



Evaluating the impact of airflow conditions and fabric thickness on nonwoven thermal resistance via local thermal non-equilibrium

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ABSTRACT

Natural-based nonwovens featuring hollow fibers are investigated for eco-friendly cold-protective clothing due to their biodegradability and low thermal conductivity. However, predicting their thermal resistance under high-velocity airflow requires resolving complex fiber-air heat interactions. The present study evaluates the standard local thermal equilibrium assumption against a local thermal non-equilibrium framework. A two-dimensional numerical porous-media model was developed, coupling external turbulent flow with internal laminar flow. The energy formulation was modified via two user-defined functions that integrate radiative thermal conductivity and interfacial convection coefficients for multicomponent hollow fibers. The developed non-equilibrium model demonstrated excellent agreement with experimental measurements. Subsequent comparisons reveal that the validity of the standard thermal equilibrium assumption is regime-dependent and dictated by fluid residence time. Under horizontal airflow (1 m/s), extended residence times permit thermal equilibrium, rendering the equilibrium assumption a suitable approximation. Conversely, vertical flow shortens fluid residence time, inducing a thermal lag between solid fibers and interstitial air. Without capturing this phenomenon, the equilibrium model overestimates convective heat loss, diverging by 23.8% from non-equilibrium predictions for the thickest sample investigated at a 4 m/s vertical flow, and underestimates the insulation benefit of thicker fabrics. By resolving the finite rate of heat exchange, the non-equilibrium framework partitions the individual heat-transfer mechanisms. Utilizing this methodology prevents the under-prediction of thermal resistance in high-wind environments, providing a predictive tool for engineering cold-protective clothing.

1. Introduction

Exposure to cold temperatures ($<10\text{ }^{\circ}\text{C}$) accelerates body heat loss and increases the risk of hypothermia [1,2]. Preventing these conditions requires well-designed cold-protective clothing, making the engineering of textile thermal insulation a scientific priority [3]. In such clothing, nonwoven fabrics are widely utilized due to their high porosity, low density, and cost-effectiveness [4,5]. While traditional nonwovens rely on fossil-based materials with associated environmental impacts [6–8], plant-based natural fibers offer a sustainable alternative, providing renewability and biodegradability while maintaining thermal protection [9,10]. Their insulating capability is dictated by structural parameters, including fiber morphology, diameter, porosity, and thickness [11–14].

Optimizing these materials requires an understanding of their thermal behavior. While standardized experimental tests quantify

macroscopic thermal resistance [15,16], numerical modeling is required to decouple structural parameters and isolate the interacting heat-transfer mechanisms within the porous structure. Numerical approaches reveal that insulation in these natural nonwovens relies on trapping still air within their porous matrices, an effect amplified by the internal cavities of hollow fibers. In the absence of external wind, conduction governs the thermal regime [17], although high porosity facilitates some radiative heat transfer [11]. However, exposure to ambient wind alters this balance; penetrating airflow induces forced convection, transforming the trapped air into a primary way for convective heat loss [18].

Building upon these micro-scale dynamics, several studies have investigated convective heat transfer within fibrous structures, demonstrating that structural and material modifications strongly influence internal airflow [19–24]. At the micro-scale, fabrics with aerogel integration achieved a 67% increase in thermal resistance by hindering

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Nomenclature

a_{sf}	Specific interfacial area density (1/m)
c_p	Specific heat of air (J/kg·K)
d_{eq}	Equivalent spherical diameter of the cylindrical fiber (m)
d_i, d_o	Inner and outer fiber diameter (m)
d_h	Hydraulic diameter of the interstitial void space (m)
h^*	Effective heat transfer coefficient (W/m ² ·K)
h_{int}	Convection coefficient at the interface (W/m ² ·K)
h_{sf}	Volumetric heat transfer coefficient (W/m ³ ·K)
K_p	Sample permeability without the film (m ²)
$K_{p, film}$	Sample permeability with the film (m ²)
k_{eff}	Effective thermal conductivity (W/m·K)
k_f	Thermal conductivity of air (W/m·K)
k_{rad}	Radiative thermal conductivity (W/m·K)
k_s	Thermal conductivity of the fiber (W/m·K)
$k_{s,a}$	Axial thermal conductivity of the hollow fiber (W/m·K)
$k_{s,t}$	Transverse thermal conductivity of the hollow fiber (W/m·K)
L	Sample thickness (m)
Nu_{dh}	Nusselt number based on hydraulic diameter
Pr	Prandtl number
p	Pressure (Pa)
q	Heat flux at the bottom of the hot-plate (W/m ²)
Re_{dh}	Reynolds number based on hydraulic diameter
TR	Thermal resistance (m ² ·K/W)
T_f	Fluid phase temperature in the textile zone (K)
T_s	Solid phase temperature in the textile zone (K)
$T_{reversed}$	Reversed flow air temperature (K)
$T_{f, zone 2}$	Side fluid temperature in zone 2 (K)
$T_{s, zone 2}$	Side solid temperature in zone 2 (K)

t	Wall thickness of the fiber (m)
u_p	Intrinsic pore velocity (m/s)
U_{in}, V_{in}	Velocity inlet for horizontal and vertical configuration (m/s)
u, v	Air velocity in x, y direction (m/s)
v_i	Volume fraction of each fiber component i
x_i	Weighing factor of the fiber component i
x, y	Space coordinates

Greek symbols

ρ_f	Air density (kg/m ³)
ρ_{bulk}	Bulk density of the nonwoven sample (kg/m ³)
ρ_{fi}, ρ_w	Fiber and wall density (kg/m ³)
ε	Total porosity of the nonwoven sample
$\varepsilon_i, \varepsilon_o$	Inner and outer porosity of the nonwoven sample
μ	Air viscosity (N·s/m ²)
ϵ	Emissivity of the boundary surfaces
σ	Stefan Boltzmann constant (W/m ² ·K ⁴)
β	Geometric shape factor
γ	Interfacial heat-flux partitioning fraction for the fluid phase

Abbreviations

MW	Milkweed
PLA	polylactic acid
LTNE	Local Thermal Non-Equilibrium
LTE	Local Thermal Equilibrium
UDF	User-Defined Function
2D	Two-dimensional domain
3D	Three-dimensional domain

internal convective flow [20]. Similarly, numerical models indicate that reducing fiber diameter from 14 to 8 mm in packed beds of constant porosity increases the pressure gradient by about 77%, which actively restricts internal air penetration [21]. Additionally, applying windproof outer layers limits internal air movement and convective heat loss, allowing porous textiles to maintain nearly constant insulation under windy conditions [22,23]. Furthermore, analyses confirm that while conduction governs low-porosity fabrics, convective thermal resistance drops as wind velocity and air permeability increase [22]. While these studies focus on restricting air movement to mitigate convective heat loss, clothing design cannot eliminate permeability entirely; a required degree of water vapor transmission must be maintained to ensure moisture management and wearer comfort.

Despite these advancements, previous models did not quantitatively partition conduction and convection, and omitted radiative heat transfer. Although Shen et al. [18] used a three-dimensional (3D) study to isolate these effects, their approach relied on bulk fabric properties rather than intrinsic fiber conductivities. Additionally, earlier research often simplified multi-component fiber blends, excluded the thermal effects of fiber hollowness, and lacked comparative data on horizontal versus vertical wind exposure. Consequently, a need remained for a methodology to separate conduction, convection, and radiation within complex porous structures, which was systematically addressed in our foundational study.

To address these limitations, a previous study developed a two-dimensional (2D) porous-media model using the local thermal equilibrium (LTE) assumption to separate conduction, convection, and radiation. The findings indicated that forced convection dominates high-wind heat loss, representing 86.2% of the total transfer at 4 m/s for the thickest sample. Furthermore, restricting air permeability improved

thermal resistance by a factor of 2.83, whereas doubling fabric thickness yielded a 9.9% increase. This demonstrates that controlling permeability is a more effective strategy for enhancing cold-protective insulation than increasing structural bulk [25].

While the foundational model quantified macroscopic heat transfer, its reliance on LTE assumes that fibers and interstitial air share identical temperatures. This approach is justified under low-velocity conditions, where solid fibers act as a thermal reservoir. However, under high-velocity airflow, particularly in vertical configurations, this thermal equilibrium may not be maintained. Wind penetration can introduce a thermal gradient between the fiber and the air, altering internal convective dynamics. To evaluate how these temperature variations affect overall insulation performance, analyzing the system through a local thermal non-equilibrium (LTNE) framework is necessary.

While the LTNE approach captures dynamic thermal lag under high-velocity airflow, its application to natural-fiber nonwovens remains unexplored. Previous studies utilizing this approach have focused on other porous media, such as packed particle beds [26]. Within these domains, models like Whitaker's correlation have been established for evaluating internal convective heat transfer [26–28].

Building upon the need to capture the internal heat exchange, this numerical study investigates the thermal resistance of a multi-component nonwoven structure composed of kapok, milkweed (MW), and polylactic acid (PLA). These fibers were selected for their physical properties, local availability, and industrial relevance [24,29]. To resolve thermal interactions under high-velocity airflow, this work implements an LTNE approach by modifying the porous media energy equations. The numerical model integrates the composite thermal properties of the fiber blend, accounts for the microstructural impact of fiber hollowness, and utilizes two user-defined functions (UDFs) to

govern radiative and internal convective heat transfer. After validating the LTNE simulation results against experimental measurements, the framework is used to quantify the influence of fabric thickness, partition the individual modes of heat transfer, and evaluate overall insulation performance across varying wind velocities in horizontal and vertical configurations. Ultimately, the findings provide a physics-driven foundation for the design and optimization of cold-protective clothing.

2. Structural and thermophysical properties

This research evaluates three nonwoven samples, labeled S1, S2, and S3. To optimize both structural integrity and thermal insulation, all samples share a uniform fiber composition of 50% solid PLA, 25% hollow kapok, and 25% hollow MW. While thickness and areal density progressively rise from S1 to S3, every fabric retains an exceptionally high total porosity (> 99%). Table 1 outlines the macroscopic characteristics of these bulk materials. The reported parameters encompass thickness (L), bulk density (ρ_{bulk}), and the specific porosities (inner ϵ_i , outer ϵ_o , and total ϵ). Additionally, the table presents air permeability evaluated both with ($K_{p, film}$) and without (K_p) a laminated cotton cover.

Establishing the thermal model also requires the geometric and thermophysical characteristics of the individual constituent fibers. These microscopic parameters include the inner (d_i) and outer (d_o) fiber diameters, wall thickness (t), fiber (ρ_f) and wall (ρ_w) densities, and overall thermal conductivity (k_s). Because the hollow kapok and MW fibers exhibit anisotropic behavior, their axial ($k_{s,a}$) and transverse ($k_{s,t}$) thermal conductivities are specifically accounted for. The exact values for these microscopic properties, along with their measurement methodologies, are detailed in previous studies [17].

With the material properties established, the subsequent section details the computational domain, aligned with the experimental bench test, to evaluate thermal resistance.

3. Measurement apparatus and computational domain

The thermal resistance of the nonwoven fabrics was evaluated using a guarded hot plate apparatus, which provided the physical foundation for a corresponding 2D computational framework. In accordance with the ISO 11092 standard [30], measurements were performed in an environmental chamber controlled at 20 °C and 65% relative humidity, with the hot plate maintained at a constant 35 °C. Fabric samples were trimmed to 30.48 by 30.48 cm and their lateral edges were sealed to minimize lateral heat loss. To systematically evaluate the influence of environmental wind, these assemblies were subjected to external airflows at velocities of 1, 2, and 4 m/s. This airflow was introduced in two distinct orientations: a horizontal configuration with the inlet positioned 12 cm from the plate, and a vertical configuration with the inlet located 23.6 cm above it [31].

To replicate the physical setups numerically, the textile was modeled as a 2D porous medium. Instead of explicitly resolving the complex internal fiber arrangement, as would be required in a 3D model with randomly distributed fibers [21], this approach relies on bulk structural parameters such as air permeability and effective thermal conductivity. Consequently, it significantly reduces computational cost without sacrificing numerical accuracy. In defining the geometry, the width of the modeled textile zone was set to match the actual fabric thickness.

Furthermore, to prevent interference from boundary conditions, the overall computational domain was extended to twice the textile dimension: in the streamwise direction for the horizontal setup, and in both directions for the vertical setup. Within this geometry, the domain was partitioned into two distinct regions: Zone 1, representing the external fluid flow, and Zone 2, corresponding to the textile section modeled as a fluid zone, representing the porous medium. To account for the three distinct fabric structures (S1–S3) across both wind orientations, six unique computational domains were developed. These aligned experimental configurations and their corresponding computational domains are illustrated in Figs. 1 and 2.

For both experimental and computational analyses, the thermal resistance (TR) was determined under steady-state conditions based on the heat flux (q) and the temperature difference (ΔT) between the hot plate and the ambient air.

To establish the reliability of these physical measurements, a comprehensive uncertainty analysis was performed. The total experimental inaccuracy was quantified by incorporating both the propagated measurement uncertainties, derived directly from instrument specifications, and the repeatability errors, represented by the standard deviation across multiple test iterations. For an exhaustive description of the experimental procedures and the computational domain, readers are referred to earlier studies [16,25,31].

Having defined the spatial extent of the computational domains, the specific boundary conditions governing the heat and momentum transfer at each physical face are outlined in the following section.

4. Boundary conditions

The boundary conditions governing the computational domains, illustrated in Fig. 1(b) and Fig. 2(b), are comprehensively detailed in Table 2. This summary defines the key flow and thermal parameters applied to the models: the horizontal and vertical inlet velocities (U_{in} and $V_{in} > 0$, respectively) are prescribed with a negative sign in the mathematical formulation of Table 2 to accurately align the incoming airflow with the adopted coordinate system. Additionally, the model defines the backflow temperature to account for potential reversed flow ($T_{reversed}$), along with the side boundary temperatures specific to the external fluid region (T_{zone1}) and the porous textile region. Unlike our previous study which utilized a single effective temperature for the textile domain [25], the present non-equilibrium framework necessitates distinct boundary conditions for the fluid ($T_{f, zone2}$) and solid ($T_{s, zone2}$) phases. Establishing these dual temperatures directly governs the updated mathematical formulations for the left-textile, right-textile, and bottom-textile boundary conditions. The physical rationale for this phase separation will be detailed in the subsequent governing equations and numerical implementation sections.

Regarding the physical domain constraints, the outer enclosure edges (denoted as the top and bottom boundaries in both the horizontal and vertical configurations) were assigned a slip-wall condition fixed at the ambient temperature (20 °C). This was purposefully adopted to minimize computational cost while accurately representing the undisturbed open-air laboratory environment. For the textile zone itself (Zone 2), the bottom surface was modeled as a no-slip wall maintained at 35 °C, and the lateral edges (left-textile and right-textile) were treated as impermeable. This constraint is not an arbitrary computational choice,

Table 1
Macroscopic properties and structural characteristics of the nonwoven fabrics [17,25].

Sample ID	Areal Density (gsm)	Area (mm ²)	L (mm)	ρ_{bulk} (kg/m ³)	ϵ_i (%)	ϵ_o (%)	ϵ (%)	$K_{p, film}$ ($\times 10^{-7}$ m ²)	K_p ($\times 10^{-7}$ m ²)
S1	108.53	106,534.70	10.20	10.640	2.080	97.385	99.466	0.089	1.818
S2	147.61	98,158.33	15.70	9.402	1.838	97.690	99.528	0.194	2.155
S3	250.13	99,562.67	22.30	11.216	2.193	97.244	99.437	0.192	2.090

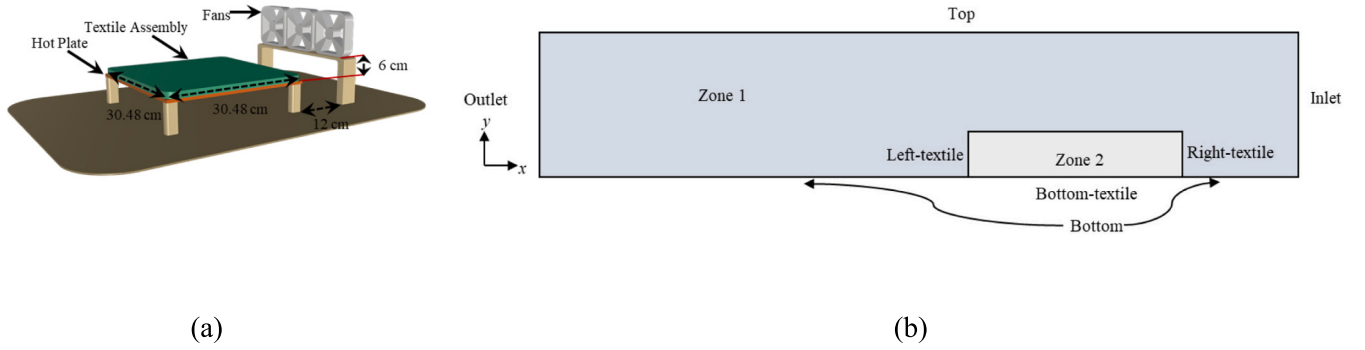


Fig. 1. Horizontal configuration for thermal resistance evaluation: (a) experimental measurement apparatus adapted from [31] and (b) corresponding 2D computational domain [25].

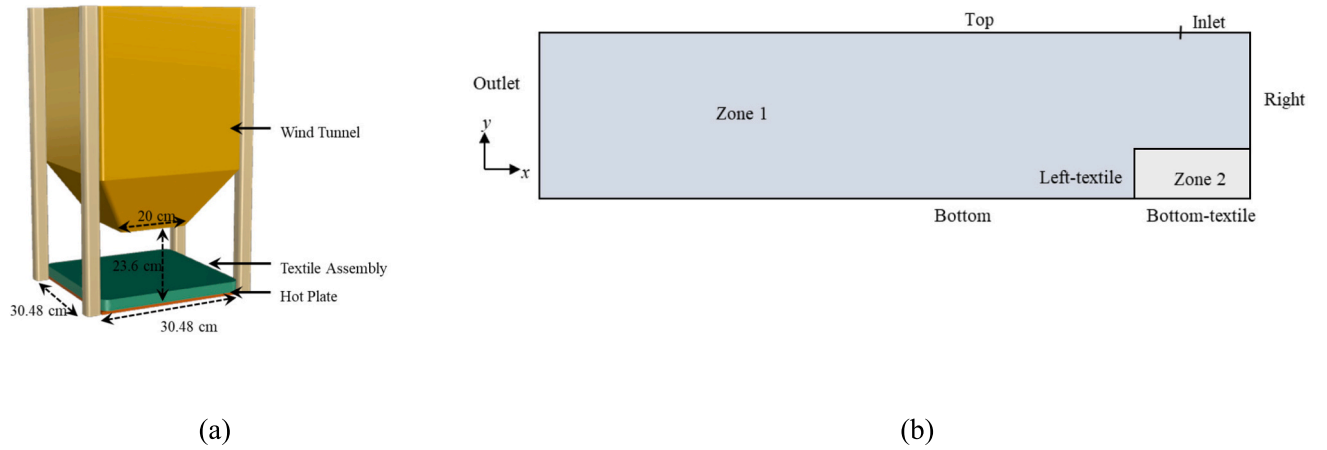


Fig. 2. Vertical configuration for thermal resistance evaluation: (a) experimental measurement apparatus adapted from [31] and (b) corresponding 2D computational domain [25].

Table 2

Applied boundary conditions for the computational models under horizontal and vertical wind orientations.

Configuration	Boundary	Condition	Mathematical formulation
Horizontal	Inlet	Velocity inlet	$u = -U_{in}; v = 0 \text{ m/s}; T = 20^\circ \text{C}$
	Outlet	Pressure outlet	$p = 0 \text{ Pa}; T_{reversed} = 20^\circ \text{C}$
	Top, Bottom	Slip wall	$\frac{\partial u}{\partial y} = 0; v = 0 \text{ m/s}; T = 20^\circ \text{C}$
	Bottom-textile	No-slip wall	$u = v = 0 \text{ m/s}; T_{f,zone2} = T_{s,zone2} = 35^\circ \text{C}$
	Left-textile, Right-textile	Coupled	$u = v = 0 \text{ m/s}; T_{zone1} = T_{f,zone2}; k_f \frac{\partial T_{zone1}}{\partial x} = \epsilon_o k_f \left(\frac{\partial T_{f,zone2}}{\partial x} \right)$
Vertical	Inlet	Velocity inlet	$u = 0 \frac{\text{m}}{\text{s}}; v = -V_{in}; T = 20^\circ \text{C}$
	Outlet	Pressure outlet	$p = 0 \text{ Pa}; T_{reversed} = 20^\circ \text{C}$
	Top, Bottom	Slip wall	$\frac{\partial u}{\partial y} = 0; v = 0 \text{ m/s}; T = 20^\circ \text{C}$
	Bottom-textile	No-slip wall	$u = v = 0 \text{ m/s}; T_{f,zone2} = T_{s,zone2} = 35^\circ \text{C}$
	Left-textile	Coupled	$u = v = 0 \text{ m/s}; T_{zone1} = T_{f,zone2}; k_f \frac{\partial T_{zone1}}{\partial x} = \epsilon_o k_f \left(\frac{\partial T_{f,zone2}}{\partial x} \right)$
	Right	Symmetry	$\frac{\partial u}{\partial x} = 0; \frac{\partial v}{\partial x} = 0; \frac{\partial p}{\partial x} = 0; \frac{\partial T}{\partial x} = 0$

but a strict physical requirement based on the ISO 11092 standard for developing the in-house bench test [30], which dictates that lateral air leakage must be prevented so that all entering air comes strictly from the top surface of the textile. Accordingly, a coupled thermal boundary condition was utilized at these sealed edges to ensure the continuity of temperature and heat flux with the adjacent domain. Finally, the inherent geometric symmetry of the vertical configuration allowed for the application of a symmetry boundary condition, meaning only half of the domain needed to be simulated. It is worth nothing that given the LTNE model in this study, the thermal boundary conditions at the interface, specifically where the clear fluid in Zone 1 meets the top surface of the textile in Zone 2, require explicit definition; however, the detailed mathematical treatment and physical justification of this interfacial boundary condition are deferred to Section 6 after the LTNE governing equations.

With the boundary conditions and spatial constraints fully defined, the heat transfer within the domains were resolved using the modeling assumptions and governing equations presented next.

5. Assumptions and mathematical framework

The numerical model was developed under three primary assumptions:

- 1- Steady-state conditions: Transient effects and fluctuating heat fluxes were excluded.
- 2- Incompressible airflow: Justified by a maximum wind velocity yielding a Mach number below 0.3.

3- Constant thermophysical properties: Fiber and air properties were treated as temperature-independent due to the small thermal gradients [18].

Due to the high external wind velocity, the airflow within Zone 1 was modeled as turbulent. The flow and heat transfer in this region were solved using the Reynolds-averaged Navier–Stokes formulation. To accurately capture the boundary layer development on the textile surface, which primarily controls convective heat transfer, the $k - \omega$ shear-stress transport model was employed [32]. This model is highly effective for wall-bounded flows because of its hybrid formulation: it smoothly blends the robust $k - \omega$ model near the wall with the $k - \epsilon$ model in the free stream. Additionally, turbulence intensities at the domain inlet were defined using laboratory measurements, following ASHRAE 55 guidelines [33]. These values, calculated as the ratio of the standard deviation of velocity fluctuations to the mean velocity, were derived directly from experimental airspeed data and are listed in Table 3. The complete set of governing equations (continuity, momentum, and energy) and further details on the turbulent flow modeling in this external zone are comprehensively detailed in the earlier study [25].

For the internal textile region (Zone 2), the fibrous structure was modeled as a porous medium to simulate external air penetration. To account for the pressure drop caused by the fabric network resisting the airflow, a momentum sink term was incorporated into the laminar fluid flow equations. Because the internal air velocities were sufficiently low, inertial losses were neglected, allowing the flow resistance to be accurately represented using Darcy's law [18]. Additionally, the textile samples were covered with a thin film layer that introduced direction-dependent flow resistance. To accurately capture this behavior, the standard air permeability of the sample without the film (K_p) was applied in the streamwise direction. Conversely, the modified permeability incorporating the film ($K_{p, film}$) was applied in the normal direction, as the film's resistance acts only perpendicular to the fabric plane and not along its length. By integrating these direction-dependent permeabilities, the final governing momentum equations in the x and y directions for the porous region are formulated as Eqs. (1) and (2). A comprehensive derivation and simplification of these equations, including the precise integration of the momentum sink terms utilizing direction-dependent permeabilities, are thoroughly detailed in the earlier studies [25,34,35]:

Momentum equation (x direction):

$$\frac{\rho_f}{\epsilon_o^2} \left(u \frac{\partial u}{\partial x} + v \frac{\partial u}{\partial y} \right) = -\frac{\partial p}{\partial x} + \frac{\mu}{\epsilon_o} \left(\frac{\partial^2 u}{\partial x^2} + \frac{\partial^2 u}{\partial y^2} \right) - \frac{\mu}{K_p} u \quad (1)$$

Momentum equation (y direction):

$$\frac{\rho_f}{\epsilon_o^2} \left(u \frac{\partial v}{\partial x} + v \frac{\partial v}{\partial y} \right) = -\frac{\partial p}{\partial y} + \frac{\mu}{\epsilon_o} \left(\frac{\partial^2 v}{\partial x^2} + \frac{\partial^2 v}{\partial y^2} \right) - \frac{\mu}{K_{p, film}} v \quad (2)$$

In prior study, energy transport within the textile region was resolved using the LTE model. The LTE framework assumes that within each computational cell, the solid fibers and the interstitial air maintain an identical temperature ($T_s = T_f = T$), thereby neglecting internal convective heat exchange between the phases. As a result of this shared temperature distribution, the thermal behavior of both phases is governed by just a single energy equation [25]. This simplification was

previously justified by the substantial difference in volumetric heat capacities; acting as a dominant thermal reservoir, the solid fibers theoretically force the internal air to rapidly adjust to the solid temperature. However, the accuracy of this assumption requires critical evaluation, especially under high wind velocities in the horizontal orientation and across all vertical configurations, where convective heat transfer effects are expected to be significantly larger. To address this limitation, the present study introduces the main methodological development: the transition to the LTNE model. Departing from previous simplifications, the LTNE relaxes the thermal equilibrium constraint to explicitly resolve the distinct temperature fields of both the solid and the fluid phase. This advancement enables a rigorous and precise quantification of the internal convective heat transfer occurring directly between the fibrous structure and the penetrating airflow. To explicitly capture this internal heat transfer under steady-state conditions, the thermal fields of the fluid and solid phases are determined by simultaneously solving two coupled energy equations. These governing non-equilibrium expressions are formulated as Eqs. (3) and (4), respectively [26,36]:

Energy equation (solid zone):

$$(1 - \epsilon) k_s \left(\frac{\partial^2 T_s}{\partial x^2} + \frac{\partial^2 T_s}{\partial y^2} \right) = h_{sf} (T_s - T_f) \quad (3)$$

Energy equation (fluid zone):

$$(\rho c_p)_f \left(u \frac{\partial T_f}{\partial x} + v \frac{\partial T_f}{\partial y} \right) = \epsilon k_f \left(\frac{\partial^2 T_f}{\partial x^2} + \frac{\partial^2 T_f}{\partial y^2} \right) + h_{sf} (T_s - T_f) \quad (4)$$

where h_{sf} represents the volumetric heat transfer coefficient at the solid-fluid interface, which is described in detail in the following section.

Within the LTNE framework, the effective thermal conductivities of the fluid and solid phases using the basic linear volumetric model is based on porosity ($(1 - \epsilon)$ and ϵ). While numerous non-linear macroscopic k_{eff} expressions exist, implementing them within a dual-equation LTNE architecture requires a mathematically non-trivial linearization process to decouple the phase conductivities, which skewing the localized heat transfer partitioning. Therefore, this straightforward volumetric decoupling ensures mathematical stability and remains the most widely validated approach for highly porous nonwoven fibrous insulations [34,35,37–40].

However, this standard formulation (Eqs. (3) and (4)) possesses two significant limitations for this specific study: it fails to account for the structural hollowness of the kapok and MW fibers, and it entirely neglects radiative heat transfer within the porous network. As established in earlier studies [17,25], the influence of radiation between fibers can be effectively modeled by introducing a radiative thermal conductivity term (k_r). Therefore, to resolve these physical omissions, two critical adjustments must be made. First, to properly account for fiber hollowness, the total porosity (ϵ) is replaced by the outer porosity (ϵ_o) in both expressions. Second, k_r is incorporated directly into the solid-phase energy equation to explicitly capture radiative exchange. By integrating these necessary modifications, the final, comprehensive LTNE equations are derived as Eqs. (5) and (6):

Modified energy equation (solid zone):

$$(1 - \epsilon_o) \left(k_s + \frac{k_r}{1 - \epsilon_o} \right) \left(\frac{\partial^2 T_s}{\partial x^2} + \frac{\partial^2 T_s}{\partial y^2} \right) = h_{sf} (T_s - T_f) \quad (5)$$

Modified energy equation (fluid zone):

$$(\rho c_p)_f \left(u \frac{\partial T_f}{\partial x} + v \frac{\partial T_f}{\partial y} \right) = \epsilon_o k_f \left(\frac{\partial^2 T_f}{\partial x^2} + \frac{\partial^2 T_f}{\partial y^2} \right) + h_{sf} (T_s - T_f) \quad (6)$$

To explicitly evaluate this radiative thermal conductivity (k_r), the present model adopts the simplified expression proposed by Farnworth [37], which assumes that radiation exchange occurs primarily between nearly opaque fibers. In previous study, k_r was approximated as a single, static value calculated from the mean temperature across the entire

Table 3

Experimental mean velocities and corresponding turbulence intensities applied at the domain inlet [25].

Configuration	Velocity (m/s)	1	2	4
Horizontal	Mean velocity (m/s)	1.11	2.08	3.91
	Turbulence intensity (%)	6.16	6.65	3.95
Vertical	Velocity (m/s)	1	2	4
	Mean velocity (m/s)	1.11	2.02	4.18
	Turbulence intensity (%)	5.12	7.71	4.70

textile zone. However, to enhance spatial accuracy in this new study, a UDF was developed and implemented within the ANSYS Fluent solver. Rather than assigning a constant thermal conductivity to the fibers, this UDF dynamically calculates an effective thermal conductivity that combines both solid conduction and local radiative heat transfer into a single term. By evaluating the specific temperature of each computational cell, this integrated property is defined by the following function:

Solid thermal conductivity UDF combining intrinsic conduction and local radiation:

$$k_{eff} = k_s + \frac{4\sigma T_s^3 d_o}{(1 - \epsilon_o)\epsilon} \quad (7)$$

where σ is the Stefan-Boltzmann constant, T_s represents the local temperature of each computational cell within the solid zone, and ϵ denotes the emissivity of the boundary surfaces. Furthermore, k_s represents the intrinsic thermal conductivity of the multi-component fabric composite. To accurately reflect the investigated fabric structures (S1–S3), the calculation of k_s relies on the specific volume fraction (v_i) of each constituent fiber, values that were established in earlier studies. By applying these v_i , the formulation for k_s explicitly accounts for fiber hollowness. It achieves this by utilizing the distinct axial and transverse thermal conductivities of the hollow fibers, an approach comprehensively detailed in earlier study [17]. Finally, the parameter d_o represents the mean outer diameter of the fibrous mixture. Because d_o is similarly governed by the multi-component nature of the fabric, it is calculated using those exact v_i via the Eq. (8):

$$d_o = \sum_{i=1}^{n=3} x_i \cdot d_{o_i} \quad (8)$$

where d_{o_i} is the outer diameter of each constituent fiber, and x_i is the corresponding weighting factor calculated from Eq. (9) based on the fiber's volume fraction v_i .

$$x_i = \frac{v_i}{\sum_{i=1}^{n=3} v_i} \quad (9)$$

Within the LTNE energy equations developed for the current case study (Eqs. (5) and (6)), the thermal coupling between the fluid and solid phases is governed by the volumetric heat transfer coefficient, h_{sf} . Mathematically, this parameter is defined as the product of the specific interfacial area density a_{sf} and the effective heat transfer coefficient (h^*) [26,41,42]:

$$h_{sf} = a_{sf} \cdot h^* \quad (10)$$

a_{sf} quantifies the total solid-fluid contact surface per unit volume of the porous domain. While the original formulation assumed spherical particles in packed beds, the present study adapts this expression for cylindrical geometry. Furthermore, to explicitly account for the effect of fiber hollowness, the total porosity is replaced by ϵ_o . By integrating these geometric realities, a_{sf} is mathematically tied directly to the physical mean outer fiber diameter (d_o), described in Eq. (8), as calculated using Eq. (13) [26,41,42].

$$a_{sf} = \frac{4(1 - \epsilon_o)}{d_o} \quad (11)$$

Deriving the h^* requires treating the thermal resistances of the fluid and solid phases as a series network [26,41,42]. To achieve this accurately, a precise distinction must be made between the characteristic lengths governing the fluid flow and the internal solid conduction.

Within the porous media, the thermal boundary layer is dictated by the interstitial void geometry. Since standard porous media correlations assume spherical particles, the cylindrical fibers are mathematically represented by an equivalent spherical diameter ($d_{eq} = 1.5 \times d_o$) [27]. The fluid flow dynamics and the resulting Nusselt number are

subsequently determined using the hydraulic diameter (d_h), defined as Eq. (12):

$$d_h = \frac{2\epsilon_o}{3(1 - \epsilon_o)} d_o \quad (12)$$

Applying the modified Whitaker correlation for thermal exchange in porous media, the corresponding Nusselt number based on the hydraulic diameter (Nu_{dh}) is evaluated as using Eq. (13) [27,28]:

$$Nu_{dh} = \left(0.4082 Re_{dh}^{\frac{1}{2}} + 0.1747 Re_{dh}^{\frac{2}{3}} \right) \cdot Pr^{1/3} \quad (13)$$

where Pr is the Prandtl number of air, and Reynolds number (Re_{dh}) as well as u_p representing the intrinsic pore velocity, are expressed by Eqs. (14) and (15) [27]:

$$Re_{dh} = \frac{\rho u_p d_h}{\mu} \quad (14)$$

$$u_p = \frac{\sqrt{u^2 + v^2}}{\epsilon_o} \quad (15)$$

Where u and v correspond to the local velocity components in the x and y directions in each computational cell, respectively.

Finally, h^* is evaluated by coupling the fluid thermal resistance (d_h) with the internal solid conductive resistance (d_o). Applying a geometric shape factor of $\beta = 8$ for cylindrical structures, h^* is defined as Eq. (16) [26,41–43]:

$$\frac{1}{h^*} = \frac{d_h}{Nu_{dh} \cdot k_f} + \frac{d_o}{\beta k_s} \quad (16)$$

To successfully integrate this evaluated heat transfer coefficient (h^*) into the ANSYS Fluent solver, an additional UDF was developed and compiled. This custom function explicitly captures the localized thermal exchange occurring between the solid fibers and the permeating interstitial air.

6. Thermal boundary conditions at the clear-fluid and porous-medium interface

At the interface between the external clear-fluid region (Zone 1) and the porous region (Zone 2), the continuity of temperature and heat flux must be explicitly defined. Because the clear fluid flows directly into the pore spaces, the fluid temperature remains continuous across the boundary ($T_{zone 1} = T_{f,zone 2}$). To ensure the conservation of total energy, the incoming macroscopic heat flux from the clear fluid is partitioned between the fluid and solid phases of the porous medium using a partitioning fraction γ [44,45]. Some established models evaluate γ based on either porosity ($\gamma = \epsilon_o$) or effective thermal conductivity weighting [45]. Applying the physical properties of the nonwoven textiles investigated in this study, specifically an ultra-high porosity ($\epsilon_o > 0.97$ considering the studied samples) and a relatively low solid thermal conductivity ($k_s = 0.119$ W/m·K), yields γ values of 0.90 and 0.97. This demonstrates that the vast majority of the interfacial heat flux naturally partitions directly into the fluid phase. Consequently, this study adopts the physically appropriate limiting case of $\gamma = 1$, representing a scenario where the total incoming heat flux enters the porous domain entirely through the fluid phase. This simplifies the governing interface equations, mathematically rendering the solid skeleton adiabatic exactly at the macroscopic boundary:

$$\frac{\partial T_{s,zone 2}}{\partial y} = 0 \quad (17)$$

Concurrently, the fluid phase exclusively governs the total interfacial energy continuity:

$$-\varepsilon_o k_f \frac{\partial T_{f,zone 2}}{\partial y} = -k_f \frac{\partial T_{zone 1}}{\partial y} \quad (18)$$

This mathematical formulation is rooted in the physical realities of the ultra-high porosity nonwoven matrix. With less than 3% solid volume fraction at the interface, the direct macroscopic conductive heat flux entering the exposed, truncated fiber tips is negligible. Imposing a zero-gradient condition on the solid at the interface does not thermally isolate the fibers; rather, thermal energy enters predominantly with the flowing fluid and is subsequently transferred to the solid matrix internally through the volumetric interphase heat-transfer term, $h_{sf}(T_s - T_f)$. This zero-normal-gradient condition for the solid phase at a through-flow inlet aligns with established LTNE treatments employed in experimentally validated porous bed models (e.g., [46]). It is acknowledged that alternative theoretical models exist for the clear-fluid/porous-medium interface. For instance, an alternative approach introduces an interface-specific heat-transfer coefficient (h_{int}) to explicitly couple the clear fluid to the solid phase at the boundary [45]:

$$-(1 - \varepsilon_o) k_s \frac{\partial T_{s,zone 2}}{\partial y} = h_{int} (T_{f,zone 2} - T_{s,zone 2}) \quad (19)$$

As established in rigorous interfacial study [47], the adiabatic condition applied in the present study represents the exact mathematical limit of this model as $h_{int} \rightarrow 0$. Specifically, because the physical interfacial contact area diminishes in porosity media applied to interface also, the interface heat transfer coefficient vanishes, mathematically forcing the formulation to collapse directly into the zero-gradient solid condition, expressed by Eq. (17). However, the exact empirical value of h_{int} for this specific nonwoven textile remains unknown, and implementing an alternative finite jump condition ($h_{int} > 0$) poses a computational limitation. Specifically, the native LTNE architecture in Ansys Fluent strictly enforces the adiabatic solid boundary at fluid-porous interfaces, and overriding this boundary condition is not currently possible even via UDFs. The authors acknowledge this software constraint as a limitation of the current study. Nevertheless, any potential divergence between these theoretical interface models is expected to be minimal due to the ultra-high porosity and the low solid volume fraction coupled with fairly low thermal conductivity of the fibers available for direct interfacial conduction.

With the governing physical models and boundary constraints fully established, the subsequent section details the numerical discretization schemes and the overall solution methodology.

7. Numerical implementation

As established in the description of the computational domain, six distinct configurations were required to represent the three fabric structures (S1–S3) subjected to both horizontal and vertical wind orientations. Spatial discretization for all cases was achieved using a structured quadrilateral mesh generated via the ANSYS meshing tool. For each of the six configurations, coarse, medium, and fine grids were systematically developed to facilitate rigorous numerical validation. The specific element counts and the corresponding grid independence study are discussed subsequently in the Results and Discussions section.

The numerical implementation of the LTNE model, done by ANSYS Fluent 20.2, fundamentally relies on the native dual-cell architecture of

the solver. Prior to LTNE activation, the external fluid zone and the porous textile zone intersect at a standard coupled wall, with the bottom boundary fixed at 35 °C and the top surface operating as an open internal boundary to allow free airflow. Upon activating the LTNE formulation, the solver generates a spatially coincident virtual solid zone to calculate solid-phase conduction. Because this solid zone possesses an independent temperature field ($T_{s,zone 2}$) distinct from the fluid temperature ($T_{f,zone 2}$), new boundary definitions are mathematically required. In accordance with the Ansys Fluent User Guide [48], this native boundary conversion dictates that fluid boundaries unsuitable for a solid zone (such as inlets, outlets, and internal flow boundaries) are converted into adiabatic walls within the dual solid zone. Conversely, remaining boundaries (such as walls and symmetry planes) are converted into solid zone boundaries of the identical type, copying the thermal conditions directly from the fluid zone. Based on this mathematical architecture, the LTNE boundary conditions for the specific newly created computational domain of the textile zone are automatically enforced as follows:

- Top surface: Because this is an open internal boundary where airflow freely enters, the newly created overlapping solid boundary is strictly defined as adiabatic.
- Bottom surface: The fixed 35 °C thermal condition of the fluid zone is directly copied to the spatially coincident solid boundary.
- Lateral and symmetry boundaries: The existing thermal boundary constraints (coupled thermal conditions and zero-gradient symmetry) are copied directly to the newly generated solid porous interfaces.

In the previous LTE study [25], the total average heat flux (q) at the bottom surface of the textile was quantified using a single effective thermal conductivity and a unified temperature gradient ($q = (\varepsilon_o k_f + (1 - \varepsilon_o) k_s) \frac{\partial T}{\partial y}$). However, to accurately capture the physics of the present LTNE framework, this singular approach is no longer valid. Instead, the total heat flux must be expressed as the superposition of the independent conductive heat fluxes through both the distinct fluid and solid phases, properly scaled by their respective volume fractions. Utilizing the outer porosity (ε_o) to establish these fractions, the updated total heat flux is formulated as Eq. (20):

$$q = \varepsilon_o k_f \frac{\partial T_f}{\partial y} + (1 - \varepsilon_o) k_s \frac{\partial T_s}{\partial y} \quad (20)$$

The governing equations were discretized and solved using the finite volume method. The solver utilized the Coupled algorithm for velocity-pressure coupling, the PRESTO scheme for pressure discretization, and second-order upwind schemes for the momentum, turbulence, and energy equations. While convergence for the general governing equations was strictly maintained at 10^{-5} , the energy equation required special treatment. Simulating LTNE across distinct coupled zones with high porosity mathematically hinders convergence [49]. Although a residual drop to 10^{-8} is typically recommended for such models, monitoring the solution revealed that this threshold was inadequate for stabilizing the heat flux at the bottom of the hot plate, the most critical parameter of this study. Consequently, to guarantee a fully converged and accurate heat flux reading at this boundary, the energy convergence criterion was

Table 4

Grid element statistics for all fabric samples and the total heat flux variations across successive mesh refinements for the S1 configuration.

Configuration	Grid resolution	Number of elements (S1)	Number of elements (S2)	Number of elements (S3)	Heat flux (S1) (W/m ²)	Change (S1) (%)
Horizontal	Coarse	36,504	44,460	47,532	70.80	–
	Medium	74,580	87,120	95,040	70.92	0.17
	Fine	150,080	176,344	193,228	71.02	0.14
Vertical	Coarse	32,927	36,820	40,502	154.93	–
	Medium	66,600	72,520	79,920	154.21	0.47
	Fine	134,656	146,228	160,956	153.99	0.14

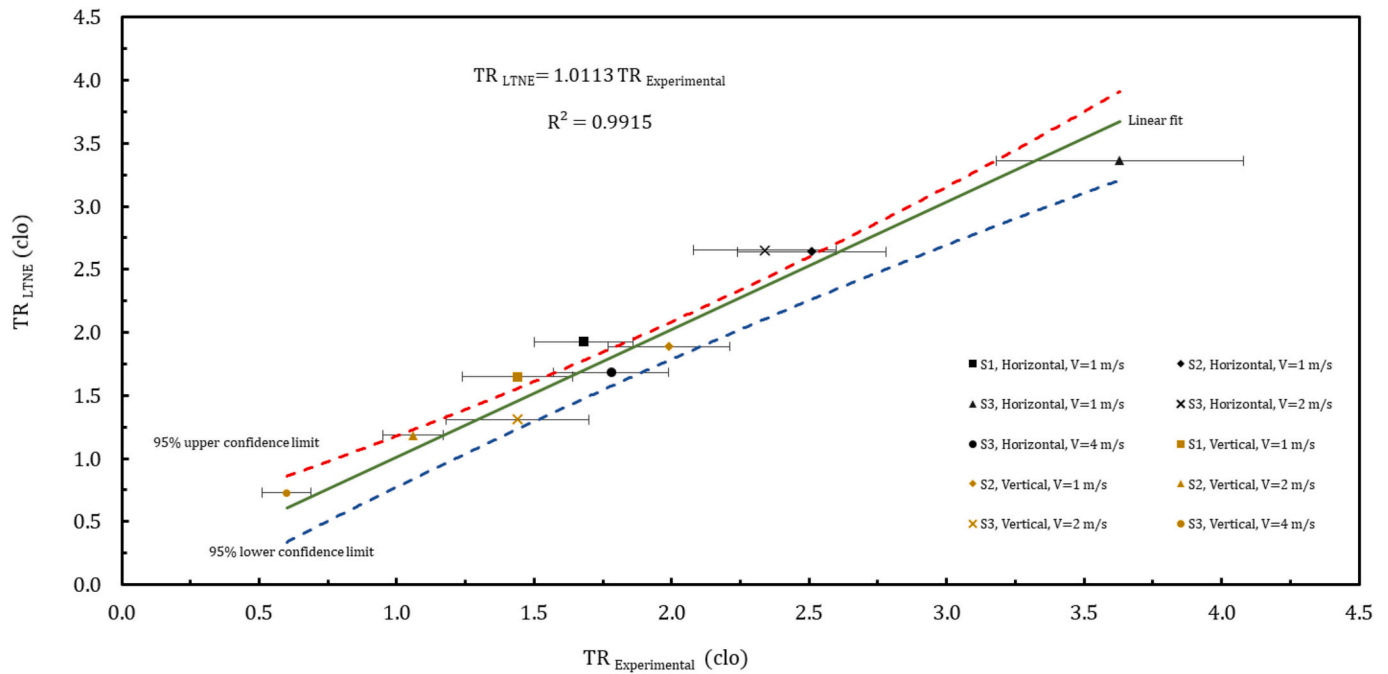


Fig. 3. Comparison of experimental and LTNE thermal resistance for samples S1-S3 under horizontal and vertical wind orientations.

rigorously tightened to 10^{-12} .

Having fully established the governing computational methodology, the following section presents and discusses the numerical results derived from these simulations.

8. Results and discussions

This section begins with a grid independence study to ensure the numerical outcomes are independent of mesh resolution. The computational methodology is then validated against experimental data utilizing a variety of nonwoven samples subjected to different wind velocities and flow orientations. Upon establishing model reliability, the study investigates the effect of fabric thickness on thermal resistance, providing a robust comparison between LTE and LTNE models. Crucially, the current LTE model represents a methodological upgrade over earlier study [25], utilizing a custom UDF to rigorously integrate radiative heat transfer. Furthermore, the distinct thermal contributions of conduction, convection, and radiation are quantified for the thinnest and thickest structures. By testing these extremes across different configurations and velocity limits, the phenomenological differences between the LTE and LTNE assumptions are exposed. Finally, the discussion evaluates the impact of increasing inlet wind velocity to further delineate the behavior of the two thermal models.

8.1. Grid independence study

A grid independence study was performed for all fabric structures (S1–S3) under horizontal and vertical wind orientations, using sample S1 as the representative model. To evaluate the most critical condition, the total heat flux was calculated using Eq. (20) at the highest inlet velocity across coarse, medium, and fine meshes. As detailed in Table 4 for S1, the relative change in total heat flux between the medium and fine grid resolutions was merely 0.14% for both horizontal and vertical cases. These marginal variations confirm that the numerical solution is independent of further spatial refinement.

Identical grid evaluations were also performed for samples S2 and S3. The observed variations in total heat flux between the medium and fine meshes were consistently marginal, remaining below 0.75% (horizontal) and 0.08% (vertical) for S2, and below 2.89% (horizontal) and

0.03% (vertical) for S3. Therefore, the medium grid was utilized for all subsequent simulations to ensure high computational accuracy while maintaining reasonable simulation times.

8.2. Model validation

The numerical LTNE model was validated by comparing simulated overall thermal resistance (TR_{LTNE}) values against experimental measurements ($TR_{Experimental}$) for samples S1, S2, and S3, with all values expressed in clo units ($1 \text{ clo} = 0.155 \text{ m}^2 \cdot \text{K/W}$) [50]. These comparisons encompassed both horizontal and vertical configurations at varying inlet flow velocities (1, 2, and 4 m/s). As illustrated in Fig. 3, the error bars denote the experimental uncertainty, which was calculated according to the methodology detailed in Section 3 (Measurement apparatus). The comparison yielded a highly linear correlation ($TR_{LTNE} = 1.0113 TR_{Experimental}$) with a strong coefficient of determination ($R^2 = 0.9915$), and almost all evaluated data points fell strictly within the 95% confidence limits. Because these minor deviations remain well within the established margin of measurement error, the statistical indicators demonstrate excellent overall agreement, thereby confirming the reliability of the developed LTNE model.

While LTNE model demonstrates excellent overall agreement with the experimental data, the minor remaining deviations can be primarily attributed to structural nonuniformities inherent to the sample manufacturing process. During fabrication, local variations in fiber packing density inevitably occur, leading to fiber accumulation in certain regions and sparse distribution in others. This physical variability alters the local airflow pathways through the textile in ways that a macroscopic, uniform porous-media representation cannot fully capture, in contrast to an explicitly resolved microscale fibrous geometry as a real porous medium. Furthermore, deviations can also be explained by using a 2D computational domain, considering that convective turbulent flow structures are inherently 3D in nature. Despite these inherent experimental and computational factors, the model accurately captures the governing physical phenomena and comprehensively reproduces the experimental trends. These results confirm that the LTNE framework provides a highly reliable and physically consistent prediction of the thermal behavior of nonwoven fabrics under varying wind conditions.

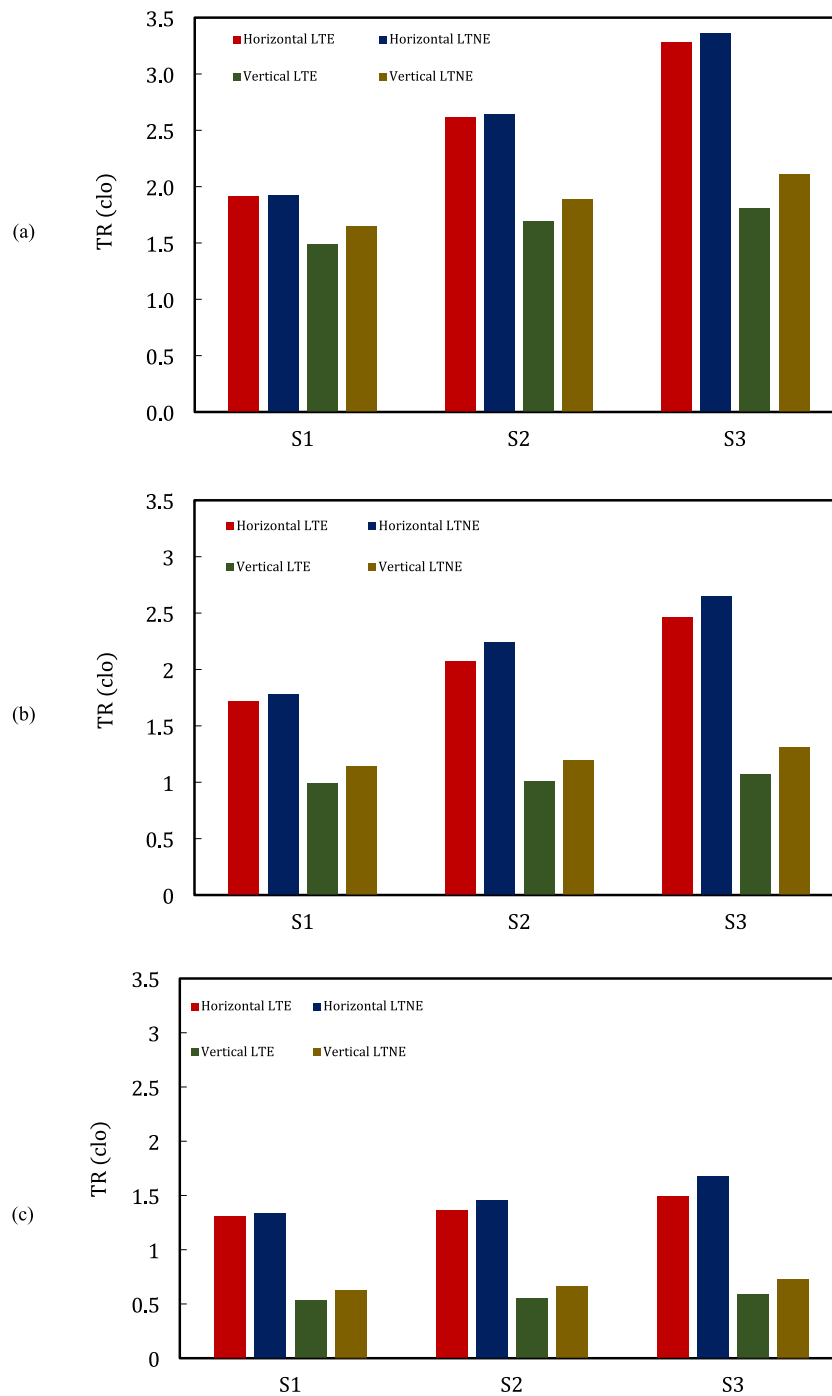


Fig. 4. Thermal resistance of samples S1–S3 comparing LTE and LTNE models under (a) 1 m/s, (b) 2 m/s, and (c) 4 m/s inlet velocities for both horizontal and vertical configurations.

8.3. Effect of fabric thickness on thermal resistance: A comparison of LTE and LTNE models

The influence of fabric thickness on thermal resistance is evaluated using an advanced numerical framework, with results for inlet velocities of 1, 2, and 4 m/s under horizontal and vertical wind orientations illustrated in Fig. 4(a–c). While earlier studies relied on a standard LTE assumption [25], the baseline LTE model is upgraded here by incorporating internal radiative heat transfer via a custom UDF. This enhanced LTE framework is then directly compared against the proposed LTNE methodology.

Both models capture similar macroscopic trends. Thermal resistance

is consistently lower in vertical orientations due to the severe impact of direct wind penetration. Furthermore, non-proportional scaling between fabric thickness and thermal resistance is evident; under a 4 m/s vertical flow, increasing structural thickness by a factor of 2.18 (from sample S1 to S3) yields only modest insulation improvements for both LTE (11.3%) and LTNE (15.9%). This reinforces that under high-velocity conditions, merely adding material bulk is an inefficient strategy for thermal protection. Despite these similarities, a noticeable discrepancy exists: the LTNE model consistently predicts higher thermal resistance. Under mild, conduction-dominated conditions (e.g., horizontal flow at 1 m/s), the deviation between the models remains below 1%, with LTNE predicting 1.92 clo for S1 compared to the LTE prediction of 1.91 clo.

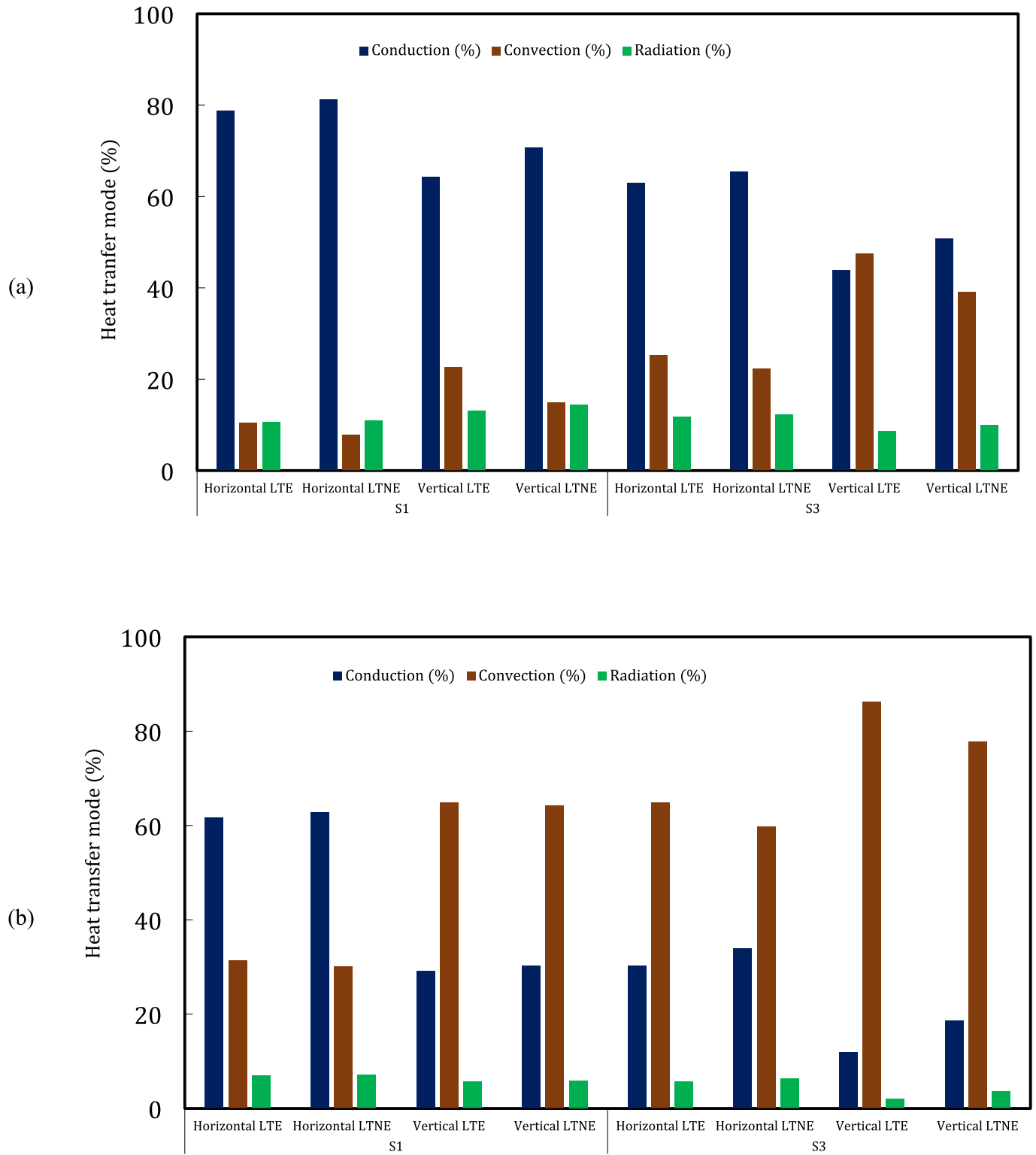


Fig. 5. Relative thermal contributions of conduction, convection, and radiation evaluated via LTE and LTNE models for samples S1 and S3 at inlet velocities of (a) 1 m/s and (b) 4 m/s.

However, as wind velocity increases, this divergence becomes pronounced. At a 4 m/s vertical flow for S3, the gap between the LTNE (0.73 clo) and LTE (0.59 clo) predictions expands to over 23.8%. This expanding divergence highlights the growing thermal lag between solid fibers and the interstitial air. While the LTE assumption provides a valuable, conservative baseline, its premise of instantaneous heat transfer tends to over-predict heat loss under severe flow. Consequently,

LTE results might initially suggest that thickness offers virtually no benefit at high speeds (e.g., S3 at 0.59 clo versus S1 at 0.53 clo). However, by capturing the finite rate of interfacial heat transfer, the LTNE model offers a more nuanced resolution, revealing that the thicker S3 sample (0.73 clo) still provides a moderate thermal benefit over S1 (0.63 clo). Ultimately, while LTE serves as a reasonable approximation for low-velocity scenarios, the LTNE framework becomes necessary for

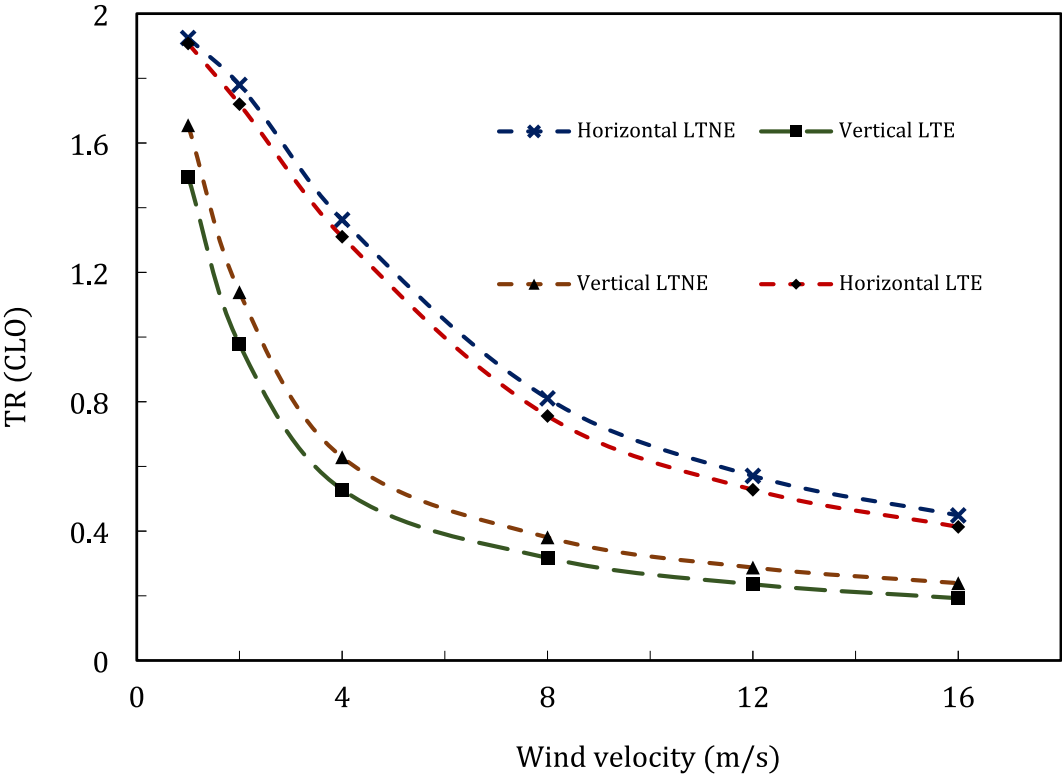


Fig. 6. Comparison of LTE and LTNE thermal resistance predictions for sample S1 across varying wind velocities and flow orientations.

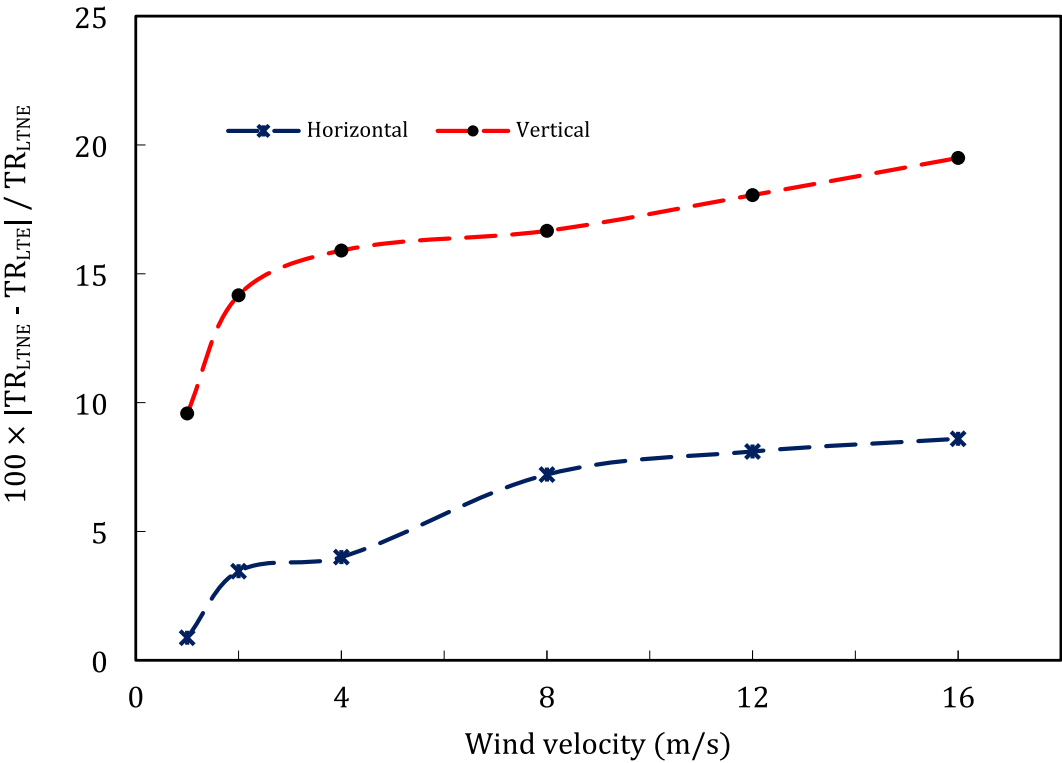


Fig. 7. Difference in predicted thermal resistance between the LTE and LTNE models across varying wind velocities.

high-velocity regimes to prevent the systematic under-prediction of a nonwoven fabric's insulation performance.

8.4. Relative contributions of heat transfer modes across LTE and LTNE frameworks

To systematically isolate the relative contributions of conduction, convection, and radiation, a numerical decoupling procedure from the previous work was applied to both the LTE and LTNE results [25]. Briefly, the convective flux ($q_{\text{convection}}$) is extracted by subtracting the combined conduction-radiation flux ($q_{\text{conduction+radiation}}$), obtained via a zero-internal-velocity simulation from the total heat flux (q_{total}).

The results of this partitioning for the thinnest (S1) and thickest (S3) samples at low (1 m/s) and high (4 m/s) wind velocities are presented in Fig. 5 (a-b). At 1 m/s and horizontal configuration in both models (Fig. 5 (a)) where air penetration is minimal, conduction dictates the thermal regime, constituting over 78% of the total flux for S1. Notably, radiation accounts for >11% of the heat transfer, a non-trivial contribution often neglected in similar analyses [18]. As fabric thickness increases to S3, the convective share naturally rises due to enhanced internal air movement. At the higher inlet velocity of 4 m/s (Fig. 5 (b)), forced convection governs the thermal regime. In the most extreme case (sample S3 under vertical flow), both models identify convection as the dominant mode. However, a critical trend is evident: the LTNE model consistently attributes a smaller fraction of the total heat flux to convection (77.7%) and a larger fraction to conduction (18.6%) compared to the LTE model (86.2% convection and 11.8% conduction). This systematic trade-off is physically significant. By enforcing $T_s = T_f$, the LTE approach assumes an infinite interfacial heat transfer rate, resulting in zero thermal resistance between the fibers and the air, which consequently overestimates convective heat loss. Conversely, the LTNE model accounts for the finite thermal resistance between the hotter solid fibers and the cooler, high-velocity air. By resolving this thermal lag ($T_s > T_f$), the LTNE model correctly moderates the convective component, meaning conduction through the solid fiber network must inherently carry a larger proportion of the thermal load. Because radiation diminishes to a value lower than 4% at these high wind speeds, accurately partitioning the conduction and convection mechanisms becomes paramount. Ultimately, while the LTE model suffices for conduction-dominated scenarios, the LTNE approach becomes needed to prevent the systematic overestimation of convective heat loss under high-wind conditions.

8.5. Comparative analysis of LTE and LTNE predictions for the thinnest structure (S1) under varying wind velocities

To evaluate the influence of the LTE assumption under high-velocity airflow conditions, the thermal resistance of the thinnest structure (S1) was analyzed across varying wind speeds. As illustrated in Fig. 6, predictions from the LTE and LTNE models are directly compared for both horizontal and vertical flow orientations.

Consistent with previously reported experimental observations [16,18,31], both models successfully capture the previously reported nonlinear decline in thermal resistance. Initially, at velocities below 4 m/s, air penetrating the porous network triggers forced convection, causing a sharp drop in insulation. As velocity increases and convection dictates the thermal regime, the resistance reaches a steady minimum. This stable trend confirms that beyond a critical velocity threshold, further increases in wind speed yield only marginal degradation of the fabric's thermal protection.

However, a closer analysis reveals a divergence between the LTE and LTNE predictions that is highly dependent on flow orientation, with Fig. 7 detailing the difference in thermal resistance at each velocity. This discrepancy is directly linked to the fluid residence time (L/u_p), where L is the characteristic length or thickness of the fabric [51]. Representing the average time an air parcel spends within the nonwoven, a longer

residence time facilitates a more complete thermal exchange between the solid and fluid phases, naturally driving the system closer to the equilibrium conditions assumed by the LTE model. In the horizontal configuration, limited airflow penetration keeps internal velocities low, extending this residence time. The permeating air is thus able to thermally equilibrate with the solid fibers, yielding strong model agreement. The LTE approach under-predicts thermal resistance by only 8.6% at 16 m/s and less than 4% at 4 m/s, confirming it as a robust and efficient approximation for horizontal orientations across varying wind speeds. In contrast, the vertical configuration promotes greater air penetration, which elevates internal velocities and drastically shortens fluid residence times. The air is advected through the textile too quickly for complete thermal equilibration, creating a substantial thermal gradient. This physical reality causes an immediate divergence between the models: even at a low velocity of 1 m/s, the predictions differ by 9.6%, a gap that rapidly expands to nearly 19.5% at 16 m/s. Incapable of capturing this thermal lag, the LTE model systematically overestimates convective heat loss and under-predicts the true thermal resistance. Ultimately, this comparative analysis confirms that the validity of the LTE assumption is fundamentally regime-dependent. While it serves as a highly efficient approximation for low-convection horizontal flows, it loses predictive capability in vertical orientations, where pronounced non-equilibrium effects make the LTNE framework strictly necessary to resolve the actual thermal physics.

9. Conclusion

This study evaluated nonwoven thermal insulation by examining the limits of the local thermal LTE assumption under varying wind intensities. By comparing LTE and LTNE predictions across different fabric thicknesses, wind velocities, and flow orientations, the research highlighted the regime-dependent nature of interfacial heat transfer. The results confirm that accurately capturing non-equilibrium effects is essential for optimizing textile design. The primary methodological developments and physical findings are summarized as follows:

- Numerical framework and validation: A 2D porous-media LTNE model was developed to resolve external turbulent and internal laminar flows. The model incorporated fiber hollowness and multi-component composition, utilizing two UDFs to evaluate interfacial convection and radiative thermal conductivity. Validation against experimental thermal resistance demonstrated high accuracy across varying flow orientations and wind speeds, yielding a strong linear correlation ($TR_{\text{LTNE}} = 1.0113 TR_{\text{Experimental}}$ and $R^2 = 0.9915$).
- Influence of thickness: Vertical flow degrades insulation, and increasing thickness yields diminishing returns at high speeds; a 2.18-fold thickness increase (S1 to S3) under 4 m/s vertical flow provides 11.3% (LTE) and 15.9% (LTNE) improvements. While predictions match within 1% under horizontal flow, they diverge by 23.8% under 4 m/s vertical flow for the thickest sample. Because the LTE model ignores thermal lag, it over-predicts heat loss (LTE S3: 0.59 vs. S1: 0.53). The LTNE model resolves this lag, showing that the thicker S3 (0.73) maintains a moderate but valuable thermal benefit over S1 (0.63) in high-velocity regimes.
- Heat transfer mechanisms: Conduction dominates under 1 m/s horizontal airflow (>78% of total flux for S1), with radiation contributing 11.8%. Under a 4 m/s vertical flow, convection governs heat transfer while radiation drops below 5%. The LTE model overestimates this convective loss (86.2% for S3). By resolving the thermal lag, the LTNE model moderates the convective fraction to 77.7%, shifting a larger, physically accurate proportion of the thermal load to conduction (18.6%) and better partitioning the heat fluxes.
- Velocity dependence and flow orientation: Thermal resistance decreases nonlinearly with wind speed, and model accuracy depends on fluid residence time. For S1, in horizontal flows, extended residence times permit thermal equilibrium, making the LTE assumption

a suitable approximation (8.6% up to 16 m/s). Conversely, vertical flows shorten residence times and induce a greater thermal gradient. The LTE model under-predicts thermal resistance by up to 19.5% at 16 m/s, confirming the LTNE framework is essential for vertical, high-velocity regimes.

CRediT authorship contribution statement

Seyyed Mohsen Mortazavinejad: Writing – original draft, Validation, Software, Methodology, Investigation, Data curation. **Ludwig Vinches:** Writing – review & editing, Supervision, Project administration, Investigation, Funding acquisition. **Stéphane Hallé:** Writing – review & editing, Supervision, Investigation, Conceptualization.

Declaration of generative AI and AI-assisted technologies in the writing process

During the preparation of this work, the authors used Gemini 3.1Pro to improve readability and language. After using this tool, the authors reviewed and edited the content as needed and take full responsibility for the content of the publication.

Declaration of competing interest

The authors declare that they have no known competing financial interests or personal relationships that could have appeared to influence the work reported in this paper.

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Data availability

Data will be made available on request.

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