways: externally by infrared radiation or internally by magnetic induction. In both heating modes the heat flux is confined to a specific length of time corresponding to a specific circumferential distance as shown in Figure 6.
Based on the above assumptions the model was applied to a finite element of variable width or angular extent which depends on the variable time step as shown in Figure 7. The differential element is exposed to a transient heat flux at the outer boundary or at the induction zone through each revolution.

![Multi-Layer Roll Structure](image)

Conduction in the $\theta$ and cross machine (into the plane) or $Z$ directions is negligible. The relation between the variable change in surface distance, $\Delta s$, and the variable time step, $\Delta t$, is

$$\Delta s = V \Delta t$$

The most general form of the energy balance for each layer then reduces to the following transient one-dimensional heat conduction equation,

$$\rho C_p \frac{\partial T}{\partial t} = k \left( \frac{\partial^2 T}{\partial r^2} + \frac{1}{r} \frac{\partial T}{\partial r} \right) + Q_{\text{ind}}$$

where thermal conductivity, $k$, and heat capacity, $C_p$, are functions of temperature and composition and the heat generation term, $Q_{\text{ind}}$, is the rate of heat generation per unit volume of the roll which applies only to the induction heating mode. In the induction mode $Q_{\text{ind}}$ is nonzero only over a thin layer in which the magnetic field couples with the roll material. This occurs effectively over a very thin layer at the interface between the inner ceramic and metal layers. There is essentially no heat generation within the ceramic.

The decay of magnetic flux, $H$, with radial distance, $r$, is a function of angular frequency, $\omega$, relative magnetic permeability, $\mu$, and electrical conductivity, $\sigma$. 
\[ H = H_0 e^{-\frac{2r}{\tau_h}} \]

where \( \tau_h = \sqrt{\frac{2}{\omega \mu_0 e}} \)

For this material, \( e = 1.47 \times 10^7 \) mho/M and \( p = 600 \). At a frequency of 3000 hertz, the thickness of the induction layer, \( d_{\text{ind}} \) in cm, is defined as the distance over which \( H \) decays to \( 1/e \) of its initial value,

\[ d_{\text{ind}} = \frac{920}{\sqrt{e} p} \]

Evaluation shows that this layer was less than the thickness of one finite thickness in the metal layer. Because the heat generation zone is very thin, heat generation was lumped into the boundary condition at the ceramic-metal interface. Thus assuming that \( Q_{\text{ind}} \) is zero and expressing the heat balance in terms of thermal diffusivity,

\[ \frac{\partial T}{\partial t} = \alpha \left( \frac{\partial^2 T}{\partial r^2} + \frac{1}{r} \frac{\partial T}{\partial r} \right) \]

where thermal diffusivity is given by

\[ \alpha = \frac{k}{\rho c_p} \]

Before defining the boundary and initial conditions, the heat balance was first expressed in finite difference form.

\[ \frac{\partial Y_i}{\partial t} = \alpha_i \left[ \frac{(Y_{i+1}-2Y_i+Y_{i-1})}{\Delta r_k^2} + \frac{(Y_{i-1}-Y_{i+1})}{2\Delta r_k R_{ik}} \right] \]

\( Y \) was an interior temperature (as opposed to an interfacial temperature), index \( i \) refers to the radial position and index \( k \) refers to the layer (i.e. 1 = outer ceramic, 2 = inner ceramic and 3 = cast iron). The radial step size for each layer was fixed and was scaled to each layer thickness. \( R_{ik} \) was the radial distance to the \( i \)th element in the \( k \)th layer. The discretation of the three layers and boundary points are shown in Figure 8.
The heat balance over each layer was subject to a flux boundary condition at each interface. For the outer ceramic layer the heat flux condition was a function of position or equivalently time.

\[ Q_{nip} = (T_0 - T_p)HF \]

\[ HF = +0.0074103 \times 7.0259t - 70.616t^2 - 14358t^3 + 873470t^4 - 10552000t^5 \]

\( T_0 \) was the surface temperature at the entrance to the nip, \( S = 0 \), \( T_p \) was the fixed sheet temperature of 85°C, and \( HF \) was the correlated function of heat flux from the experimental data shown in Figure 5. \( Q_{nip} \) has units of W/cm².

For the nip zone, \( S < S_{nip} \),

\[ T_1 = Y_1 + \frac{Q_{nip}}{k_{c1} \Delta r_{c1}} \]

**Open Zones Between The Nip And The Lamp**

For the two zones open to the air in which heat losses occur through convective and radiative heat transfer, the surface temperature boundary condition was given by

For \( S_{nip} < S < S_{11} \) or \( S_{12} < S < S_{12} \),

\[ T_1 = \frac{h_{air} T_{air} + \frac{k_{c1}}{\Delta r_{c1}} Y_1 + Q_{r1}}{h_{air} + \frac{k_{r1}}{\Delta r_{c1}}} \]

where \( Q_{r} \) is the radiative heat flux in either of the two open zones given by
Temperatures are in absolute, \( e \) is the thermal emissivity assumed equal to 0.8, and \( \sigma \) is the Stefan-Boltzmann constant equal to \( 4.374 \times 10^{-19} \) W/cm\(^2\)°K\(^4\). \( h_{\text{air}} \) is the convective heat transfer coefficient given by

\[
\begin{align*}
VS < 15 & \quad h_{\text{air}} = 4.0 \times 10^{-3} \ k_{\text{air}} \sqrt{\frac{VS}{C}} \\
VS \geq 15 & \quad h_{\text{air}} = 6.2 \times 10^{-3} \ k_{\text{air}} \frac{VS^{0.8}}{C^{0.2}}
\end{align*}
\]

where \( k_{\text{air}} \) is the thermal conductivity of air equal to 0.0260 W/m°K and \( VS \) is defined in terms of the circumference and the rotational frequency, \( \text{RPS} \), as

\[
VS = \text{RPS} \ C^2
\]

The boundary condition in this zone is only approximate since the radiative heat flux is evaluated at the previous value of the surface temperature while the convective term is used to compute the surface temperature.

For the heating zone, \( S_h < S < S_{12} \), the surface temperature is defined differently for each of the two heating modes. For external infrared roll heating, the surface temperature was given by

\[
T_1 = Y_1 + \frac{Q_1}{k_{cl}} \Delta r_{cl}
\]

where \( Q_1 \) is the lamp heat flux given by an offset term and a PI controller algorithm defined as

\[
Q_h = Q_{10} + Q_{1c}
\]

\[
Q_{h0} = - \frac{(H_{\text{nip}} + H_{11} + H_{12})}{A_0 T_1}
\]

\[
Q_{hc} = - K \left[ \varepsilon + \frac{1}{T_1} \int_0^t \varepsilon \ dt \right]
\]

where \( \varepsilon \) is the temperature error given by

\[
\varepsilon = T_0 - T_{sp}
\]
For the induction heating mode, heat was lost through the surface through convection and radiation. The surface temperature was defined through similar equations. This defines a third loss zone which is discussed further in the section on Overall Heat Balance.

The second boundary condition for the interfacial temperature between the two ceramic layers was based on the equality of the heat flux at the interface,

\[ at \ r = R_1, \quad k_{c1} \frac{\partial \nu}{\partial r} \bigg|_{R_1^-} = k_{c2} \frac{\partial \nu}{\partial r} \bigg|_{R_1^+} \]

This condition was used to evaluate the interfacial temperature, \( T_2 \), as follows:

\[ T_2 = \frac{k_{c1} \frac{Y_{n1-1}}{\Delta r_{c1}} + k_{c2} \frac{Y_{n1}}{\Delta r_{c2}}}{\frac{k_{c1}}{\Delta r_{c1}} + \frac{k_{c2}}{\Delta r_{c2}}} \]

For the external infrared heating mode, the boundary condition between the ceramic and metal layers was also based on the equality of heat flux across the interface,

\[ at \ r = R_2, \quad k_{c2} \frac{\partial \nu}{\partial r} \bigg|_{R_2^-} = k_{s} \frac{\partial \nu}{\partial r} \bigg|_{R_2^+} \]

from which the interfacial temperature, \( T_3 \) was evaluated as follows

\[ T_3 = \frac{k_{c2} \frac{Y_{n2-1}}{\Delta r_{c2}} + k_{s} \frac{Y_{n2}}{\Delta r_{s}}}{\frac{k_{c2}}{\Delta r_{c2}} + \frac{k_{s}}{\Delta r_{s}}} \]

For the induction heating case the boundary condition becomes

\[ T_3 = \frac{k_{c2} \frac{Y_{n1-2}}{\Delta r_{c2}} + k_{s} \frac{Y_{n2-1}}{\Delta r_{s}}}{\frac{k_{c2}}{\Delta r_{c2}} + \frac{k_{s}}{\Delta r_{s}}} \]

For the induction heating case, an additional boundary was assigned to the end of the induction zone. An additional interfacial temperature, \( T_{3i} \) was defined by applying the equality of flux at the interface between the induction zone and metal zone. The induction heat flux was assumed to be applied at the interface.
As with the external infrared heating mode, the induction heat flux was controlled by means of a feedback loop in which the base flux level was defined through an approximate overall energy balance. The total flux level was the sum of the base level and a correction based on the error detected by the feedback loop. The algorithm for the induction heat flux was the same as that used for the external infrared heating case described above.

The boundary conditions at the metal-oil interface was defined in terms of a known interfacial temperature $T_4$.

The initial temperature profile was defined as an arbitrary function of radial position. The initial radial profile was chosen for a variety of purposes which could include the determination of the time required to reach a pseudo-steady nip inlet temperature and to reach a steady state temperature profile in the minimum computation time.

$$Y = Y_0(r)$$

Integration begins at the entrance to the nip, $S = 0$ and $t = 0$. In assigning the initial profile the surface temperature, $T_1$, was assumed equal to $Y_1$ and all interfacial temperatures are interpolated from the adjacent interior temperatures.

**Roll Geometry And Zone Residence Times**

Roll geometrical parameters are defined below. The nip distance was based on the roll speed and nip residence time. The length of the heating zone was a specified model parameter and the length of the second heat loss zone was an adjustable fraction of the heating zone length. The first heat loss zone length was based on the other specified distances. The positions of the transitions between the zone, $S_i$, was based on the specified zone lengths.

$$C = \pi D_0$$

$$S_{nip} = L_{nip} = V_{tnip}$$

$$L_h = \text{heating zone length}$$

$$L_{12} = fL_h$$

$$L_{11} = C - (L_{nip} + L_h + L_{12})$$

$$S_2 = S_1 + L_{11}$$

$$S_3 = S_2 + D_h$$

$$D_e = D_0 - 2tk$$
and

\[ A_o = \pi \frac{D_o^2}{4} \]

\[ A_r = \pi \frac{D_r^2}{4} \]

Other time constants were defined in a similar fashion.

**Overall Heat Balances**

Several overall heat balances were defined in order to quantify and compare the rate of heat addition, removal and flow from various sections of the roll. From these values it was possible to extract the thermal efficiency of the process.

The total energy transferred in the nip per unit roll width per revolution was

\[ E_{nip} = C \sum_{j=0}^{j_{max}} k_{clj} \frac{\Delta T_{lj}}{\Delta r_{cl}} \Delta t_j \]

The rate of heat transferred in the nip was given by

\[ P_{nip} = \frac{E_{nip}}{t_{rev}} \]

where \( t_{rev} \) is the time per revolution.

For the heat transfer from the roll to the oil, these terms become

\[ Q_{oil j} = -k_s \frac{(Y(n3-3)j - T4)}{\Delta d_s} \]

\[ E_{oil} = A_i \sum_{j=0}^{j_{max}} Q_{oilj} \Delta t_j \]

\[ P_{oil} = \frac{E_{oil}}{t_{rev}} \]

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The time-averaged surface temperature was defined as

\[
T_1 = \frac{\sum_{j=0}^{\text{max}} T_{1j} \Delta t_j}{t_{\text{rev}}}
\]

Other average temperatures were defined in a similar fashion.

The energy transferred between the ceramic and the metal was given by

\[
E_{\text{cer2}} = A_{i2} \sum_{j=0}^{\text{max}} Q(c2)_j \Delta t_j
\]

where \(Q(c2)_j\) was given by

\[
Q(c2)_j = k_c \frac{(T_{3j} - T_{n2-2})}{\Delta d_{c2}}
\]

Terms representing total energy losses in the zones between the nip and the heating zones were calculated by the same method used in the nip zone.

Two overall energy balances were defined on the ceramic as

\[
\Sigma_c = \sum_{i=1}^{5} E_i
\]

where 1 refers to the heat transfer zones, 1 = nip, 2 = first air zone, 3 = second air zone, 4 = third air zone, 5 = ceramic metal interface. A second overall balance on the ceramic was defined in terms of the internal temperature gradients in the ceramic defined above and the transfer between the metal and ceramic.

The overall balance on the steel was defined as

\[
\Sigma_s = \sum_{i=1}^{3} E_i
\]

where \(i_z = 1\) denotes the ceramic-metal interface, 2 is the metal-oil boundary and 3 is the heat added by induction.
For the induction heating case, the total energy transferred across the metal ceramic interface was defined as

$$E_{s2} = A_{i2} \sum_{j=0}^{j_{\text{max}}} Q(s2)j \Delta t_j$$

where the heat flux was defined as

$$Q(s2)j = -k_s \frac{(Y(n2-1)j - T3j)}{d_{\text{ind}}}$$

An overall roll energy balance was defined as the total energy transferred in one revolution through the surface and metal-oil interfaces. The overall heat balances were then used to determine the consistency of the flux calculations, overall energy closure and numerical accuracy, the net energy transferred to and from the ceramic and the overall energy losses to the air and oil.

The results of these calculations are presented in an earlier section. The connection between overall energy closure and numerical accuracy requires some consideration. Given the fact that the overall energy flows through the various interfaces depend only on the coupling between these zones, it may be said that agreement between the energy flows in each of the roll zones (i.e. ceramic and steel) as well as overall energy closure provides a good estimation of the unaccounted energy losses from the system due to either modelling error or numerical roundoff. This error is quite small thus indicating the accuracy and consistency of the numerical results.

**Numerical Algorithm**

The parabolic heat transport equation in each layer was discretized in the r-direction in each layer in a fashion analogous to the method of lines. Each zone was assigned the same number of points. The size of each interval was thus proportional to the thickness of each layer. As described in the experimental section, the thickness of the cast iron layer was approximately 100 mm. Thus the thickness of each layer is approximately 10 mm while the thickness of the ceramic layers are approximately 0.08 mm each. The highly transient surface boundary condition is expected to cause the thermal gradients to vary rapidly both in time and space within the top layers of the roll. These high frequency variations require a disproportionate fraction of the points be located in the ceramic layers. As shown theoretically(9), if the dimensionless variable $\lambda$ is given by,

$$\lambda = \alpha \frac{\Delta t}{(\Delta r)^2}$$

then the minimum step for stability is,
For the outer ceramic layer, $\alpha = k / \rho C_p = 1 \times 10^{-6} \text{ m}^2/\text{s}$, and $\Delta r_1 = 0.076 \text{ mm}$. Thus the minimum time step for stability and convergence is approximately,

$$\Delta t \leq 2.9 \times 10^{-3} \text{ seconds}.$$
where \( Y \) is the appropriate set of \( Y \) values at the half time interval. If all the trial \( \Delta t \) values are positive, the trial increment was feasible and the minimum time increment is selected. The final increments are then scaled based on the ratio of the minimum time step to the time step for each \( i \). Thus the equation with the highest derivative determines the time step. Furthermore, the time step is consistent with an implicit set of changes in \( Y \). If one or more of the time steps in the trial vector is negative, those trial steps are not feasible.

Any infeasible trial increment was reduced by a factor of two and a new trial vector is computed at the half interval. A new vector of trial time steps is determined. The process is repeated until a feasible set is determined. In those cases in which after an arbitrary number of interval halving (usually 10) no feasible solution is obtained, it is assumed that the derivative is zero (i.e. a local pseudo steady state exists for this variable), the time step for this variable is dropped from the vector of trial time steps, and this variable is not incremented. The minimum time step is then based on the reduced set of feasible trial variables.

The elapsed time is then incremented until a complete revolution is made. If the elapsed time exceeds the time per revolution, the minimum time step is set to the remaining time and the final increments are scaled based on this time step. If the time increment jumps the location from the nip zone into the air zone, there is currently no backward stepping to assure that the computations begin exactly at the transition between each zone. However, since the time steps are always several orders of magnitude smaller than the time in any zone, the resulting error is judged to be negligible.

**DISCUSSION OF NUMERICAL RESULTS**

**Calculation Of Heat Flux**

The method used to calculate heat flux from experimental surface temperature measurements assumes a semi-infinite ceramic of constant void volume. The more general numerical model described in the previous section was used to calculate heat flux from measured surface temperature data. Figures 9 and 10 show heat flux as calculated by both methods using the same surface temperature data. The similarity of the results suggest that the assumption of semi-infinite thickness was valid and that the assumed uniform void volume of 0.1 was appropriate.
Figure 9. Heat Flux Calculated Using Infinite Ceramic Slab Assumption.

Figure 10. Heat Flux Calculated Using Analytical Multi-Layered Ceramic Model.
To further demonstrate this result the numerical model was used to compute temperature profiles in the two layer ceramic coated steel platen as a function of time during impulse drying, as shown in Figure 11. Over a time interval of 40 ms the temperature at the interface between the ceramic and the steel layers at e remained constant and equal to the temperature on the heated side of the steel platen at f. This result implies that heat transfer to the sheet of paper was entirely from the ceramic coating and that the infinite thickness assumption was valid.

![Graph of temperature profiles](image)

**Figure 11.** Predicted Temperature Profiles In The Ceramic Coated Steel Platen During Impulse Drying.

**Comparison Of Heating Modes**

Using the measured nip heat flux as input to the numerical heat transfer model, infrared and induction heating modes were investigated in terms of internal roll temperature profiles and heating efficiency. A commercial lineal roll speed of 760 m/min was simulated. In each of the simulations, an ingoing roll surface temperature of 370°C was assumed.

To investigate the influence of roll internal temperature, 148°C was chosen as a conservative lower limit while 315°C was chosen as an upper limit. Figures 12 and 13 show temperature profiles for the infrared heating mode at 148°C and 315°C respectively. Profiles at various locations around the roll were chosen as defined in Figure 1. The durability of the roll coating will increase if temperature fluctuations at the ceramic/cast iron boundary are minimized. For the infrared case, where energy is supplied at the roll surface, these fluctuations were held to less than 10°C during the time that the roll moved through the heating zone from location D to Location E.
Figure 12. Roll Temperature Profiles For The Case Of Infrared Heating Assuming An Internal Roll Temperature of 148°C.

Figure 13. Roll Temperature Profiles For The Case Of Infrared Heating Assuming An Internal Roll Temperature of 315°C.
Figures 14 and 15 show temperature profiles for the induction heating mode at internal roll temperatures of 148°C and 315°C respectively. As energy was input to the roll near the ceramic-cast iron interface, temperature fluctuations of the order of 30 to 50°C occurred.

Figure 14. Roll Temperature Profiles For The Case Of Induction Heating Assuming An Internal Roll Temperature of 148°C.

Figure 15. Roll Temperature Profiles For The Case Of Induction Heating Assuming An Internal Roll Temperature of 315°C.
From a coating durability standpoint the present two layer zirconium oxide ceramic coating is well suited to infrared heating but is less suitable for induction heating.

The four simulations were also used to examine energy losses to the external ambient and to the internal heat transfer medium. Figure 16 shows the distribution of energy transferred to the roll. Using an infrared energy source a maximum of 31 kJ/m² will need to be transferred to the roll. If the system can be designed for higher oil temperatures, as little as 19 kJ/m² will be required.

![Graph showing energy distribution](image)

Figure 16. Distribution Of Energy Transferred To The Roll.

Figure 17 shows the same information in terms of roll heating efficiency, defined as the percentage of the energy used to heat the roll that is actually transferred to the sheet. Efficiencies of about 50% should be anticipated. Here again the advantages of operating at higher oil temperature are clearly apparent.
Figure 17. Roll Heating Efficiency.
CONCLUSIONS

A number of process and design issues have been addressed in this work. Roll durability is expected to improve when cyclic temperature fluctuations at metal-ceramic boundaries are minimized. The simulations show that the magnitude of these fluctuations are dependent on the location within the roll where heat is applied. In addition, the energy efficiency of the process has been shown to depend on the internal temperature of the roll as well as the mode of heating.

From a process design standpoint, the simulations were used to estimate the energy requirements of the process and to demonstrate that further research is needed to develop an optimum ceramic coating for induction heating.

REFERENCES


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NOMENCLATURE

Units: seconds, distance m (meters), mass (kg), heat (joules), power (watts) temperature K.

T interfacial temperatures
Y interior temperature
S distance traversed along the roll surface
t time
Z Cross machine coordinate
θ angular coordinate
ΔE Energy transfer (j/cm rev)
Σ Overall energy transfer
Q heat flux
Δr radial increment
d layer thickness
R radial position
V linear surface velocity
h heat transfer coefficient
k thermal conductivity
α thermal diffusivity
p magnetic permeability
ε temperature error (for control)
τ time constant
ρ density
C_p heat capacity
H magnetic flux
e electrical conductivity
ω magnetic flux frequency
f adjustable parameter
H, HT total heat transferred
RPS rotational frequency
K proportional gain
σ Stephen-Boltzmann constant
e thermal emissivity
τ_I reset time constant
n1, n2, n3 number of finite increments in layers 1, 2, 3
Project 3470

v void fraction
D diameter
L length
th thickness
A area

Subscripts

c1 outer or first ceramic layer
c2 inner or second ceramic layer
s metal
ind induction zone
h heating zone
l1 first heat loss zone
l2 second heat loss zone
0 entrance to the nip
p paper
air air
r radiation
oil heat transfer medium, oil
so solid
a absolute
in inside
o outside
i radial index
k layer index
j time step
l energy zone
1,2,3,4 interfacial position

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